
Aero-propulsive Performance Assessment Approach to Boundary Layer Ingestion Aircraft



Nicolas G.M. Moirou

Centre for Propulsion and Thermal Power Engineering
School of Aerospace, Transport and Manufacturing
Cranfield University

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Supervisors: Prof P. Laskaridis and Dr D.S. Sanders

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Abstract

A promising solution towards more sustainable and efficient aircraft propulsion relies upon the ingestion of the boundary layer flow that develops around the airframe. Amongst the plethora of concepts, the propulsive fuselage concept appears to be the most pragmatic configuration, as a direct adoption of conventional tube-and-wing aircraft, which has an additional propulsor integrated around its tail. Nonetheless, there is a lack of consensus in the quantification and interpretation of the performance of such vehicles. Long-established momentum-based bookkeeping schemes break down as their underlying assumptions do not hold true in highly-integrated airframe-propulsion systems. Alternative approaches have been brought forth by considering holistically the aircraft to evaluate its performance and decompose its aerodynamic forces. Notably, energy- and exergy-based approaches improve one's understanding on the cause and effect of boundary layer ingestion mechanisms but require high computational demands with dense grids. In sought of a universal approach, energy- and momentum-based methods are used together in this work to quantify the coupled aerodynamic performance of boundary layer ingestion aircraft. The strengths of *near-field* momentum integrations are coupled with more informative energy-based flow assessments. The design space of a propulsive fuselage aircraft is explored via CFD after a reduction of its modelling to an axi-symmetric partial assembly of the fuselage and propulsor. With variations in the thruster position along the tail, its flow passage through the fan and pressure rise, and exhaust design, best performance is achieved with a concept where the propulsor lies at 90% of the fuselage chord, for a fan hub radius of 30% of the fuselage radius, that ingests around 43% of the boundary layer mass-flow, and applies a pressure rise of 1.29, to generate around a third of the total propulsive force requirement whilst savings 11% of fuel relative to a short-to-medium range aircraft propelled by state-of-the-art turbofans. The reasons for such savings are detailed with a first-of-its-kind fully energetic flow decomposition which aims at attributing boundary layer ingestion benefits to changes in propulsor design.

Keywords

boundary layer ingestion, aircraft concepts, aerodynamics, propulsion integration, computational fluid dynamics

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Nomenclature

Abbreviations

AD	Actuator Disc
AIP	Aerodynamics Interface Plane
ARF	Absolute Reference Frame
BC	Boundary Condition
BFM	Body-Force Model
BL	Boundary Layer
BLI	Boundary Layer Ingestion
BPR	By-Pass Ratio
BWB	Blended-Wing Body
CDI	Circumferential Distortion Index
CFD	Computational Fluid Dynamics
CV	Control Volume
DPC	Disruptive Propulsive Concept
EIS	Entry-Into-Service
ERC	Exergy Recovery Coefficient
ESC	Exergy Saving Coefficient
EWB	Exergy Waste Coefficient
FPR	Fan Pressure Ratio
FSC	Fuel Saving Coefficient
HWB	Hybrid-Wing Body
IPR	Intake Pressure Recovery
LPT	Low-Pressure Turbine

NPF	Net Propulsive Force
NVF	Net Vehicle Force
ORFEC	Over- and Rear-Fuselage Engines Concept
PER	Potential for Energy Recovery
PFC	Propulsive Fuselage Concept
PFS	Propulsive Force Share
PIV	Particle Image Velocimetry
PSC	Power Saving Coefficient
RDI	Radial Distortion Index
RRF	Relative Reference Frame
SFC	Specific Fuel Consumption
SMR	Short-to-Medium Range
TeDP	Turbo-electric Distributed Propulsion
TS	Thrust-Split
TSFC	Thrust-Specific Fuel Consumption

Greek Symbols

α	Hade angle, °
β_{boat}	Boat-tail angle, °
β_{plug}	Plug-cone angle, °
δ	Boundary layer thickness, m
Δ	Boundary layer area, m ²
Δ^*	Boundary layer mass displacement area, m ²
$\Delta_{nac,TE}$	Nacelle trailing edge radial offset coefficient, -
ζ	Shock wave identifier, -
η	Efficiency, -
θ	Force in the thrust domain, N
Θ	Rate of volumetric pressure work, J.s ⁻¹
Θ^*	Boundary layer kinetic energy area, m ²

κ	Propulsor efficacy, -
μ	Dynamic viscosity, Pa.s
ξ	Shear-layer identifier, -
ρ	Density, kg.m ⁻³
$\bar{\tau}$	Viscous stress tensor, Pa
Υ	Jet-plume identifier, -
ϕ	Force in the drag domain, N
Φ	Rate of volumetric viscous dissipation, J.s ⁻¹
ψ_{slope}	Tail slope angle, °
Ψ	Surfacic force, N
Ω	Flux force, N

Roman Symbols

$\dot{\mathcal{A}}_{tot}$	Rate of total anergy generation, J.s ⁻¹
b	Local width parameter, -
$\mathcal{D}$	Drag, N
e	Mass-specific internal energy, J.kg ⁻¹
$\dot{\mathcal{E}}_m$	Rate of mechanical energy, J.s ⁻¹
$\dot{\mathcal{E}}_p, \dot{E}_p$	Rate of pressure-defect energy, J.s ⁻¹
$\dot{\mathcal{E}}_p$	Rate of energy from the propulsor, J.s ⁻¹
$\dot{\mathcal{E}}_\tau$	Rate of viscous boundary-work, J.s ⁻¹
$\dot{\mathcal{E}}_{th}$	Rate of thermal exergy, J.s ⁻¹
$\dot{\mathcal{E}}_u, \dot{E}_u$	Rate of stream-wise mechanical energy, J.s ⁻¹
$\dot{\mathcal{E}}_v, \dot{E}_v$	Rate of transverse mechanical energy, J.s ⁻¹
$\mathbf{f}$	Volume-specific body force vector, N.m ⁻³
$\mathcal{F}$	Force, N
f_{plug}	Non-dimensional plug angle, $f_{plug} = \beta_{plug}/\beta_{boat}$, -
$\dot{h}$	Rate of change in altitude, m.s ⁻¹
H	Total enthalpy, J

$\dot{I}$	Momentum flux force, N
L	Length, m
L/D	Length-over-diameter ratio, -
$L/\mathcal{D}$	Lift-to-drag ratio, -
$\dot{m}$	Mass-flow rate, kg.s^{-1}
$\hat{\mathbf{n}}$	Unit normal vector, -
p	Static pressure, Pa
P	Total pressure, Pa
$\mathcal{P}_K$	Mechanical energy inflow rate, J.s^{-1}
$\mathcal{P}_S$	Net propulsor shaft power, J.s^{-1}
$\mathcal{P}_V$	Rate of volumetric pressure work, J.s^{-1}
q	Dynamic head, Pa
$\mathbf{q}$	Heat conduction flux vector, $\text{J.m}^{-2}.\text{s}^{-1}$
$\dot{q}$	Volumetric heat addition/removal, $\text{J.m}^{-3}.\text{s}^{-1}$
R	Radius, m
$\mathcal{R}$	Range, m
$\mathcal{S}$	Control surface, -
$\mathcal{T}$	Thrust, N
$\mathbf{V}'$	Velocity vector in the relative reference frame, m.s^{-1}
$\mathbf{V}$	Velocity vector in the absolute reference frame, m.s^{-1}
$\mathcal{V}$	Control volume, -
W	Weight, N

Sub-/Super-scripts

∞	Quantity at free-stream
$(\bar{})$	Area-averaged quantity
$(\tilde{})$	Ideally expanded quantity
A/C	Quantity on aircraft
B	Quantity on body surface

BP	Quantity on by-pass
e	Quantity on boundary layer edge
f	Fuel quantity
FE	Quantity at fan-exit
FF	Quantity at fan-face
FS	Quantity on free-stream surface
G	Gauge/Gross quantity
<i>hub</i>	Quantity at the hub
int, std, ov	Intrinsic, standard and overall quantities
N	Net quantity
nac	Quantity on nacelle
NZ	Quantity at nozzle
P	Quantity on propulsor
post	Quantity on post-exit stream-tube
pre	Quantity on pre-entry stream-tube
ref	Reference quantity
SC	Quantity on side cylinder
TE	Quantity at trailing-edge
<i>tip</i>	Quantity at the tip
TP	Quantity on Trefftz plane

1

Introduction

1.1 Environmental Impact of Aviation

With the worldwide population growth and average living standard improvements, the humankind activity never ceased to impact the global warming since 1900 [1]. As a consequence of the industrial revolution, greenhouse gas emissions rapidly increased contributing to temperature anomalies amongst other phenomena. Recently raising the public awareness, it is evident that human behaviour changes need to go in pair with technology advancements to facilitate a sustainable growth and halt further global warming.

The aviation industry is contributory to these emissions of carbon dioxide with a share of 2.5% (Fig. 1.1), whereas the transportation of people and goods amounts to around a quarter of the total emissions [2]. Whilst both passenger numbers and passenger-kilometres almost constantly rose since the democratisation of commercial aviation, yearly rise expectations of 4 to 5% are reported, with the Asia-Pacific region leading the air transport traffic growth [3]. This demand growth affects airlines that need to increase their fleet to meet the demand. For instance, Airbus predicts that the current fleet in service will more than double by 2038 whilst only retaining a third of the aircraft already in service [3].

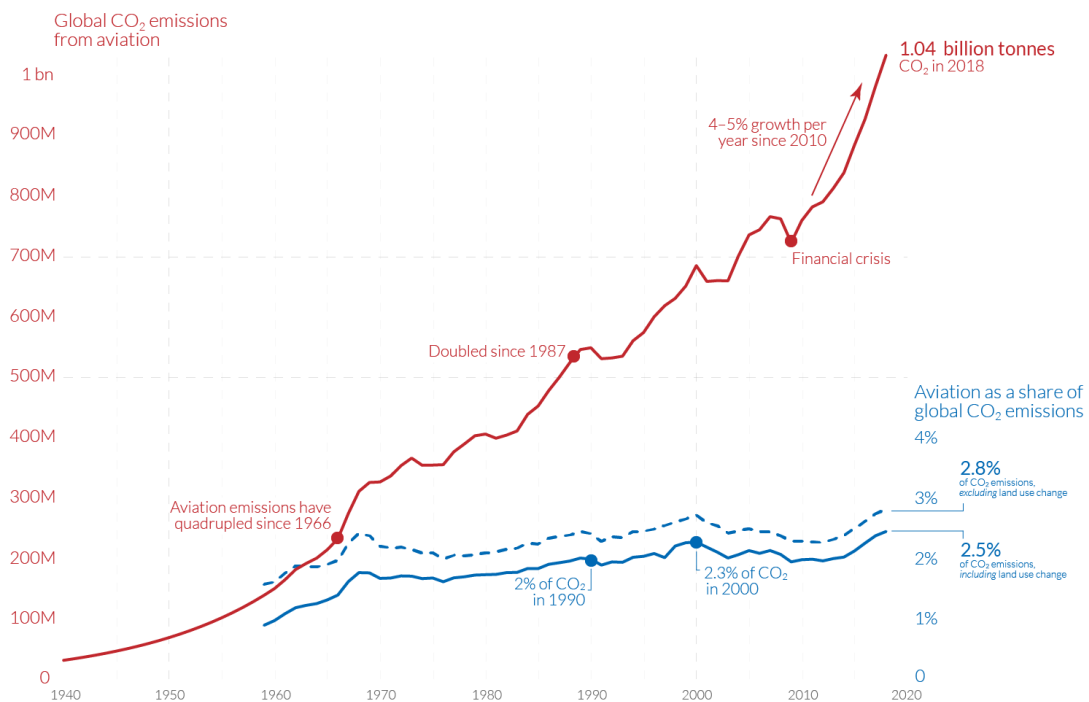


Figure 1.1: Growth of aviation emissions, in absolute (ref) and relative (blue) values from 1940. Figure retrieved from Ref. [4], and data from Ref. [2, 5]

To guarantee sustainable air transportation, stringent regulations have emerged from different parties. The European Commission, National Aeronautics and Space Administration (NASA) and the European Union Aviation Safety Agency (EASA) have set different targets to drastically reduce the fuel consumption and emissions whilst enhancing acoustic performance [6–11]. Notably, reductions of 75% in carbon dioxide (CO_2) emissions relative to 2000 levels are targeted, primarily through fuel consumption reductions.

1.2 Rationale of Boundary Layer Ingestion

To date, a means of reducing aero-engines fuel consumption is through improvements in propulsive efficiency (i.e. measure of the useful work done in propelling the aircraft versus the total mechanical energy imparted to the flow in doing so). This quantity therefore depends on the vehicle speed and can be improved by reducing the mass-specific thrust via an increase in the turbofan by-pass ratio (BPR) [12]. Recently developed turbofans offer BPR of around 9 to 12 by enlarging the fans, and unavoidably increasing the engine and aircraft weight. Consequently, larger nacelles incur external aerodynamic penalties due to larger wetted areas and interference with wings. It is therefore evident that current technologies are reaching their limits in propulsive efficiency [13].

To prevent these aerodynamic penalties, open rotors, or propfans, appear as promising solutions by eliminating the fan cowl (Fig. 1.2) [12]. Nonetheless their major drawback is the noise generation experienced both by passengers seated in the cabin, and around airports and cities (as there are no cowls with acoustic liners to shield the noise) [14].

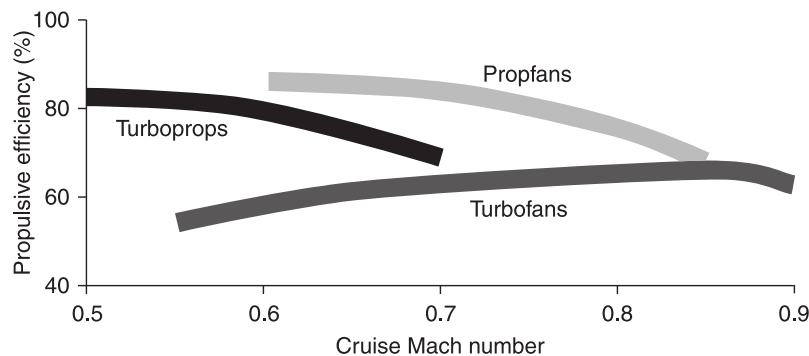


Figure 1.2: Comparison of propulsive efficiency from different engines, function of the cruise Mach number, retrieved from [12]

Alternatively, an equivalent BPR (eBPR) is reproducible with a distributed array of engines or propulsors. The main advantage of this configuration is to conserve an equivalent mass-flow rate through the fans whilst reducing the aircraft weight [15]. Depending on the position at which this array is installed, synergistic fuel savings are claimable through an increase in airframe aerodynamic efficiency.

In that context, and to achieve the ambitious objectives of fuel and emission reduction, it is notwithstanding that revolutionary aircraft concepts are necessary, whereby the pursuit of innovative designs and technologies is now required. More efficient systems rely on closer integration levels of the propulsion system within the airframe. In doing so, natural fuel savings are achieved by harvesting part of the energy present in the close-to-the-wall flow, also described as boundary layer (BL) [16]. These aircraft concepts utilise boundary layer ingestion (BLI) by capitalising on synergies between the airframe and propulsion system in a more holistic aircraft design approach to fully exploit the aerodynamic benefits of closer integration. Originating in the marine sector with submersible vehicles, a propeller is integrated to take advantages of the flow travelling around the body [17, 18], hence making that technology also applicable to airborne systems.

1.2.1 Emergence of Efficient Aero-propulsive Systems

In recently proposed aircraft concepts, higher levels of integration enable the embedding of the propulsion systems within the airframe and the ingestion of part of the boundary layer flow developing around the fuselage. The consequence of this greater aerodynamic coupling is the impact increase of the propulsion system integration on the aerodynamic performance, relative to conventional aircraft. In doing so, some concepts integrate propulsors with individual cowls whilst other favour a distributed array, with either a single or multiple cowls.

In the former, aft-mounted propulsion systems trade off their under-wing turbofans for mounted-flush aero-engines (e.g. MIT D8 [19] in Fig. 1.3a), or on either side of a lifting fuselage (e.g. ONERA NOVA [20] in Fig. 1.3b). Alternatively, propulsive fuselage concepts (PFC) such as the NASA STARC-ABL [21] in Fig. 1.3c, and Bauhaus Luftfahrt CENTRELINE [22] and DisPURSAL [23] integrate a fully azimuthal propulsor around the tail of a conventional aircraft to complement the propulsive force production from the under-wing turbofans.



Figure 1.3: Novel aircraft configurations based on boundary layer ingestion

In the latter, arrays of propulsors are either integrated under or above the wings similar to the ONERA DRAGON [24] in Fig. 1.3e, or in more disruptive conceptual aircraft above the top-surface of a hybrid- or blended-wing body (BWB) like the NASA N3-X [15] in Fig. 1.3f, or Airbus ZEROe project¹.

Whilst these concepts have been reviewed in Ref. [25], there are other efficient aero-propulsive concepts under development favouring synergistic benefits from boundary layer ingestion and distributed propulsion (Fig. 1.3d) [26], or propellers over propulsors² [27–29].

1.2.2 Opportunities and Challenges

Amongst all conceptual designs previously mentioned, propulsive fuselage concepts appear as the most pragmatic to potentially meet the environmental goals of 2035. The concept relies on the adoption of conventional tube-and-wing aircraft around which a boundary layer ingestion propulsor³ encapsulates the tail.

¹Airbus MAVERIC

²Airbus ZEROe “Pod”

³In this work, a propulsor refers to a fan within a cowl.

The fully azimuthal nature of the propulsor provides a *simple* installation around current aeroplanes towards near-term objectives. The attractive opportunity of ingesting the circumferential flow also contributes to the attenuation of further viscous dissipation within the aircraft wake (Fig. 1.4), and of unsteady fan's blade loading as a result of flow distortion. Additionally, following some assumptions, the aircraft modelling can be simplified and studied axi-symmetrically [31, 33, 34], and its comparison with state-of-the-art aircraft enables its performance evaluation (via its fuel savings).

Historically, and conventionally, weak aerodynamic coupling between podded engines and the airframe allowed one to isolate aero-engines for their design and propulsive performance evaluation [35, 36]. Later, the tighter integration under the wings led to revised approaches and performance evaluations [37, 38]. However, these approaches become problematic when considering higher degrees of integration where the underlying assumptions of these conventions become invalid, and require alternative approaches that consider holistically the system of study.

Long-established bookkeeping schemes require the break-up of the airframe (drag) and propulsion system (thrust) to gain a better understanding of the aerodynamics. However, these conventions [35, 36] are invalid for BLI as they are based on certain assumptions to define drag as the resulting force between the forces acting on a body versus those in a potential flow, and thrust as a surrogate of the useful work generated by the propulsion system. Whilst valid with infinite (or semi-infinite) stream-tubes, the assumption related to thin BL relative to the entire mass-flow captured does not hold true as its impact on the shape of the stream-tubes affects the experienced forces.

Instead, a favoured approach considers the system holistically whereby a balance between power sources and sinks aids evaluating the overall performance of the combined airframe-propulsion system [34, 39]. Taking for example the stream-wise velocity profile from Fig. 1.4, steady-level flight is maintained in the reference and novel aircraft scenarios, where greater attenuation is achieved by the latter. It becomes evident that by re-energising the boundary layer flow (or a portion thereof), the jet stream velocity compensates less to maintain the flight condition and therefore, less energy is present in the flow to dissipate as it tries to resume to its initial state⁴.

⁴This holistic approach helps understand BLI rather than considering a system-level perspective of reduced ram drag.

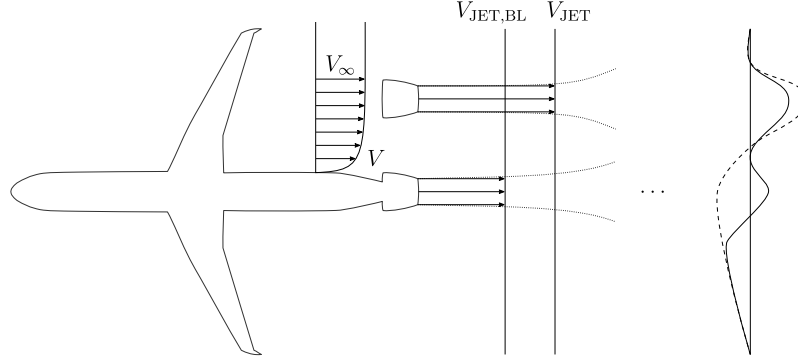


Figure 1.4: Boundary layer ingestion benefits from propulsive efficiency improvements (i.e. ram drag reduction), or specific thrust reduction between the turbofan and thruster, or by analogy from wake kinetic energy attenuation between a conventional aircraft propelled by under-wing turbofans (---) and a novel aircraft with re-scaled turbofans and a thruster (—), relative to a clean flow (bottom half)

The assessment of these different losses in the system from an absolute reference frame (ARF) standpoint enables an improved causality of the BLI mechanisms [34, 40]. Unlike the relative reference frame (RRF) that echoes experimental testing conditions, the ARF considers the aircraft as the moving body through an initially quiescent environment, thus imparting momentum and energy to its surroundings. With that perspective, the energy present in the boundary layer is harvested by the BLI fan, rather than filling a defect of momentum in the flow [41]. On this basis, an improved physical interpretation of the flow mechanisms is enabled and their flow-paths traceable [42].

Nonetheless, in addition to the challenges related to the bookkeeping schemes, the propulsive efficiency, as used in conventional propulsion systems, also becomes ill-defined and introduces a power saving coefficient (PSC) aiming at quantifying the relative performance of a vehicle [43]. In their study, Smith Jr. demonstrated significant power savings originating from the filling of the BL momentum deficit at a lower fuel consumption whilst attenuating the aircraft's wake [43]. Initially studied as wake-filling, the propulsion system was hypothetically placed further down the aircraft to evaluate the potential savings. More practically but with ideal scenarios, these benefits are also studied along a conical tail of a conventional aircraft where greater savings are claimed at the vehicle trailing edge [42].

Lots of recent philosophical thoughts around BLI led to energy-based formulations [39, 44], and their development [34, 45] over momentum-based methods because of their claimed advantages. This has been thoroughly tested out on simple topologies, such as flat plates and bodies of revolution where different levels of attenuation were

performed with actuator discs [42, 46]. Conversely, complex topologies and aircraft designs have been looked at but with rather rudimentary accountings [47, 48].

1.3 Research Aims

The motivation of this work is therefore driven by the opportunities of bridging the gap between theoretical developments and their application, and bringing forth energy-based understanding within the design space exploration of complex topologies of propulsive fuselage concepts. To that end, it is intended to evaluate the boundary layer ingestion mechanisms, the propulsor integration effects, and the benefits at cruise. The aim of this research is to:

Assess the vehicle aero-propulsive performance using a hybrid momentum- and energy-based approach through the exploration of the design space of propulsive fuselage concepts.

In other terms, the research question to be answered is:

What are the fundamental flow mechanisms and how do the aircraft design variables influence the aero-propulsive performance of a propulsive fuselage aircraft?

To achieve this aim, the objectives are to:

1. Critically review the state of the art of boundary layer ingestion propulsion to inform the selection of models and methods for further research;
2. Develop a framework for the evaluation of the aero-propulsive performance of aircraft configurations based on a hybrid momentum- and energy method;
3. Identify and understand the impact of fuselage shapes on boundary layer developments;
4. Identify the aft-end propulsion system's design space and key design variables to develop an appropriate parametrisation methodology;
5. Explore partially the thruster's design space, and identify the sources of benefits to relate them to integration and design effects;
6. Evaluate the aero-propulsive performance of a propulsive fuselage aircraft along its cruise segment.

1.4 Scope and Research Approach

1.4.1 Scope

To meet the aforementioned aim and objectives, a propulsive fuselage aircraft is studied numerically with the help of CFD RANS along the cruise segment of a short-to-medium haul flight. From the conceptual design of the novel aircraft, assumptions are made on the weak aerodynamic coupling between the fuselage and thruster assembly and the rest of the airframe to enable the simplification of the investigations with axis-symmetric studies.

This work is undertaken as part of a Clean Sky 2 project entitled “Supporting Understanding of Boundary Layer Ingestion Model Experiments” (SUBLIME) which received funding from the European Union’s Horizon 2020 research and innovation program under Grant Agreement No. 864803. This work provides advancements in the state of the art of fully circumferential aft-mounted propulsors, as well as the development of an aero-propulsive performance evaluation approach to be use for numerical and experimental quantifications. Whilst this thesis discusses part of the results obtained from CFD within the project, they helped to relate the predicted

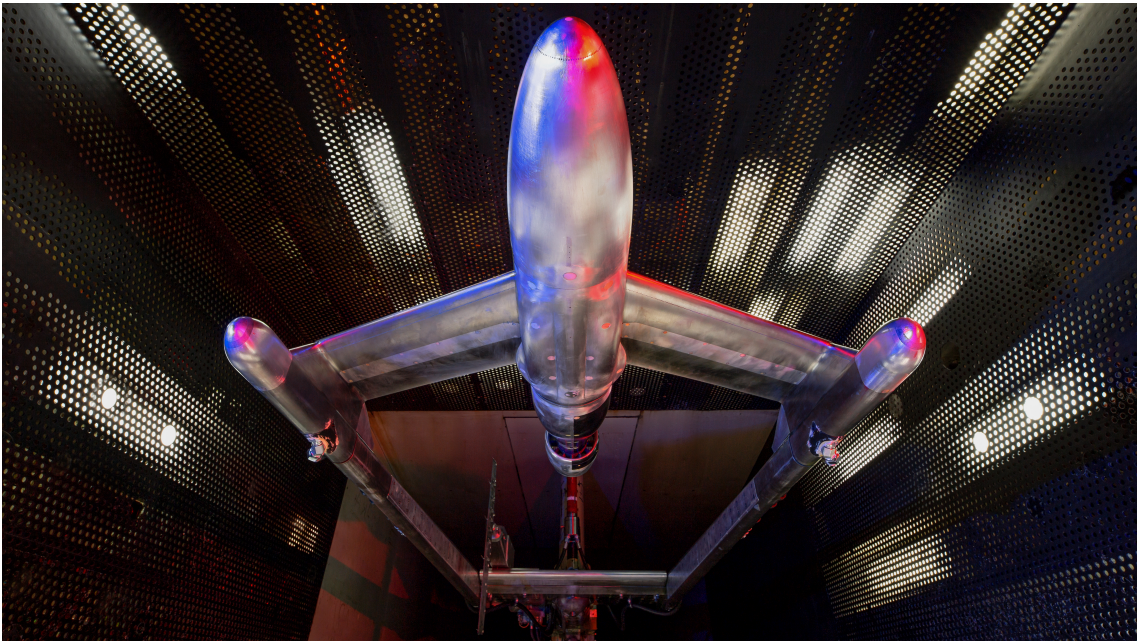


Figure 1.5: Wind-tunnel model of the boundary layer ingestion aircraft studied in the SUBLIME project, courtesy of Aircraft Research Association

performance at in-flight conditions to the wind-tunnel experiments of a sub-scaled model, given in Fig. 1.5, performed by the project partners.

1.4.2 Research Approach

The research initiated with a thorough review of the advancements in the state of the art of boundary layer ingestion propulsion concepts. The plethora of studies on tools and methods development, analytical, numerical and experimental investigations of different BLI aircraft helped shape the prospects in the field and guide the approach to be taken in this thesis.

In the context of the first objective of this thesis, the recommendations made on the directions in further studies are followed by the author to outline the research direction. Notably, the down-selection of the propulsive fuselage concept as case study was made because of its potential to meet near-term objectives in consumption and emission reduction, and its direct adoption of conventional aircraft.

To enable the aerodynamic and propulsive investigations of this concept, the strengths of existing book-keeping schemes gathered in the context of the first objective help to work towards the second objective of this thesis. In the scope of developing a framework to evaluate the aero-propulsive performance of BLI aircraft concepts, a hybrid momentum- and energy-based approach is developed.

Working towards the integration of a propulsor at the back of a tube-and-wing aircraft, simplifications on its modelling are made to perform CFD investigations. As such, and towards the third objective, the fuselage is isolated from the rest of the aircraft components. The flow response to changes in the fuselage shape is studied to identify key design variables before the propulsor integration. On the latter, the design space is identified to define an adequate parametrisation, with respect to the fourth objective, and enable the design space exploration towards the fifth objective.

During the design space exploration of the thruster position and shape, sources of benefits are identified. In the context of the sixth objective, the aero-propulsive performance of the concept is evaluated on all designs explored. The flow-field investigation is related to the vehicle performance to address the main aim and research question, and to inform the direction of further studies.

1.4.3 Dissertation Outline

This dissertation contains six chapters, each being related as schematically shown in Fig. 1.6 and in accordance to the different objectives and aim. The present chapter introduces the context of this thesis, the research aim and objectives, the approach followed to address them and the contribution.

Chapter 2 is directly related to Objective 1 and reviews the advancements in boundary layer ingestion, with the different bookkeeping schemes, figures of merit and their numerical and experimental models. The different conceptual aircraft and their plethora of studies are summarised and critiqued to identify grey areas that need further investigations.

Chapter 3 develops a all-in-one modular framework that designs propulsive fuselage aircraft, evaluates the aero-propulsive performance of the designs and performs a phenomenological energetic analysis of the flow-field. It answers Objective 2 and addresses part of Objective 4. In that context, the down-selection of a BLI aircraft leads to its numerical parametrisation for CFD studies. The hybrid approach to the vehicle performance evaluation comes from the strengths of the existing bookkeeping schemes identified in Chapter 2 and the lack of a general consensus.

Chapter 4 is split in two parts to address Objectives 3, 4, 5 and 6. The former focuses on the aerodynamics of bare fuselage, and the potential for energy recovery by mechanical devices (Objective 3). The flow response to design changes in the fuselage nose and tail is studied to advise on factors responsible for greater potential recoveries of energy. The second part assists in the definition and parametrisation of the aircraft model (Objective 4), and emphasises on an initial design space exploration of the propulsor position along the aircraft tail, its size and fan operating condition (Objective 5) whilst also looking at the benefits of compact nacelles and the auxiliary savings resulting from under-wing turbofans rescaling (Objective 6).

Chapter 5 focuses on an energetic analysis of the flow-field by exploring the design space of the propulsor's exhaust system. The Chapter directly applies the methodologies developed in Chapter 3 whilst answering to Objectives 5 and 6. From the vehicle aero-propulsive performance, the layers are peeled off to assign changes in performance to design variations and flow mechanisms, identified from the different features. Primary and secondary effects of the design parameters are

identified and objective function reductions are suggested for future optimisation studies.

Chapter 6 concludes this dissertation by summarising the key findings and contributions that address the aim of this research, and provide recommendations on steps to develop further from this basis.

This approach aims at addressing all objectives toward the aim previously mentioned, in a different order, which are depicted in Fig. 1.6 between the objectives and thesis chapters.

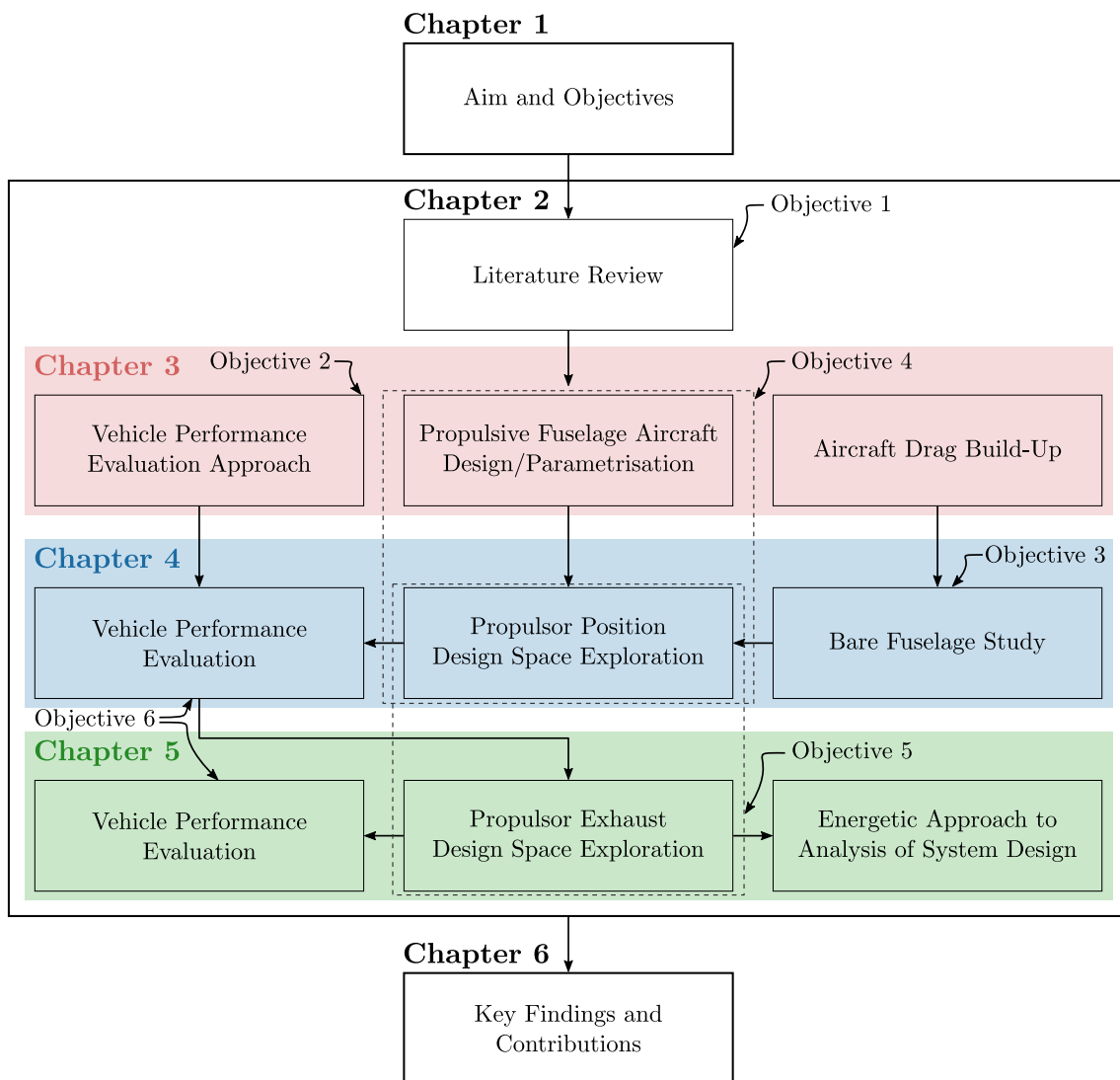


Figure 1.6: Flowchart indicating the connections between chapters and their links to the objectives and research question

1.5 Research Contributions

1.5.1 Knowledge Contribution

There are numerous contributions to knowledge contained in this dissertation. These are detailed in the individual chapters formatted as papers, and listed in Chapter 6, and repeated below for emphasis:

- A thorough and critical literature review on the different bookkeeping schemes, numerical models and methods, experimental progress and conceptual aircraft featuring boundary layer ingestion propulsion. The existing momentum-, energy- and exergy-based accountings are reviewed and compared. The numerical aerodynamic and propulsive models are classified by their degrees of fidelity and compared. The numerous studies on aircraft concepts based on boundary layer ingestion are reviewed, critiqued and mapped out to trace the evolution of the studies refinement.
- A modular framework on the design and aero-propulsive performance evaluation of novel boundary layer ingestion aircraft is developed. In the context of this work, a simplified model of a propulsive fuselage concept is considered whereby a parametrisation is offered. A hybrid momentum- and energy-based aero-performance evaluation approach is presented to assess the aircraft top-level performance at cruise. Within this approach, performance metrics are defined to characterise the propulsive force share of the thruster relative to the turbofans, its efficiency and the fuel savings. A direct contribution from the propulsive force share is that it provides inputs to aero-engine manufacturers for sizing.
- The power balance initially developed by Drela in Ref. [44] is corrected to account for local compressibility effects in the fan stage. Whilst the net pressure-work and kinetic energy inflow rate, or power addition to the flow, is considered in the initial formulation as $\mathcal{P}_K$, it neglects the reversible portion of the pressure-work which can apply work to the fluid. As such, a correction is provided with $\mathcal{P}_K^*$ to isolate the portion of energy present in the flow that can do work, to the one that is already irrecoverably lost in the propulsor domain.

- Efforts towards a definition of the propulsive efficiency with application to boundary layer ingestion propulsion systems are provided. Whilst there are flaws associated with the conventional definition as its value can theoretically exceed 100% with boundary layer ingestion, a challenge still exists in a true definition of propulsive efficiency in these cases despite the numerous efforts [16, 34, 49]. This formulation corrects the power addition term, on the denominator, by considering the previous contribution mentioned.
- A design space exploration of a propulsive fuselage aircraft with an energetic approach. This extends the knowledge on the position, size and shape of aft-end propulsors as previous work using energy methods was done on ideal scenarios [42], and more rudimentary approaches on complex aircraft geometries [31]. The design space exploration highlights fuel savings benefits on a propulsive fuselage concept as different amounts of forward force are generated by the thruster, the rest being provided by under-wing turbofans. Notably, a direct inverse relationship is reported between the propulsive force share and propulsor efficiency (and power saving coefficient).
- A flow feature segmentation is performed on 2D axi-symmetric flows. Whilst previous work has focused on the definition of identifiers to isolate specific regions of the flow-field, a precedence procedure was introduced in Ref. [34] and used in this work. The aerodynamic decomposition approach gives recommendations on the influence of the design changes on the aero-propulsive performance, and their inherent losses. This work is a stepping stone in the attribution of losses with respect to design changes and their effects on the flow.

1.5.2 Bibliographic Contribution

Over the course of this research project, the author has directly and indirectly contributed to the general knowledge, and advanced the state of the art, on boundary layer aerodynamics, boundary layer ingestion propulsion and the aero-propulsive embedding of aft-mounted propulsors. Part of the contribution has been disseminated on a set of presentations, reports and peer-reviewed journal publications.

Journal Publications

1. N.G.M. Moirou, D.S. Sanders, and P. Laskaridis. “Advancements and Prospects of Boundary Layer Ingestion Propulsion Concepts”. In: *Progress in Aerospace Sciences* 138 (2023). DOI: 10.1016/j.paerosci.2023.100897

Journal Publications Plan

- N.G.M. Moirou, and D.S. Sanders. “Energetic Approach towards Causal Analyses of Exhaust System Design in Propulsive Fuselage Aircraft Concepts”. In preparation for submission.
- N.G.M. Moirou, N.E. Mutangara, and D.S. Sanders. “Fundamental Considerations in the Design and Performance Assessment of Propulsive Fuselage Aircraft Concepts”. In preparation for submission to *Journal of Aircraft*.
- N.G.M. Moirou, and D.S. Sanders. “Performance Evaluation Approach for Design Space Explorations of Propulsive Fuselage Aircraft Concepts”. In preparation for submission to *Journal of Propulsion and Power*.

Conference Presentations

3. N.G.M. Moirou, N.E. Mutangara, D.S. Sanders, and P. Laskaridis. “Exploring the Design Space of a Sustainable and Efficient Aircraft featuring Boundary Layer Ingestion”. In: *57th 3AF International Conference on Applied Aerodynamics*. Bordeaux, France (2023).
2. N.G.M. Moirou, N.E. Mutangara, D.S. Sanders, and P. Laskaridis. “Towards Greener Aviation through Propulsive Fuselage Aircraft: A Hybrid Momentum- and Energy-based Approach for Assessing the Power Saving Coefficient”. In: *12th EASN International Conference on Innovation in Aviation and Space for Opening New Horizons*. Barcelona, Spain (2022).
1. N.G.M. Moirou. “Performance Evaluation of a Propulsive Fuselage Aircraft”. In: *1st Share and Inspire Conference*. Cranfield, United Kingdom (2022).

Project Reports

2. N.G.M. Moirou, N.E. Mutangara, D.S. Sanders, and P. Laskaridis. *Preliminary Wind-Tunnel Model Sizing and Scalability Study*. Tech. Rep. Cranfield University (2020).
1. N.E. Mutangara, N.G.M. Moirou., I. Lamprakis, D.S. Sanders, and P. Laskaridis. *SUBLIME Literature Review (Part 3)*. Tech. Rep. Cranfield University (2020).

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Advancements and Prospects of Boundary Layer Ingestion Propulsion Concepts

The aviation sector is experiencing an increasing pressure to reduce emissions via long-term strategies for a ceaselessly growing number of flight passengers. Aircraft currently in operation have typically been designed by considering the airframe somewhat separately from the propulsion system. In doing so, conventional aero-engine architectures are approaching their limits in terms of propulsive efficiency, with technological advancements yielding diminishing returns. A promising alternative architecture for improving the overall performance of the next generation of commercial aircraft relies upon boundary layer ingestion (BLI). This technology aerodynamically couples the airframe with a strategically positioned propulsion system to purposely ingest the airframe's boundary layer flow. Nonetheless, there is a lack in consensus surrounding the interpretation and quantification of BLI benefits. This is primarily because conventional performance accounting methods breakdown in scenarios of strong aerodynamic coupling. Subsequently, there is a major challenge in defining appropriate performance metrics to provide a consistent measurement and comparison of the potential benefits. This review examines the various accounting methods and metrics that have been applied in evaluating BLI performance. These are discussed and critiqued in the context of both numerical and experimental models. Numerically, the geometric, aerodynamic and propulsive models are sorted by their orders of fidelity along with the plenitude of methods used for flow feature identification enabling a phenomenological understanding of BLI. Particular attention is then given to experimental BLI models with their different set-ups, methods and associated limitations and uncertainties. Finally, the numerous unconventional BLI aircraft concepts are categorised, compared and critiqued with reference to their associated design exploration and optimisation studies.

2.1 Introduction

Turbojet engines and their development initiated back in the 1930s with Frank Whittle and Hans von Ohain whose latter, contributed to the first turbojet-propelled aircraft, the Heinkel He 178, in 1939 after the conception of his continuous cycle combustion engine. Through the years, the position, size and shape of the turbojets evolved into more capable and powerful engines before the introduction of the turbofan concept as a means to greater fuel efficiency. These engines continue to be the workhorse of modern day commercial aviation but the improvements in propulsive efficiency enabled by ingesting greater amounts of mass-flow enlarges the fan diameter and ipso facto increases the weight and drag of the aircraft.

Despite their constant technological advancements, diminishing returns have been achieved [1]. In addition, the ceaseless growth in passengers necessitates the civil aerospace industry to pay heed to aircraft emissions. To respond to this demand and to lean towards more sustainable strategies, stringent regulations are emerging. The European Commission (E.C.), National Aeronautics and Space Administration (NASA) and the European Union Aviation Safety Agency (EASA) have set different targets [2–7] to drastically reduce the fuel consumption and emissions whilst enhancing acoustic performance. A summary of these strategies and goals are given in Table 2.1 whereof CO₂ and noise emissions reductions of *only* 38 and 37%, respectively, were achieved by 2015. Whilst this represents significant progress compared to an aircraft from 2000, efforts must be strengthened to meet the challenging goals set for 2050.

Table 2.1: EU Vision 2020, EU Flightpath 2050 and NASA N+3 environmental goals

	EU Vision 2020	EU Flight- path 2050	NASA N+3 2025	NASA N+3 2035+
Reference techno- logy year	2000	2000	2005	2005
CO ₂	-50%	-75%	Neutral growth	-50%
NO _x	-80%	-90%	-80%	> -80%
Perceived noise	-50%	-65%	-40%	> -50%

To achieve these ambitious objectives, it is notwithstanding that revolutionary aircraft concepts are necessary, whereby the pursuit of innovative designs and technologies is now required. A potential solution is the concept of boundary layer ingestion (BLI), which focuses on synergies between airframe and propulsion system in a more holistic aircraft design approach to fully exploit the aerodynamic benefits of closer integration. The concept originates from the marine sector, whereby the submersible vehicles' propulsor takes advantages of the flow travelling around the body [8, 9], hence making that technology also applicable to airborne systems. Through the introduction of a power saving coefficient (PSC), Smith Jr. demonstrated significant power savings originating from the filling of the boundary layer (BL) momentum deficit at a lower fuel consumption whilst attenuating the aircraft's wake [10]. However, improved aircraft aerodynamic design will try to minimise drag whilst maximising lift, i.e. maximise the lift-to-drag ratio. Minimising drag through optimisation, flow control [11, 12] with laminarisation [13], surface plasma [14, 15] and their applications [16] will result in a reduced energy content of the BL, and subsequently reduce its harvesting and ultimately benefits. Therefore, caution should be taken not to try and maximise BLI benefits by inadvertently penalising the airframe lift-to-drag ratio.

Commonly, the aircraft's aerodynamics are assessed as a still vehicle around which air is moving, like in wind-tunnel testing. This is referred to herein as the relative reference frame (RRF). However, to enable more physically intuitive interpretations, the absolute reference frame (ARF) is preferred whereby the aircraft moves through an initially quiescent atmosphere [17, 18] as illustrated with

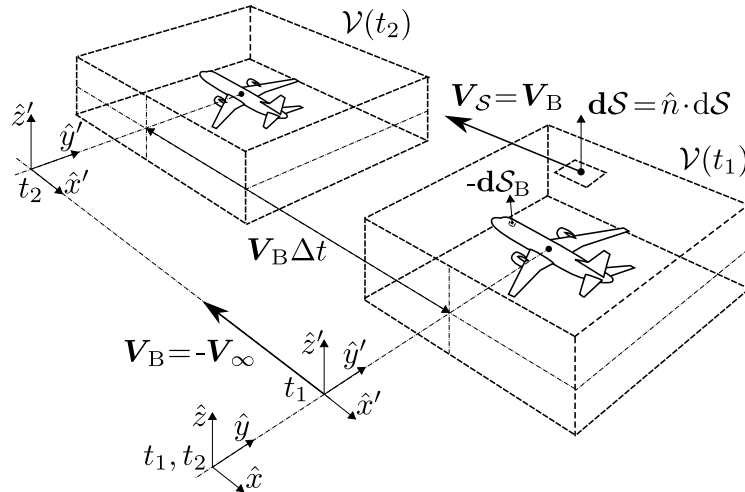


Figure 2.1: Motion of a flying aircraft from t_1 to t_2 with its control volume in the ARF $(\hat{x}, \hat{y}, \hat{z})$ and the RRF $(\hat{x}', \hat{y}', \hat{z}')$, adapted from Ref. [17]

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moving control volumes (moving in space but fix relative to the aircraft) in Fig. 2.1. The aircraft motion through the air displaces the air particles to satisfy the no-slip condition on the airframe wall surfaces, whereby constricting their near-wall velocity, imparting energy to the surrounding flow, and developing a BL. A strategic propulsor integration enables the boundary layer's energy to be harvested, rather than wasted within the aircraft's wake, increasing the vehicle's efficiency. Those power benefits are reported to vary between 5 and 13% [19–21] and the fuel burn to be reduced by between 12 and 36% [20, 22] despite presenting numerous challenges [23], notably:

- the aircraft configurations and architectures need to ensure the aircraft operability and safety at any point along its mission (on- and off-design) even if an engine becomes inoperable,
- the different aircraft concepts integrate propulsion systems in different fashions facing different levels of distortion and potentially aggravating the aircraft's performance and operability,
- the transmission architectures that can be of the form of turbo-mechanical, turbo-electric or hybrid-electric distributed propulsions present other thermal management concerns or additional weight penalties.

It is noteworthy that the close engine-airframe integration [24–28] makes the distinction between the positive (thrust) and negative (drag) forces challenging because of their strong interaction. Therefore, the well-established thrust-drag bookkeepings set out by the MIDAP (Ministry-Industry Drag Analysis Panel) Study Group [29] and the SAE (Society of Automotive Engineers) Committee [30] become invalid, because the assumptions upon which they are based no longer hold. Yet, the momentum-based principle can still be considered if treated cautiously with control volumes defined around the vehicle and propulsion system but still do not enable a meaningful distinction between thrust and drag [31, 32]. Consequently, a favoured approach is to consider the system more holistically [33]. From this approach, further developments are enabled [18, 34, 35] leading to unifications of these approaches [36–38] to gain a wider understanding of the different BLI aircraft configurations and performance.

The renewed interest in BLI has led to a profusion of publications and reviews of the state of the art [23, 32, 39–44]. Hendricks gathered NASA's efforts in the

modelling approaches and introduced the degree of coupling between the vehicle aerodynamic and propulsion system models. In search of proposing a unified momentum-based book-keeping to treat BLI, Habermann et al. [32] first reviewed and categorised part of the literature based on the different performance book-keeping methods. A division between momentum-, energy- and exergy-based accounting was presented along with their associated metrics, precautions and requirements. However, these were solely evaluated to suit the universality and applicability to the conceptual design phase. Additional analogous reviews from Refs. [43, 44] treat the different conceptual BLI aircraft which are split into three distinctive architectures (tube-and-wing, lifting fuselage and unconventional airframes) with an emphasis on the former. The diverse concepts are summarised in both publications where the former authors' focus are on the numerical modelling and the benefits offered by the configurations whereas the latter work offers a more in-depth analysis of the results and the main contributions of the works. Notwithstanding the multiple reviews available, limited opinions and critiques are provided on the methods and models chosen by the researchers community leading to their work and results obtained.

This paper therefore seeks to assess the theoretical, numerical and experimental progress related to BLI. From the conservation equations, the different book-keeping approaches are derived in Section 2.2. The close integration of the engine within the airframe prevents the aerodynamic to be studied in isolation from the propulsion system raising an ambiguity and discrepancies between the methods. Unlike previous reviews solely assessing the suitability to the conceptual design phase, a primary contribution of this paper lies in a comprehensive review of the existing approaches to evaluate the aircraft performance at any phase of the design process. Notably, the book-keeping schemes and metrics are discussed with their applicability to both numerical and experimental investigations.

Additionally, it is noted from the previous reviews introduced a definite lack of review in experimental practices despite the recent efforts. Consequently, in tandem with the theoretical and numerical development of methods and their applications that are reviewed and critiqued in Section 2.3, an emphasis is given to experimental studies and specifically to their challenges and risks. The different apparatuses and techniques are discussed in Section 2.4 to contribute to this review and highlight the current difficulties emanating from BLI.

A thorough survey of various studies in Section 2.5 gives prominence to the assumptions, methods and models used to mitigate the results claimed. Unlike previous reviews only mentioning discrepancies amidst all studies, this paper seeks to share the authors' opinions on the models and methods used to discuss the BLI-related and synergistic benefits offered by the peculiar architectures and therefore the underlying reasons that have led to those discrepancies.

Overall, the paper aims at addressing the gaps unfilled by previous work and contrasting the plenitude of studies based on the methodologies and approaches followed that yield sometimes important divergences in the benefits reported. In doing so, one is provided with a comparison of the different schemes features, degrees of fidelity in the modellings and common practices in experimental investigations but also their unresolved challenges. Further, recommendations are given on the direction of the BLI investigations and complementary technologies.

2.2 Performance Accounting and Metrics

In retrospect, to investigate the behaviour and performance of aircraft before their test flight, sub-scaled mock-ups were assessed in wind-tunnels [45]. Nonetheless, in the course of time, a growing interest emanated from the rise of new technologies and particularly with the development of computer-based solvers that replicate the flow physics - computational fluid dynamics (CFD) [46]. Whether the formulations use low- or high-order models, the cost represented by the simulations was drastically reduced compared to the manufacturing and testing of scaled aircraft. An interest still exists in performing wind-tunnel testing to ensure that the computations accurately predict the flow phenomena but regardless of the nature of the testing, the evaluation of the aircraft performance is based on equivalent book-keepings which are reviewed, hereafter.

2.2.1 Performance Accounting

Regardless of whether the tests are experimental or numerical, a robust analysis and hence accounting needs to be developed to assess the vehicle performance in a consistent manner. AGARD [29] and SAE [30] appear as pioneers in that respect with the development of a standardised thrust-drag accounting method. This

book-keeping considers stream-tubes delimited by fictitious transverse surfaces at thermodynamic stations of interest. The stream-tubes can also be referred as control volumes whereby the momentum conservation aids in determining the net forces experienced by either surface integrations of pressure and skin-friction forces on all physical surfaces or, momentum flux integrations over a transverse control surfaces positioned behind the body. These two approaches are referred to as *near-field* and *far-field*, respectively. Although this accounting has been long established, in the performance evaluation of BLI aircraft, its applicability is discussed by Ref. [33]. The distinction between thrust and drag becomes complex and some references [10, 47–49] introduce an ingested drag quantity highlighting that drag cannot be evaluated separately as it becomes part of the re-accelerated flow. Alternative means of by-passing this ambiguity have come forth such as revised momentum-based approaches [31, 32, 48], energy- and exergy-based methods with Refs. [18, 33] and Ref. [35] respectively, and vortical-based approaches [50, 51].

2.2.1.1 Fundamental Laws and Control Volume Theory

To rigorously examine the aerodynamic performance of an aircraft, it is essential to evoke control volume (CV) theory to which the fundamental laws of mass, momentum and energy conservation may be applied. Broadly speaking, and considering steady flight, the outer bounds ($\mathcal{S}_{FS} \cup \mathcal{S}_{SC} \cup \mathcal{S}_{TP}$) of the control volume (Fig. 2.2) are typically considered to be fixed in space relative to the aircraft and observer positioned in the same inertial reference frame. This has been referred to herein as the RRF, where the flow is viewed as passing over the *stationary* aircraft,

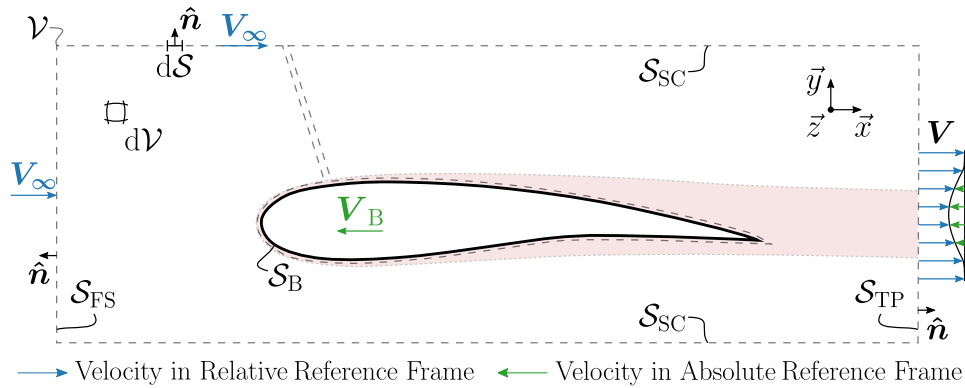


Figure 2.2: Two-dimensional cutaway of a three-dimensional control volume encapsulating an aerofoil with its boundary layer development (shaded region)

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and is of practical advantage in numerical modelling and wind-tunnel testing experiments.

The integral forms of the fundamental equations are usually derived with respect to this reference frame, and the closed surface integrals are expanded via the summation rule of integration to allow them to be split and rearranged into *near-field* and *far-field* contributions. The *far-field* integration is simplified through substitutions that express velocity and pressure terms relative to the free-stream conditions (Table 2.2). This is convenient because the integrations on those surfaces sufficiently ahead $\mathcal{S}_{\text{FS}}$, above, below, or out to the sides $\mathcal{S}_{\text{SC}}$ of the aircraft become zero, and the only surface integral remaining is that behind the aircraft $\mathcal{S}_{\text{TP}}$, which is across an area commonly referred to as the Trefftz plane.

Reducing the *far-field* surface integrals to the Trefftz plane, is a seemingly pure mathematical manipulation for convenience, and is a feature of almost all *far-field*

Table 2.2: Conservation equations in the relative (RRF) and absolute (ARF) reference frames, respectively

Change of Reference Frame	
RRF	$\mathbf{V}' \quad \quad p = p_G + p_\infty$
ARF	$\mathbf{V}' = \mathbf{V} + \mathbf{V}_\infty \quad \quad p = p_G + p_\infty$
Mass Conservation	
RRF	$\oint_S \rho (\mathbf{V}' \cdot \hat{\mathbf{n}}) \, dS = 0 \quad (2.1)$
ARF	$\oint_S \rho (\mathbf{V}' \cdot \hat{\mathbf{n}}) \, dS = 0 \quad (2.2)$
Momentum Conservation	
RRF	$\oint_S \rho (\mathbf{V}' \mathbf{V}' \cdot \hat{\mathbf{n}}) \, dS = \oint_S (-p \hat{\mathbf{n}} + \bar{\bar{\tau}} \cdot \hat{\mathbf{n}}) \, dS + \iiint_V \rho \mathbf{f} \, dV \quad (2.3)$
ARF	$\oint_S \rho (\mathbf{V} \mathbf{V}' \cdot \hat{\mathbf{n}}) \, dS = \oint_S (-p_G \hat{\mathbf{n}} + \bar{\bar{\tau}} \cdot \hat{\mathbf{n}}) \, dS + \iiint_V \rho \mathbf{f} \, dV \quad (2.4)$
Mechanical Energy Conservation	
RRF	$\oint_S \rho \left(\frac{\mathbf{V}'^2}{2} \right) \cdot \hat{\mathbf{n}} \, dS = \oint_S (-p \mathbf{V}' + \bar{\bar{\tau}} \cdot \mathbf{V}') \cdot \hat{\mathbf{n}} \, dS + \iiint_V [p \nabla \cdot \mathbf{V}' - (\bar{\bar{\tau}} \cdot \nabla) \cdot \mathbf{V}' + \rho (\mathbf{f} \cdot \mathbf{V}')] \, dV \quad (2.5)$
ARF	$\oint_S \rho \left(\frac{\mathbf{V}^2}{2} \right) \cdot \hat{\mathbf{n}} \, dS = \oint_S (-p_G \mathbf{V} + \bar{\bar{\tau}} \cdot \mathbf{V}) \cdot \hat{\mathbf{n}} \, dS + \iiint_V [p_G \nabla \cdot \mathbf{V}' - (\bar{\bar{\tau}} \cdot \nabla) \cdot \mathbf{V}' + \rho (\mathbf{f} \cdot \mathbf{V}')] \, dV \quad (2.6)$
Total Energy Conservation	
RRF	$\oint_S \left[\rho \left(e + \frac{\mathbf{V}'^2}{2} \right) \mathbf{V}' \right] \cdot \hat{\mathbf{n}} \, dS = \oint_S (-q - p \mathbf{V}' + \bar{\bar{\tau}} \cdot \mathbf{V}') \cdot \hat{\mathbf{n}} \, dS + \iiint_V \rho (\dot{q} + \mathbf{f} \cdot \mathbf{V}') \, dV \quad (2.7)$
ARF	$\oint_S \left[\rho \left(e + \frac{\mathbf{V}^2}{2} \right) \mathbf{V}' \right] \cdot \hat{\mathbf{n}} \, dS = \oint_S (-q - p \mathbf{V} + \bar{\bar{\tau}} \cdot \mathbf{V}) \cdot \hat{\mathbf{n}} \, dS + \iiint_V \rho (\dot{q} + \mathbf{f} \cdot \mathbf{V}) \, dV \quad (2.8)$

decomposition methods, including momentum- [50], energy- [33], exergy- [35] and vortical-based [51] methods. However, this mathematical manipulation has inadvertently, and seemingly unwittingly to the authors of the aforementioned references, been a transformation to the ARF, whereby the aircraft is perceived as moving through the flow instead of the other way round. This was first pointed out by Sanders and Laskaridis [18], in their contributions to further developing the mechanical energy-based approach of Drela [33]. Subsequently, Lamprakis et al. [17] show this by re-deriving the fundamental equations for a general control volume in motion (but still fixed in space relative to the aircraft), alike Fig. 2.1 with respect to the ARF by means of the Leibniz-Reynolds general transport theorem. Assuming that the control volume remains fixed in space relative to the aircraft, the generalised fundamental equations may be simplified by assuming steady flow, and applying them to the RRF and ARF, respectively. These forms of the equations are summarised in Table 2.2.

Mass and Momentum Conservation

Considering a simple representation of an aerofoil as given in Fig. 2.2 and assuming steady conditions, the mass and momentum conservation equations can be written in integral form over an arbitrary volume $\mathcal{V}$ enclosed by a surface $\mathcal{S} = \mathcal{S}_{FS} \cup \mathcal{S}_{SC} \cup \mathcal{S}_{TP}$, as given by Eqs. (2.1) to (2.4) where ρ is the fluid density, $\mathbf{V}$ the velocity vector at any given point of the volume, p the static pressure of the fluid, $\bar{\tau}$ the viscous stress tensor, $\mathbf{f}$ the net body force per unit mass exerted on the fluid within the volume $\mathcal{V}$ and $\hat{\mathbf{n}}$ the unit normal vector along a surface $\mathcal{S}$ and pointing out of the volume $\mathcal{V}$ by convention [52].

Energy Conservation

The total energy conservation within a control volume $\mathcal{V}$, is given by Eqs. (2.7) and (2.8) [53] where e is the mass-specific internal energy, $\mathbf{q}$ the heat flux due to thermal conduction and $\dot{q}$ a volumetric source term. The internal energy, i.e. energy associated with the atoms and molecules of a system, is obtained from the first law of thermodynamics [54] and subtracting it from the total energy (Eqs. (2.7) and (2.8)) yields the mechanical energy (Eqs. (2.5) and (2.6)) which can also be derived from the scalar multiplication between Eqs. (2.3) and (2.4) respectively and $\mathbf{V}$.

It is possible to harvest the energy present in the flow in two manners, through a mechanical device or a thermal device. The former only recuperates the available mechanical energy which needs to be isolated from its total form, whereas the thermal components are related to thermal analyses whereby a heat-exchanger could be used. Therefore, Eqs. (2.5) and (2.6) complement Eqs. (2.7) and (2.8) and are further detailed with energy-based approaches in Section 2.2.1.3.

2.2.1.2 Momentum-based Approaches

The Newton's second law yields the momentum conservation equations given in Eqs. (2.3) and (2.4) and describes the force that is equal to the time rate of change of momentum [52]. Those forces can be of two forms, body or surface forces and the time rate of change of momentum is a combination of the net outflow and the momentum of the fluid within its volume (in unsteady flows).

Conventional Thrust-Drag Book-keeping

By convention, the forces on bodies are extracted [29, 30] to determine the system performance and allow a drag estimation and decomposition where the perturbations in the momentum equation are substituted by forms of enthalpy and entropy [50, 55–57].

In these methods, the domain is split into a drag sub-domain and a thrust sub-domain. A crude assumption is made on their definitions whereby any particle travelling through the engine contributes to the thrust (shaded region in Fig. 2.3) and the remainder towards penalties, or drag. The forces that act in the thrust domain, i.e. inside the stream-tube, are denoted θ (not represented in Fig. 2.3) whereas the forces that act in the drag domain are denoted ϕ . In either domain, these terms describe the integrated pressure and friction forces on either unbounded or wall surfaces. The fluxes across the different stations (free-stream - FS, fan-face - FF, nozzle - NZ, etc.) define the gauge stream forces and help defining the intrinsic, standard and overall net thrusts, Eqs. (2.9a), (2.9c) and (2.9e), respectively. Nonetheless, these definitions only describe the net thrust as a result of the internal flow. To account for the forces generated outside of the thrust-domain that act as performance penalties, the net propulsive force (NPF) is considered for each net thrust definition, respectively Eqs. (2.9b), (2.9d) and (2.9f).

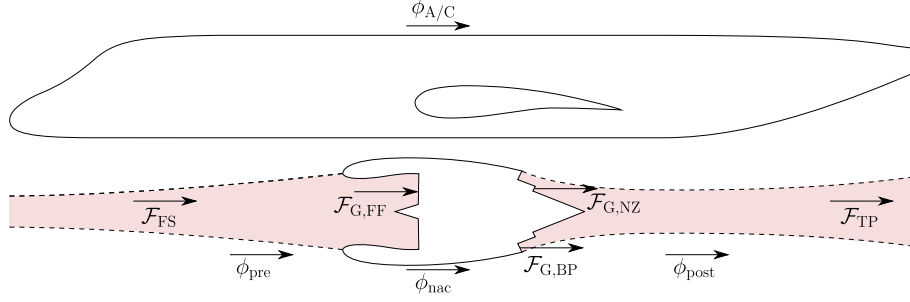


Figure 2.3: Forces decomposition on stream-tubes, nacelle and airframe based on the thrust-drag accounting

$$\mathcal{F}_{N,int} = \mathcal{F}_{G,BP} + \mathcal{F}_{G,NZ} - \mathcal{F}_{G,FF} \quad (2.9a)$$

$$\text{NPF}_{int} = \mathcal{F}_{N,int} - \phi_{nac} \quad (2.9b)$$

$$\mathcal{F}_{N,std} = \mathcal{F}_{G,BP} + \mathcal{F}_{G,NZ} - \mathcal{F}_{G,FS} \quad (2.9c)$$

$$\text{NPF}_{std} = \mathcal{F}_{N,std} - \phi_{pre} - \phi_{nac} \quad (2.9d)$$

$$\mathcal{F}_{N,ov} = \mathcal{F}_{G,TP} - \mathcal{F}_{G,FS} \quad (2.9e)$$

$$\text{NPF}_{ov} = \mathcal{F}_{N,ov} - \phi_{pre} - \phi_{nac} - \phi_{post} \quad (2.9f)$$

In practice, the standard definitions are the most common however, they do not consider the forces exerted on the exhaust plug and this brought forth a modified *near-field* approach [58]. In addition to the thrust and drag interactions, installation effects can also be assessed by integration of the forces over the external surfaces, e.g. wing, $\phi_{A/C}$. The magnitude of the interferences can then be assessed by comparison with an idealised configuration to create a superposition model of a clean-wing and isolated engine [58–60].

Revisited Thrust-Drag Book-keepings

Despite being applicable to podded, or pylon-mounted engines, the conventional thrust-drag book-keeping established on the decoupling of thrust and drag does not hold true in BLI. As alluded to the original drag-only contribution of the airframe is partly ingested by the BLI propulsor and should be accounted in the thrust domain. This highlights the ambiguity of considering the forces applied on the wall surfaces as ϕ or θ .

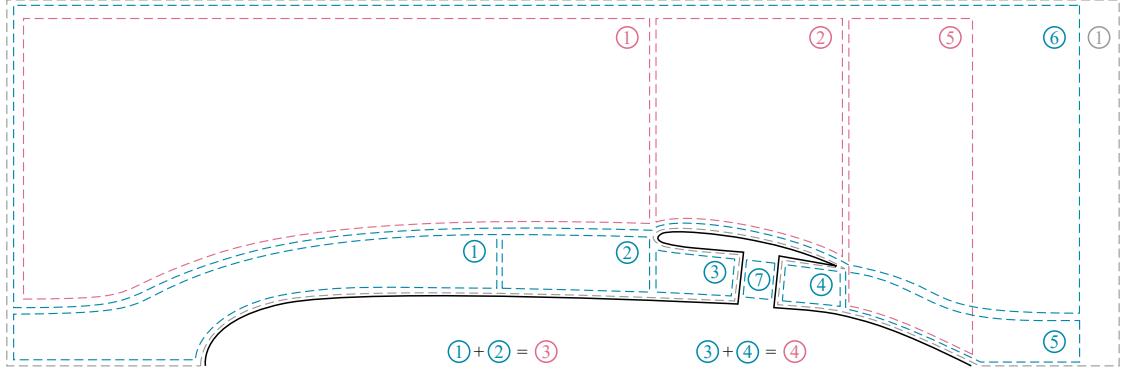


Figure 2.4: Control volume divisions based on Habermann et al. [32] ① to ⑤, Sanders [31] ① to ⑦, and Gray [62] ①

Similarly, modified *near-field* thrust-drag accountings are proposed by Matesanz-García et al. [61] and by Habermann et al. [32]. In both book-keepings, the interaction between the jet-stream and the wake is omitted and only the interferences on the pre-entry stream-tube are considered. Figure 2.4 depicts a common control volume scheme for all the book-keepings presented.

Particularly, in Matesanz-García et al.'s [61] approach, the stream-tubes from ① to ③ cover all stream-lines from free-stream to the propulsor's AIP. The net vehicle force (NVF) is computed from the difference between the modified drag, $\mathcal{D}^*$, and the modified thrust, $\mathcal{T}^*$:

$$\text{NVF} = \mathcal{D}^* - \mathcal{T}^* \quad (2.10)$$

The former results from the addition of the forces outside of the pre-entry stream-tube ③, i.e. the force on the pre-entry unbounded surface, ϕ_{pre} , and the nacelle wall, ϕ_{nac} , as given in Eq. (2.11). The latter is defined as the difference between the gross propulsive force at the fan-exit and the gauge force at the inlet plane of ① (Eq. (2.12)). The modified gross propulsive force, GPF^* , is given by Eq. (2.13) as the sum of the gauge force at the fan-exit and the forces on the walls behind the propulsor.

$$\mathcal{D}^* = \phi_{\text{pre}} + \phi_{\text{nac}} \quad (2.11)$$

$$\mathcal{T}^* = -\mathcal{F}_{\text{FS}} + \text{GPF}^* \quad (2.12)$$

$$\text{GPF}^* = \mathcal{F}_{\text{FE}} - \theta_{\text{exh}} - \theta_{\text{cone}} \quad (2.13)$$

In Ref. [61] definition, ϕ and θ refer to the conventions given in Ref. [59] and $\mathcal{F}$ denotes the sum of the axial pressure and viscous forces on fictitious survey planes.

Nonetheless, the NVF only advises on the net thrust changes from variations in the fuselage or BLI propulsor designs but does not allow any performance evaluation. In that respect, the NVF of the podded reference aircraft, $\text{NVF}_{\text{A/C,ref}}$, needs to be defined as the difference between the NPF generated by the main engines and the airframe drag. To isolate the thrust contribution from the BLI propulsor, based on the assumption that the force on the airframe remains unchanged between a BLI and a podded aircraft, and changes in this force are attributed to the thrust only, a relative net thrust force ($\mathcal{F}_{RN}$) is defined in Eq. (2.14) as the difference between the NVF of the BLI and that of the podded aircraft [31]. This metric becomes of interest when evaluating the influence of the changes in the design of the fuselage or the propulsor in the BLI aircraft performance assessment.

$$\mathcal{F}_{RN} = \text{NVF} - \text{NVF}_{\text{A/C,ref}} \quad (2.14)$$

In a similar fashion and to retract any kind of ambiguity related to the thrust and the drag domains, Gray [62] considers the net axial force of the combined BLI fuselage-propulsor (① from Fig. 2.4) and introduces a non-dimensional force coefficient $C_{\mathcal{F}_x}$, Eq. (2.15). In that manner, the thrust-drag axiom that states that changes in the airframe only affect the force on the fuselage whereas those on the propulsion system only affect the propulsive force is discarded to account for the stream-wise net force. Analogous to the relative net force $\mathcal{F}_{RN}$, the comparison in $C_{\mathcal{F}_x}$ between the podded aircraft and the BLI one assesses the benefits of the configuration in net force count.

$$C_{\mathcal{F}_x} = \frac{2\mathcal{F}_x}{\rho_{\infty} V_{\infty}^2 A_{\text{ref}}} \quad (2.15)$$

Based on the work from Seitz and Gologan [48], Habermann et al. [32] also developed a *near-field* momentum-based book-keeping whereby the net thrust of the system is defined as the sum of the fan disc force, $\mathcal{F}_{\text{disc}}$, and the integrated forces over the wall surfaces. The overall CV that assesses the momentum flux at the outlet is divided into sub-domains following the notations from ① to ⑤ in Fig. 2.4 and the relation between the outlet momentum flux force and the *near-field* integration is given by Eq. (2.16). The NVF following their approach is defined in Eq. (2.17).

$$\begin{aligned} \dot{I}_{\text{out},5} &= \dot{I}_{\text{in},1} + \dot{I}_{\text{in},3} + \mathcal{F}_{p,\text{in},1} + \mathcal{F}_{p,\text{in},3} \\ &\quad + \mathcal{F}_3 + \mathcal{F}_4 + \mathcal{F}_{\text{disc}} + \mathcal{F}_2 + \mathcal{F}_5 - \mathcal{F}_{p,\text{out},5} \end{aligned} \quad (2.16)$$

$$\text{NVF} = \mathcal{F}_3 + \mathcal{F}_4 + \mathcal{F}_{\text{disc}} + \mathcal{F}_2 + \mathcal{F}_5 \quad (2.17)$$

The $\dot{I}$ terms describe the momentum fluxes at the inlet or outlet stations, *in* and *out* respectively, the $\mathcal{F}_p$ terms correspond to the sum of the axial pressure forces, $\mathcal{F}_{\text{disc}}$ is the force of the fan which can be modelled in several natures, e.g. boundary conditions, body-force or fan model, and the $\mathcal{F}$ terms correspond to the sum of the forces on the unbounded and wall surfaces inside the entire domain. The numbers associated in the subscripts refer to the CV they are associated with, from ① to ⑤. The proposed method is offered as an universally applicable technique to all coupled airframe-propulsion systems and extendible from semi-empirical methods to high-fidelity CFD or wind-tunnel testing.

However, similar to Ref. [61] with Eq. (2.14), the comparison with the podded configuration is necessary to assess the thrust generated by the BLI propulsor. Nonetheless, these accounting systems are based on the assumption that although there is a strong interaction between the airframe, the propulsion system, and the *near-field* flow, the crude assumption that these can be somehow decoupled is taken.

As a more versatile approach, Sanders [31] offers to not only consider a single unique definition for thrust, but suggests considering several thrust-force accounting systems in combination to aid in the interpretation of the physical mechanisms underpinning BLI propulsion. It is first demonstrated by Sanders that the post-exit stream-tube cannot be omitted in BLI investigations and that therefore an additional term, ϕ_{post} - in analogy to the conventions from Refs. [29, 30], needs to be accounted. Due to the interaction that exists between the jet-plume and the body wake, a difficulty arises in the position at which the transverse plane accurately calculates the axial net force of the configuration and therefore constraints the *far-field* integration to only be computed at the Trefftz plane.

Inherently, this yields another split of the overall CV whereby the pre-entry stream-tube is divided into an additional pre-diffusive zone ② accounting for the propulsor installation effects upstream. Also, rather than describing the accounting

as thrust-drag, the word “force” is used to make a distinction from the “drag” definition made by Refs. [30, 59], which is based upon the Prandtl’s extension to D’Alembert’s paradox where drag is defined as the residual force between real and potential flows. In potential flows, the resulting force over a body is equal to zero and is referred to as a buoyancy force. This local pressure force’s direct contribution to drag, acting on different locations of the airframe, may be obtained by subtracting the local buoyancy pressure force from the local pressure force from the real viscous flow. However, in conventional thrust-drag approaches, this procedure relies on the assumption that BLs are thin relative to the airframe, and that the differences between the potential and real flow stream-tubes are negligible. Additionally, to apply this thinking to a meaningful separation between thrust and drag, these conventions rely on a clear distinction between the airframe’s versus propulsor’s stream-tubes. This is because Prandtl’s extension to D’Alembert’s paradox only applies to infinite (or semi-infinite) stream-tubes such that the net buoyancy for each stream-tube is indeed zero. However, when employing BLI propulsion, these infinite stream-tubes are merged and cannot be isolated from one another. Moreover, the ingested BL is on the same scale as the propulsion system, and the differences between the potential and real flow’s stream-tubes are no longer negligible, but instead are rather significant. Therefore, the well-established thrust-drag bookkeepings [29, 30] become invalid as the assumptions upon which they are based no longer hold.

More rigorously, force is used in lieu of drag but does not alter the NVF definition which is equal to the summation of all the forces from *near-field* integration, or *far-field*, as given by Eq. (2.18) following the notations from ① to ⑦ pictured in Fig. 2.4.

$$\begin{aligned} \text{NVF} &= \underbrace{\Psi_1 + \Psi_2 + \Psi_3 + \Psi_7 + \Psi_4 + \Psi_5 + \Psi_6}_{\text{near-field}} \\ &= \underbrace{\Omega_{\text{in},1} + \Omega_{\text{in},6} + \Omega_{\text{out},5} + \Omega_{\text{out},6} + \Omega_{\text{out,SC},6}}_{\text{far-field}} \end{aligned} \quad (2.18)$$

In comparison to the previous accounting, Ψ is introduced in place of the ambiguous ϕ and θ and quantifies any form of force on a wall surface. The term Ω also yields the summation of both the momentum flux force and the pressure force, as well as the shear forces, omitted or neglected by Habermann et al., on permeable surfaces:

$$\Omega_i = \dot{I}_i + \mathcal{F}_{p,i} + \iint_{S_i} \bar{\tau} \cdot \hat{n} \, dS \quad (2.19)$$

The CV division offered in Ref. [31] enables several thrust-force definitions by encompassing different CV as well as ensuring the momentum conservation. Notably, the standard net thrust definition used by Refs. [32, 61] is also defined by Sanders along the propulsive, intrinsic, pre-diffusive standard, pre-diffusive overall and overall net thrusts [31].

Alternatively, an ideally expanded momentum defect method, also referred to as isentropic expansion, is proposed [10, 47, 63–65]. The method consists in defining a velocity profile experienced by a free-stream ingesting propulsion system equivalent to the one seen by a BLI propulsor inlet (Fig. 2.5). In other words, any point in the flow-field sees its stagnation pressure expanded to free-stream. The resulting velocity, $\tilde{V}$, is called isentropic velocity and is compatible with the energy and exergy approaches reviewed in Sections 2.2.1.3 and 2.2.1.4.

$$\tilde{V} = \sqrt{\frac{2\gamma R}{\gamma - 1} T \left[1 - \left(\frac{p}{p_\infty} \right)^{-\frac{\gamma-1}{\gamma}} \right]} \quad (2.20)$$

The integral form of the momentum conservation (Eq. (2.3)), can be re-written following Eq. (2.21) as the expanded momentum defect, $\tilde{P}$. The thrust of the system given in Eq. (2.22) is obtained from the difference of that quantity between the inlet and outlet of the propulsion system whereby a decrease in defect yields a forward force.

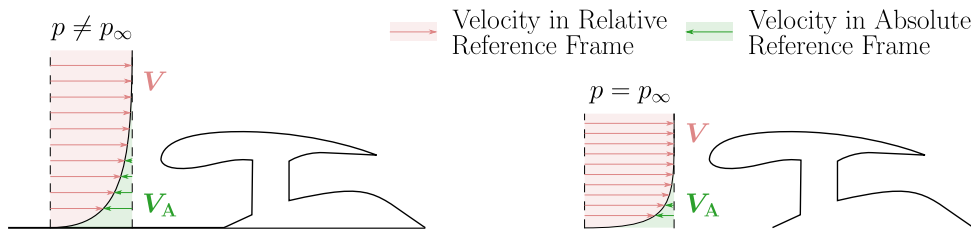


Figure 2.5: Boundary layer velocity profile at the BLI propulsor's AIP (left) and its equivalent at fully-expanded isentropic conditions (right)

$$\tilde{P} = \oint_S \rho (\mathbf{V}_\infty - \tilde{\mathbf{V}}) \mathbf{V} \cdot \hat{\mathbf{n}} \, dS \quad (2.21)$$

$$\mathcal{T} = \tilde{P}_{\text{FF}} - \tilde{P}_{\text{NZ}} \quad (2.22)$$

A comparison of this method with energy approaches is given in Ref. [66] whose conclusion is that the isentropic expansion method is suitable at pre-design phases as long as a low interaction is observed between the airframe and propulsor, questioning its real applicability to BLI.

To conclude, the different features of the momentum-based are the relatively low computational demand required but the lack of information that are obtainable using energy-based methods. To relate the performance to physical phenomena, alternative approaches are considered and detailed hereafter.

2.2.1.3 Energy-based Approaches

Considering the system holistically, one can draw a unique CV around the vehicle of interest (① from Fig. 2.4) and evaluate the forces, mechanical energies and powers from an energy standpoint.

Retrieving the momentum conservation equations in Eqs. (2.3) and (2.4) and forming the dot product with $\mathbf{V}$ yields the mechanical energy equations Eqs. (2.5) and (2.6) in integral form. Its integration over ① gives the general form of the mechanical power balance (Eq. (2.23)) [33] where the left-hand side terms of the equation describe the total power input and the right-hand side the power output.

$$\mathcal{P}_S + \mathcal{P}_V + \mathcal{P}_K = W\dot{h} + \dot{\mathcal{E}}_u + \dot{\mathcal{E}}_v + \dot{\mathcal{E}}_p + \dot{\mathcal{E}}_w + \Phi \quad (2.23)$$

On the left, the power production or inflow encompasses $\mathcal{P}_S$ describing the net propulsor shaft power which integrates all forces on all moving body surfaces, $\mathcal{P}_V$ characterising the rate of volumetric pressure work provided by the fluid by expansion and $\mathcal{P}_K$ giving the mechanical energy inflow rate. On the right, the terms describe the total energy outflow rate that respectively correspond to the reversible power changes during ascent and descent, the stream-wise kinetic energy

deposition rate, the transverse kinetic energy deposition rate, the rates of wake pressure-defect work and wave pressure work, as well as the power sinks with the rate of viscous dissipation, Φ .

In contrast to the momentum-based approaches, the mechanical power production and consumption of the aircraft are balanced in lieu of the forces - with the net vehicle force previously defined. Nonetheless, Hall et al. [36] compare the previously described ideal expansion method to the power balance method with the evaluation of the dissipation/drag. The airframe dissipation is referred to as the effective drag power that is available in the air and is compared against the effective drag; respectively the net propulsive power is compared to the effective thrust. Both methods yield similar values, in coefficient forms, for low angle of attacks only and no detail, nor explanation, are given on the discrepancies as the incidence increases.

Sato [34] extends the power balance method from Drela [33] by providing a derivation of an analytical expression of profile drag estimates. The aerodynamic performance of integrated configurations is assessed with a fuselage with an unducted and ducted actuator disc at its rear, a ducted actuator disc and two interfering aerofoils. The work is also extended to a system-level optimisation of fuel burn on a hybrid-wing body configuration [34].

To enable an improved interpretation of the causality between the power sources and sinks of the system, the ARF is considered over a RRF in Ref. [18]. An example is the power transferred from the airframe to the flow developing a BL over its surface as the aircraft moves through an initially quiescent atmosphere. From this point of view, Eq. (2.23) can be rearranged in Eq. (2.24) to exploit an explicit formulation of the forces imbalance over powered bodies for the evaluation of partial assemblies in isolation to the rest of the airframe (fuselage $\mathcal{F}_B$ and propulsor $\mathcal{F}_P$).

$$(\mathcal{F}_B + \mathcal{F}_P) \cdot \mathbf{V}_\infty = \overbrace{\dot{\mathcal{E}}_u + \dot{\mathcal{E}}_v + \dot{\mathcal{E}}_p + \dot{\mathcal{E}}_\tau}^{\dot{\mathcal{E}}_m} + W\dot{h} + \dot{\mathcal{E}}_P + \Phi + \Theta \quad (2.24)$$

Analogous to the notations taken in Ref. [33], the $\dot{\mathcal{E}}_i$ terms relate to the $\dot{E}$ given in Ref. [33] after a change of reference frame and $\dot{\mathcal{E}}_\tau$ is introduced to represent the rate of viscous boundary-work. Amongst the findings made by Sato [34], his investigations on $\mathcal{P}_V$ led to the identification of a loss mechanism referred to as baroclinic power. Θ is therefore defined as the reciprocal of $\mathcal{P}_V$ to characterise the rate of volumetric pressure work from which a part is self recoverable and the other deemed more appropriately to be a sink. In addition, this term is demonstrated to not be negligible as initially expressed in Ref. [33] at either subsonic or supersonic conditions.

2.2.1.4 Exergy-based Approaches

The exergy-based analyses extend the energy-based approaches by including the conservation of kinetic exergy, which considers the total enthalpy of a working fluid. The investigation of the total enthalpy addresses both the mechanical and thermal aspects granting a broader perspective on the amount of recoverable energy present in the flow. These two forms of energy represent the total energy of the fluid and are equivalent by virtue of the first law of thermodynamics [54]. Nonetheless, if the mechanical energy $\dot{\mathcal{E}}_m$ can be ideally converted into mechanical work, only a portion of the thermal energy $\dot{\mathcal{E}}_{th}$ can be ideally converted into mechanical work and this is governed by the second law of thermodynamics [54].

The approach is similar to the energy-based one whereby a unique control volume surrounding the aerodynamic body is considered. By extension of the power balance, the supply and outflow require the accounting of exergy/anergy terms. The terms exergy and anergy are employed to represent the part of energy that has an economic value, i.e. usable energy, and the part of destroyed energy, or equivalently generated anergy, by irreversible phenomena [67]. In the BL developing over the airframe, both mechanical and thermal perturbations are generated whereof a part of these perturbations dissipate (either over the surface or in the wake and contribute to the total anergy, $\dot{\mathcal{A}}_{tot}$). The available amount of energy is thus recoverable and defines the term exergy, lumping the mechanical and thermal exergy, $\dot{\mathcal{E}}_m$ and $\dot{\mathcal{E}}_{th}$ respectively.

In the balance between the exergy supply, and the exergy outflow and anergy generation (Eq. (2.25)) [68], the left-hand side terms provide the exergy supply to the system with $\dot{\mathcal{E}}_p$ being the rate of mechanical energy/exergy by the propulsor

(similarly as in Eq. (2.24)) and $\dot{\mathcal{E}}_q$ the rate of thermal energy/exergy that can be provided by heated, or cooled walls, heat exchangers etc. [69] The right-hand side terms describe the exergy outflows, $\dot{\mathcal{E}}_m$ and $\dot{\mathcal{E}}_{th}$, and the anergy generation, $\dot{\mathcal{A}}_{tot}$, along the rate of change in altitude, $W\dot{h}$ (as in Eq. (2.24)).

$$\dot{\mathcal{E}}_P + \dot{\mathcal{E}}_q = W\dot{h} + \dot{\mathcal{E}}_m + \dot{\mathcal{E}}_{th} + \dot{\mathcal{A}}_{tot} \quad (2.25)$$

To demonstrate the applicability of the book-keeping and its benefits, Arntz's [35] studies focus on two-dimensional aerofoils and three-dimensional wings [70], the NASA Common Research Model (CRM) [71] and a two-dimensional BWB configuration [72] based on the exergy principles. The approach is later further developed with an harmonised formulation from Aguirre et al. [73]. An aero-thermo-propulsive vehicle performance evaluation approach is also offered by Ref. [37] who investigated the benefits of ingesting the thermal BL.

2.2.1.5 Comparison

The introduced accounting schemes present major differences despite all being based on the conservation principles. These differences are summarised in Table 2.3 and further detailed. Numerically, it appears clear that the computational demand is much more important in *far-field* approaches than *near-field* as a dense grid is required downstream of the aircraft. However, with *near-field* methods, the information extractable are rather limited. The forces are decomposable in pressure and fiction and because of the thrust and drag ambiguity, the net axial force is often preferred. As an alternative, the ideal expansion method has been vastly studied but Ref. [66] reports that the approach only stands when low interaction is observed between the airframe and propulsion

Table 2.3: Comparison of the different features offered by the reviewed approaches

Approach Family	Computational Demand	Applicability/Validity	Phenomenology	Thermal Effects
Momentum (near-field)	low	✓	✗	✗
Momentum (far-field)	high	✓	✗	✗
Ideal Expansion	low	?	✗	✗
Energy	high	✓	✓	✗
Exergy	high	✓	✓	✓

systems. This comment is contradictory to BLI and the tight integration of the engine putting into question its applicability. Therefore, *far-field* methods are often considered as they provide more insights on the flow phenomena. With the momentum approach, the drag is decomposed in different fashions such as the profile, wave, lift-induced and vortex components. Nonetheless, there are limitations in the causes and effects of the different phenomena observed. Momentum approaches are Galilean invariant, as opposed to Galilean covariant, when considered in the relative reference frame. The absolute reference frame enables the covariance whereby relationships between work and energy on the flow are transferable and change by an equal amount when viewed from different reference frames yielding in the energy and exergy formulations. Physic-based and more intuitive interpretations are possible with these two approaches whereby exergy is transferred to the flow through the no-slip condition and transfers in energy forms take place in the flow, making useful work identifiable. Lastly, the difference between the two is purely thermodynamic as thermal energy aspects are considered with exergy-based analyses allowing the study of heated or cooled-down flows.

Therefore, if objectives are to evaluate the overall performance of the aircraft concept or optimise it, *near-field* are sufficient as the net axial force can be related to the power consumption and savings. For detailed analyses of the aerodynamics and propulsion, energy and exergy analyses are recommended despite their increased computation cost with denser grids in the *far-field*. These two approaches enable the flow phenomena to be studied in detail and related to the underpinning causes and effects of BLI.

2.2.2 Performance Metrics

Stemming from the manifold of book-keepings presented, different figures of merit are hereafter discussed and critiqued. The highly integrated nature of the aerodynamic and propulsion in BLI requires different levels of assessment that can be segmented in system-, aircraft- and mission-level evaluations.

2.2.2.1 Intake Performance

In the same vein as with an engine pod, the intake performance can be characterised but in addition to the fan-intake interaction, the propulsor integration effects can be introduced in scenarios of strong aerodynamic coupling. The position at which the propulsor is positioned plays an important role in the amount and quality of the ingested flow and thus its spillage or suction effects. For examples, the diffusion, distributions at the aerodynamic interface plane (AIP) and distortion are important characteristics to quantify the blade loading and assist designers with the constraints the fan would face [74]. In BLI, as opposed to a pod, a natural pre-diffusion takes place along the fuselage wall generating a BL which is incomparable to the flow observed ahead of under-wing turbofans. The low velocity region close to the fuselage wall is responsible for the greatest power savings (from its energy abundance observed in the ARF) but also plays an

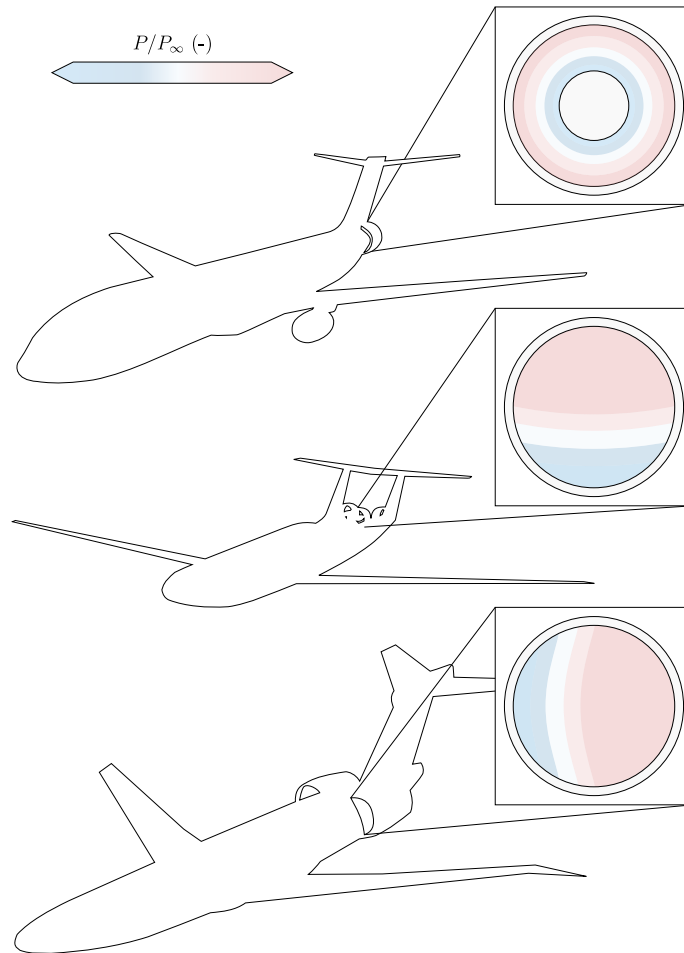


Figure 2.6: Schematic of governing distortion types (normalised total pressure) for various embedded propulsion systems

essential role in the fan operation as higher levels of distortion are experienced by the fan. Figure 2.6 represents the types of distortion faced by the engines featured in the different BLI aircraft concepts.

In CFD, the grid resolution allows almost continuous data to be computed but, in experiments, data points can only be measured where probes are positioned (Fig. 2.7) following some installation constraints and potential flow perturbations, e.g. blockage or turbulence. A generic disposition of the probes along the rakes is illustrated in Fig. 2.7 where eight equi-spaced arms contain five probes each placed at the centroids of equal areas. This technique is commonly used for podded engines that face a free-stream flow nonetheless, in BLI, different aircraft configurations come with different engine integrations and therefore the positioning of the arms might differ as higher distortion is expected closer to the wall surfaces.

In experimental set-ups, properties are therefore measured by multi-probe rakes [75, 76] but numerically, the fineness of the grid allows for a more refined distribution. To characterise the total pressure distortion, multiple metrics can be used in tandem: the inlet, or intake, pressure recovery, IPR (Eq. (2.26)), the distortion coefficient measured on a specific section of the full-annulus, commonly a 60° section, $DC(60)$ (Eq. (2.27)) [77] and the radial and circumferential distortion indices, RDI (Eq. (2.28)) and CDI (Eq. (2.29)), respectively [78].

$$\text{IPR} = \frac{\bar{P}_{\text{AIP}}}{\bar{P}_\infty} \quad (2.26)$$

$$DC(60) = \max \left(\frac{\bar{P}_{\text{AIP}} - \bar{P}_{60,\text{AIP}}}{q} \right) \quad (2.27)$$

$$\text{RDI} = \max \left(\frac{\bar{P}_{\text{AIP}} - \bar{P}_{i=0}}{\bar{P}_{\text{AIP}}}, \frac{\bar{P}_{\text{AIP}} - \bar{P}_{i=4}}{\bar{P}_{\text{AIP}}} \right) \quad (2.28)$$

$$\text{CDI} = \max_{i=0}^{n=3} \left(\frac{1}{2} \left[\frac{\bar{P}_i - P_{\min_i}}{\bar{P}_{\text{AIP}}} + \frac{\bar{P}_{i+1} - P_{\min_{i+1}}}{\bar{P}_{\text{AIP}}} \right] \right) \quad (2.29)$$

The IPR describes the total pressure losses between a survey plane upstream, which can be at free-stream or anywhere along the fuselage and the AIP. This makes its utilisation rather complicated as it not only describes the inlet but also the aircraft's fuselage aerodynamic performance. In highly embedded configurations, the other metrics are therefore preferred. The $DC(60)$ identifies a sectional distortion coefficient that measures the difference between the area-average total pressure $\bar{P}_{\text{AIP}}$

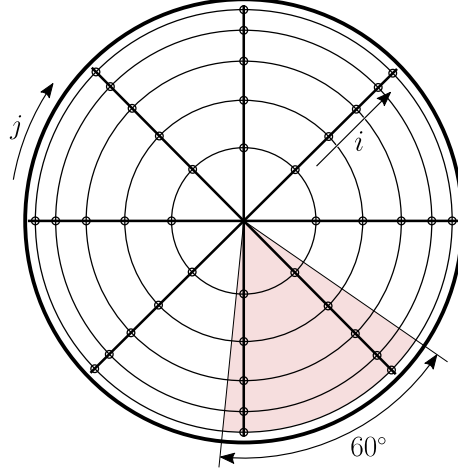


Figure 2.7: Rake and pressure probes arrangement with a fictitious critical sector of 60° , remastered from Ref. [79]

and the lowest total pressure in a 60° section of the AIP, the RDI describes the non-uniformities in the radial direction where $\bar{P}_{i=0}$ is the total pressure of the inner ring and $\bar{P}_{i=4}$ that of the outer ring, and lastly the CDI assesses the non-uniformity of the circumferential total pressure distribution at specific radial positions where $\bar{P}_i$ is the average total pressure of the i -th ring and P_{min_i} the minimum pressure along that same ring i . These metrics are favoured as they identify different portions of the flow subject to high total pressure defects, notably with a section, and the radial and circumferential distributions.

2.2.2.2 Propulsion Systems Performance

To describe the propulsion systems' performance, fan performance metrics can be equally applied from propulsors to gas-turbine engines. During the fan operation, its performance is described with isentropic and polytropic efficiencies that are related by the fan pressure ratio (FPR):

$$\eta_{isen} = \frac{\text{FPR}^{\frac{\gamma-1}{\gamma}} - 1}{\text{FPR}^{\frac{\gamma-1}{\gamma_{poly}}} - 1} \quad (2.30)$$

The isentropic efficiency (Eq. (2.30)) describes the ratio of ideal work to the actual work done or in another terms, the energy input to achieve a given pressure rise in inviscid flow versus real viscous flow. Independent of the size of the compressor stage, the polytropic efficiency η_{poly} describes an aerodynamic performance which lumped end-to-end assesses the penalties in the stage.

The propulsive performance of an engine does not simply reduce to its amount of propulsive force it generates but rather how the amount of energy provided by the fuel is converted into motion. As such, the propulsive efficiency, η_{prop} given in Eq. (2.31) describes the amount of work done on the aircraft from the amount of energy provided after consideration of the thermal efficiency η_{th} which describes how efficiently the power input is converted into power output in a power-plant [80]. Nonetheless, the reduction of η_{prop} with conventional approaches is often discussed as it involves the flaws associated with its value that can be greater than 100% for BLI.

$$\eta_{prop} = \frac{\mathcal{T} \cdot V_{\infty}}{\dot{\mathcal{E}}_P} = \frac{2 V_{\infty}}{V_{FF} + V_{FE}} \quad (2.31)$$

BLI is often seen as a means of increasing the propulsive efficiency, and translates to a lower specific thrust that is obtained from a smaller deviation between the outlet and inlet velocities, or a smaller inlet velocity (Eq. (2.31)). Taking into considerations the specificities of BLI, another form of propulsive efficiency is derived by Plas et al. [47] accounting for the momentum and energy BL thickness in its kinetic energy shape parameter form H^* and the ratio of ingested drag to the total aircraft drag β . Nonetheless, this expression is also ill-defined as when β and H^* respectively approach 0 and 1, Eq. (2.32) reduces to Eq. (2.31) and is also bounded by 2.

$$\eta_{prop} = \frac{1 + \beta}{\frac{H^*}{2} + \beta \left(1 + \frac{V_{NZ} - V_{\infty}}{2V_{\infty}}\right)} \quad (2.32)$$

In search of converging towards a better definition of the propulsive efficiency, Sanders and Laskaridis [18] introduce a propulsive efficacy, κ , defined in Eq. (2.33). It can be noted a difference in Eqs. (2.31) and (2.33) where the numerator could be assimilated similar by taking the difference in net axial force power between the unducted baseline and the BLI configuration but the denominator differs. Whereas both are considered as the power supply, Eq. (2.31) considers the net mechanical energy flow rate, $\mathcal{P}_K$ or $\dot{\mathcal{E}}_P$, whereas Eq. (2.33) accounts for the net propulsor shaft power $\mathcal{P}_S$. In the former definition, $\mathcal{P}_K$ heavily depends on the position at which the inlet and outlet stations are positioned [34] and do not account for the fan's blades rotational friction losses. On the other hand, $\mathcal{P}_S$ quantifies the overall amount of power consumed by the fan, including all kind of losses then generated to provide the useful work.

$$\kappa = -\frac{\mathcal{F}_{RN}}{\mathcal{P}_S} \cdot V_\infty \quad (2.33)$$

The negative sign is required by convention from CFD solvers for the computation of the relative force $\mathcal{F}_{RN}$. This metric offers a manifold of advantages: its maximum value is capped at 100% as opposed to the two previous definitions, its definition is analogous to Ref. [36] under the assumption of a uniform jet velocity, the power demand is used to account for all internal losses and therefore gets closer to a real definition of useful work divided by work input.

Nonetheless, κ still should not be mistaken with a *strict* definition of the propulsive efficiency as the numerator does not represent the exact useful work. Additionally, the denominator is not exact either but closer to the actual power input. Therefore, the metric is suggested over the others but the authors would like to draw attention to further developments. For a strict definition, the propulsive efficiency should be defined as the ratio of useful work over the power available for utilisation.

2.2.2.3 Aircraft-Level Performance

To evaluate the benefits of BLI over a conventional aircraft, it is essential to first relate the amount of propulsive force generated by the BLI propulsor to the overall requirement. As such, Valencia [81] introduces the concept of thrust-split (TS) which divides the relative net force provided by the BLI propulsors to the aircraft's overall force requirement:

$$\text{TS} = \frac{\mathcal{F}_{RN}}{\mathcal{F}_{A/C}} \quad (2.34)$$

To prevent any ambiguity with the force distribution amongst the different engines, net stream-wise forces are also used by Ref. [62] with Eq. (2.15).

To account for the propulsive efficiencies of both BLI and under-wing engines and their relative force generated, savings are evaluated from an energy standpoint with Smith Jr. [10] who introduced a power saving coefficient (PSC). The metrics contrasts the power consumption from a BLI configuration to that of a *some-sort* of equivalent podded aircraft for a given net axial force:

$$\text{PSC} = 1 - \frac{\dot{\mathcal{E}}_{\text{P}}}{\dot{\mathcal{E}}_{\text{P,ref}}} \quad (2.35)$$

The PSC appears as a common point of comparison and assesses the performance of a BLI configuration albeit being subject to several challenges. Initially, Smith Jr. defines the PSC for two equivalent propulsor disc areas but intrinsically in the case of BLI, the aft-mounted propulsor presents the advantages of reducing the size of the podded engines. This questions the equivalence that one would consider for the calculation of the PSC, e.g. same fan diameter or mass-flow rate. Thus, it results that albeit having a unique definition, the saving quantification is subject to the reference aircraft and engine considered.

In addition, it is common to define the PSC using $\mathcal{P}_K$ or $\dot{\mathcal{E}}_{\text{P}}$ [82, 83] although it is the authors' opinion that this should not be considered and rather $\mathcal{P}_S$ as discussed above for κ :

$$\text{PSC} = 1 - \frac{\mathcal{P}_{\text{S,BLI}}}{\mathcal{P}_{\text{S,A/C,ref}}} = 1 - \frac{\dot{m}_{\text{fuel,A/C,BLI}}}{\dot{m}_{\text{fuel,A/C,ref}}} \quad (2.36)$$

With Eq. (2.36), the net shaft power is used in the BLI and reference configurations to quantify the benefits of ingesting the BL. Further, the power is defined as the product between the fuel mass-flow rate $\dot{m}_{\text{fuel}}$ going through the engine's combustion chamber and the fuel heating value h_{fuel} . However, assuming the same fuel properties in both BLI and non-BLI aircraft, the PSC reduces to the form given by Eq. (2.36) and should be used in turbo-electric architectures (Section 2.5.1).

In search of alleviating the ambiguity related to the propulsion systems and the equivalence in the BLI and non-BLI scenarios, Baskaran et al. offer to substitute the power consumption from a non-ideal under-wing engine by a fictitious ideal propulsor whose consumption would be equivalent to the aircraft drag power (ideal propulsive efficiency), $\mathcal{F}_{\text{A/C}} \cdot V_{\infty}$. The definition of the PSC is thus traded for a strict definition yielding the minimum savings achievable by the BLI propulsor (Eq. (2.37)).

$$\text{PSC}_{\text{strict}} = 1 - \frac{\dot{\mathcal{E}}_{\text{P}}}{\mathcal{F}_{\text{A/C}} \cdot V_{\infty}} \quad (2.37)$$

2. Advancements and Prospects of Boundary Layer Ingestion Propulsion Concepts

Although Eq. (2.37) considers $\dot{\mathcal{E}}_p$, the tentative of using the isolated body aerodynamics for reference prevents the use of low- or high-order models to derive the power consumption of the propulsion system.

Conversely, at conceptual design, the isolation of the fuselage, or airframe, allows one to evaluate the potential for energy recovery (PER) by assessing the irreversibly lost energy that manifests itself as viscous dissipation, Φ . In that scenario, the maximum power is achieved with a fictitious propulsor that fills the entire BL without generating jet loss or affecting the surrounding flow and is compared against the force power of the fuselage, $\mathcal{F}_B \cdot \mathbf{V}_\infty$. PER is defined by Sanders and Laskaridis [18] and also later referred as $\text{PSC}_{\text{ideal}}$ by Baskaran et al. [84].

However, accounting for the irrecoverable thermo- compressibility effects with the volumetric pressure work Θ_∞ present in the flow, PER is later improved by Lamprakis et al. [69] in Eq. (2.38) discarding Sanders and Laskaridis' definition for completeness and exactness. Despite the direct relation between PER and PSC, PER grants an initial potential performance metric of the airframe where the traverse plane can integrate the viscous losses at various stream-wise positions along the airframe.

$$\text{PER} = 1 - \frac{\Phi_{\text{TE}} + \Theta_\infty}{\mathcal{F}_B \cdot \mathbf{V}_\infty} \quad (2.38)$$

Integrating on transverse planes behind the body, in the case of wake ingesting configurations, a complement to PER is defined by Mutangara et al. in which the available, i.e. recoverable, energy is tracked at various positions within the wake [85]. This metric allows one to evaluate the potential performance of an aerodynamic shape, but yet does not necessarily imply better savings, as given by PSC, with an installed configuration.

As an extension of the previously defined performance metrics, recovery of the thermal energy rate is also assessed. For instance, in the same vein as the PSC, an exergy saving coefficient (ESC) is defined in Eq. (2.39a) [68] by comparing the exergy saving from the initial supply (in both BLI and podded aircraft scenarios). This metric describes the improvement from the reference configuration through the installation of the BLI propulsion system, but also accounts for pressure field interactions. Therefore, by splitting the exergy into the mechanical and thermal

forms, the ESC can be directly related to the exergy waste coefficient (EWC) and exergy recovery coefficient (ERC) by Eq. (2.39b).

$$\text{ESC} = 1 - \frac{\dot{\mathcal{E}}_{\text{P}}}{\dot{\mathcal{E}}_{\text{P,ref}}} \quad (2.39\text{a})$$

$$= \text{EWC} \cdot \text{ERC} \quad (2.39\text{b})$$

Arntz [68] defines the exergy waste coefficient (EWC) under three different forms (two reported here), as it depends on the position at which the exergy potential is evaluated: in the far wake, for a powered configuration at equilibrium (Eq. (2.40a)) and for an airframe not subject to additional exergy supply (Eq. (2.40b)). Analogous to the energy-based methods, EWC echoes PER^c [85] or $\text{PSC}_{\text{ideal}}$ [84] by accounting for the thermal management and quantifies the amount of exergy that is destroyed, or the amount of total anergy that is produced.

$$\text{EWC} = \frac{\dot{\mathcal{E}}_m + \dot{\mathcal{E}}_{th}}{\dot{\mathcal{E}}_{\text{P}}} \quad (2.40\text{a})$$

$$= \frac{\dot{\mathcal{E}}_m + \dot{\mathcal{E}}_{th}}{\mathcal{F}_{\text{B}} \cdot V_{\infty}} \quad (2.40\text{b})$$

Lastly, the consideration of a device that is able to recover the exergy through its ingestion, e.g. BLI propulsor or heat exchanger, can be described by the exergy recovery coefficient (ERC) which evaluates how effectively the exergy present in the flow is recovered:

$$\text{ERC} = \frac{\dot{\mathcal{E}}_{\text{P,ref}} - \dot{\mathcal{E}}_{\text{P}}}{\dot{\mathcal{E}}_{m,\text{ref}} + \dot{\mathcal{E}}_{th,\text{ref}}} \quad (2.41)$$

Overall, PER and EWC appear as the metrics to be used early in the design phase to evaluate the potential of the configuration. When the propulsion systems are integrated, it appears clear that the PSC quantifies the savings enabled by the configuration relative to the reference considered. However, as stated by Hall et al. [86], the PSC is not an absolute metric and is rather circumstantial. Its value can vary widely depending on the baseline selected for comparison, and likely to be dependent on other secondary or unrelated differences in airframe design.

2.2.2.4 Mission-Level Performance

In addition to the performance and savings that can be claimed at design points, the integration of those over an entire flight mission allows the benefits to be compounded and computed in terms of fuel savings or extended mission range. Particularly, with the Transport Aircraft System OPTimization (TASOPT) framework developed in Ref. [87], the aircraft's weight, and aerodynamic and propulsive performance are predicted from low-order models. The design-mission is segmented in its different phases where the thrust requirement and weight loss are tracked along the change in altitude and range. At any profile point, the climb and weight-loss gradients are computed and suitably integrated over the mission to yield the altitude and weight profiles. The trajectory equations are then integrated resulting in a form of the Breguet range equation (Eq. (2.42)) and allowing for the fuel burn to be interpreted from the difference in aircraft weight from take-off to landing.

$$\mathcal{R} = \frac{C_L}{C_D} \frac{V_\infty}{\text{TSFC}} \ln \left(1 + \frac{W_{fuel}}{W_{A/C}} \right) \quad (2.42)$$

The mission range is function of the lift-to-drag ratio, the flight speed in the RRF V_∞ , the thrust-specific fuel consumption (TSFC) which divides the fuel-flow by the amount of thrust generated and describes the efficiency of the fuel to produce the output force, and the weights of the fuel carried W_{fuel} and aircraft $W_{A/C}$.

Subsequently, from the product between the TSFC and the flight velocity, the power-specific fuel consumption (PSFC) implements the power balance [33] into the range equation to yield the power-based Breguet equation for both non-BLI and BLI aircraft, Eqs. (2.43) and (2.44) respectively. The range comparison between the two aircraft advises on the range extension suggested by BLI, or the fuel savings granted for an equivalent range to the non-BLI aeroplane.

$$\mathcal{R}_{\text{non-BLI}} = \frac{C_L}{C_{\Phi_{\text{non-BLI}}} + C_{\dot{\epsilon}_v}} \frac{1}{\text{PSFC}_{\text{non-BLI}}} \ln \left(1 + \frac{W_{fuel}}{W_{A/C}} \right) \quad (2.43)$$

$$\mathcal{R}_{\text{BLI}} = \frac{C_L}{C_{\Phi_{\text{BLI}}} + C_{\dot{\epsilon}_v}} \frac{1}{\text{PSFC}_{\text{BLI}}} \ln \left(1 + \frac{W_{fuel}}{W_{A/C}} \right) \quad (2.44)$$

By convention, the specific fuel consumption describes a form of efficiency of the system but when BLI is involved, the metric does not yield much information. Indeed, a BLI aircraft may have a higher TSFC than a tube-and-wing aircraft but as its thrust requirement is lowered by the reduction in the aircraft's wake, the overall fuel consumption may be lower. Consequently, with the gain in popularity of the energy-based methods, the PSFC aims to alleviate these ambiguities. Nevertheless, neither the TSFC or the PSFC account for the installation effects and additional penalties associated. Although the definitions do not consider either any other form of energy that could eventually be used to power the BLI propulsion systems such as used in hybrid-electric architectures [88–90], the NPF and power supply can substitute the thrust generation and power consumption from the propulsion system to take into account these additional losses.

In this perspective, Seitz and Gologan [48] define the energy-specific air range (ESAR) which indicates the change in range per change in energy independently of the source of energy used:

$$\text{ESAR} = \frac{d\mathcal{R}}{d\dot{\mathcal{E}}_P} = \eta_{ov} \cdot \frac{L/D}{W_{A/C}} \quad (2.45)$$

They demonstrate the strong sensitivity to the wing aspect ratio and Reynolds number, and conversely the weak correlation with the design Mach number, or fan efficiency. The metric quantifies how efficient is the aircraft compared to the amount of energy used $\dot{\mathcal{E}}_P$ by the systems for a specific range $\mathcal{R}$, or alternatively by how much the range can be extended with the same supply. The ESAR is also defined as a function of the aircraft overall efficiency, lift-to-drag ratio, and aircraft weight but no explanation is retrieved on the way drag is calculated and as previously stated, that quantity is not directly extractable. Therefore, alternative means have been defined and follow.

In the same vein, for quantifying the aircraft performance along the mission, Hileman et al. [91] introduce the payload fuel energy efficiency (PFEE) which quantifies the amount of energy, contained within the fuel, that is consumed relative to the payload, in cargo or passenger aircraft. The authors also offer to use the PFEE as an environmental performance metric with the example of a carbon dioxide intensity metric that would divide the emissions of CO_2 by the product of PFEE and the fuel energy content per unit mass. The concept is further investigated by Sato who redefined the PFEE as the payload fuel energy intensity (PFEI) in Eq. (2.46)

that can also be re-written as a function of the aircraft aerodynamic and propulsive performance using a modified Breguet range equation in its thrust or power balance terms.

$$\text{PFEI} = \frac{W_{fuel} h_{fuel}}{W_{payload} R} \quad (2.46)$$

From an exergy standpoint, Arntz et al. [35] define a range equation $\mathcal{R}_\mathcal{E}$, alike with the energy-based formulation, with the exergy specific fuel consumption (ESFC), the lift, the anergy generation coefficients and the weights of the empty aircraft and the fuel carried:

$$\mathcal{R}_\mathcal{E} = \frac{C_L}{C_{\dot{A}^*}} \frac{1}{\text{ESFC}} \ln \left(1 + \frac{W_{fuel}}{W_{A/C}} \right) \quad (2.47)$$

It is opined that Eq. (2.47) is akin to Eqs. (2.43) and (2.44) with the substitutions of the energy terms for exergy terms, thus considering the thermal energy aspects. Whereas increasing the thrust-, power-, or exergy-specific fuel consumption would exhibit a larger range, in the energy- and exergy-based book-keeping schemes, the objective is rather to reduce the losses associated, either in the form of Φ or $\dot{A}^*$, respectively in Eqs. (2.44) and (2.47).

At a mission level, the range equation to consider is circumstantial as it depends on the approach considered for the study. As such, all range equations are considered equivalent but for the reasons previously exposed, Eqs. (2.42) and (2.45) are ambiguous as they depend upon the lift-to-drag ratio whereof drag is not extractable. Whether the energy or exergy analyses are followed, viscous dissipation or total anergy generations appear as more pragmatic and the difference in ranges quantify a form of mission savings.

2.2.2.5 Discussion on Figures of Merit

The numerous metrics retrieved from the literature appear to present different levels of implementation or practicality. In their work, Ref. [32] rates their reviewed metrics by degrees of universality and applicability at conceptual design phases based on weighed sub-criteria. As summarised in Table 2.3, the energy/exergy-based approaches feature higher degrees of universality but lower

applicability to conceptual design phases due to their high computational demand. However, at refined design phases, these methods aid in the interpretation of the physical mechanisms underpinning BLI propulsion.

At any given point along the flight envelope, the PSC appears as the most versatile figure of merit to quantify the savings enabled by BLI relative to the considered reference. However, as mentioned, this presents several challenges from which a common reference aircraft is necessary to compare different novel aircraft as well as the means of quantifying the amount of power required in both scenarios. Equation (2.36) is thought to best describe the power savings considering the shaft power of the BLI propulsion system due to the caveat related to $\dot{\mathcal{E}}_P$. In concert with the PSC, the TS should act as a trade-off metric between the propulsive force share from the thruster and the savings claimed (in thrust-splitting architectures).

Extending from the PSC for considering the entire flight envelope, the difference between the novel aircraft and its reference can either be calculated in terms of extended range (for the same fuel/power carried) or fuel block savings (for the same design range). As such, either Eqs. (2.43) and (2.44) or Eq. (2.47) can be used but the ESAR allows to combine both changes (Eq. (2.45)). However, the authors only retrieved Ref. [48] discussing the metric and it seems that no other reference applied it.

2.3 Numerical Methods and Models

2.3.1 Numerical Methods

To evaluate a vehicle's performance, it can either be performed from a scaled-down wind-tunnel model or a numerical geometry based on aerodynamic and propulsive models. Inherently for the latter, the performance is subject to the modelling fidelity and the time allocated to perform those assessments. A trade-off between the degree of fidelity and computational cost of the investigations must be found giving rise to the development of more robust and developed low-order models. This section therefore introduces several methods that are commonly used in the evaluation of BLI aircraft, notably with the breakdown of the drag components as well as the different flow features present in the flow-field.

2.3.1.1 Drag Decomposition

In the analysis of the aerodynamic forces, their decompositions allows one to capture the phenomenology into different components Drela. compares different breakdown methods and categorises them into different attributes: exactness, consistency, and uniqueness [92]. The treatment of the *near*- and *far-field* integrations is achieved by different methods considering tightly integrated engine- airframe configurations.

Near-field Forces

The *near-field* examination of forces on physical surfaces consists in attributing local forces to either the pressure or friction components:

$$\mathcal{F} = \underbrace{\oint_{S_B} -p\hat{n} \, dS}_{\mathcal{F}_{\text{pressure}}} + \underbrace{\oint_{S_B} \bar{\tau} \cdot \hat{n} \, dS}_{\mathcal{F}_{\text{friction}}} \quad (2.48)$$

Alternatively, a recasting of the *near-field* investigation with the actual forces on the bodies, into a flux definition enables a further component to be included in the breakdown:

$$\mathcal{F} = \underbrace{\oint_S -p_G\hat{n} \, dS}_{\mathcal{F}_{\text{pressure}}} + \underbrace{\oint_S \bar{\tau} \cdot \hat{n} \, dS}_{\mathcal{F}_{\text{friction}}} - \underbrace{\oint_S \rho(\mathbf{V}\mathbf{V}\hat{n}) \, dS}_{\mathcal{F}_{\text{flux}}} \quad (2.49)$$

This integration is performed on permeable inlet and outlet stations defining a control volume surrounding a body. Conventionally, this form emanates from the momentum conservation whereby the stream-wise $\mathcal{F}_{\text{flux}}$ is often unjustly characterised “thrust”. In addition to the previous reasons given on the debatable designation of thrust, the reference pressure used to define the gauge pressure p_G brings another ambiguity in BLI scenarios as to how should the reference pressure be defined (free-stream, integrated at a given location along the airframe...).

To enable a refined interpretation of the experienced force by the body, a novel *near-field* decomposition method is proposed [93] which separates the pressure field into a constitutive dissipative (viscous, $\mathcal{F}_{p,\mu}$) and non-dissipative (Euler, $\mathcal{F}_{p,E}$) contribution. Equation (2.50) decomposes the viscous and Euler components along

the body’s surface. The universality of the partial pressure field (PPF) analysis [93–95] allows one to couple it to momentum- and energy-based analyses, similar to the velocity decomposition proposed in Refs. [73, 96]. With a more robust decomposition of force terms, Mutangara et al. [97] improves the velocity decomposition approach by coupling it with PPF and demonstrates its applicability with an assessment, understanding and tracing of the phenomenological sources of aerodynamic drag on sub- to transonic flow regimes over a NACA 0012 aerofoil.

$$\mathcal{F}_{\text{pressure}} = \underbrace{\oint_{\mathcal{S}_B} -p_E \hat{n} \, d\mathcal{S}}_{\mathcal{F}_{p,E}} + \underbrace{\oint_{\mathcal{S}_B} -p_\mu \hat{n} \, d\mathcal{S}}_{\mathcal{F}_{p,\mu}} \quad (2.50)$$

“Ingested Drag”

Several references [47, 48, 64, 98] refer to an “ingested drag” quantity $\mathcal{D}_{ing}$ that is computed at the inlet station of the BLI fans using Eq. (2.49). They all agree on defining the quantity as the amount of drag captured inside the internal stream-tube that crosses the fan, as the perturbations emanate from the development of the BL around the airframe. Following that reasoning, the profile drag ingested appears as a positive thrust contribution and can be interpreted as a reduction in ram drag. Here, the authors disagree as an effect from BLI is the attenuation of the aircraft’s wake and not its drag. Inherently, depending on the size of the BLI fans integrated, the portion of $\mathcal{D}_{ing}$ can be outweighed by the penalties caused by the nacelle.

Far-field Forces

An extension of the control volume defined in Eq. (2.49) recasts the *near-field* force expression into a *far-field* definition. Whereas the previous definition can be applied to ④ from Fig. 2.4, the *far-field* integration is performed on ① but yields the same information as juxtaposing several smaller CV to form ①.

From this point of view, the forces can be subdivided in more fashions with the profile (skin-friction, or lift-independent), wave, and induced (lift-dependent, or vortex) force components.

In the wake, surface integrals can be extracted by identifying the change in total energy, through enthalpy terms, as the thrust, and the generation of chaos and losses, with entropy terms, as the profile, wave, lift-induced and interference drag [51, 99]. From this decomposition, Gauss' theorem transforms the surface integrals in volume integrals which can be related back to the local physical mechanisms within the flow-field. Nonetheless, this process requires the separation of the shear layer, shock, vortex and jet regions from the entire flow-field (Section 2.3.1.2) that respectively correspond to the profile, wave and lift- induced drags and thrust [55].

More specifically for the profile drag prediction of an isolated fuselage, Nicolosi et al. [100] investigated slender fuselage geometries in CFD. They divided up the fuselages into three sections - nose, cabin and tail, isolating fuselage drag coefficients. This method proves its importance at a conceptual stage where estimates must be made on the fuselage shape.

Additionally, other volume integral approaches [50, 56] use surface integrals to approximate the lift-induced drag. Nonetheless, the complexity in the extraction is detailed by Spalart [101] who calls into question the rigour to which lift-induced drag can be defined. Méheut et al. [57] compare different profile and induced-drag definitions that despite providing an accurate phenomenological breakdown do not apply to all configurations and therefore suggest and test theirs both numerically and experimentally.

An energetic/exergetic decomposition discards the artificial concepts like ingested drag and enables a physical interpretation of the contributions by investigating the power sources and sinks. However, these methods only treat the stream-wise component and Lamb vector methods can be employed to approach the lift forces [51]. Notably, the viscous component present in shear layers is isolated (and can be combined to the wave component to characterise the profile force). A drag breakdown of an aerofoil and a wing is offered by Aguirre and Duplaa [103] along with exergetic drag curves and a complement with a *far-field* wave anergy extraction suitable for wind-tunnel applications. However, the formulation of the exergy method [35] does not allow for drag prediction and decomposition in wind-tunnel testing and a velocity decomposition technique is proposed to suit an exergy analysis [96].

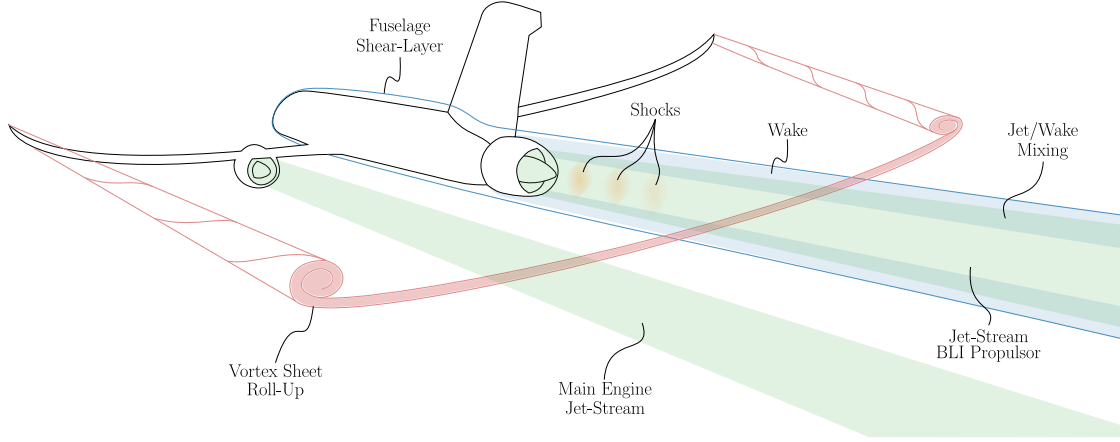


Figure 2.8: Flow features around a propulsive fuselage aircraft

2.3.1.2 Flow Features Extraction

To attribute the benefits and penalties to particular mechanisms in the flow, their identification in numerical studies is achieved by different formulations. First, in BLI, it is vital to qualify the BL and quantify its quantities before its (partial) ingestion. In parallel, the different flight conditions and aircraft geometries affect the flow that develops around the airframe. As such, it is also possible to identify and extract regions of the flow where major accelerations are experienced and generate shocks, produce a propulsive force or form vortices. Figure 2.8 depicts the flow features extractable in CFD by different means discussed hereafter.

(Quasi-)1D Boundary Layer

At conceptual-level, to prevent the resolution of the Navier-Stokes equations in CFD, Goldberg et al. [104] propose a method to evaluate the performance of a propulsion system by using BL theory and one-dimensional gas dynamics. On their BWB aircraft, the power law [105] that defines the BL velocity profile and thickness over a flat-plate is used to approximate the airframe upper surface. After a validation procedure to assess the exactness of the developed method when compared to CFD, a 0.2% difference in total pressure losses is reported and 3.8% in the captured height of the incoming flow on the NASA N3-X [104]. Nevertheless, it should be stressed that the model, despite being able to capture relatively well the inlet flow conditions at a lower cost, is based on flat-plate theory and is only suitable for BWB-like configurations not subject to strong pressure gradients on curved surfaces and whereof installation effects must be accounted for [106, 107].

Alternatively, still in the scope of reducing the computational cost, Ref. [108] proposes a coupled method which makes interactions between an aircraft sizing tool and an engine cycle model. The aircraft model is fed into a potential flow solver that filters out the viscous region of the flow and defines the edge of the BL. The streamlines exported from that initial run help define the BL properties such as the thickness and momentum defects to describe the inlet properties. The engine cycle model then computes the reduced ram drag and inlet pressure losses to assess the performance of the propulsion system. For validation, the results are compared with experimental data of the MIT D8 [109] and give a 4.9% discrepancy, at the centreline, in BL thickness and 2.3% in kinetic energy defect.

In a similar vein, following a layered strategy, Kaiser et al. [110] developed a quasi-analytical method based on the application of several theories to account for the pressure gradients and installation effects. To start with, potential theory, alike Ref. [108], provides the pressure gradient along the body. Then, boundary layer theory is used and feeds the actuator disc and blade element theories to consider installation effects, from which the former advises on the effects upstream and downstream of the fan, and the latter provides a radial work distribution across the fan stage.

Shear Layers

As an alternative to using inviscid, or potential flow solvers, one can identify the shear layer regions from viscous compressible CFD. Ultimately, when relating to the shear layer, the δ_{99} definition arises as to the first method which identifies any point normal to the surface that reaches 99% of the free-stream velocity - or, of the temperature for the thermal layer. Nevertheless, besides flat-plate theory, this method is argued when changes in curvature and adverse pressure gradients are experienced and δ_{99} rather takes the definition of any point normal to the surface that reaches 99% of the edge velocity. This, on the other hand, implies the BL edge to be identified and its velocity to be extracted.

Lovely [111] examines vorticity to calculate the BL momentum thickness from CFD but despite being powerful on simple bi- and three-dimensional geometries, it proves to be impractical for complex geometries and unsteady simulations [112]. Consequently, rather than considering integral methods, scalar expressions are also employed by Lovely [111] with entropy and vorticity but present the drawback of

also capturing regions like shock waves. Baskett and Haines [112] developed an approach which iteratively calculates a conservation equation yielding a shear layer identification in both laminar and viscous flows and successfully omitting shocks despite the relatively high computational demand from the iterative process.

From an energy point of view, Sanders and Laskaridis normalised the viscous dissipation Φ by the absolute volumetric pressure work and coupled it to the normalised turbulent viscosity factor which metric can capture the laminar and viscous zones as well as filter the shocks without an iterative procedure [18]. Despite identifying correctly the viscous layer, some deviations appear with complex geometries (powered aircraft):

$$\xi = \left(\frac{\mu + \mu_t}{\mu} \right) \frac{\overbrace{(\nabla \cdot \bar{\bar{\tau}}) \cdot \mathbf{V}}^{\Phi}}{|p \nabla \cdot \mathbf{V}|} \quad (2.51)$$

Shock Wave

The shock waves identification in CFD is not as trivial as locating flow regions whereby the local Mach number rises to unity. The discontinuity and mesh definition make difficult the identification of the region which thus requires the consideration of the gradient. For that matter, Lovely and Hairnes [113] developed an algorithm capable of identifying shocks in steady and transient conditions from one- to three-dimensional models. The implementation of a filter prevents the identification of falsely located shocks which result from numerical discrepancies. A first algorithm treats the flow-field by identifying regions whereby a shock test scalar is unity and rejects a subset of surrounding grids that make-up that surface. Alternatively, another algorithm filters out grid elements that fall below a particular pressure gradient magnitude threshold but requires an appropriate definition of that threshold. The last technique enquires jumps in density and temperature around the shocks. After comparing the three different techniques, Lovely and Hairnes conclude that additional filtering is not as effective as a sole pressure gradient magnitude threshold of 0.95 on ζ [18, 71]:

$$\zeta = \frac{\mathbf{V} \cdot \nabla p}{a |\nabla p|} \quad (2.52)$$

Recently, Saetta and Tognaccini [114] implemented an unsupervised algorithm to identify the viscous and wave regions of the flow-field by use of machine learning. Unlike the deterministic methods reviewed above, the regions are identified in their work without input thresholds. These efforts tested on aerofoils and wings on subsonic and transonic regimes enable the flow regions to be isolated without visual inspection from the user [114] .

Vortex

To evaluate the influence of the lift-induced drag, source of noise and energy loss, that results from the difference in pressure between the pressure- and suction- surface of any lifting body, it becomes necessary to identify the structures formed from the shed vorticity at the wing-tips. Soto-Valle et al. [115] compare different vortex identification methods and differentiation schemes, notably with the experimental work of Graftieaux et al. [116] using particle image velocimetry (PIV) and Hunt et al. [117] whose method is based on vorticity magnitude.

Velocity gradient-based vortex identification methods are notably the most well-known, with popular examples such as the Q- and λ_2 -criterion. To concisely elaborate further on these, the Q-criterion [117] defines a vortex as a connected fluid region which pressure is lower than the ambient and which second invariant is positive, i.e. greater vorticity than strain-rate magnitude ($Q > 0$) [118]. On the other hand, the λ_2 -criterion defines a vortex as a region whereof the value of λ_2 is negative; which is achieved by identifying a region of minimum local pressure which entrains the vortex [119]. Both methods do carry their own inherent limitations in particular scenarios discussed in Ref. [118] which should be considered during application. A more detailed description of other types of identification methods can be found in Refs. [51, 115, 120].

Jet-stream

Whereas the preceding methods identify the mechanisms responsible for *drag* with its profile, wave and lift-induced components, enthalpy aids determining the region responsible for *thrust*. Most references [55, 56, 99] favour a force decomposition rather than the identification of the whole jet-plume however, in configurations not subject to heat addition from the airframe, the propulsor is the only source of

stagnation enthalpy raise therefore it appears logical to build an identifier on this metric. Sanders and Laskaridis [18] isolate the jet-plume volume from a large deviation in total enthalpy when compared to the free-stream condition. Nevertheless, they report the importance of the threshold as shear stresses can cause a redistribution of the free-stream enthalpy outside of the jet region, particularly when the wake of the BLI aircraft merges with the jet. A 10% deviation in total enthalpy is found to be insensible to changes in jet-stream viscous dissipation rate, Φ , and volumetric pressure work, Θ and thus defines the identifier, Υ :

$$\Upsilon = \frac{\Delta H}{H_\infty} > 0.1 \quad (2.53)$$

2.3.2 Aero-Propulsive Numerical Modelling

In numerical simulations and more importantly with regards to BLI, fully coupled multidisciplinary modellings are essential [42, 62]. The tight integration of the propulsion system within the airframe entails the use of a coupled aerodynamic and propulsive model to study the aero-propulsive benefits of BLI. Formerly, uncoupled analyses were performed in which the aerodynamic performance was assessed without any propulsion model [26, 121, 122], or the propulsion system was evaluated without the aerodynamic performance [28, 98, 123]. With the never-ceasing development of numerical solvers, the aerodynamic and the propulsion are weakly- or strongly-coupled [42] and the propulsion model can either be a 0D/1D thermodynamic cycle that iteratively communicates with the solver or with boundary conditions (BC), actuator discs (AD) or body-force models (BFM), if not modelling the full rotating fan.

To numerically model the different BLI concepts, one needs to design the airframe, whether it be in two or three dimensions, generate its structured or unstructured mesh, and model the aerodynamic of the flow and propulsion system with BC, AD, BFM, or single passage or full annulus fan. Different CFD software allow for externally communicating with thermodynamic cycles which can interact and iteratively update the propulsion system model back to the CFD. The numerous aero-propulsive modellings from the references reviewed in this work are categorised in Fig. 2.9 for different fidelities. Additionally, a review from

2. Advancements and Prospects of Boundary Layer Ingestion Propulsion Concepts

Wind-Tunnel	[132]			Columns: Propulsion System Models Rows: Aerodynamic Models [X]: Propulsive Fuselage Concepts [X]: Over- and Rear-Fuselage Engines Concepts [X]: Disruptive Propulsive Concepts			[180] [76] [20, 83, 184]
CFD URANS, LES, DNS							[181] [185] [122, 186, 187]
CFD RANS 3D	[130] [131] [70] [121] [122]	[135, 136]	[142] [147] [149] [150, 151] [152] [153, 154]	[157] [156] [160]	[161] [165] [167] [19, 27] [130] [76] [82, 166]	[169, 170] [171, 172] [176] [21, 173, 174] [36] [149] [175]	[177] [178, 179] [182, 183] [185] [122, 186, 187]
CFD RANS 2D	[70] [134]	-----	[143–145] [48] [148] [146]	[61] [155] [158] [159]	[162] [163, 164] [49, 158] [72] [110]	[168]	-----
CFD Euler	[130] [121] [133]		[108]			[47]	
Vortex Panel and Correction	[26] [121]		[138] [139] [104, 106, 107] [140] [141]				
Empirical Model		[22]	[124] [125] [126] [127] [128, 129]				
No Model			[28] [98] [123]				[137]
	No Model	Empirical Model	0D/1D Cycle	Boundary Condition	Actuator Disc	Body-Force Model	Propulsor

Figure 2.9: References mapped out with their respective aerodynamic models (y-axis) and propulsive models (x-axis)

Menegozzo and Benini [43] focuses on the numerical modelling approaches along with their methods. They collate the studies and characterise the geometric, aerodynamic and propulsion models.

First, in the modelling of the airframe, different numerical degrees of fidelity can be designed. For two-dimensional axi-symmetric studies, a common practice relies on the extraction of a reference aircraft contour that accommodates a rear-mounted propulsor [143–145]. Alternatively, the fuselage can be approximated with an elliptical nose, cylindrical centre body, S-shaped curve leading to the propulsor and aft-cone downstream of it such as in Refs. [61, 155, 159]. Habermann et al. [155] approximate an A330-300 in such a way that it prevents shocks on the outer

contour from previous work by Seitz and Gologan [48] and Bijewitz et al. [163, 164]. In a similar vein, Battiston et al. [159] and Matesanz-García et al. [61] approximate the fuselage using either circular arc and Bell-Mehta curves [188] for the former or iCST curves [189] for the latter to define the propulsor region. It is opined that only BLI concepts based on a tube-and-wing aircraft baseline can be approximated with models in axi-symmetric two dimensions and all other conceptual designs require three dimensions bringing additional challenges in the modelling, or 2D planar investigation for particular cut-through planes.

The 3D modelling of the NASA STARC-ABL aircraft [22] relies on openVSP [190] which is a 3D modular modelling software to generate and optimise geometries [21, 169, 170, 173, 174] that are meshed volumetrically via mesh morphing [191]. Nonetheless, although being acknowledged for accurately predicting the physics, structured meshes are computationally expensive and rather complicated to generate. Instead, over-set meshes are often preferred for their *simple* implementation which allow the model to be split down into several sub-domains individually meshed such as with the ONERA NOVA [19] using Pointwise [192], Refs. [131, 146, 156] with STAC-CCM+ [193], Refs. [194] with ANSYS ICEM [195, 196] or using Cart3D [197, 198], AutoGrid [199] or GSMH [200].

The choice made in the meshing structure is critical as it impacts the accuracy of the results obtained in CFD. For instance, structured meshes are favoured for refined grids to capture the different aerodynamics effects close to the surfaces like viscous dissipation. The choice in turbulent modelling is also vital as it drives the mesh quality requirements notably with the y^+ to solve the viscous layers or decompose the pressure field. To overcome these potential issues, it is recommended to perform grid and domain sensitivity studies following procedures such as suggested by Celik et al. [201].

As per the numerical simulations, a variety of commercial software and in-house codes are used. To cite a few, ANSYS Fluent [202], openFOAM [203] and ONERA elsA [204] are common but DLR Tau [205] or ANSYS CFX [206] can also be named. Based on differentiated adjoints, ADflow [207] is extensively used on the NASA STARC-ABL [21, 62, 161, 169, 170, 173, 174]. Although most work is based on steady computations, motivations are found in the unsteady resolutions [181, 186, 187].

On the propulsion side, BC's are common practice whereby the fan-face is accounted as a pressure outlet whereas the fan-exit ensures mass continuity through the propulsor stage with a balanced mass-flow inlet [61, 143, 144, 155, 159]. With BC, the propulsion system can incorporate a thermodynamic cycle, which most widespread models are called GasTurb [208] used in Refs. [48, 49, 140, 148, 158, 163, 164, 167] or OpenMDAO with pyCycle¹ [21, 62, 173, 174] which is based on NASA's software NPSS [209]. The outflow from the fan-face is fed into the 1D cycle and iteratively calculates the engine properties communicated back to the CFD solver at the fan-exit. To virtually take into account the effects of the blades that are not captured by the BC, immersed boundary conditions have been developed and implemented by the ONERA [160, 210, 211].

Alternatively, and increasing the fidelity of the solutions, actuator discs [35, 161–164, 212] or body-force models [21, 149, 168, 169, 172, 173, 176] with source terms can be used to simulate the fan effects.

The most computationally demanding representation of the propulsor is the full-annulus blade model of the fan which requires a fine meshing of each blade passage [137, 177, 179, 183, 213] but allows an accurate representation of flow blockage and swirl with RANS or unsteady RANS.

2.3.3 Direction of Numerical Analyses

As given by Fig. 2.9, a multitude of numerical studies have been conducted in the past two decades. It appears clear that high fidelity is often sought, and more with years, with RANS modelling and actuator discs or body force models. Nonetheless, *simple* propulsive modelling as provided with the boundary conditions often suffices to focus on aerodynamic behaviours and flow mechanisms. There is however a distinct progress towards higher modelling with 3D RANS and BFM as preliminaries have matured. The choice of models is rather circumstantial and highly depends on the objectives of the studies. For example, BC satisfy most propulsive conditions and enable a simple representation whilst still studying aerodynamics effects. On the other hand, BFM are likely chosen to study the flow through the fan stage with the transport of the distortion to the exhaust and jet plume.

¹<https://github.com/OpenMDAO/pyCycle>

2.4 Experimental Apparatuses and Practices

Whilst computational fluid dynamics constantly evolved to accurately represent the flow phenomena, numerous experimental studies have been performed on sub-scale models , and have been subject to the following challenges:

- the similitude between the full- and sub-scale models is not ensured [214];
- the propulsor emulation and operability cannot be directly scaled;
- the instrumentation requires calibration and sometimes correction;
- the utilisation of sparse measurement might require coupling with CFD (data fusion).

2.4.1 Aircraft Models

A major difficulty in wind-tunnel experiments is the scaling effects [215]. Not only geometric, but also kinematic and dynamic similarities need to be ensured for similitude which require the model to be identically scaled, non-dimensional numbers such as Mach, Reynolds and Prandtl, to be equivalent and flow-field properties like pressure or drag coefficients to be the same [216–218]. Even though the free-stream Mach number and geometry scaling are often possible, it is common to note a Reynolds mismatch which prevents similitude [83, 219, 220]. Notably, a review on sub-scaled models and similitude theory on structural and vibratory components is provided by Coutinho et al. [221] but also applies to aerospace. With regards to the scaling of aerospace models, a reasonable state-of-the-art scaling for subsonic flows on small Reynolds numbers is findable but degrades as testing approaches transonic conditions and higher Reynolds numbers [215].

In addition to the testing conditions, the wind-tunnel facility and model support also play a detrimental role in the accuracy of the results obtained. The influence of the walls on the surrounding needs be accounted with for example different cross sections to prevent the pressure field to be affected or transpiration to attenuate the development of the walls' boundary layers. Also, the mounting supports need to be

cautiously selected depending on the BLI aircraft architecture to not capture losses or disturb the flow around and behind the model (Section 2.A.1).

When conventional aircraft are experimentally tested, the development of the boundary layer around the airframe and inside the podded engines' intakes is of minor importance whereas it is the essence of BLI. If the aircraft is linearly scaled and equivalent flight conditions are replicated, the Reynolds number will mismatch as well as the BL development impacting the flow properties at the AIP. When similitude is not respected, mainly from Reynolds mismatch [222, 223], corrections on scaling are necessary [215, 224, 225]. To reduce those corrections, the NASA N2A-ExTE [132], MIT-Aurora-Pratt & Whitney D8 [20, 76, 83, 109, 184] and also propulsive fuselages [180, 220, 226] attempted to mitigate this mismatch by reducing the free-stream velocity of the testing to match the Reynolds number albeit producing a Mach number difference. This unavoidably impacts the compressibility effects and affects the flow that is ingested by the propulsor.

Inherently, all this affects the operability of the BLI propulsor and an additional challenge lies in its own scaling. Indeed, if comparison is performed between CFD and experiments and both between an aircraft flying at flight conditions and its sub-scaled version at wind-tunnel conditions, the amount of mass-flow ingested needs to be compared [46]. As mentioned, if similitude is not ensured, the amount of energy imparted into the flow (from an ARF standpoint) is different and the mass-flow needs to be scaled accordingly to the cost of the fan operation (distortion) and its emulation with the potential savings stemming out. Whilst commonly a fan is used within a nacelle, additional concerns might be brought to the discussion if the fan faces high amount of distortion [76] and the blades are not designed to sustain it. Instead, the fan can be substituted with an ejector-powered engine simulator from which high-pressure gas pumps the ingested flow [227, 228]. Despite its effectiveness in podded engines to generate thrust, no experimental data have been found on BLI in the literature despite an on-going project [229]. Nevertheless, additional challenges emerge with the importance of designing the assembly to minimise losses. The addition of mass-flow in the system also necessitates the control volume calculations to consider these extra source terms in the wake and requires the book-keeping schemes (e.g. power balance) to be adapted.

2.4.2 Measurement Techniques and Instruments

2.4.2.1 Force

Through the years, the set-ups and instrumentation never ceased to evolve and led to more practical performance evaluations. To prepare for future research campaigns, Atinault et al. [165] at ONERA conducted a synergistic experimental and numerical study of a movable electric ducted fan ingesting the wake from a body of revolution at low-speed conditions. The aerodynamic forces experienced by the body are measured from a floor balance whereas the thrust generated is monitored from a six-component balance mounted on the engine. Blunt bodies of different sizes are modelled to ensure a constant BL area that corresponds to that of the engine's fan face. As for the engine, it can be positioned axially from the body's trailing-edge to 500mm behind it, and radially to ingest different mass-flow rates - radial offset from the fuselage's centreline (Section 2.A.2). Similarly, Sabo and Drela [230] tested another body of revolution with an equivalent approach and came to the same conclusions that maximum power savings are achieved for a propulsor positioned at the trailing-edge of the fuselage with a PSC of 22% for Atinault et al. and 25% for Sabo and Drela. In both scenarios, the further away axially and radially, the lower the BLI benefits as energy dissipates and more free-stream air is ingested. Unlike the main balance used by Refs. [20, 83, 165], forces on the model were measured by a load cell that can only evaluate the axial component. Nonetheless, the objectives of Ref. [230] were to obtain a net-zero stream-wise force and evaluate the benefits of BLI and the load cell fitted that purpose.

2.4.2.2 Pressure

With regards to the pressure measurements, they are of high importance to quantify the amount of mass-flow that is ingested by the BLI propulsor, the total pressure distortion at the AIP, the total pressure at the nozzle and within the wake, and the pressure distributions to validate *near-field* forces along surfaces. A simple arrangement of pressure rakes is its fix mounting on the model where guidelines are provided in Ref. [75] on military intakes - BLI was not considered at the time of writing. The rake disposition agrees with the one discussed in Section 2.2.2.1 and illustrated in Fig. 2.7. Nonetheless, the fixed position of the arms and probes provides relatively sparse data which needs to be interpolated and therefore reduces

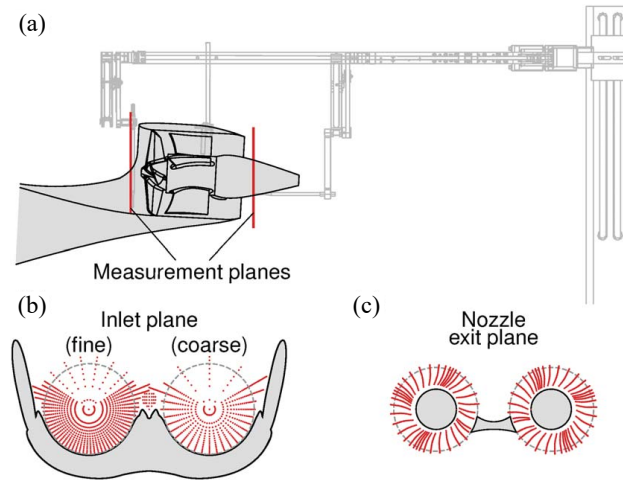


Figure 2.10: Experimental (a) model-mounted survey systems and measurement plane locations, (b) measurement grids for BLI inlet and (c) nozzle exit surveys, from Ref. [76]

the accuracy of the measurements. As a solution, rotating rakes have been developed [231] in which the arms move altogether to sweep the AIP plane and obtain a more populated data coverage (Fig. 2.10) [20, 76, 83, 109, 184]. The drawbacks from this practice nonetheless is the time required to measure all data in the wind-tunnel and the potential flow perturbations (Section 2.A.2).

Although these techniques are popular for engine's inlet and outlet planes measurements, in the wake, other methods are favoured, e.g. traversing wake rakes [180, 219]. The measurements in the mid-to-far-field become essential to apply energy-based methods from which the energy-flux transfer is described and the power balance ensured [180]. Notably, previous experiments yield power consumption reductions of respectively 10 and 18% for the wake ingesting and BLI configurations but additional testing are required to predict in-flight savings.

Along wall surfaces, pressure can also be measured by means of pressure taps [220] or pressure-sensitive paint [232, 233] nonetheless, the latter technique is more fastidious despite providing CFD-like pressure distributions on surfaces. To enable the paint to react to the pressure, the model needs to be thoroughly cleaned and get an epoxy base layer before being baked. The pressure-sensitive paint is then applied adding some surface roughness and drag up to 10 drag counts or 1% of the lift coefficient [232]. Through a lighting excitation and a camera set-up (Section 2.A.3), oxygen molecules react with pressure and luminescent probes enable the painting to be seen.



Figure 2.11: Photograph of an oil flow visualisation around the tail of a novel aircraft wind-tunnel model, courtesy of ARA

2.4.2.3 Velocity

A flow visualisation technique that does not capture the flow velocity but its path along surfaces consists in tracking oil particles left on the model surfaces. As the air travels along the wind-tunnel model, the thin oil droplets follow the path showcasing streaklines. Figure 2.11 depicts an example on a BLI aircraft tested in Aircraft Research Association (ARA) transonic wind-tunnel as part of the SUBLIME project (Supporting Understanding of Boundary Layer Ingestion Model Experiment) [229].

An optical measurement lies in the particle image velocimetry (PIV) technique [234] which relies on the displacement of small particles entrained by the flow around an aerodynamic body. A pulsed light-sheet (Fig. 2.12) generated by a laser and a lens illuminates a field of view where a synchronised camera records images of the light pulsations. Examples of laser and camera set-ups are discussed in Section 2.A.3. Although the cost and precautions to be taken for such set up are onerous, they gained popularity for large wind-tunnel facilities. The challenges for such measurement techniques are first the manipulation of the laser which are delicate pieces of equipment that can only be mounted outside of the wind-tunnel section to avoid sensitivity to changes in temperature and pressure. Moreover, the inherent vibrations in large facilities require complex alignment methods to prevent

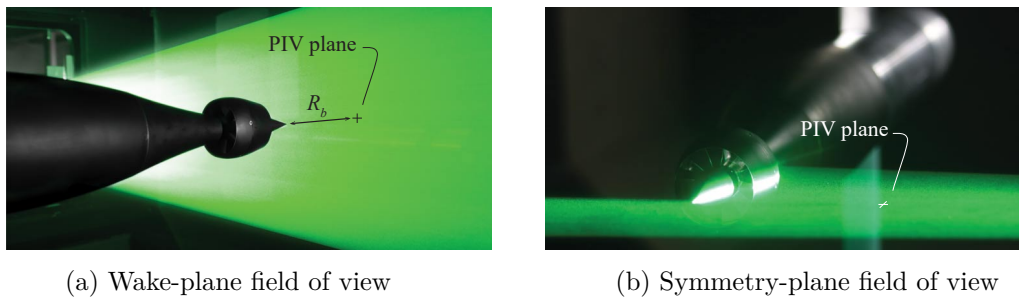


Figure 2.12: Stereoscopic particle image velocimetry measurement set-up, from Ref. [180]

blurry photographic captures as well as oscillations from the model itself entering transient conditions and from which unsteady measurements are more complex.

Nonetheless, PIV is a non-intrusive technique for obtaining velocities without disturbing the flow [180]. For BLI, its applications can be upstream of the propulsor to characterise the mass-flow capture ratio, the BL momentum and energy profiles (Fig. 2.12a) and therefore the distortion, as well as downstream where the jet-plume mixes with the wake (Fig. 2.12b).

Lv et al. [219] use PIV to compare BL and wake ingestion configurations on an axi-symmetric body. To capture sheets of velocity fields, particle seeds populate a region for which lasers extract their velocities and map out the entire flow-field - BL included. Nonetheless, the light reflection close to the wall surface prevents the sub-viscous layer to be well captured and information lacks to characterise the flow ingested by the propulsor [226]. Further, Della Corte et al. [180] extended the work by investigated the aerodynamics to characterise the BL (Fig. 2.12a) and wake flow (Fig. 2.12b) which set-up is further detailed in Section 2.A.3.

2.4.3 Performance Evaluation

Another difficulty related to wind-tunnel testing is the data collection process and the data usability. Although sparse data can often be interpolated following different methods (Section 2.A.2), the uncertainty associated increase as well as its propagation when determining other metrics - i.e. uncertainty in indirect measurements [235].

In most experiments, a balance is used on the model to determine the different forces and momenta experienced [20, 219, 226] but mid-/far-field approaches also enable the calculation of the body force with notably an exergy-based approach [73] through a decomposition of the flow-field in an isentropic and non-isentropic regions [96]. As the isentropic region is akin to a potential flow, no losses occur and the viscous losses, or drag, are captured by the non-isentropic part of the flow. Nonetheless, depending on the mounting system used, the rake instrumentation cannot be positioned far away downstream and a close installation to the body's trailing-edge requires a correction of the data [73]. Equally, the propulsive power can be determined by a mid-field wake investigation using an energy approach, as intended in Ref. [180]. A major drawback from these approaches is to use a fine wake rake spacing to map out

the distributions and reduce the uncertainty propagation. Nonetheless, it appears to be as an effective means of determining the axial force, or power, without any balance.

As the axial net body force is determined, it can be related to the power consumption from the propulsive device. With a power measure on the shaft, savings can be computed and compared to the reference aircraft to report savings, if any [20]. However, there is a caveat in the emulation of the propulsion system whereby it should ingest an equivalent non-dimensional mass-flow rate as the full-scale aircraft at flight conditions, or be represented with the same size, linearly scaled-down. Indeed, the performance of the tested BLI mock-up is related to the wind-tunnel conditions and not the flight conditions where similitude needs to be ensured to translate the data. As similitude is not respected, either the kinematic or dynamic similarity alters the performance.

2.5 Aircraft Conceptual Designs

New conceptual designs arose with the consideration of BLI propulsion systems. Whilst some concepts adapt long-established tube-and-wing aircraft, others endorse more unconventional shapes like lifting or blended-wing bodies. Therefore, to highlight their differences, these configurations are distinguished into three categories:

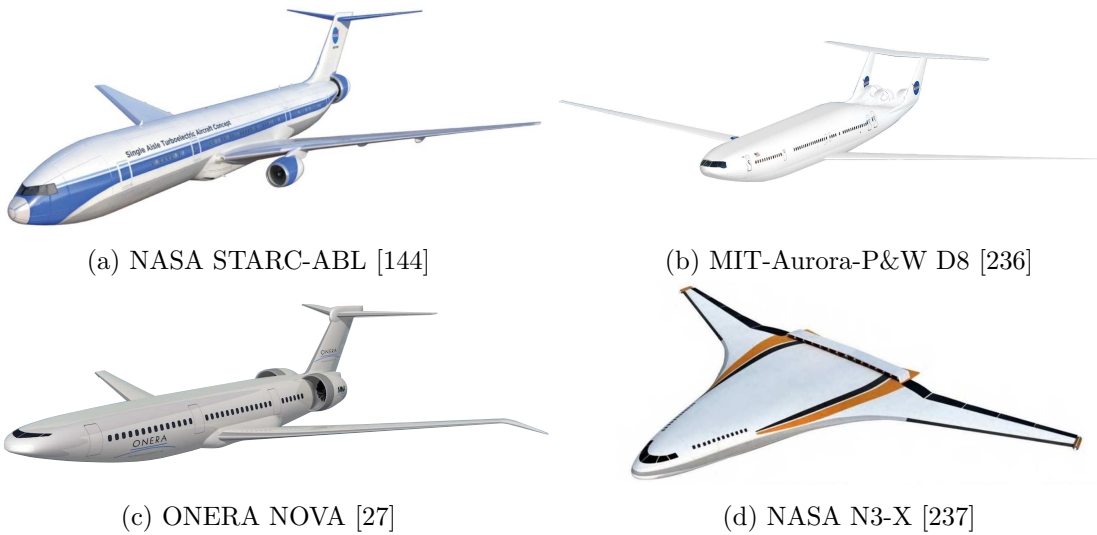


Figure 2.13: BLI aircraft designs

2. Advancements and Prospects of Boundary Layer Ingestion Propulsion Concepts

- Propulsive fuselage concepts (PFC): based on a conventional tube-and-wing configuration, a circumferential BLI fan is mounted around the aircraft's tail ingesting the boundary layer developed, e.g. NASA STARC-ABL (Fig. 2.13a);
- Over- and rear-fuselage engines concepts (ORFEC): traded-off the under-wing turbofans for over- or rear-side-mounted engines on a lifting-fuselage, e.g. MIT-Aurora-P&W D8 (Fig. 2.13b) and ONERA NOVA (Fig. 2.13c);
- Disruptive propulsive concepts (DPC): revisited entirely the conventional airframe to incorporate hybrid- or blended-wing bodies with a distributed array of fans, e.g. NASA N3-X (Fig. 2.13d).

To perceive at a glance the abundance of publications available in the literature, Fig. 2.14 maps out the different references by distinguishing the aircraft's concepts by their colours (blue for PFC, orange for ORFEC and green for DPC). The inner region relates to the different book-keeping, methods and models previously reviewed. Moving outside the schematic, parametric studies, design of experiments and design space explorations are reported whereby the different references are connected by solid and dashed arrows to highlight the research efforts and more

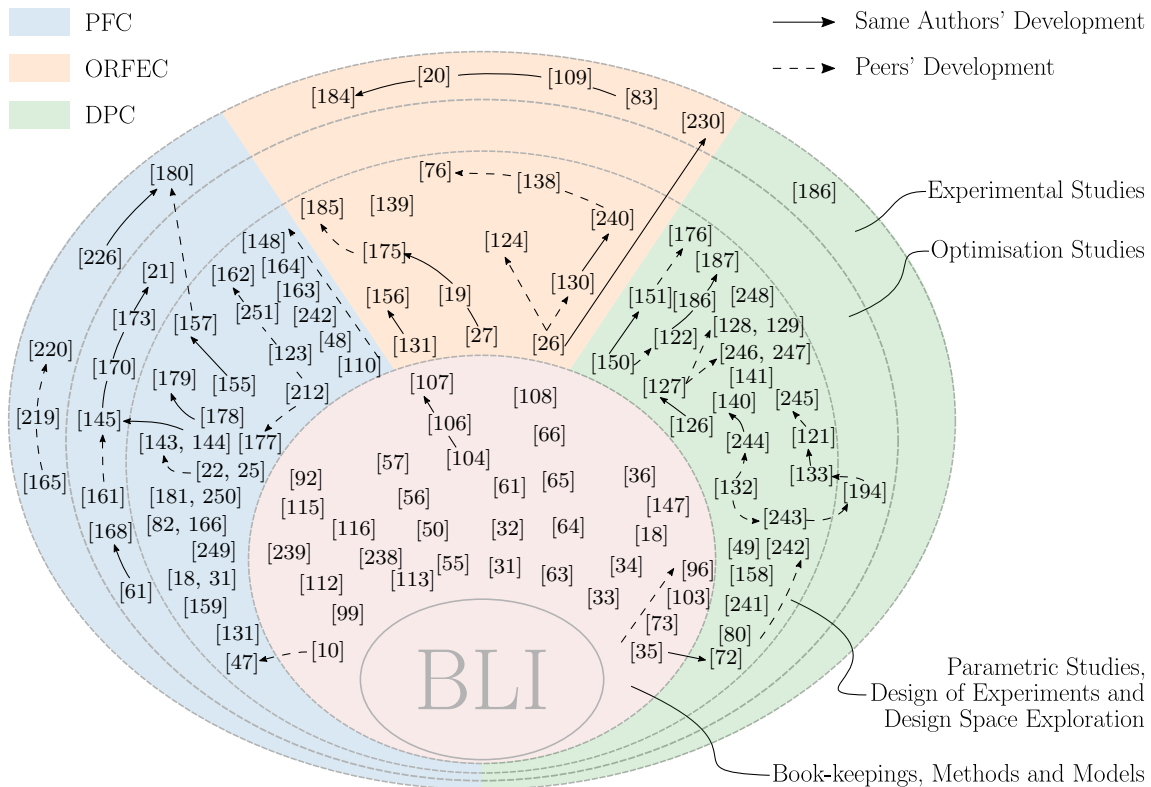


Figure 2.14: Map of BLI studies in the literature

refined studies (from aircraft- to system-level). Follows the optimisation studies as well as the experimental work on the outer ring. At first glance, it is noted important efforts in the PFC studies which aim to appear as an alternative to conventional aircraft in a nearer future. Long-term objectives aimed by the ORFEC and DPC lack optimisation studies which are expected to be performed after a down-selection of the aircraft architecture and systems.

2.5.1 Propulsive Fuselage Concepts

The propulsive fuselage concept (PFC) refers to configurations with an aft-mounted propulsor encircling the rear fuselage section of the aircraft. This resembles an early project, the FMA IA 36 Cóndor [252], with five engines positioned circumferentially ingesting the fuselage boundary layer of a short-range aircraft featuring a regional aircraft capacity. In more recent concepts, the airframe structure is comparable to a conventional tube-and-wing aircraft partly powered by two under-wing turbofans and also providing electrically or mechanically the power necessary to drive the BLI propulsor and propel the aeroplane through a thrust-split (TS, Eq. (2.34)) configuration (Fig. 2.15) .

In the case of a TS of 100%, power is extracted from the under-wing engines and fed into the aft-propulsor which provides the entire aircraft's propulsive force requirement. The turbofans therefore either incorporate a free turbine to supply the power demand from the rear propulsor, or the energy, respectively torque, is extracted from the low-pressure shaft, and the configuration is described as fully turbo-electric, respectively turbo-mechanical.

Amongst the conceptual designs based on the PFC, the NASA STARC-ABL [25], the European Union projects CENTRELINE [212] and DisPURSAL [49] can be named. The former targets short-to-medium missions using the Boeing 737-800 as reference whereas the CENTRELINE and DisPURSAL accommodate wider bodies for long-haul based upon the Airbus A330-300. A specificity of the latter is its propulsion system which does not account for a *simple* propulsor but for a gas-turbine engine at the tail. Section 2.B compares these different designs in terms of aircraft operating conditions at design point (steady-level cruise flight) as well as the under-wing engines and propulsor characteristics.

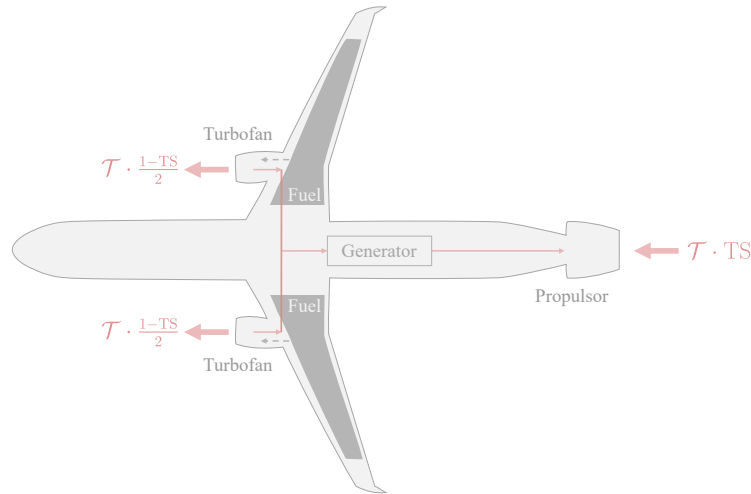


Figure 2.15: Propulsive fuselage and thrust-split concepts

2.5.1.1 NASA STARC-ABL

The NASA STARC-ABL (Single-aisle Turboelectric AiRCraft with Aft Boundary Layer) has been introduced in 2016 by Welstead and Felder [25] and is based on a turbo-electric distributed propulsion (TeDP) architecture (Fig. 2.13a). The fuel coming from the wings is fed into the main under-wing turbofans and mixed with the compressed air to drive the turbine. The engines partly propel the aircraft and generate power through a generator, directly related to the aft-propulsor ingesting the boundary layer coming from the fuselage to generate the remaining force requirement. The architecture leads to an overall efficiency increase albeit losses due to the power conversion from mechanical to electrical, the transmission of the electrical power to the aft-propulsor and the conversion back to mechanical power needs to be addressed.

Welstead and Felder initiated the STARC-ABL design using OpenVSP [190] and performed analytical studies to obtain estimates on fuel reduction along a whole flight mission using FLOPS [253]. Inspired by the Refined SUGAR concept [254–257] for the technology assumptions and flight mission of the baseline configuration (N3CC), the study aimed at determining if any potential benefit exists in using turbo-electric architectures. Nonetheless, no high-order tools have been used and a generic BL from a Boeing fuselage was used at both on- and off-designs. It is thus opined that the results from Welstead and Felder rely heavily upon the Boeing CFD solution and do not represent a realistic fuel consumption from the high angle-of-attack and potential flow separation not considered. In addition, the BLI propulsor was run at fixed power input, regardless of the flight

condition showing a 12% fuel burn reduction for a full mission in comparison to a conventional podded variant. However, because it operates at constant power, a smaller and lighter electrical power system was installed, reducing the aircraft weight and hence its consumption. The assumptions are very optimistic as more power is needed during the ascent phase of the mission however, for a BLI fan ingesting only a portion of the fuselage's BL, the propulsion system efficiency was increased, and the gain in size and weight of the twin under-wing turbofans allowed the weight of the additional electrical system components to be offset. Overall, the conceptual design exploration study showed that the STARC-ABL concept was a good first candidate for turbo-electric systems and subsequently, numerous studies followed from Gray et al. [143–145, 169] and Yildirim et al. [21, 173, 174] which contributed to the development and optimisation of the design.

From this work, Gray et al. [143, 144, 169] developed an aero-propulsive analysis model for fully-coupled airframe propulsion, using a 1D thermodynamic cycle. A study on the effects of the fan pressure ratio (FPR) on the propulsor performance was conducted which results showed a growth of the BL thickness for reduced FPR - at fixed shaft power, resulting in the BLI-installed propulsion system outperforming the conventional podded engines. Also found, was that a larger nacelle (lower FPR) leads to slower flow ahead of the fan, causing the static pressure effects to propagate much farther forward than the change in velocity profiles suggested. This discovery emphasises the fact that a strong interaction exists between the airframe aerodynamics and the propulsor. However, the inferences gained from the study may provide a premature conclusion and it can be argued that the FPR is not entirely responsible for that strong interaction but it is believed that the propulsor's size plays a larger role in the integration effects.

Later, Gray and Martins [145] showed that small BLI propulsors offer a better power saving coefficient (PSC). However, it is essential to note that the transmission efficiency between the under-wing podded engines and BLI propulsors plays a crucial role as suggested by Fig. 2.16. Considering a transmission efficiency of 90% results in a PSC of only 1% which could call to question the usefulness and benefits of BLI propulsors but an increase to more realistic targets for the next decade, 95% or even 98%, could lead to a PSC increase to almost 5%.

To ensure the fan operability, maintaining a low level of distortion is vital. Ordaz et al. [142] developed an adjoint- based design capability to improve the fan efficiency. Whereas the distortion metric therein investigated is the $DC\theta$, it is

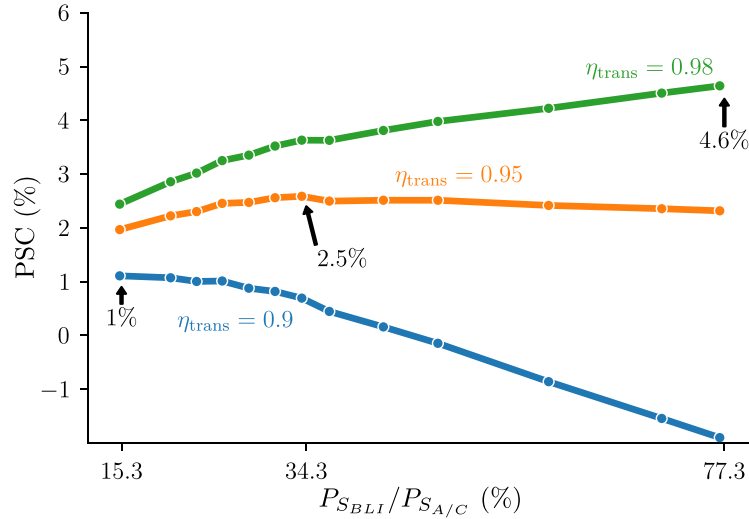


Figure 2.16: Overall aircraft PSC at cruise versus the fraction of shaft power used for BLI under different transmission efficiencies, η_{trans} , adapted from Ref. [145]

opined that this metric cannot solely assess the distortion performance. However, it is a first step in the demonstration of a consistent adjoint-based sensitivity analysis for gradient-based optimisations. The mesh adaptation capability is able to reduce the distortion error associated as well as to enable the design optimisation under free-stream and BLI conditions. Nevertheless, it is emphasised that the study only applies under particular circumstances, i.e. steady cruise conditions at a different altitude than the design one, of a bare fuselage without wings. Kenway and Kiris [161] demonstrated that the wing down-wash has a significant impact on the fan-face distortion by investigating a three-dimensional tube-and-wing model. Figure 2.17 depicts a comparison between a ducted body (tube only) and a tube-and-wing ducted body, where the authors report that the distortion metric doubles when mounting the wings. The shape sensitivities at all design conditions point in the same direction, and modifications made at the design point improve all other conditions - showcasing the distortion independence between the tube-and-wing baseline and its optimised geometry. As the wing down-wash was the main driver, and since the optimiser had no control over it, the fuselage and nacelle shapes are nearly independent of the design condition to mitigate this effect.

Following on the design optimisation but more particularly on the nacelle and plug-cone shapes, Lee et al. [171, 172] emphasised their research on a quasi-2D through flow model. The method accounts for the incoming BL radial effects from a 1D mass-flow rate BC that is corrected and constructed with a quasi-2D model. In

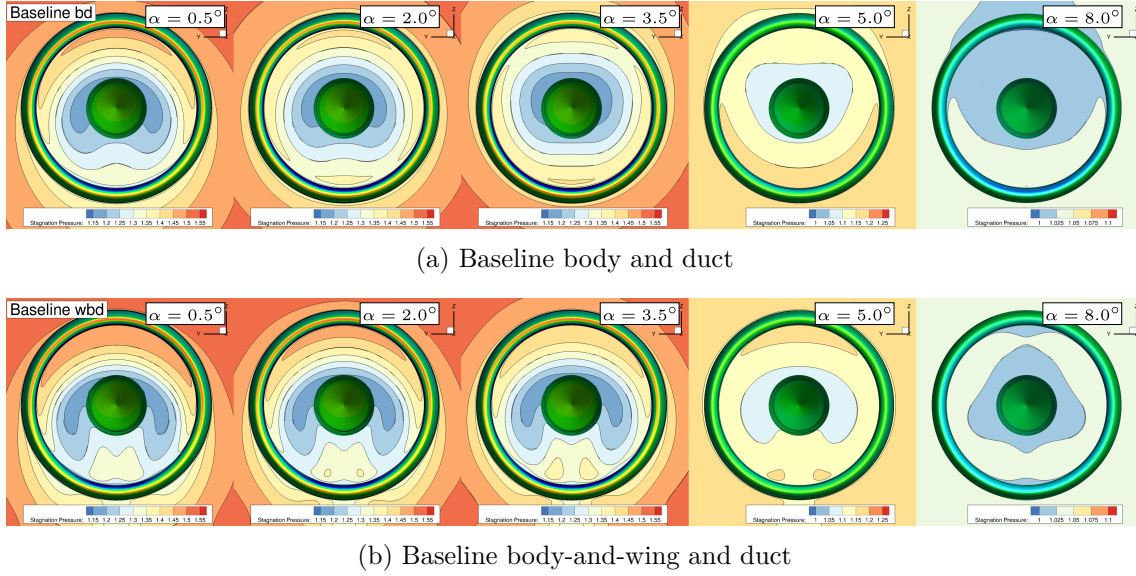


Figure 2.17: Computed total pressure contours at AIP for different angles of attack highlighting the down-wash effects, Ref. [161]

order to optimise the wake recovery, the exhaust cone and nacelle shapes follow a set of optimisations yielding a reduction in entropy rise all along the plug as the jet flow accelerates, and a wake loss reduction with the less cambered nacelles.

The first-of-its-kind optimisation study on coupled aero-propulsive design variables is performed by Gray et al. [170]. The propulsor size varies with respect to the FPR totalling 25 shape parameters to take into consideration on a 3D aircraft model, along with the angle of attack and a body force representing the propulsor behaviour, hence amounting to 27 design variables. To strategically reduce the computational demand, three different BLI designs were down-selected. They are designed to contribute to different stream-wise forces where the highest value corresponds to the smallest propulsor matching the shaft power used in Ref. [25]. When subject to distortion, the largest BLI propulsor requires 1.2% more shaft power than the non constrained configuration to obtain the same output. Through the design optimisation of the aft-fuselage and inlet region, distortion could be mitigated albeit not falling below 0.03. Bigger flexibility in the design variables could allow reducing the distortion metric even more however, it is stressed that this study lies as the first fully-coupled aero-propulsive optimisation study accounting for both aerodynamic and propulsion design variables.

Additional efforts from Yildirim et al. contributed to the design optimisation of the propulsion system with the use of the coupled aero-propulsive approach developed by Gray [62]. Optimisation studies at cruise condition were initiated for different

net force and FPR [173] yielding 9 optimised geometries. The study is further extended with the PSC as an objective function yielding 18 designs [174]. Using these 18 optimised geometries for cruise, off-design was investigated to cover the entire mission profile [21]. Starting with cruise and take-off conditions as the study bounds, the approach and hold stages complete the mission profile. It is noteworthy that the previous work performed at cruise does not imply an optimal behaviour of these designs at low velocity and altitude therefore the PSC is also tracked at off-design - for different altitudes, Mach numbers, lift coefficients and FPRs. The results suggest that although the PSC is insensitive to changes in the altitude at low flight velocity, the power requirement from the podded fan is more sensitive to FPR than the BLI fan [258].

2.5.1.2 EU CENTRELINE

Towards the European efforts, the CENTRELINE project (ConcEpt validation sTudy foR fusElage wake- fillIng propulsioN intEgration) extends the work initiated in DisPURSAL (detailed in Section 2.5.1.3). This concept considers the Airbus A330-300 as a state-of-the-art baseline whereby an equivalent design is derived based on the potential technologies available in 2035. The project targets the proof of concept and its experimental validation whilst aiming at reducing the aero-structural complexity and the intake distortion and losses.

CENTRELINE, alike STARC-ABL, is composed of two turbofans mounted under-wing that electrically drive the aft-mounted propulsor as well as partially propelling the aircraft. Introduced by Seitz et al. [212], this EU-funded project aims at addressing several challenges. Notably, a thorough understanding of the aerodynamic effects of the fuselage wake-filling propulsion system, the coupled aero-structural design as well as the layout and design of the fuselage fan drive train and the multidisciplinary systems design integration and optimisation.

Setting off with aero-structural studies from Goraj et al., the aircraft 3D model was parametrised and generated and the robustness of the configuration evaluated [259]. The models parametrisation allows for numerous studies to be performed even if numerous components is not considered, reducing the practicality of this tool and giving profits to only series of products and manifold simulations. To contrast this lack at the early stage, the authors informed on the continuing development of these tools along the project.

In tandem, Bijewitz et al. designed the under-wing turbofans from parametric studies accounting for significant power off-takes as well as its aft-mounted propulsor model [123]. They highlight the difference in power extraction from a single low-pressure spool to a coupled low- and high-pressure spool. The high demand in power from the propulsor necessitates to extract a portion from the high-pressure spool. These constraints identified at off-design were shown to be alleviated with the latter configuration thus, rising the need for further investigations in the optimal power off-take strategies.

In the review publication from Habermann et al. [32], figures of merit are discussed with their limitations. Regarding these limitations, Seitz et al. [162] provided a rigorous approach to the PSC evaluation of PFC. Although it is expected that improving the transmission systems efficiency results in higher achievable power savings, it can be mitigated if the effective propulsive device efficiency of the reference increases, emphasising on the importance of the baseline definition. A comparison of the mechanically- and electrically-driven transmission systems highlights that the former option features higher potentials in fuel savings.

A series of trade studies from Troeltsch et al. [251] provide guidance on the design refinements and gauge potential optimisations. The study justifies the plausibility of such configuration providing improvements compared to their reference aircraft. On the propulsive performance, the turbine cooling highly impacts the potential reductions in block fuel and additional parametric studies need to be performed. On the fan design, a greater blade height tends to reduce the fuel consumption overcoming the longer and heavier undercarriage penalties. However, this is suggested to be further assessed with higher fidelity studies. To follow on, Castillo Pardo and Hall carried out an experimental campaign compared to CFD computations on the BLI fan [178] and later focused their work on the fan design assisted by CFD [179]. The numerical modelling of the free-vortex fan tested experimentally is run using RANS CFD which results on the axial velocity, swirl angle, stagnation and static pressures reinforce the confidence in the approach developed [178]. This allows a fan design optimisation to handle better the non-uniformities restoring the work input and increasing the operating range. Further developments led to a final optimised design that increases the forward force by 1.2% and the stability margin by 4-10% relative to its datum design [179]. Petit et al. [177] pushed forward the blade design optimisation, albeit taking the perspective from a propeller. The tool is coupled to CFD and aims at comparing

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two configurations: a free-stream ingesting configuration and its BLI form. It is opined that the study performed is not entirely representative of the PFC design-point as the cruise Mach number herein studied is 0.47 and the altitude 20,000 feet. Although it is noted that this work relies upon as an introductory work for future improvement and wider applications.

At a larger scale, a systematic design space exploration is applied to a 2D axisymmetric model using surrogate models [155]. The study intends to characterise the effects of geometrical parameters on aircraft performance. The approach starts with independent parameters' variations (parametric studies) to then be varied in combinations. Whilst the procedure permits an understanding of the mechanisms at an early stage, several key points need to be highlighted. Indeed, the model parametrisation presents important drawbacks such as a variation in either the fan axial position or its radius alters the body slenderness as well as the nacelle shape. This strong interdependence notably affects the amount of BL ingested as well as the BL profile and the integration effects. Another important point is the accuracy of the results obtained as it is reported an over-prediction of 10 to 30% of the absolute drag that one can question the paper's conclusions. Nonetheless, the tools developed in this study could lead to further optimisations of 2D axis-symmetric models later extendible to 3D.

More recently, Habermann et al. [157] demonstrated the fuselage tail upsweep effects on the PFC by investigating the inlet distortion caused by the 3D flow behaviour and its impact on the aero-propulsive performance. Needless to mention the significant 3D effects on the propulsion system performance due to the highly distorted incoming flow, a non-negligible consequence of this detrimental effect is the increase in shaft power which is correlated to the upsweep angle. Notwithstanding this drawback, the penalties are alleviated with a nacelle design adjustment. When variations in the thickness-to-chord (t/c) ratio are made on the nacelle upper and lower regions, the PFC performance is restored to the axis-symmetric baseline geometry (Fig. 2.18) despite the fact that no interaction with the other aircraft components, i.e. wings and tail plane, has been considered.

The final investigation performed on the CENTRELINE project is an experimental low-speed wind- tunnel campaign using a power balance analysis. Della Corte et al. [180] scaled down a wing-less axis-symmetric fuselage version of the PFC. The net axial force of the body and PIV allow for momentum- and energy-based analyses to be conducted. At axial equilibrium, the propulsor installation implies a

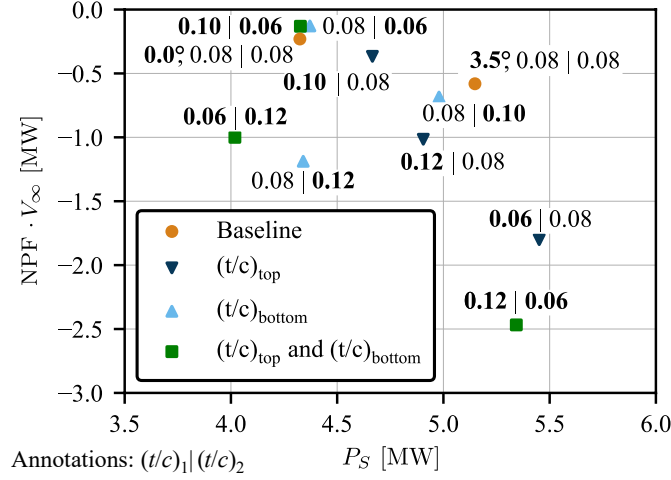


Figure 2.18: Comparison of the aero-propulsive performance between the baseline and geometries with variations in thickness-to-chord ratios (t/c), from Ref. [157]

flow acceleration through the fan which increases the mass-flow flux and is represented by a BL thinning upstream. This observation goes against Ref. [144] who reported that the nacelle integration thickens the BL upstream however, the flight conditions differ as Gray et al.'s study was at transonic conditions and Della Corte et al.'s testing between M0.04 and M0.12 omitting compressibility effects. Nevertheless, one drawback from the testing campaign is that when outlet guide vanes are omitted, the nozzle rotates coincidentally with the fan generating a local strong hub vortex in the wake caught by a low total pressure in the wake which increases the wake losses and biased the performance of the system. The fan installation highlights the BLI benefits with an axial momentum flux and kinetic

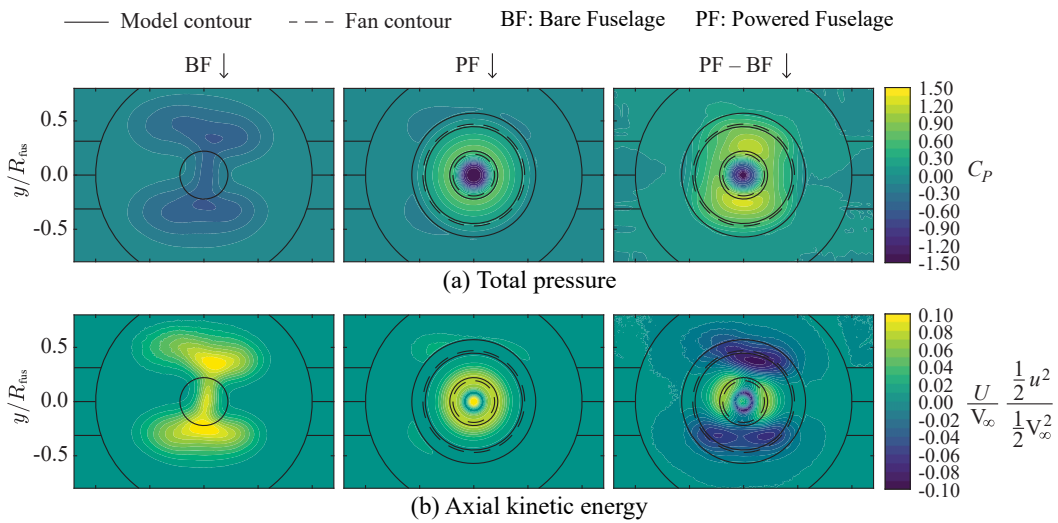


Figure 2.19: Measured total pressure and axial kinetic energy in the wake-plane field of view for the bare fuselage, powered fuselage and their difference, from Ref. [180]

energy deposition rate increase upstream, and a wake attenuation downstream in the wake (Fig. 2.19). At different thrust levels nonetheless, the power balance analysis suggests that performance decays from the ideal wake-filling conditions with an increase in dissipated mechanical energy within the wake. This work represents a ground-breaking experimental power balance analysis of an axi-symmetrical BLI propulsive fuselage albeit being performed at low subsonic condition. Additionally, the authors claim an application of the power balance where neither the shaft power or viscous dissipation rate are reported preventing the balance or PSC to be calculated.

2.5.1.3 EU DisPURSAL

As alluded, the CENTRELINE project resulted from an initiative started in 2013 from another EU-funded project called DisPURSAL (Distributed Propulsive and Ultra-high by-pass Rotor Study at Aircraft Level). Although DisPURSAL initially focused on distributed propulsion systems with a hybrid-wing body (Section 2.5.3.1), they also considered a PFC slightly different from the ones detailed above. In place of a propulsor fan installed at the rear-fuselage, the propulsion system pictured in Fig. 2.20 is preferred. The fan is driven by a gear system mounted on a drive-shaft thereto connected to the core engine that is alimented from the incoming air passing through a S-duct.

A multidisciplinary pre-concept design is initiated by Isikveren et al. [158] where the preliminary aircraft sizing is defined from CFD-based results on a 2D axi-symmetric model powered by an actuator disc. Following, Kaiser et al. compared the previous

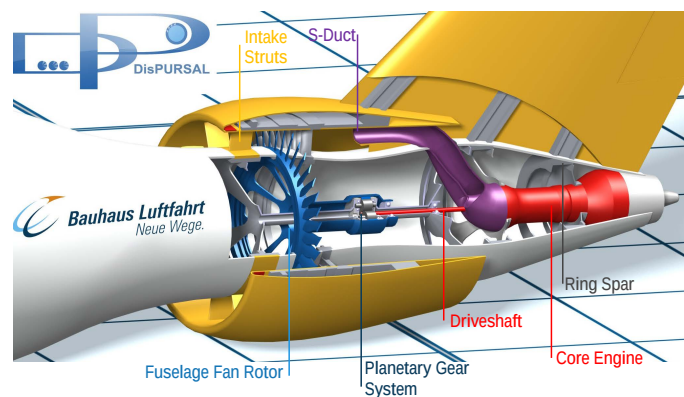


Figure 2.20: Cutaway view of the rear-mounted DisPURSAL propulsion system, from Ref. [260]

results with their quasi-analytical method for investigating the power savings under different disc loadings [110]. The relative difference in terms of BL, momentum and energy thicknesses between their model and the CFD results are respectively of -5.1%, 5.7% and 8.0% making the approximates fast and reasonable compared to computational simulations. Also early in the project, Seitz and Gologan [48] developed a book-keeping based on thrust and drag with common metrics such as the PSC as well as the estimated specific air range (ESAR) based upon the specific air range (SAR). Although it is reminded that the results pertain to an arguable thrust-drag split on BLI configurations. An outcome from their parametric studies are the small sensitivity of propulsive efficiency to speed changes whereas it is more significant on specific thrust in comparison with podded engines.

A comparative efficiency analysis on both powered fuselage and distributed propulsors concepts is performed by Mirzoyan et al. [242]. Focusing on the PFC, two reference configurations are used: an entry-into-service (EIS) year 2000 represented by the Airbus A330-300 powered by the Rolls-Royce Trent 700 and a conventional tube-wing configuration equipped with advanced geared turbofans targeting an EIS year 2035. Their investigations predict a fuel burn reduction of 32% between the two references, from which 9.2% can be added when the BLI technology is implemented, compounding to 38.3% with respect to the state-of-the-art 2000 configuration.

The fuel burn prediction is confirmed by a multi-disciplinary design investigation on various FPR for given duct heights using actuator discs modelling [163]. The performance is investigated with the use of an integration factor, f_{int} which not only captures the drag spillage from the nacelle but also covers the additional effects from the fan and nacelle integration to the fuselage. It is found that f_{int} primarily depends on the propulsor intake size where a decreased duct height implies more spillage and a stronger interaction between the nacelle stagnation point and the main stream going through the fan whereas the dependence on FPR has a suction effect which reduces the spillage for greater FPR's. The integrated performance assessment from Ref. [163] proves to confirm the semi-empirical methods from Ref. [48].

Whilst recommendations would have been on extending the design space to different thrust-split configurations as well as flight conditions, these trade studies are conducted in Ref. [164]. It is shown that the greatest impact on block fuel occurred for changes in the intake pressure ratio which depends on the flight conditions. Varying the Mach number and sizing the aircraft according to their

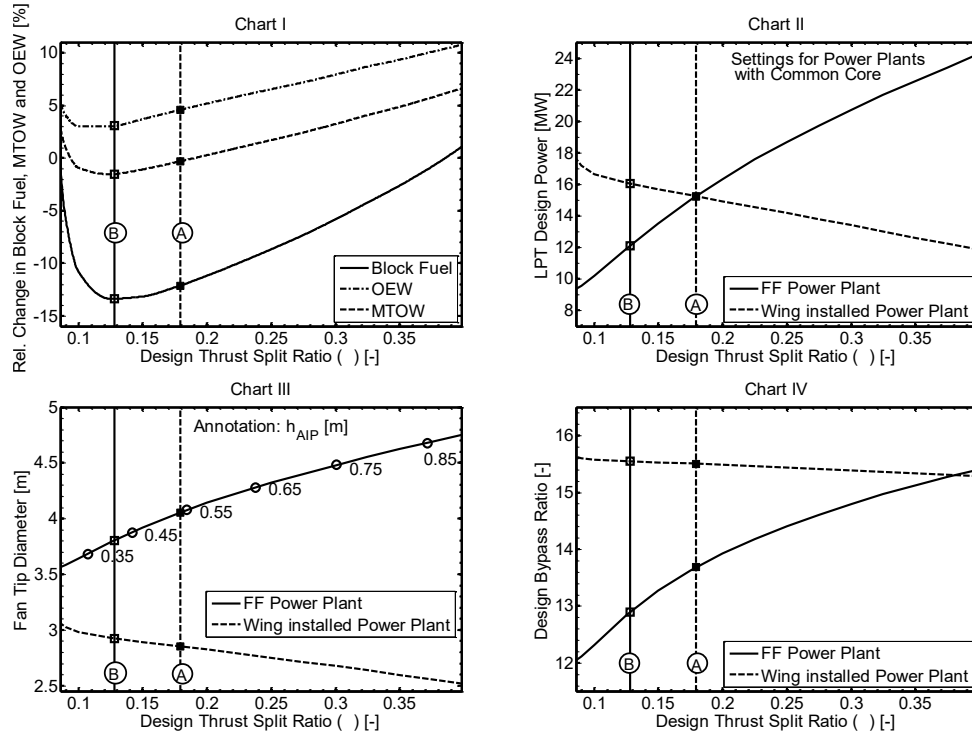


Figure 2.21: Characteristics of thrust-split optimality study for two designs (A and B), FF denoting fuselage-fan, i.e. BLI propulsor, coupled to two under-wing engines for a design range of 4,800nmi carrying 340 PAX with a cruise segment flown at M0.82 and FL350, retrieved from Ref. [148]

optimum cruise speeds, savings in terms of block fuel could be reduced to 14% but when compared at identical speeds, the benefits fall down to 11% [164]. Extending the trade study to different specific thrusts, a fuel-burn reduction of 12.1% is expected whereas adjusting the thrust-split ratio could lead to an additional 1.3% benefit [148] as highlighted by the two designs (A and B) from Fig. 2.21. The results from these studies are promising and it is also emphasised that cycle settings such as overall pressure ratio, turbine entry temperature or specific thrust would potentially lead to bigger fuel burn reductions.

2.5.1.4 Other Significant PFC Studies

In addition to the three PFC designs, other studies have contributed towards the development of methods or tools.

To complete NASA's efforts on the STARC-ABL, Refs. [82, 166] examined the potential aero-propulsive benefits of using a BLI configuration on the NASA CRM aircraft using the power balance method. One of the main objectives from

applying this method is to evaluate the dissipation reduction, i.e. irrecoverable mechanical energy, through changes in the stream-wise velocity from the BL development. The study demonstrates the use of different tools, e.g. NASA's VGRID [261] for meshing, NPSS [209] for the engine cycle, USM3D [262] for the CFD solver and CDISC [263] for the *on-the-fly* geometry re-design.

Whether with or without the iterative surface curvature alterations from CDISC, an actuator disc modelling the fan at the rear of the aircraft offers a reduction of power of around 15% which mainly comes from the reduction in velocity of the propulsor incoming flow. Although, the estimates are relatively optimistic as detrimental interactions from the nacelle and engine integration are not implemented, the work done is a good starting point for designing and evaluating the potential BLI benefits.

Giannakakis et al. also used the power balance method for the fuel burn evaluation of such configuration and challenged the previous efforts made by NASA and MIT on the mass evaluation of those configurations [147]. They highlight that although the under-wing turbofans can be scaled down as their contribution diminishes, a fuel burn penalty of around 3% is expected due to a 31% mass increase in the turbo-electric system installation compared to the baseline. On the other hand, Hall et al. predicted a 5.4% increase in mission fuel burn for the non-optimised geometries [125]. Both studies agree on the penalty of mounting a BLI propulsor in terms of fuel consumption but when optimisation is performed on the design and the uncertainty in the results is quantified, the authors suggest opposite results with a 0.7% penalty in the best scenario for Ref. [147] and a 4.8% benefit for Ref. [125]. Another study analytically estimates the BLI benefits at aircraft-level enabling a quick and cost-effective design space exploration [249]. They emphasise the importance of the assumptions made on the under-wing turbofans and BLI propulsor as their size and mass largely contribute to the fuel burn estimation. The assumptions on the transmission system efficiency, engine efficiencies as well as the mission range also play an important role and in that respect, the authors compared several candidates and assessed their expected performance.

Continuing with the application of the power balance method, the aerodynamics of a different design from Tse and Hall is investigated [181]. Unlike the previous concepts presented, an array of multiple circumferentially distributed aft-fuselage BLI propulsors are electrically driven by the under-wing turbofans as illustrated in Fig. 2.22. Although this concept deviates slightly from the PFC, it could be discussed that it belongs to a distributed PFC category along with a similar design

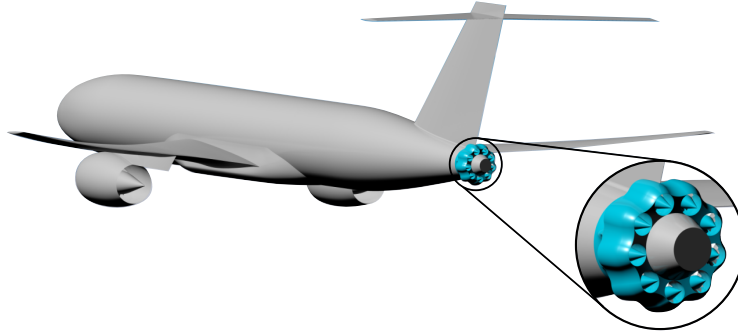


Figure 2.22: Distributed propulsive aft-fuselage aircraft concept, modified from Ref. [181] with the authors' permission

by NASA [250]. Both concepts seem to originate from the FMA IA 36 C ndor [252] that featured 5 engines in a likely manner, as opposed to the propulsors herein considered. A major strength of this device is the possibility to independently control the propulsors allowing some degree of thrust vectoring despite not ingesting as much BL as the other PFC. The concept expects to fare better benefits in weight, power system integration and noise. notably, Tse and Hall offer the first-of-its-kind URANS CFD assessment on a three-dimensional model with a full annulus fan modelling (rotating blades/mesh) and the application of the power balance method [181].

A preliminary design space exploration has also been performed by Battiston et al. [159] on a scaled wind-tunnel BLI aircraft model as part of the SUBLIME project. Following some wind-tunnel constraints, they investigate the influence of the nacelle intake, height and length on the propulsion system performance of a 2D axi-symmetric model. It is concluded that tightly embedded short intakes are more promising in terms of shaft power requirements for a given net assembly force. Although it is noteworthy that this applies to a particular design that has not been optimised in any way and a different parametrisation of the model could lead to other conclusions. Similarly, Matesanz-Garc a et al. [61] explore a thorough design space on a medium haul reference aircraft where they assess the system performance with the thrust-split, thrust-specific power consumption, fan-face distortion - radial distortion index, and intake pressure recovery. The exploration of 16 design variables allows them to conclude that less aggressive intake slopes are favoured to reduce the distortion levels and enable a narrow-down of the design space for multi-objective optimisation. They follow with a design optimisation on the exhaust design for a 2D axi-symmetric PFC with the implementation of a BFM [168]. A benefit of 0.32% is achieved on the aircraft's required force with a

compact exhaust and they stress that the exhaust and nacelle after-body have a considerable impact on the aerodynamic performance.

Because the majority of studies chose to study the flow-field impacts on the propulsion system rather than the opposite, Gray et al. [144] demonstrated the importance of coupling the two with the use of CFD. However, this coupling has a computational cost that Pokhrel et al. [146] offer to reduce through a novel approach that assesses the effect of the propulsion system on the flow-field. To follow this study, Ahuja and Mavris [156] investigate the BLI effects caused by changes in fan annulus and mass-flow demand assessing both the on- and off-design impacts. Although the metrics used for the performance assessment could be criticised, it is agreed that both the propulsor size and mass-flow demand play an important role in the performance as different amounts of energy are ingested by the fan and hence contribute to the system's effectiveness.

Lastly, the different PFC are mapped out in Fig. 2.23 with their propulsors various axial and radius positions along the tail. Ref. [156] notes different degrees of integration and influence whereas here the authors offer to segment these effects in three manners: the primary, secondary and tertiary effects. In the former, the position and size of the BLI propulsor can be named as their integration differ. More refined design aspects such as the pre-diffusive slope, the cowl definition or the plug-cone shape belong to the second category whereas the exhaust shape, droop angle or weighed-curve parametrisation would have more minimal effects but still contribute to greater performance levels after optimisation.

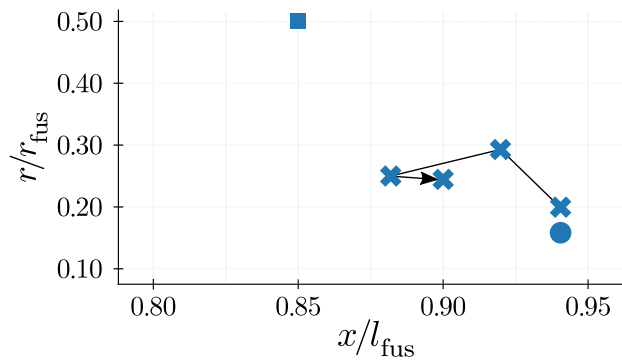


Figure 2.23: Positions of the STARC-ABL (●), CENTRELINE (×) with its evolution (→) and DisPURSAL (■) relative to their fuselage dimensions

2.5.2 Over- and Rear-Fuselage Engines Concepts

Unlike the PFC associated to a typical tube-and-wing airframe, the over- and rear-fuselage engines concept (ORFEC) widens its fuselage for a lifting-body and trades its under-wing turbofans for flush-mounted or semi-buried turbofans at the rear of the fuselage. The popular concepts based on this configuration are the ONERA NOVA [27], wherefrom the Airbus Nautilus concept was born [175], and the MIT-Aurora-Pratt & Whitney D8 [26] which aircraft and turbofans characteristics are summarised in Section 2.B.

2.5.2.1 ONERA NOVA and Airbus Nautilus

Introduced in 2015 by the ONERA, the French Aerospace Lab, the NOVA (Nextgen ONERA Versatile Aircraft) concept is a medium-haul twin-aisle configuration laying seven seats per row. Its wide-body lift-generating fuselage allows for its length to be shrank by 14% compared to a narrow-body aircraft of same capacity. The high aspect-ratio wings also feature downward with swept winglets, and at the rear of the fuselage a vertical tail plane accommodates two semi-buried turbofans on either side. This configuration is observable in Fig. 2.13c.

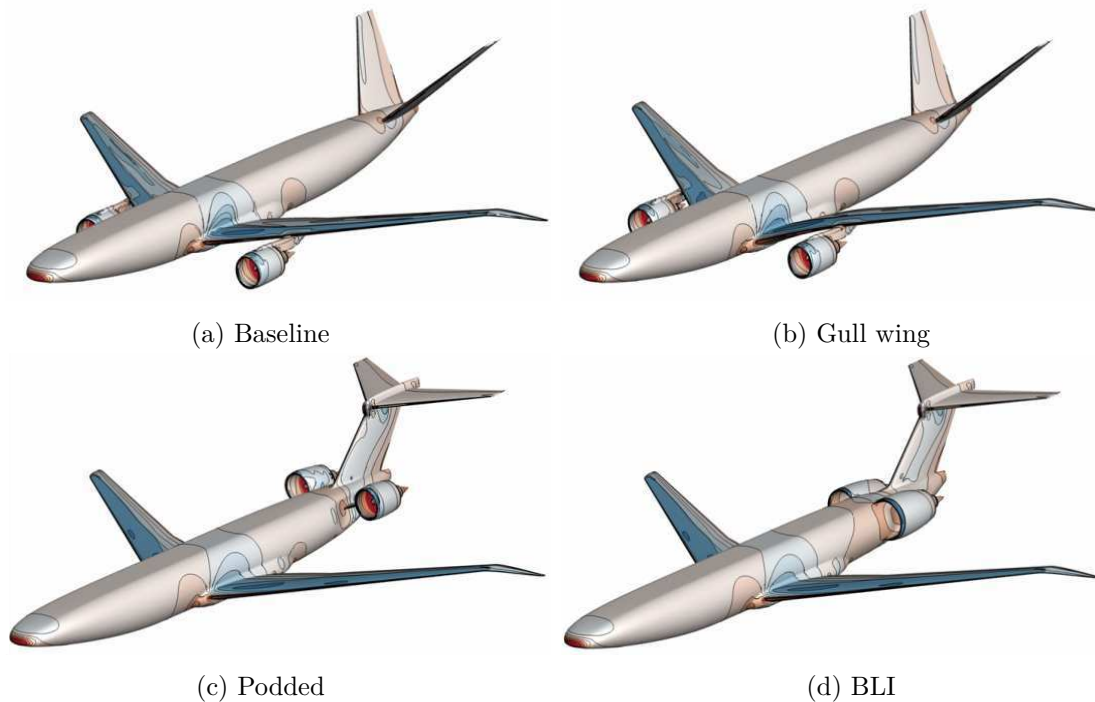


Figure 2.24: Development of the different NOVA configurations, adapted from Ref. [19]

Wiert et al. are at the origin of this conceptual design whereof after sizing the preliminary fuselage, they investigated its aerodynamics using CFD [19, 27]. During its development, four configurations were considered namely (a) the baseline with under-wing turbofans and V-tail planes, (b) the gull-wing design and, (c) the rear podded-equivalent of (d) the BLI configuration which has aft-mounted turbofans around the T-tail plane as given in Fig. 2.24. They compare two actuator disc models and conclude on the robustness of the results obtained with one yielding a 5% PSC at cruise - the other model does not replicate the expected physical behaviour and represents a bias in fan power prediction. Also, in the non-BLI scenario, thanks to the investigation of the multiple designs, it appears that higher by-pass ratios (BPR) could be considered if mounted under gull-wing concepts but other innovative architectures such as strut-braced- or box-wing configurations should be taken into consideration.

Few publications are available on the NOVA for several reasons. Its tightly-embedded engines on either side of the tail favour separation and distortion within the intakes whilst the flight Mach number of 0.82 fosters shocks on the cowls. Also, little benefits arose from the preliminary studies despite noting encouraging noise reductions which would meet the EU environmental targets. Whereas it seems that its development has been ceased - from the available literature, Airbus joined their efforts with the development of another ORFEC, the Airbus Nautilus which intends to maximise the ingestion of the BL whilst limiting the amount of distortion at the fan-face.

To ease the development of the Airbus Nautilus, the pre-existing NOVA podded reference (Fig. 2.24c) was used whereof the rear-end of the aircraft was modified to accommodate two ultra-high by-pass ratio engines on two distinct parts of a split-fuselage. This design compares with the PFC previously detailed permitting the ingestion of the entire BL whilst taking

Wiert and Negulescu [175] studied the aerodynamics of a simplified axi-symmetric engine using AD and BFM to represent the fan. A more uniform pressure distribution was input to the AD fan-face to prevent flow separation and another AD was used at the fan-exit to mimic the swirl from the fan blades. As for the BFM, it was used to prove confidence in the results shown by the AD models. Nevertheless, axi-symmetric studies are not suitable for the Nautilus concept and therefore an introductory study on the 3D design is performed to showcase the strong three-dimensional effects at the rear-end of the fuselage. To highlight the

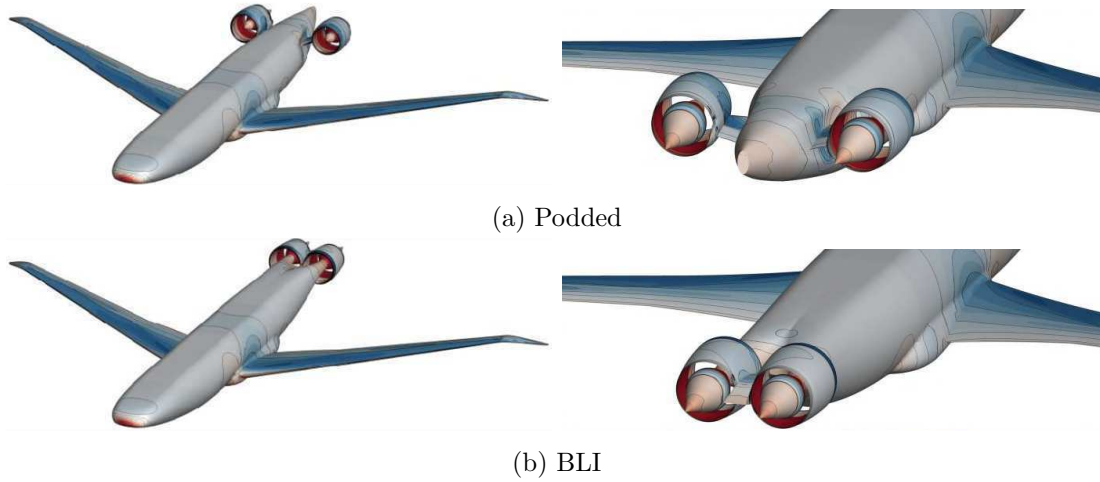


Figure 2.25: Representation of the (a) podded configuration from the NOVA (Fig. 2.24c without tailplane), and (b) the Airbus Nautilus derivative, adapted from Ref. [175]

separation, two designs iterations are compared whereby the lateral distance gap is adjusted. It is noted that the farther apart the nacelles, the stronger the separation but the lower the nacelle interaction drag [175]. all the advantages of ultra-high BPR (Fig. 2.25).

A further step to the engine integration is studied by Godard and Negulescu [185] with both steady and unsteady RANS computations using elsA [204]. The former is used to estimate the power savings without considering the fan performance whereas the latter isolates the fan stage using the steady computation for the inlet BC. A comparative assessment of different URANS codes on the fan/airframe aerodynamic is performed in Ref. [102] using an isolated ultra-high BPR turbofan which can be installed on the ONERA NOVA gull-wing configuration. The results obtained by Godard and Negulescu suggest a PSC greater than 10% although the distortion is not considered - resulting in fan performance loss. After completing a fan design optimisation on an isolated engine, half of the initial performance loss was restored albeit the authors emphasise that the fan stage design must be coupled to the airframe to accurately represent the integrated losses.

2.5.2.2 MIT-Aurora-Pratt & Whitney D8

In a different fashion, MIT, Aurora and Pratt & Whitney joined efforts in the design of the D8, also referred as the “double-bubble” aircraft, or simply D8, in reference to its shape. Initially designed with three engines at its back, the revised version

dropped one to comply with the operational and economic drivers of 2011 [26]. D8's uniqueness lies in its layout: a wide twin-aisle lifting body - producing 19% of the aircraft's total lift, upturned nose, shrunked, smaller, lighter and nearly unswept wings and two flush-mounted BLI turbofans embedded within a π -tail (Fig. 2.13b).

Drela outlines the considerations in the aerodynamic development of the D8 through a preliminary vehicle multidisciplinary optimisation (TASOPT [87]) [26]. The motivation for using TASOPT over other codes is that it uses structural theory, variable wing aerofoils and CFD for drag prediction, full engine flow-path simulations and variable flight trajectory, in lieu of historical correlations, wetted-area methods, tables/decks and correlations, and fixed cruise profile, respectively. Although TASOPT acts as the main evaluation tool for the configuration, the different components were developed using *traditional* methods [26]. In addition, as a means of validating the aerodynamic performance and stability characteristics, experiments on a 1:20 scale model in a wind-tunnel were performed. Results suggest a close correlation between the experiments and TASOPT with the latter under-predicting the lift-to-drag ratio by about 10%. When compared to a Boeing 737-800, the author claims a fuel-burn reduction of up to 33% which provides a launchpad for further investigations. However, it is difficult to decipher what proportion of these benefits may be directly attributed to BLI as a mechanism, versus all of the other drastic airframe configuration changes.

To complement that work, the benefits are assessed from a CFD standpoint whereby the mechanical power is predicted to be reduced by 9% when the axial net force of the D8 and reference aircraft match [130, 240]. A groundwork is established on a clean aircraft with the CFD validation of wind-tunnel conditions at different angle of attacks [130]. Good agreements with the experimental data are obtained enabling future studies with a BLI-mounted propulsor [240]. Additional examinations indicate that a reduction of around 6% of the overall dissipation is obtained with the integrated engines compared to their podded form.

Later, Uranga et al. [20, 83] completed these studies with additional experimental investigations in the NASA Langley 14×22 ft subsonic wind-tunnel on two 1:11 scale models of the D8 (podded and integrated configurations). A 6% electrical power saving was observed, a surrogate of fuel burn, with a 95% confidence interval of 2.3% in power. Nevertheless, those benefits only contribute at system-level and wind-tunnel scale. At full-size, the D8 fuel savings estimates amount to 15% when secondary drag and weight reductions are considered. Notwithstanding the fuel burn

benefits claimed, those results are shown to be rather conservative when compared to the N+3 Phase 1 that predicted a reduction of 36% [264]. To associate the savings to their benefits sources, Ref. [184] investigated the same pair of configurations with constant nozzle areas between the podded and BLI layouts and assessed the reduction in mechanical power as shown in Fig. 2.26.

It is reported a reduction of 57-69% of jet dissipation, 23-38% of surface dissipation and 5-8% in wake dissipation between the configurations leading to an equivalent increase of 3% in propulsive efficiency or a cruise power requirement reduction of between 7.6 and 8.5% by ingesting between 9 and 14% of the airframe dissipation. In practice, if the BLI configuration flies at the same power demand as the podded configuration, the BLI propulsors would be about 40% smaller in size, thus lighter.

Taking into account incidence angles, a synergistic numerical-experimental performance assessment is carried out by Hall et al. [76] on a 1:11 scale model for various propulsive power settings. In the wind-tunnel, the fan inlet distortion is investigated and the numerical set-up allows for the entire flow-field to be examined which assists in interpreting the underlying mechanisms. Three mechanisms are observed: a reduced stagnation pressure in the BL and stream-wise vorticity through the rolling up over the fuselage are consequences of the aircraft's angle of attack, and fan distortion interaction that results from the combination of near- and far-field effects.

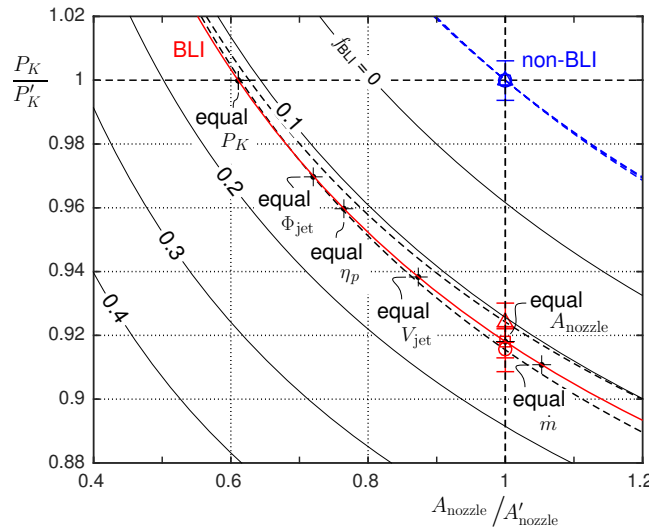


Figure 2.26: Range of potential BLI propulsors with their cruise power reduction at different nozzle areas. Prime indicates non-BLI quantities and non-primed quantities relates to BLI, retrieved from Ref. [184]

From the D8 design initiated in 2011, the various studies performed confirmed the feasibility of the figures claimed as part of the N+3 Phase 1 program. Thus, Yutko et al. present a thorough conceptual design by assessing the aircraft weight, balance and airline operations as well as the aircraft and BLI propulsor aerodynamic performance through CFD studies [138]. The analysis is performed on an EIS of 2016 and 2035 using foreseeable improvements in technologies. Through the use of a in-house code, higher fidelity estimates are given on a full design cycle with a 26% block fuel improvement, compared to a B737-800, which compares well with the 27% estimated by TASOPT.

Whereas the double-bubble conceptual aircraft seems promising from its potential benefits claimed, a question arises as regards the reference aircraft one should consider, i.e. a D8 unpowered equivalent design or a conventional A320/B737 aircraft. Whether one or the other, PSC or fuel block benefits can differ. It is also noteworthy that the D8 is designed to fly at M0.72 which highly contributes towards advantageous reductions when compared to the ONERA NOVA flying at M0.82.

2.5.2.3 Other Significant ORFEC Studies

At ONERA, aeroacoustics studies have been performed on the different NOVA configurations. Mincu et al. developed ghost immersed BC to non-negligibly reduce the computational demand whilst preserving the quality of the results [210]. They firstly validated their code on generic shapes to then applied it to full aircraft configurations, e.g. Airbus A320-like and NOVA, for the evaluation of noise shielding. Later, Lorteau et al. extended the work with over-wing nacelle configurations that are compared to under-wing engines mounted on a NOVA-derived airframe using the immersed BC for fan noise installation assessments at take-off [160]. They highlight that over-wing engines could reduce the perceived noise by 12 decibels (dB) when compared to their reference.

On the ONERA NOVA BLI configuration, Delft University of Technology with Romani et al. [182, 183] investigated the fan noise and its turbulent flow effects developing. For that numerical study, a high-order modelling coupled with a CFD/CAA analysis was performed on the whole aircraft with a fan/OGV stage model. The highly distorted flow going through the S-duct-like intake and propulsor provokes unsteadiness at the fan stage that is represented by haystack

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peaks. Nonetheless, the BLI configuration, compared to an isolated engine, suggests a 5 to 10dB reduction on the ground arc encouraging the use of the ONERA-designed aircraft for noise reduction targets.

Similarly to the ONERA NOVA, DLR, the German Aerospace Centre, derived a BLI concept from the Airbus A320 family. Implementing the foreseen technologies of 2035, a baseline aircraft is designed to assist with the BLI performance benefits. Silberhorn et al. [139] designed two intermediary designs to isolate the impact of the BLI propulsion system's integration and its benefits. They reported that major benefits arise from the tailplane whose conventional shape has been substituted by a T-tail, reducing the wetted area. The semi-buried turbofans on either side of the rear-end fuselage yield a 1.1% fuel benefit which questions the benefits of such configuration when considering the existing uncertainties.

At the conceptual level, an emphasis is given to the sensitivity of the BLI effects to the ORFEC airframe designs in Ref. [131]. They perform numerous CFD investigations on different key design variables in the scope of identifying and classifying the critical parameters. With the use of the power balance method, they highlight that the axial and circumferential position of the BLI propulsor plays a key role - especially as the wings have a great impact. The macro parameters such as the length, aspect ratio or angles play a more important role than the detailed parameters, e.g. radii, camber angles, nose shape, for the reason that shock or separation is more likely to happen with *important* design changes.

In the same vein as the joint MIT-Aurora-P&W D8, NASA contributed towards the understanding and development of the same aircraft layout but adapted its flight mission from a range of 3,000nmi at a cruise altitude of 37,000ft and flying 180 passengers at M0.72 originally, to the same mission but a flight Mach number of 0.785. For differentiation, the next studies described will refer to the NASA D8 (ND8). Marien et al. [124] assessed the fuel consumption impact of the ND8 BLI configuration whereby the BLI effects were considered in terms of wake dissipation reduction and propulsive efficiency increase, and penalties with the inlet pressure loss increase, propulsor efficiency reduction and weight increase. Several references have been considered to benchmark the BLI benefits: a 2.8% block fuel increase is noted when analytically disabling the BLI impact. When taking into reference the ND8 underwing-podded version, the fuel block decreases by 5.6%. Regardless of the reference configuration considered, Marien et al. notably showed that the D8 aircraft flying at their flight mission outperforms any kind of non-BLI aircraft in terms of

fuel block reduction but presents lower benefits than the original D8 mission. To complement that work at NASA, Clark et al. assessed the aircraft system noise of the ND8 [265, 266], and that of the ND8 and single-aisle tube-and-wing concepts [267] to comply with the NASA N+3 goals.

2.5.3 Disruptive Propulsive Concepts

Despite the previous concepts' differences from the customary aircraft, one could argue that they are still relatively close to a conventional airframe. Therefore, this last category gathers disruptive propulsive concepts (DPC) which utterly revisit the design of an aeroplane.

They appear in the form of hybrid-wing or blended-wing bodies (HWB and BWB, respectively) prevailing in the most synergistic and seamless embedded propulsion systems integration. Because these designs differ from one another, they cannot be introduced and described as a unique one. Nevertheless, amongst those concepts, the ONERA DRAGON, the EU DisPURSAL, the CMI SAX-40 or the NASA N2 and N3-X can be named wherefrom the majority arranges an array of propulsors electrically driven by another propulsion system. A comparative summary of the aircraft and propulsion systems' characteristics is given in Section 2.B.

Such configurations bring additional technological challenges that must be understood up-front despite being of interest for more than two decades [153]. Laskaridis et al. provide a top-level assessment of distributed propulsion concepts applied to different aircraft architectures and mission specifications demonstrating the possibility of novel synergistic technologies [248]. These technologies and concepts based on distributed electric propulsion are also reviewed by Kim et al. [41] and particular attention is given to the evolving trends in BWB aircraft designs by Okonkwo and Smith [40].

2.5.3.1 EU DisPURSAL

Previously, the EU-funded project DisPURSAL was discussed with its PFC but initially, a DPC was investigated featuring a HWB whence an array of propulsors is mounted on the upper side of the airframe. Within the array, four BLI propulsors are powered by two gas-turbine engines laid as two groups of three propulsion systems (Fig. 2.27).

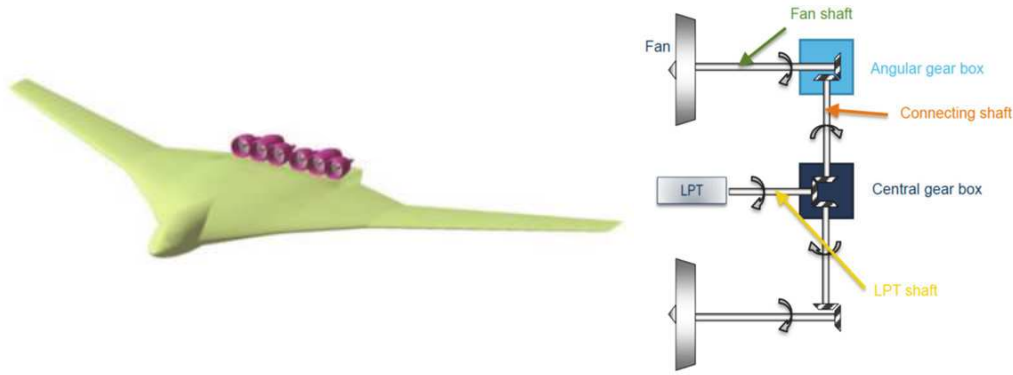


Figure 2.27: Schematic of the DisPURSAL architecture and power transmission, modified from Ref. [49]

Alike the PFC, the DisPURSAL DPC is conceived to perform a 4,800nmi mission accommodating 340 passengers using an Airbus A330-300 for benchmarking the performance benefits. Isikveren et al. [49, 158] provide a down-selection of the aircraft characteristics amongst 20 identified candidates taking inspiration from Steiner et al. [80]. In this previous work, a qualitative evaluation of the integrated and distributed propulsion system configurations is given based on the power system integration, its efficiency, operability, weight and noise. After rating all the configurations based on six categories and twenty-nine criteria following a procedure akin to Steiner et al., the retained layout by Ref. [49] is composed of two pairs of propulsion systems which are themselves composed of a core gas-turbine and two mechanically-driven fans on either sides, amounting to six propulsive modules. A preliminary 2D set of computations suggests that efforts should be given during the project on the design parameters as they inherently affect the Mach distribution and lift: higher FPR accelerate the flow upstream reducing the BL thickness, and high incidence favours shocks along the aircraft's upper surface thickening the BL ingested. The initial results indicate a 9% reduction in fuel burn compared to an EIS 2035 A330-like aircraft, albeit based on low-fidelity results (2D with a span-wise cut at the engine axis).

Despite showing promising BLI benefits, the DPC bring new architectural challenges. Amongst fully- mechanical, turbo-electric and fully-electric distributive systems, Isyanov et al. conclude that the former is the most feasible in the near future thanks to its minimal technical risk however it might not be the most efficient system [241]. When super-conductive technologies becomes available, alongside electrical components improvements, fully-electric transmission systems appear as the best performing architectures regardless of the positions of the core

engines and driven fans. Yet, this remains assumptions and experimental or numerical campaigns are necessary to approve the configurations' performance and efficiencies.

To follow with a more thorough comparative efficiency analysis, Mirzoyan et al. [242] oppose their potential DPC with their PFC and two reference aeroplanes, namely an Airbus A330-300 reflecting an EIS year 2000 and 2035. For comparable missions, the DPC aircraft reduces the block fuel by 38% compared to its EIS 2000 reference and by just below 8% compared to the 2035 reference, just like the PFC. With regards to the environmental challenges, both concepts should meet the noise requirements with the DPC outperforming the PFC thanks to its shielding. NO_x and CO₂ levels are predicted to be up to the same standards and comply with the ACARE targets with a reduction of 40-42% compared to the 2000-reference aircraft.

2.5.3.2 CMI SAX-40

With the aim of designing an aircraft whose noise would be almost imperceptible around an airport, the Silent Aircraft Initiative was born from the partnership between Cambridge University, U.K., and MIT, USA, with their Cambridge-MIT Institute (CMI). Their concept aircraft, the Silent Aircraft eXperimental, is a result of an iterative design process (from SAX-01 to SAX-40) to achieve low noise and improved fuel burn. The observation made on current aircraft is that the engines are the main sources of noise generation with the rotating fan and high-speed jet. On approach, the noise induced by the airframe contributes as much as the engines with the flaps, slats and undercarriage. To mitigate this perceived noise, a greater integration of the engine and aeroplane is necessary and a potential solution is to use the airframe to shield the engines' noise by installing them above the fuselage. Therefore, the SAX-40's layout is based on a BWB which installs three buried engines at its rear upper surface.

Based on Liebeck's work [132], merged fuselage and wing airframes suggest higher lift-to-drag ratios and significantly lower empty weight than conventional tube-and-wing aircraft. This observation motivated the CMI to investigate the engine installation on these layouts [244]. Following some constraints on the structure behaviour, payload, fuel tanks and doors, the most feasible location is judged to be above, or within, the centre-body, and behind the cabin, to benefit from the ingestion of the BL. The wings also aid with their large surface area

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allowing to accommodate the embedded engines at the back of the aircraft following an array architecture. The investigation of different configurations draws several conclusions on the future endeavour. A large exhaust area would contribute towards lower noise emissions, and embedding the engines will mitigate the drag and allow for boundary layer ingestion. Nonetheless, a trade-off needs to be made with regards to the engines count as a greater number will lower the weight and drag generated but also lower the thermal efficiency.

First, from the airframe side, multiple studies contributed towards its development for low-noise and low-emissions. From the SAX-10, slight modifications in the aircraft design led to the SAX-12 [194]. The configuration is derived from multidisciplinary designs models and optimisation which final version, i.e. SAX-12, is assessed in terms of performance. The combination of the different modules, namely the propulsion, the aerodynamic and mission profile, allowed for the weight and noise evaluation. Although suggesting promising behaviours, the figures still do not meet the aggressive targets of the “silent aircraft”. Dowling and Hynes [243] took over with a major revised version of the aircraft and its SAX-20 derivative. The disruptive configuration contributes towards shielding noise but to further reduce the perceived noise close to the airports, large and low speed jets from the engines are necessary. The SAX-20 using UHBR engines with variable nozzle exhausts still could not meet the target but the investigators were confident that further design iterations will allow it.

From the understanding gained on the engine installation, Hall and Crichton [140] and Crichton et al. [137] emphasised their work on the engine design and fan design, respectively. In the former study, a gearbox and other shaft speed reduction devices (multi-spool) - coupled with a low-speed fan and high gap-chord spacing turbine, are proposed to further reduce the turbomachinery source noise on a three-engine architecture. The latter study, takes back the initial configuration with four engines and focus on the fan design that will efficiently propel the aircraft at both take-off and cruise conditions whilst keeping low the noise emissions. Further designs iterations led to the most-known layout of the “silent aircraft”, the SAX-40. The joint efforts from the different partners resulted in parallel developments on the airframe design [133], engine design [268], BLI performance [47] and off-design noise performance - take-off [245] and approach [269].

It has been mentioned that the SAX airframe has seen several aerodynamic and aeroacoustic improvements which led to the changes in design observable in



Figure 2.28: Main conceptual aircraft designs generations: SAX-12, SAX-20, SAX-40, retrieved from Ref. [133]

Fig. 2.28. The SAX-12 was designed to minimise the take-off weight accommodating four semi-buried engines consuming 88 passenger-miles per gallon and generating 80 and 83 dBA (A-weighted correction on decibels) of noise at the airport perimeter at take-off and approach, respectively. Moving towards higher numerical fidelities, the second generation of SAX was aimed at reducing stall speed. Therefore, the centre-body leading-edge design became critical resulting in revisions from SAX-15 to SAX-29. In that configuration, three engines were favoured and 3D RANS calculations were computed. The methodology developed therein was validated such that the centre-body shape of the SAX-29 could be used in the subsequent designs. Hence, the third generation was obtained using optimisation capabilities for reducing noise and fuel burn. The final version, the SAX-40, incorporates three BLI gas generators driving nine fans reducing the fuel burn from 88 passenger-miles per gallon with the SAX-12 to 124 and the noise emissions to 63 dBA corresponding to the background noise of a well-populated area. In conclusion, the SAX-40 conceptual aircraft achieved the objectives set despite integrating a number of technical challenges that could yield considerable risks that need to be overcome [121, 133].

Although the “silent aircraft” main aim was to design an aeroplane acoustically unnoticeable around the airports, other challenges in terms of fuel consumption and mechanical reliability needed to be accounted for. As an answer to these aspects, a BLI multi-fan embedded turbofan is proposed integrating a variable area nozzle, a low-pressure turbine, an axial-radial high-pressure compressor, and a low-speed fan optimised for cruise and take-off conditions and advanced acoustic liners [268]. The assessment of the BLI propulsion system performance was conducted by Plas et al. [47] introducing the power saving coefficient (PSC), the ratio of ingested drag to the total aircraft drag, distortion and efficiencies. Its application to the SAX-40 resulted in a 3 to 4% decrease in power input and a PSC of 10-40% depending on the fidelity of the fan model chosen.

2.5.3.3 NASA N2 to N3-X

With regards to NASA's efforts to meet their environmental goals [5, 6], a campaign had been initiated with their N+1, N+2 and N+3 programs. Their aircraft concepts belong to each aircraft's architecture herein reviewed with the STARC-ABL as PFC, the D8 for the ORFEC and the N3-X for this latter category (DPC). Hendricks [42] reviewed NASA's in-house BLI modelling efforts and methodologies, weighing the strengths and weaknesses of each. More specifically on the HWB and BWB concepts, Okonkwo and Smith [40] reviewed the evolving trends of those concepts whilst Liou et al. [270] reported the challenges and progress of HWB aircraft with a focus on the embedded propulsion systems of the NASA N2-B and N3-X.

Initially, the NASA N2-A concept originated in the 2000s with two pylon-mounted turbofan engines at the top-back of the wing-body. In 2006, NASA funded a one-year project to evaluate the synergistic benefits of distributed propulsion and airframe integration on the CESTOL configuration (Cruise Efficient Short Take-Off and Landing) propelled by twelve high BPR turbofans [271, 272]. In 2011, Boeing concluded that the N+2 goals could be reached with a revised version of the N2-A called the N2A-ExTE. In contrast, the N2-B concept embeds three BLI propulsion systems on the upper surface of the wing-body, each composed of three side-by-side propulsors driven by a common turbine [273]. The N3-X was a direct answer to N+3 goals and can accommodate 300 passengers in its HWB form where two wing-tip mounted turboshafts power an array of propulsors embedded in a mail-slot-like nacelle on the body's upper-surface at its trailing-edge. This architecture echoes the TeDP using superconducting generators and electrical transmission systems where the specifications are discussed in Ref. [28]. A complementary study on the effects of BLI on TeDP systems recommended a narrowed-down range of additional, detailed, studies to be conducted at higher fidelity. [150]

In that respect, a couple of studies from Florea et al. [122, 187] contributed towards the development of the N2A-ExTE's propulsion system. First, to demonstrate the feasibility of a low-distortion BLI propulsor, coupled inlet/fan design optimisations are performed. To target a minimum total pressure loss and distortion, and a preliminary assessment on the fan performance impact, 3D CFD on different intake designs were introduced. This study led to an optimal design whose intake length is reduced by five times relative to the initial design yielding a

3-5% fuel burn benefit relative to a pylon-mounted configuration. Additionally, shrinking the inlet allowed a reduction in pressure loss (maintained below 0.4%) and distortion harmonic amplitudes by 30-50%. To carry on with a distortion-tolerant fan design, Florea et al. [186] analysed the aerodynamics of a coupled inlet/fan stage. Also, an experimental wind-tunnel inlet/fan configuration was designed and run for the preparation of an experimental campaign. These investigations are completed by Bakhle et al. with their aeromechanic analyses of a BLI fan [274]. In parallel to these system-level studies, the aircraft-level investigation of its aerodynamics is conducted by Hardin et al. [152]. The propulsive efficiency is assessed by Ochs et al. [64] and the benefits resulting from BLI are identified. These benefits amount to around 4% for a fan that has been primarily designed for aerodynamic performance without additional nacelle drag. Hardin et al. [152] reported a reduction of drag from 2 to 3% from the lack of pylon and reduction in nacelle wetted surface area, thus Ochs et al. expect benefits of up to 7% with these losses.

From the Friedman's [275] 3D CFD, the BL, Mach number and pressure profiles were extracted for Felder et al. [150] to design the N3-X concept. With the use of FLOPS [253] and NPSS [209], for the mission range and propulsion system assessments, the actual TeDP concept is derived from the Boeing 777-200LR. The N2A-ExTE therefore predicts to reduce the fuel burn by 70-72% compared to the B777 reference aircraft, depending on the technology used, i.e. cryo-cooled and LH₂-cooled, respectively. The strategic position of the propulsors' array at 85% of the aircraft chord also plays an important role in its propulsion and noise shielding. A noise and emissions assessment is conducted by Berton and Haller [237] on two derived N3-X versions, one with thrust-vectoring, and the other with relocated turboshafts to reduce noise propagation. The latter version exceeds the N+3 objectives by 12EPNdB with a noise emission of 64EPNdB (Effective Perceived Noise in decibels).

A multi-fidelity methodology is proposed by Kim and Liou [176] based on a BFM. The model is implemented on a 3D CFD model of the N3-X and is validated against the R4 fan rotor designed by General Electric Aircraft Engines and tested at the NASA Research Glenn Center. At M0.84, FL350 and for an angle of attack of 2°, sectional side views of the propulsors array are presented where a strong shock is observed at the centreline. Additionally, the difference in BL thickness is noted due to the chord length travelled.

Complementary to those efforts, a NASA grant was awarded to Cranfield University in 2013, following the work from Liu et al. [126] on distributed propulsion systems, including TeDP. They built low-order models for assessing the BLI at different inlet conditions, and the weight estimates of the propulsion system and the thermal cycle at both on- and off-design conditions. The effects of BLI on the propulsion models are also examined predicting a fuel burn reduction of 10% considering cryo-cooling and superconductors with the generators' efficiency above 99% and where recommendations on the FPR and equivalent BPR (eBPR) are given to maximise those benefits. Notably, they highlight that the benefits are highly affected by the intake losses, as noticed by Florea et al. [122, 187], and the evaluation of the inlet flow distortion needs to be further assessed as it strongly ties to the fidelity of the modelling. Thus, to improve the inlet distortion evaluation fidelity, Liu et al. [127] provide a modelling approach which examines both radial and circumferential distortion by separating their various impacts. Compared to other methods, lower benefits are expected with notably a 0.5-1.5% drop in PSC. Nonetheless, those studies are independent of blade shape and only suitable in the preliminary stages. It is believed that the consideration of new blade designs could curb fan pressure losses and efficiency drops.

To provide insights into the potential benefits of distributed propulsion and BLI, Kirner et al. developed a tool on conventional tube-and-wing configurations [246], and followed up with BWB configurations [247]. The latter assessed stability, mass estimation, drag prediction and simplified distortion analysis based on 2D power-law profiles. The figures give a 5.3% benefit in fuel burn for a 2% total pressure loss and a 7% benefit if the losses are reduced to 1%. Kirner [276] also demonstrated that altering the mass-flow and thrust fraction produced by the fans can mitigate these duct losses and hence, offer more benefits.

Merging technologies, Valencia [81] presented a preliminary design evaluation of different propulsion system architectures involving synergies from distributed propulsion, BLI and electrical pieces of equipment for hybrid-electric propulsion. Despite the potential benefits that can arise from the technologies' implementation, the main challenges originated from the high level of integration, leading to mechanical design complexities, intake pressure losses and BLI induced distortion.

At a system level, Valencia et al. [129] assessed the performance of TeDP using a 1D approach to model the effects of distortion on propulsor performance. To maximise the benefits available with current technologies, the thrust-split concept

was introduced and showed a reduction in thrust-specific fuel consumption with an optimum obtained at a TS of 65%. A step further in the propulsion system modelling led to a comparative study between parametric 1D and parallel compressor methods [128]. Despite offering comparable benefits, the 1D approach tends to show optima at lower FPR compared to the parallel compressor method. The reason stands in the high sensitivity to intake losses captured by the latter. Additionally, for low thrust-splits, turbofans with higher BPR were more favourable but minimal thrust-specific fuel consumption was obtained for thrust-splits above 60%.

In a rapid preliminary analysis of BLI propulsor performance, Goldberg et al. [104] reported the influence of local flow behaviours on both individual propulsors as well as the entire array. The span-wise flow variation resulted in an increase in power demand due to the BL thinning and the higher Mach number at the AIP [106]. The work is further extended with the method application to off-design conditions [107]. For comparison, the propulsors at the centreline showcase better performance at off-design due to the difference in flow condition than at the edge of it. The on- and off-design performance in specific-power and -propulsive force is illustrated in Fig. 2.29. Importantly, it is stressed that the map is obtained for 100% rotational speed, not representative of the full flight envelope. A full mission is thus investigated [141] and recommendations are given on the optimal thrust-split to reduce the fuel consumption by a mere 1.5%.

An exergy management formulation proposed by Arntz et al. [70] was introduced for predicting the aerodynamic performance of unpowered configurations. The validation of the code is achieved through various 2D and 3D test cases and compared against a momentum near-field approach. The authors highlight that

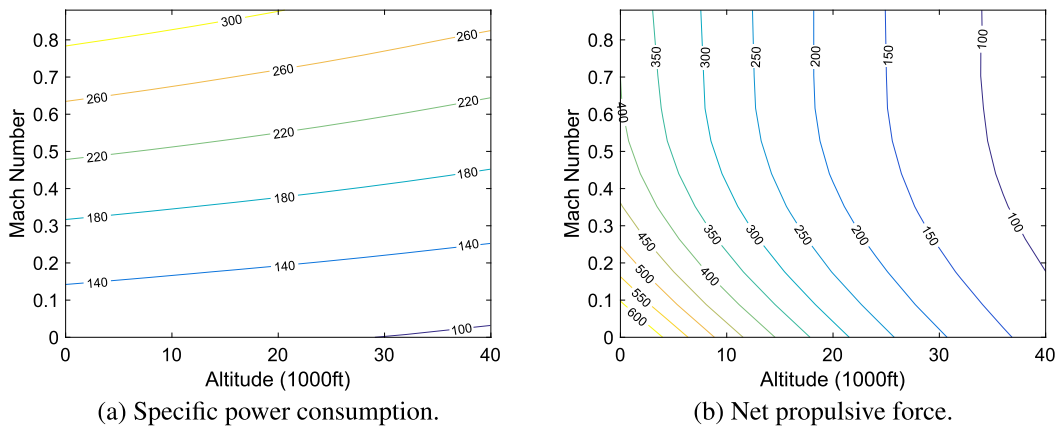


Figure 2.29: Propulsor array performance in terms of (a) specific power consumption and (b) net propulsive force as a function of altitude and Mach number, from [107]

approximations in the velocity gradients could lead to discrepancies on coarser meshes and recommend a fine grid to be used to mitigate those penalties. Therefore, an application on a N3-X 2D representation led to the conclusion that an exergy-based approach is to favour aero-thermo-propulsive performance as the decoupling between the internal and external flow is unnecessary [72]. BLI improvements of up to 50% are reported and little exergy is wasted in the jet and wake exhibiting an exergy-waste coefficient (EWC) lower than 3%. Additionally, the implementation of heat transfer upstream of the propulsion system yields a further 1.5% fuel burn benefit. Similarly Sato [34] introduced liquefied natural gas along with proper thermal management techniques which allowed for cooling the outer wing delaying laminar to turbulent transition and promoting favourable laminar flow on the wing surfaces. This yielded an additional 2% in fuel burn reduction when compared to conventional jet fuels amounting to a total reduction of 57% relative to a Boeing 777-200LR reference. The propulsion system performance was estimated from cycle performance tools and coupled to an aerodynamic assessment of the airframe based on 3D CFD using an energy analysis. The BL properties as well as the wake, shock and vortex- induced dissipation sources were identified in this method.

With the introduction of synergistic technology combinations by Sato [34] with liquefied natural gas and Arntz and Atinault [72] with heated walls, Smith and Laskaridis [277, 278] offered an airframe integration for LH₂ hybrid-electric propulsion system. Despite the challenges associated with BWB, one advantage offered is the implementation of liquid hydrogen tanks. A preliminary design is suggested highlighting a number of specific issues and their potential solutions. On a wider range, accounting for short-, medium- and long-range mission aircraft, Rompokos et al. review synergistic technology combinations by short-listing two potential candidates based on thirty-four criteria [279].

2.5.3.4 ONERA DRAGON

In another fashion, ONERA developed the DRAGON (Distributed fans Research Aircraft with electric Generators by ONERA) that is not based on HWB or BWB but incorporates distributed propulsors located along the wings' pressure sides that are powered by two turboshafts installed on either side of the rear-fuselage, as illustrated in Fig. 2.30 (left). It is known that lowering the FPR whilst increasing the BPR



Figure 2.30: ONERA DRAGON [167], ESAero ECO-150 [136] and NASA CHEETA [282]

contributes to higher efficiencies however, it is jeopardised by operability, integration, size and weight challenges. Dividing the main turbofan into an array of fans allows to maintain a low FPR whilst maintaining full operability using a hybrid-electric distributed propulsion solution [134].

ONERA's expertise started in 2013 with the AMPERE project whereby distributed electric propulsion was studied on a high-wing concept plane incorporating 40 electric-ducted fans located along the wing-span leading-edge of a small business aircraft [280]. To better understand the mechanisms associated with such configurations, numerical simulations were carried out along a wind-tunnel campaign for providing data validation. A collaboration with ISAE-Supaero, France, allowed the design space exploration of a large passenger aircraft [281]. From these campaigns, they extended further their understanding of aero-propulsive layout by investigating a high-speed distributed propulsive concept [134].

To comply with today's aeroplanes, the DRAGON is investigated for a mission of 1,200nmi at a cruise Mach number of 0.8 accommodating 150 passengers [134]. A 2D study assessed the interactions between the aerofoil and a fan installed at its rear-lower surface. A design is selected to provide a clean supersonic flow over the wing whilst preventing separation on the pressure side but nonetheless requires further investigation on 3D models to confirm the design on a flap-wing integration. Moreover, an initial mission assessment advised to reduce the range from 1,200nmi to 800nmi for comparison with short-to-medium range aircraft. With these top-level aircraft requirements, the DRAGON reduces the fuel burn by 8.5% when compared to an EIS 2035 podded aircraft. Extending that investigation, it is suggested that the DRAGON performs better than its reference as long as the range remains below 1,100nmi and that uncountable improvements at disciplinary- and system-levels could be obtained as the aircraft did not undergo any optimisation.

From the understanding gained in Ref. [134], 3D RANS computations were performed [167]. Aware of the unrealistic 2D modelling, a step-by-step approach is

followed to move to 3D computations. Still, numerous challenges arise from this switch: periodicity between the fans must be ensured, inlet and nozzle sections must be well-defined, the 2D aerofoil needs to be represented with a cylindrical section before the inlet and the FPR needs to be incrementally increased to ensure convergence. A comparison of the models highlights good agreement despite operating at different FPR. In spite of concluding that the DRAGON performs better for a mission range under 1,100nmi [134], Freeman and Schiltgen report that turbo-electric distributed propulsion aircraft prove most benefits on a 3,500nmi design mission, such as performed by an Airbus A320 [136]. Consequently, ONERA's efforts focused at a 2,750nmi range to size their fully-distributive electric propulsion aircraft. For this design mission, 26 ducted fans powered by two turbo-shafts offer a reduction of 7% in fuel burn, against an EIS 2035.

Notwithstanding the long-term goals envisaged by the DPC, the concept appears as the most widespread architecture and it is the authors' decision to present only the ones above. Numerous other studies have been done by fellow researchers and are of interest. Amongst those, can be named the TU Delft Flying-V², which reduced model flying campaign already started despite lacking the BLI technology, the ESAero ECO-150 [135, 136, 283, 284] and NASA CHEETA [282] which designs can be compared with the ONERA DRAGON (Fig. 2.30), and the Airbus projects MAVERIC³ whose scaled flying demonstrator already performed a flight or the ZEROe concepts with their BWB⁴ based on a hydrogen propelling system.

2.5.4 Foreseeable Benefits

Numerous studies, led by institutions and universities, contribute towards the understanding of BLI through their various aircraft concepts. Whether they focus more on the noise or fuel burn reductions, all studies intend to comply with the challenging targets set by the EU and NASA.

The various architectures give rise to promising environmental-friendly contributions that are partly summarised in Fig. 2.31. Although they are compared in terms of fuel burn benefits and power savings, they consider their own references to highlight the BLI benefits and a fair comparison is only possible if

²TU Delft Flying V

³Airbus MAVERIC

⁴Airbus ZEROe

they share a common conventional reference aircraft at similar mission. Yet, Fig. 2.31 indicates a qualitative comparison between the concepts for their respective references given in Section 2.B. The power savings are reported against their podded equivalent to demonstrate the benefits of BLI only whereas the fuel savings are given against a reference aircraft showcasing synergistic benefits.

It is opined that the PFC, based on tube-and-wing configurations, suggest less fuel burn benefits as only the BLI benefits are assessed whereas the new propulsion architectures implemented in the ORFEC and DPC yield higher fuel burn reductions as benefits arise from other sources - e.g. airframe and propulsion architectures.

Particularly for the N3-X, very high benefits are claimed, but put into perspective, when compared to a pylon-mounted reference aircraft, the fuel consumption is reduced by *only* 18% from the sole BLI benefits as majority of the reductions result from the aircraft configuration and its transmission architecture [150]. Specifically, between the N3-X and SAX-40, a large difference is observed in power savings and fuel burn reduction but it is reminded that efforts were mainly given to acoustic performance and noise reduction for the latter.

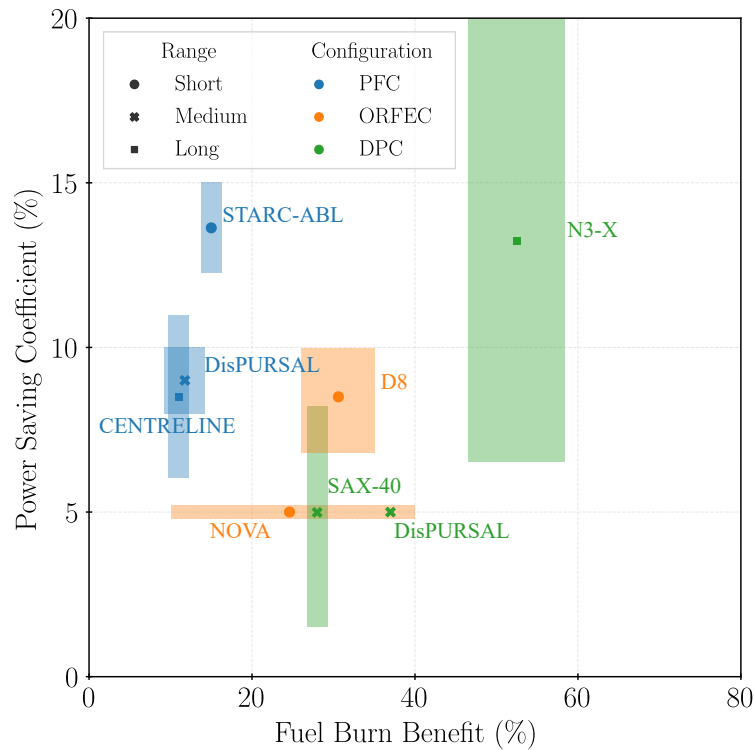


Figure 2.31: Boundary layer ingestion benefits in power savings coefficient, relative to the concepts' podded equivalent, and technology and synergistic benefits in fuel burn, relative to their tube-and-wing reference

2.6 Conclusions

In this review, the literature on boundary layer ingestion propulsion concepts was scrutinised to identify and collate the different approaches employed to evaluate the performance as well as the numerical and experimental models and set-ups (Fig. 2.9), and the design exploration and optimisation of prospective civil aircraft. The diverse challenges and their present achievements were presented in accordance with a classification of the different aircraft concepts (Fig. 2.14). A time-line of these concepts and progress, amongst others, is depicted in Fig. 2.32 for the retrieved literature.

As regards the different performance evaluation approaches, energy- and exergy-based analyses yield the most information (Table 2.3). The introduction of the absolute reference frame with these book-keeping schemes enhances the interpretations one might perform on the aerodynamics with causal work-energy pathways and the underpinning boundary layer ingestion mechanisms. Nonetheless, momentum-based approaches are usable if the net axial force suffice, e.g. optimisation studies. Ideally, these approaches should be used in synergy, unlike suggested by Hendricks [42] who sees the approaches serving different

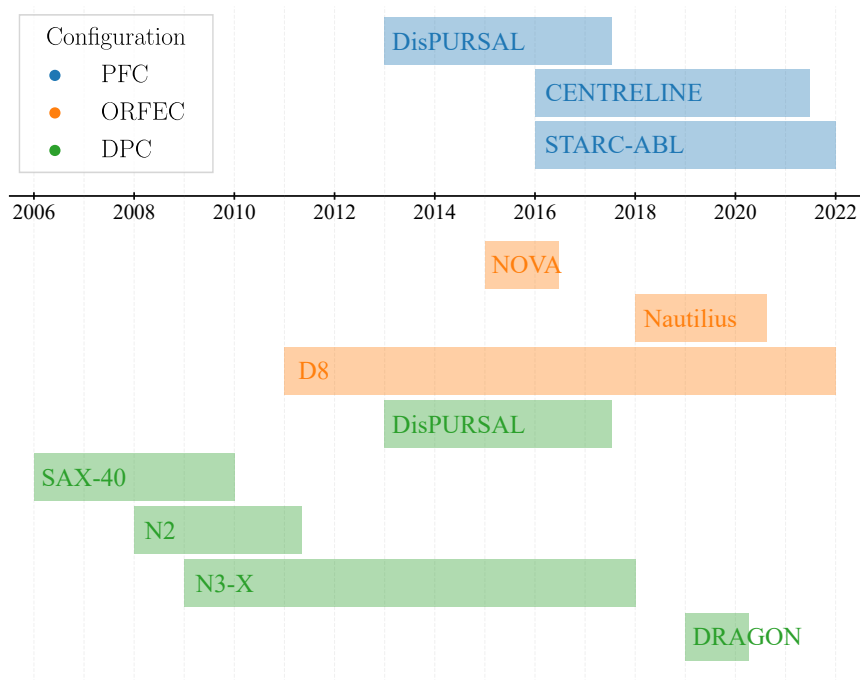


Figure 2.32: Timeline of aircraft concepts featuring boundary layer ingestion based on the literature retrieved

purposes. Although initial work attempts to unify all approaches, efforts should keep going into that direction and eventually lead to a unique and universal book-keeping scheme for highly integrated aircraft. Additionally, the numerical modelling plays an important role in the aero-propulsive performance reported. As an example, boundary conditions to mimic the propulsor's fan ideally fill the defects in the flow and do not transport the total pressure distortion through the stage which would reduce the potential savings.

To maximise the overall performance of these aircraft, strongly-coupled approaches need to be developed between the aircraft's aerodynamics and the propulsion system. This innately requires the development of multidisciplinary frameworks that allow inter-communications between the aerodynamic and propulsion solvers. Despite seeing current numerical efforts going into that direction, this requires the use of high-order fidelity tools which necessitates experimental validations. In spite of the progress in experimental instrumentations, set-ups and techniques that are reviewed, many challenges arise with respect to the similitude, i.e. geometric, kinematic and dynamic similarities, and current state-of-the-art work conducted at low-speed testing is preferred, often omitting the compressibility effects related to boundary layer ingestion.

Notwithstanding the different aircraft concepts to meet the environmental targets, rather *small* benefits arise from boundary layer ingestion. However, the majority of the concepts enable the use of other technologies, e.g. lighter materials, liquid hydrogen (LH₂), turbo-electric distributed propulsion, enabling synergistic and larger benefits.

- The propulsive fuselage concept (PFC) offers the opportunity to partially meet the aims in the short- term, particularly because of the lack of aero-acoustics analyses. A main advantage of this arrangement nonetheless is the *easy* inclusion of the aft-mounted propulsor from an engineering point of view but operability and safety investigations need to ensure its further development and use;
- The over- and rear-fuselage engine concept (ORFEC) still presents a premature development even if it demonstrates promising savings and intensive efforts in experimental techniques. The wide-body lifting- fuselage enables a reduction of the aircraft's length and an increase of the wing's aspect ratio both contributing to synergistic improvements that the PFC

cannot offer. Nonetheless, as the engines provide the entirety of the thrust requirement, safety studies need to also be performed to ensure safe flights in case of an inoperable engine;

- The disruptive propulsive concept (DPC) promises the greatest benefits in the long-term, including noise reduction from the body's shielding and facilitates the integration of liquid-hydrogen tank. However, multi-disciplinary challenges need to be overcome and a lack of optimisation and experimental studies prevents the full exploitation of the configuration's benefits.

It is emphasised that for only assessing the benefits from boundary layer ingestion, concerns should be given to the choice in reference aircraft. The difference between the boundary layer ingestion aircraft and its reference should only be on the propulsion systems installation, i.e. embedded against podded, otherwise synergistic benefits are captured, e.g. transmission, architecture, aerodynamics. Nevertheless, the comparison between the engines yields another challenge, should it compare size, mass-flow rate, or output force, encouraging the use of idealised scenarios to claim minimal achievable benefits. These observations raise several key points, an alteration in traditional performance accounting approaches must be considered, particularly taking note of the recent new energy- and exergy-based approaches to help discover absolute metrics that can quantify and characterise boundary layer ingestion performance, without having to resort to a comparison against a podded equivalent.

Lastly, recent efforts are observed in the field of experiments and aircraft scaling but numerous challenges are still present as regards the similitude between the full- and wind-tunnel-scale models. Despite the constant development of numerical solvers and the wide knowledge of the researchers in the field, experimental data are vital to ensure that the models used numerically accurately represent the reality. Nonetheless to compare, or validate, the data, efforts should not stop in the cutting-edge data acquisition techniques and uncertainty calculations to couple experiments with numerical investigations.

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Appendix 2.A Experimental Apparatuses Description

2.A.1 Wind-Tunnel and Model Installation

Wind-tunnels can be of different cross sections where the aircraft model is mounted in. Large subsonic wind-tunnels such as the NASA Langley 14- by 22-foot represented in Fig. 2.A.1 accommodates the D8 concept that is tested at low-speed conditions. The model is attached from the wing root with a pitch head and trunnion mounting system controlling the height and angle of attack, itself connected to the wind-tunnel floor.

In a different manner, the Delft Low Turbulence Tunnel has an octagonal cross section with a height of 1.25 m and a width of 1.8 m. Unlike the D8 which features wings and a π -tail, the aircraft model of the CENTRELINE project only presents

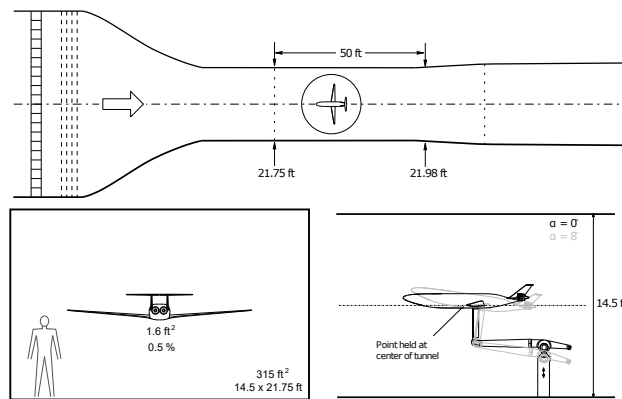


Figure 2.A.1: Schematic of the top, rear and side views of the MIT D8 aircraft in the NASA Langley’s 14×22 wind-tunnel, retrieved from Ref. [83]

an axi-symmetric fuselage connected to the floor and roof by fairings containing a support beam to measure the forces applied on the structure (Fig. 2.A.2).

As regards the model support, it should allow the model to cover the range of motion desired, e.g. yaw, pitch and roll, without obstructing any data acquisition from the instrumentation. The support must also respond to various needs: to be discrete from an aerodynamics standpoint, to allow supply and eventually return of energy or flow, and more importantly to insure mechanical hold with limited oscillations and deformations.

For rear-powered BLI architectures, sting and strut supports are depicted in Fig. 2.A.3. A single straight sting attachment (Fig. 2.A.3b) from the downstream end of the model can be used on PFC but prevents the measurement of any data in the wake. In place, Z-sting or fin sting supports (Figs. 2.A.3a and 2.A.3c) allow an important clearance behind the model for wake measurements but prevent skidding. With a strut, full mobility is provided whilst also freeing the wake from

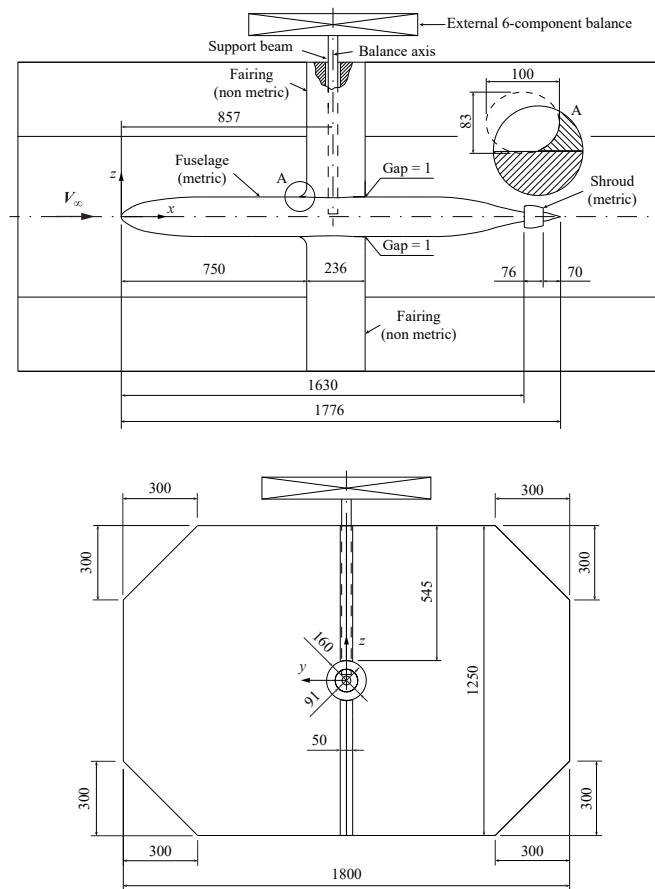


Figure 2.A.2: Schematic of the side and rear views of the CENTRELINE aircraft in the Low Turbulence Tunnel of Delft University of Technology, from Ref. [180]

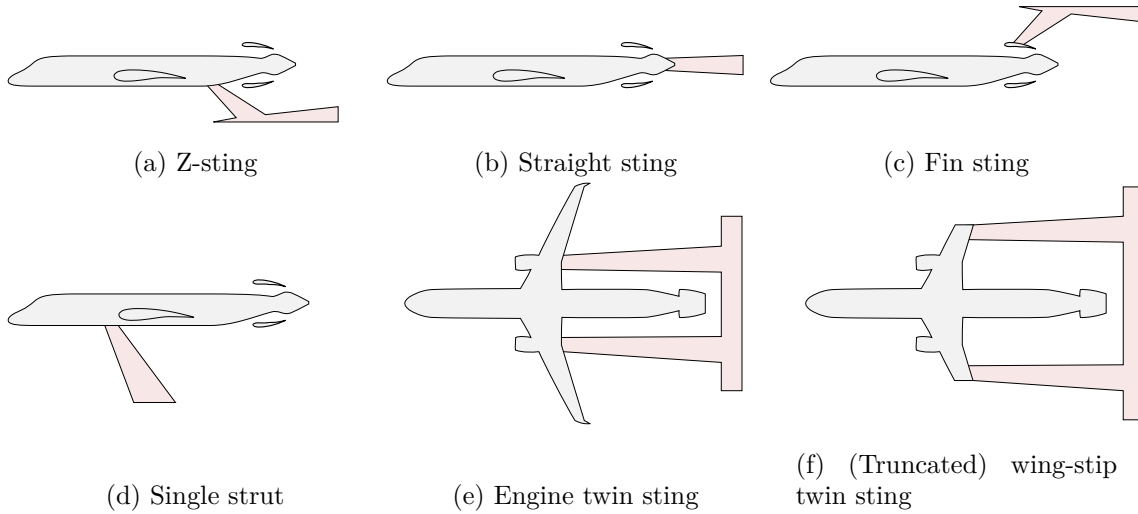


Figure 2.A.3: Schematics of wind-tunnel model supports applicable for powered boundary layer ingesting aircraft

any instrumentation. A combination of the Z-sting and strut supports is favoured for the MIT D8 [20, 83] (Fig. 2.A.1) whereas the CENTRELINE project used two struts at the top and bottom of the model (Fig. 2.A.2) mounted on the roof and floor of the wind-tunnel, respectively, but data correction is necessary to filter the perturbations caused by the struts [180]. Lastly, models can be supported at the tip of truncated wings (Fig. 2.A.3f) or from the under-wing engines (Fig. 2.A.3e) also allowing full mobility of the system as well as the instrumentation to be installed in the wake - to some extent. Nonetheless, data correction might also be necessary to remove the vortices generated around the booms.

2.A.2 Apparatuses

Measuring Balance

Further to the six-component balance as used by Ref. [165] and depicted in Fig. 2.A.4, Ref. [285] designed and developed a six-component balance with a floating frame to mount models on. The forces and moments acting on the model are measured by the six rods installed perpendicular to each other to obtain all components.

In essence, balances are a transducer that converts a load into an analogue output proportional to the force applied. Several balances exist which are more or less adapted to the different supports. The six-component balance is the most spread

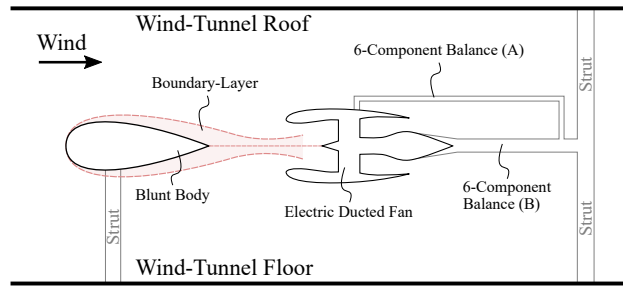


Figure 2.A.4: Schematic of the experimental set-up with measurements of (A) the nacelle drag and other forces and momentums, and (B) thrust, torque and other forces and momentums, remastered from Ref. [165]

apparatus for sting-mounted models but prevents any wake measurements. For the strut fixture, the balance can equally be used as the rear-region of the fuselage is *live* and comes to the form of a small or large flanged device. Rotating shaft balances are also designed to measure the torque during powered tests but the range of the forces and moments are significantly lower than the flanged balances.

As an alternative to balances, load cells can also be employed to measure the axial force on a system. The cells measure both compression and tension and are referred as pancake, S-beam - with higher accuracy, or in-line - more compact and with a larger force range possibility.

Wake Rake

For pressure measurements, fixed rakes are often used in conventional propulsion systems to characterise the flow distortion at the AIP or the total pressure distribution at the nozzle plane. Equally, the method can be followed for BLI configurations but the ingestion of the BL necessitates a different distribution of the arms to accurately map the flow distribution. As such, rotating rakes (Fig. 2.A.5) have been introduced [231] and vastly used in the D8 project [20, 76, 109]. The arm(s) rotate(s) and data is either acquired continuously or intermittently with different degrees of revolution (Fig. 2.10).

In the wake, uneven distributions of probes can be coupled to a traversing rake which covers different axial and lateral positions in the wake (Fig. 2.A.6). Alike the rotating rake, the traverse rake can continuously sweep over the survey plane or intermittently and take measures.

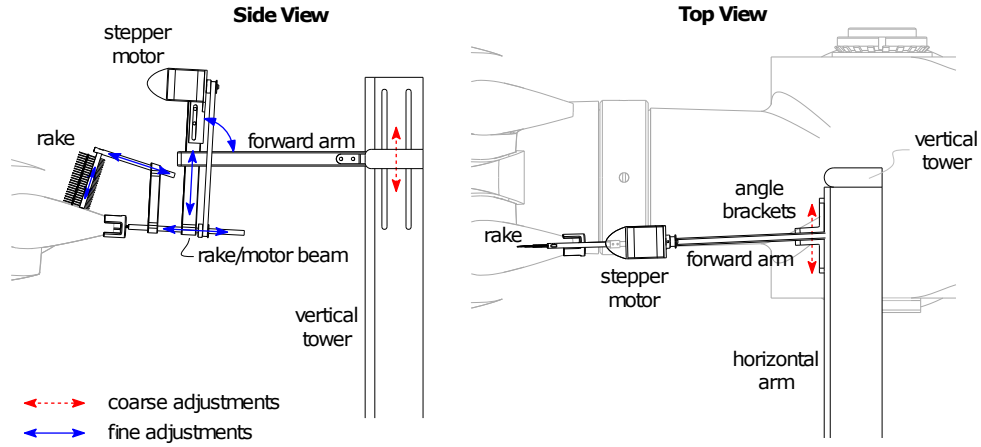


Figure 2.A.5: Rotating rake of pressure measurements in the NASA Langley's 14×22 wind-tunnel [109]

In either method, interpolation might be necessary to solve the relatively sparse distribution of points. To that extent, methods such as the followings can be used for radial and circumferential distributions:

- bi-linear: standard linear interpolation for both radial and circumferential directions [75];
- linear and cubic spline: standard linear interpolation in the radial direction and cubic spline in the circumferential direction;
- bi-cubic spline: cubic spline in both directions;
- guided: guided interpolation uses a template to enhance the current distribution in both directions.

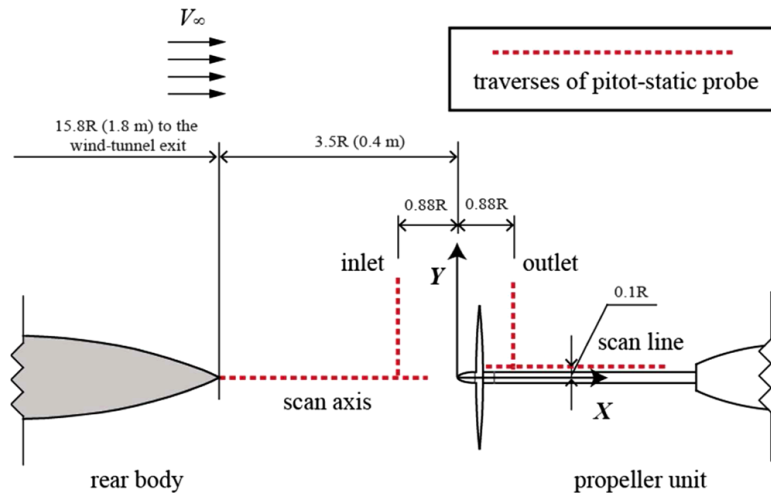


Figure 2.A.6: Traverse rake of pressure measurements in the Delft University of Technology's OJF wind-tunnel [219]

2.A.3 Optical Measurements

Pressure-Sensitive Paint

To obtain a detailed field of pressure distribution, a pressure-sensitive paint system uses multiple cameras and lamp illuminating the surfaces of interest priorly covered to react to changes in temperature and pressure. In the case of the Aircraft Research Association (ARA), UK, their system is composed of 12 cameras and 16 UV lamps to obtain a full coverage (Fig. 2.A.7). To enable this capture, the model is painted following a particular procedure. A white epoxy base layer is applied on clean surface before baking the model at 65° for up to 24 hours. This process provides a better surface roughness to adhere to, isolates the active layer from internal changes in temperature and improves the signal by reflecting more light outwards.

The paint used contains particular molecules (PtTFPP) that absorb photons for wavelengths around 400 nm and emit light around 690 nm. The emission process (Fig. 2.A.7) is quenched by the presence of oxygen and highlights a range of different intensities of red sensitive to the local pressure - low pressure means less oxygen and a brighter red. The data acquisition can take up to eight minutes as comparison is needed between wind-on and -off images. The obtained data field is then calibrated to a CAD mesh from which markers are identified against pressure taps to reduce the temperature effects.

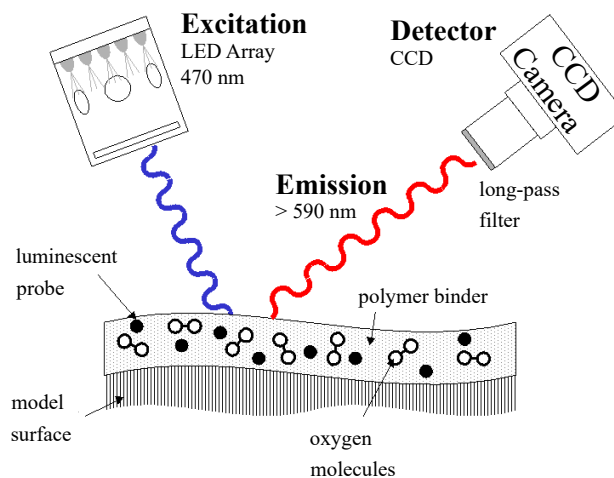


Figure 2.A.7: Pressure-sensitive paint, light and camera system in the ARA 9x8 Transonic Wind-Tunnel [232]

Particle Image Velocimetry

A second optical measurement technique is related to the velocity field acquisition by use of particle image velocimetry (PIV). The flow travelling around the model is seeded with tracer particles small enough that they faithfully follow the flow. A set-up of laser(s) (Fig. 2.A.8) illuminate a slice, or a sheet, by subsequent pulsations in the region of interest where cameras capture images. The displacement of the particles over the span of the two photographic taken defines the velocity of the particle and thus the local velocity of the flow. Afterwards, from a data acquisition software, the region of the flow illuminated is mapped alike obtained with CFD.

PIV is a non-intrusive measurement technique from which data can be simultaneously acquired over a wide 2D or 3D area, depending on the camera set-up. For instance, stereoscopic PIV utilises two cameras to extract the displacement in a particular plane whereas dual plane stereoscopic PIV would cover two planes of laser light. The size of the seeds is chosen to be sufficiently small to not affect the stream-flow and represent the testing when seeds are not included. Nonetheless, the materials chosen for the model are also of importance as they can absorb or reflect part of the laser light with different levels. As such, an anodization on aluminium increases the corrosion resistance but also prevents the reflection of the light sheet very close to the surface such that the sub-viscous layer velocity can be captured - unlike in Ref. [180]. Other techniques are reviewed in Ref. [234].

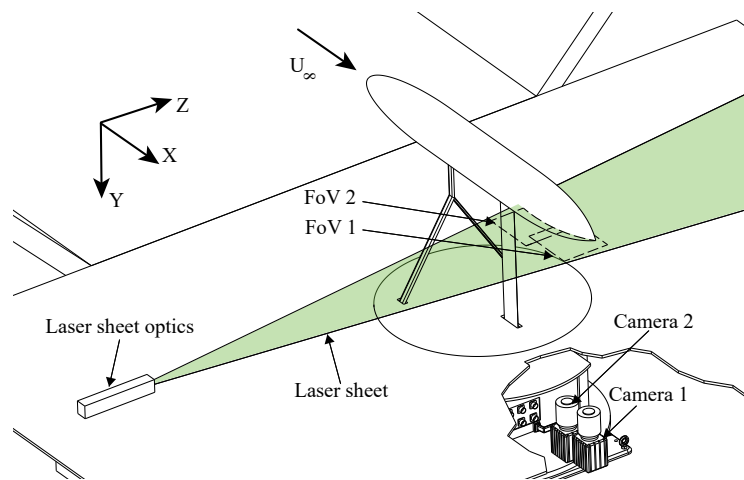


Figure 2.A.8: Updated stereoscopic particle image velocimetry set-up in the Delft University of Technology's OJF wind-tunnel [226]

Appendix 2.B Novel Aircraft Characteristics

The different novel aircraft characteristics are summarised in Table 2.B.1 and categorised following the paper structure to highlight the different concept features. Notably, for the PFC, the under-wing engines describe the main engines as an additional propulsor is aft-mounted (BLI power-plant) whereas main turbofans are aft-mounted on the fuselage for the ORFEC. As for the DPC, different distributed architectures are selected.

Table 2.B.1: Aircraft and propulsion systems characteristics of the different boundary layer ingestion aircraft

Characteristic	Unit	Propulsive Fuselage Concepts				Over- and Rear-Fuselage Engine Concepts				Disruptive Propulsive Concepts			
		STARC-ABL	CENTRELINE	DisPURSAL		NOVA	B767-200	B737-800	DisPURSAL	SAX-40	N3-X	DRAGON	
Aircraft	Reference Aircraft	B737-800	A330-300	A330-300	A321-200 / B767-200	NOVA	B767-200	B737-800	A330-300	B777-200	B777-200LR	A320-B737	
	Range	3,500 - 6,500	6,500 - 12,000	4,800 - 9,000	3,000 - 5,500	3,000 - 5,500	3,000 - 5,500	3,000 - 5,500	4,800 - 9,000	4,000 - 7,400	7,500 - 13,900	2,750 - 5,000	
	Fuel Block	9,680	39,300	38,380	19,700	19,700	15,700	15,700	38,960	-	38,550	5,120	
	Passengers	154	340	340	180	180	180	180	340	215	300	150	
	Cruise Altitude	37,000	35,000	35,000	37,000	37,000	37,500	37,500	35,000	40,000	30,000	-	
Airframe	Cruise Mach	0.785	0.82	0.8	0.82	0.82	0.72	0.72	0.8	0.8	0.84	0.78	
	Airframe Length	42.1*	70.5	69	38.3	38.3	37.9	37.9	37	42.1	40.6	-	
	Cabin Diameter/Height	3.96	6.09	6.07	3.9	3.9	7.62	7.62	-	3.96	-	-	
	Cabin Width	-	-	-	4.9	4.9	5.4	5.4	-	-	-	-	
	Wing Span	36	64.6	65	43.1	43.1	35.8	35.8	65	36	66	36	
Main Engine	Fan Diameter	1.32	2.85	2.77	2.16	2.16	1.85	1.85	1.88	2.16	-	-	
	Nacelle Diameter	1.48	3.05	-	2.5	2.5	-	-	-	-	-	-	
	By-Pass Ratio	6.4	15.5	18.1	16	16	10	10	20	12.3	-	0	
	Fan Pressure Ratio	-	-	-	1.4	1.4	1.45	1.45	-	-	-	-	
	Overall Pressure Ratio	58	60	-	42	42	40	40	51	49	-	37	
	Boundary Layer Ratio	-	-	-	40	40	40	40	-	-	-	-	
	Power	-	-	-	-	-	13	13	-	-	-	-	
BLI Power-plant	Fan Diameter	2.06	2.34	4.13	-	-	-	-	-	1.2	1.1	-	
	Nacelle Diameter	2.29	-	-	-	-	-	-	-	-	-	-	
	By-Pass Ratio	∞	∞	17.9	-	-	-	-	∞	12.3	∞	∞	
	Fan Pressure Ratio	1.25	1.4	1.39	-	-	-	-	-	1.5	1.3	1.25	
	Inlet Pressure Recovery	-	0.99	0.86	-	-	-	-	-	-	-	-	
	Boundary Layer Ratio	45	19 - 36	29	-	-	-	-	-	16.6	-	-	
	Thrust-Split	44	6	13	-	-	-	-	-	100	100	100	
	Power	2.6	8	12	-	-	-	-	-	-	30	-	
Power Saving	Coefficient	12-14 [21]	6-11 [162]	8-10 [48]	5 [19]	5 [19]	7-10 [20]	7-10 [20]	5 [80]	2-10 [47]	10-20 [104]	-	
	Fuel Burn Benefits	15 [22]	11 [212]	13 [148]	-	-	36 [20]	36 [20]	37 [242]	28 [121]	55 [34]-70 [270]	-	

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Hybrid Momentum- and Energy-Based Aero-propulsive Performance Evaluation Approach

A promising alternative architecture for improving the overall performance of the next generation of commercial aircraft relies upon boundary layer ingestion by embedding the propulsion system within the airframe. The ingestion of the boundary layer flow that develops around the fuselage by aft-mounted propulsors enables a natural energy harvesting, lowering fuel consumption. Nonetheless, in scenarios of strong aerodynamic coupling, the validity of conventional book-keeping schemes breaks down bringing forth alternative approaches, and showcasing the need for consensus surrounding the interpretation and quantification of BLI benefits which motivates the development of a unification method. To evaluate the vehicle aero-propulsive performance, a framework on the design and analysis of highly-integrated aircraft and novel technologies is developed. The common near-field momentum-based bookkeeping schemes are discussed and unified with a more holistic approach based on energy methods to enable more intuitive physical interpretation of the aerodynamics. The present work considers a propulsive fuselage concept whereby the aircraft tail is morphed to accommodate an integrated rear-mounted circumferential boundary-layer ingesting propulsor. On the savings quantification, new metrics are introduced to bound the theoretical extrema. Despite the application of the hybrid method to this configuration, its universality is introduced for more disruptive concepts, with notably the methods and metrics.

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3.1 Introduction

Through the years, the position, size and shape of aero-engines evolved towards more capable and powerful engines. To further improve the engines' propulsive efficiency, turbofans have been introduced to ingest larger amounts of mass-flow, hence reducing the specific-thrust but unavoidably increasing the engines' size. Despite the constant efforts in their developments, it is notwithstanding that the current aircraft configurations are approaching their limits in terms of performance improvements [1].

It is therefore necessary to switch from evolutionary to revolutionary concepts. Complementary engines, as obtained through distributed propulsion, enable the reproduction of an equivalent by-pass ratio (eBPR), or more disruptive aircraft concepts that increase the lift-to-drag ratio whilst reducing the aircraft weight [2]. Further, and towards mutual improvements, the synergies between the airframe and engines are exploited with boundary-layer ingestion (BLI) which necessitates a more holistic aircraft design approach. Amongst the numerous conceptual aircraft that arose from BLI [3–7], the propulsive fuselage concept (PFC) illustrated in Fig. 3.1 adapts the conventional tube-and-wing aircraft by integrating an additional aft-mounted propulsor encapsulating the fuselage's tail-cone.

However, it is noteworthy that novel architectures make the distinction between thrust and drag forces challenging because of the close aerodynamic coupling between the propulsion system within the airframe. Effectively, the drag definition is based on the d'Alembert paradox and its Prandtl extension from which infinite stream-tubes in inviscid and viscous scenarios are compared and their resulting force computed. With higher integration levels, the viscous layer that develops along the fuselage cannot be neglected (relative to the rest of the mass-flow ingested) and largely impacts the stream-tube shapes and consequently their forces experienced. Therefore, the well-established thrust and drag book-keeping schemes

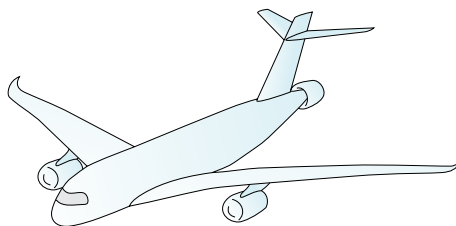


Figure 3.1: Generic propulsive fuselage concept

[8, 9] are revisited with momentum-based approaches [10–12], as reviewed by Moirou et al. [13], but a favoured approach aims at considering the system as a whole. Energy-based methods evaluate the performance of highly-coupled airframe-propulsion configurations relative to a baseline [14–16] whilst mitigating the ambiguity of forces and advising on the causality of BLI mechanisms through an assessment of the flow-field and its decomposition in different features. Nonetheless, for enhanced interpretations, the flow-field is better studied when the aircraft is seen as displacing its surrounding quiescent atmosphere through work done by the no-slip condition along its surfaces, which is thereafter transferred as energy to the flow [14, 17]. This point of view is referred to as absolute reference frame (ARF) in this work, and opposed to the conventional relative reference frame (RRF) that sees the flow travelling around a static aircraft.

Further to this, a caveat exists in the strict definition of the propulsive efficiency as values above 100% are achievable with BLI. As an alternative, a power saving coefficient (PSC) has been initially introduced by Smith Jr. [18]. Nonetheless, there is still a caveat in defining a fair reference for comparison, leading to discrepancies amongst the community [13]. Although the PSC is a surrogate of fuel savings, there are inherent losses such as thermal and core efficiencies which are not accounted when considering shaft powers for PSC calculations such as found in Refs. [19, 20]. Instead, a fuel saving coefficient would consider the power input rather than the shaft power, accounting for all the aforementioned losses and indicating smaller, but more realistic, savings. However, there is no absolute metric to judge performance, and benefits can only be judged relative to a reference configuration, using relative metrics, where no universal baseline is defined.

The present work therefore aims to address certain gaps related to the aero-propulsive performance evaluation of novel aircraft. Notably, interim steps are ensured within the proposed framework that designs and analyses the performance of a propulsive fuselage concept [13] (aft-mounted fully azimuthal propulsor installed on a short-to-medium range single-aisle aircraft, to complement two under-wing turbofans). Its aero-propulsive performance assessment requires a systematic methodology from which a dedicated software is developed based on a structured design and analysis strategy (Fig. 3.2). Notably, an interim step in the aircraft design is taken to better evaluate the propulsor integration effects and quantify the fuel savings. The modular framework named DAHANT (Design and

3. Hybrid Aero-propulsive Performance Evaluation Approach

Analysis of Highly Integrated Aircraft and Novel Technologies) is composed of, initially, five independent steps:

- parametric and automatic powered and unpowered geometry and mesh generations,
- viscous compressible CFD resolution of the external aerodynamics and boundary-layer ingesting propulsion,
- under-wing engines modeling,
- hybrid momentum- and energy-based performance evaluation approach,
- phenomenological analysis and flow breakdown strategy.

The analysis combines momentum and energy approaches for improved interpretations of the flow mechanisms. As such, the power balance method firstly introduced by Drela [15] is improved with a corrected term that enables a definition closer to the propulsive efficiency. Along with the discussions on the PSC, a breakdown of the origins of the savings is given with metrics aiming at circumventing the caveats related to the reference aircraft and engine considered. The strength of these metrics is that they give bounds to the PSC values by removing the ambiguity of a baseline.

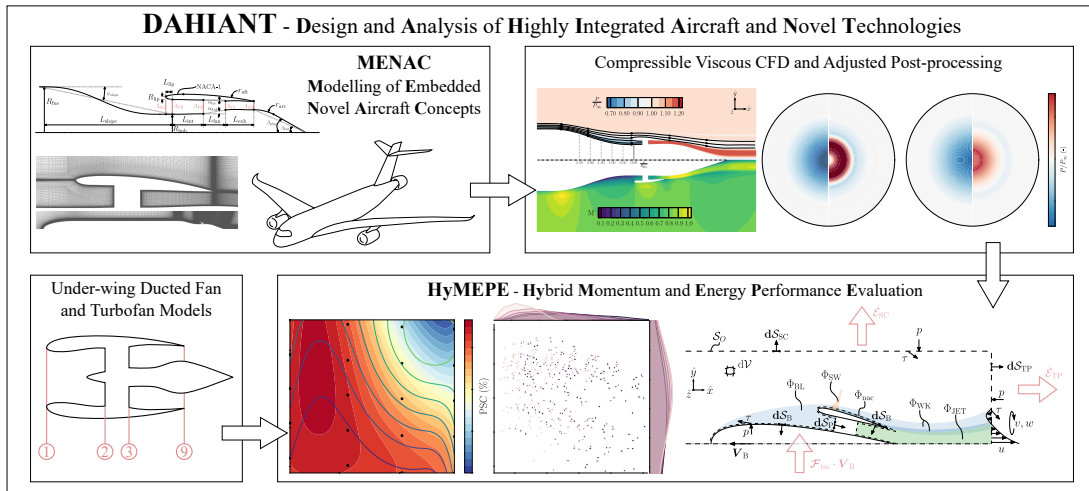


Figure 3.2: DAHIAANT framework (Design and Analysis of Highly Integrated Aircraft and Novel Technologies) with its geometry, mesh, calculation and analyses modules

3.2 Design of Propulsive Fuselage Aircraft

3.2.1 Reference State-of-the-Art Aircraft

To demonstrate the applicability of the hybrid momentum- and energy-based approach developed, a propulsive fuselage concept (PFC) is investigated. A PFC is described as such when a circumferential propulsor, or engine, encapsulates the rear-end of the aircraft's tail such as found in the NASA STARC-ABL or CENTRELINE projects, amongst others reviewed by Moirou et al. [13]. For that purpose, and to benchmark the concept against a reference, a medium-range single-aisle aircraft is used and its tail thereafter morphed to accommodate the BLI propulsion system.

3.2.1.1 Airframe Outer Outline

Assuming weak aerodynamic coupling between the fuselage and the rest of the airframe, a simplified two-dimensional (2D) axi-symmetric fuselage is derived from the reference aircraft. Its chord L_{fus} is segmented in three distinct parts: an ellipsoidal nose, a cylinder of constant radius R_{fus} for the cabin and a conical tail. Although it is common to consider a frustum to represent the aircraft tail, a cone is favoured in this approach to prevent singularities in the mesh and potential flow recirculation behind the vehicle. The outer outline of the fuselage approximation is extracted from a top-view of the conventional aircraft given in Fig. 3.3.

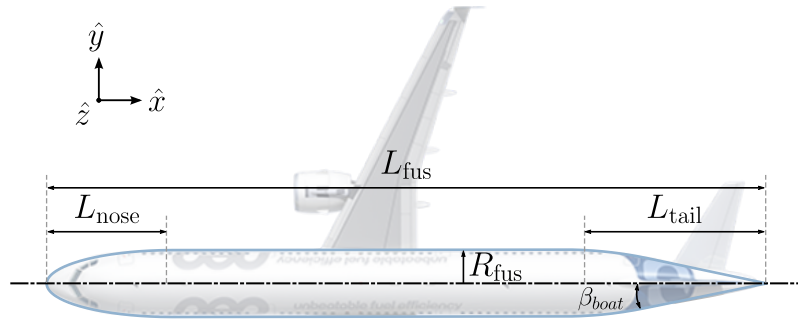


Figure 3.3: Schematic of the reference aircraft's axi-symmetric fuselage approximation with an ellipsoidal nose and conic tail, superimposed over the top view of an A321, courtesy of Airbus

3.2.1.2 Under-Wing Engines

To ensure a constant vehicle speed V_B at cruise, two under-wing engines as state-of-the-art turbofans (TF) are selected to propel the single-aisle aircraft, e.g. CFM LEAP-1 or PW1100G, and developed by Chalmers University of Technology (private communication). The two-spool turbofan architecture is composed of an 11:1 bypass ratio single-stage fan of pressure ratio 1.49, 3-stage intermediate-pressure and 10 high-pressure compressor and 2-stage high-pressure and 7-stage low-pressure turbine for an overall pressure ratio of 44. The free-stream conditions are defined following the International Standard Atmosphere (ISA) model based on the cruise altitude and Mach number, and assuming isentropic inflow conditions. The engine performance is evaluated at on-design from which a nacelle drag estimation sub-process determines the nacelle drag based on the flight conditions [21] to determine an installed thrust-specific fuel consumption.

3.2.2 Propulsive Fuselage Aircraft

To attenuate the wake losses, energy is harvested from the viscous layer developing around the fuselage. Whilst a convenience exists in retrofitting a BLI propulsor around a conical tail [10], larger degrees of integration rely on a strategic tail reshaping to obtain a natural flow pre-diffusion before entering the fan. Despite the opportunity of ingesting the entire circumference of the fuselage's boundary-layer flow rich in energy and subject to reasonable distortion [22], there are numerous challenges that arise on the propulsor position, size and shape. These difficulties are added to the determination of the propulsor and vehicle performance. Nevertheless, this concept is a good candidate to meet the environmental goals related to commercial aviation as penalties from the fan's blade loading are reduced, and the flow-field can be simplified and studied axi-symmetrically.

3.2.2.1 Fuselage Morphing Sequence

To not violate the reference geometry constraints and approximations, the propulsor must be integrated whilst ensuring a pre-diffused inflow and avoiding recirculation upstream of the nacelle. To that end, the tail must be morphed and a way of doing that is by introducing a slope and smooth transitioning towards the plug of the

aircraft, where a circular arc connects to the right-angled plug of angle β_{plug} to ensure continuity and tangency.

Tail-Slope Redesign

For accommodating the propulsor, the tail-cone radius is shrunk in its middle to ensure minimal distortion, meet the Mach number target and avoid separation ahead of the propulsor intake. This implies the fuselage contour to be defined following a certain curvature before entering the intake. Seeking a robust, versatile, and yet easy to implement method allowing for design space explorations, the Bell-Mehta fifth-order polynomial is selected preventing any early separation (seventh-order polynomial) and non-uniformity (third-order polynomial) [23]. The equation that defines the tail-slope is given by Eq. (3.1) where x' corresponds to the non-dimensional stream-wise distance, x/L_{slope} , and where $y_i = R_{fus}$ and $y_j = R_{hub}$ are the radial locations at the leading-edge of the slope and at the throat, respectively. The slope angle ψ_{slope} , given in Fig. 3.4 measures the slope inclination between the horizontal continuity of the cabin section and the slope end point meeting the *future* propulsor throat axial position.

$$y(x) = y_i - (y_i - y_j) \left[6(x')^5 - 15(x')^4 + 10(x')^3 \right] \quad (3.1)$$

Morphed Reference

The aircraft featuring the morphed tail described can be used as an interim reference podded configuration whereby the flow properties on this configuration help derive the ducted powered configurations. The numerical computation of the model at different flight conditions, i.e. Mach and Reynolds numbers, provides a dataset of viscous layer properties at any axial position along the tail section. This includes the entire BL profiles properties at different axial position but also fractions of the

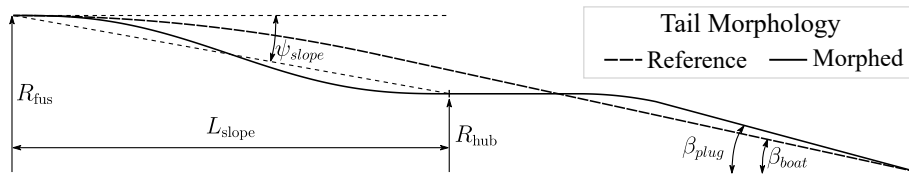


Figure 3.4: Tail morphing sequence from its reference to its morphed shape

BL thickness. This dataset allows one to perform interpolations both axially and radially to obtain estimates on the mass-flow rate, flow velocity and pressures that will be experienced at the propulsor intake. The versatility of this approach stands in the fact that either a throat area, A_{TH} , or a target mass-flow rate, can be used as inputs at the propulsor axial position, L_{slope} .

3.2.2.2 Integrated Aft-Fuselage Propulsor

From that clean morphed fuselage defined, a BLI propulsor is integrated where Fig. 3.5 depicts a schematic of the axi-symmetric propulsive fuselage and a zoomed-in region of the tail which features the pre-diffusive slope, the BLI propulsor and the aft-cone.

The propulsor is integrated at the slope's end-point of the un-installed geometry. A first approximation is made on the areas, A_{TH} and A_{FF} , using the BL properties of the morphed reference to target the flow passage within the intake. It is agreed that this remains estimates as it is well-known [24] that the presence of the nacelle thickens the BL, nonetheless, the assumption taken herein is believed to be sufficient for the preliminary design phase.

The entire design process is depicted in Fig. 3.6 and is further detailed in the following. This approach permits the coupling of estimations on flow properties at different stations (Stn. TH, FF, FE and NZ), whilst designing the propulsor's different sections such as the intake, cowl and exhaust.

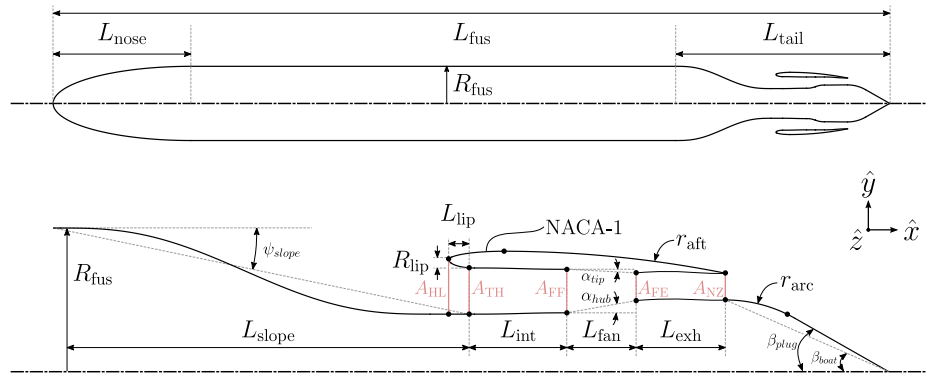


Figure 3.5: Schematic of an axi-symmetric fuselage with a boundary-layer ingesting propulsor mounted on a morphed tail representing a propulsive fuselage concept with designs parameters (black) and thermodynamic stations (pink)

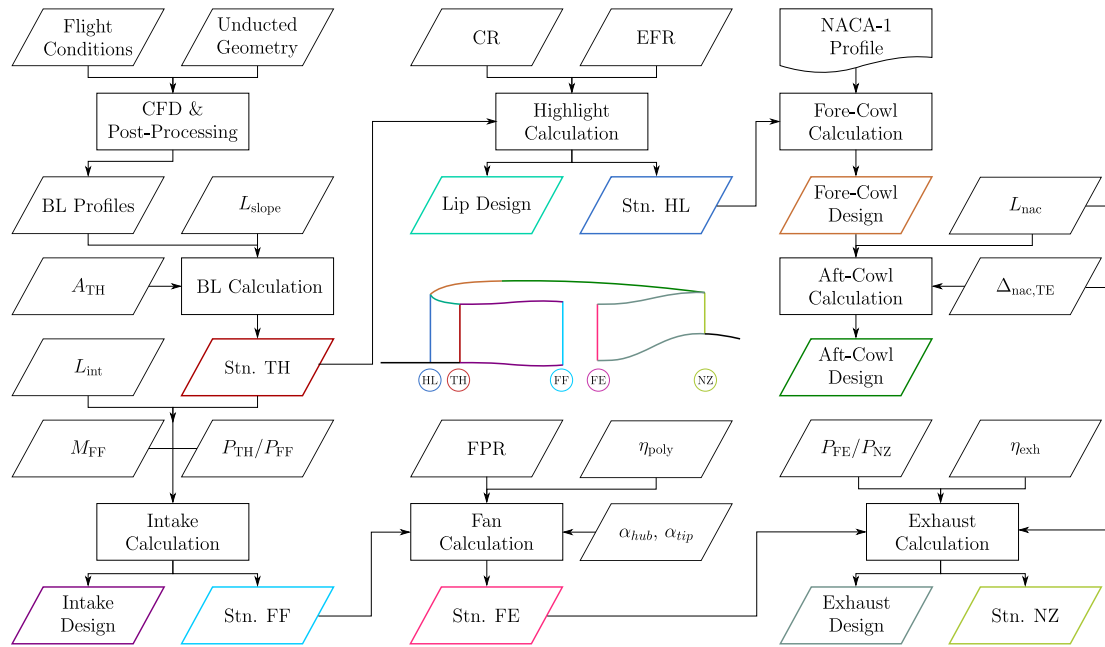


Figure 3.6: Flowchart of the propulsor design routine

Lip Shape

Moving backward from the throat station, one can define the lip design of the intake which follows the shape of a quarter of an ellipse. The contraction ratio (CR) is defined such that $CR = A_{HL}/A_{TH}$ whereby A_{TH} is specified as an input, or derived from the target mass-flow rate and its BL interpolation. Several studies in the literature converge towards a CR of 1.25 albeit others suggest values ranging between 1.17 and 1.3 [25–27]. Initiatives from Langley [28] started in 1979 with axi-symmetric high by-pass ratio turbofans whereby it was down-selected a CR of 1.25, and later Re [29] experimentally studied the pressure distributions along cowls at different Mach numbers for the same CR of 1.25. Therefore, there is a certain confidence in using 1.25 in this work.

From the lip thickness, the ellipse fineness ratio (EFR) drives the length as it is defined as the major-to-minor radii ratio: $\text{EFR} = L_{\text{lip}}/R_{\text{lip}}$. Luidens et al. [30] explored inlet designs and in particular EFR of 1.5, 2.0 and 2.5 where the latter suggests best performance at higher flight Mach numbers. Despite their application to podded engines in 1979, Berrier et al. [31] corroborated their claims with an EFR of 2.0 designed for Mach numbers between 0.77 and 0.83 and an EFR of 3.0 for higher Mach numbers, with their study conducted on BLI flush-mounted inlets. Despite featuring an integrated propulsor, large propulsors might experience transonic conditions and an EFR of 3.0 is selected for safety.

Cowl Outline

The nacelle cowl is split in two parts, its fore- and aft- sections, separated at a point where the cowl maximum radius is obtained, i.e. the crest. One might be interested in separating the two to consider different profiles and assess their influence on the flow. In this work, the fore-cowl is defined from a NACA-1 Series [28] using its sixth profile. The NACA-1 Series has been used in several studies [27–29] whereof different profiles have been derived from the baseline [28]. Although the definition is intended for axi-symmetrical podded engines, it is the author’s opinion that this remains a reasonable design for consistent comparison at a preliminary stage. In Re’s work, the effect of highlight to crest ratio has been studied at different flight Mach number between 0.60 and 0.92 [29], which are herein respected.

The aft-cowl, defined from the position at maximum radius, is modelled with a circular arc [25, 27, 31] which tangents at the crest and ends at the cowl’s trailing-edge defined from the nacelle length, L_{nac} , and its radial offset from the leading edge, $\Delta_{\text{nac,TE}}$. It is reminded that the work intended here is not to define optimal geometries or contribute to optimisations in any sort, therefore the choice is made on the arc shape as an easy and robust implement. Nonetheless, in the event that refined geometries need to be used, the work from Heidebrecht et al. [32] on turbofans using Class-Shape Transformation (CST) may be used.

Intake Calculation and Design

Going back to the results obtained from the BL calculations to define the flow properties and area at the throat, the intake is designed. Within the intake, the flow can be further diffused if this has not been achieved ahead of it. Its second role consists in routing the flow through the fan to conform its continuity from the throat to the exhaust even with the presence of a centreline offset (as designed in an S-duct) [26, 31]. Its length, L_{int} , can vary from ultra-short to long intakes, similar to the podded engines conventions. In light of this practice, the intake length has been preliminarily defined as a function of the throat radius, between its hub and tip. That factor is chosen to give a reasonable average geometry and is based on the author’s judgement only.

With the aid of the intake pressure recovery, $P_{\text{TH}}/P_{\text{FF}}$, and either the target fan-face Mach number, M_{FF} , or a diffusion split ratio defined by Eq. (3.2) (which characterises

the flow diffusion within the intake) [10], intake calculations are performed to obtain the intake parametrisation as well as the Stn. FF's flow properties, area and position.

$$\text{DSR} = \frac{M_B - M_{\text{TH}}}{M_B - M_{\text{FF}}} \quad (3.2)$$

The duct that defines the intake from the throat TH to the fan-face FF allows for the use of continuity assumptions on the pressure recoveries. The flow properties and areas are iteratively calculated from known total and static quantities using the isentropic relationships that can be found in common gas turbine engines performance literature [33, 34]. The fifth-order Bell-Mehta polynomial is used and given by Eq. (3.1) for both the hub and shroud surfaces of the intake ($y_i = y_{\text{TH}}$ and $y_j = y_{\text{FF}}$). In similar concepts, the parametrisation of the nacelle shape used in the NASA STARC-ABL by Refs. [6, 22, 35] is not detailed whereas Christie et al. [36] used intuitive Class-Shape Transformation curves for application to preliminary and optimisation studies on turbofans. It is however the authors' opinion that this level of refinement is not as easy to implement as the Bell-Mehta parametrisation, and neither necessary for early investigations, and therefore reinforces the use of the former for the intake region.

Fan Calculation and Design

Subsequently, for any given fan pressure ratio, FPR, a technology level is assumed through a polytropic efficiency, η_{poly} . To determine the flow properties at the fan-exit station (Stn. FE), isentropic relationships as given in common gas turbine performance literature [33, 34] are used. Through the fan stage, a distributed momentum is applied to idealise a constant velocity at the exit station. In between, the fan stage features physical outlet and inlet stations from which hade angles (α_{hub} and α_{tip}) [34] mimic a realistic stage aiming at reducing the blade loading and tip clearance, and route the flow towards the exhaust. Their values have a direct implication within the calculations made as the fan-exit area will be determined, the axial position of that plane, and subsequently the fan width. No previous work could be reference however, guidelines from Walsh and Fletcher [34] have been followed who recommended values between 5 and 10 degrees respectively for the tip and hub. Those values have been concluded from their work as to reduce the blade loading and hence improve the fan efficiency.

To model the propulsion system, outlet and inlet planes have been down-selected for ease of implementation numerically as well as simplifying the flow physics and providing a uniform flow distribution to the exhaust. Intrinsically, the flow conditions at the fan-face define the initial conditions of the iterative process detailed in Section 3.2.3.2.

Exhaust Calculation and Design

The exhaust defined from the fan-exit, FE, to nozzle plane, NZ, transports the flow subject to fewer losses than in the intake as it experiences a favourable pressure gradient. Thus, assumptions on the nozzle total pressure losses, P_{FE}/P_{NZ} , and isentropic exhaust efficiency, η_{exh} , are made. Isentropic relations as used for the intake and fan calculations are also followed from Saravanamuttoo et al. [33], and advise on the flow properties at the nozzle plane. However, in that respect, a target of choked conditions at the exit is set which implies iterative calculations for determining the nozzle area. As mentioned, the exhaust duct's role is to direct the flow from the fan exit to the nozzle which is subject to a favourable pressure gradient and therefore to fewer losses. Because of this, the design of the exhaust duct is not deemed critical and a trade-off is permitted to favour other design aspects. Its walls are defined in the same way as they are for the intake (fifth order polynomial from Bell and Mehta [23]). The wall that defines the hub starts at the fan-exit hub radius and axial position and ends at the nozzle station which has been defined from the exhaust calculations resulting in a nozzle area. The input given by $\Delta_{nac,TE}$ allows for its hub position to be derived and hence Eq. (3.1) to be applied. The same process is followed for the nacelle inner walls which starts at the tip of the fan-exit and finishes at the cowl trailing-edge ($y_i = y_{FE}$ and $y_j = y_{NZ}$).

Downstream, to avoid separation from an abrupt change in geometry along the plug, tangency is assured with a circular arc r_{arc} (Fig. 3.5) giving a practical β_{plug} larger than the boat angle β_{boat} extracted from the reference aircraft.

3.2.2.3 Rescaled Under-Wing Engines

Alike the reference aircraft, a conventional kerosene-fed propulsion system is considered to propel the aircraft. In the turbo-electric architecture selected and illustrated in Fig. 3.7, mechanical power is extracted from the engines' low-pressure

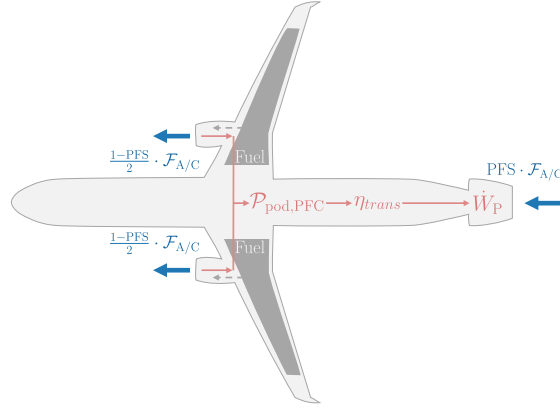


Figure 3.7: Turbo-electric architecture of a propulsive fuselage concept, modified from Chapter 2

turbines to electrically drive the aft-mounted propulsor. Nonetheless, the transmission system and conversion process are not modelled in that work and the inherent losses are approximated with transmission losses given by η_{trans} . A benefit emanating from the thrust-splitting configuration is the rescaling of the turbofans as a lower thrust is required. To that end, the turbofan candidate selected for the reference aircraft can simply be retrofitted in the PFC or scaled-down accounting for the different thrust and power off-take requirement combinations. Engine decks are therefore generated to be coupled with the aft-mounted propulsor's power requirement advising on the engines' fuel consumption.

3.2.3 Numerical Set-up and Implementation

3.2.3.1 Mesh Topology

The different aircraft parametrised, an automated structured grid generation methodology is implemented within MENAC (Modelling of Embedded Novel Aircraft Concepts) using ANSYS ICEM 20.2 [37]. All aircraft issued from the geometry generator are encapsulated by bullet-shape domains allowing a multi-block strategy. To avert flow impingements to the far-field, a coupled grid and domain convergence study following Ref. [38]'s procedure, and detailed in Section 3.A, advised on 10 fuselage chords upstream and above the body $d_{far-field}$, and a Trefftz position d_{outlet} 40 fuselage lengths behind it. The mesh parametrisation is elaborated in a fashion that elements close to the wall ensure a fineness with a y^+ that falls between 0.9 and 1.0 along the whole body, and the grid coarsens by a certain coefficient the farther away from the vehicle, with a

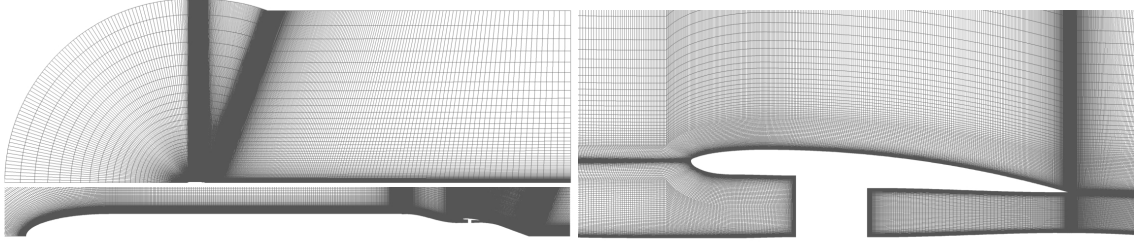


Figure 3.8: C-grid around a propulsive fuselage with the domain's mesh, around the fuselage and the propulsor

capped value before the Trefftz. The multi-grid structure is composed of a C-grid topology surrounding the vehicle from its centreline. In the ducted configuration, a C-grid topology is nested within the clean mesh, i.e. mesh of the reference aircraft (Fig. 3.8), to refine the nacelle and propulsor region.

3.2.3.2 Computational Strategy

Compressible and viscous computations are performed using ANSYS Fluent 20.2 [39] solving the Reynolds-Averaged Navier-Stokes equations in the 2D axi-symmetric domain. The far-field is represented by the circular arc and top surface of the bullet shape where a pressure far-field condition is set to $M_B = 0.78$ and $Re = 2.73 \cdot 10^8$ at ISA conditions. The flow that exits the Trefftz is defined by a pressure outlet boundary condition. Within the domain, a no-slip condition is ensured on the walls of the aircraft where the surrounding flow is resolved using the Shear-Stress Transport (SST) $k-\omega$ turbulence model [40] (Fig. 3.9).

As for the propulsion model, named expressions are used between the fan-face and -exit boundary conditions, which iterative procedure is depicted in Fig. 3.10. The fan-face sees the flow leaving its surface defining an initial particular mass-flow also

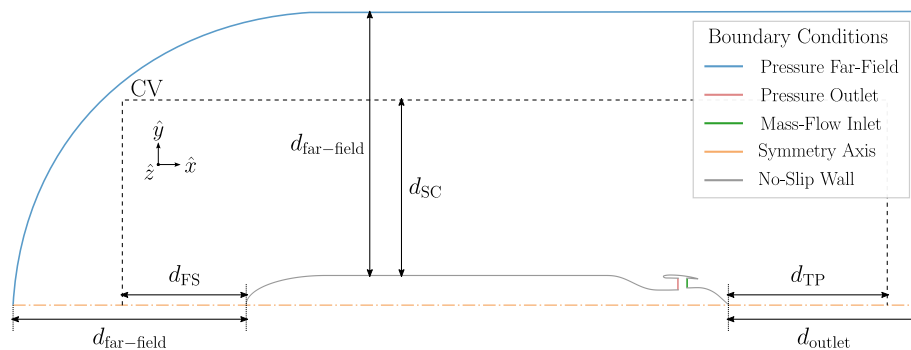


Figure 3.9: Schematic of the domain, propulsive fuselage model, boundary conditions and analysis control volume

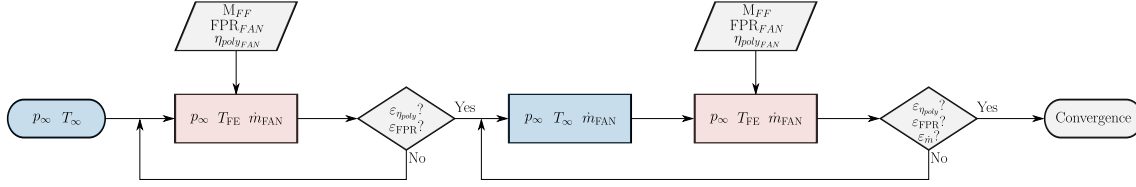


Figure 3.10: Fan modeling iterative procedure within the CFD with conditions at the fan-face (blue) and fan-exit (red)

regulated by the nozzle plane. Through the operating conditions at which the fan performs, isentropic relationships are used to determine the fan-exit flow conditions ensuring continuity through the propulsor stage. The pressure rise is computed and compared against the target values until a certain threshold is met, otherwise the mass-flow rate obtained at the fan-exit is fed back into the fan-face and the iterative procedure carries on till convergence. To ensure convergence of the propulsion model, threshold criteria are defined on the polytropic efficiency of the fan and its pressure ratio. At the fan-exit, total temperature and pressure, and polytropic efficiency are calculated until convergence is obtained. The convergence is tracked through the error, or difference, between the target and obtained fan pressure ratio (FPR), target and obtained polytropic efficiency and mass imbalance through the fan modeling.

In addition to the propulsion model and residual convergences, *near-* and *far-field* force comparison calculations are performed. A unique control volume encapsulates the aircraft where the distances upstream d_{FS} and above d_{SC} are defined from the domain convergence study (Section 3.A), and survey transverse planes d_{TP} in the wake are swept to inquire the flow-field. In the *near-field*, pressure and shear-stress are computed along all physical surfaces. In the *far-field*, the force is extracted from energy-based integral components. Both forces are iteratively compared until convergence below a certain threshold is obtained (Section 3.B).

3.3 Power Balance of Partial Assemblies

3.3.1 Governing Equations with Moving Control Volumes

Long-established conventions in aircraft aerodynamic analyses segment the propulsion system from the rest of the airframe to decompose the positive (thrust) and negative (drag) force contributions of the system. In doing so, infinite distinguishable stream-tubes are defined to assess the experienced forces through

the D'Alembert paradox and its Prandtl extension. In novel aircraft concepts featuring BLI, a common stream-tube is shared by the airframe and the propulsion system which makes the distinction between thrust and drag ambiguous. To circumvent these ambiguities and enable phenomenological analyses, energy-based approaches have been developed [15, 16]. However, in these practices, the body is considered fixed in its environment with the flow moving over it, herein called the relative reference frame (RRF). The RRF is analogous to wind-tunnel tests, where the flow is pre-energised by the wind-tunnel's fan and its interaction with the body of study results in losses that detract from the initial supply and are attributed to drag.

In contrast, to overcome the drag experienced by the body, propulsion systems are considered to provide the opposing force (thrust) and maintain equilibrium. This is done by imparting an excess in momentum to the flow that cancels out the deficit resulting from the airframe. In that perspective and when considering the absolute reference frame (ARF) standpoint, an inconsistency is raised. In the RRF, the useful work is zero as the system appears static whereas in the ARF useful work is achieved and described as thrust (thrust power). The advantages of the ARF perspective were argued in Ref. [14], wherein the power balance method of Ref. [15] was re-derived for steady flows to account for this transformation. More recently, Sanders [17] provided a more rigorous derivation from first principles in terms of moving control volume theory, which fully generalises the formulations to also include unsteady components. Following Ref. [17], Fig. 3.11 illustrates a CV surrounding an aircraft which is fixed in the RRF but perceived to be moving at the aircraft's flight speed,

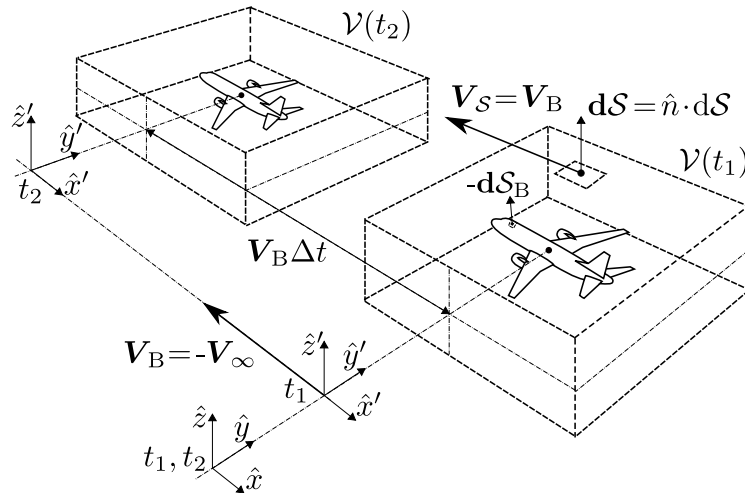


Figure 3.11: Motion of a flying aircraft from t_1 to t_2 with its control volume in the ARF ($\hat{x}, \hat{y}, \hat{z}$) and the RRF ($\hat{x}', \hat{y}', \hat{z}'$), adapted from Ref. [17]

$\mathbf{V}_B$, from the ARF perspective. The relationship between the two reference frames is given by Eq. (3.3) in term of a local absolute velocity $\mathbf{V}$ and relative velocity $\mathbf{V}'$, respectively.

$$\mathbf{V} = \mathbf{V}' + \mathbf{V}_B = \mathbf{V}' - \mathbf{V}_\infty \quad (3.3)$$

As mentioned, the mass, momentum, mechanical energy and total energy conservation equations are often simplified in the RRF and do not enable their utilisation with moving control volumes. The fully generalised mass, momentum, mechanical energy and total energy conservation equations, applicable to inertial reference frames, are derived for moving control volumes using the Leibniz-Reynolds transport theorem given in Eq. (3.4) and are written in Eqs. (3.5) to (3.8) with respect to the ARF.

$$\frac{d}{dt} \iiint_{\mathcal{V}(t)} (\cdot) d\mathcal{V} = \iiint_{\mathcal{V}(t)} \frac{\partial}{\partial t} (\cdot) d\mathcal{V} + \oint_{\mathcal{S}(t)} (\cdot) \mathbf{V}_S \cdot d\mathcal{S} \quad (3.4)$$

$$\frac{d}{dt} \iiint_{\mathcal{V}(t)} \rho d\mathcal{V} + \oint_{\mathcal{S}(t)} \rho (\mathbf{V} - \mathbf{V}_S) \cdot d\mathcal{S} = 0 \quad (3.5)$$

$$\frac{d}{dt} \iiint_{\mathcal{V}(t)} \rho \mathbf{V} d\mathcal{V} + \oint_{\mathcal{S}(t)} \rho (\mathbf{V} - \mathbf{V}_S) \mathbf{V} \cdot d\mathcal{S} = \oint_{\mathcal{S}(t)} (-p\hat{n} + \bar{\bar{\tau}} \cdot \hat{n}) \cdot d\mathcal{S} + \iiint_{\mathcal{V}(t)} \rho \mathbf{f} d\mathcal{V} \quad (3.6)$$

$$\begin{aligned} \frac{d}{dt} \iiint_{\mathcal{V}(t)} \rho \frac{\mathbf{V}^2}{2} d\mathcal{V} + \oint_{\mathcal{S}(t)} \rho \frac{\mathbf{V}^2}{2} (\mathbf{V} - \mathbf{V}_S) \cdot d\mathcal{S} = \dots \\ - \oint_{\mathcal{S}(t)} (p\mathbf{V} - \bar{\bar{\tau}} \cdot \mathbf{V}) \cdot d\mathcal{S} + \iiint_{\mathcal{V}(t)} (p\nabla \cdot \mathbf{V} - (\bar{\bar{\tau}} \cdot \nabla) \cdot \mathbf{V} + \rho \mathbf{f} \cdot \mathbf{V}) d\mathcal{V} \end{aligned} \quad (3.7)$$

$$\begin{aligned} \frac{d}{dt} \iiint_{\mathcal{V}(t)} \rho \left(e + \frac{\mathbf{V}^2}{2} \right) d\mathcal{V} + \oint_{\mathcal{S}(t)} \rho \left(e + \frac{\mathbf{V}^2}{2} \right) (\mathbf{V} - \mathbf{V}_S) \cdot d\mathcal{S} = \dots \\ \oint_{\mathcal{S}(t)} (-\mathbf{q} - p\mathbf{V} + \bar{\bar{\tau}} \cdot \mathbf{V}) \cdot d\mathcal{S} + \iiint_{\mathcal{V}(t)} \rho (\dot{q} + \mathbf{f} \cdot \mathbf{V}) d\mathcal{V} \end{aligned} \quad (3.8)$$

In the above equations and to relate to Eq. (3.3), $\mathbf{V}_S$ refers to the control surface $\mathcal{S}$ velocity whereas $\mathbf{V}$ quantifies the fluid velocity in the ARF. It becomes thus evident that when $\mathbf{V}_S = 0$, the common form of the equations, also summarised in

Ref. [13], are equivalent to considering a fixed control volume in space and reduce to the motion perceived in the RRF.

3.3.2 Universal Mechanical Energy Balance

3.3.2.1 Control Volume Description

The governing equations are applied to a reference short-to-medium range single-aisle aircraft to which a circumferential propulsor is integrated around the fuselage's tail. The propulsion system is turbo-electrically driven by two main engines mounted under the wings through a thrust-splitting architecture to supplement their propulsive force [13].

From the volume $\mathcal{V}$ at t_1 in Fig. 3.11, a two dimensional cutaway bounds the surface $\mathcal{S}$ given in Fig. 3.12. The bounding surface is composed of an outer surface $\mathcal{S}_O$ represented as the side cylinder and Trefftz planes ($\mathcal{S}_O = \mathcal{S}_{SC} \cup \mathcal{S}_{TP}$) and an inner surface. This latter is further split into the fuselage and propulsor's nacelle walls $\mathcal{S}_B$ and any permeable propulsive surfaces $\mathcal{S}_P$ (that bounds the volume $\mathcal{V}_P$ that would represent the volumetric body forces exerted by the propulsor to the flow).

3.3.2.2 Mechanical Energy Balance

To enable physically intuitive interpretations, and inspired from the work of Renard and Deck [41], Ref. [14] contextualises the power balance method from Drela [15] with insightful energy transfer pathways in the ARF. Whilst the RRF can be misleading as the atmosphere acts both as a source and a sink, the ARF circumvents these

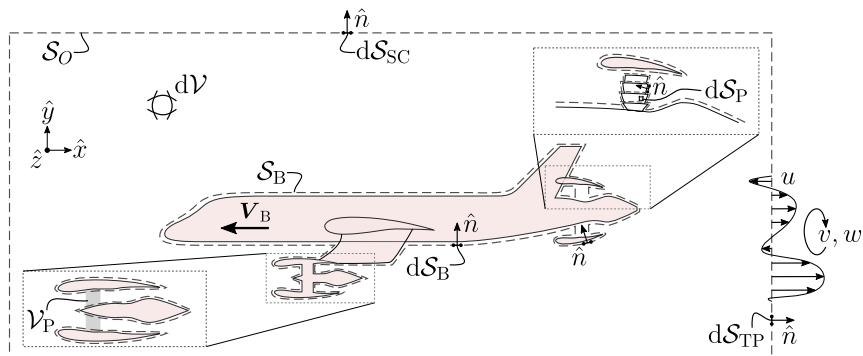


Figure 3.12: Schematic of a control volume encapsulating a powered aerodynamic body

ambiguities through the introduction of the Galilean covariance (as opposed to the Galilean invariance in the RRF) [17]. The forces acting on the body's surfaces obey the no-slip condition and thus act as interfaces for transferring energy to the flow, forming a boundary layer.

The direct application of Eq. (3.7) to the CV defined in Fig. 3.12 yields the mechanical energy balance given in Eq. (3.9), and rearranged such that the left-hand side characterises the power sources to the flow, and the right-hand side the different sinks.

$$\begin{aligned}
 & - \underbrace{\oint_{S_P} (p_G \mathbf{V}_S' - \bar{\bar{\tau}} \cdot \mathbf{V}_S') \cdot d\mathbf{S}}_{\mathcal{P}_S} + \underbrace{\iiint_V \rho \mathbf{f} \mathbf{V}' \cdot d\mathbf{V}}_{\mathcal{P}_f} + \underbrace{\overbrace{\oint_{S_B} (p_G \mathbf{V}_B - \bar{\bar{\tau}} \cdot \mathbf{V}_B) \cdot d\mathbf{S}}^{-Wh}}_{-F_B \cdot V_B} + \underbrace{\iiint_V \rho \mathbf{f} \mathbf{V}_B \cdot d\mathbf{V}}_{-F_f \cdot V_B} = \dots \\
 & - \underbrace{\oint_{S_O} \rho \frac{V^2}{2} \mathbf{V}' + p_G \mathbf{V} - \bar{\bar{\tau}} \cdot \mathbf{V} \cdot d\mathbf{S}}_{\mathcal{E}} + \underbrace{\iiint_V -p_G \nabla \cdot \mathbf{V} \cdot d\mathbf{V}}_{\Theta} + \underbrace{\iiint_V (\bar{\bar{\tau}} \cdot \nabla) \cdot \mathbf{V} \cdot d\mathbf{V}}_{\Phi} \quad (3.9)
 \end{aligned}$$

The first left-hand side term describes the amount of shaft power related to any moving fan's blade (propulsor with its blades in Fig. 3.12 top-right corner), and the work applied on the flow relative to the body. Relative to the total energy balance in Eq. (3.10), the shaft power corresponds to a rise in total enthalpy in the flow ΔH_0 , as given in Eq. (3.11) where $\mathbf{V}_S$ corresponds to the velocity on the propulsor's control surface and obeys the no-slip condition. Additionally, in this context, the propulsor does not provide heat to the flow and therefore, the heat conduction vector $\mathbf{q}$ is zero in adiabatic flows. The substitution $p_G = p - p_\infty$ is also introduced as a further simplification.

$$\begin{aligned}
 & \oint_{S_O} \rho \left(e + \frac{V^2}{2} \right) \mathbf{V}' \cdot d\mathbf{S} + \oint_{S_P} \left[\rho \left(e + \frac{V^2}{2} \right) (\mathbf{V} - \mathbf{V}_S)^0 \right] \cdot d\mathbf{S} = \dots \\
 & - \oint_{S_O} \left(\mathbf{q}^0 + p \mathbf{V} - \bar{\bar{\tau}} \cdot \mathbf{V} \right) \cdot d\mathbf{S} - \underbrace{\oint_{S_P} \left(\mathbf{q}^0 + p \mathbf{V}_S' - \bar{\bar{\tau}} \cdot \mathbf{V}_S' \right) \cdot d\mathbf{S}}_{\mathcal{P}_S} \quad (3.10)
 \end{aligned}$$

$$\begin{aligned}
 - \oint_{\mathcal{S}_P} (p_G \mathbf{V}'_S - \bar{\bar{\tau}} \cdot \mathbf{V}'_S) \cdot d\mathcal{S} &= \oint_{\mathcal{S}_O} \left[\rho \left(e + \frac{\mathbf{V}^2}{2} \right) \mathbf{V} + p_G \mathbf{V} - \bar{\bar{\tau}} \cdot \mathbf{V} \right] \cdot d\mathcal{S} \\
 \mathcal{P}_S &= -\Delta H_0
 \end{aligned} \tag{3.11}$$

The second term on the left-hand side $\mathcal{P}_f$ can equally be used when the fan's blades are substituted by a body force across a volume (as given in gray in Fig. 3.12, at the bottom-left corner). It is equivalent to $\mathcal{P}_S$, and to the total enthalpy rise associated with the shaft power. This representation is commonly favoured in early stage design when the propulsor's geometry is unknown [20, 35, 42]. However, sometimes it is more convenient to model the propulsor as a set of inlet and outlet boundary conditions and use $\mathcal{P}_S$ [43–45].

3.3.2.3 Propulsor Mechanical Power Caveat

In either case, i.e. $\mathcal{P}_S$ and $\mathcal{P}_f$, these notations differ from the power that crosses over the fan's inlet and outlet surfaces and which represents the additional energy deposition rate into the flow from the propulsion systems, originally denoted $\mathcal{P}_K$ in Ref. [15], and commonly used in the literature [46–48]. In this scenario, Drela [15] derives $\mathcal{P}_K$ to represent the change in mechanical energy flux between the inlet and outlet stations, on either side of the fan. But there is a caveat regarding $\mathcal{P}_K$ because, unlike total enthalpy rise, its value will change according to the interface location of the inlet and outlet boundary conditions. This is because $\mathcal{P}_K$ inherently includes the pressure volume work, Θ_P , within the fan's volume, $\mathcal{V}_P$, that is bound by the inlet and outlet interface planes. Retrieving the mechanical energy conservation equation in the ARF from Eq. (3.12), and by identification with the previously derived total energy equation in Eq. (3.10), the non-equivalence between the two power sources is shown in Eqs. (3.13) and (3.14).

$$\oint_{\mathcal{S}} \rho \left(\frac{\mathbf{V}^2}{2} \right) \cdot d\mathcal{S} = \oint_{\mathcal{S}} (-p_G \mathbf{V} + \bar{\bar{\tau}} \cdot \mathbf{V}) \cdot d\mathcal{S} + \iiint_{\mathcal{V}} [p_G \nabla \cdot \mathbf{V} - (\bar{\bar{\tau}} \cdot \nabla) \cdot \mathbf{V}] \, d\mathcal{V} \tag{3.12}$$

$$\begin{aligned}
 \Leftrightarrow \oint_{S_O} \rho \left(\frac{\mathbf{V}^2}{2} \mathbf{V}' \right) \cdot d\mathbf{S} + \oint_{S_P} \rho \left(\frac{\mathbf{V}^2}{2} \right) (\mathbf{V} - \mathbf{V}_S) \cdot d\mathbf{S} = \dots \\
 - \oint_{S_O} (p_G \mathbf{V} - \bar{\bar{\tau}} \cdot \mathbf{V}) \cdot d\mathbf{S} - \oint_{S_P} (p_G \mathbf{V}_S' - \bar{\bar{\tau}} \cdot \mathbf{V}_S') \cdot d\mathbf{S} - \dots \\
 \underbrace{\iiint_{V_P} -p_G \nabla \cdot \mathbf{V}_S' dV}_{\Theta_P} - \underbrace{\iiint_{V_P} (\bar{\bar{\tau}} \cdot \nabla) \cdot \mathbf{V}_S' dV}_{\Phi_P} \quad (3.13)
 \end{aligned}$$

$$\begin{aligned}
 - \underbrace{\oint_{S_P} (p_G \mathbf{V}_S' - \bar{\bar{\tau}} \cdot \mathbf{V}_S') \cdot d\mathbf{S}}_{\mathcal{P}_S} = \underbrace{\oint_{S_O} \left(\rho \frac{\mathbf{V}^2}{2} \mathbf{V}' + p_G \mathbf{V} - \bar{\bar{\tau}} \cdot \mathbf{V} \right) \cdot d\mathbf{S}}_{\mathcal{P}_K} + \dots \\
 \underbrace{\iiint_{V_P} -p_G \nabla \cdot \mathbf{V}_S' dV}_{\Theta_P} + \underbrace{\iiint_{V_P} (\bar{\bar{\tau}} \cdot \nabla) \cdot \mathbf{V}_S' dV}_{\Phi_P} \quad (3.14)
 \end{aligned}$$

Further, the rate of pressure work within the fan volume Θ_P is composed of isentropic (reversible) and non-isentropic (irreversible) components that can subsequently be identified in Eq. (3.15). As such, the rate of volumetric pressure work Θ is segmented into its two components, Θ_{rev} , the recoverable or reversible work, and the local irreversibilities, Θ_{irr} , echoing the Baroclinic power from Sato [49] imparted to the flow and due to non-isentropic compression and dilation waves encountered in transonic compressible flows.

$$\mathcal{P}_S \equiv \mathcal{P}_f \equiv \Delta H_0 \equiv \mathcal{P}_K + \Phi_P + \Theta_P = \underbrace{\mathcal{P}_K + \Theta_{\text{rev},P}}_{\mathcal{P}_K^*} + \underbrace{\Phi_P + \Theta_{\text{irr},P}}_{\Phi_P^*} \quad (3.15)$$

3.3.2.4 Work-Energy Transfers in Flight

To balance out the terms of Eq. (3.9), a plausible change in potential energy is introduced with $W\dot{h}$ whereby any excess or deficit in power in the system would respectively result in climb or decent, from changes in altitude $\dot{h}$. This potential energy-rate equates the pseudo-work rate of forces that act on the flow. W corresponds to the aircraft weight and $\dot{h}$ the rate of change of altitude and can either be positive or negative, and therefore be placed on either side of Eq. (3.9),

whereby the descent of the aircraft would impart energy to the atmosphere through conversion of its potential energy.

The mechanical energy fluxes $\dot{\mathcal{E}}$ (Eq. (3.16)) crossing over the outer boundary surface $\mathcal{S}_O$ can conveniently be decomposed into the side cylinder and Trefftz plane surfaces, $\dot{\mathcal{E}}_{SC}$ and $\dot{\mathcal{E}}_{TP}$ respectively, following Fig. 3.12 conventions. As a whole, $\dot{\mathcal{E}}$ is constituted of the wake stream-wise kinetic energy deposition rate $\dot{\mathcal{E}}_u$, the transverse kinetic energy deposition rate $\dot{\mathcal{E}}_v$, and the rates of wake pressure-defect work $\dot{\mathcal{E}}_p$, viscous boundary work $\dot{\mathcal{E}}_\tau$ and wave-outflow energy deposition $\dot{\mathcal{E}}_w$. Additionally, $\dot{\mathcal{E}}_u$ and $\dot{\mathcal{E}}_v$ sum up to $\dot{\mathcal{E}}_k$ characterising the overall wake kinetic energy deposition rate.

$$\dot{\mathcal{E}} = \underbrace{\iint_{\mathcal{S}_{TP}} \left(\frac{\rho}{2} \mathbf{V}^2 \right) \mathbf{V}' \cdot \hat{n} \, d\mathcal{S}}_{\dot{\mathcal{E}}_k} + \underbrace{\iint_{\mathcal{S}_{TP}} p_G \mathbf{V} \cdot \hat{n} \, d\mathcal{S}}_{\dot{\mathcal{E}}_p} + \underbrace{\iint_{\mathcal{S}_{TP}} -\bar{\bar{\tau}} \cdot \mathbf{V} \cdot \hat{n} \, d\mathcal{S}}_{\dot{\mathcal{E}}_\tau} + \dots$$

$$\underbrace{\iint_{\mathcal{S}_{SC}} \left[\left(\frac{\rho}{2} \mathbf{V}^2 \right) \mathbf{V}' + p_G \mathbf{V} - \bar{\bar{\tau}} \cdot \mathbf{V} \right] \cdot \hat{n} \, d\mathcal{S}}_{\dot{\mathcal{E}}_w} \quad (3.16)$$

At the outer boundaries (SC and TP), only the excess in energy fluxes is captured, if both surfaces are defined sufficiently far from the body such that they do not experience perturbations from the flow. The energy initially deposited within the flow $\mathcal{V}$ is converted into other forms as represented by the sinks Φ and Θ . This energy transfer takes place in the flow as the excess tries to resume to its initial quiescent state. The first term Θ measures the volumetric pressure work rate as the fluid experiences compression and dilation waves against the atmospheric pressure. Analogous to Drela's power balance term $\mathcal{P}_V$, the change in sign attributes this quantity to a power loss, or sink, associated with non-isentropic compressibility changes [49]. The last term Φ represents the accumulation rate in viscous dissipation along the flow path that manifests itself as heat and measures the irreversible mechanical losses.

Alternatively, the mechanical energy balance can be written under a non-dimensional form using the free-stream dynamic pressure power. This non-dimensionalisation reduces to coefficients $C_{\mathcal{X}}$, where $\mathcal{X}$ can take the form of any term from the balance.

Nonetheless, to not depend on the reference surface area $\mathcal{S}_{\text{ref}}$ selected, the term is preferred on the left-hand side of the equation. That form of Eq. (3.17) enables the comparison of specific terms $\mathcal{X}$ between different configurations, e.g. sizes or designs.

$$C_{\mathcal{X}} \mathcal{S}_{\text{ref}} = \frac{\mathcal{X}}{\frac{1}{2} \rho_{\infty} V_{\infty}^3} \quad (3.17)$$

3.3.2.5 Fan Isentropic Efficiency

From the power balance, the fan efficiency η_{fan} is defined as the ratio of isentropic work $\mathcal{W}_{is}$ over actual work $\mathcal{W}_a$ and clearly highlights the difference behind $\mathcal{P}_K$ and $\mathcal{P}_S$ (or $\mathcal{P}_f$), as discussed in Section 3.3.2.3. The isentropic work quantifies the amount of energy present and available to do work onto the flow relative to the actual energy that has been provided (i.e. accounting for the intrinsic losses). The enthalpy-entropy diagram given in Fig. 3.13 depicts the rise in total pressure across a compressor stage (fan here) and the changes in both enthalpy and entropy. It is clear that the corrected additional power $\mathcal{P}_K^*$ represents the reversible portion of work from Θ is introduced. Equation (3.18) approaches the fan's isentropic efficiency as the control volume $\mathcal{V}_P$, tightly wraps around the fan's geometry and its boundary layers and shock structures. Therefore, it can be assumed that it provides a good approximation of the isentropic efficiency when the inlet and outlet interfaces are

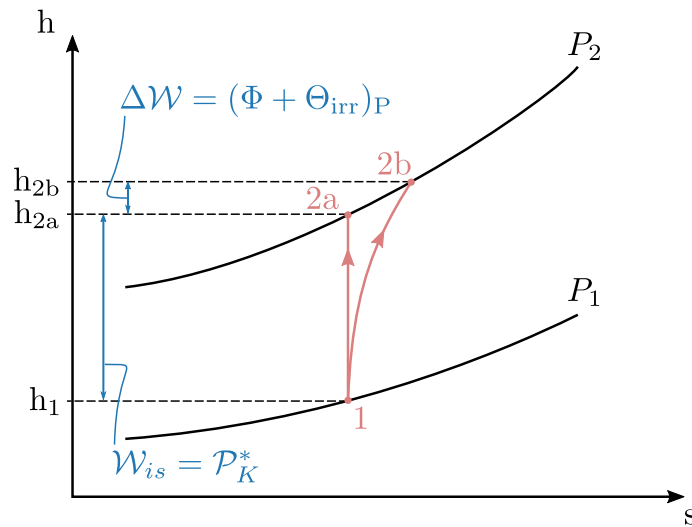


Figure 3.13: Enthalpy-entropy diagram for a compressor stage (fan) between its inlet 1 and outlet 2 stations

positioned reasonably close to the fan geometry. Subsequently, the fan's isentropic efficiency can be used to deduce $\Theta_{\text{rev,P}}$.

$$\eta_{fan} = \frac{\mathcal{P}_K^*}{\mathcal{P}_S} = \frac{\mathcal{P}_K + \Theta_{\text{rev,P}}}{\mathcal{P}_S} = 1 - \frac{(\Theta_{\text{irr}} + \Phi)_P}{\mathcal{P}_S} \quad (3.18)$$

From Eq. (3.18), η_{fan} corresponds to the isentropic efficiency of the fan and therefore allows $\Theta_{\text{rev,P}}$ to be deduced. Similarly, the amount of total irreversible losses in the fan stage can be quantified although not being separable in their $\Theta_{\text{irr,P}}$ and Φ_P constituents. This justifies important improvements in the balance of power terms where it was thought in Ref. [15] that Θ was negligible, then proved to not be in Ref. [14], and further decomposed to impact the useful work possible on the flow.

3.3.3 Application to Decoupled Aero-Propulsive Partial Assemblies

To simplify the analysis, one can crudely assume weak aerodynamic coupling between the fuselage assembly (fuselage and thruster) and the remaining aerodynamic components (wings, pylons, under-wing engines and vertical and horizontal tail planes) via an approach similar to a drag build-up method (Fig. 3.14). In doing so, the fuselage/thruster assembly experiences a net force imbalance $\mathcal{F}_{\text{imb}}$, even in steady-level flight condition, which must be accounted for within the energy balance. The imbalance experienced by the partial assembly is comprised of two contributions given in Eq. (3.19) with a change in potential energy of the sub assembly and the force power of the rest of the components decoupled from the assembly. Therefore, the previous imbalance suggested by $(W\dot{h})_{\text{net}}$ lumps together $(W\dot{h})_{\text{fus+prop}}$ for the simplified assembly, and $\mathcal{F}_{\text{rest}} \cdot \mathbf{V}_B$ for the remaining components.

$$\begin{aligned} (W\dot{h})_{\text{net}} &= (\mathcal{F}_{\text{imb}} \cdot \mathbf{V}_B)_{\text{fus+prop+rest}} \stackrel{?}{=} (W\dot{h})_{\text{fus+prop}} + (W\dot{h})_{\text{rest}} \\ &\stackrel{?}{=} (\mathcal{F}_{\text{fus}} + \mathcal{F}_{\text{prop}}) \cdot \mathbf{V}_B + \mathcal{F}_{\text{rest}} \cdot \mathbf{V}_B \end{aligned} \quad (3.19)$$

This formulation becomes advantageous as it simplifies a problem by isolating a system, and its performance evaluation. To that end, $\mathcal{F}_{\text{imb}}$ is quantified with an

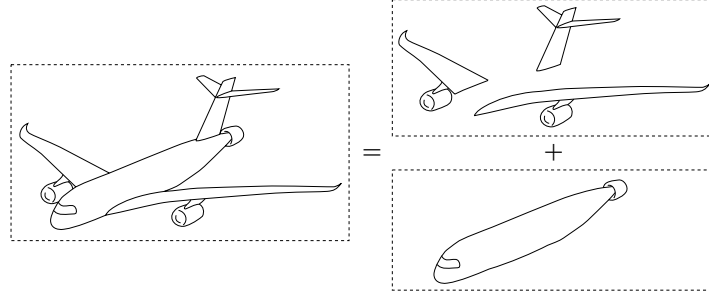


Figure 3.14: Decomposition of the propulsive fuselage concept into partial assemblies: fus+prop at bottom and rest at top

explicit formulation of forces imbalance over partial assemblies (fuselage, $\mathcal{F}_{\text{fus}}$, and BLI propulsor, $\mathcal{F}_{\text{prop}}$), and proves its usefulness when thrust-splitting architectures are considered to deduce the remaining force necessary by the under-wing engines to maintain the net change in potential energy of the entire aircraft.

In these scenarios, the energy balance in Eq. (3.9) reduces to Eq. (3.20) where the net potential energy change is substituted with its sub-assembly quantity, related to an imbalance force.

$$\mathcal{P}_S + \mathcal{P}_f - (\mathcal{F}_{\text{fus}} + \mathcal{F}_{\text{prop}}) \cdot \mathbf{V}_B = (Wh)_{\text{rest}} + \dot{\mathcal{E}} + \Phi + \Theta \quad (3.20)$$

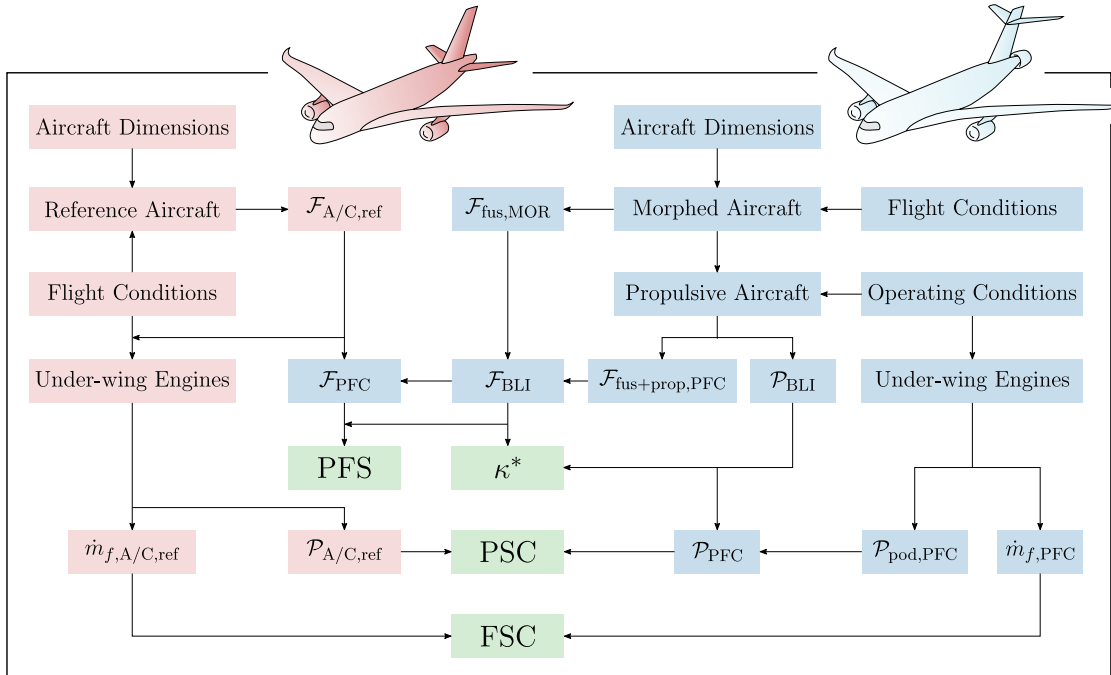


Figure 3.15: Diagram of the overall aero-propulsive performance assessment (green) from the reference (red) and propulsive (blue) aircraft models and properties

3.4 On-Design Aero-Propulsive Performance Evaluation Approach

The hybrid performance evaluation approach consists in combining strengths of existing momentum- and energy-based book-keeping schemes [10, 14, 15]. The strategy illustrated in Fig. 3.15 aims at assessing the aero-propulsive performance of an aircraft featuring a BLI mechanical device over a state-of-the-art reference aircraft.

3.4.1 Reference Aircraft Benchmarking

To assess the BLI benefits, a reference aircraft model is used as a benchmark. Although being simplified, the fuselage stream-wise net force $\mathcal{F}_{\text{fus,ref}}$ computed from CFD on the axi-symmetric model has been verified against a drag build-up approach, further detailed in Section 3.C, with its derived profile and induced drag components. The drag polars suggest that when isolating aerodynamically the bare fuselage from the rest of the airframe (alike Fig. 3.14), the fuselage drag amounts to around a quarter of the entire aircraft drag (Fig. 3.16). A correlation matrix is then derived from various aircraft and flight conditions enabling the CFD-issued fuselage stream-wise force to be extrapolated to the entire aircraft's experienced force $\mathcal{F}_{\text{A/C,ref}}$ under different cruise conditions, despite only considering one in this work.

To maintain a steady cruise level, the aircraft propulsive force requirement $\mathcal{F}_{\text{A/C,ref}}$ is matched and split into two under-wing engines providing equal amounts of output

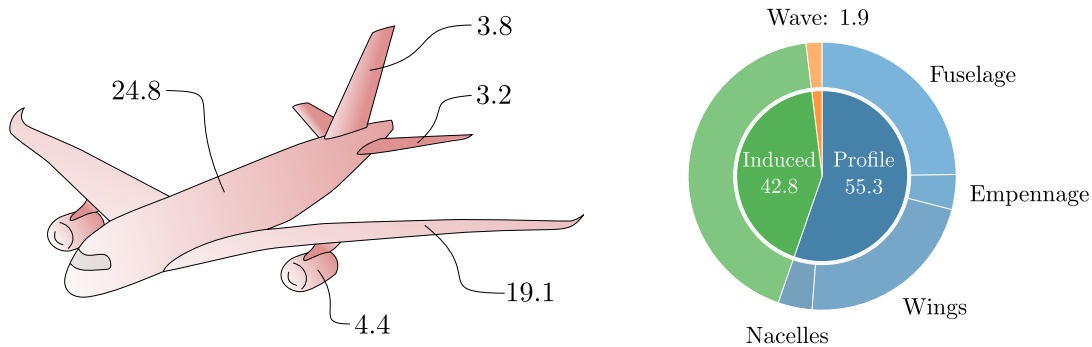


Figure 3.16: Aircraft's components profile drag portions (left) and profile, lift-induced and wave drag shares (right)

force. The two state-of-the-art turbofans pylon-mounted under either wings are considered fed by kerosene which power supply is given by the product of the fuel mass-flow $\dot{m}_f$ and its heating value h_f :

$$\mathcal{P}_{A/C,\text{ref}} = \dot{m}_{f,A/C,\text{ref}} \cdot h_{f,A/C,\text{ref}} \quad (3.21)$$

Alternatively, in fully-electric architectures where a power source feeds the engines, an in-house propulsion model is used to determine the power consumption to generate the output force.

3.4.2 Propulsive Force Share

An interim step is taken before the numerical computation of the propulsive fuselage concept whereby the net axial force $\mathcal{F}_{\text{fus,MOR}}$ of the un-installed morphed fuselage is computed. This force quantity is essential to be compared with the net axial force of the propulsive fuselage $\mathcal{F}_{\text{fus+prop,PFC}}$ to isolate the propulsive force arising from the BLI propulsor integration $\mathcal{F}_{\text{BLI}}$. It is also vital to determine what would be the morphed aircraft drag, if this vehicle was flying under the same assumptions as the reference. On both morphed and propulsive fuselages, the net axial force is computed from a *near-field* integration of the local pressure and skin-friction forces (Eq. (3.22)). The *near-field* is selected over other *far-field* integration methods that have been compared in Section 3.B. The rapidness and ease of implementation of the *near-field* integration motivates its choice over the integral of the power terms at the Trefftz plane.

$$\mathcal{F} = \iint_{\mathcal{S}} (p - p_{\infty}) \hat{n} - \bar{\tau} \cdot \hat{n} \, d\mathcal{S} \quad (3.22)$$

However, to allow the isolation of the propulsive force from the thruster, an assumption is taken where all changes in force between the un-installed and installed configurations are attributed to the propulsor integration, e.g. propulsor forward force and nacelle losses, despite the strong effect the integration has upstream along the tail [24]. This assumption is given by Eq. (3.23) as the resulting force between the propulsive fuselage and its clean morphed equivalent. Assuming the same drag decomposition from the aerodynamic decoupling, and

lift-to-drag ratio of the reference aircraft, the propulsive fuselage aircraft entire drag $\mathcal{F}_{\text{PFC}}$ is deduced.

$$\mathcal{F}_{\text{BLI}} = \mathcal{F}_{\text{fus+prop,PFC}} - \mathcal{F}_{\text{fus,MOR}} \quad (3.23)$$

Consequently, to enable a comparison between several PFC designs, a propulsive force share (PFS) is introduced as a means of quantifying the portion of propulsive force provided by the aft-mounted fan relative to the aircraft's overall requirement. The propulsive force share is defined in Eq. (3.24) as the ratio of the force contribution from the BLI thruster to the aircraft propulsive force requirement. This non-dimensional form shows its importance when one designs different aircraft and evaluates the change in propulsive force under equivalent power supply, or inversely, the change in power requirement to maintain the same force share.

$$\text{PFS} = \frac{\mathcal{F}_{\text{BLI}}}{\mathcal{F}_{\text{PFC}}} \quad (3.24)$$

3.4.3 Propulsive Efficiency Surrogate

The propulsive efficiency is defined as the efficacy at which the propulsive power available is converted into useful work acting on the flow. Whilst a surrogate of this metric was offered by Sanders and Laskaridis in Ref. [14] with κ , $\mathcal{P}_S$ was wrongly, but knowledgeably, used as the available power source. In parallel, other studies [46] use the ambiguous $\mathcal{P}_K$ term. In the former definition, losses are considered in the “available” power whereas in the latter, recoverable pressure work is omitted. Therefore, Eq. (3.25) offers an improvement, or correction of the denominator from previous definitions with the real power available to exert work on the flow, $\mathcal{P}_K^*$.

$$\kappa^* = \frac{\mathcal{F}_{\text{BLI}} \cdot \mathbf{V}_B}{\mathcal{P}_K^*}, \quad \text{where } \mathcal{P}_K^* = \eta_{fan} \cdot \mathcal{P}_{S,\text{BLI}} \quad (3.25)$$

This provides efforts towards a *real* propulsive efficiency definition, despite remaining a surrogate of it, since the numerator is still erroneous as it does not provide a correct definition of useful work but rather a propulsive power.

3.4.4 Power Consumption and Savings

The overall power consumption of the aircraft is driven by the thruster. Its requirement to operate at its given conditions to generate the forward force is either provided by a unique power source in the fully-electric architecture, or from the turbofans in the turbo-electric one. As such, it is important to consider the shaft power demand from the fan $\mathcal{P}_{S,BLI}$ rather than its available power to generate work $\mathcal{P}_K^*$, as losses are taken into account.

In the fully-electric scenario, the power consumption from the turbofan $\mathcal{P}_{pod,PFC}$ is obtained from the in-house code, as detailed earlier, and is added to the thruster power demand to yield the total power consumption $\mathcal{P}_{PFC}$ from the PFC.

In the turbo-electric thrust-splitting architecture, the power requirement from the aft-mounted thrust drives the power off-take from the main under-wing engines, to which a transmission efficiency η_{trans} is applied. This is done so to approximate losses from the extraction of the mechanical power from the low-pressure turbine, conversion into electrical power, transport and conversion back to a mechanical supply. This black-box is considered as such as the transmission system sizing and efficiency are out of the scope of this work.

Based on the assumptions that the overall aircraft stream-wise force remains unchanged between the reference aircraft and the PFC, there is an opportunity to rescale the under-wing engines to improve further the aero-propulsive performance of the novel aircraft, as their force generation requirement is reduced. Initially producing half of the propulsive force, each turbofan in the PFC contribute to half of the remaining portion of forward force (1-PFS).

Therefore, in the PFC, the overall power consumption from the aircraft is given by $\mathcal{P}_{PFC}$ which lumps together the power consumption from the podded engines $\mathcal{P}_{pod,PFC}$ and the power supply to the BLI fan accounting for various transmission efficiencies η_{trans} as given in Eq. (3.26).

$$\mathcal{P}_{PFC} = \mathcal{P}_{pod,PFC} + \mathcal{P}_{BLI} = \mathcal{P}_{pod,PFC} + \frac{\mathcal{P}_{S,BLI}}{\eta_{trans}} \quad (3.26)$$

To quantify the savings of the propulsive fuselage concept, the power consumption is compared with the reference aircraft, in a similar fashion that the work from

Smith Jr. [18]. Smith Jr. introduced a power saving coefficient (PSC) to quantify the reduction in mechanical power from a wake-filling engine relative to a podded one of equivalent areas. However, a main benefit related to BLI is the reduction in forward force contribution from the under-wing turbofans which can intrinsically be translated into a reduction in engine size. To circumvent the ambiguity related to the equivalence between the two propulsion systems, further investigations [50, 51] and subsequent PSC definitions were introduced [46, 51–53]. In this work, the PSC is defined by Eq. (3.27) by comparing the power requirement from the BLI aircraft with the reference aircraft where savings in power are achieved with positive values.

$$\text{PSC} = 1 - \frac{\mathcal{P}_{\text{PFC}}}{\mathcal{P}_{\text{A/C,ref}}} \quad (3.27)$$

Although power savings are reported, the metric is a surrogate of fuel burn, as given by Eq. (3.21). With the assumptions that the same fuels with the same properties are used in both reference and novel aircraft, the PSC reduces to a fuel saving coefficient (FSC) as introduced in Eq. (3.28). The consideration of future fuels for the PFC predicts to contribute further towards larger FSC, e.g. additional fuel savings of 36% could theoretically be claimed if liquid hydrogen (LH_2) was used similarly to kerosene, and no tanks for storage or heat exchanger for operation were required.

$$\text{FSC} = 1 - \frac{\dot{m}_{f,\text{PFC}}}{\dot{m}_{f,\text{A/C,ref}}} \quad (3.28)$$

3.5 Power Saving Coefficient Sensitivity to Propulsion Systems Performance

Inevitably, to obtain greater savings, the aft-mounted propulsor needs to outperform the state-of-the-art turbofans. Large force contributions from the thruster enable a down-scaling of the under-wing engines which inherently decrease their installation aerodynamics penalties and contribute to further benefits. However, larger penalties arise from the integration of the propulsor around the tail and it becomes therefore necessary to understand the sensitivity of the PSC to the different propulsion systems sizes and performance.

3.5.1 Impact of Reference Engines

As identified by Moirou et al. in Ref. [13], there are large discrepancies in PSC for similar concepts. This is primarily due to the choice made in the reference aircraft, and its reference engines. In PFC scenarios, the comparison with a conventional tube-and-wing aircraft carrying the same number of passengers and operating at the same flight conditions gives the benefits emanating only from BLI, with the most like-to-like comparison. In more disruptive airframes, the comparison is not as easy as the benefits claimed could either be attributed to BLI or to the redesign of the airframe. In these scenarios, the reference aircraft can either be defined equivalent with podded engines, hence showcasing the BLI benefits only, or as a tube-and-wing aircraft and therefore quantifying the synergistic benefits given by the novel architecture.

Focusing on PFC, savings in terms of power ranging from 6 to 15+% [20, 54] are not only attributed to differences in the propulsive performance of the thruster, but also depend on the reference aircraft and engines. As a matter of fact, fuel savings of 15% are already obtained by comparing similar aircraft propelled with two LEAP-1 turbofans versus two CFM56 [55]. This discrepancy is a result of differences in architectures such as the by-pass ratio (11:1 versus 6:1), overall pressure ratio (40 versus 31) and advanced technologies like found in the blades, bleeding, and combustion chamber. To circumvent the first ambiguity associated to the different claims, a consensus needs to be found amongst researchers to have common points of comparison.

3.5.2 Performance of Aft-End Thruster

To illustrate the sensitivity of the PSC with respect to the reference turbofans, a propulsive fuselage concept design is considered. A propulsor is circumferentially integrated around the aircraft tail at 90% of the fuselage chord length. Its fan ingests around 43% of the mass-flow present in the BL and applies a pressure ratio of 1.40 with a polytropic efficiency of 90%. The thruster generates 40.6% of the overall propulsive force requirement (each under-wing engine contributing to 29.7%) with a thruster propulsive efficiency of 72.88%. A representation of the concept is given in Fig. 3.17 with the Mach contours distribution and streamlines.

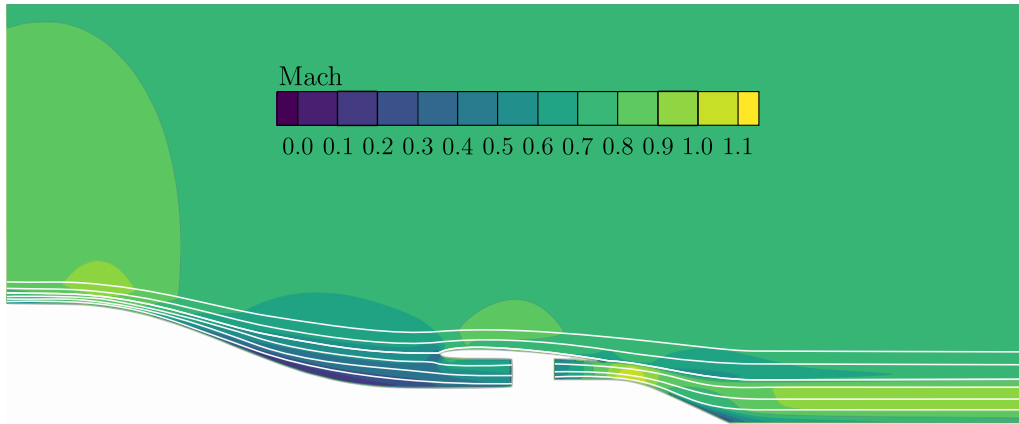


Figure 3.17: Mach number contours around a propulsive fuselage concept, with streamlines through the propulsor and over its cowl

3.5.3 Aft-Mounted Propulsor with Retrofit State-of-the-Art Under-Wing Turbofans

A first approach is taken by retrofitting the turbofans from the reference aircraft onto the PFC. By doing so, technology levels are set but the engine operates at off-design conditions as less thrust is required, and power is extracted from the shaft to drive the thruster.

Whilst there is no minimal bound to the PSC (i.e. the minimum is achieved by mounting inefficient under-wing engines), retrofitting the same engines is the most pragmatic approach to provide conservative savings. For example, considering the PFC introduced above, a PSC of 10.35% is achieved (between a PFC with retrofit TF engines versus the reference aircraft with the same engines). These savings increase to 16.17% if the reference aircraft is propelled by two CFM56 turbofans, highlighting almost 6% of savings from the change in turbofans.

To understand the reasons of such performance, a savings decomposition is enabled through the simple modelling of the TF with an electric ducted fan. A crude assumption is taken whereby the same output force is generated by the fan within a nacelle (versus the TF). In doing so, component efficiencies are isolated such as the intake, fan and exhaust and the benefits broken-down in Fig. 3.18. Notably, a *fair* replica of the TF without its core suggests a PSC of 9.50%, attributing 0.85% of the savings to the engine core ((b) in Fig. 3.18). Similarly, increasing the exhaust efficiency to the technology level envisaged on the thruster attributes 3.22% to the exhaust (c), which compounds to 4.51% when total pressure losses

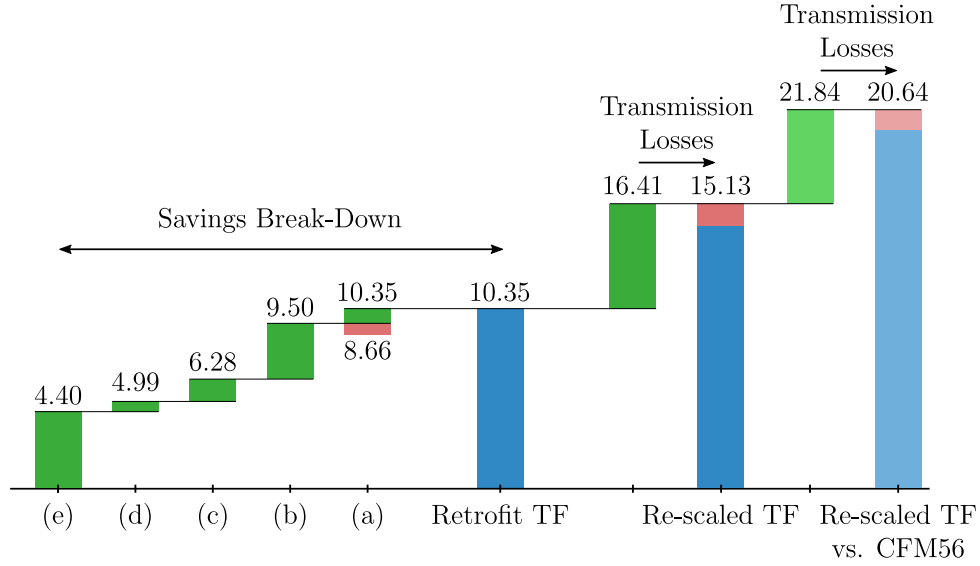


Figure 3.18: Power saving coefficient sensitivity to different propulsion system efficiencies

are reduced (d). On the fan, bringing down its pressure ratio from 1.49 to the operating FPR of the thruster (to maintain an equivalent shaft rotational speed), reduced the PSC to 4.40, corresponding to 0.59% of savings attributed to the fan (e). These figures are obtained in the retrofit scenario, meaning that the turbofans mounted on both the reference and PFC aircraft have the same characteristics. The remaining 4.40% are then the pure consequence of BLI energy harvesting, and the nacelle size down-scaling as the mass-flow rate through the engines reduces. However these figures are all obtained when ideal transportation of the power is guaranteed, and decreasing this efficiency to 95% reduces the PSC by 1.69%, to 8.66% (a), and to 14.59% (versus 16.17 with CFM56 mounted on the reference aircraft).

3.5.4 Aft-Mounted Propulsor with Re-designed State-of-the-Art Under-Wing Turbofans

Another benefit from BLI is the smaller thrust requirement from the turbofans as part of the propulsive force is provided by the thruster. Therefore, there is an opportunity in redesigning the turbofans such that their on-design point is at cruise for the given force and power off-take combination. In doing so, the retrofit TF are substituted with redesigned engines and compared to the reference aircraft. When conserving the TF on the reference as defined in Section 3.2.1.2, additional savings of 6.06 and 6.47% are achieved with this particular thruster design (with 100% and

95% transmission efficiencies, respectively). Trading the TF in the reference aircraft with two CFM56, the savings amount to 21.84 and 20.64% respectively with 100 and 95% transmissions efficiencies. The redesign of the TF therefore offers around 6% more in PSC and contribute greatly to the BLI benefits.

This clearly demonstrates the ambiguity that stands in the definitions of the under-wing engines in both reference and propulsive fuselage aircraft as PSC between 4.4 and 21.8% can be reported. It becomes rather important in these scenarios to describe the engine models to compare concepts between them.

3.5.5 Impact of the Thruster Design and Performance

It has been shown that for a given BLI propulsor design and performance, PSC is very sensitive to the under-wing engine models. However, PSC is mainly driven by the thruster design and performance as it harvests the energy present in the BL flow, and enables further savings from the under-wing turbofans by re-scaling them.

To try and appreciate the influence the thruster design has on the vehicle performance and the fuel savings, Fig. 3.19 gathers the relative difference in FSC between the designs and the initial design considered previously, with retrofit turbofans (with variations in parameters reported on the axes).

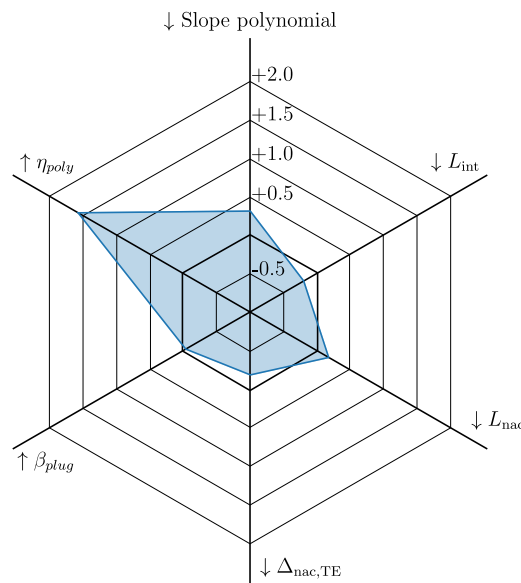


Figure 3.19: Spider web plot representing the delta in FSC between the baseline PFC and different thruster designs, as given by the axes, for reduction or increase in their values

It is observed that mild benefits are achievable by changing the polynomial order of the Bell-Mehta curve (with a slender slope, alike in Ref.[24]), shortening the nacelle or reducing the plug angle, with *only* 0.5% additional benefits. However, it is pre-empted that these design parameters can have detrimental effects on the performance as sharp accelerations, or flow separation can be expected on these wall surfaces. This highlights that several intensity (or sensitivity) levels are observed, as given by the throat area, A_{TH} , fan polytropic efficiency, η_{poly} , or the polynomial order of the Bell-Mehta curve defining the pre-diffusive slope.

Section 3.D and Table 3.D.1 provide a summary of the alterations in some design and performance parameters and their impact on PSC. For instance, improving the polytropic efficiency of the fan by 5% raises the PSC by 1.6%. Conversely, an increase in fan pressure ratio from 1.4 to 1.6 reduces the PSC by 3.4%, and ingesting a larger amount of mass-flow present in the BL showcases penalties of 3.03% (relative to the reference aircraft). It is hypothesised that primary effects on the performance and savings are observed with changes in mass-flow and pressure rise, and secondary effects with changes in the propulsor shape, for a given position of the propulsor along the aircraft tail.

3.5.5.1 Influence of the Thruster Efficiency

Greater fuel savings are achieved with improved BLI propulsor performance. In the previous discussions, the fan of the PFC featured an initial fan polytropic efficiency of 90%, which is brought to 93 and 95% respectively to represent potential technology improvements in the future. Consequently, improvements in PSC of 1.04 and 1.63% are achieved for the same net axial force, using the retrofit engines. These savings are primarily due to the fact that a similar amount of energy is harvested by the fan from the flow. The higher fan polytropic efficiency requires less additional power to apply the same pressure rise, to target the same output force, yielding larger savings.

For comparison, Fig. 3.20 illustrates the difference in entropy behind the fan in the two scenarios. Notably, the higher propulsive efficiency reduces the intensity of the jet plume, given by a lower entropy generation, which reduces the jet/wake mixing viscous dissipation rate and contributes to a better utilisation of the energy present in the flow. The increase in fan polytropic efficiency leads to a rise in propulsive efficiency κ^* from 72.88% to 80.56%. This results from a reduction in electric power demand from 4.21 MW to 3.94 MW (a 9.4% reduction) from the thruster's fan.

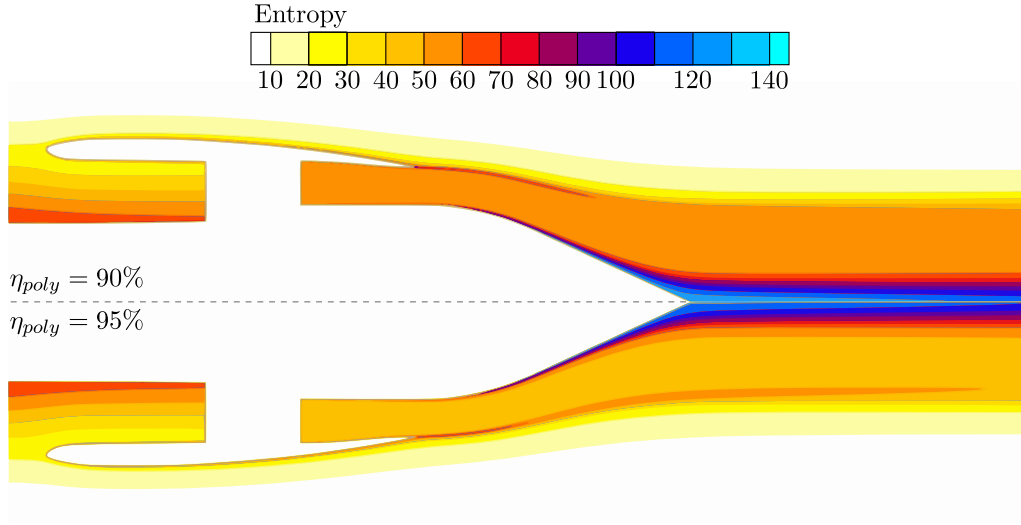


Figure 3.20: Difference in entropy between the baseline thruster with a fan polytropic efficiency of 90% (top) and the same design with a fan polytropic efficiency of 95% (bottom)

Subsequently, the jet dissipative effects, Φ_{JET} are reduced by 4%¹. However, relative to the power addition in the flow, as given by $\mathcal{P}_{S,\text{BLI}}$, the thruster with higher polytropic efficiency dissipates more relative to the baseline. In other terms, the jet dissipates 9.29% of the energy provided by the fan ($\Phi_{\text{JET}}/\mathcal{P}_{S,\text{BLI}} = 9.29\%$), in the baseline configuration, whereas 9.55% is dissipated for the larger polytropic efficiency.

3.5.5.2 Influence of the Slope Design in the Thruster Performance

In addition to the propulsor performance impact on the savings, the impact of the pre-diffusive slope on its surroundings is investigated with the local pressure coefficient C_p and skin-friction coefficient C_f distributions. Three Bell-Mehta polynomial orders are represented in Fig. 3.21. Notably, a higher order prevents initial abrupt changes in velocity as noted with a larger C_f just behind the change in curvature point. However, the higher order favours a larger steepness at mid-curve with a large negative C_p followed by an abrupt change in positive C_p where a flow detachment upstream of the nacelle is observed. This is confirmed by C_f where no skin-friction is noted along this portion of the surface. In these cases, flow recirculation increases the drag of the aircraft that needs to be overcome by the propulsion systems to maintain steady-level flight and consume more fuel.

¹The jet-plume viscous dissipation rate is computed following the precedence procedure detailed in Chapter 5, where the different flow features are identified and decomposed.

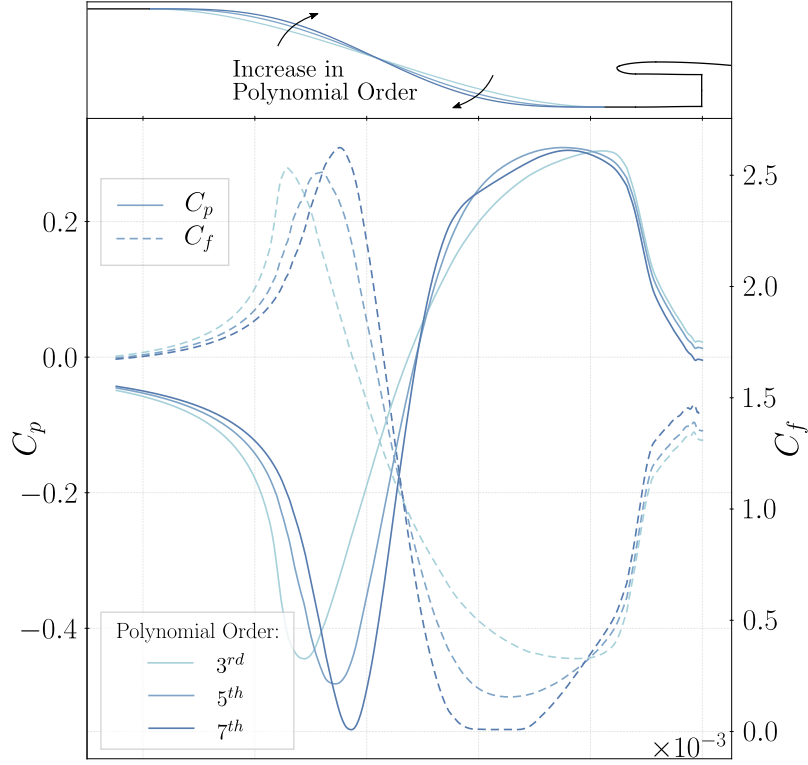


Figure 3.21: Local pressure and skin-friction coefficients distribution along the pre-diffusive slope for different curves polynomial orders

3.5.6 Aft-Mounted Propulsor with an Ideal Under-Wing Propulsion System

To circumvent the definition of the engines used to complement the force generation of the thruster, they can be fully idealised where propulsive efficiencies of 100% are achieved. These are unrealistic as the device size will be close to infinite to reduce the jet velocity and losses in the plume, but in doing so, the idealisation of the under-wing engines defines the maximal bound achievable by a BLI thruster design.

Generically, assuming a PFS is ensured in the PFC with an aft-mounted thruster, an external ideal device with a 100% propulsive efficiency, η_{prop} , is considered to contribute towards the remaining force requirement whilst only requiring the remaining force power to operate. The savings that would be achieved in that scenario are therefore represented with the potential power saving coefficient (PPSC) given by Eq. (3.29) as a special case of Eqs. (3.27) and (3.28).

$$\text{PPSC} = 1 - \frac{\mathcal{P}_{\text{BLI}} + (1 - \text{PFS}) \mathcal{F}_{\text{PFC}} \cdot V_B}{\mathcal{P}_{\text{A/C,ref}}} \quad (3.29)$$

In the example taken earlier, the PPSC amounts to 61.32% with ideal transmission efficiency, and 60.39% when reduced to 95%. Nonetheless, despite providing any tangible interpretation, as unachievable², the metric principal strength is its universality. Indeed, by considering an ideal propulsion system converting its force power into forward force, a fair and unique comparison is enabled between different aircraft concepts to only evaluate the performance of the aft-mounted propulsor, and hence its sole BLI benefits. Its universality allows one to consider turbo- or fully-electric aircraft architecture whereby a unique power source would provide energy to all propulsion systems. This way, absolute values of PSC's are rather insignificant but enable a quantification of the improvements granted by changes in the thruster design, or propulsive performance. Nonetheless, one should be careful and not optimise aircraft with this as an objective function as the optimiser will try to reduce the PFS and make the ideal propulsion system provide all propulsive force necessary.

A step further towards mitigating all challenges related to the definition of the on-board engines consists in comparing the PFC with a reference aircraft itself propelled with an ideal propulsive source. In this scenario, the savings are given by Eq. (3.30), as an extension to the definition given in Ref. [56] not considering thrust-splitting aircraft architectures.

$$\text{PSC}_{min} = 1 - \frac{\mathcal{P}_{BLI} + (1 - \text{PFS}) \mathcal{F}_{PFC} \cdot V_B}{\mathcal{F}_{A/C,ref} \cdot V_B} \quad (3.30)$$

Nonetheless, positive PSC values are only expected when the thruster, in the PFC, outperforms the ideal propulsion systems. In these cases, pure BLI benefits are highlighted showcasing the energy recuperation by the fan from the viscous flow. The main strength of this metric is therefore to represent the achieved savings by comparing different BLI propulsor designs and propulsive performances. Notably, retrieving the previous examples, the *minimum*³ PSC is -9.12% with ideal transmission versus -11.74% with a 95% transmission efficiency.

²There would be no need for BLI if ideal propulsive efficiency could be achieved by under-wing turbofans.

³There is a caveat in the use of the word “minimum” as it considers unrealistic ideal scenarios. However, consistency is kept in the naming convention with Ref. [56] which considers non-thrust-splitting aircraft.

3.5.7 Ideal Under-Wing and Aft-Mounted Propulsion Systems

Conversely, the maximal energy harvesting is achieved when an hypothetical ideal BLI propulsor is integrated to the aircraft. To estimate these benefits, the clean morphed fuselage is used to compute the irreversible losses at the vehicle trailing edge and define a potential for energy recovery (PER) [14, 56]. This definition relates back to the energy-based balances between the power sources and sinks within a unique control volume of an unpowered configuration whereby the theoretical maximum recoverable energy is defined.

Nonetheless, this definition differs from Ref. [14] whose only involves the viscous dissipation rate and over-predicts the real potential recovery, and Ref. [56] whose accounts for all compressibility effects, and does not decompose the reversible and irreversible components, but accounts for the residual losses.

Generically, PER is better defined by taking an exergy standpoint to dissociate the mechanical and thermal exergy rates, and their recoverable (energy/exergy) and irrecoverable (anergy) components. However, in adiabatic investigations (no heat added to the system), a corrected energy-based definition of PER is given by Eq. (3.31) accounting for the viscous dissipation rate at the trailing edge Φ_{TE} (which can be corrected from an exergy point of view as the viscous dissipation represents a loss in mechanical energy but a gain in thermal exergy [16]) and the rate of irrecoverable volumetric pressure work $\Theta_{irr,TE}$. Even though Φ is composed of an irrecoverable part dissipated as heat and another that could be recovered from an heat exchanger, this is outside the scope of this study. Similarly, Θ contains an irrecoverable component of the rate of volumetric pressure work from compressible effects represented with Θ_{irr} and a recoverable component that cancels out further downstream in the aircraft wake.

$$PER_{cc} = 1 - \frac{\Phi_{TE} + \Theta_{irr,TE}}{\mathcal{F}_{fus,MOR} \cdot V_B} \quad (3.31)$$

Overall, the quantity PER_{cc} , accounting for the compressibility correction, is interpreted as the maximal power benefit achievable by using an unducted BLI thruster that ingests the entire viscous layer and perfectly fills its energy defect without interfering with the flow-field upstream and around. Nonetheless, this

does not represent a practical maximum as PSC beyond this value can be achieved when the free-stream ingesting ideal propulsion system is traded for engines, hence lowering the performance of the reference aircraft and showcasing the benefits of ingesting the developed boundary-layer flow. However, the metric attracts one attention at preliminary design stages when shaping the fuselage, or airframe, to gauge the potential savings the vehicle offers.

Similar to the other PSC metrics defined in powered aircraft, PER_{cc} values can be compared between designs to evaluate the potential gains the different airframe shapes could offer. For reference on the morphed baseline considered earlier, PER_{cc} is evaluated at 11.4%.

Lastly, recent efforts in decomposition methods allow one to track the potential for energy recovery along bodies. In the definition of Eq. (3.31), this is not possible as the denominator corresponds to the fuselage drag and is not breakable. However, when considering velocity decomposition methods as given in Ref. [57], and partial pressure field in Ref. [58], their unification performed by Mutangara et al. in Ref. [59] allows to track all reversible and non-recoverable components of the power balance terms and therefore quantify the PER at any given position. An advantage of such method is to quantify the potential for energy harvesting at the fictitious position of the BLI propulsor and therefore pre-morph the aircraft tail before the propulsor integration.

3.5.8 Universality of the Metrics

The three metrics introduced ($PPSC$, PSC_{min} and PER_{cc}) have all been applied to a propulsive fuselage concept, however they are equally applicable to other concepts. The comparison with ideal propulsion systems removes all kind of ambiguities associated with the performance of the reference propulsion system, or the airframe. For example, the MIT D8 claims power savings of 7 to 10.5% [46, 60] which are the pure result of BLI benefits (comparison of the lifting airframe with podded engines versus embedded engines), but reports fuel savings of 36% purely based on changes in configuration (lifting fuselage versus tube-and-wing), which could compound to 65-71% with technologies advancements in 20-30 years [60, 61]. Similarly on the NASA N3-X, fuel savings of more than 70% are expected when comparing the concept to a Boeing 777-200LR [62]. These figures could appear as very optimistic but are mainly based on the point of comparison.

In the examples given, PER_{cc} becomes essential to pre-evaluate the potential savings the airframe offers. In these lifting fuselages, crude assumptions can be taken whereby the fuselage upper surface is assimilated to a flat plate and where it is known to yield potential recuperation of 8 to 15% of the energy imparted to the flow (as a function of the Reynolds number) [56]. For better estimates, CFD on the airframe can be performed and PER evaluated at any given position.

The main advantage arises from PSC_{min} where the reference airframe is propelled by ideal propulsion systems that consume the drag power of the configuration to generate the thrust required. The metric can be used to evaluate the performance of the integrated engines where their positions, numbers, shapes etc. can be changed. Additionally, when idealising the BLI propulsion system in such a way that it only fills the defects in momentum, or by analogy harvest the energy present in the flow, the metrics tend towards PER_{cc} where the delta in values attributes the losses to integration penalties (of the duct(s)).

3.6 Conclusion

A performance evaluation approach for novel aircraft is developed within a new framework that designs aircraft based on boundary layer ingestion and analyses their aero-propulsive performance. The aircraft modelling is simplified to 2D axisymmetric studies for demonstration of the novel approach and the work is based on the strengths of existing momentum- and energy-based methods to which new metrics are introduced.

Whilst studies on complex geometries use rather rudimentary bookkeeping schemes using momentum to evaluate the vehicle performance, recent efforts in developing energy-based methods have mostly been applied on simple or ideal case studies. This work therefore aims to bridge that gap by presenting an approach demonstrated on propulsive fuselage concepts. A strength of this approach stands in the simplification of the aircraft model to a fuselage and propulsor assembly to determine the performance of the entire vehicle.

The power balance is corrected with its $\mathcal{P}_K$ term that represents the additional power imparted to the flow from moving surfaces (such as the fan's blades). In this correction, the available power to perform work on the fluid is isolated from the total power input by identifying the inherent losses (viscous dissipation rate and

irrecoverable pressure work within the propulsor domain). This correction leads to a closer definition of a propulsive efficiency definition despite not incorporating a correct numerator (useful work), left for further consideration. A correct quantification of the propulsor efficiency is vital as it drives the vehicle performance and the fuel savings.

It is popular to use power (or fuel) savings as the main metric to compare novel aircraft configurations to conventional ones, and to relate the savings to the environmental goals for aviation. Nonetheless, it has been reported in several works that there is a controversy behind the power saving coefficient (PSC) with its definition, the assumptions considered by one, and the reference aircraft and engines used. Therefore, towards removing all ambiguities, new metrics are introduced aiming at quantifying the performance of the novel aircraft concepts.

On clean fuselages, the potential for energy recovery (PER) is corrected to account for compressibility effects and importantly its non-recoverable part that needs to be removed from the potential savings achievable. Extending further to integrated airframe-propulsion systems, the PSC is bounded by two metrics, namely the practical minimal PSC and the maximum potential PSC. On the former, PER is retrieved when the thruster performance asymptotes ideal propulsive performance. The difference between them associates the losses to the propulsor integration penalties. On the latter, further savings are achievable by rescaling and redesigning the under-wing turbofans, yielding the maximum power saving coefficient when they asymptote ideal propulsive efficiency.

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Appendix 3.A Coupled Domain Size and Grid Refinement Study

A grid dependence study is conducted following Celik et al.'s procedure alongside a domain size study [38]. Precedence is given to fine boundary layers along the fuselage and wake regions to accurately capture the net stream-wise force from *near-field* and *far-field* computations. To ensure the boundary conditions do not interfere with the flow-field around the fuselage, three domain sizes are considered that extend further downstream the body. The domain and control volume for analysis follow Fig. 3.9 where $d_{FS} = 10 L_{fus}$ and $d_{TP} = 20 L_{fus}$. To evaluate the sensitivity of the solution to the presence of the outlet boundary condition at the Trefftz plane, three domain sizes are investigated: $d_{outlet} = 20 L_{fus}$, $d_{outlet} = 30 L_{fus}$ and $d_{outlet} = 40 L_{fus}$, respectively representing the small, medium and large domains.

The meshed fuselages are computed using CFD with their three domain and mesh sizes and the solution convergence is monitored along the iterations in Fig. 3.A.1. The *near-field* force $\mathcal{F}_{fus,nf}$ is computed following Eq. (3.22) whereas the *far-field* one $\mathcal{F}_{fus,ff}$ is computed from Eq. (3.20), where both variables are respectively identified as ϕ_1 and ϕ_2 in Table 3.A.1. Lastly, their relative difference is also represented and identified as ϕ_3 in Table 3.A.1.

Table 3.A.2 summarises the coupled grid and domain convergence study that follows Ref. [38] notations whereby the following is considered: the number of cells

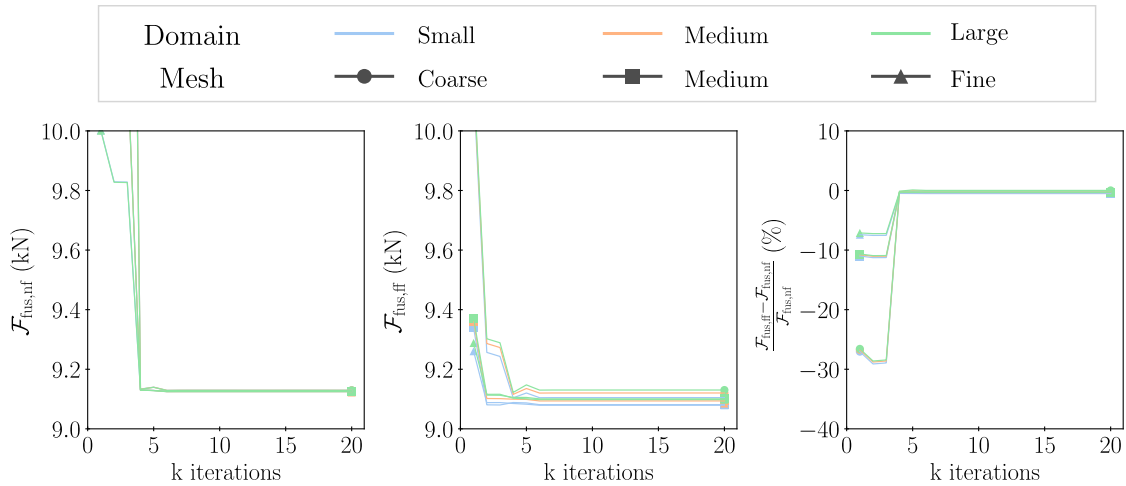


Figure 3.A.1: CFD steady solution convergence relative to the number of iterations (in thousands)

3. Hybrid Aero-propulsive Performance Evaluation Approach

Table 3.A.1: Domain and size convergence study on three grid and three domain sizes

Domain	Small			Medium			Large		
	Coarse	Medium	Fine	Coarse	Medium	Fine	Coarse	Medium	Fine
N	56356	517096	1815516	64848	525398	1834598	73533	528363	1855043
h	3.156	1.042	0.556	3.420	1.201	0.643	3.605	1.345	0.718
ϕ_1 (N)	9129.49	9125.07	9126.57	9129.33	9124.92	9122.10	9129.14	9124.76	9126.28
ϕ_2 (N)	9104.78	9081.17	9079.25	9120.38	9093.67	9084.95	9130.30	9100.58	9098.09
ϕ_3 (%)	-0.271	-0.481	-0.519	-0.098	-0.342	-0.407	0.013	-0.265	-0.309

Table 3.A.2: Domain and size convergence study on three grid and three domain sizes: grid convergence index

Domain	Small		Medium		Large	
	Coar./Med.	Med./Fine	Coar./Med.	Med./Fine	Coar./Med.	Med./Fine
r	3.029	1.874	2.846	1.869	2.681	1.874
ε_1 (N)	-4.42	1.5	-4.412	-2.827	-4.379	1.515
ε_2 (N)	-23.607	-1.923	-26.712	-8.73	-29.721	-2.489
ε_3 (%)	-0.210	-0.038	-0.244	-0.065	-0.278	-0.044
p_1	-	0.759	-	0.083	-	0.866
p_2	-	1.391	-	0.475	-	1.683
p_3	-	0.829	-	0.634	-	1.166
ϕ_1^{ext} (N)	9132.84	9122.62	9177.74	9177.74	9132.39	9122.66
ϕ_2^{ext} (N)	9111.21	9082.55	9161.89	9118.92	9137.28	9101.90
e_1^a (%)	0.05	0.02	0.05	0.03	0.05	0.02
e_2^a (%)	0.26	0.02	0.29	0.10	0.33	0.03
e_1^{ext} (%)	0.04	0.03	0.53	0.58	0.04	0.02
e_2^{ext} (%)	0.07	0.02	0.45	0.28	0.08	0.02
GCI ₁ (%)	0.03	0.02	0.11	0.13	0.03	0.02
GCI ₂ (%)	0.09	0.01	0.57	0.13	0.10	0.01
ϕ_1^{min} (N)	9130.00	9120.16	9167.85	9124.93	9129.49	9120.57
ϕ_1^{max} (N)	9135.68	9125.07	9187.63	9230.56	9135.28	9124.76
ϕ_2^{min} (N)	9103.17	9081.17	9109.78	9093.68	9128.55	9100.58
ϕ_2^{max} (N)	9119.26	9083.93	9214.01	9144.17	9146.02	9103.23

N, grid size h and key variables ϕ_i . In addition, the comparison between the coarse and medium mesh (Coar./Med.), and medium and fine mesh (Med./Fine) is given with the following: the refinement factor r, variable error ε , apparent order p , extrapolated variable ϕ^{ext} , its approximate error e^a , its extrapolated error e^{ext} and the grid convergence index GCI. Also, to support Fig. 3.B.1, the error range with its minimum and maximum values on the extrapolated variables, is provided.

Table 3.A.2 indicates a very close agreement in forces obtained from the *near-field* force and *far-field* energy formulations, which is within 0.01, -0.27, and -0.31% for the coarse, medium and fine grids, respectively, with the larger domain. This is significant, considering the Trefftz plane position selected far away from the body. Furthermore, the numerical uncertainty in the fine grid solution is less than 0.6% for all domain sizes, based on the GCI definition.

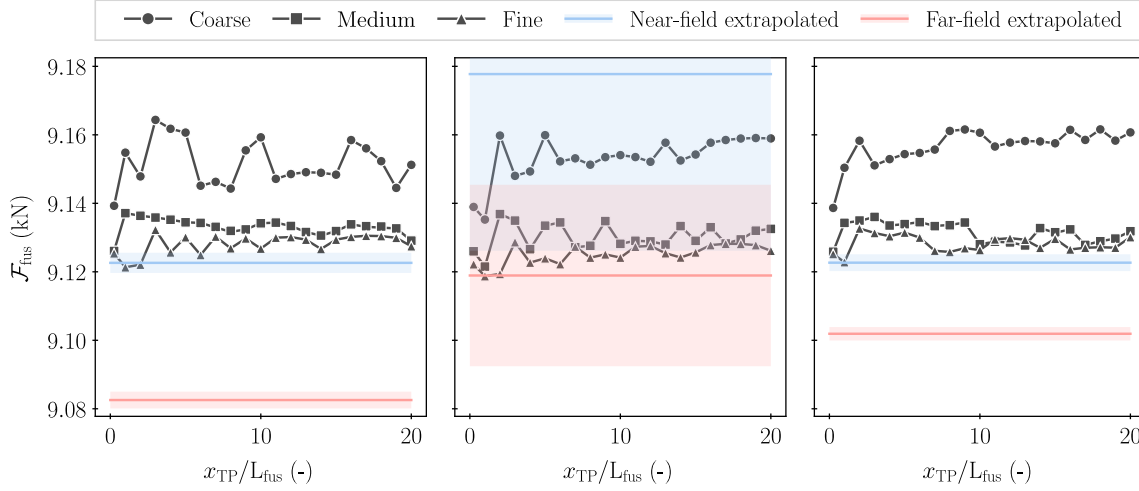


Figure 3.B.1: Comparison of net stream-wise force between the *near-field* and *far-field*, as computed, and their extrapolated values from Table 3.A.2

Appendix 3.B Force Convergence Comparison

The net stream-wise force is evaluated from *near-field* integration as previously mentioned, as well as in the *far-field* by sweeping different transverse plane at each fuselage chord behind the body until d_{TP} at twenty fuselage lengths behind is reached. This allows to track the convergence of the far-field force from an energy standpoint relative to the *near-field* force, from skin-friction and pressure forces. The spatial convergence is given in Fig. 3.B.1.

It is observed on the larger domain a good prediction of the total net stream-wise force for all grids just behind the fuselage, even more so for the finest two meshes. This convergence advises that, on the larger domain size, the medium mesh size can be down-selected for the next investigations as it provides a good agreement with a mesh more than three times denser (around 0.53 million versus 1.85 millions cells).

Appendix 3.C Aircraft Drag Build-Up

To relate the CFD solution of the bare fuselage to the entire aircraft drag, a drag build-up method is followed from Refs. [63, 64]. In this approach, available data from short-to-medium range aircraft are retrieved and mapped out under different flight Mach numbers at a flight-level of 35,000 ft above the sea level. Figure 3.C.1 summarises the correlation between the lift coefficient and the lift-to-drag ratio

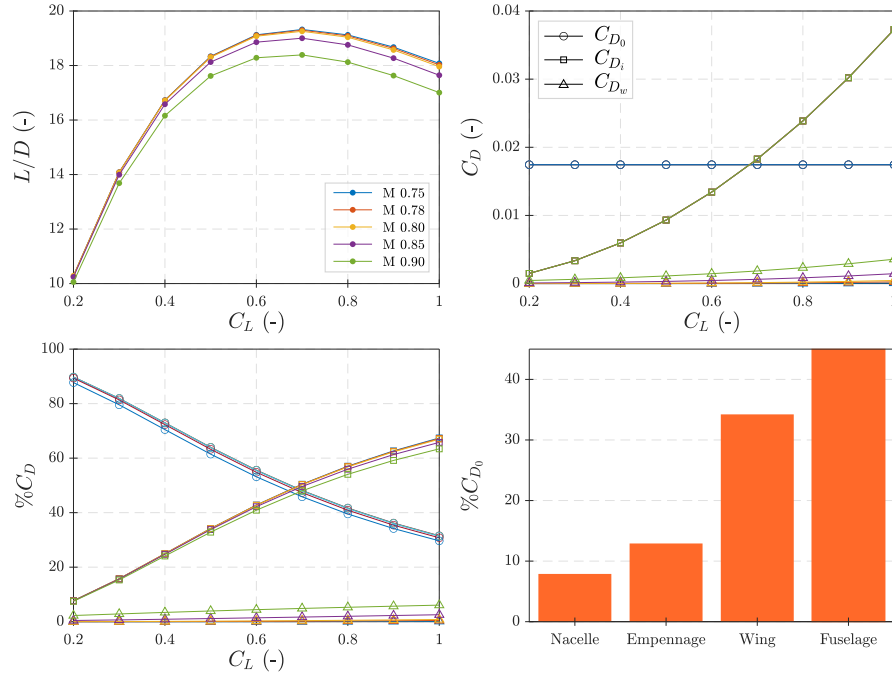


Figure 3.C.1: Drag build-up of the reference aircraft, for an assumed lift-to-drag ratio $L/D = 19.0$ at $M_B 0.78$, corresponding to a lift coefficient of $C_L = 0.6$

(L/D) to relate it to an overall drag coefficient broken-down into its profile, induced and wave drag components. Notably for the aircraft considered, a L/D of 19 advises on $C_L = 0.6$ (top-left corner) which corresponds to a 55% lift-independent C_{D_0} , 43% lift-induced C_{D_i} and 2% wave drag C_{D_w} (bottom-left corner). Of these 55%, the different aircraft components aerodynamically decoupled are computed (bottom-right corner) and their shares given with: 7.9% for the nacelles, 12.7% for the empennage assembly (vertical and horizontal stabilisers), 34.5% for the wings and 44.9% for the bare fuselage. Overall, this represents a share drag of 24.8% of the overall aircraft drag that is attributed to the fuselage (as the non-lifting fuselage is assumed to not have any lift-induced and wave drag), permitting the estimation of the overall aircraft drag from CFD by just computing the bare fuselage.

Appendix 3.D Sensitivity of the Power Saving Coefficient

The power saving coefficient (PSC) sensitivity is reported in Table 3.D.1 with respect to changes in under-wing turbofan performance and BLI thruster design

Table 3.D.1: Power saving coefficient sensitivity to propulsion systems performance and design, with reference to the nomenclature given in Fig. 3.5, as their values are increased $\uparrow$ or decreased $\downarrow$

Thruster + Retrofit Turbofan (TF) versus.		Turbofan (TF)	Change relative to the baseline (%)	CFM56
Baseline	-	0.1035	-	0.1617
A_{TH}	$\uparrow$	-0.0303	-13.38	0.0363
FPR	$\uparrow$	0.0693	-3.42	0.1297
η_{poly}	$\uparrow$	0.1198	+1.63	0.1770
L_{int}	$\downarrow$	0.1015	-0.20	0.1598
L_{nac}	$\downarrow$	0.1058	+0.23	0.1638
$\Delta_{nac,TE}$	$\downarrow$	0.1014	-0.21	0.1597
β_{plug}	$\uparrow$	0.1032	-0.03	0.1614
Slope polyn. order	$\downarrow$	0.1064	+0.29	0.1644
η_{trans}	$\downarrow$	0.0866	-1.69	0.1457

and performance. These comparison are rather simplistic and qualitative to guide one on the influence the design variables have and their strengths. Also for reference, both TF as defined in this work, and previous generation CFM56 are used for comparison.

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Considerations in the Design and Performance Assessment of Boundary Layer Ingestion Aircraft

A potential contribution to a synergistic development of aircraft results in a tight embedding of the propulsion system within the airframe as featured in propulsive fuselage concepts. This work examines fundamental considerations in the design phase of these concepts with from a Galilean covariant perspective on aerodynamic work-energy relationships in flight. Prior studies have shown the aerodynamic sensitivity to design changes on pre-defined fuselage designs using momentum-based analyses. It is here intended to generalise these observations and enhance them with an aero-propulsive performance assessment undertaken in the absolute reference frame, believed to best evaluate these effects with intuitive physics-based observations. Following previous investigations on the potential for energy recovery of fuselages of different slenderness ratios, this work extends the observations by focusing on the tail shape and length. In a systematic approach, the propulsor installation design space is explored with macro design parameters (position, size and operating conditions) whereby the aero-propulsive performance is assessed suggesting up to 11% in fuel savings on the cruise segment. Further, the introduction of compact nacelles is proposed along with a re-scaling of the under-wing turbofans for enhanced fuel savings – additional 5%. Overall, this work comprehensively characterises the aerodynamic response to fuselage and propulsor design changes which inform designers and aerodynamicists on the potential performance of their concepts to meet the environmental targets of future aircraft.

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4.1 Introduction

To realise the ambitious environmental goals set by the European Commission [1], it is notwithstanding that the current aircraft are approaching their limit in terms of improvements in propulsive efficiency [2]. Therefore, in contrast to conventional configurations, there is an opportunity to exploit potential aerodynamic benefits with a closer integration between the airframe and propulsion system. Taking inspiration from the marine industry [3], a propulsor is integrated around the back of the airframe to ingest the boundary layer (BL) forming over it to achieve aero-propulsive performance improvements [2]. Whilst the kinetic energy excess imparted by the fuselage to the flow in conventional configurations is eventually wasted within the aircraft's wake, its ingestion by an aft-mounted propulsor benefits the aircraft performance by reducing its fuel consumption [4].

Amongst the conceptual aircraft based on boundary layer ingestion (BLI) reviewed by Moirou et al. [5], the propulsive fuselage concept (PFC) [6, 7], given in Fig. 4.1, appears as the most pragmatic from its adaptation of the conventional tube-and-wing aircraft, whereupon an aft-mounted propulsor encapsulates the tail-cone. The main advantages of this configuration is the direct installation on current airframe, the opportunity to ingest the entire circumference of the fuselage's BL and consequently, to reduce the viscous dissipation that would otherwise have occurred in the wake by harvesting the energy to produce a propulsive force. Additionally, the quasi-circumferential nature of the flow reduces the penalty from the fan's operation in circumferentially distorted flow and also allows the flow-field to be approximated axi-symmetrically [8–11].

Through cautious application of the momentum-based approaches, it is possible to correctly quantify the net forces acting on the configurations, however they do not yield much information and conventionally require the break-up of the airframe (drag) and propulsion system (thrust) to gain a better understanding [12].

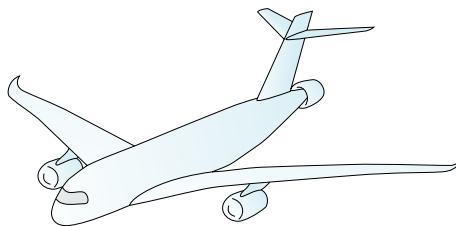


Figure 4.1: Generic propulsive fuselage concept, from Chapter 3

Conventional methods [13, 14] are nonetheless invalid for BLI as they are based on certain assumptions to define drag as the resulting force between the forces acting on a body and those in a potential flow, and thrust as a surrogate of the useful work generated by the propulsion system. An alternative approach enables more physically intuitive interpretations and quantification of the underlying physics than the limited insights offered by momentum-based methods [12], as well as circumventing potentially misleading associated metrics [4, 15, 16]. Assessing the different losses in the system from an absolute reference frame (ARF) standpoint enables improved identification and interpretation of the causal mechanisms governing BLI aerodynamics. Unlike the relative reference frame (RRF) that echoes experimental testing conditions, the ARF considers the aircraft as the moving body through an initially quiescent environment thus, imparting momentum and energy to its surroundings. The energy transferred to the flow can be traced to localised losses in different flow phenomena. This enables a decomposition, quantification and interpretation of the various mechanisms underlying BLI performance.

Aero-propulsive performance is inherently dependent on both the airframe and propulsion system. As such, it is important to assess the sensitivity of both with respect to changes in design and operating conditions. Ahuja and Mavris [17] with a systematic approach, explored the BLI performance sensitivities to airframe design changes using the power balance method. In their work, they investigated different nose shapes, 3D up-swept tails and tail ramps but their metrics are questionable and a focus was made on engines positioned alike the MIT D8 [18] and ONERA NOVA [19]. Kenway and Kiris [20] quantify the wing downwash effects on the BLI fan inlet distortion and the importance of considering it in optimisation studies even though they pre-empt attenuated effects through simple wing-root re-designs. Despite the recommendations, wing-less airframes are of interest for reduced computational cost and focus on the impact of other design variables independently of the downwash generated. In that sense, Gray et al. [8] initiate a coupled aero-propulsive analysis with a single design variable (fan pressure ratio – FPR) at constant fan power input and conclude on the strong dependence of the overall performance to the aerodynamic and propulsive performance. The coupling strength indicates that the overall aero-propulsive performance is likely to also depend on design variables such as the tail and nacelle geometries, and fan input power. Battiston et al. [11] observe the effects of the nacelle size and fan radial position on the system performance but they had to

consider some geometric constraints that prevent the generalisation of the findings. They also confirm the importance of considering the FPR in the design exploration which is also supported by Habermann et al. [9] with their multi-dimensional design exploration. Although the strong inter-relation between the geometric variables do not allow conclusive implications, some observations are made on their concept and are worth being generalised [9, 11]. Therefore, there is a knowledge gap in the flow behaviour when altering fuselage design parameters in propulsive fuselage concepts.

To address this gap observed, the present work investigates first the flow-field and its changes for various bare fuselage tail designs. From the tail morphing to accommodate a BLI propulsor, additional design aspects are studied. First, the potential for energy recovery (PER) [4, 21, 22] is compared between conic and morphed tails. Then, the serpentine slope effects and its critical inclination angle are investigated. To bound the design space both axially and radially, the critical plug-cone angle is identified. These observations lead to multiple morphed tail designs from which PER is evaluated. The integration of aft-mounted propulsors enables the coupled aero-propulsive performance evaluation. To that end, several propulsors are designed with different sizes and fan operating conditions. In comparison with a state-of-the-art short-to-medium range aircraft, the aero-propulsive performance and fuel savings of the different concepts are reported and recommendations are provided on potential performance improvements.

4.2 Fuselage Design Considerations

4.2.1 Fuselage Specifications and Numerical Implementation

Taking reference from a short-to-medium (SMR) aircraft, e.g. Airbus A321 of length 44.51m or Boeing B737-MAX10 of length 43.79m, the fuselage is isolated from its entire airframe. Its approximation and parametrisation given in Fig. 4.2 follows three distinct parts: an elliptic nose, a cylindric cabin and a conic tail. Therefore, all portions are driven by a fineness ratio, defined as the ratio of length over the radius of the component i ($FR_i = L_i/R_i$). In that respect, the ellipse approximates the aircraft's nose from both a side- and top-view, and its down selection follows.

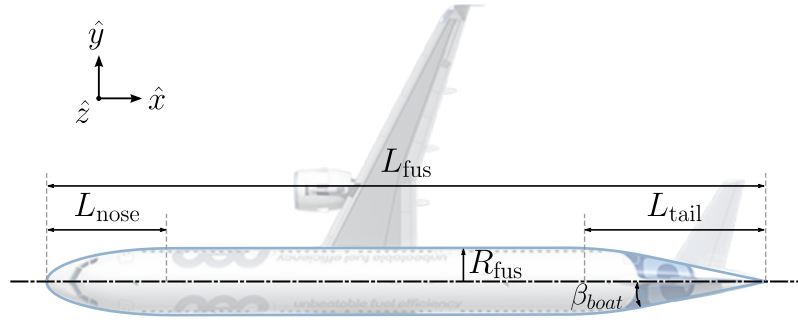


Figure 4.2: Schematic of the reference aircraft's bare fuselage approximation with an ellipsoidal nose and conic tail from an Airbus A321, courtesy of Airbus

Similarly, the closest tail fineness ratio to the SMR aircraft's frustum tail, FR_{tail} , is derived with a boat-tail angle β_{boat} .

Within the DAHIANT (Design and Analysis of Highly Integrated Aircraft and Novel Technologies) framework (Chapter 3), the MENAC (Modelling of Embedded Novel Aircraft Concepts) module is used for geometry generation. Encapsulated within a bullet-shaped domain, the fluidic volume is subsequently meshed. Following an automated structured grid generation routine, the domain is meshed using ANSYS

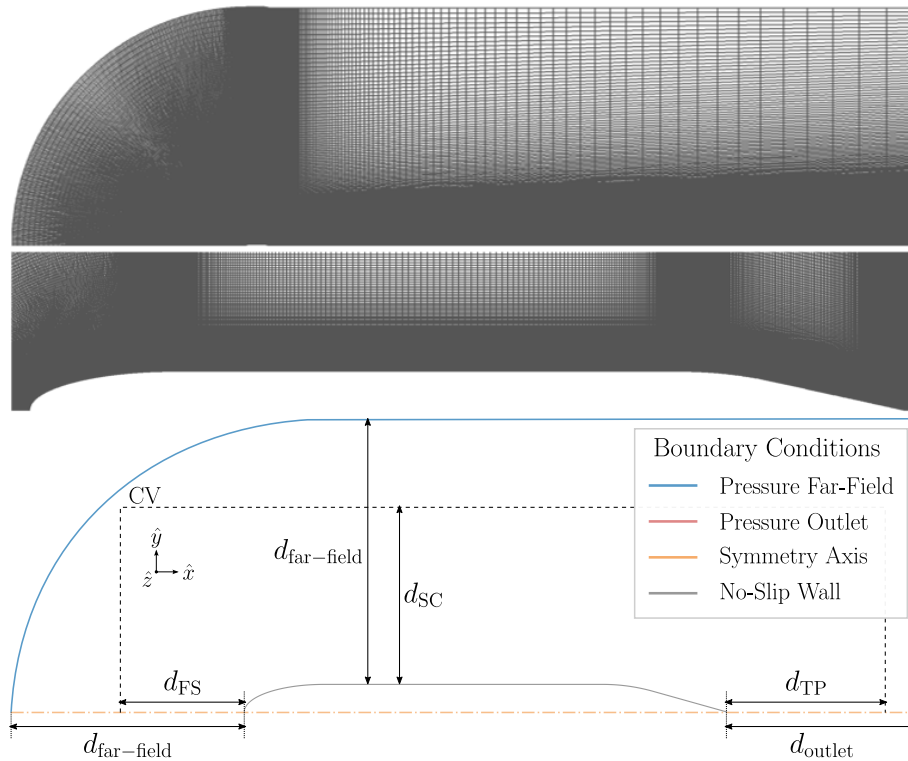


Figure 4.3: Grid of the bullet-shaped domain encapsulating the fuselage, grid close to the fuselage wall, and schematic of the domain, boundary conditions and analysis control volume

ICEM 20.2 [23] and sets-up the CFD in Fluent 20.2 [24] (Fig. 4.3). The bullet domain is defined such that its position does not interfere with the body through its boundary condition after undertaking a grid and domain size convergence study (Chapter 3), based on the procedure from Celik et al. [25], and adopted from Sanders and Laskaridis [4]. As such, 10 fuselage chords are ensured upstream and around ($d_{\text{far-field}}$) and 40 behind (d_{outlet}). The mesh follows a C-topology where elements close to the walls ensure a fineness with a y^+ falling between 0.9 and 1.0 along the entire body and coarsen the farther away. Representative flight conditions of an Airbus A321 are used for the CFD with a flight Mach number $M_B = 0.82$ and FL350 (35,000 ft above sea-level) for $Re = 2.87 \cdot 10^8$. In effect, these surrounding conditions are given in the CFD with a far-field pressure condition and a pressure outlet (Fig. 4.3).

4.2.2 Nose Down-Selection Considerations

A nose is often a distinguishable feature of an aircraft with its flight deck position and shape, and an isometric view suggests that no single profile can be extracted for a 2D axi-symmetric representation. To approximate a nose contour, top- and side-view contours of an Airbus A321 are considered for reference and compared against three ellipsoidal approximations of different fineness ratio (Fig. 4.4). Distinctively, an ellipse of $FR_{\text{nose}} = 3.71$ is considered as the closest approximation of the top- and side-views and two shorter noses are considered up to an hemispheric approximation, i.e. circular arc. To evaluate the flow response to the different noses and select the

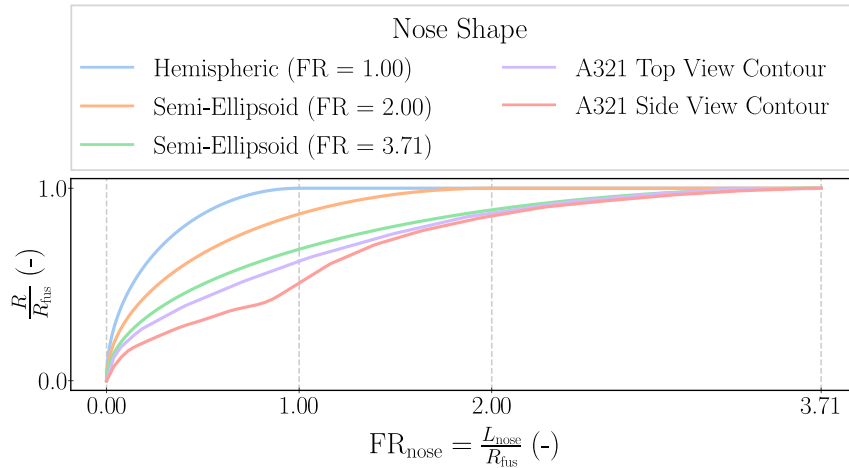


Figure 4.4: 2D axi-symmetric nose modelling of an A321 side- and top-view contours and elliptical approximations of different fineness ratios

reference shape for future studies, the BL properties along the fuselage, and the energy-flux transfers within the wake are compared.

4.2.2.1 Figures of Merit

To evaluate the differences in BL properties from the different nose designs, three boundary layer parameters are used: the BL area Δ given in Eq. (4.1a), its mass displacement area Δ^* , given in Eq. (4.1b), and its kinetic energy area Θ^* in Eq. (4.1c). In comparison with the conventional 2D thickness definitions, these areas are applicable to 3D axi-symmetric scenarios whereby a local width parameter b is introduced [26]:

$$\Delta = \int_0^\delta b(n) \, dn \quad (4.1a)$$

$$\Delta^* = \int_0^\delta \left(1 - \frac{\rho V}{\rho_e V_e}\right) b(n) \, dn \quad (4.1b)$$

$$\Theta^* = \int_0^\delta \left(1 - \frac{V^2}{V_e^2}\right) \frac{\rho V}{\rho_e V_e} b(n) \, dn \quad (4.1c)$$

where δ quantifies the boundary layer thickness, ρ the fluid density and V its velocity, and whereby e quantifies the boundary layer edge properties.

Along with the development of the BL properties, common mechanical energy-rate terms present in the energy- and exergy-based analyses [4, 15, 16] are calculated along survey planes along the fuselage. In doing so, the power balance is given in Eq. (4.2) where the terms on the left represent the power sources to the flow, and on the right their sinks.

$$\mathcal{P}_S + \mathcal{P}_f = W\dot{h} + \underbrace{\dot{\mathcal{E}}_k + \dot{\mathcal{E}}_p + \dot{\mathcal{E}}_\tau}_{\dot{\mathcal{E}}_m} + \Phi + \Theta \quad (4.2)$$

where $\mathcal{P}_S$ describes the amount of shaft power related to any moving fan's blade, $\mathcal{P}_f$ the power input to the flow when the fan's blades are substituted by a body force representation inside a volume, $W\dot{h}$ the change in potential energy due to changes in altitude¹, $\dot{\mathcal{E}}_m$ the mechanical energy deposition rate and Φ and Θ (Eq. (4.3))

the system losses manifesting themselves under the form of viscous dissipation and volumetric pressure work rates, respectively.

$$\Phi = \iiint_{\mathcal{V}} (\bar{\tau} \cdot \nabla) \mathbf{V} \, d\mathcal{V} \quad (4.3a)$$

$$\Theta = - \iiint_{\mathcal{V}} p_G \nabla \mathbf{V} \, d\mathcal{V} \quad (4.3b)$$

4.2.2.2 Flow Response to Nose Shape

Figure 4.5 depicts the evolution of the BL area (Eq. (4.1a)) both along the fuselage and within the wake. It is observed that the hemispheric nose stands out from the other shapes. From the aircraft's shoulder a jump in the BL area caused by a strong local shock is observed. That shock is discernible from the mass displacement area Δ^* with a sharp increase that causes an unrecoverable thickening of the shear-layer. The semi-ellipsoid approximation of $FR_{\text{nose}} = 3.71$ stands amidst the other shapes that are not subject to any shock. Its deviation from both A321 profiles yields a small difference in the BL properties but suggests a quasi-identical, yet greater, thickness and energy transfer from the fuselage to the flow. In other terms, a rear-mounted BLI propulsor will experience a slightly smaller mass-flow from the elliptic approximation than the more representative contours for an equivalent propulsor size.

The difference in energy is confirmed by the right-hand side column's charts that depict the flux transfer breakdown from the aircraft's trailing edge to the Trefftz. The kinetic energy deposition rate and pressure-defect work are transferred into unrecoverable forms of energy Φ and Θ . The decay of power overtime results from the absence of a body that imparts additional energy to the flow which self-recovers to its initial quiescent form. Whereas a steep rate of dissipation is observed on the different nose shapes, the hemispheric nose necessitates more time to fully dissipate with a gradual rate. Notably, the volumetric pressure work Θ cannot be neglected any longer as the shock left a residual irrecoverable pressure-defect work.

It is concluded that from the perspective of the BL growth, energy imparted to the flow and its recoverable part from an ideal device mounted at the aircraft's

¹This quantity is equal to the fuselage drag power, $\mathcal{F}_{\text{fus}} \cdot \mathbf{V}_B$, when assuming steady-level flight

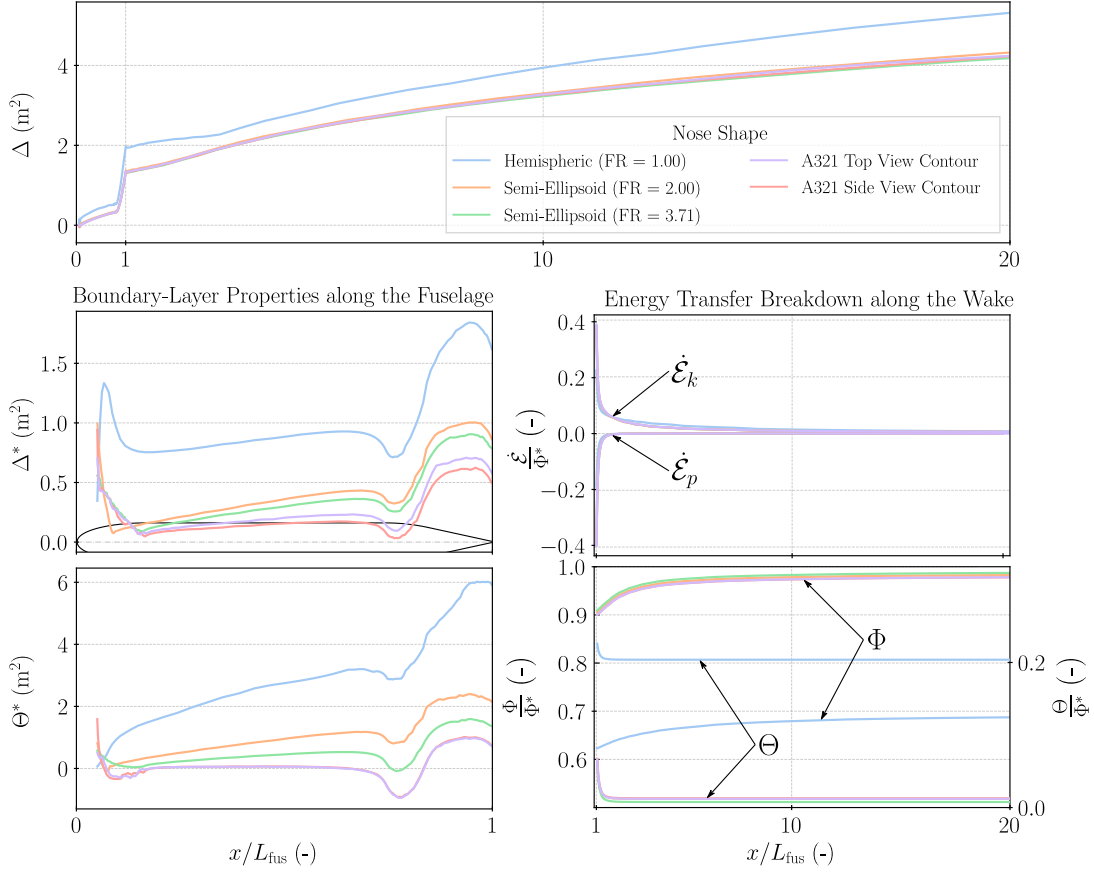


Figure 4.5: 3D axis-symmetric boundary-layer area (top), displacement area and kinetic energy area (left), and wake energy-flux transfers (right) for 5 different nose shapes at FL350 and $M_B0.82$

rear, the semi-ellipsoidal nose of $FR_{nose} = 3.71$ suits the nose shape approximation. However, it is stressed that the elliptic approximation is only acceptable under shock-free conditions which otherwise would dissipate a higher amount of non-recoverable energy and hence diminish the aerodynamic potential performance of the aircraft design. The elliptic nose shape is thus selected for the SMR reference aircraft and is acknowledged as a good candidate to represent the nose shape.

4.2.2.3 Boundary Layer Sensitivity to Mach Number

To follow on the flow response due to changes in nose geometries, a further investigation is performed using the precedent hemispheric nose. At a glance, a distinctive trend arises from the different Mach numbers where higher free-stream Mach numbers favour shock generation and thicken the BL. For flight conditions below transonic, i.e. $M0.75$, no shock is observed on the BL displacement area Δ^* . Whereas $M0.65$ and $M0.70$ flight conditions do not differ in the different charts of

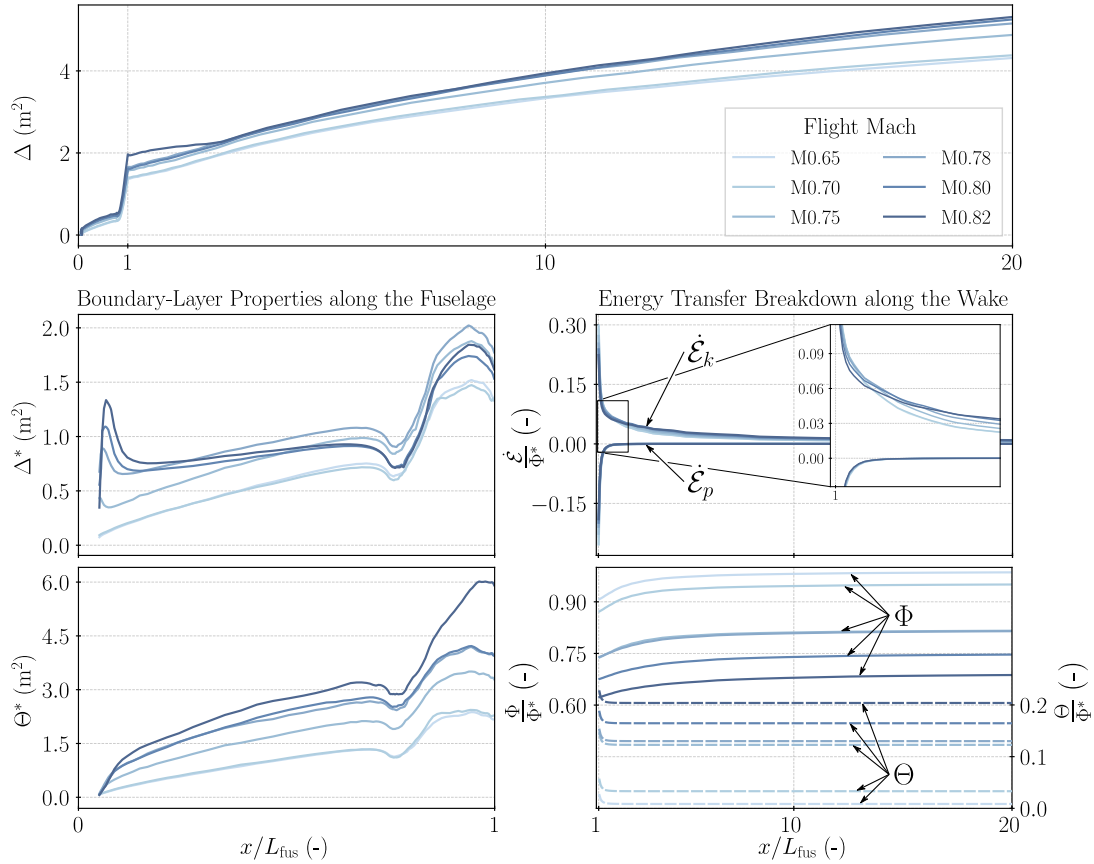


Figure 4.6: 3D axi-symmetric boundary-layer area (top), displacement area and kinetic energy area (left), and wake energy-flux transfers (right) for Mach numbers between 0.65 and 0.82 on the hemispherical nose approximation

Fig. 4.6, at M0.75 the flow experiences a thicker BL from the aircraft shoulder suggesting a higher amount of energy imparted, not necessarily recoverable. The growth along the fuselage does not recover and suggests higher potentials from the momentum and energy defects. Above those conditions, a shock develops at the shoulder which eventually impacts its flow downstream and intensifies with respect to flight conditions. Nevertheless, this additional defect generated does not echo with a higher aerodynamic behaviour as can be explained by the kinetic energy deposition rate downstream of the body and its transfer into viscous dissipation rate. Not only the dissipation rate increases irrecoverably but the volumetric pressure work rises as the shocks and compressibility effects become more important. In practice, the overall power increase and the portion of pressure-defect work takes over the viscous dissipation rate due to the stronger shock.

4.2.3 Influence of the Tail Length and Morphology

4.2.3.1 Rationale

Previous studies assessed the influence of the flight conditions on the energy recovery [21, 22, 27]. More favourable effects are observed with the chord Reynolds number, body Mach number, angle of attack and body fineness ratio. For instance, Airbus Beluga-like aircraft suggest larger potential for energy recovery than slender bodies like the Airbus A380 and A320 families [27], but slender bodies retain quasi zero-pressure gradient dissipative characteristics over their surfaces and allow their approximations with flat plate theory. Despite investigations on the fuselage slenderness ratio, the influence of the tail fineness ratio on the BL properties and potential energy recuperation has not been studied, and important considerations must be taken on the *quality* of the incoming airflow. Lamprakis et al. show the effects an aft-mounted propeller has on the near-field forces [21] but it is pre-empted that they differ when integrating a BLI propulsor, due to the presence of the nacelle. Therefore, the upstream flow needs to be further analysed as it is expected to highly impact the performance of the fan (with distortion and energy harvesting). On conventional aircraft, the rear-end of the aircraft is assimilated as a non-axisymmetric frustum (to prevent tail strike) whose trailing edge serves as an exhaust to the auxiliary power unit (APU). However, for the sake of simplification in this work and to focus on BLI aerodynamics, the requirements of tail-strike up-sweep and APU have been omitted, and approximated with an axi-symmetric cone.

4.2.3.2 Morphing Sequence

First, different tail fineness ratios are defined with the conic tail whose mid-body fuselage length is extended to maintain the aircraft's chord length. Whilst a retrofit integration of the BLI propulsor around the conic tail can be performed [28], more tightly integrated propulsion systems necessitate a morphing of the tail to axially direct the flow through the fan. In doing so, some of the integration effects can be quantified by means of comparison with a bare morphed fuselage. This is a necessary stepping stone to decompose the installation effects, as to whether they are attributed to the morphing or nacelle inclusion.

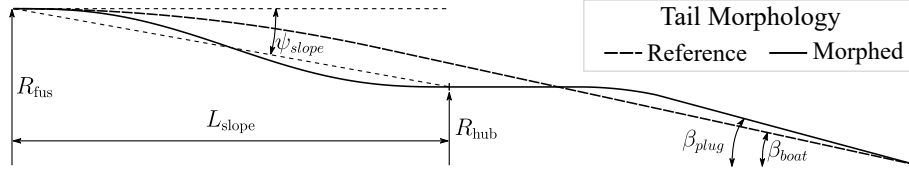


Figure 4.7: Tail morphing sequence from its reference to its morphed shape, from Chapter 3

To that end, the conical tail is used as reference whereby a serpentine substitutes the initial arc leading to the cone. This serpentine offers a natural extended pre-diffusion of the flow to better direct the flow towards the fan. Ensuring adequate spacing for the propulsor's nacelle integration, the plug-cone features a shorter length with a steeper slope. An illustrative example is given in Fig. 4.7 following the morphing described.

Following the morphing sequence of the tail, Fig. 4.8 depicts a set of four conic tail designs (solid curves) and a morphed tail (dashed curve). The tail fineness ratio, FR_{tail} , is swept for a constant fuselage radius where the different tail lengths are reported from the shortest L1 to the longest L4. The closest approximation to the reference aircraft's frustum is given by L2 which morphed tail equivalent is reported as L2' in the following.

4.2.3.3 Figures of Merit

In scenarios where no external force is given by either $\mathcal{P}_S$ or $\mathcal{P}_f$, the fuselage drag power is deposited into the flow and transferred in different power forms as given in Eq. (4.4).

$$-\mathcal{F}_{fus} \cdot \mathbf{V}_B = \dot{\mathcal{E}}_m + \Phi + \Theta \quad (4.4)$$

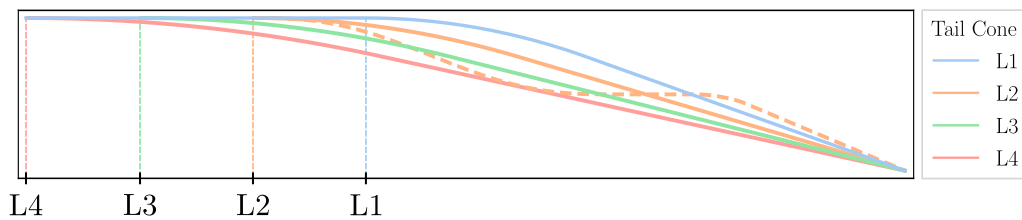


Figure 4.8: 2D axi-symmetric modelling of conic tail-cones of different lengths, and a morphed tail (dashed) derived from L2

However, part of the mechanical energy-rate present in the boundary layer is not entirely exploitable. To segment the recoverable part, the velocity at any point of the flow-field is decomposed into its isentropic and non-isentropic components [29]. Therefore, applying the decomposition in Eq. (4.5), $\dot{\mathcal{E}}_m^*$ denotes the isentropic portion and $\overline{\dot{\mathcal{E}}_m}$ the non-isentropic portion of the mechanical energy.

$$\dot{\mathcal{E}}_m = \dot{\mathcal{E}}_m^* + \overline{\dot{\mathcal{E}}_m} \quad (4.5)$$

Introduced by Sanders and Laskaridis, the potential for energy recovery (PER) is used as a means of describing the portion of the boundary layer flow rich in non-dissipated energy potentially extractable by a mechanical device. Initially defined in Ref. [4] with the viscous dissipation rate at the trailing edge of the bare fuselage Φ_{TE} (Eq. (4.6a)), the definition is later corrected in Ref. [21] to account for the irreversible compressibility-induced losses Θ_∞ (Eq. (4.6b)). These two definitions are volumetric integrals and do not enable the accumulation of PER to be tracked along the surface. In Ref. [22], the accumulation of the non recoverable energy $\overline{\dot{\mathcal{E}}_m}$ relative to the overall energy-rate deposited by the fuselage can be quantified at any given survey plane along a surface or in the wake (Eq. (4.6c)). Using the velocity decomposition method, it is now possible to apply the strict definition whereby the irreversible compressibility-induced losses $\Theta_{irr,TE}$ are not approximated with their residual value (Eq. (4.6d)). In essence, Θ is split in its reversible and irreversible components from a power balance re-arrangement and the reversible portion considered in the potential recoverable energy.

$$\text{PER}_{\text{Sanders}} = 1 - \frac{\Phi_{TE}}{\mathcal{F}_B V_B} \quad (4.6a)$$

$$\text{PER}_{\text{Lamprakis}} = 1 - \frac{\Phi_{TE} + \Theta_\infty}{\mathcal{F}_B V_B} \quad (4.6b)$$

$$\text{PER}_{\text{Mutangara}} = \frac{\overline{\dot{\mathcal{E}}_m}}{\mathcal{F}_B V_B} \quad (4.6c)$$

$$\text{PER} = 1 - \frac{(\Phi + \Theta_{irr})_{TE}}{\mathcal{F}_B V_B} \quad (4.6d)$$

4.2.3.4 Aerodynamic Sensitivity

To characterise the flow sensitivity to changes in the plug-cone shape, Fig. 4.9 depicts the BL area and its kinetic energy area along with $PER_{Mutangara}$ for all conic tails and its morphed derivative. It appears that the longer the fuselage mid-section (i.e. the shorter the tail), the slightly thicker the BL and its excess properties along the mid-body as the wetted area increases and more energy is deposited into the flow. These discrepancies are nonetheless quite negligible at the trailing edge but play an important role as the flow experiences different pressure gradients around the tail.

First, as the boundary layer flow approaches the tail (represented by the vertical dashed lines for the different tails), it is dragged down to follow the body's curvature and locally accelerates before diffusing and decelerating along the slope as it experiences an adverse pressure gradient. For instance, L1 experiences a steeper boat angle than L4 causing a sharper rise in velocity around the convex

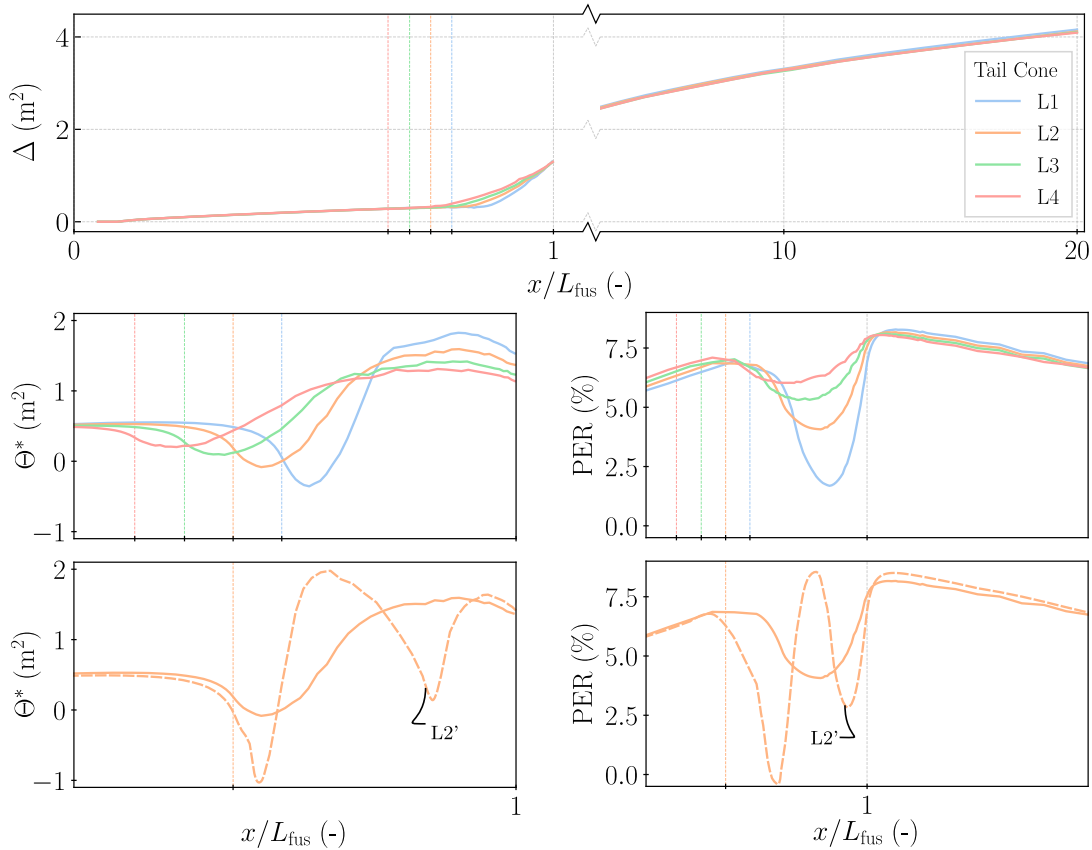


Figure 4.9: 3D axis-symmetric boundary-layer area (top), kinetic energy area (left) and potential for energy recovery from Eq. (4.6c) (right) for the four conic tails of different lengths and a morphed derivative at FL350 and $M_B 0.82$

fuselage region therefore contributing towards a local defect in energy as the velocity at any point within the boundary layer is larger than its edge quantity.

The comparison of the conic tail L2 with its morphed derivative L2' follows in the last row of Fig. 4.9 whereby an important drop in BL properties is experienced before a sharp bounce back. This phenomenon is justified by the steeper change in geometry curvature which then plateaus – to later integrate the propulsor, and then a replica of the precedent curvature at the plug-cone. This double phenomenon in BL properties results from two strong favourable pressure gradients intertwined by adverse gradients which last phenomenon is smaller than the previous as the flow pre-diffused. It is opined that the consideration of either cone shape has an impact on the BL properties at any location along the tail and results in a benefit in excess mechanical energy that can be harvested.

Tracking the accumulation of the non recoverable energy relative to the overall energy-rate deposited by the fuselage, PER can be quantified at any given survey plane along a surface or in the wake. Therefore, an astute positioning of a retrofit BLI propulsor around the conic tail would benefit from the maximum excess and could potentially offer a higher efficiency.

Despite allowing one to track the accumulation of energy deposition rate that can be harvested mechanically, and being a contribution to this paper, the progress of PER along the body is subject to several factors. Its comparison with the definitions from previous works [4, 21], given in Fig. 4.10, yields significant discrepancies. Notably, larger differences for L1 than for L4 are explained by shorter and steeper boat tails which cause adverse pressure gradients and affect the reported accumulation of $\overline{\dot{\mathcal{E}}_m}$ as seen with Fig. 4.9. These pressure gradients are responsible for large deviations given in Θ and predominantly in its reversible component Θ_{rev} . This sharp rise in Θ is the result of a larger isentropic compression and dilation process captured by the other definitions reported in Fig. 4.10. Additionally, a delay is noted between the kinetic energy area Θ^* and PER. This is caused by the difference in survey plane definitions.

In the former, planes perpendicular to the wall surface are used to the BL edge to determine the BL properties whereas vertical surveys are defined in the decomposition method to sweep over the entire domain height. Lastly, the fineness of the meshes generated alter the decomposition yielding higher numerical dissipation. As the tail length increases, slender aft-bodies experience lower

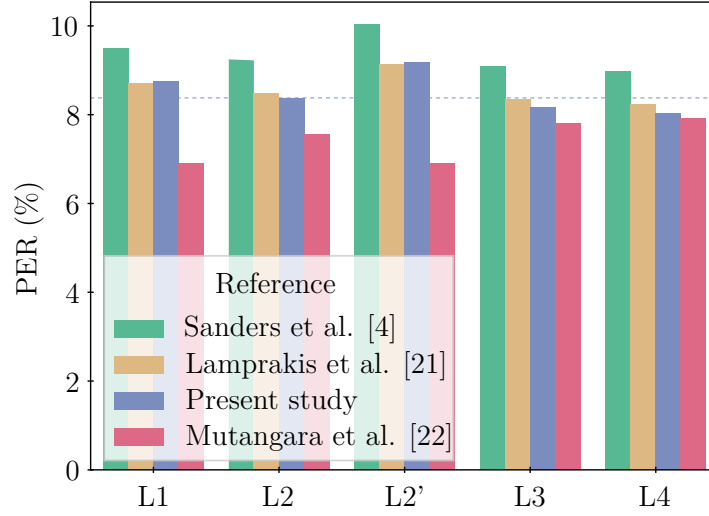


Figure 4.10: Comparison of the potential for energy recovery refinement definitions on the different tails given in Fig. 4.8 at the fuselage trailing edge, with the PER value of the present study (Eq. (4.6d)) on the tail L2 given in a dashed line for ease of comparison

pressure gradients but generate viscous dissipation as a result of shear stresses along the wall surface. Despite ensuring fine meshes to capture the viscous sub-layer, this turbulent region would need very fine grids to decompose well the velocity components. Nonetheless, it is found that the viscous dissipation rate is highly sensitive to the mesh fineness where non-dimensional first cell heights y^+ below one underestimate its value.

Overall, a decreasing trend in PER values is noted with respect to the tail length. The comparison of the conic tail with its morphed derivative yield a larger recovery potential as postulated. The concave diffusive slope imparts more energy to the flow which does not dissipate upstream of the survey plane and reports larger mechanical benefits.

The main difference in PER appears between the conic and morphed geometries where the latter favours pressure-defect work over viscous dissipation (Fig. 4.10). This can be explained by the increased dilation from changes in curvatures and the larger wetted area. Nonetheless, all the energy is not transformed into Θ or Φ leaving a greater aerodynamic potential as given by PER (Fig. 4.11). The difference does not justify a better aerodynamic performance from the morphed tail over its conic reference but yet slightly contributes to higher potentials as the pre-diffusion of the flow is greater. Figure 4.11 depicts that transfer of energy between L2 and L2' whereby slightly smaller diffusive effects are observed for the morphed tail region. It appears however clear that at the fuselage trailing edge, the non recoverable energy

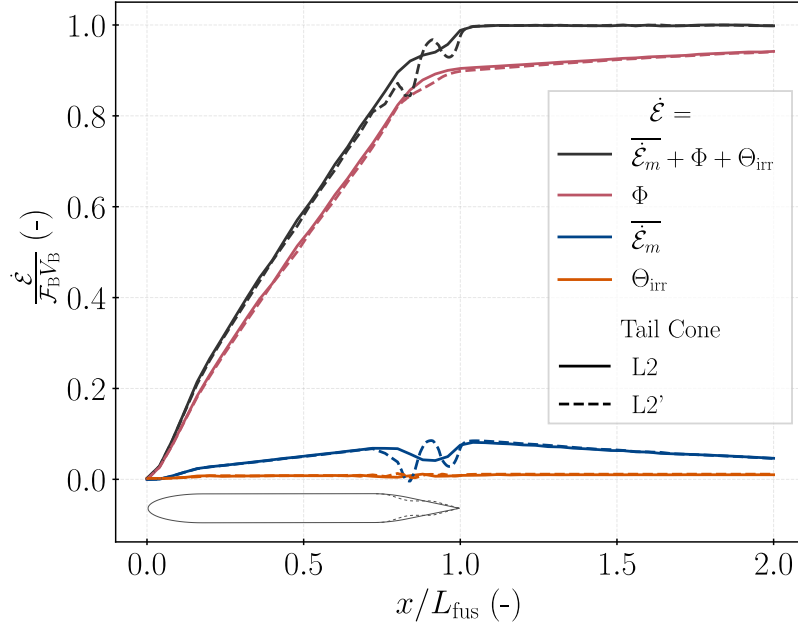


Figure 4.11: Energy decomposition and their evolution along the fuselage surface and in the mid-field wake

$\overline{\dot{\mathcal{E}}_m}$ is slightly underpredicted, as previously mentioned, and therefore negatively impacts $PER_{Mutangara}$ value, which can still be used as a conservative approach. With the integration of a propulsor which fan could be placed around 50-60% of the tail length (Fig. 4.9), ingesting the bottom layer of the BL rich in energy would reduce the fan power requirement to operate as well as reduce the integration penalties, e.g. over-cowl viscous dissipation. However, as expressed before, the pressure gradients faced by the flow locally affect PER around the mid tail to a larger extent with the conic than the morphed tail. Despite advising to integrate a thruster around the middle of the morphed tail, the morphology plays an important role in the recoverable work and the fan size still needs to be assessed.

4.2.4 Influence of the Morphed Shape

4.2.4.1 Slope Angle Effects

Unlike the previous investigation on the conic tails with their derivation in a morphed candidate, the flight speed is reduced to $M_B = 0.78$ ($Re = 2.73 \cdot 10^8$) for the rest of the paper to agree with the cruise mission of the PFC and determine a morphed baseline at its condition. To understand the influence of the shaping, morphed fuselages are designed with different combinations of slope lengths and angles. First,

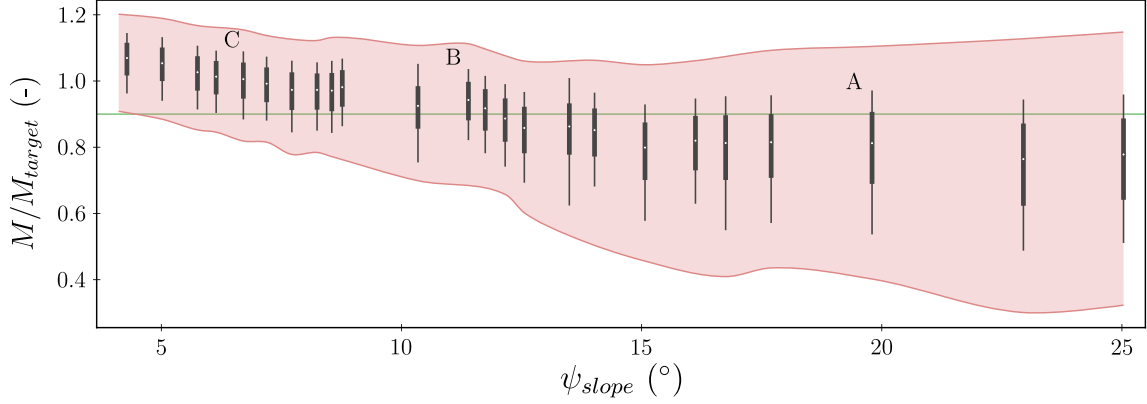


Figure 4.12: Distribution of mass-flow averaged Mach numbers at the slopes' end-points with respect to different inclination angles, with 3 identified designs for later use, relative to a threshold of 90% of the Mach number target

the pre-diffusive slope is investigated as a non-dominated design variable to direct the flow before entering the propulsor. As such, the slope angle is defined as $\psi_{slope} = L_{slope}/(R_{fus} - R_{hub})$ between the tail starting point at the fuselage radius and the hub radius of the envisaged propulsor throat position, marking the end of the slope.

Each fictitious position of the propulsor's throat is discretised in 40 survey planes equi-spaced from the fuselage wall to the boundary layer edge. The mass-flow averaged Mach number and mass-flow rate are computed on each survey. Figure 4.12 reports the Mach number relative to the fan-face target to ensure its operation with respect to all morphed tail slope angles ψ_{slope} to depict the influence of the pre-diffusive slope. The cloud bounds the Mach number extrema for target ingestions of 20 to 100% of the boundary layer mass-flow, following a kernel distribution. Inside it, the white scatter quantifies the mean Mach number of this design, and the box plot covers the 25th to 75th quartiles which extend with matches representing the extrema (not including outliers). Additionally, a filter is applied on the mass-flow rate integrals to ensure that no reverse flow is captured at the axial position which would enlarge the throat area to capture the target mass-flow rate and increase the load and distortion faced by the fan.

For ψ_{slope} larger than 13° in Fig. 4.12, more than 75% of the calculated Mach numbers on the survey planes fall below the 90% threshold defined. To ensure the condition is met at the fan on these designs, its size should be large enough to ingest enough mass-flow, and its intake should undesirably be designed convergent to locally accelerate the flow generating an undesired larger distortion. Therefore, morphed fuselages which slope angles causing Mach numbers to fall under the threshold as well as Mach

numbers that will lead to only large propulsors are discarded, and their integrated propulsive concepts not designed.

Conversely, for designs leading to Mach numbers close and below the target, the integration effects further diffuse the flow before entering the intake justifying the threshold defined. As for the designs giving a Mach number above the target, additional diffusion is ensured within the propulsor intake to slow down the flow.

4.2.4.2 Plug Angle Constraints

In addition to the constraints on the slope angle, the integration of the propulsor along the tail constrains the plug length (for a given tail length) as well as its angle (with respect to the slope angle). As such, if the change in curvature is too important, the flow separates downstream of the fuselage impacting its aerodynamic performance. Through the integration of the retrofit propulsor around the morphed

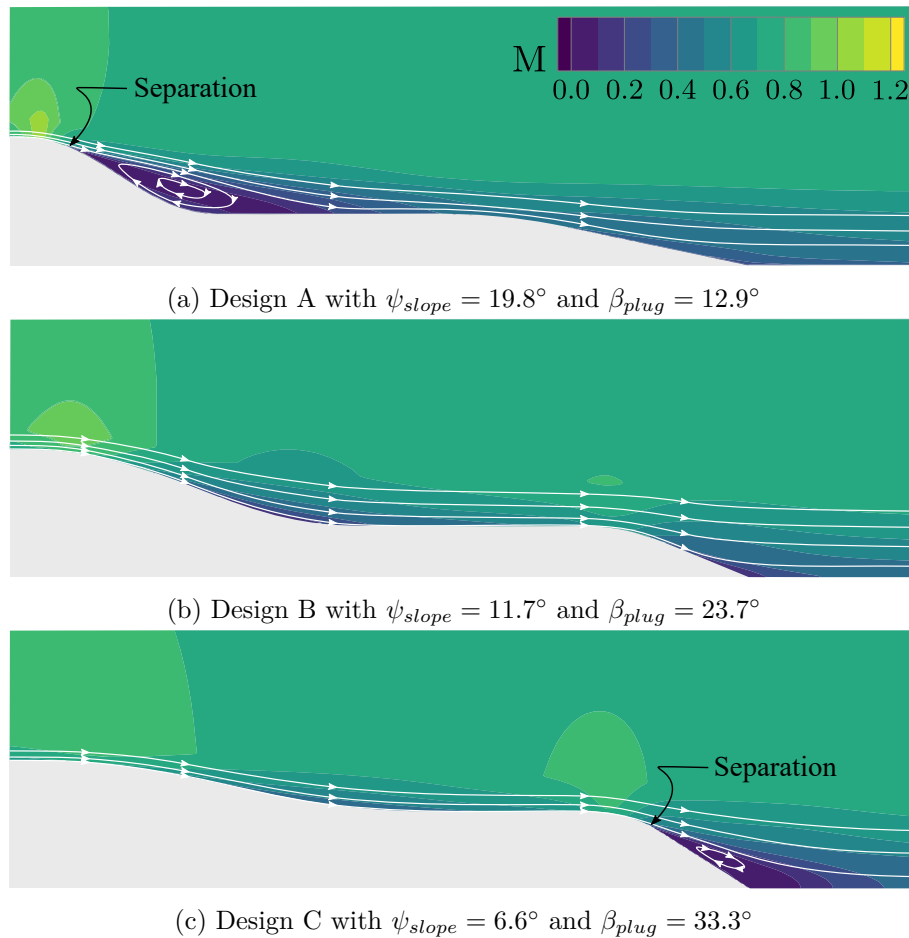


Figure 4.13: Computed Mach number contours around the three designs identified in Fig. 4.12

tail, the flow exiting the exhaust at a higher speed than the bare fuselage will also likely separate. To prevent the generation of these propulsive designs, the pressure and skin-friction coefficients are tracked along the plug surface. The absence of friction on a body's surface suggests a flow separation, and as a by-product a sharp rise in the pressure coefficient. In doing so, the flow-field around the tail's plug is reported in Fig. 4.13 for the three designs previously identified in Fig. 4.12.

As a result of separation on Fig. 4.13a for $\psi_{slope} = 19.8^\circ$, the Mach numbers and mass-flow rates computed for Fig. 4.12 suggested reversed flow on the survey planes which disregarded the designs. Conversely, for Fig. 4.13c with $\psi_{slope} = 6.6^\circ$, a large majority of the flow conditions computed fell above the fan requirements. However, the slope length restrains the plug length (as the tail length is imposed) featuring a steep angle of $\beta_{plug} = 33.3^\circ$ and showcasing separation downstream of the fuselage. Following the fair assumption that separation will remain for powered aircraft, fuselages alike design C are also disregarded. In comparison, when both slope and plug angles fall below their critical angles, flow-field similar to the design B in Fig. 4.13b are obtained and are further explored.

Therefore, from the observations made on the slope and plug surfaces, the design space is bounded by the aggressive angles leading to separation on the morphed fuselages. Retrieving Fig. 4.12, the slope angles are bounded by the critical slope angle and its induced plug angle between 7 and 13° , with steep plugs and steep slopes respectively. These designs are represented in Fig. 4.14 where designs alike A are removed from the bottom-left corner, and alike C from the top-right corner.

4.2.4.3 Slope Length and Hub Radius Effects

Further to the narrowed-down morphed fuselages and from the possibility of tracking PER along the fuselage, the definition from Mutangara et al. [22] is followed. Based

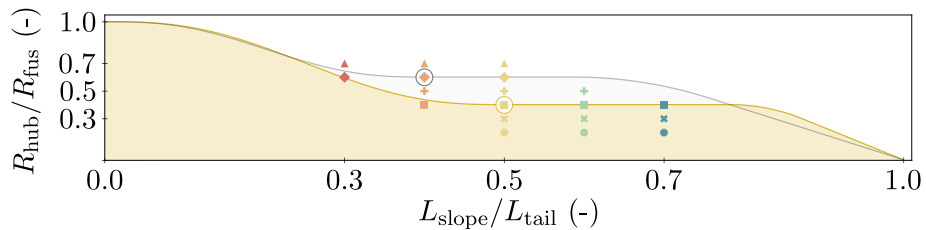


Figure 4.14: Schematic of two morphed fuselages for $L_{slope}/L_{tail} = 0.4$ and 0.5 , and $R_{hub}/R_{fus} = 0.6$ and 0.4 , respectively in grey and yellow with ○

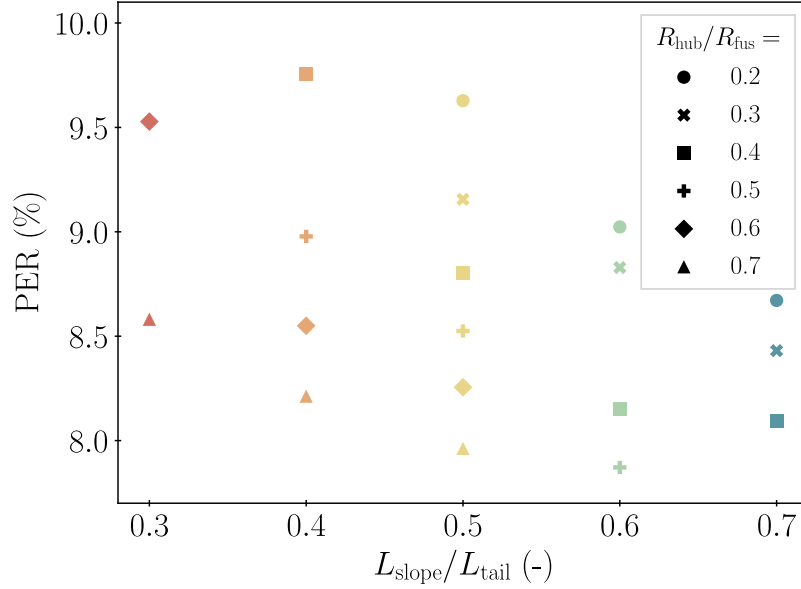


Figure 4.15: Potential for energy recovery with respect to the different slope lengths and hub radii given in Fig. 4.14

on the designs given in Fig. 4.14 with respect to different hub radii and slope lengths (in markers), $\text{PER}_{\text{Mutangara}}$ values are reported in Fig. 4.15. As expressed above, more aggressive slope designs provide additional energy to the viscous flow, locally present in the form of pressure, with low shear stresses hence contributing to higher PER values as viscous dissipation is delayed.

This phenomenon is observed where the slope length is constrained to low values featuring a somewhat sharp ramp. With increasing slope lengths, PER decreases as more streamlined flows experience larger shear stresses responsible for viscous dissipation. For the same reasoning, low hub radii favour PER as the rise in energy deposition rate is not yet converted into irrecoverable losses. In addition, the monotonicity in PER with hub radii is respected regardless of the slope length.

Lastly, the uneven distribution for $L_{\text{slope}}/L_{\text{tail}} = 0.4$ and 0.6 is observed as the lowest hub radius of the former axial location does not experience reverse flow but is still subject to a strong pressure gradient contributing to $\dot{\mathcal{E}}_p$ locally increases the perceived PER. Conversely for the latter location, the opposite is observed for higher radii where the somewhat steep plug angle affects the mechanical energy-rate terms at the survey station and reduce the potential for energy recovery.

4.3 Propulsor Integration: Propulsive Fuselage Concept

4.3.1 Propulsor Parametrisation

From the considerations on the bare fuselages, the propulsors are mounted on the aft-end circumferentially around the morphed region. After discussing the tail morphing with Fig. 4.7, the thruster is defined with a fan and its encapsulating nacelle. Figure 4.16 depicts the propulsive fuselage concept with its macro positioning parameters.

To not violate the constraints associated with the reference fuselage, subsequent to the tail morphing sequence, the new geometry embeds a propelling fan and casing. Similar to existing concepts, this design considers a serpentine pre-inlet outline (5th polynomial Bell-Mehta [30]) in lieu of tapered [6] or smooth S-shaped [7] slopes. As the integration of the BLI propulsor must guarantee a pre-diffusion whilst avoiding any flow recirculation upstream of the nacelle, a Bell-Mehta fifth-order polynomial is used as a versatile and robust implement [30] to direct the inflow. The propulsor parametrisation follows Chapter 3 with a combination of Bell-Mehta polynomials, circular and elliptic arcs and a fore-cowl NACA-1 Series [31].

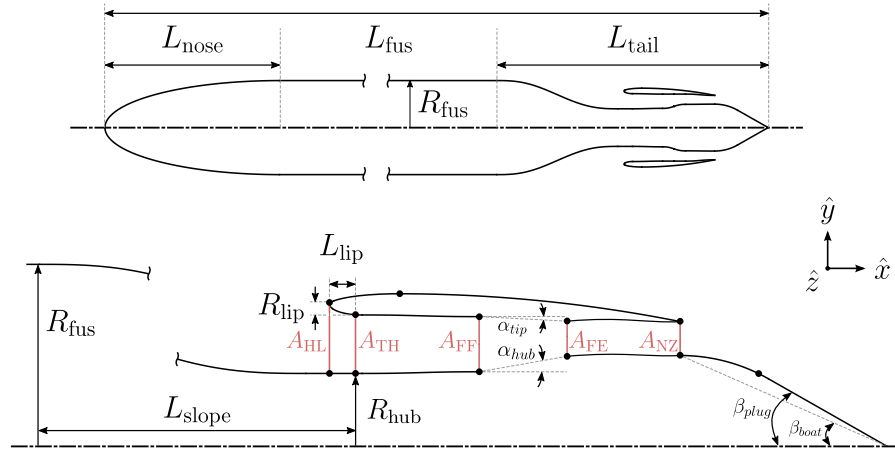


Figure 4.16: Schematic of an axis-symmetric fuselage with a thruster at its tail featuring a pre-diffusive slope, propulsor intake, cowl, exhaust, and aft-cone, adapted from Chapter 3

4.3.2 Integration Effects

To evaluate the influence of the propulsor integration, two propulsors are integrated to ingest only the sub-viscous layer (around 25% of the boundary layer mass-flow) and the entire boundary layer mass-flow and compared with the clean flow over their morphed fuselage. An example of this installation is given in Fig. 4.17 with the clean morphed tail represented with dashes (from Fig. 4.14 in yellow) and a fictitious nacelle. It was identified by Gray et al. a growth of the boundary-layer thickness by 15 and 25% between the clean flow (unintegrated tapered tail) and two propulsors of different sizes [8]. Herein, the two designs in Figs. 4.18a and 4.18b are considered where the boundary layer thickness grows by respectively 4 and 8% (from 77.5% of the fuselage radius to 80.5% and to 83.8%, respectively) at $x/R_{\text{HL}} = 0$, or an increase in 12% and a decrease in 4.3% in mass-flow rate relative to the morphed fuselage, respectively.

Ref. [8] reports the sensitivity of the aerodynamics to FPR for a constant fan power input, where the fan diameter and its pressure ratio (or rotational speed) have an inverse relationship whereby a larger fan rotates more slowly and compresses less the air through its stage. The integration effects upstream are therefore subject to changes in the fan and propulsor sizes rather than only its pressure ratio, as stated in Ref. [8], as the nacelle lip is shifted vertically up or down. The speed reduction along the tapered tail extends one propulsor radius upstream of it whereas in serpentine geometries, Figs. 4.18c and 4.18d suggest that these penalties are dependent on the propulsor size. For the two geometries considered, the aerodynamics is affected one highlight radius upstream of the nacelle ($x/R_{\text{HL}} = 1$) for the small propulsor whereas as the size increases, penalties occur up to two highlight radii upstream ($x/R_{\text{HL}} = 2$). These differences from Ref. [8] are a direct result of the tail morphology which is more subject to local effects. Notably with the large throat, the flow is pushed

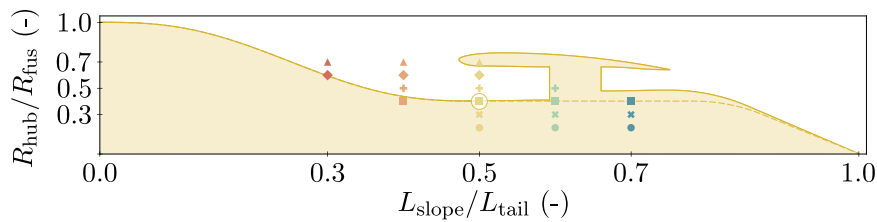


Figure 4.17: Schematic of an integrated propulsor (—) at $L_{\text{slope}}/L_{\text{tail}} = 0.5$ and $R_{\text{hub}}/R_{\text{fus}} = 0.4$ ingesting 60% of the mass-flow present in the boundary layer, as denoted with $\bigcirc$, from its morphed fuselage (---) in Fig. 4.14

4. Considerations in the Design and Performance Assessment

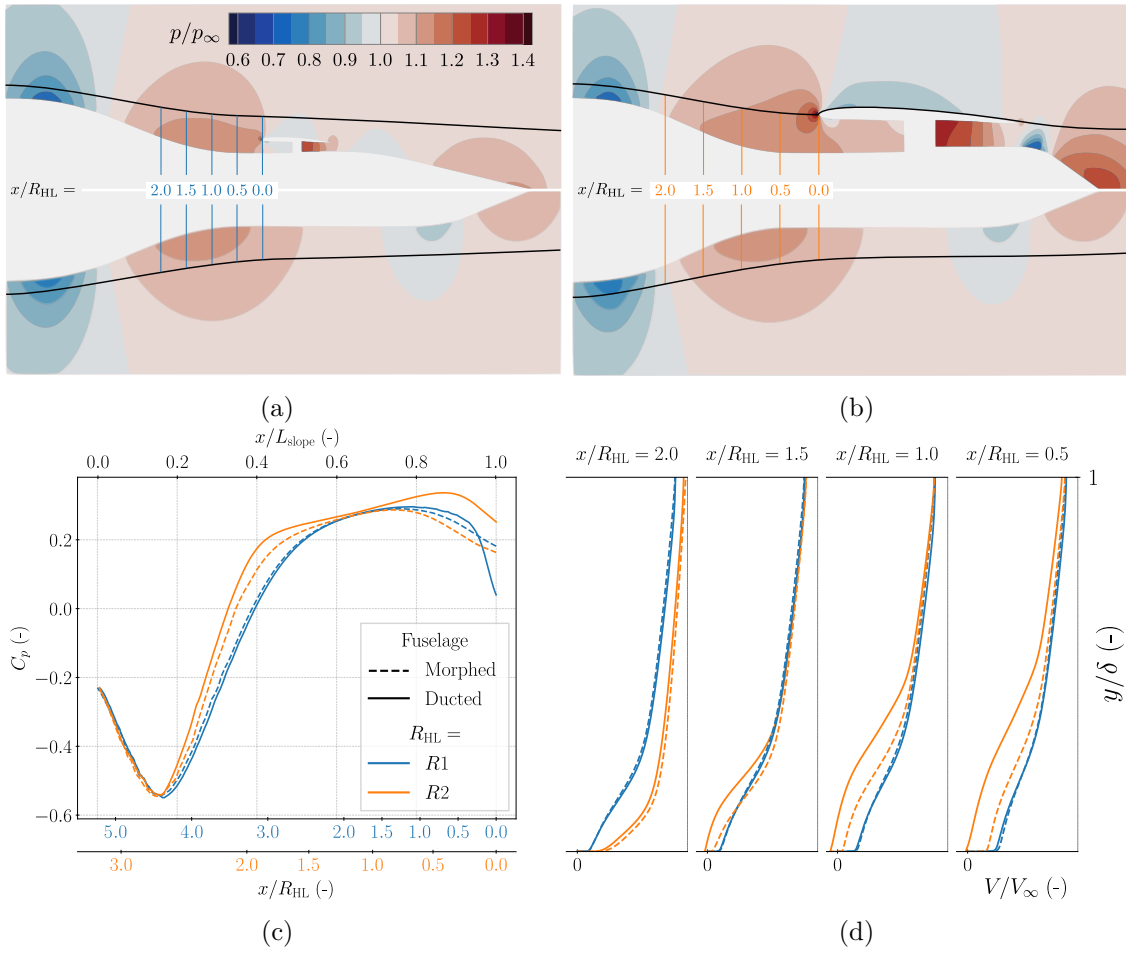


Figure 4.18: Comparison of normal static pressure (a) for a propulsor ingesting only the sub-viscous layer (—) and (b) for a propulsor ingesting the entire boundary layer (—), and comparison of (c) pressure coefficients and (d) velocity profiles along the fuselage slopes between the morphed and the ducted geometries at the same position (Fig. 4.17) from the propulsor highlight axial position. Integration effects upstream are reported as fractions of the propulsors highlight radii.

further forward up to half the boundary layer thickness as a result of the adverse pressure gradient from the nacelle lip faced (Figs. 4.18a and 4.18b), and these effects reduce in height the further away from the highlight, reversing the flow very close to the wall surface in some cases (Figs. 4.18c and 4.18d).

Overall, the observations made on the local integration effects upstream of the propulsor agree with those from Gray et al. despite their tapered tail geometry and operating at a lower vehicle speed ($M_B = 0.72$):

- the boundary layer thickness grows with the integration of a propulsor;
- this growth further increases with respect to the propulsor diameter;
- the lower half of the boundary layer experiences a speed reduction up to 2 highlight radii upstream of the nacelle;

- this speed reduction is correlated with the propulsor diameter;
- the local pressure gradients caused by the propulsor integration propagate farther upstream than the changes in the velocity profiles.

4.3.3 Aero-propulsive Performance Metrics

In the PFC, a turbo-electric architecture is selected. In this configuration, the main turbofans mounted under the wings generate part of the aircraft thrust requirement whilst power is extracted from the low-pressure turbines to feed the aft-mounted thruster. Therefore, to evaluate the aero-propulsive performance of all candidates, the overall power consumption needs to be calculated and compared against a reference. To maintain the same steady-level cruise mission than the reference aircraft, the fuel-flow rate is used as performance metric. In that respect, the investigations are only performed at design point where the power saving coefficient (PSC), a surrogate of fuel savings, is reported. Additionally, the share of propulsive force between the thruster and main turbofans is given by the propulsive force share (PFS) as the ratio of the force produced by the aft-mounted fan relative to the overall aircraft requirement.

4.3.3.1 Power Consumption and Savings

Even though under-wing turbofans should be designed to provide power to the aft-mounted propulsor and generate the thrust demand at on-design, this work uses a retrofit state-of-the-art turbofan, designed by Chalmers University of Technology (private communication), comparable to the CFM LEAP-1 or PW1100G. Therefore, all PSC values reported under-estimate the real savings the novel aircraft might achieve as its main turbofans should be down-scaled as less thrust is required from them. Therefore, there would be additional savings on drag and weight (albeit the latter is not considered here). Nevertheless, this approach allows one to compare all propulsor designs generated in a similar manner by only assessing the benefits from the thruster and not accounted for the further aero-propulsive benefits from the re-scaling.

To that end, the shaft power $\mathcal{P}_S$ from the power balance in Eq. (4.2) describing the aft-mounted fan power demand is given as $\mathcal{P}_{\text{BLI}}$ to differentiate it from the main turbofans:

$$\mathcal{P}_{\text{BLI}} = \iint_{\mathcal{S}_p} \left[\rho \left(e + \frac{\mathbf{V}^2}{2} \right) \mathbf{V} + p \mathbf{V} - \bar{\tau} \cdot \mathbf{V} \right] \cdot \hat{n} \, d\mathcal{S} \quad (4.7)$$

In the turbo-electric aircraft architecture considered, this energy is an electrical power demand which needs to be first converted from mechanical energy, as extracted from the low-pressure turbine (LPT) of the under-wing turbofans. However, this modelling is out of the scope of this paper and rather, a transmission efficiency is introduced as η_{trans} and can be related to the actual power extracted from the low-pressure shaft:

$$\mathcal{P}_{\text{BLI,PFC}} = \frac{\mathcal{P}_{\text{BLI}}}{\eta_{trans}} \quad (4.8)$$

Therefore, the overall power consumption $\mathcal{P}_{\text{PFC}}$ from the engines is given in Eq. (4.9) whereby $\mathcal{P}_{\text{pod,PFC}}$ accounts for two turbofans mounted on either sides.

$$\mathcal{P}_{\text{PFC}} = \mathcal{P}_{\text{pod,PFC}} + \mathcal{P}_{\text{BLI,PFC}} \quad (4.9)$$

Relative to the reference aircraft that only produces the required forward force, the power saving coefficient is defined with:

$$\text{PSC} = 1 - \frac{\mathcal{P}_{\text{PFC}}}{\mathcal{P}_{\text{A/C,ref}}} \triangleq 1 - \frac{\dot{m}_{f,\text{PFC}}}{\dot{m}_{f,\text{A/C,ref}}} = \text{FSC} \quad (4.10)$$

which is reducible to the right-hand side formulation with $\dot{m}_f$, the fuel-flow rate, assuming that the same fuel properties are considered in both aircraft.

In this work, the latter formulation is used based on engine decks for combinations of power off-takes and force generation. To that end, the fuel saving coefficient (FSC) is used in lieu of the PSC but represents the same metric.

4.3.3.2 Propulsive Force Share

In the propulsive fuselage concept, the propulsive force is split in between the under-wing engines and the aft-mounted thruster. The portion of force generated at the back relative to the main turbofans is given by the propulsive force share (PFS):

$$\text{PFS} = \frac{\mathcal{F}_{\text{BLL,PFC}}}{\mathcal{F}_{\text{PFC}}} \quad (4.11)$$

This non-dimensional form shows its importance when one designs different aircraft and evaluates the change in propulsive force under equivalent power supply, or inversely, the change in power requirement to maintain the same force. The overall force requirement from the PFC ($\mathcal{F}_{\text{PFC}}$) is estimated from a drag build-up isolating the propulsive fuselage from the rest of the aircraft (Chapter 3). All forces $\mathcal{F}$ are computed from the near-field integration of pressure and friction around all wall surfaces:

$$\mathcal{F} = \iint_S (p - p_\infty) \hat{n} - \bar{\tau} \cdot \hat{n} \, dS \quad (4.12)$$

To obtain the direct contribution of the propulsive fuselage, the forces $\mathcal{F}_{\text{fus+P,PFC}}$ on the propulsive fuselage are compared against the morphed clean fuselage $\mathcal{F}_{\text{fus,PFC}}$ previously used to define the integration effects:

$$\mathcal{F}_{\text{BLL,PFC}} = \mathcal{F}_{\text{fus+P,PFC}} - \mathcal{F}_{\text{fus,PFC}} \quad (4.13)$$

4.4 Novel Aircraft Aero-propulsive Performance Evaluation

From the recommendations on the possible location installations, different propulsor sizes, shapes and fan operating conditions are explored. In doing so, it is necessary to define how the aero-propulsive performance is evaluated.

4.4.1 Propulsor Position Effects on Aero-propulsive Performance

Following the observations and recommendations made on the clean morphed fuselage, the macro parameters (slope length, hub radius, throat area and fan pressure ratios) are explored in a systematic approach. Following the nomenclature from Fig. 4.17, Fig. 4.19 depicts the global pareto between the propulsive force and

the power savings for the different slope lengths $L_{\text{slope}}/L_{\text{tail}}$ and hub radii $R_{\text{hub}}/R_{\text{fus}}$ with respect to different propulsor sizes and fan operating conditions. It is reminded that the fuel savings are calculated between the novel aircraft featuring retrofit turbofans (operating at off-design) and the reference aircraft. Therefore, all savings reported are under-estimated in terms of the propulsor resizing, but probably overestimated from other aspects, like weight estimations, transmission efficiency, and 3D aerodynamics of the flow due to tail-cone up-sweep.

With respect to the positioning of the propulsor along the tail, two main categories are observed. First, for tails with short slopes, the best performance is obtained for large hub radii. This confirms the observations made on the slope angle whereby, when constrained in length, the slope's end-point radius must be sufficiently high to prevent flow recirculation upstream and provide an homogeneous flow distribution at the aerodynamic interface plane (AIP) for a target Mach number. With these designs, a long streamlined plug-cone prevents separation and a recirculation bubble behind the fuselage. Conversely, for long slopes, a low hub radius offers a natural flow pre-diffusion whilst guaranteeing a relatively low plug angle for better force distribution, i.e. forward force production. Whilst the mid-length of the tail regroups all propulsors in the 4 to 10% FSC and 10 to 60%

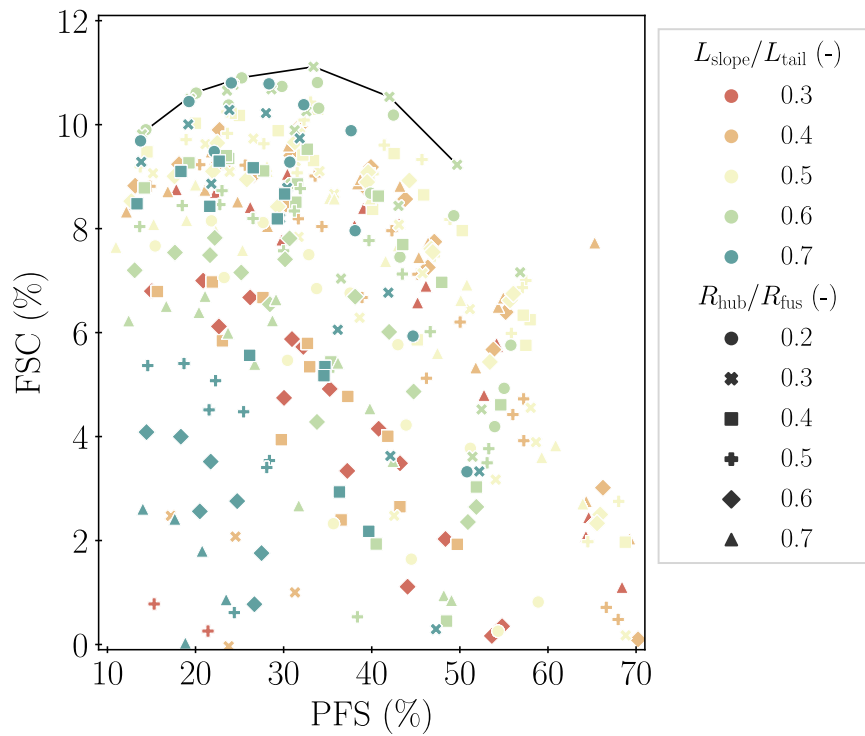


Figure 4.19: Overall performance of a propulsive fuselage concept for various BLI propulsor positions along the aircraft tail, with symbols following Fig. 4.17 convention

force share, a larger force contribution from the thruster reduces the savings. Steeper plug angles contribute to larger force generation with propulsors ingesting more mass-flow but require larger power demands yielding less BLI benefits.

To depict the best performing candidates of each marker combinations, Fig. 4.20 maps out the best PFS and FSC at each propulsor location. For the given combination of slope lengths and hub radii, the different propulsor sizes and fan operating conditions are compared to down-select the best fuel savings. As suggested, optima in savings suggest the PFS to be bounded between 15 and 35% of the aircraft thrust requirement to maintain its steady cruise segment.

The largest force shares are split into two locations also yielding large savings:

- at high hub radial positions for short slope lengths (≈ 0.4) but these bulky designs would increase the aircraft mass whilst potentially favouring flow separation upstream of the nacelle at high angle of attacks;
- at a mid-to-low hub radial position for mid-range slope lengths of $\approx 0.5/0.6$ where milder weight penalties are envisaged and pre-separation prevented.

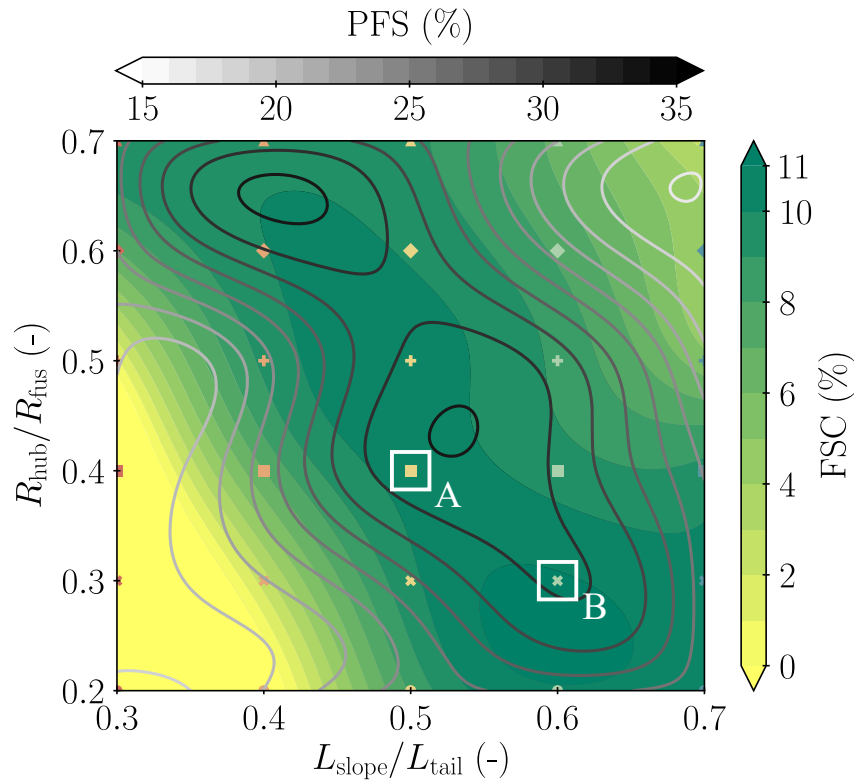


Figure 4.20: Computed contour map of a propulsive fuselage concept overall performance for various BLI propulsor positions along the aircraft tail

Nonetheless, by zooming in this latter region, the propulsive forces generation require different intensities of power to run the fan. In that respect, the highest savings are obtained at slope lengths around 60% of the tail length for low hub radii as the sub-viscous boundary layer is rich in energy. Also, there are fairly similar savings for a broad PFS and it would probably be better to operate on the lower PFS range, because this would incur less penalties later on when weight of the system and transmission efficiency are considered.

4.4.2 Influence of Propulsor Size and Operation

Retrieving the two combinations of PFS and FSC identified as A and B from Fig. 4.20, Fig. 4.21 gathers the aero-propulsive performance differences at $L_{\text{slope}}/L_{\text{tail}} = 0.5$ and $R_{\text{hub}}/R_{\text{fus}} = 0.4$, and at $L_{\text{slope}}/L_{\text{tail}} = 0.6$ and $R_{\text{hub}}/R_{\text{fus}} = 0.3$, for different mass-flow rates ingested relative to the entire boundary layer, BLR, and the fan pressure ratio, FPR.

Notably, an inverse relationship between PFS and FSC is observed whereby lower combinations of BLR and FPR provide important savings ($\approx 10\%$) for maximum a third of the overall aircraft's force production. This intuitive result translates the BLI beneficial effects whereby a great amount of energy is harvested from the fan to which small power input is provided to mitigate the wake losses whilst generating a forward force. With increases in BLR and FPR variables, larger forces in the forward direction are achieved but to the cost of the power savings. Similar patterns are

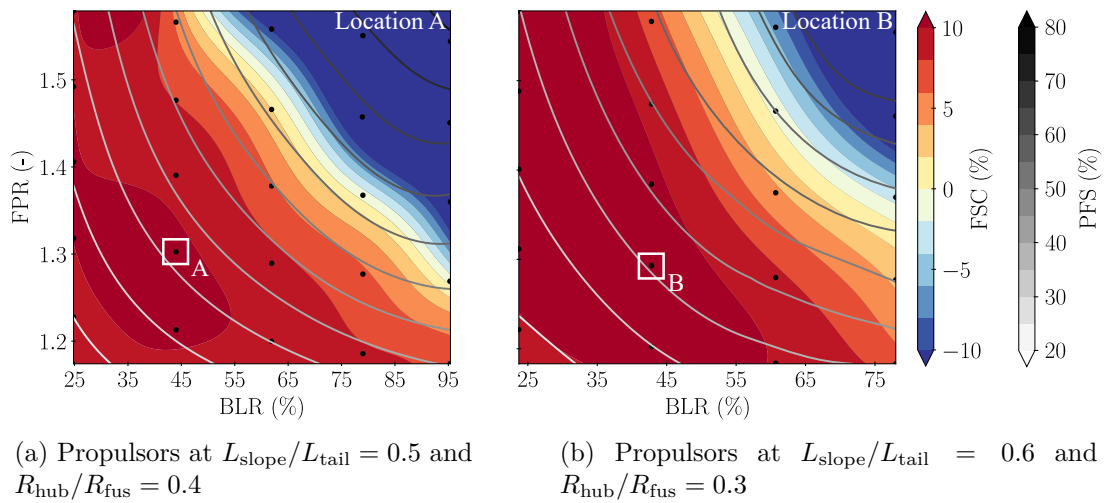


Figure 4.21: Computed contour maps of a propulsive fuselage concept overall performance for various BLI propulsor operating conditions at two different positions along the tail

obtained at both locations whereby geometric constraints on the plug-cone prevent high BLR propulsors in Fig. 4.21b. At constant FPR, the ingestion of larger mass-flow rates across the fan returns less benefits as the energy present in the flow asymptotes to zero in the absolute reference frame. Therefore, to maintain the pressure rise across the fan stage, greater power consumption is required yielding diminishing returns. Larger FPR's provide a compound effect on the force share whereby its gradient is greater than at low FPR for larger fan diameters.

Conversely, for a given fan design, an increase in its rotational speed increases the pressure rise with greater power inputs. Smaller propulsors inside the fuselage tail feature low-diameter fans harvesting all benefits from the sub-layer whilst producing a mild amount of propulsive force. With larger diameters, less benefits are harvested and larger penalties are imparted from the large wetted area of the nacelle cowl. However, larger portions of forward force are enabled. Therefore, it is of interest to investigate the potentials benefits offered by compact nacelles even if it is pre-empted to increase the distortion levels at the fan-face despite potentially be balanced out by smaller aerodynamic penalties around the nacelle.

4.4.3 Benefits of Compact Nacelles

4.4.3.1 Intake Length Reduction

Carrying on from the slope analyses to ensure an adequate pre-diffusion, it is observed from the velocity profiles larger distortion than experienced with a clean free-stream flow ingested by a turbofan mounted under the wings [20]. Despite the larger distortion, the pre-diffusion ensures an homogeneous flow distribution to the throat on its circumference (from the axi-symmetry considered herein) and only the radial distribution (span-wise) is investigated. In events where the pre-diffusion is not sufficient for the flow to reach the target Mach number at the fan-face, further diffusion is obtained from the intake design. With the consideration of compact nacelles around the propulsors, the total pressure distribution and radial distortion index are studied as complex flow diffusion from the intake and acceleration from the fan are combined.

To that end, from the baseline design accounting for one throat diameter for the intake length, other designs are derived considering 0.7 and 0.4 throat diameters. Both propulsor designs from Fig. 4.18 are investigated whereby 25% and 100% of the

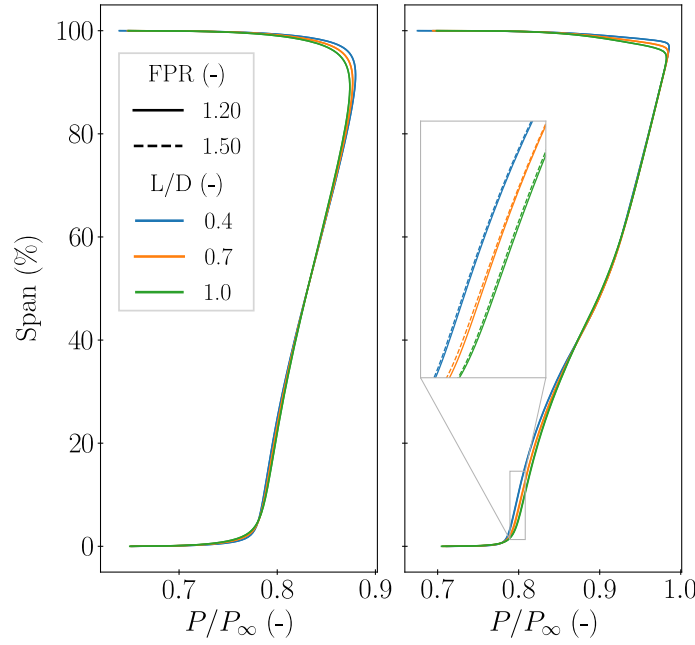


Figure 4.22: Normalised distribution of the total pressure along the fan span for the small propulsor *R1* (left) and large propulsor *R2* (right) under two fan pressure ratio and three intake lengths

boundary layer mass-flow are ingested. In effect, this represents a reduction of the length over diameter ratio (L/D) from 1 to 0.7 and 0.4 respectively. In comparison, state-of-the-art under-wing engines' intakes range between 0.65 and 0.85 for SMR aircraft [32] whereas short intakes are designed around 0.5 [33].

Figure 4.22 depicts the distribution of the total pressure at the fan-face normalised by the free-stream total pressure for two propulsor sizes under the different intake L/D and fan pressure ratios. Similarly for both propulsor throat sizes, an increase in the intake compactness sees the AIP pushed further forward (i.e. closer to the throat) increasing the total pressure distribution close to the nacelle shroud. Notably on the larger propulsor, suction effects in the lower region caused by larger fan pressure ratios reduce the total pressure distribution as the flow is slightly accelerated. Closer to the tip, the same phenomena are observed on all propulsors whereby larger diffusion occurs for longer intakes but the boundary layer forming on the interior of the nacelle reduces the total pressure distribution.

As regards the radial distortion index, RDI [34], values are reported in Table 4.1. For the different propulsors considered, increases between 10 and 14% in RDI are observed when the intake L/D 's are reduced from 1.0 to 0.4. Although these increase are non-negligible and at the fan designers discretion [35, 36], in this study the fan is modelled with ideal boundary conditions and therefore not transporting the

Table 4.1: Radial distortion indices for two propulsor's intake dimensions and two fan pressure ratios

R_{HL}	FPR = 1.20			FPR = 1.50		
	L/D = 1.0	L/D = 0.4	$\Delta(\%)$	L/D = 1.0	L/D = 0.4	$\Delta(\%)$
$R1$	0.0399	0.0455	+14.0	0.0401	0.0456	+13.7
$R2$	0.0865	0.0954	+10.3	0.0869	0.0957	+10.1

experience distortion through the fan stage which would be expected to increase the viscous dissipative losses. As degrees of fidelity are increased for the fan modelling, one should not omit the distortion effects and reduce the compactness of the intake to mitigate them.

4.4.3.2 Plug-cone Extension

From the geometries studied above and their intake length reductions, the nacelle lengths are reduced by the same amount to maintain an equivalent exhaust than the baseline and elongate the plug, reducing its angle. For small propulsors, the reduction in nacelle length does not impact considerably the plug shape and its effects on the surrounding flow-field. Conversely, with larger propulsors, the elongation offered in the plug-cone prevents abrupt changes with smaller plug angles and favours the force distribution. Notably, Fig. 4.23 depicts the large propulsor operating at two fan pressure ratios for the baseline and compact nacelles.

Shrinking the propulsor's nacelle enables the plug-cone length to be extended and its angle to be reduced from 35.8 to 25°. The reduction in angle yields a smaller

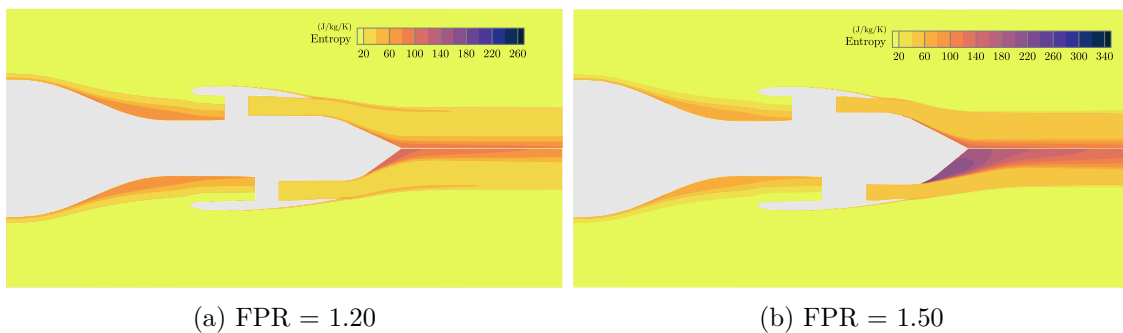


Figure 4.23: Contours of entropy around large propulsors ($R2$) ingesting the entire boundary layer under fan pressure ratios of (a) 1.20 and (b) 1.50 for two intake/nacelle/plug-cone length combinations

entropy generation behind the cone but not necessarily a better force redistribution on the plug arc and cone. As the jet velocity increases with FPR, the flow is more unlikely to remain attached to the wall surface and reducing the plug angle positively contributes to the net axial force. Notably, at low FPR, the reduction in plug angle from 35.8 to 25° favours the force redistribution along the plug but its benefit is outbalanced by the local distribution within the exhaust. As the FPR increases, the benefits offered by a smoother plug arc contribute to beneficial forward forces on the rear-ward facing surfaces whilst maintaining equivalent forces within the exhaust. These observations lead to slender plug-cone shapes preventing sharp accelerations along the plug arc just downstream the exhaust and contributing to large entropy losses. These observations agree with those of Ref. [37] who noted better performance by elongating the plug-cone with their slightly concave surface.

4.4.3.3 Overall Benefits

In addition to the benefits obtained on the force redistribution along the plug-cone, improved aerodynamic performance arise from the reduction in size of the nacelle length with a smaller wetted surface and a slight reduction in entropy, as observed in Fig. 4.23. From an energy standpoint, using a compact nacelle for the propulsor over the baseline previously considered reduces the overall viscous dissipation rate Φ by 4 and 20% for the larger propulsor throat area, under FPR ratios of 1.20 and 1.50 respectively. These benefits therefore translate to lower fan power demand for the same net axial force, or to a larger force contribution from the thruster relative to the two main engines under the wings.

Nonetheless, these observations are made on a unique propulsor position along the tail and cannot be generalised to all combinations shown in Fig. 4.17. Whereby possible, the intake length is therefore reduced to favour more compact nacelles, leaving a longer and less steep plug-cone, to offer benefits to the flow redistribution and energy utilisation. An example of optimisation studies on compact exhausts is studied in Ref. [38] whereby improvements of 0.32% of the total aircraft thrust requirement is observed from their reference.

4.4.4 Auxiliary Savings from Re-scaled Turbofans

A benefit from producing part of the aircraft propulsive force requirement from the thruster is to re-scale the main power-plants under the wings. Maintaining a similar specific thrust, the fan by-pass and pressure ratios can be reduced to generate the output force and feed the aft-mounted fan. To that end, following the work from Giannakakis et al. on the propulsion system characteristics along a typical SMR aircraft mission [39], the baseline turbofans are re-scaled based on the top-of-climb conditions.

To ensure a constant vehicle speed V_B at cruise, state-of-the-art turbofans propelling single-aisle aircraft are used, e.g. CFM LEAP-1 or PW1100G. The two-spool turbofan architecture developed by Chalmers University of Technology (private communication) is composed of an 11:1 by-pass ratio single-stage fan, 3-stage intermediate-pressure and 10 high-pressure compressor and 2-stage high-pressure and 7-stage low-pressure turbine for an overall pressure ratio of 44. The free-stream conditions are defined following the International Standard Atmosphere (ISA) model based on the cruise altitude and Mach number and assuming isentropic inflow conditions. The engine performance is evaluated at on-design from which a nacelle drag estimation sub-process determines the nacelle drag based on the flight conditions [40] to determine an installed thrust-specific fuel consumption (SFC). Whilst the size and components of the reference aircraft's engines are simply retrofitted in the propulsive fuselage concept, the core is assumed constant and the fan diameter, or by-pass ratio, is scaled down to provide the force and power required. The turbofan maps comparison between the retrofit and re-scaled engines is given in Fig. 4.24.

Table 4.2 summarises the fuel savings difference from the retrofit engines and their re-scaled versions for the cruise segment considered herein and the best two propulsors from Fig. 4.21. It is opined that the concept B from Fig. 4.21b has diminishing returns compared to the concept A from Fig. 4.21a through the re-scaling of the engine. However, both PFC incorporate the same re-designed turbofans which are designed to provide the power off-take of concept A yielding the best benefits. Overall, whilst both propulsors are sensibly similar and only vary by their position, additional savings of between 3 and 5% are obtained with the newly designed turbo-electric architectures, neglecting practical transmission deficiencies and mass changes.

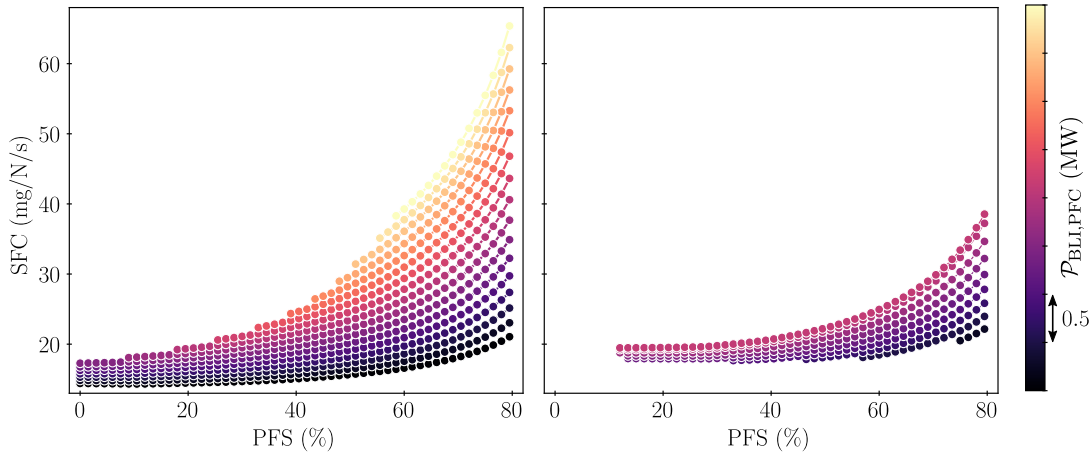


Figure 4.24: Turbofan maps of the baseline retrofit (left) and the re-scaled (right) engines

Concept B showcases the best savings when considering retrofit engines under the wings. Its thruster is installed at 90% of the fuselage length, a relative hub radial position of 30% of the fuselage radius and a fan operating at a pressure ratio of 1.29. Comparatively to the existing concepts, the axi-symmetric concept from CENTRELINE installs its thruster at 88% of the fuselage length with a hub positioned at 25% of the fuselage radius and operates at a FPR of 1.35 [9] and the STARC-ABL with an axi-symmetric fuselage which thruster is installed at 94% of its chord, relative hub position of 15% and FPR of 1.25, later optimised by Yildirim et al. with up-swept fuselage tails [41]. Despite being designed for long-haul flights with a fuselage length of 67m, the former concept agrees with the non-dimensional parameters of this study whilst the latter, designed for a similar mission features a smaller hub radius due to its tapered slope.

Table 4.2: Comparison of fuel saving coefficients (FSC) in percent between the baseline retrofit and the re-scaled turbofans of Concept A: $L_{\text{slope}}/L_{\text{tail}} = 0.5$, $R_{\text{hub}}/R_{\text{fus}} = 0.4$, BLR = 44.0%, and FPR = 1.30, and Concept B: $L_{\text{slope}}/L_{\text{tail}} = 0.6$, $R_{\text{hub}}/R_{\text{fus}} = 0.3$, BLR = 42.8%, and FPR = 1.29, relative to existing projects (with * denoting assumed values)

	Concept A	Concept B	CENTRELINE [9]	STARC-ABL [41]
$x_{\text{fan}}/L_{\text{fus}}$ (-)	0.87	0.90	0.88	0.94*
$R_{\text{hub}}/R_{\text{fus}}$ (-)	0.40	0.30	0.25	0.15*
FPR (-)	1.30	1.29	1.35	1.25
FSC (Retrofit) (%)	10.30	11.10	-	-
FSC (Re-scaled) (%)	15.30	14.04	11.5 [42]	12.8

4.5 Conclusion

Fundamental design considerations are presented on novel aircraft configurations based on aft-mounted thrusters. To come to these recommendations, a short-to-medium range aircraft is used as reference and derived as an axi-symmetric propulsive fuselage concept. From bare fuselages, conical tails of different lengths, later leading to morphed tails to accommodate aft-mounted propulsors, are investigated. The aerodynamic sensitivity to design changes is assessed with boundary layer properties along the potential for energy recovery. These quantities are tracked along slender and more complex bodies of revolution where the growth in boundary layer kinetic energy area suggests higher harvestable energy (+0.8%) with an astute integration of a mechanical device. With morphed tails, thin propulsor hubs favour the energy deposition rate in the flow, translated by higher potentials for energy recovery (+0.7%) and performance. Critical inclination angles at the slope upstream of the nacelle and at the plug-cone downstream of it are identified and bound the design space for the propulsor integration.

A focus on the propulsor integration effects upstream along the slope show that pressure gradients are affected further upstream than deviations in the velocity field as a function of the propulsor diameter. Smaller propulsors tend to slow the flow down further forward up to a fan radius upstream whilst growing the boundary layer thickness by 4% whereas, larger propulsors thicken the boundary layer by 8% and reduce the flow speed up to two fan radii, specifically in the viscous sub-layer region.

Through a design exploration of different propulsor sizes and fan pressure ratios, an overall aero-propulsive performance assessment is followed at cruise. The best performance compromise between the fuel savings and share of propulsive force between the thruster and the turbofans is generally obtained for propulsors integrated on a low fuselage hub radius, for a fan axial position around the mid-tail, or 90% of the fuselage chord length. These combinations enable greater harvestable energy from the streamlined flow driven by the slope, whilst preventing strong shocks and flow separation along the plug-cone. As regards the propulsor operation, smaller propulsors benefit from the ingestion of the most beneficial part of the boundary layer flow, reducing the power input required from the fan and thus yielding greater savings. However, maximum shares of around a third of the

overall aircraft propulsive force requirement is conceivable as greater shares reduce the fuel savings (an inverse relationship exists between the force share and the fuel savings).

On the designs studied, an increase in the compactness of the nacelle tends to increase the overall aero-propulsive performance of the concept despite the unavoidable rise in the radial distortion index. Further studies on these aspects are recommended on a case-to-case basis, especially with higher orders of fidelity on the propulsion modelling to replicate the distortion at the fan exit, which is pre-empted to negatively impact the propulsive performance. For a given fan power input, greater output force can be achieved from compact nacelles with slender plug-cones, reducing the propulsive force requirement from the turbofans and enabling a larger down-scaling of them. In doing so, an illustrative propulsive fuselage concept is extracted from the design space where a comparison in performance is performed for aircraft featuring retrofit turbofans versus re-scaled turbofans. Fuel savings on the latter are reduced by an additional 5% for the cruise segment considered.

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Energetic Approach to Causal Analyses of Exhaust System Design

The embedding of the propulsion system within the airframe, as is the case with propulsive fuselage concepts, offers a promising contribution to synergistic aircraft development featuring boundary layer ingestion. However, the lack of consensus surrounding the interpretation and quantification of benefits has led to the development of various novel aerodynamic assessment methods. This paper combines the absolute reference frame standpoint with an energy-based approach to provide an alternative perspective on performance accounting with the aim to better characterise the causality of aerodynamic mechanisms depositing energy into the flow. This approach is applied to a propulsive fuselage concept and studied numerically by exploring its propulsor exhaust and nozzle design space. The aero-propulsive performance is assessed in response to parametric design variations supported by a flow feature segmentation that quantifiably attributes aerodynamic energy consumption to various flow phenomena. Notably, power savings of up to 10.5% are achieved when the aft-end thruster produces 33% of the aircraft's thrust requirements and assists two retrofit state-of-the-art under-wing-mounted turbofans. Furthermore, evaluating the design variables' influence provides recommendations for potential improvements and reducing objective functions for optimisation studies.

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5.1 Introduction

To realise the ambitious environmental goals defined by the European Commission [1], it is notwithstanding that current aircraft are approaching their limit in terms of improvements in propulsive efficiency [2]. Therefore, in contrast to conventional configurations, there is an opportunity to exploit potential aerodynamic benefits with closer integration between the airframe and propulsion system. Inspired from the marine industry [3], a propulsor is integrated around the airframe to ingest its boundary layer (BL) to achieve aero-propulsive performance improvements. In conventional aircraft, podded engines accelerate free-stream flow to counterbalance the momentum of the airframe's wake. As a result, there is excess kinetic energy imparted to the flow in both the jet plume and airframe wake, which eventually dissipates and is lost. Boundary layer ingestion (BLI) aircraft, on the other hand, accelerate the BL/wake flow, thereby harvesting its energy, minimising loss via wake attenuation, and reducing the aircraft fuel consumption [4].

Subsequently, numerous conceptual aircraft [4] have been based upon boundary layer ingestion [5–9], from which the propulsive fuselage concept (PFC) illustrated in Fig. 5.1 appears as the most pragmatic from its direct adaptation of a conventional tube-and-wing aircraft whereupon an aft-mounted propulsor encapsulates the tail-cone [4]. The main advantages of this configuration are the opportunity to ingest the entire circumference of the fuselage's BL, and consequently reduce the viscous dissipation that would have occurred in its wake, while simultaneously utilising that energy to produce a propulsive force. Additionally, the circumferential nature of the flow, neglecting three-dimensional effects, reduces penalties arising from the fan's operation in circumferentially distorted flow and also allows the flow-field to be approximated axi-symmetrically.

The aerodynamics intricacies associated with integrated engines prevent the direct application of conventional thrust and drag book-keeping schemes [10] because

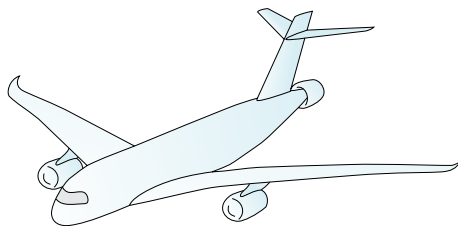


Figure 5.1: Generic propulsive fuselage concept, from Chapter 3

their founding assumptions become invalid [11]. Conventionally, the approach relies on a separation between the airframe and propulsion system to quantify the drag (resulting force between the viscous and potential flows) and the thrust (a surrogate of useful work generated), respectively. This clear division has long been necessary to define propulsive efficiency but is inappropriate in highly integrated configurations like BLI. Although a correct quantification of the net forces acting on BLI configurations can be determined from *near-field* integration, they yield limited information. Moirou et al. [4] examined various alternative momentum-based book-keeping schemes [12–14] but reported potential misrepresentations of thrust as a surrogate of useful work, which was evident by the fact that propulsive efficiency could exceed 100%. In light of the limited insights offered by momentum-based methods [15], as well as potentially misleading associated metrics, energy-based approaches appear to offer an alternative that enables more physically intuitive interpretations and quantification of the underlying physics [16–18].

The causality behind the generation of aerodynamic forces is certainly illusive when considered from the momentum balance, whereas aerodynamic work-energy relationships offer improved insights into cause and effect [11]. In that perspective, the aircraft performance is evaluated through powers in lieu of forces where pathways from the different aircraft's power sources are traced to power consumption terms, or sinks in the flow, and challenges in defining propulsive efficiency are circumvented by the adoption of the power saving coefficient (PSC) [19]. Enhanced interpretations are possible from the absolute reference frame (ARF) standpoint [16], which sees the aircraft moving through an initially quiescent atmosphere, as opposed to the relative reference frame, which perceives the aircraft as stationary with the flow moving over it. Despite these potential advantages, energy-based methods have not yet been fully adopted and momentum-based approaches are still predominantly used to evaluate the performance of different PFCs [12, 14, 20] as well as optimising them [21–26]. Although these investigations provide a quantification of the overall aero-propulsive performance, there is a potential to adopt and apply energy-based methods [27–29] within design space exploration and optimisation studies to leverage the ability to capture energy transfer pathways in response to altering design parameters [16].

To address this gap, the framework developed in Chapter 3 (DAHIANT – Design

and Analysis of Highly-Integrated Aircraft and Novel Technologies) is used on two-dimensional axi-symmetric representations of a PFC. A typical short-to-medium range aircraft defined as reference, from which the fuselage tail is morphed to accommodate a BLI propulsor. In the scope of understanding the underlying reasons behind the aero-propulsive performance of such concept, a vast design space exploration of the propulsor's exhaust is conducted. That exploration follows previous investigations [24, 30] that considered a frozen baseline from which variations in the exhaust system led to an optimised geometry. However, this previous work relied upon momentum-based accounting whereas this work adopts an energy-based approach which benefits from a flow feature decomposition procedure to trace losses generated in the flow [16]. In doing so, the causal factors are identified and the benefits related to their underpinning mechanisms responsible for enhanced aero-propulsive performance. After an exploration of the design space, an astute down-selection of candidates leads to an aero-propulsive performance evaluation and recommendations for future optimisation studies. To the authors' knowledge, this work is the first of its kind with a detailed and physically intuitive interpretation of the relation that exists between the fluid work and energy, applied to a PFC (but applicable to other disruptive concepts).

5.2 Performance Accounting Approach

5.2.1 Universal Mechanical Energy Balance

5.2.1.1 Control Volume Description

The governing equations derived in the ARF in Chapter 3 are applied to a short-to-medium range single-aisle reference aircraft to which a circumferential propulsor is integrated around the fuselage's tail. Two main engines mounted under-wing constitute a turbo-electrical and thrust-splitting aircraft architecture also commonly identified in the literature as a propulsive fuselage concept (PFC) [4].

A two dimensional cutaway of the control volume bounds the surface $\mathcal{S}$ given in Fig. 5.2. The bounding surface is composed of an outer surface $\mathcal{S}_O$ represented as the side cylinder and Trefftz planes ($\mathcal{S}_O = \mathcal{S}_{SC} \cup \mathcal{S}_{TP}$) and an inner surface. This latter is further split into the fuselage and propulsor's nacelle walls, $\mathcal{S}_B$, and any

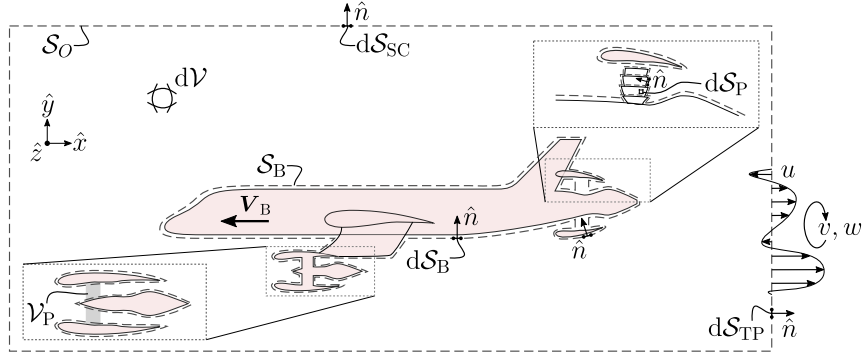


Figure 5.2: Schematic of a control volume encapsulating a powered aerodynamic body, from Fig. 3.12

permeable propulsive surfaces, $\mathcal{S}_P$ (that bounds the volume $\mathcal{V}_P$ that would represent the volumetric body forces exerted by the fan to the flow).

The direct application of the mechanical energy conservation equation to the CV defined in Fig. 5.2 yields the mechanical energy balance in Eq. (5.1) rearranged such that the left-hand side characterises the power sources to the flow, and the right-hand side the sinks:

$$\begin{aligned}
 & - \underbrace{\oint_{\mathcal{S}_P} (p_G \mathbf{V}'_S - \bar{\tau} \cdot \mathbf{V}'_S) \cdot d\mathbf{S}}_{\mathcal{P}_S} + \underbrace{\iiint_{\mathcal{V}} \rho \mathbf{f} \mathbf{V}' \cdot d\mathbf{V}}_{\mathcal{P}_f} + \overbrace{\oint_{\mathcal{S}_B} (p_G \mathbf{V}_B - \bar{\tau} \cdot \mathbf{V}_B) \cdot d\mathbf{S}}^{-W\dot{h}} + \underbrace{\iiint_{\mathcal{V}} \rho \mathbf{f} \mathbf{V}_B \cdot d\mathbf{V}}_{-F_f \cdot \mathbf{V}_B} = \dots \\
 & - \underbrace{\oint_{\mathcal{S}_O} \rho \frac{\mathbf{V}^2}{2} \mathbf{V}' + p_G \mathbf{V} - \bar{\tau} \cdot \mathbf{V} \cdot d\mathbf{S}}_{\mathcal{E}} + \underbrace{\iiint_{\mathcal{V}} -p_G \nabla \cdot \mathbf{V} \cdot d\mathbf{V}}_{\Theta} + \underbrace{\iiint_{\mathcal{V}} (\bar{\tau} \cdot \nabla) \cdot \mathbf{V} \cdot d\mathbf{V}}_{\Phi} \quad (5.1)
 \end{aligned}$$

5.2.1.2 Propulsor Mechanical Power Caveat

In Eq. (5.1), $\mathcal{P}_S$ describes the amount of shaft power related to any moving fan's blade (propulsor with its blades in Fig. 5.2 top-right corner), and the work applied to the flow (relative to the body) and corresponds to a rise in total enthalpy in the flow ΔH_0 . $\mathcal{P}_f$ can equally be used when the fan's blades are substituted for a body force representation inside a volume ($\mathcal{V}_P$ in Fig. 5.2) and is equivalent to $\mathcal{P}_S$ and the total enthalpy rise. This representation is often favoured when the propulsor's geometry is unknown [23–25]. However, sometimes it is more convenient to model the propulsor as a set of inlet and outlet boundary conditions. In this scenario, Drela

[17] derives the commonly referred to $\mathcal{P}_K$ term to represent the change in mechanical energy flux between the inlet and outlet stations, on either side of the fan. But there is a caveat regarding $\mathcal{P}_K$ because, unlike total enthalpy rise, its value will change according to the interface location of the inlet and outlet boundary conditions. This is because $\mathcal{P}_K$ inherently includes the pressure volume work, Θ_P , within the fan's volume, $\mathcal{V}_P$, that is bound by the inlet and outlet interface planes.

The rate of pressure work within the fan volume Θ_P is composed of isentropic (reversible) and non-isentropic (irreversible) components that can subsequently be identified in Eq. (5.2). As such, the rate of volumetric pressure work Θ is segmented into its two components, Θ_{rev} , the recoverable or reversible work, and the local irreversibilities, Θ_{irr} , echoing the Baroclinic power from Sato [31] imparted to the flow and due to non-isentropic compression and dilation waves encountered in transonic compressible flows.

$$\mathcal{P}_S \equiv \mathcal{P}_f \equiv \Delta H_0 \equiv \mathcal{P}_K + \Phi_P + \Theta_P = \underbrace{\mathcal{P}_K + \Theta_{\text{rev},P}}_{\mathcal{P}_K^*} + \underbrace{\Phi_P + \Theta_{\text{irr},P}}_{\Phi_P^*} \quad (5.2)$$

The corrected additional power $\mathcal{P}_K^*$ accounts for the local reversible portion of Θ within $\mathcal{V}_P$ that will cancel out with the reversible portion of Θ in the surrounding external volume (Chapter 3). Noting that Θ_{irr} and Φ , within the propulsor volume, represent the fan's internal losses, they can also be related to the fan's isentropic efficiency. Subsequently, Chapter 3 describes how the fan's isentropic efficiency can be used to approximate $\Theta_{\text{rev},P}$. Furthermore, the amount of total irreversible losses Φ_P^* within the fan stage can be approximated, although not being separable in their $\Theta_{\text{irr},P}$ and Φ_P constituents.

5.2.1.3 Work-Energy Transfers in Flight

A plausible change in potential energy is introduced with $W\dot{h}$ whereby any excess or deficit in power in the system would respectively result in climb or decent, with the rate of change in altitude $\dot{h}$. This potential energy-rate equates the pseudo-work rate of forces that act on the flow. As the term can either be positive or negative, it can be placed on either side of Eq. (5.1), whereby the descent of the aircraft would impart energy to the atmosphere through conversion of its potential energy.

The mechanical energy fluxes $\dot{\mathcal{E}}$ (Eq. (5.3)) crossing over the outer boundary surface $\mathcal{S}_O$ can conveniently be decomposed into the side cylinder and Trefftz plane surfaces, $\dot{\mathcal{E}}_{SC}$ and $\dot{\mathcal{E}}_{TP}$ respectively, following Fig. 5.2 conventions. As a whole, $\dot{\mathcal{E}}$ is constituted of the wake stream-wise kinetic energy deposition rate $\dot{\mathcal{E}}_u$, the transverse kinetic energy deposition rate $\dot{\mathcal{E}}_v$, and the rates of wake pressure-defect work $\dot{\mathcal{E}}_p$, viscous boundary work $\dot{\mathcal{E}}_\tau$ and wave-outflow energy deposition $\dot{\mathcal{E}}_w$. Additionally, $\dot{\mathcal{E}}_u$ and $\dot{\mathcal{E}}_v$ sum up to $\dot{\mathcal{E}}_k$ characterising the overall wake kinetic energy deposition rate.

$$\dot{\mathcal{E}} = \underbrace{\iint_{\mathcal{S}_{TP}} \left(\frac{\rho}{2} \mathbf{V}^2 \right) \mathbf{V}' \cdot \hat{n} \, d\mathcal{S}}_{\dot{\mathcal{E}}_k} + \underbrace{\iint_{\mathcal{S}_{TP}} p_G \mathbf{V} \cdot \hat{n} \, d\mathcal{S}}_{\dot{\mathcal{E}}_p} + \underbrace{\iint_{\mathcal{S}_{TP}} -\bar{\bar{\tau}} \cdot \mathbf{V} \cdot \hat{n} \, d\mathcal{S}}_{\dot{\mathcal{E}}_\tau} + \dots + \underbrace{\iint_{\mathcal{S}_{SC}} \left[\left(\frac{\rho}{2} \mathbf{V}^2 \right) \mathbf{V}' + p_G \mathbf{V} - \bar{\bar{\tau}} \cdot \mathbf{V} \right] \cdot \hat{n} \, d\mathcal{S}}_{\dot{\mathcal{E}}_w} \quad (5.3)$$

At the outer boundaries (SC and TP), only the excess in kinetic energy fluxes are captured, if both surfaces are defined sufficiently far from the body such that they do not experience pressure perturbations from the flow. The energy initially deposited within the flow $\mathcal{V}$ is eventually converted into other forms as represented by the sinks Φ and Θ . This energy transfer takes place in the flow as it tries to resume to its initial quiescent state. The first term Θ measures the volumetric pressure work rate as the fluid experiences compression and dilation waves against the atmospheric pressure. Analogous to Drela's power balance term $\mathcal{P}_V$, the change in sign attributes this quantity to a power loss, or sink, associated with non-isentropic compressibility changes [31]. The last term Φ represents the accumulation rate in viscous dissipation along the flow path that manifests itself as heat and measures the irreversible mechanical losses. When measured at the Trefftz plane (TP) to capture all losses from the system, Φ and Θ are preferably referred to as Φ_∞ and Θ_∞ ¹.

Alternatively, the mechanical energy balance can be written under a non-dimensional form using the free-stream dynamic pressure power. This non-dimensionalisation reduces to coefficients $C_{\mathcal{X}}$, where $\mathcal{X}$ can take the form of any term from the balance.

¹This notation is only used when the Trefftz plane is located very far downstream.

Nonetheless, to not depend on the reference surface area $\mathcal{S}_{\text{ref}}$ selected, the term is preferred on the left-hand side of the equation. That form of Eq. (5.4) enables the comparison of specific terms $\mathcal{X}$ between different configurations, e.g. sizes or designs.

$$C_{\mathcal{X}} \mathcal{S}_{\text{ref}} = \frac{\mathcal{X}}{\frac{1}{2} \rho_{\infty} V_{\infty}^3} \quad (5.4)$$

5.2.2 Application to Decoupled Aero-Propulsive Partial Assemblies

To simplify the analysis, weak aerodynamic coupling between the fuselage assembly (fuselage and thruster) and the remaining aerodynamic components (wings, pylons, under-wing engines and vertical and horizontal tail planes) is assumed via an approach similar to a drag build-up method (Fig. 5.3). In doing so, the fuselage assembly experiences a net force imbalance $\mathcal{F}_{\text{imb}}$, even in steady-level flight condition, which must be accounted for within the energy balance. The imbalance experienced by the partial assembly is comprised of two contributions given in Eq. (5.5) with a change in potential energy of the sub assembly and the force power of the rest of the components decoupled from the assembly. Therefore, the previous imbalance suggested by $(W\dot{h})_{\text{net}}$ lumps together $(W\dot{h})_{\text{fus+prop}}$ for the simplified assembly, and $\mathcal{F}_{\text{rest}} \cdot \mathbf{V}_B$ for the remaining components.

$$\begin{aligned} (W\dot{h})_{\text{net}} &= (\mathcal{F}_{\text{imb}} \cdot \mathbf{V}_B)_{\text{fus+prop+rest}} \stackrel{?}{=} (W\dot{h})_{\text{fus+prop}} + (W\dot{h})_{\text{rest}} \\ &\stackrel{?}{=} (\mathcal{F}_{\text{fus}} + \mathcal{F}_{\text{prop}}) \cdot \mathbf{V}_B + \mathcal{F}_{\text{rest}} \cdot \mathbf{V}_B \end{aligned} \quad (5.5)$$

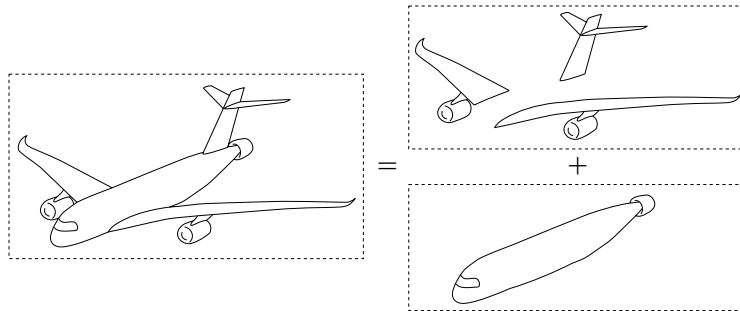


Figure 5.3: Decomposition of the propulsive fuselage concept into partial assemblies: fus+prop at bottom and rest at top, from Chapter 3

This formulation becomes advantageous as it allows a problem simplification by isolating a system and evaluate its performance. To that end, $\mathcal{F}_{\text{imb}}$ is quantified with an explicit formulation of forces imbalance over partial assemblies (fuselage, $\mathcal{F}_{\text{fus}}$, and BLI propulsor, $\mathcal{F}_{\text{prop}}$), and proves its usefulness when thrust-splitting architectures are considered to deduce the remaining force necessary by the under-wing engines to maintain the net change in potential energy of the entire aircraft.

In these scenarios, the energy balance in Eq. (5.1) reduces to Eq. (5.6) where the net potential energy change is substituted with its sub-assembly quantity, related to an imbalance force.

$$\mathcal{P}_S + \mathcal{P}_f - (\mathcal{F}_{\text{fus}} + \mathcal{F}_{\text{prop}}) \cdot \mathbf{V}_B = (W\dot{h})_{\text{rest}} + \dot{\mathcal{E}} + \Phi + \Theta \quad (5.6)$$

5.2.3 Flow Feature Decomposition

Within the flow, different features are identifiable leading to different flow mechanisms and better flow assessments. Following the different flow feature identification methods reviewed by Moirou et al. [4] and the flow decomposition precedence introduced by Sanders [32], main and sub features are extracted in this work as given by Fig. 5.4.

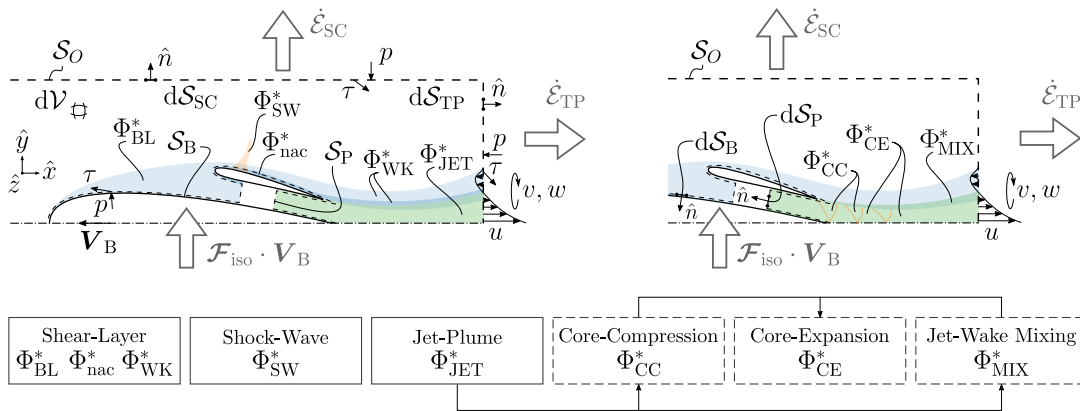


Figure 5.4: Flow-chart of the precedence process for work flow-paths identification and schematic of the corresponding mechanisms

5.2.3.1 Main Features

First, the main features are represented by their associated power losses on the left hand side of Fig. 5.4 and obtained following the procedure detailed in Section 5.A: the boundary layers (BL and nac), the wake (WK), the shock waves (SW) and the jet plume (JET). However, there is a caveat in the local decomposition of the total mechanical loss. While the rate of viscous dissipation Φ is a positive-only quantity, the rate of volumetric pressure work Θ can locally take positive or negative values. The separation of Θ into its isentropic and non-isentropic parts attributes respectively a recoverable constituent, and an irreversible loss. Although it is easier to assess only the viscous dissipation rate on the segmented flow regions, the irrecoverable losses can all be lumped together in a mechanical loss term Φ^* under the form of Eq. (5.7) where a bulk flow quantity Φ_{bulk}^* is added to quantify the losses not captured numerically by the different identifiers.

$$\Phi^* = \Phi_{\text{BL}}^* + \Phi_{\text{nac}}^* + \Phi_{\text{WK}}^* + \Phi_{\text{SW}}^* + \Phi_{\text{JET}}^* + \Phi_{\text{bulk}}^* \quad (5.7)$$

To consider the volumetric pressure work Θ , an exact tracking is enabled by only considering the irreversible component Θ_{irr} . Sato [31] introduced Φ^* as the sum of all energy forms within a feature however, this can double account for quantities that are in the form of available energy in a volume and transformed into another form in another feature, e.g. the BL kinetic energy dissipated in the wake. Therefore, it becomes pragmatical to only track the progress of viscous dissipation and irrecoverable volumetric pressure work rates, as defined with Eq. (5.8), keeping the notation introduced by Sato.

$$\Phi^* = \Phi + \Theta_{\text{irr}} \quad (5.8)$$

5.2.3.2 Jet Plume Partitioning

The precedence procedure in Section 5.A successfully demonstrates the decomposition of the various flow mechanisms present in BLI and allows Φ_{JET}^* to be decomposed into its core-compression (CC), core-expansion (CE) and jet/wake mixing (MIX) regions (Eq. (5.9)). The advantage of this application is the

quantification of the individual contributions to the overall benefits, whether in form of viscous dissipation or irreversible pressure work.

$$\Phi_{\text{JET}}^* = \Phi_{\text{CC}}^* + \Phi_{\text{CE}}^* + \Phi_{\text{MIX}}^* \quad (5.9)$$

Nevertheless, with the consideration of boundary conditions to model the propulsor blades and its stage in CFD, detailed later, large contributions of pressure work and viscous dissipation rate are neither solved or captured. To prevent the use of the Θ decomposition in its isentropic and non-isentropic components, which still needs to be proven and validated, the analysis entirely focus on the viscous dissipative losses Φ . The inclusion of Θ_{irr} is thus left for consideration in further work.

Finally, following the flow-paths of the system, the mechanical energy balance on partial assemblies is re-written accounting for all flow mechanisms in Eq. (5.10).

$$\begin{aligned} \mathcal{P}_S + \mathcal{P}_f + (\mathcal{F}_B + \mathcal{F}_P) \cdot \mathbf{V}_\infty = & (Wh)_{\text{rest}} + \dot{\mathcal{E}} + \Phi_{\text{BL}}^* + \Phi_{\text{nac}}^* + \Phi_{\text{SW}}^* + \Phi_{\text{WK}}^* + \dots \\ & \underbrace{\Phi_{\text{CC}}^* + \Phi_{\text{CE}}^* + \Phi_{\text{MIX}}^*}_{\Phi_{\text{JET}}^*} + \Phi_{\text{bulk}}^* + \Theta_{\text{rev}} \end{aligned} \quad (5.10)$$

5.2.4 Aircraft- and System-Level Performance Assessment

To evaluate the performance of the PFC, the reference aircraft is defined following a drag build-up approach (Chapter 3) to indicate the propulsive force requirement from the under-wing engines. Its isolated fuselage axial net force (indicated as “Reference” in Fig. 5.5), $\mathcal{F}_{\text{fus,ref}}$, is evaluated via computational fluid dynamics (CFD) and related back to the overall aircraft drag $\mathcal{F}_{\text{A/C,ref}}$. Stemming from the reference aircraft, a reference morphed fuselage geometry is derived (“Morphed” in Fig. 5.5) to provide an improved understanding of the integration effects from the aft-mounted propulsor. Subsequently, the net force on this PFC isolated fuselage is also evaluated from CFD with $\mathcal{F}_{\text{fus,MOR}}$. To extract the force contribution from the BLI propulsor, the net axial force ($\mathcal{F}_{\text{fus+prop,PFC}}$) on the PFC (“Ducted” from Fig. 5.5) is determined from *near-field* integration over the vehicle in CFD.

It is stressed that thrust is used as a notional quantity and should represent useful work, or the positive contribution of the force distribution over the aerodynamic body. In that respect, propulsive force is preferred to thrust where the thruster

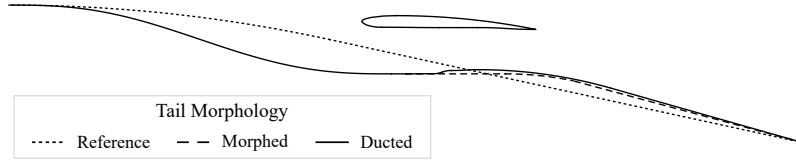


Figure 5.5: Morphing sequence of a rear-mounted BLI propulsor from its reference and morphed tail fuselages, adapted from Chapter 3

contribution in the PFC is characterised with a propulsive force share (PFS). Assuming that the remaining force requirement is provided by the under-wing propulsion systems to maintain a steady cruise condition, the numerator is obtained from the difference between the powered PFC and unpowered morphed fuselages:

$$\text{PFS} = \frac{\mathcal{F}_{\text{fus+prop,PFC}} - \mathcal{F}_{\text{fus,MOR}}}{\mathcal{F}_{\text{A/C,MOR}}} \quad (5.11)$$

The performance of the aft-mounted BLI propulsor can be quantified with a surrogate of propulsive efficiency. Nonetheless, the propulsive efficiency metric as defined for podded configuration is not applicable because of the ambiguity in establishing the useful work done by the propulsion system (thrust-power). Alternatively, Moirou et al. [4] advised on using the propulsor efficacy κ from Ref. [16]. In search of converging towards a practical definition, κ evaluates the efficacy of the BLI assembly to convert the fan mechanical power into a surrogate of useful work. However, a caveat appears as to how to correctly define useful work as losses are generated in the thruster plume which affects the numerator. Additionally, to circumvent the use of the shaft power from the BLI propulsor $\mathcal{P}_{S,\text{BLIP}}$ by Sanders and Laskaridis, a corrected metric κ^* is introduced in Eq. (5.12) accounting for the corrected additional power $\mathcal{P}_K^*$.

$$\kappa^* = \frac{(\mathcal{F}_{\text{fus+prop,PFC}} - \mathcal{F}_{\text{fus,MOR}}) \cdot V_\infty}{\mathcal{P}_K^*} \text{ where } \mathcal{P}_K^* = \eta_{fan} \cdot \mathcal{P}_{S,\text{BLIP}} \quad (5.12)$$

The energy harvested by the BLI propulsor from the flow reduces the power demand from the fan required to achieve the configuration's desired net force. These benefits are quantifiable with a power saving coefficient (PSC) by means of comparison between the novel aircraft and its reference. As the thrust-splitting architecture distributes the thrust requirement to the BLI propulsor and under-wing turbofans, combinations of PFS and PSC can be used to target the

best compromise. Although the BLI propulsor power $\mathcal{P}_{S, \text{BLIP}}$ is extracted from CFD at the specific force contribution, the power supply from the under-wing power-plants to the thruster $\mathcal{P}_{\text{BLIP}}$ is used to be related to a transmission system's efficiency η_{trans} . Additionally, the power consumed by the under-wing engines to generate thrust needs to be modelled in both the PFC and reference aircraft (Section 5.3.3). As such, the podded engines define $\mathcal{P}_{\text{A/C,ref}}$ for the reference model and are coupled with the thruster to define the PFC total power consumption $\mathcal{P}_{\text{PFC}}$.

$$\text{PSC} = \frac{\mathcal{P}_{\text{A/C,ref}} - \mathcal{P}_{\text{PFC}}}{\mathcal{P}_{\text{A/C,ref}}} = 1 - \frac{\mathcal{P}_{\text{POD}} + \mathcal{P}_{\text{BLIP}}}{\mathcal{P}_{\text{A/C,ref}}} = 1 - \frac{\mathcal{P}_{\text{POD}} + \frac{\mathcal{P}_{S, \text{BLIP}}}{\eta_{\text{trans}}}}{\mathcal{P}_{\text{A/C,ref}}} \quad (5.13)$$

The reduction of Eq. (5.13) to a turbo-electric aircraft architecture links directly the power supply from the under-wing turbofans to the power off-take from the low-pressure turbine. Therefore, in thrust-splitting scenarios, the power off-take is computed along with the remaining propulsive force requirement to map out different engine decks and the power saving coefficient is reduced to a fuel saving coefficient (FSC) from which fuel-flow rates $\dot{m}_f$ are quantified assuming identical fuel properties:

$$\text{FSC} = 1 - \frac{\dot{m}_{f, \text{PFC}}}{\dot{m}_{f, \text{A/C,ref}}} \quad (5.14)$$

Extending the work from Lamprakis et al. [33] for their isolated studies, the ideal minimum power saving coefficient achievable, PSC_{min} , is calculated by substituting the under-wing engines with ideal propulsion systems (i.e. propulsive efficiency of 100%), in both the reference and novel aircraft. In the thrust-splitting architecture, this propulsion system therefore provides the remaining force requirement $((1 - \text{PFS}) \mathcal{F}_{\text{PFC}})$ to maintain a steady-level flight. Despite not representing the absolute minimum value (as deteriorating the PFC's main engines would reduce the value of PSC), the notation PSC_{min} is conserved:

$$\text{PSC}_{\text{min}} = 1 - \frac{(1 - \text{PFS}) \mathcal{F}_{\text{PFC}} \cdot V_{\text{B}} + \mathcal{P}_{\text{BLIP}}}{\mathcal{F}_{\text{A/C,ref}} \cdot V_{\text{B}}} \quad (5.15)$$

This metrics proves its usefulness by circumventing the choice of the reference main engines. It is observed in Ref. [34], that the efficiency of the propulsor is altered whether the same mass-flow, jet velocity or power addition is used to

compare the podded engine to the thruster. Additionally, the consideration of a turbofan's technology level affects the reported PSC as the savings are conferred by both the additional fan and a more recent turbofan.

5.3 Morphed Propulsive Fuselage Concept

From the geometry reduction made with partial assemblies, the fuselage and thruster assembly is further simplified with a two-dimensional axi-symmetric approximation to reduce the computational demand. This lower order modelling constrains the degrees of freedom for a design space exploration but still allows one to investigate the influence that many macro parameters have on the surrounding flow-field, and therefore the vehicle performance. In the same vein as the NASA STARC-ABL [8] and the CENTRELINE [35] projects, amongst other concepts reviewed in Ref. [4], a conventional single-aisle short-to-medium range aircraft is morphed into a PFC to accommodate an aft-integrated propulsor.

5.3.1 Reference Airframe

A conventional short-to-medium range aircraft is used as reference to derive an axi-symmetric body of revolution lacking wings, belly fairing, empennage and stabilisers. Its approximation is based upon a drag build-up approach (Chapter 3) where the fuselage is isolated as a 2D axi-symmetric aerodynamic body computed in CFD and whose net body force is related back to the overall aircraft propulsive force requirement to maintain a steady-level flight. The fuselage's overall

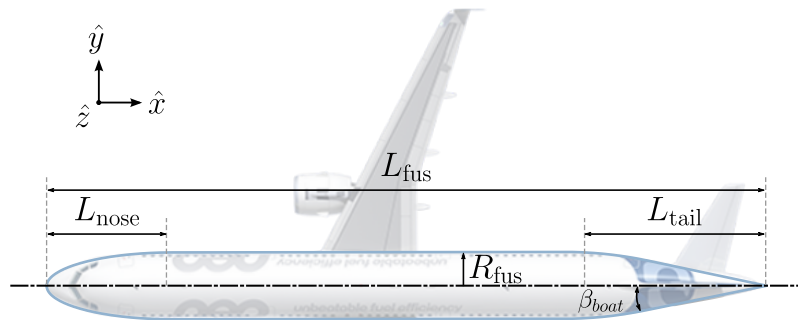


Figure 5.6: Schematic of the reference aircraft's bare fuselage approximation with an ellipsoidal nose and conic tail, superimposed over the top view of an A321, courtesy of Airbus

dimensions comply with the reference airframe, of length L_{fus} , from which its nose and tail sub-parts are respectively approximated with an ellipsoid and a cone and the middle section representing the cabin with a cylinder of constant radius R_{fus} . Figure 5.6 illustrates the derivation of the Airbus A321 fuselage contour for the geometry modelled in CFD.

To benchmark the relative performance of the propulsive fuselage concepts over the reference, and assess the BLI benefits, the reference aircraft is propelled in two different ways: with a fictitious propulsion system of propulsive efficiency of 100% and with state-of-the-art turbofan engines (further detailed in Section 5.3.3).

5.3.2 Propulsive Fuselage Aeroline

5.3.2.1 Morphing Sequence

While a convenience² exists in retrofitting a BLI propulsor around a conical tail [32], other considerations favour reshaping the fuselage tail to accommodate better the propulsor. This allows the flow to enter the BLI fan axially whereas the retrofit places constraints on the nacelle design, which incur losses due to separation over the aft body of the nacelle [32]. By allowing the tail to be morphed, we can relax this constraint and mitigate those losses. However, this introduces new trade-offs with possible flow separation ahead of the intake due to the serpentine curve, as well as behind the propulsor because of steep plug angles. The sequence from the reference tail to its ducted reshaped form is illustrated in Fig. 5.5. In the same vein as an existing concept [35], this design favours a serpentine pre-inlet outline in lieu of a tapered slope [8].

While an adequate diffusion must be ensured for the flow to enter the BLI fan, it can either be done with a pre-diffusion, an internal diffusion within the intake, or a combination of both. In any case, an acceptable Mach number is required to ensure efficient fan operation. In this work, a nominal position of the propulsor is selected to prevent separation upstream of the thruster, and recirculation behind the fuselage. To that respect, a Bell-Mehta fifth-order polynomial is used as a versatile and robust way to direct the inflow [36]. The propulsor parametrisation follows Fig. 5.7 with

²The convenience lies in closer comparisons between the BLI and reference aircraft, because the fuselage geometry is equivalent.

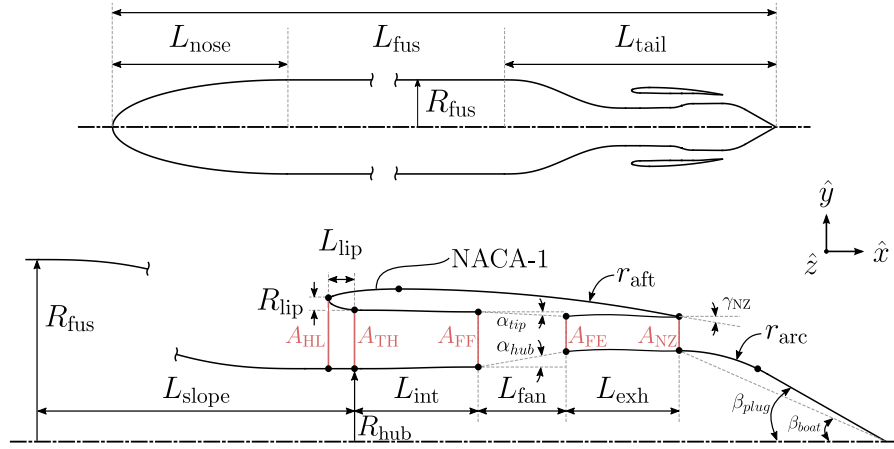


Figure 5.7: Schematic of an axi-symmetric fuselage with a thruster, with its tail slope, propulsor intake, cowl, exhaust, and aft-cone geometries parametrisation (black) and thermodynamic stations (pink), adapted from Chapter 3

a combination of Bell-Mehta polynomials, circular and elliptic arcs and a fore-cowl NACA-1 Series [37], where further details are provided in Chapter 3.

Despite the rather simple representation, the design variables defined allow one to explore the rear-end design space (exhaust and plug) and the propulsor size and pressure ratio simultaneously, while evaluating their influence on the aero-propulsive performance.

5.3.2.2 Aerodynamic and Propulsive Modelling

As part of the DAHIANT (Design and Analysis of Highly-Integrated Aircraft and Novel Technologies) framework developed in Chapter 3, the 2D axi-symmetric models are generated from the MENAC (Modelling of Embedded Novel Aircraft Concepts) module, a Python-based in-house code which formats output data points for ANSYS ICEM 2020R2 mesher [38]. The grid is automatically generated

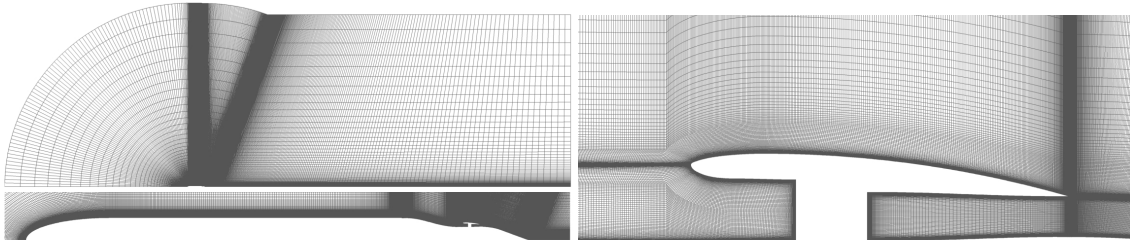


Figure 5.8: Coupled C-structure grid around a propulsive fuselage with the mesh of the domain, around the fuselage and around the propulsor, from Chapter 3

following a structured C-topology surrounding the vehicle centreline nested within a bullet-shaped domain (Fig. 5.8). To avert flow impingement in the far-field, ten fuselage chords are ensured upstream and above the body ($d_{\text{far-field}}$) and forty fuselage lengths behind the body (d_{outlet}) using a coupled grid and domain convergence study following Ref. [39]’s procedure [16]. The mesh parametrisation is elaborated in a fashion that elements close to the wall ensure a fineness with a y^+ that falls between 0.9 and 1.0 along the whole body, and coarsen by a certain coefficient the farther away from the vehicle, with a capped value as it approaches the domain outlet’s.

The flow-field is numerically computed using ANSYS Fluent 2020R2 [40] solving the Reynolds-Averaged Navier-Stokes equations. The Shear-Stress Transport (SST) $k-\omega$ turbulence model is selected with second-order spatial discretisation and no-slip condition over the walls, whereas the far-field conditions are modelled with a pressure far-field upstream the body and a pressure outlet condition at the exit plane (Fig. 5.9).

The BLI propulsor is modelled using boundary conditions (BC) following an iterative procedure where the fan-face is accounted as a pressure outlet whereby a static pressure and a target mass-flow rate are provided. To set a technology level, a fan polytropic efficiency target is defined. Ensuring continuity through the fan stage, the fan-exit is defined as a mass-flow inlet. An iterative procedure between the fan-face flow properties, fan pressure ratio (FPR) and polytropic efficiency ensures a correct mass-flow averaged total temperature with a monitoring of the convergence of the relative difference in FPR, polytropic efficiency and mass-flow imbalance.

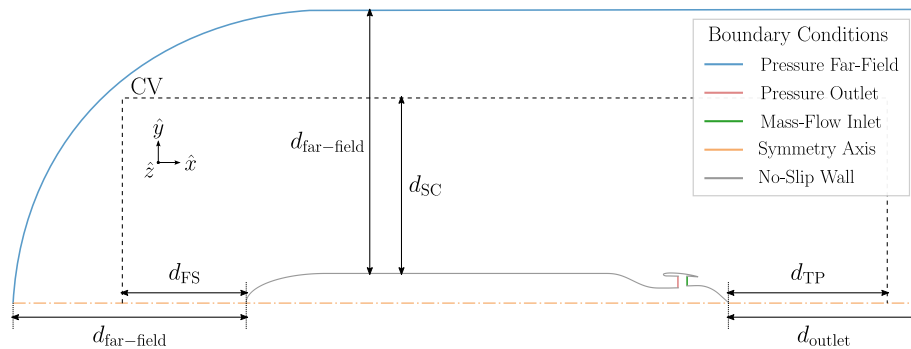


Figure 5.9: Schematic of the domain, propulsive fuselage model, boundary conditions and analysis control volume, from Chapter 3

5.3.3 Under-Wing Engines

To ensure a cruise mission similar to the reference aircraft, a steady-level condition is maintained at $M = 0.78$ and 35,000ft over the sea-level (FL350), i.e. $Re_c = 2.73 \cdot 10^8$ based on the fuselage chord length. In either reference or propulsive aircraft, the free-stream conditions are defined following the International Standard Atmosphere (ISA) model based on the cruise altitude and Mach number and assuming isentropic inflow conditions. Based on state-of-the-art propulsion systems propelling short-to-medium range aircraft, e.g. CFM LEAP-1 or PW1100G, a two-spool turbofan architecture is selected and developed by Chalmers University of Technology (private communication). The engine is composed of an 11:1 by-pass ratio single-stage fan (with a pressure ratio of 1.49), 3-stage intermediate-pressure and 10-stage high-pressure compressor driven by 2-stage high-pressure and 7-stage low-pressure turbines for an overall pressure ratio of 44. From the low-pressure shaft, power is extracted to feed the thruster via a black-boxed system where a transmission efficiency is applied to the system ($\eta_{trans} = 100\%$ herein). Despite the fact that BLI benefits enable the on-board engines to be down-scaled as their thrust requirements reduce, enlarging the savings, the reference engine at its size is conserved (retrofitting) operating at off-design for different power off-take and force requirements.

5.3.4 Design Space Sampling

The exhaust and nozzle design space exploration (DSE) is performed through combinations of parametric studies with variations in nacelle trailing edge droop angle, γ_{NZ} , plug angle, β_{plug} and hub and tip hade angles, α_{hub} and α_{tip} , respectively. These parameters are qualified as macro as they allow to change the shape of the propulsor exhaust, direct the outflow, change the plug cone shape and the nacelle aft-end cowl.

The deviations in nacelle droop are described as relative radial positions from the nacelle leading edge radius, $\Delta_{nac,TE}$, in preference to the nacelle trailing edge (TE) droop angle, γ_{NZ} . This coefficient multiplies the throat radius, from A_{TH} in Fig. 5.7 to define the radius offset between the leading and trailing edges radial positions, and is preferred because of the scaling experienced by the nacelle as the throat height increases to ingest more mass-flow. The droop angle is altered as the nacelle

scales up or down whereas the relative position of the TE is unchanged, facilitating comparison between designs of different sizes.

The hade angles defined at the fan hub and tip describe the angles between the fan-face and fan-exit stations [41] and entail changes in the exhaust starting points, hence shapes. For conciseness, only the hub hade angle is reported within the results but it is stressed that the designs are generated in pairs with a difference of 10° between the angles, i.e. $\alpha_{hub} = 10^\circ, 5^\circ, 0^\circ$ and $\alpha_{tip} = 0^\circ, -5^\circ, -10^\circ$ respectively.

Lastly, from the reference airframe boat-tail angle β_{boat} (Fig. 5.6), derivatives are generated with the plug angle β_{plug} (Fig. 5.7) which inherently adjusts the circular arc radius r_{arc} and chord, thus length, to ensure continuity and tangency at the nozzle plane and plug cone. To enable a comparison between designs, a non-dimensional plug angle coefficient is introduced as the ratio of the plug angle to that of the boat, $f_{plug} = \beta_{plug}/\beta_{boat}$.

To approach the exploration of the aft-mounted propulsor's design space, a clustered (i.e. grouped) quasi-systematic (i.e. quasi equally distributed) sampling is used. The population is clustered in combinations of propulsor sizes that ingest different amounts of mass-flow subject to different fan pressure ratio (FPR). The throat height, or area, is represented with the non-dimensional boundary layer ratio (BLR) which allows comparison amongst the different geometries under equivalent flight conditions. The variable is computed as the ratio of ingested mass-flow rate at the throat thermodynamic station, where $x_{TH} = L_{fus} + (L_{slope} - L_{tail})$, over the mass-flow present in the entire BL of height δ that develops over the morphed un-installed fuselage at the same axial position, i.e. considering the fuselage contour without propulsor. This comparison provides a non-conservative estimation of the mass-flow rate through the propulsor, but consistent amongst all geometries, from the un-installed fuselage as it excludes the interaction effects that are known to thicken the BL [12].

Subsequent to the clusterisation, a quasi-systematic seeding (Fig. 5.10) on the nacelle trailing edge droop, plug and hade angles is conducted. These deviations are illustrated in Fig. 5.10 after following the design routine retrieved from the MENAC module of DAHIAN (Chapter 3). The different combinations are also illustrated in Fig. 5.11 (blue scatters of (a)) where the kernel density estimates in the primary diagonal of Fig. 5.11(a) represent the distribution and density of all design combinations with respect to the particular design variable given in the

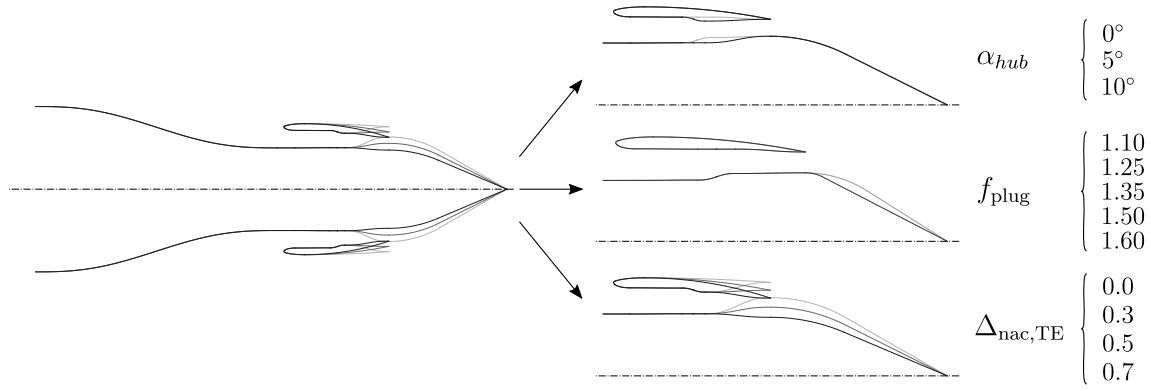


Figure 5.10: Variations in the exhaust and plug design, following the design variables defined in Fig. 5.7

rows. Overall, a population of 1,350 geometries is obtained with 5 BLR, 5 FPR, 4 nacelle trailing edge droop, 3 hade angles combinations and 5 plug angles, discarding the unrealisable geometries due to the parametrisation, i.e. out of the 1,500 possible design combinations.

5.4 Propulsive Fuselage Concept Performance Evaluation

5.4.1 Performance Metrics and Objective Functions Reduction

The close integration of the propulsion system within the airframe enhances the aerodynamic and propulsion coupling and their inter-dependent effects and as such, it is important to describe their correlations. Although this work focuses on the exploration of the rear-end design space of a propulsive fuselage concept, an opportunity is given to reduce the objective functions for a potential design optimization. Figure 5.11 represents a summary of the design space exploration and is split into four quadrants as well as a grid system, for ease of reading and reference to the text. Figure 5.11(a) details the population explored, Fig. 5.11(b) the Pearson and Spearman indexes related to Fig. 5.11(c) which shows the correlation between the design parameters and the aero-propulsive performance parameters, and Fig. 5.11(d) that shows the correlation between the dependent

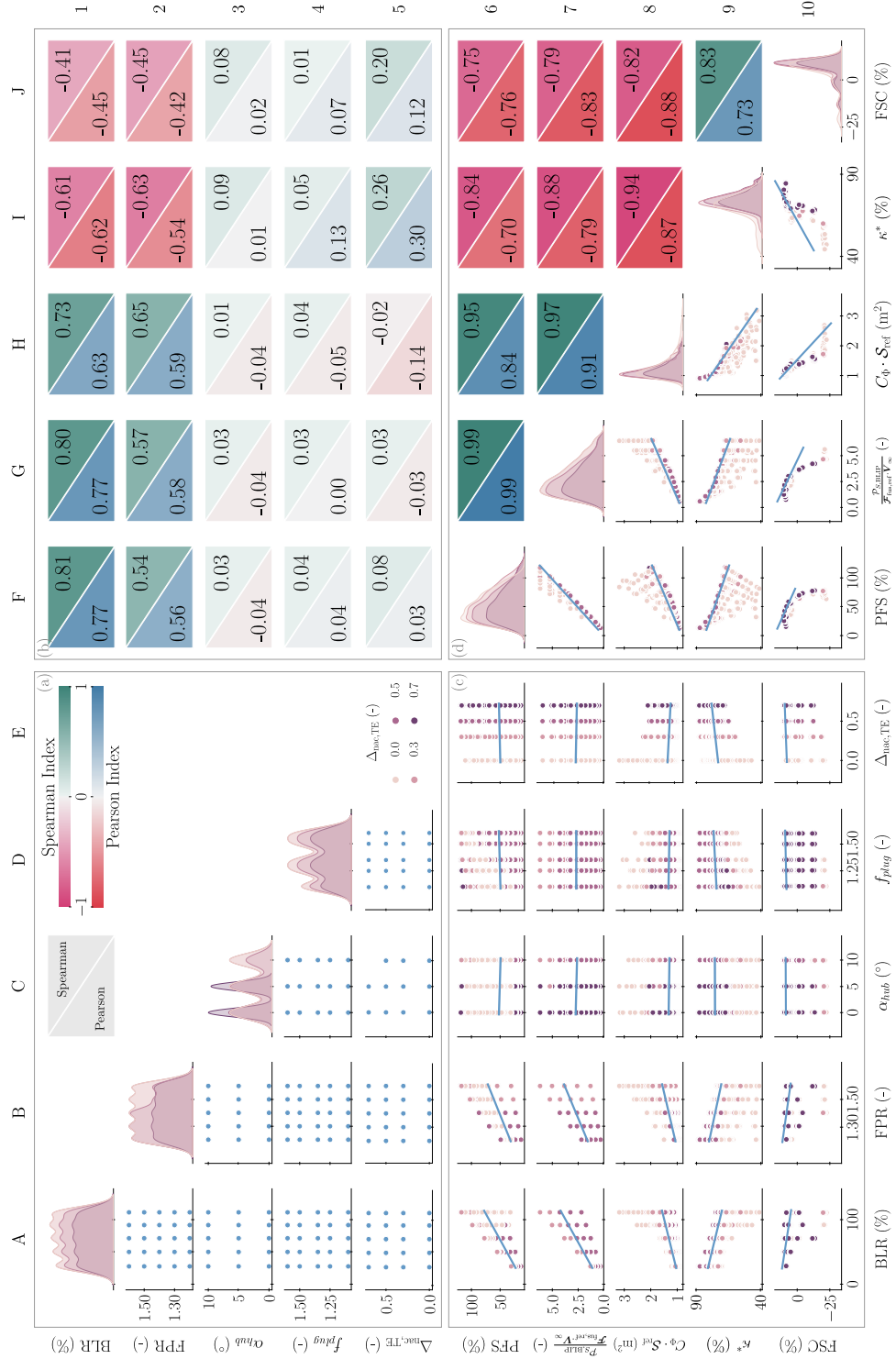


Figure 5.11: Design space exploration with the sample density (main diagonal): (a) clustered quasi-systematic seeding, (b) Pearson and Spearman indexes related to the (c) correlation with the performance evaluation metrics, and (d) coupled metrics correlation with their Pearson and Spearman indexes

and independent parameters used to evaluate the aero-propulsive performance of the different PFC:

- propulsive force share, PFS (Eq. (5.11));
- non-dimensional BLI fan power demand, $\mathcal{P}_{S, \text{BLIP}} / (\mathcal{F}_{\text{fus, ref}} \cdot V_{\infty})$ (from Eq. (5.1));
- viscous dissipation rate coefficient, $C_{\Phi} \mathcal{S}_{\text{ref}}$ (from Eqs. (5.1) and (5.4));
- corrected BLI propulsor efficacy, κ^* (Eq. (5.12));
- power or fuel saving coefficient, PSC or FSC (Eqs. (5.13) and (5.14)).

The PFS is inherently related to the stream-wise net assembly force which the aircraft is subject to. With greater mass-flow and fan pressure ratio, higher shaft power is required by the fan but also more energy is imparted to the flow. Part of this provided energy is used as useful work and the remainder is lost and essentially captured by the viscous losses. The better the energy is used, the higher the efficiency, or efficacy, and the larger the benefits would be in terms of power consumption or savings (Fig. 5.12). To illustrate these trade-offs, the dependency of each metric with another is further detailed.

The relation between the PFS and $\mathcal{P}_{S, \text{BLIP}}$ is quantified by very close to 1 Pearson and Spearman indexes (Fig. 5.11) indicating a large positive correlation with a low spread. This illustrates the favourable use of the energy as more power is imparted by the fan into the flow, as previously mentioned. Nevertheless, light colours, depicting mounted-flush TE designs, deviate from the regression line, suggesting larger losses as the jet plume exits the exhaust at constant power input. Ultimately, these points are represented with higher viscous dissipative losses as a lower amount of useful work is reported (correlation between $C_{\Phi} \mathcal{S}_{\text{ref}}$ and both PFS and $\mathcal{P}_{S, \text{BLIP}}$ in Figs. 5.11 and 5.12). At constant power demand, i.e. constant fan operating condition, different amounts of losses are reported for the different designs downstream of the fan. Those losses inversely quantify the useful work and thus the propulsive force produced by the thruster. Yet, a very high correlation is reported between the amount of work provided to the flow and how efficiently it is used predominated by a polynomial distribution (as given by the Spearman indexes in H7 and I7 in Fig. 5.11).

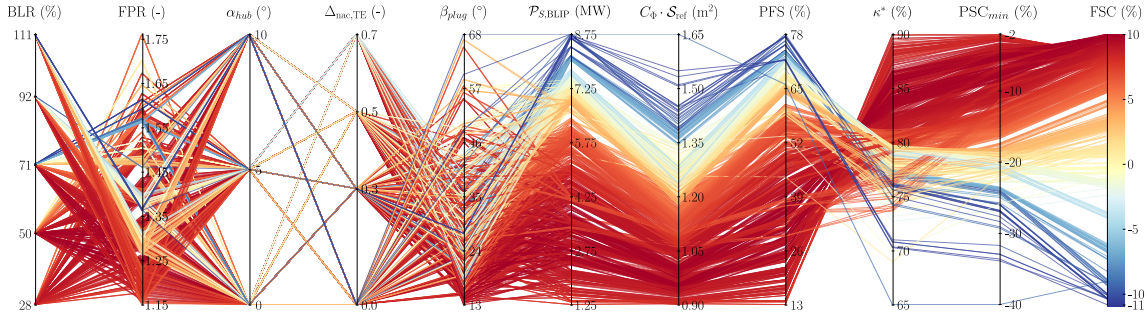


Figure 5.12: Parallel coordinate visualisation of the uncorrected filtered design space explored with their associated vehicle's performance: design space explored with 1,500 design geometries from which 150 are discarded due to geometric constraints and filtered out to 1,056 discarding excessive power requirements from the thruster

Innately, this efficiency is given by the propulsor efficacy κ^* . Although the spread is larger, as observed with the kernel distributions, the strong correlation can be explained by the large density of points on the higher-end of κ^* . Intrinsically, to obtain the highest efficacy, larger benefits arise from the energy harvested from the ingested flow. This translates to a smaller amount of power input by the fan to the flow at a target output force. Increasing further the amount of power in the flow can lead to larger amounts of output force (as discussed above) or favour losses. As such, the indexes report large negative correlations with the previously described metrics, κ^* , and notably with the viscous losses coefficient C_ϕ .

To reduce the candidate objective functions of a multi-objective optimisation (MOO), the Pearson and Spearman indexes are used. An intrinsic advantage of reducing the number of objective functions lies in the reduction of the sample generation to better direct optimisation. Notably, the strong positive correlation between the fan power input, the net assembly force and the viscous dissipation rate advises to reduce the objective functions to one. Additionally, a very strong negative correlation and monotonicity is reported with I6 to I8 in Fig. 5.11 between κ^* and the aforementioned performance metrics. This is a direct result of its definition and therefore either one could be used as an objective function. Although, a strong correlation between κ^* and FSC is observed (J9), the latter needs to be related back to the under-wing engine efficiency and the turbo-electric transmission system. Indeed, further optimisations on the power-plant's aerodynamic and propulsive performance will contribute towards larger savings relative to the reference aircraft.

To conclude, it is advised to reduce the objective functions to a propulsive force or input power by defining a target on the other quantity, in tandem with κ^* or FSC.

Although the trends given in Fig. 5.11(d) do not allow one to associate benefits to particular geometric characteristics, they provide an overview of the entire design space with respect to variations in the nacelle droop. To better understand the aero-propulsive effects the design variables have, Fig. 5.12 is discussed further in the following section.

5.4.2 Performance Sensitivity to Design Variations

To better understand the reasons behind the aircraft- and system-level aerodynamic performance at cruise, the design variables effects are evaluated. First, the reasons for the beneficial force redistribution on the aft aircraft are detailed. Then, the power requirement and losses are compared against propulsor efficacy and power savings. Finally, a design variable reduction is proposed to evaluate the PFC benefits on a narrowed-down design space.

5.4.2.1 Primary and Secondary Effects

To better understand the causes and effects related to the performance of a configuration, the different design variables are investigated separately and related to the performance metrics. To that end, the sensitivity, correlation and monotonicity of the performance metrics to the design variables are evaluated with several tools at the anti-diagonal quadrants (Fig. 5.11(b) and (c)).

The scatter distributions with their linear regressions, illustrate the effects of the design parameters given on the x-axis against the different performance metrics given on the y-axis. The linear regression represents the linear correlation that exists between two variables which is strengthened by the Pearson Index representing the degree of linear correlation with a scalar. A highly positive index indicates a strong positive correlation between the variables suggesting that an increase in the design quantity results in an increase in the performance metric. In conjunction with the Pearson Index, the Spearman Index describes a higher order correlation between the variables, indicating its monotonicity.

It can be observed that both BLR and FPR have a strong effect on the different performance metrics, as an increase in either quantity is translated by positive or negative trend lines. These will be characterised as primary effects as they prevail

over the so-called secondary effects, suggested by more refined design variations, α_{hub} , f_{plug} and $\Delta_{nac,TE}$. The secondary effects are described such that no direct strong correlation can be extracted out of their sensitivities, i.e. the linear regressions remain flat, indicating that they have a less significant impact. For a constant intake design, changes in the exhaust and nozzle designs imply variations in the force redistribution downstream, hence on the amount of useful work, or *thrust* produced. To decompose the effects the design parameters have on the performance metrics, their relationships are hereafter individually described.

5.4.2.2 Propulsive Force Share Sensitivity

The net assembly force represents the stream-wise balance of forces on the partial PFC assembly and is used to define the relative force of the BLI propulsor when compared with the bare fuselage (numerator of Eq. (5.11)). From the comparison of these isolated forces with the entire aircraft, the PFS reports the portion of positive contribution from the BLI propulsor and is used as a means of evaluating the performance of the PFC. With respect to BLR and FPR (A6 and B6 in Fig. 5.11), an increasing trend can be observed as the higher the mass-flow rate, and/or the pressure rise across the fan, the greater the amount of energy present in the flow and the larger the force output. Nonetheless, away from the regression line, some geometries suggest different PFS driven by the force experienced by the plug-cone that can be explained by an attached flow, or conversely separation, or even an undesired recirculation bubble. The positive trend is emphasised with strong Pearson and Spearman indexes with respect to the BLR and FPR (F1 and F2), suggesting a linear correlation.

After identifying BLR and FPR as primary variables, the PFS is depicted in Fig. 5.13 with respect to the other design variables. This split enables a better understanding of the dependence of the amount of positive force redistributed downstream the propulsor, independently from the mass-flow ingested and fan operating condition. In accordance with the design exploration, the breakdown is represented with different plug angles on the x-axis, and line colours and line styles for the droop and hade angles, respectively (intending to breakdown C6, D6 and E6).

As mentioned, an increase in either BLR or FPR dominates the change in relative forces. However, a change in monotonicity is observed with regards to the nacelle

5. Energetic Approach to Causal Analyses of Exhaust System Design

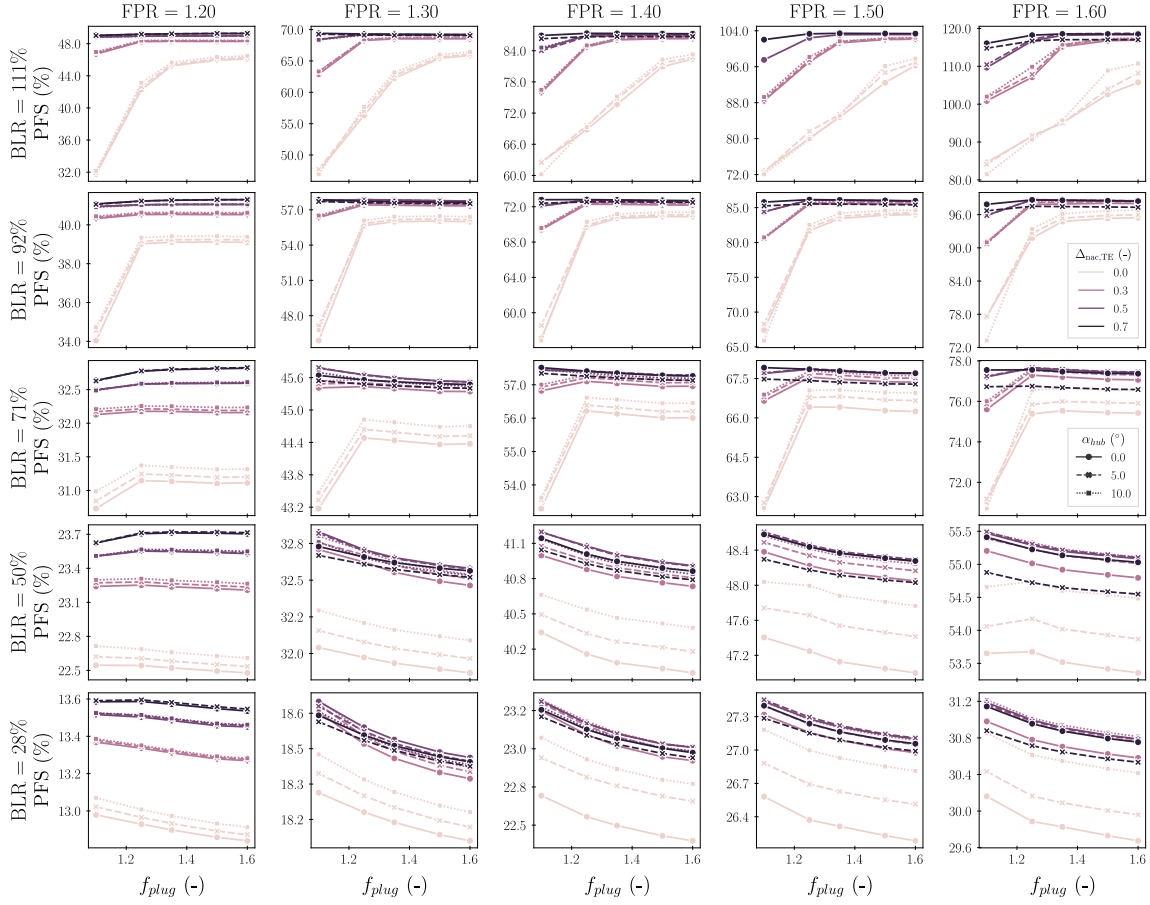


Figure 5.13: Propulsive force share sensitivity to the hade, plug and droop angles for BLR and FPR combinations

droop. The nested trailing edge offers a distinct better force redistribution than the aligned-edges geometries, in general, regardless of the hade angles. In addition, as the FPR increases, the hade angle affects the forward force as it is responsible for the initial flow direction before leaving the exhaust. To replicate a similar flow pattern, combinations of high fan-exit with high nacelle trailing edge radius or lower fan-exit with lower nacelle trailing radius showcase the higher benefits, as it prevents the non-beneficial forces applied on the forward facing wall surfaces within the exhaust. In other terms, favourable energy utilisation to be converted into useful work predominates over the generation of irreversible viscous dissipation. In tandem, a steeper plug downstream of the circular arc penalises the local force redistribution on the rearward facing surface and causes a decrease in the force production quantity. Overall, local maxima are achieved on the PFS at combinations of large droop and low plug angles. This is a direct effect of the highly pressurised jet-stream which pressure is converted into velocity and must be adequately directed towards the plug curvature to benefit from its redistribution of

forces (and useful work) otherwise favours recirculation and energy losses.

As the throat area increases, more mass-flow travels through the fan, which, subject to equivalent non-dimensional pressure rise, behaves differently downstream. However this growth scales the BLI propulsor's geometry both in height and length, pushing backward the nacelle TE and reducing the plug length (at a given fan location). For a constant plug hub radius, this increases the boat and plug angles, despite keeping the same ratio f_{plug} . For a given nacelle design, a higher plug angle increases the chord of the arc and prevents early flow separation which locally directs favourable pressure forces on the rearward facing wall surface, and the generation of useful work. On these designs, the larger the mass-flow, the less likely the flow remains attached to the wall surface as observed by the change in trend of $\Delta_{nac,TE} = 0.0$ for all FPR compared. As the FPR increases, the plug arc and steepness affect the PFS to a smaller extent than for the high radial position of the nacelle trailing edge droop but prevail for all designs. The monotonicity for the different droop angles swept is nonetheless conserved suggesting that the integrated designs offer better PFS.

With respect to the different plug angles, and associated arc lengths, the correlation between the PFS and β_{plug} changes from decreasing trends for low mass-flow to an increase in the PFS at higher BLR. This change in trend suggests a change in optima in plug angles as a function of BLR, as explained above. Nonetheless, this effect can be mitigated by the droop angle of the nacelle TE. Therefore, based on the parametrisation and design exploration, conclusions are drawn on the design effects on the force generation.

- The BLR has a primary effect on the performance of the PFC. As more mass-flow travels through the fan, the throat area is increased and the nacelle length is scaled accordingly affecting both the nacelle TE droop and plug angles which behave differently under different amounts of mass-flow.
- The FPR also has a primary effect on the system performance as it provides more energy to the flow leaving the BLI propulsor and directs more intensively the jet plume. This rise in pressure affects the flow and its likelihood of remaining attached to the plug's wall surface, therefore contributing to larger overall losses.

- The hade angle has a relatively negligible influence on the force redistribution but nonetheless can locally prevent the negative force distribution within the exhaust, and thus slightly improve the PFS.
- The plug angle has a relatively large contribution on the performance as an increase in mass-flow can either favour or prevent flow separation but its angle can mitigate losses.
- The droop angle has the largest effect of the exhaust design variables, as it also drives the plug hub radius and therefore the plug angle, exerting a combinatorial effect which scales with the throat area.

5.4.2.3 Fan Power Requirement

Similar to PFS, the power demand $\mathcal{P}_{S, \text{BLIP}}$ from the fan is inherently a function of the amount of mass-flow that goes through the fan stage (A7 in Fig. 5.11) as well as the pressure rise applied (B7), regardless of the design changes downstream of the fan. Indeed, the power demand is obtained from the change in total enthalpy flux between the fan outlet and inlet stations and therefore only captures the changes based on BLR and FPR. This illustrates the five points depicted A7 and B7 for a constant BLR where all exhaust designs are superimposed. As BLR increases, more power needs to be imparted to the flow to reach the same FPR, as a greater mass-flow is ingested and less benefits can be claimed from the excess energy present in the flow. Comparable correlations are obtained between the PFS and $\mathcal{P}_{S, \text{BLIP}}$ which is consequently translated by Pearson and Spearman indexes almost equal to 1 between the two performance metrics (G6).

This behaviour is observed as the geometry upstream of the nacelle is frozen for the rear-end design space explored. In the event that the nature of the flow changes upstream of the fan, the quality of the BL ingested with its excess in momentum and energy would play a role in the amount of power that would be required for a targeted output force. Nonetheless, this is not the case in this scenario and intuitively, a strong relation exists between either BLR or FPR and the shaft power (G1 or G2), as opposed to the changes downstream that do not play a role (G3 to G5).

5.4.2.4 Mechanical Energy Loss Sensitivity

As the pressurised flow exits the fan, changes in exhaust and nozzle design alter the performance of the system. Expectantly, low BLR and FPR combinations exhibit the least amount of overall losses $C_\Phi \mathcal{S}_{\text{ref}}$ (A8 and B8). The prevention of irrecoverable loss generation is enabled by harvesting the flow portion rich in energy that would have contributed to viscous dissipation generation in the aircraft wake. As both BLR and FPR increase, a larger overall dissipation is noted (A8 and B8) and emphasised by higher deviations with mounted-flush nacelle TE ($\Delta_{\text{nac,TE}} = 0.0$). For instance, $\Delta_{\text{nac,TE}}$ advises that different amounts of losses are obtained as the nacelle TE is more integrated, showcasing an optimum in the maximum overall losses obtained (E8). Although the nacelle droops effects are not represented by strong Pearson and Spearman indexes in H5 (little to no correlation exists), it appears clear that it has an important effect on the losses generated.

5.4.2.5 Effects on the Propulsive Efficiency Surrogate κ^*

Intrinsically, the propulsor efficiency actually recasts the non-dimensional thrust-specific power consumption and describes how effectively the power supply has been utilised through a form of useful work - the remainder being eventually dissipated. For these reasons, it is opined that the amount of mass-flow ingested plays the most important role in the efficiency of the BLI propulsor (here κ^*), along with how well the fan attenuates the flow, influenced here by the FPR. In the ARF, the BL inner layers are highly energised and offer the greatest benefits as harvesting this energy lowers the power required by the fan to produce a particular PFS. The correlation between BLR and κ^* (A9) is quantified by indexes of around -0.6 illustrating that increases in PFS require more power. This argument also holds true for the FPR (B9) which requires a larger power input to apply the same pressure rise as BLR increases. The decreasing trends are noted as both BLR and FPR increase highlighting that the amount of power input outweighs the additional thrust power generated and agrees with the higher losses experienced. Both Pearson and Spearman indexes emphasise the independence of κ^* with regards to the hade and plug angles (I3 and I4). Mild effects and correlations are noted with design changes downstream (plug and droop angles) and κ^* as suggested with I4 and I5. However, large values of f_{plug} and $\Delta_{\text{nac,TE}}$ (D9 and E9) prevent low efficiencies.

5.4.2.6 Power Saving Coefficient Changes

Despite the large density of points in PSC located between 0 and 10%, it is hypothesised that a large mass-flow under a high pressure cannot cope with the change in curvature given by the plug angle as the flow leaves the exhaust from a high radial position, $\Delta_{\text{nac,TE}} = 0.0$. This is illustrated by light yellow colours in the 10th row of Fig. 5.11 mainly due to the short plug arc leading to early separation and a less beneficial force redistribution along the plug-cone for the same amount of power input. Overall, greater savings are dominated by lower plug angles and larger nacelle droop angles as given by Fig. 5.12. The droop angle, to a greater extent, correlates with the FSC as its spread decreases as $\Delta_{\text{nac,TE}}$ increases (D10 in Fig. 5.11), notably since the flow is less likely to experience separation along the plug.

5.4.3 Propulsor Efficiency and Power Savings

Better performance is achieved as the nacelle TE radius embeds within the fuselage tail, and therefore to reduce the number of geometries to be further analysed, precedence is given to optima in PFS in Fig. 5.13 for each subset with respect to the nacelle droop. Accounting for the sole effect of the nacelle trailing

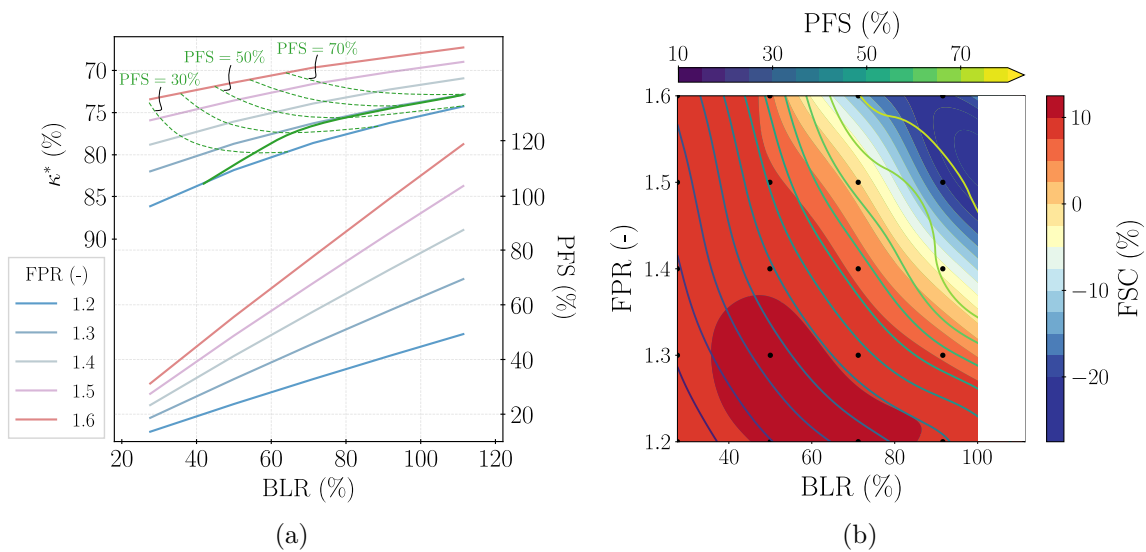


Figure 5.14: Computed performance of the best performing nacelle trailing edge droops in terms of propulsive force share (PFS) and (a) propulsor efficacy κ^* , with κ^* optima at target PFS (dashed green), and optimum (solid green), and (b) fuel saving coefficient (FSC) under different BLR and FPR combinations

edge droop for all different BLR and FPR combinations, the trade-off between the fuel savings and the PFS is represented in Fig. 5.14b. However, it is reminded that the computation of the FSC is made by retrofitting the baseline's engines, and absolute values of FSC would be positively altered by rescaling and optimising the engines therefore, conservatives FSC are reported.

Figure 5.14a depicts the change in gradient in PFS with regards to both BLR and FPR. Expectantly, for a given BLI intake design (BLR), increasing the FPR by providing more energy to the flow, comes to a higher cost than the thrust power as irrecoverable losses are generated, hence reducing the propulsor efficacy. Nonetheless, for a given PFS, local optima in κ^* are obtained from the different propulsor's operating conditions (dashed green curves). Considering the optimum of each local performance yields the solid green curve maximising κ^* . Despite not having captured the range of PFS below 20%, combinations of low BLR and FPR tend to showcase the best propulsor efficacies in spite of inevitably reducing the PFS quantity. However, it is stressed that designs of fans with FPR of 1.2 are challenging and this low value is only considered for the parametric study. It is also reminded that a transmission efficiency was not considered in this work, and that it would be favourable to operate at lower PFS because the amount of power transferred from the turbofan's low-pressure turbine is less, therefore less losses and weight of the transmission system.

The larger efficacies identified prove the benefits related to BLI as the sub regions of the BL are filled with energy. The harvesting of that energy available prevents excessive power input to produce a given output force. Nevertheless, it is stressed that diminishing returns on the BLI-related benefits are obtained as larger mass-flow rates are ingested. It is reminded that the weight of the BLI propulsor is not accounted in this study and therefore the savings claimed might be mitigated in the real case scenario. For instance, at PFS = 30%, two propulsors have an efficiency of $\kappa^* = 80\%$ but one ingests 62% of the BL mass-flow operating at FPR 1.2 and the other 43% at FPR 1.3. While more power savings could be obtained by operating at a lower FPR while ingesting more mass-flow, these benefits can be outweighed when accounting for the mass drawbacks related to installation. Also to mention, the fan stage is modelled with boundary conditions which ideally harvest all energy excess present in the flow and provide a constant momentum distribution at the outlet.

Ultimately, the performance of the BLI propulsor plays an important role in the

savings that can be claimed from the configuration. Notably, at a constant mass-flow ingested, a rise in pressure across the fan relates to a rise in $\mathcal{P}_{S,BLIP}$ and suggests larger amounts of loss within the exhaust jet, reducing the propulsor efficacy and the fuel savings (Fig. 5.14b). At low BLR, the flow is naturally rich in energy excess, requiring less power input from a *small* fan and therefore, a mild pressure rise showcases all benefits from BLI (large savings) but prevents important force shares. At larger mass-flows, the demand in $\mathcal{P}_{S,BLIP}$ increases much faster as less benefits are claimed from the ingestion of the upper portion of the BL and therefore, more loss is generated within the plume as the flow mixes with the wake. Despite benefiting the forward force portion which increases for a given pressure rise, smaller savings are reported. This inverse relationship between PFS and FSC is also given by Fig. 5.11, and a trade-off is found where less loss is generated within the plume while contributing from 25 to 40% of the system force and saving $\approx 10\%$ of power relative to the reference aircraft.

Regardless of the further considerations that can be accounted, the optimum FSC for a given PFS suggests a local maximum defining a local pareto front. From the recommendation given from Fig. 5.14a, i.e. the propulsor efficiency increases at a low BLR and FPR for a given PFS, Fig. 5.14b recommends a FSC local maximum of $\approx 10\%$ for a PFS between 25 and 40%, yielding a propulsor design that ingests 50% of the mass-flow present in the BL and operating at a FPR of 1.3. With respect to the design, parameters of $\alpha_{hub} = 0^\circ$ and $\alpha_{tip} = -10^\circ$ for the hade angles combination, f_{plug} of 1.10 and $\Delta_{nac,TE} = 0.5$ achieve this performance. This is partly explained by the preceding analysis where a low BLR coupled to a low FPR warrants the best benefits thanks to the fan modelling selected and the energy naturally imparted from the airframe to the flow.

5.4.4 Power Savings Breakdown

A decomposition of the power savings is enabled by considering different under-wing propulsion systems to attribute contribution to the aerodynamics or propulsion systems. With the consideration of under-wing ducted fans to complement the aft-mounted propulsor (in place of the turbofans presented), the PSC reduces to 4.1%. This considers pure aerodynamic effects whereby the propulsive fuselage concept is compared against the reference aircraft powered by two under-wing ducted fans and highlights the power reduction by ingesting the BL and its energy imparted

by the fuselage. As losses in the by-pass exhaust are introduced in the under-wing ducted fans, PSC increases to 8.5% showcasing 4.4 points related to the exhaust efficiency difference. The additional 2 points difference are therefore related to the engine core to total to 10.5% of FSC. Although these percentages only apply to the best candidate identified, they are applicable to systems in the same performance range. Additionally, these results are achieved when considering an axi-symmetric fuselage, in reality three dimensional effects would reduce the performance of the aft-end fan, e.g. larger distortion levels, and increase the fan power demand. It is therefore believed that the 10.5% reported herein are optimistic and unlikely to be achieved, but the consistency in the modelling allows qualitative comparisons and interpretations.

Additionally, the 4% savings attributed to the aerodynamics benefits agree with the findings from Giannakakis et al. [42] with their 4% in thrust-specific fuel consumption (SFC) when they assessed the influence of the mass change when considering turbo-electric propulsion architectures. An increase of 31% of the propulsion systems mass is reported which compounds to a 2.8% fuel burn penalty over their flight mission considered.

5.4.5 Compromise on Overall Performance

Considering the entire population as given in Fig. 5.11, the compromise between the PFS and FSC is given in Fig. 5.15, with the best performing geometry described earlier provided with its dimensions. It is opined that when considering a compromise between the FSC and PFS and not accounting for size, weight and transmission efficiency penalties, several candidates could be further analysed along the pareto. From a datum point and alluding to the understanding and previous explanations provided, a breakdown in improvements is permissible whereby an increase in BLR increases the portion of force provided by the BLI propulsor as well as an increase in the FPR however, they both occur at the cost of power savings. Both parameters impart more work to the flow but respectively the losses attributed to Φ and Θ_{irr} . The high-end droops more favourably direct the outflow that positively contribute to a larger force along the plug but to prevent separation, a compromise between the arc and the plug steepness must be ensured to adequately propel the aircraft for a trade-off between power consumption and useful work.

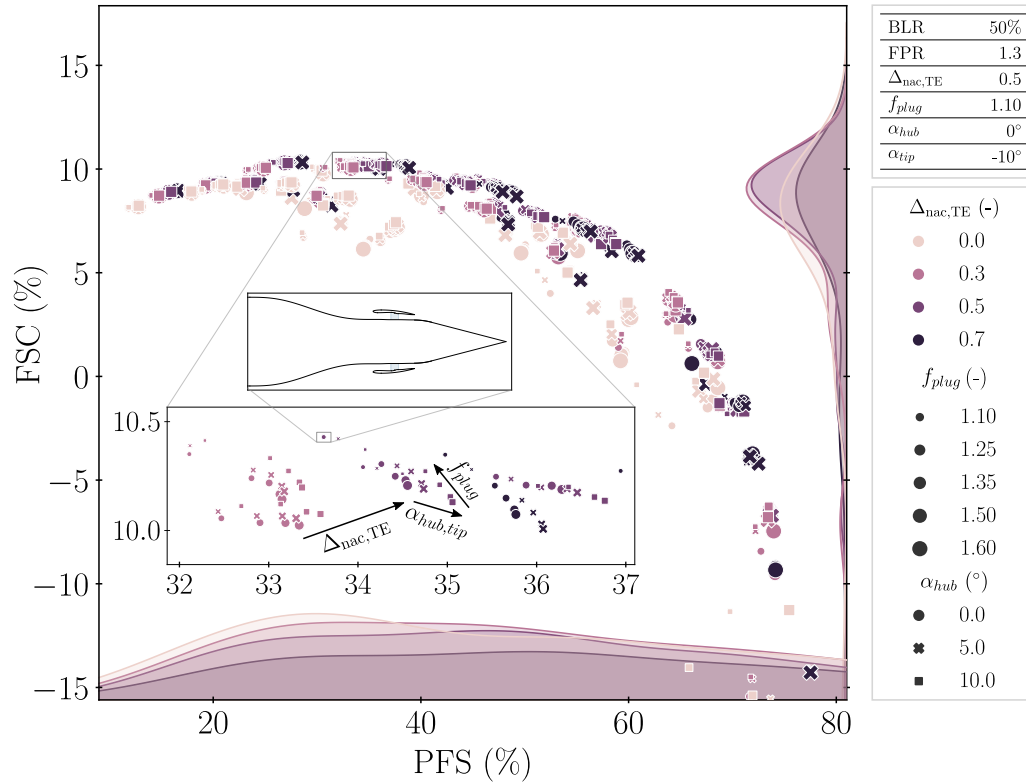


Figure 5.15: Computed fuel saving coefficient with respect to the different thruster designs embedded at a given position, and using retrofit under-wing turbofans

The supplementary kernel distributions on both PFS and FSC in Fig. 5.15 indicate the repartition and density of the geometries within the matrix. It is noted a roughly even distribution along the PFS axis as a result of the quasi-systematic spread of BLR and FPR combinations. However, those contributions are achieved for different shaft powers that are partly described by the FSC. Notably, the most integrated nacelle rear-ends are concentrated in a positive region of interest (dark tones) whereas the lowest droops suggest a more even distribution (light tones) which spread in less-beneficial scenarios. This comforts the choice of the geometry at those particular flight conditions for the given inlet constraints nonetheless, it is of interest to investigate the contributions of Φ and Θ within the domain along their decompositions in the different flow features. As a matter of fact, the work provided by the vehicle to the flow is either utilised or wasted and as such, the promotion or inhibition of the integrated performance can be characterised. While Φ is only positive and can therefore enable a local decomposition, Θ can only be considered globally if not split into its reversible and irreversible components.

5.4.6 Volumetric Viscous Dissipation and Pressure Work

The irreversibly lost energy that manifests itself as viscous dissipation can be characterised along with the volumetric pressure work. Figure 5.16 illustrates the dependency of both these variables, Φ and Θ , and the total mechanical loss Φ^* , for the different BLR and FPR clusters after the variable reduction previously introduced.

Expectantly, Φ is correlated to BLR and FPR (Fig. 5.16a) which, as a coupled effect, increases at higher rates for larger combinations, as more work is provided to the flow from the fan. Through the expansion process within the jet plume, the overall pressure work (Fig. 5.16b) suggests a converse trend wherefrom larger amounts of mass-flow that go through the propulsor contribute greatly to the forward force production. Notwithstanding its greater contribution with respect to both BLR and FPR, the viscous dissipation still outweighs those benefits albeit flattening for lower FPR (Fig. 5.16c). An indication on the performance is therefore provided on the low FPR and high BLR propulsors which greatly contribute towards positive force (thrust direction) while not imparting more irrecoverable losses into the flow thanks to the expansion process. That combined increase therefore covers a larger PFS range (Fig. 5.16d) hence, suggesting optima to the lower-half range of PFS. The further increase in PFS results in irrecoverable loss which lowers the benefits claimed by the configuration.

However, it is important to stress that any increase in gauge static pressure results in a positive contribution in Θ which highlights a conspicuous caveat with respect to Θ . The highly compressible nature of the flow between the fan-face and face-exit presents a large positive contribution that is omitted by the BC used. Tse [43] demonstrates the variation in $P_V = -\Theta$ between the propulsor inlet and varying outlet locations which integrand presents a large positive contribute of Θ . Nonetheless, its proportion relative to the overall pressure work is yet to be defined, and is expected to depend on both BLR and FPR, which in other terms means that the replication of this study with a body-force model (BFM) rather than BC would impact the overall pressure work as well as the system's losses and thus impacts the benefits. Albeit being confined in the small region of the fan stage, it can be hypothesised that Θ should appear positive, and even greater with respect to both BLR and FPR. A physical interpretation could stand in the

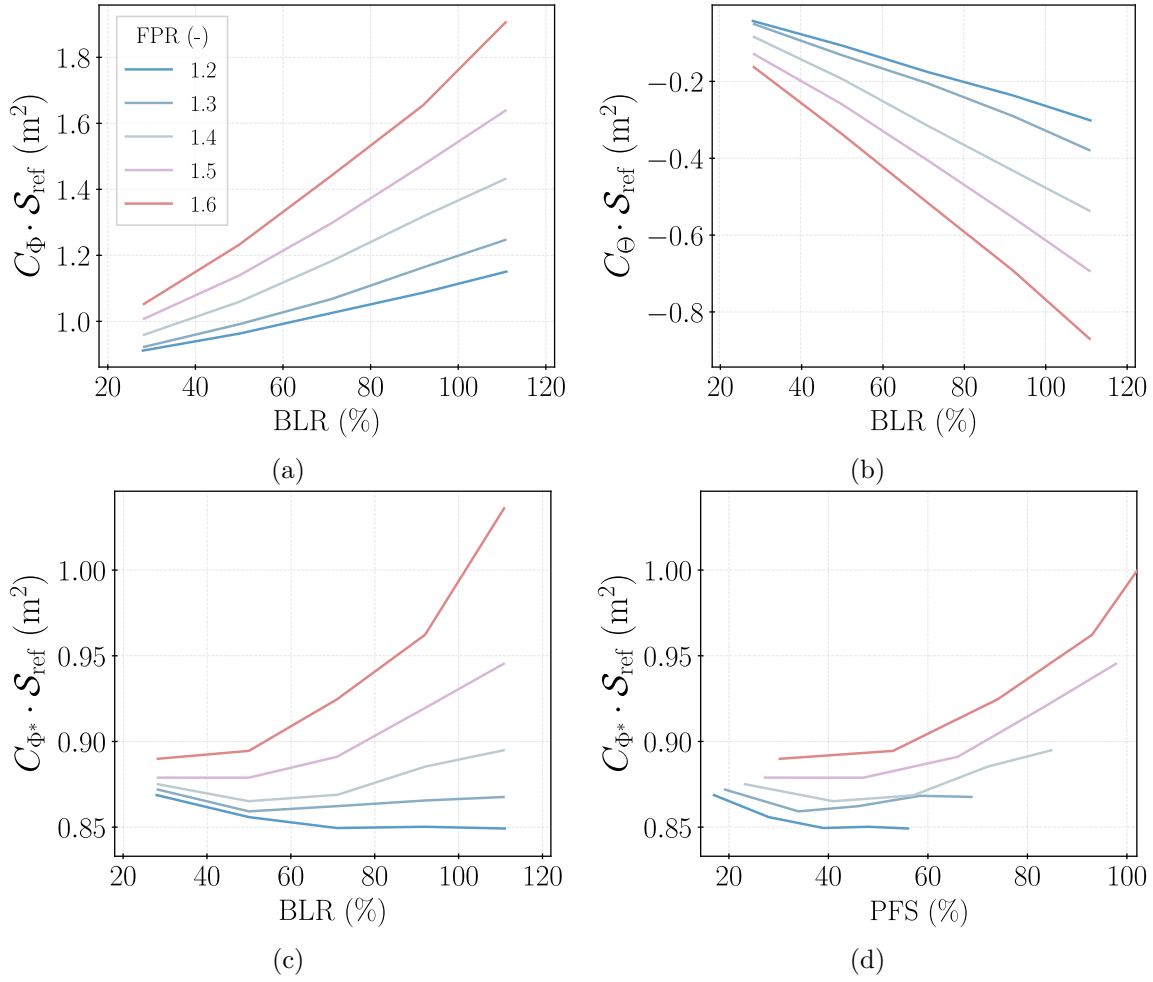


Figure 5.16: Computed volumetric rate of (a) viscous dissipation, (b) pressure work, and overall losses as a function of (c) BLR and (d) PFS

amount of mass-flow that is displaced by the amount of work imparted, echoing the displacement thickness defined in the BL properties.

For the reason that, although being correct for the study herein presented, Φ^* loses its full meaning because of the misleading interpretation that follows Θ . As such, Φ is favoured to characterise the performance of the system from a reduction of its quantity, i.e. a reduction in dissipation insinuates a better use of the provided power and hence a better performance as Φ is a positive-only term. However, it could be argued that the fan contribution to Φ is also missing but that would not change the trends obtained in Fig. 5.16 albeit increasing their growth gradients.

The comparison of the viscous dissipation and pressure work for all vehicles designed at BLR = 50% and FPR = 1.30 is given in Fig. 5.17. The changes in droop and plug angles non negligibly allow the design to be further refined and attenuate the losses occurring in the flow. Notably, the comparison between the different designs and

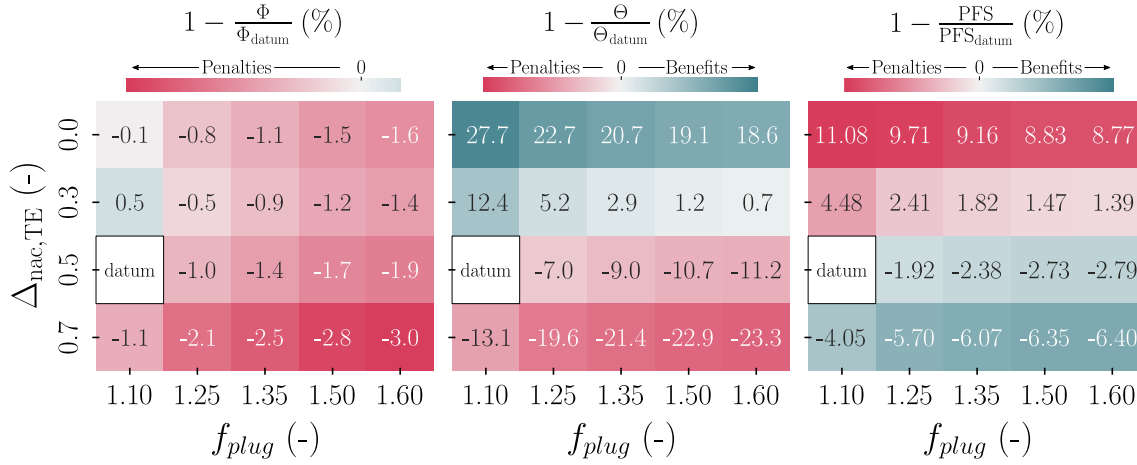


Figure 5.17: Computed volumetric rate of viscous dissipation and pressure work, and propulsive force share as a function of droop and plug angles, at datum BLR = 50% and FPR = 1.30, neglecting hade angle effects

the best performer identified from Fig. 5.15 as datum, highlights the importance of both angles. At a given nacelle TE droop, smoothening the circular arc of the plug and increasing the steepness of the plug downstream of it enhances the shear stress and the viscous dissipation, and conversely, an abrupt plug cone reduces the viscous loss. This observation cannot be generalised and only applies to the investigated size as the mass-flow rate and pressure rise are reasonably low allowing the flow to remain attached to the plug wall surface.

Despite the $\pm 2\%$ error on FPR, the trends remain similar whereby penalties in forward force are obtained for mounted-flush nacelle trailing edges and increase with large plug angles. Intrinsically, this does not only affect the forward force but also the irrecoverable loss, power consumption and hence savings.

At constant power addition $\mathcal{P}_K^*$, any change in the losses Φ results in changes in either Θ or Wh to match a specific net force or would otherwise generate a force imbalance that will be represented by the PFS (Fig. 5.17). It has been seen in Fig. 5.16 that the coefficients do not have the same amplitude and therefore the relative differences depicted here do not have the same extents. There is a caveat in the absolute value and relative difference in Θ but it is nonetheless fair to assume that apart from the local pressure gradient difference that arise from changes in design, Θ should be constant up to the fan stage. To relate its quantity to the performance, its value must be positive and as low as possible. However, the sum of smaller Φ and Θ implies smaller net force whereas it also exists combinations of smaller, or greater, Φ and Θ which indicate a change in rate of potential energy.

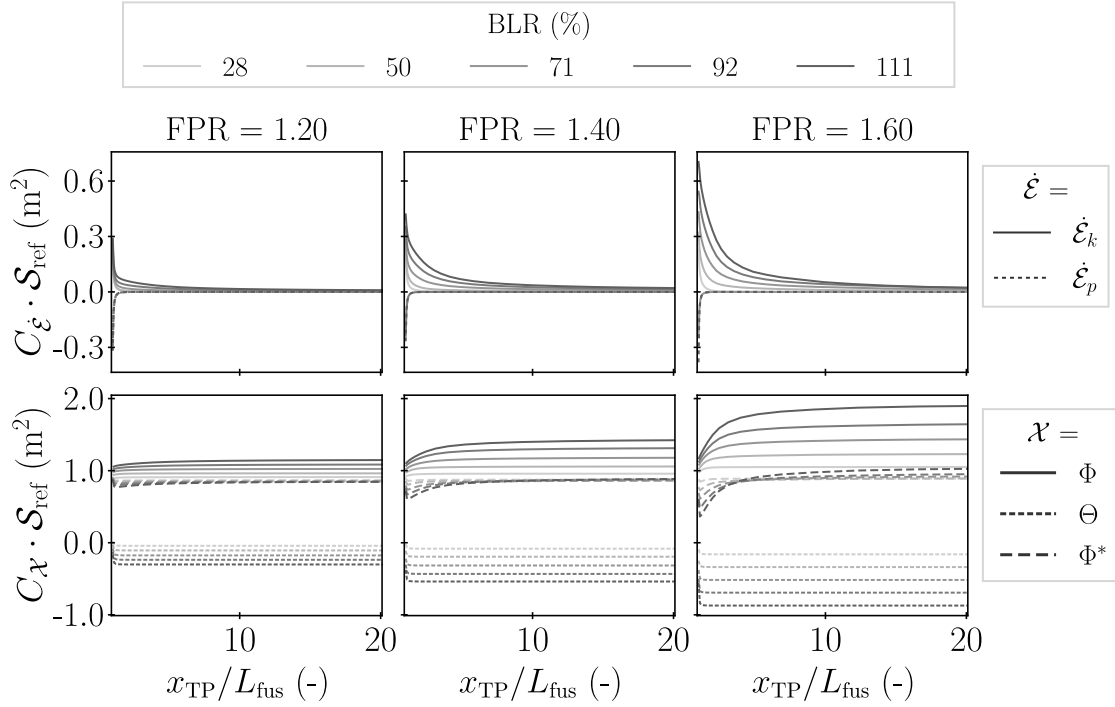


Figure 5.18: Computed energy flux transfer between the kinetic energy deposition rate and the volumetric rate of viscous dissipation, pressure work and overall sink for various BLR and FPR

5.4.7 Energy Flux Transfers

Another important aspect that helps understand how efficiently the provided power is used is through the energy flux transfers between the different sources and their conversion into losses. Figure 5.18 represents the preceding matrix with the transfer of power in the flow region behind the aircraft, i.e. $x_{\text{TP}}/L_{\text{fus}} > 1$. The first row represents the decay of the mechanical energy present in the flow as it attempts to resume to its initial quiescent state. Respectively, the bottom row illustrates the accumulation of Φ and Θ during the process. Notably, a rapid exchange takes places between the $\dot{\mathcal{E}}$ terms and Θ in response to the isentropic expansion within the jet plume. The remaining quantity of $\dot{\mathcal{E}}$ diminishes at a slower rate as a result of shear within the flow, represented by the accumulation of viscous dissipation Φ . The transfer processes suggest a precedence given to Θ which remainder is eventually lost. Interestingly, the left-over amount of energy appears more important for large FPR and increases with respect to the mass-flow that travels through the fan. This indicates that the surplus comes to a cost and lowers the savings offered by the configuration while not enhancing its propulsive performance.

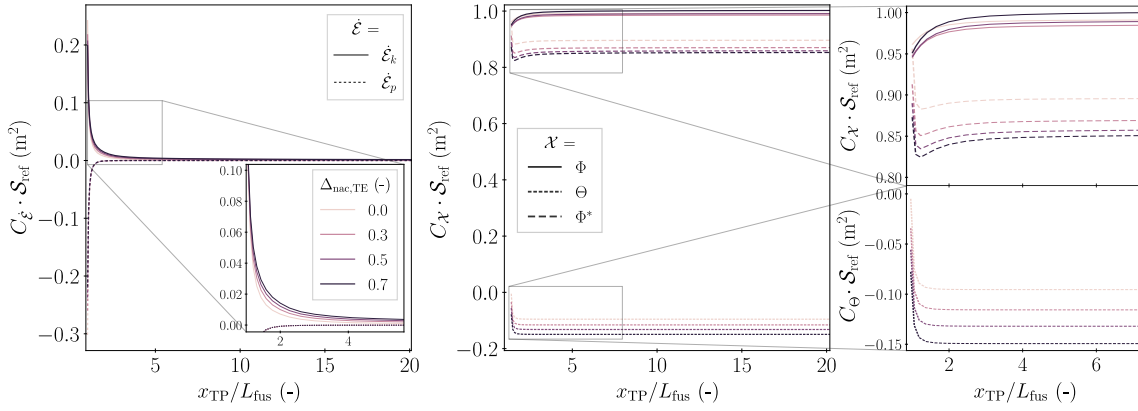


Figure 5.19: Computed energy flux transfer between the kinetic energy deposition rate and the volumetric rate of viscous dissipation, pressure work and overall sink as a function of the nacelle droop

To echo the fallacy presented on Θ , a BFM representation should invert the monotonicity observed whereby higher FPRs imply higher compression and higher local Θ shifting upward its quantity at $x_{TP}/L_{fus} = 1$. As the available energy is getting converted, the higher BLRs experience higher amounts of pressure work which residual quantity (non-isentropic) lies higher than for low BLR.

Although Fig. 5.18 describes the influence of both BLR and FPR in terms of power transfers, an interest exists in characterising the influence of the hade, plug and droop angles. Due to the subtle variations in hade and plug angles, quasi-insensitive transfers are reported and therefore, only the droop influence is depicted in Fig. 5.19 for the BLR and FPR combination of the best performing candidate (BLR = 50% and FPR = 1.30) and its hade and plug angles.

Starting with the nacelle trailing edge that is mounted-flush with its front-end, the available energy $\dot{\mathcal{E}}$ is less effectively used as given by the highest dissipation (for a given fan energy deposition rate). It is noted that the penalty in additional dissipation is caused upstream of the vehicle's trailing edge, meaning that losses occur within the exhaust or along the plug. By embedding the nacelle rear-end into the fuselage tail, it is observed the same precedence for Θ which remaining energy flux dissipates from shear but it is noted that while the three lowest $\Delta_{nac,TE}$ dissipate a roughly similar amount of energy Φ at $x_{TP}/L_{fus} = 20$, a better utilisation of the energy is obtained for the middle design of the three. An explanation lies in the amount of energy that has dissipated just downstream of the propulsor. At $x_{TP}/L_{fus} = 1$, Φ of both $\Delta_{nac,TE} = 0.3$ and 0.5 are smaller than for 0.0 and 0.7 indicating better designs between the fan-exit and the fuselage end-point. As for Θ , it is fair to assume that the pressure work is identical upstream of the fan at

constant BLR and just downstream at the same FPR. The difference reported at $x_{TP}/L_{fus} = 1$ thus suggests a larger expansion process that occurs within the exhaust and upstream of the trailing edge and hence contributing to a higher performance as the flow leaves the rearward facing plug surface.

To summarise, it appears that higher BLR and FPR are responsible for larger quantity of dissipation and pressure work (Fig. 5.18) but this is also affected by the exhaust/nozzle design. This agrees with the design variable reduction performed which highlights different combinations of design parameters for the different propulsor operating conditions explored.

5.5 Flow Feature Decompositions

It is of interest to segment the flow-field into its different features and breakdown the contributions of Φ and Θ , relative to the overall losses present in the flow Φ_∞ and Θ_∞ (equivalent to the reported Φ and Θ in the previous section, to prevent confusion). As such, the main flow-features that are the shear layer, shock and jet plume are analysed, where the latter is further decomposed into the compression, expansion and mixing regions.

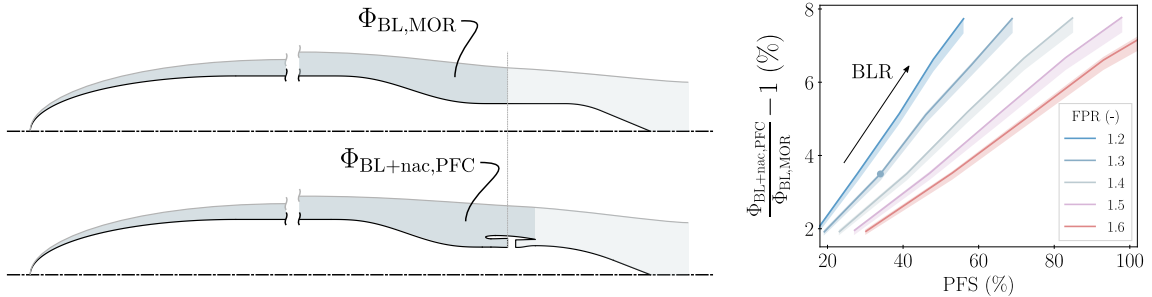


Figure 5.20: Integration penalties in terms of viscous dissipation rate between the installed nacelle configuration (PFC) and the baseline morphed fuselage (MOR) with respect to different propulsor sizes, fan operating conditions and nacelle shapes as explored (shaded areas), with an identified datum (marker) for further comparisons

5.5.1 Main and Secondary Boundary Layers

5.5.1.1 Integration Penalties

After isolating the shear layer from the rest of the domain using ξ [16], a geometrical split is made at the nacelle's trailing edge whereby its portion upstream is characterised as the BL, and downstream as the wake (Fig. 5.20). The BL is aimed at being further decomposed into a main BL that forms over the fuselage and the secondary viscous layer that stems from the cowl's leading edge (nac), which both mix above the cowl and combine farther downstream in the wake.

The propulsor integration penalties are first assessed from a BL growth and intrinsic shear losses relative to the morphed fuselage (unintegrated) whose BL is trimmed at the fan-face location of the integrated propulsor (Fig. 5.20). Despite not being ideal, this geometric split aims at segmenting the BL from the wake in both geometries for a fair comparison where the primary flow entering the fan is then assigned to the jet feature and therefore, its part within the intake should be accounted in the integration losses.

Figure 5.20 depicts the shear layers and geometric segmentation for both designs which viscous dissipation rates Φ are given on the right-hand side. Whereas the losses contained within the BL are insensitive to the FPR if plotted versus the BLR, the parameter is included to further decompose the losses when compared with the PFS. A given PFS can be achieved by several BLR and FPR combinations which are given by the different solid lines obtained from the best performing candidates defined earlier yielding different losses, with slight variations given by different nacelle droops, or designs (shaded).

5.5.1.2 Propulsor Size Influence

In essence, the amount of viscous dissipation within the BL increases with respect to the mass-flow ingested by the fan as the propulsor size increases, yielding a greater wetted surface over the nacelle, which enhances the viscous dissipative effects, insensitively to the FPR. The BL growth caused by the second forming BL favours the shear-stresses in the mixing region of the main and secondary flows over the cowl contributing towards a higher dissipation. Within the propulsor's intake, an

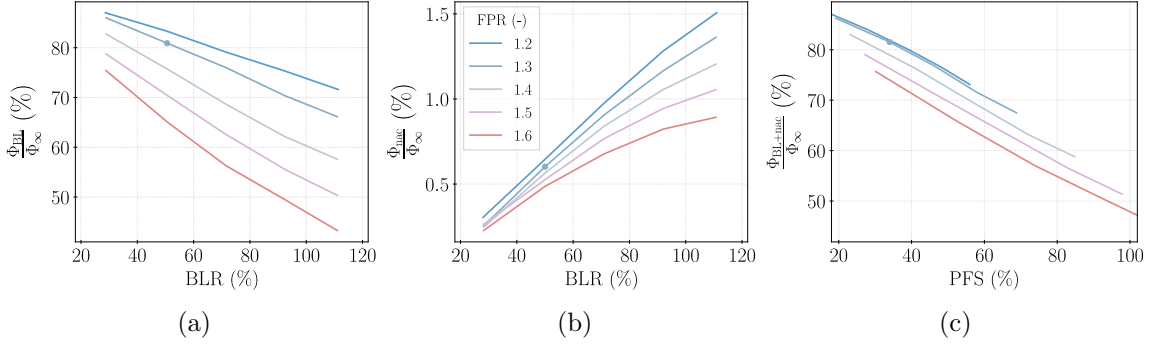


Figure 5.21: Computed non-dimensional dissipation in the (a) main, (b) secondary and (c) combined boundary layers for different BLR and FPR combinations

extra contribution results from the larger intake length that increases with respect to the amount of mass-flow ingested (as a result of a larger throat area), despite the smaller velocity gradients experienced on the nacelle interior walls. Overall, a minimum increase of 2% in viscous dissipation rate is reported for a fan ingesting 28% of the BL mass-flow. These losses increase up to 8% for the explored design space as the nacelle is scaled up both radially and axially (ingesting the entire BL). This increase in irrecoverable loss is non-negligible as it would need to be mitigated by the jet losses to perform as well as the smaller propulsors.

To further understand how the viscous losses within the BL scale to the overall losses with respect to the propulsor size and fan operating conditions, Fig. 5.21 depicts the decomposition between the main BL, and the secondary one developing around the propulsor's cowl (nac). All dissipations are non-dimensionalised by the total amount of viscous losses Φ_{∞} within the entire domain from the far-field to the Trefftz plane. On the main BL, a decreasing trend indicates that although its absolute contribution increases with the propulsor size, its relative one is outweighed by the larger dissipative effects that take place in the wake and the jet-stream regions as more mass-flow and more pressure is provided to the flow (Fig. 5.21a). Notably, for the design space explored, the relative BL viscous losses vary between 43 and 87% of the overall losses while the absolute BL dissipation amounts to around 89-92.5% of the un-installed baseline drag power which mitigates a maximum recovery of 11% at low BLR.

With the integration of the propulsor, the additional dissipation taking place from the propulsor's cowl is observable in Fig. 5.21b ranging from 0.3 and 1.5% of the total dissipative losses of the design. Nevertheless, this drawback enables energy harvesting before conferring additional power to the flow. Lumping those losses

with the former BL, a maximum recovery of 5.5 to 11% is envisaged with these designs when compared against the un-installed baseline.

Lastly, Fig. 5.21c represents the losses contained within the combined BL relative to the overall vehicle's losses. It is noted that for FPR's of 1.2 and 1.3, similar losses portions within the BL are obtained, suggesting that an equivalent remainder is contained within the other features. It is therefore preempted that optima can be found where the jet plume region is the lowest as a larger wake attenuation is achieved, i.e. for a given size greater attenuation results in less wake dissipation as the wake-only feature should remain equivalent.

5.5.1.3 Nacelle Shape Influence

Retrieving the results from Fig. 5.20, changes in the nacelle design for a given size do not largely contribute towards greater losses but scale with respect to the propulsor size. At the particular cluster of BLR = 50% and FPR = 1.30, the nacelle and nozzle shapes influence is evaluated by means of comparison with the best performer (datum). Despite trimming geometrically the BL at the nacelle TE, the plug angle indirectly influences the viscous dissipation by affecting the overall losses present in the domain Φ_∞ by altering its pressure gradients in the surrounding flow, hence its viscous losses portion to the total losses. After ensuring that the hade angles were not influencing the viscous dissipation, Fig. 5.22 depicts the difference in viscous dissipation rate within the entire BL, i.e. the relative difference between the viscous dissipation rate of a geometry present in the BL and that of the best candidate, labelled as datum.

Relative to datum, negative values represent a higher contribution of the BL losses. It is noted a mere difference between the designs at constant operating conditions with respect to both the nacelle TE inclination and the plug arc and steepness. Notably, the mounted-flush nacelle presents a bell-shaped plug arc favouring the flow acceleration at the exhaust's exit whereas the integrated designed smoothen the plug shape showcasing a strong adverse pressure gradient at the nacelle aft-cowl, decelerating the over-cowl flow velocity and enhancing the shear stress and the viscous dissipation rate. The plug angle has a low influence on this metric as a larger arc chord merely affects the gradient along the plug arc but presents a larger contribution at the TE of the plug due to the increased steepness.

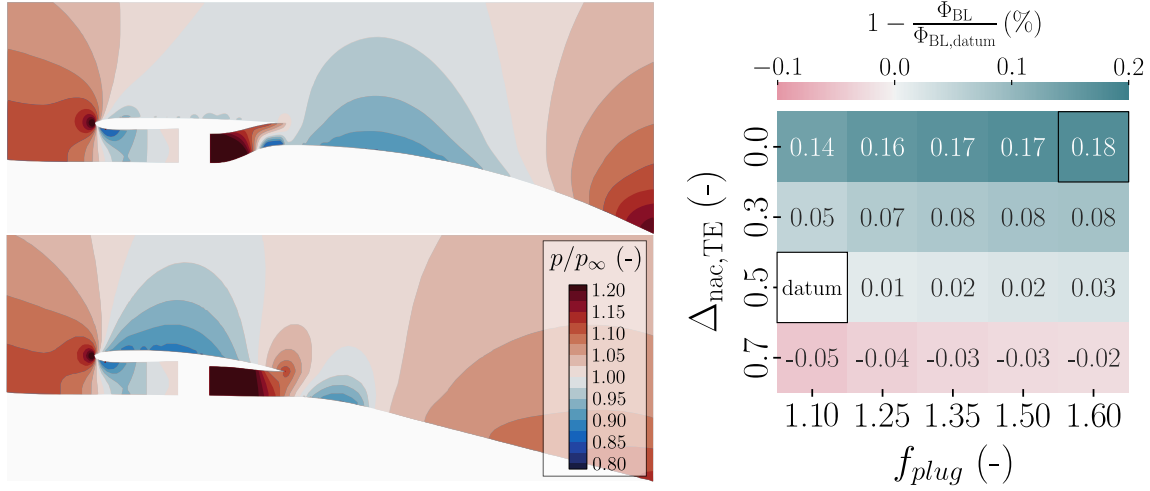


Figure 5.22: Normal static pressure contours from the two boxed designs (datum on bottom) with the computed difference in viscous dissipation rate within the BL between datum and the geometries at BLR = 50% and FPR = 1.30

Overall, it is opined that the geometries experience relatively similar dissipation rates in the BL, with differences below 0.5% (Fig. 5.20) yielding to between -0.05 and 0.18% relative to the overall propulsive fuselage viscous losses.

Although these integration penalties exist and amount to 2-8% of additional viscous losses, they are inevitable to allow energy harvesting from the viscous flow. Assuming a through-flow without additional penalties within the propulsor, this leaves larger losses within the wake due to the delay in dissipation which can be mitigated by propelling the fuselage from its aft. To mitigate those losses through the BL ingestion, the losses within the jet plume are assessed with respect to the fan operating conditions and propulsor design.

5.5.2 Propulsive Fuselage's Plume

5.5.2.1 Propulsor's Operating Conditions

A consequence of a larger vehicle surface is a higher drag power contribution that eventually dissipates within the wake. As the amount of mass-flow ingested by the fan grows, a relatively larger dissipation emanates from the larger propulsor's nacelle which directly participates in some integration penalties, as explained. Additionally, the delay in the shear dissipation process implies part of the amount of work imparted into the flow to decay overtime. In comparison with the morphed geometry, the totality of the dissipation that has not happened within the BL, i.e. along the

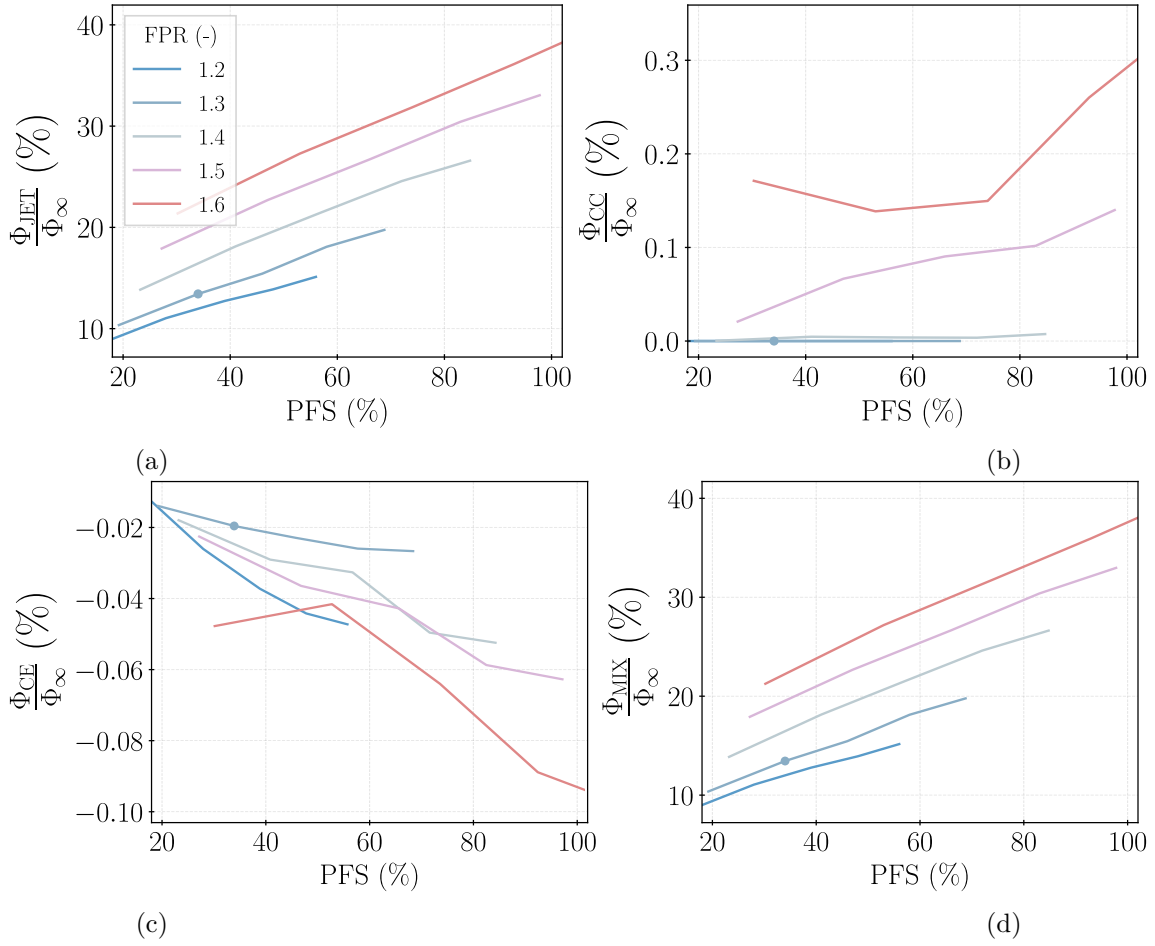


Figure 5.23: Computed portion of the viscous dissipation rate in the (a) jet plume, (b) core-compression, (c) core-expansion, and (d) jet/wake mixing regions relative to the overall viscous loss with the previously identified datum design (marker)

vehicle's surface, would take place behind the aircraft whereas in the case of BLI, an attenuation of that wake is enabled by a partial or total ingestion of the viscous flow.

To initiate the characterisation of this attenuation, the flow leaving the propulsor is identified by the jet-stream identifier Υ following the precedence methodology. Figure 5.23 respectively depicts the entire jet-stream, and its segmentation into the core-compression and -expansion, and the mixing regions whereby, the power sink Φ is non-dimensionalised by the overall vehicle's viscous dissipation Φ_{∞} .

Fundamentally, Fig. 5.23a depicts a higher contribution of dissipation within the jet as the mass-flow increases and the mixing losses intensify (Fig. 5.23d). Nonetheless, their rates of progress vary as a coupled effect between BLR and FPR where the amount of additional work provided by the fan is greater than the one harvested and therefore contributes towards larger losses although being responsible for greater

propulsive forces. At a target PFS, it appears clear that several propulsor designs meet the requirements but impart a larger loss which is transcribed by a larger power requirement. At larger FPR, shocks are experienced within the jet plume which partly contribute to losses, and intensify with larger mass-flows (Fig. 5.23b). Although their contributions fall below 1% of the overall dissipation, the shocks imply a greater dissipation in their surrounding cells that is not captured from ζ . Numerically, larger losses should be captured in either the core-expansion or mixing regions, as defined per the control volumes, but an unphysical negative loss is reported for the former which is fair to assume that these losses are purely numerical. Although being negative, the values are negligible and the flow is assumed to expand fully isentropically (Fig. 5.23c). The small regions of the flow captured by the core-compression combined with the isentropic nature of the expansion process leave the mixing region, between the wake and the jet, to be the major contribution of losses. These losses increase with both BLR and FPR as more shear stresses are experienced. These stresses result from friction activities between the low velocity of the air particles within the wake that are entrained by the high-speed jet flow. One of the BLI objectives is to mitigate these mixing losses by attenuating the wake from a trade-off between energy harvesting and further energy deposition rate from the fan to fully fill the defects and not contribute towards greater losses, or effectively reduce them.

5.5.2.2 Exhaust and Plug Designs Influence

Retrieving the best performing geometry (marked as datum), its propulsor operating condition is isolated to solely assess the influence of the droop and plug angles on the losses. The relative difference between all the designs and datum is depicted in Fig. 5.24 whereby a positive difference suggests a local benefit.

Expectantly, the high radial position of the nacelle TE (mounted-flush) positions the nozzle hub radius also high radially to maintain a choked nozzle with its corresponding surface area. The short arc chord on the plug given by a low f_{plug} accelerates the flow leaving the exhaust potentially leading in separation after a strong shock (at high FPR as observed in Fig. 5.25). At lower FPR's, a weak shock or series of shocks is observable along the plug's convex surface. The shock forming develops within the jet/wake mixing region which generates strong shear stresses and irrecoverably dissipates the energy into heat. Figure 5.24c depicts a

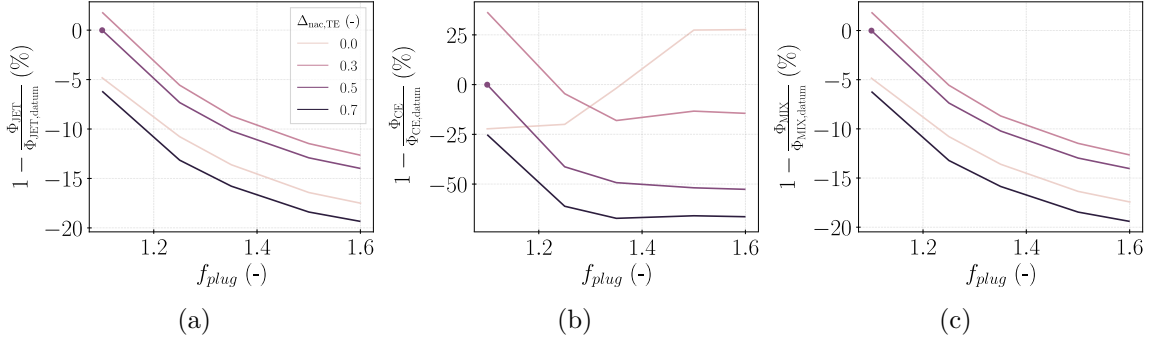


Figure 5.24: Computed relative difference in viscous dissipation rate in the (a) jet plume, (b) core-expansion, and (c) jet/wake mixing at BLR = 50% and FPR = 1.30 with datum (marker)

local dissipation increase of 5% in the mixing volume relative to datum whereas the expansion process that follows the shock also experiences part of the dissipation as noted by the -25% difference in Φ_{CE} of Fig. 5.24b. Nevertheless, it is reminded that these isentropic losses are a pure product of the numerical split and the large discrepancies with datum are on negligible values. Additionally, the non monotonicity observed for $\Delta_{nac,TE} = 0.0$ at low plug angles is caused by the rapid flow acceleration generating shocks and separating the flow from the surface. These effects are not noted for the other designs as the nacelle aft is more nested within the fuselage favouring a continuous streamlined flow along the plug.

Apart from this mechanism, a general trend is observed with respect to the plug shape. As the arc chord is extended and the plug cone angle increases downstream, more dissipation occurs within the jet plume (Figs. 5.24a and 5.25). A stronger adverse pressure gradient (observable in Fig. 5.22) is experienced by the flow hence showcasing a region of low velocity close to the trailing edge of the vehicle which increases the velocity gradient within the plume and stream recirculation that enhances the shear stresses.

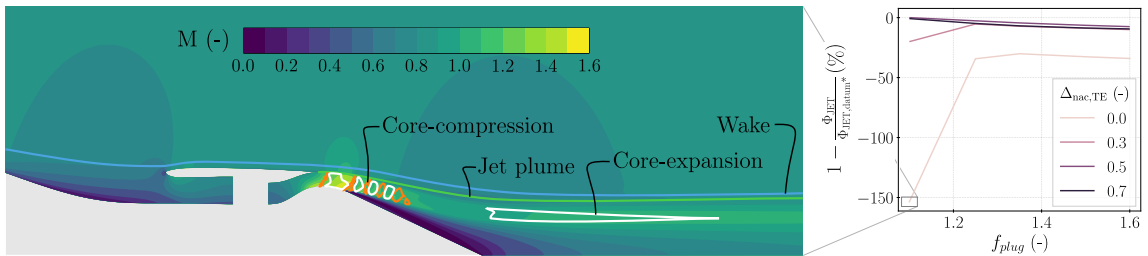


Figure 5.25: Mach number contours of the boxed geometry at BLR = 92%, FPR = 1.50, $\alpha_{hub} = 0^\circ$, $f_{plug} = 1.10$ and $\Delta_{nac,TE} = 0.0$, and droop and plug angles effects on the jet viscous dissipation rate

With respect to the offset between the leading and trailing edge radii, the plug angle and its term f_{plug} are also implicitly affected by the changes in the droop. This causes the dissipation phenomena to be partially explained by the above comments made. Notably, a $\Delta_{nac,TE}$ of 0.3 generates less loss than the other designs suggesting that an optimum potentially stands in between 0.0 and 0.5 and therefore a particular exhaust shape combined with a plug arc allows a better utilisation of the energy present in the flow. As the propulsor size increases and ingests more mass-flow, the highly pressurised flow is further accelerated along the bell-shaped plug leaving the wall's surface with a separated region underneath (along the centreline). Intrinsically, these losses are directly related to the arc shape of the plug which causes different degrees of shocks and flow recirculation and mitigation of these losses is enabled by increasing the arc curvature (Fig. 5.25).

5.5.3 Wake Attenuation

To evaluate the wake attenuation from the different geometries explored, Fig. 5.26 is used to depict the portion of losses within the jet and wake-only volumes relative to the morphed unintegrated design's wake loss. Similar to the comparison made on the BL with Fig. 5.20, the best geometries for various propulsor sizes and FPR are given with solid curves where shades represent variations in performance from the different nacelle and plug designs swept.

It is noted that for both FPR of 1.2 and 1.3, similar relative losses are experienced within the jet and wake tending to reduce the wake losses by conveying additional propulsive force. The relative low work input from the fan mitigates the additional losses generated while conveying a mild propulsive force. As the mass-flow increases

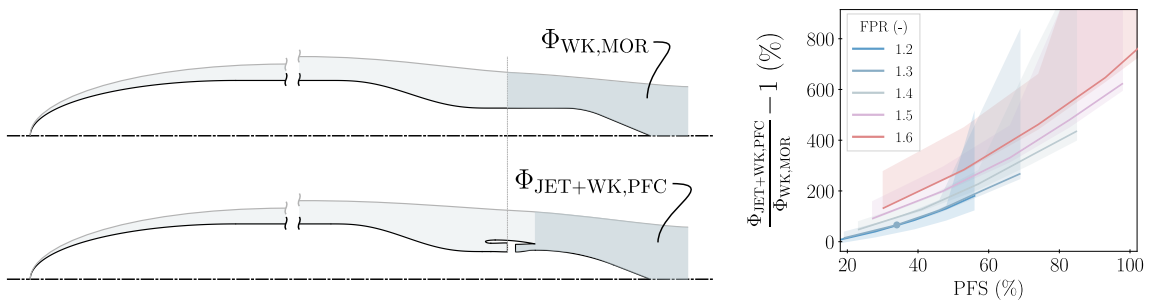


Figure 5.26: Viscous dissipation rate attenuation within the jet and wake between the installed nacelle configuration (PFC) and the baseline morphed fuselage (MOR) with respect to different propulsor sizes, fan operating conditions and nacelle shapes as explored (shaded areas), with the previously identified datum design (marker)

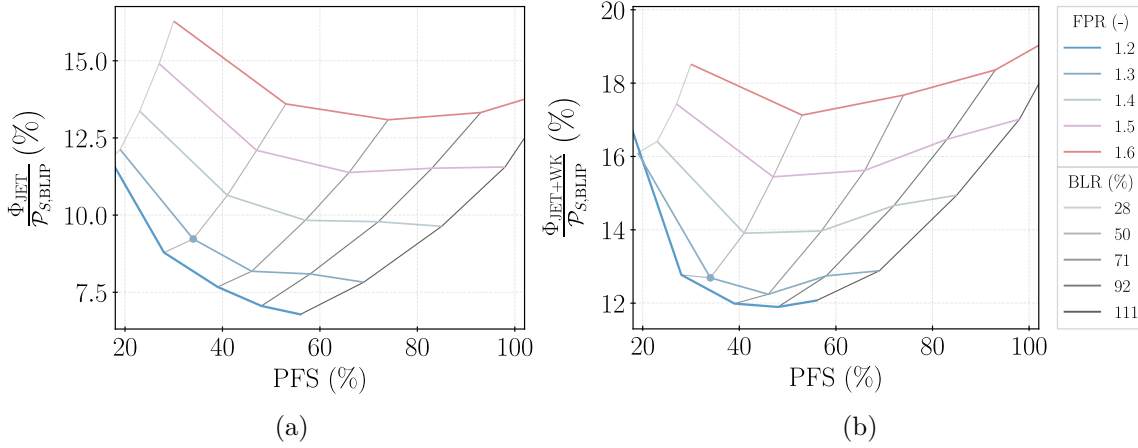


Figure 5.27: Computed portion of viscous dissipation rate in (a) the jet and (b) the jet and wake, relative to the power demand from the aft-mounted fan to provide the pressure rise targeted

with wider intakes, more important viscous losses are observed to which variations in designs yield larger undesired differences as shocks and separation are observed along and behind the plug.

The comparison of these jet losses with the amount of work provided by the propulsor, as given in Fig. 5.27a indicates minima in irrecoverable losses in the high-end of BLR where the amount of work provided from the fan to the flow is better used with lower losses to generate a forward force. Nonetheless, a compromise with the contribution of the wake losses needs to be ensured as the jet portion cannot be isolated to assess the performance. In that respect, Fig. 5.27b depicts the contribution of these losses downstream the propulsor relative to the fan power consumption. A distinct change in monotonicity is observed and explained by the conversion of the energy rate into useful work and dissipative losses. For a given amount of mass-flow ingested, increasing the pressure rise across the fan stage contributes towards more forward force but unavoidably to viscous losses which are detrimental to the performance and savings. At a target propulsive force, the optimal size combined with a pressure rise needs to be carefully considered as the nacelle integration effects can outweigh these benefits. In that respect, it is of interest to compare the jet and wake losses relative to the overall losses as well as these overall losses relative to the morphed fuselage (Fig. 5.28).

Contrasting the losses behind the propulsor with the overall vehicle's losses, Fig. 5.28a highlights the previous findings with almost superimposed performance of the wake attenuation between the two lower ends FPR. Notably, for a target

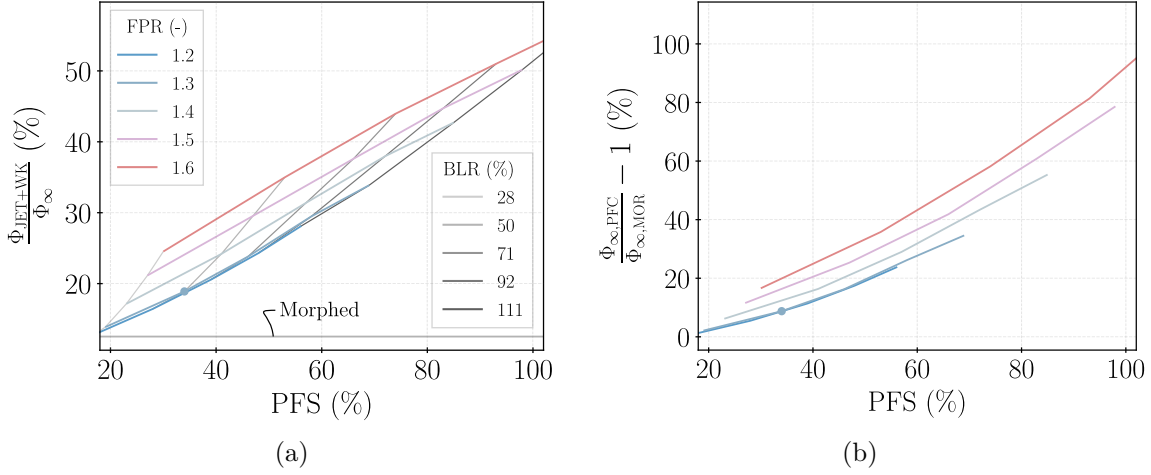


Figure 5.28: Computed (a) portion of viscous dissipation rate in the jet and wake relative to the wake of the baseline morphed fuselage (MOR) with respect to different propulsor sizes, fan operating conditions and (b) total viscous losses between the powered and unpowered fuselages

PFS, the larger the FPR the larger the viscous dissipation however, it exists an optimum BLR in which those losses are mitigated by utilising better the energy rate imparted by the fuselage.

The combination of the two previously described relations yield Fig. 5.28b giving the relative amount of total viscous losses between the unpowered and powered fuselages. It can be observed additional losses of 10% for relatively small propulsor geometries ingesting up to 70% of the BL mass-flow and providing up to 40% of the forward force requirement from the aircraft.

5.6 Conclusion

The intricacies associated with the highly coupled engine-airframe aerodynamics prevents direct application of convetional thrust and drag book-keeping schemes and their distinction. Although alternate bookkeeping schemes using control volume analyses based on momentum exist, they offer limited insights that rely on pressure and skin-friction decompositions. To provide more physically intuitive interpretations, the paper combines an energy-based approach with the absolute reference frame standpoint to investigate the work-energy relationships of flight. This perspective considers the initially quiescent air as displaced by the aircraft, depositing energy and forming a viscous layer that can be harvested by an

integrated propulsor to yield fuel savings in turbo-electric thrust-splitting aircraft architectures.

The examination of the design space of the rear-end of a propulsive fuselage concept reveals the dominant design variables that impact the aero-propulsive performance, including variations in the propulsor size, fan pressure ratio, exhaust design, nacelle end-point position and nozzle shape. To provide a more comprehensive understanding of the energy deposition, transfer and loss, a precedence procedure is introduced to decompose different flow features, allowing for clearer identification of causal pathways and quantification of the contributions of pressure work and viscous dissipation rates relative to the overall vehicle's losses. While the majority of the energy present in the viscous layer is irrecoverably dissipated before being harvested by the aft-mounted fan, recoveries of 6 to 11% of the energy present are enabled.

To demonstrate the effects of the integration penalties and wake attenuation, a comparison is made between a morphed baseline design and its integrated equivalent. The surrounding flow-field is dissected into its main features, with losses stemming from the pure wake resulting from the spilled-out over-cowl flow. These losses are mitigated by the jet plume which is further decomposed to identify and characterise the attenuation and loss factors. Through this investigation, the different rear-end designs are examined and the findings are summarised:

- the propulsor size and fan pressure ratio dominate the aero-propulsive performance (optimal combinations can be found in terms of power demand for a given force share);
- the nacelle trailing edge droop directs the primary flow leaving the propulsor and is responsible for the work applied and lost (embedded nacelle ends are favourable);
- the nozzle/plug curvature can mitigate the losses by preventing an early flow detachment on the wall surface;
- steep plug angles favour recirculation behind the vehicle, disregarding large propulsors in this study, and slender nozzles perform better.

The design space explored revealed that the greatest savings are achieved with a propulsive fuselage concept that ingests half of the mass-flow present in the boundary

layer. By retrofitting two under-wing at their original scale (not down-scaled), a conservative fuel saving coefficient of 10.5% is achieved, contributing one third of the aircraft's thrust. However, due to the expected detrimental effects of three-dimensional geometric features, these savings are optimistic. To converge towards more representative potential savings, it is necessary to re-scale the main power-plants to operate at a lower force generation, use propulsors with elongated plug-cones to prevent flow separation, and optimise the design of the aft-mounted fan before accounting for three-dimensional effects.

The methodology introduced in this work is extendible to other innovative aircraft designs to facilitate physical understanding of the various flow mechanisms involved in hybrid-wing or lifting fuselages. Furthermore, there is an opportunity for advancing the definition of propulsive efficiency by extending the formulation of the corrected kinetic energy inflow rate term $\mathcal{P}_K^*$. Increasing the level of accuracy in the propulsion model would validate the proposed formulation and bring us closer to a propulsive power equivalent, i.e. the denominator of the propulsive efficiency equation. This would also enable the analytical prediction of irrecoverable losses within the propulsive domain, modelled through boundary conditions, and improve the power balance.

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Appendix 5.A Flow Decomposition Precedence Procedure

The viscous layers represented with the main boundary layer forming over the fuselage, the secondary boundary layer developing over the propulsor cowl, and their subsequent wake, are identified along with the wave-induced losses and the jet plume using the following method:

- The viscous portion of the flow is first extracted from the computed flow solution as per defining the amount of mass-flow ingested by the propulsor relative to the amount of mass-flow contained within the BL. The shear identifier ξ introduced by Sanders and Laskaridis [16] is followed whereby each cell is contrasted against the volume integral of the quantity to define a threshold value. The shear layer encompasses both boundary layers (the main and secondary), as well as the wake that starts downstream of the vehicle's nacelle.
- The shock-waves that might form over the propulsor cowl or the fuselage are identified by ζ from the work done by Lovely and Hairnes [44] with a cut-off value of 0.95 based on the recommendations from Refs. [16, 45]. Despite the difficulties to isolate the shock regions, the approach is robust enough to isolate them even though Ref. [45] suggests an improved method. The user input requirement is not suitable in this work as a large design exploration is performed.
- The jet plume is extracted from Υ where previous sensitivity studies have been conducted by Sanders and Laskaridis [16]. The jet plume is isolated by means of total enthalpy deviation accounting for a 10% rise in the quantity downstream of the propulsor. The jet plume is anticipated to attenuate the wake region of the flow whereby additional features are identified and isolated.

A step further into the flow breakdown emphasises on the different features confined within the propulsor jet. The flow characteristics are obtained following the precedence procedure introduced by Sanders [32] on the isolated plume:

1. Jet region extraction: Υ is utilised for a neat separation between the plume and its surrounding flow. Within its volume, a succession of compression and dilation waves form and identify the core of the plume.
2. Core-compression filter: ζ identifies the sub-region of the jet experiencing strong compression. Even though the shocks are filtered-out of the jet-flow, the delay imparted in viscous dissipation does not allow a fair quantification.
3. Jet/wake mixing: ξ previously used for the highlight of the shear layer enables the separation of the mixing volume, or viscous attenuation, from the pure jet, i.e. jet-core. The highly dissipative nature of the mixing volume is isolated from the jet-stream leaving behind a purely isentropic expansion process, described as the core-expansion.

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6

Conclusions

A synthesis of the research on the definition of a hybrid momentum- and energy-based aero-propulsive performance evaluation approach and its application to propulsive fuselage aircraft is given in this thesis. The main findings are first summarised and related back to the research objectives. Then the contribution to knowledge is provided before suggestions on future work.

6.1 Summary of Key Findings

This thesis presents the research towards the development and application of an energy-based method to relatively complex aircraft concepts featuring boundary layer ingestion. This research aims at bridging the gap between past developments and practical applications with potential future aircraft. The specific conclusions and main findings are grouped according to the research objectives, and given as follows:

Objective 1: Critically review the state of the art of boundary layer ingestion propulsion to inform the selection of models and methods for further research

Chapter 2 collates the advancements in boundary layer ingestion propulsion concepts with an emphasis on the theoretical, numerical and experimental studies. The close integration of the propulsion system within the airframe prevents their aerodynamic to be studied in isolation as well as the use of long-established, conventional bookkeeping schemes. Consequently, this led to the development of re-visited momentum-based approaches but also brought forth alternatives based on energy and exergy. The strengths of the latter approaches lies in the information they hold as more detailed aerodynamic force decompositions are possible. Additionally, a different standpoint (using the absolute reference frame), rather than the common one (the relative reference frame), can be taken to improve the physical interpretations of the flow phenomena. Their drawback is, nonetheless, the high computing demand with dense grids to capture correctly the flow-field phenomena, not only locally around the vehicle but farther downstream in the wake. Unlike previous publications, a comprehensive review of the existing approaches is offered with discussions on the performance metrics, and their

applicability at any phase in the aircraft design, as well as both numerically and experimentally [1].

With regards to the experiments, Chapter 2 provides, to the author's knowledge, the first review publication on the experimental apparatuses, aircraft models, instrumentation techniques and performance evaluation for aircraft featuring boundary layer ingestion. Notably, it is observed that there is a common difficulty in obtaining similitude between wind-tunnel and in-flight conditions where in most cases, the Mach number is brought down to conserve an equivalence in Reynolds number. Specifically with boundary layer ingestion, for experiments evaluated with energy-based methods, it had been proven that compressibility effects, especially at transonic conditions, cannot be omitted and must be corrected for low subsonic experiments. The challenges related to the experiments are, as stated, similitude not ensure between the wind-tunnel and in-flight conditions, the boundary layer ingesting propulsor emulation as a consequence. This is because the airframe and propulsion system scale differently with Reynolds number, which poses a challenge because the propulsion system is dependent on the boundary layer properties it ingests.

Lastly, a large survey of the different aircraft concepts based on boundary layer ingestion categorises them in three-fold. Short term goals, as set by the different environmental parties, seem feasible when adopting current aircraft and mounting a third propulsor around their tail. On these designs, primary, secondary and tertiary effects are identified and related to the concept design. Whilst primary effects arise from major design variables such as the position and size of the propulsor, tertiary effects are related to specific design points alike the nacelle cowl parametrisation, or the duct intake length. Longer term goals seem possible with two other categories of aircraft concepts: aft-mounted turbofans that trade off the under-wing turbofans, or more disruptive concepts based on distributed propulsion. With these aeroplanes, synergistic benefits arise from the reshaping of the airframe with lifting fuselages, larger wing aspect ratios, new materials, and the use of hydrogen. However, in all cases, a main challenge arise as to how to accurately quantify the aero-propulsive performance of the concepts relative to conventional aeroplanes. The discrepancies in savings claimed are identified as being related to the modelling chosen by researchers, as well as the reference used for comparison.

Objective 2: Develop a framework for the evaluation of the aero-propulsive performance of aircraft configurations based on a hybrid momentum- and energy method

The lack of consensus surrounding the quantification and interpretation of boundary layer ingestion benefits due to the breakdown of past performance accounting methods leads to a need of a universal accounting scheme. From the challenges, strengths and drawbacks of existing methods, a unified momentum- and energy-based evaluation approach is proposed in Chapter 3.

The versatility of the aero-propulsive evaluation approach makes it usable on entire aircraft and on partial assemblies. In this work, a model reduction is undertaken assuming weak aerodynamic coupling between the fuselage and aft-end propulsor assembly, and the rest of the aircraft components. This simplifies the aircraft modelling for CFD RANS studies to which the performance evaluation is ensured.

In this approach, near-field momentum and far-field energy methods are unified to quantify the vehicle performance at cruise. Whilst a fully energetic approach could have been chosen, the requirements of a dense boundary layer mesh encourage the use of near-field pressure and skin-friction integration to compute the stream-wise force. The comparison with the energetic approach suggests a difference of 0.26%, on the fluid domain and mesh considered, as reported in Chapter 3.

With regards to the performance, three key metrics are identified to quantify the propulsive force of the thruster, the propulsor efficiency, and the power (or fuel) savings relative to a reference. Nonetheless, large discrepancies in fuel savings are found in the literature as its values depend on many parameters (e.g. reference aircraft, reference engines, flight conditions...). To circumvent these ambiguities, and enable comparison between peers, new metrics are introduced to bound the power saving coefficient with its minimal and maximal values. Despite their tangibility, they are universal as based on ideal situations (where engines approach ideal propulsion efficiency) and do not prevent the use of the other metric to relate the savings to technology advancements.

The approach is applied to a propulsive fuselage concept in Chapter 3 where the power saving coefficient is bounded between -9.12% and 61.32%. When considering ideal transmission efficiency in either fully-electric or turbo-electric aircraft

architectures, this coefficient is evaluated at 10.35% (when compared against an Airbus A320 Family or Boeing 737 propelled by two turbofans similar to the CFM LEAP-1 and PW1100G), and at 16.17% when the turbofans of the reference aircraft are CFM56. These figures increase to 16.40 and 21.84% respectively when the turbofans in the propulsive fuselage concept are resized.

Objective 3: Identify and understand the impact of fuselage shapes on boundary layer developments

The approximation of the fuselage into an axi-symmetric fuselage is validated via a drag build-up in Chapter 3 at the specific cruise conditions (flight level 350 and Mach number of 0.78). The flow response to design changes in the fuselage shape is undertaken with variations in the nose and tail shapes.

The approximation of the aircraft nose with ellipsoidal shapes, as considered in Chapter 4, is permitted as long as no shock are generated at the aircraft shoulder (between the nose and the cabin). The shock generation, and its strength, greatly impact the development of the boundary layer that negatively impact the potential for energy recovery. The fuselage tail influences the aerodynamic and mostly the pressure gradient on the surroundings. This impacts locally the flow velocity, and subsequent energy-based quantities used in the potential for energy recovery (PER) metric. Whilst differences of 0.5% are noted on PER for the different conic tails, this increases to 1% when the tail is morphed to later accommodate the propulsor. Nevertheless, a greater potential should be taken carefully as it results from an increase in drag and a trade-off must be ensured between the fuselage drag and PER.

On these morphed tails, critical slope and plug angles of around 13° and 30° , respectively, are observed. Above these values, flow recirculation occurs along the tail slope and behind the plug-cone, which would expectantly reduce the performance of the vehicle as more propulsive force is required.

Objective 4: Identify the aft-end propulsion system's design space and key design variables to develop an appropriate parametrisation methodology

The previous observations helped bound the propulsor design space with notably its axial and radial positions along the tail. Nonetheless, these were considered on a clean fuselage where no integration would happen either upstream or downstream the nacelle. It is therefore evident that the axial location at which the propulsor is integrated, as well as radially are important factors to consider in the parametrisation. Additionally, the asymptotic nature of the boundary layer profile suggests larger benefits in ingesting the bottom viscous region of the flow than the upper region where little excess in kinetic energy are observed. To quantify the amount of mass-flow travelling through the fan, a non-dimensional parameter described as the boundary layer ratio is used and defined as the ratio of mass-flow going through the fan relative to the mass-flow present in the entire boundary layer around the clean morphed fuselage. With the location and flow passage, the propulsor is modelled accounting for simple parametrisations (i.e. enabling macro parameters to be varied and not relying on very detailed multi-point designs). As such, the propulsor fore-cowl is represented with a NACA-1 profile, its aft-cowl with a circular arc, and the intake and exhaust ducts following Bell-Mehta polynomial curves.

Objective 5: Explore partially the thruster's design space, and identify the sources of benefits to relate them to integration and design effects

Three main focus points have been considered in exploring the design space: the propulsor location along the tail (variations in axial and radial positions), the propulsor operating conditions (variations in the fan-face area, that regulates the flow passage, and fan pressure ratio), the exhaust system and plug-cone designs (variations in exhaust shape, nacelle trailing edge inclination, and plug-cone angle).

With respect to the propulsor size, integration penalties have been assessed both in terms of forces and energy. It is found that larger throat areas affect the flow further upstream, notably the pressure gradient, reducing the flow velocity closer to

the wall, reversing the flow in some instances. The nacelle cowl scaling with respect to the throat area to conserve non-dimensional design parameters increase the total viscous dissipation rate over the cowl. Investigations on compact nacelles enable the reduction in intake and nacelle lengths. On the former, short to ultra-short intakes increase by up to 14% the radial distortion index but showcase similar total pressure profiles along the span-wise direction as natural pre-diffusion in enabled with embedded propulsion systems. Shrinking the nacelle by almost 20% reduces the plug-cone angle by 10° which reduces further the generation of entropy, and utilises better the energy present for propulsive force generation. In addition to these aerodynamic benefits, lighter nacelles contribute to lighter aircraft which would yield greater fuel savings.

An increase in fan pressure rise accelerates further the jet flow within the exhaust and contributes towards larger losses, due to mixing between the jet plume and wake. Nonetheless, these penalties can be mitigated with nacelle trailing edges more integrated within the tail. Larger integration levels are more beneficial as the bell forming on between the exhaust and plug-cone is less convex, reducing the jet acceleration along its surface and likelihood to separate.

Objective 6: Evaluate the aero-propulsive performance of a propulsive fuselage aircraft along its cruise segment

The aero-propulsive performance of propulsive fuselage concepts is evaluated via the share of forward force from the thruster, its efficiency and the fuel savings it yields. Two main regions of interest for aft-end propulsors are identified. First for large hub radii of around 65% of the fuselage radius, at axial locations between 35 and 45% of the tail length, fuel savings of around 10% are obtained. It is however pre-empted that large hub radii imply large wetted surfaces around the nacelle cowl contribution to irreversible losses, and greater mass. The second region, most promising in the author's opinion, also yields around 10% of fuel savings along the cruise segment with propulsors located around 30 and 50% of the fuselage radius, at 50 to 60% of the tail length.

The design space exploration conducted on the propulsor size indicates maximum savings of 11.1% when generating around a third of the aircraft total force requirement to maintain steady-level flight conditions. It is shown that fuel savings are achievable for a large amount of candidates by considering retrofit turbofans.

In effect, the turbofans from the reference aircraft are re-used on the propulsive fuselage concept to both generate a propulsive force and provide mechanical power to the thruster. Nonetheless, a bi-product of boundary layer ingestion is the reduction in thrust generation requirement by the under-wing turbofan which can be down-scaled (contributing to lower nacelle drag) and also re-designed to operate at the new design point. In considering this, improvements in savings between 4 and 6% seem achievable.

Locally, on the exhaust system design, more integrated nacelle trailing edges suggest greater performance by reducing mixing losses. This is complemented by slender plug cones that improve the aero-propulsive performance by preventing detachment with high jet flows. Nonetheless, this work considered a finite aircraft chord (i.e. not extendible) and the limits in slenderness have not been explored. It is however reminded that elongating the aircraft tail, or propulsor plug-cone could cause issues at take-off as the take-off angle would be impacted.

6.2 Knowledge Contributions

There are numerous contributions to knowledge contained in this dissertation. These are listed in Chapter 1, detailed in the individual chapters formatted as papers, and repeated below for emphasis:

- A thorough and critical literature review on the different bookkeeping schemes, numerical models and methods, experimental progress and conceptual aircraft featuring boundary layer ingestion propulsion. The existing momentum-, energy- and exergy-based accounted are reviewed and compared. The numerical aerodynamic and propulsive models are classified by their degrees of fidelity and compared. The numerous studies on aircraft concepts based on boundary layer ingestion are reviewed, critiques and mapped out to trace the evolution of the studies refinement.
- A modular framework on the design and aero-propulsive performance evaluation of novel boundary layer ingestion aircraft is developed. In the context of this work, a simplified model of a propulsive fuselage concept is considered whereby a parametrisation is offered. A hybrid momentum- and energy-based aero-performance evaluation approach is presented to assess the

aircraft top-level performance at cruise. Within this approach, performance metrics are defined to characterise the propulsive force share of the thruster relative to the turbofans, its efficiency and the fuel savings. A direct contribution from the propulsive force share is that it provides inputs to aero-engine manufacturers for sizing.

- The power balance initially developed by Drela in Ref. [2] is corrected to account for local compressibility effects in the fan stage. Whilst the net pressure-work and kinetic energy inflow rate, or power addition to the flow, is considered in the initial formulation as $\mathcal{P}_K$, it neglects the reversible portion of the pressure-work which can apply work to the fluid. As such, a correction is provided with $\mathcal{P}_K^*$ to isolate the portion of energy present in the flow that can do work, to the one that is already irrecoverably lost in the propulsor domain.
- Efforts towards a definition of the propulsive efficiency with application to boundary layer ingestion propulsion systems are provided. Whilst there are flaws associated with the conventional definition as its value can theoretically exceed 100% with boundary layer ingestion, a challenge still exists in a true definition of propulsive efficiency in these cases despite the numerous efforts [3–5]. This formulation corrects the power addition term, on the denominator, by considering the previous contribution mentioned.
- A design space exploration of a propulsive fuselage aircraft with an energetic approach. This extends the knowledge on the position, size and shape of aft-end propulsors as previous work using energy methods was done on ideal scenarios [6], and more rudimentary approaches on complex aircraft geometries [7]. The design space exploration highlights fuel savings benefits on a propulsive fuselage concept as different amounts of forward force are generated by the thruster, the rest being provided by under-wing turbofans. Notably, a direct inverse relationship is reported between the propulsive force share and propulsor efficiency (and power saving coefficient).
- A flow feature segmentation is performed on 2D axi-symmetric flows. Whilst previous work has focused on the definition of identifiers to isolate specific regions of the flow-field, a precedence procedure was introduced in Ref. [5] and used in this work. The aerodynamic decomposition approach gives recommendations on the influence of the design changes on the

aero-propulsive performance, and their inherent losses. This work is a stepping stone in the attribution of losses with respect to design changes and their effects on the flow.

6.3 Recommendations for Future Work

The main recommendations for future research activities are provided below:

6.3.1 Propulsion Modelling

Irreversible Losses in the Fan Stage

There is a caveat in the fan modelling used in CFD when considering boundary conditions as it omits the fluidic volume between the fan-face and fan-exit stations. Consequently, the viscous dissipation rate and pressure work cannot be isolated neither quantified. Whilst an analytical formulation is given in this work, validation still need to be performed by replacing boundary conditions with higher orders of fidelity as given with body force models. In doing so, the different terms present in the power balance can be quantified and confirm, or refute, the formulation on the propulsor efficiency. In addition, the partial pressure field method [8] can be used in the propulsor domain to decompose the reversible part from the irreversible part and provide a fully decomposable

Transport of Total Pressure Distortion

Working towards the transportation of the total pressure distortion, an interim step, before moving to body force models, relies on profiles input at the fan-exit after application of a pressure rise. Whilst a uniform pressure is applied in this work, a replica of the distortion transportation can be done with pressure profiles. Further, the total pressure distortion can be naturally transported through the fan stage when considering a body force model. In addition, it enables other work to be carried, as mentioned earlier, as well as quantifying the negative impact on the propulsor performance and the savings reported.

Boundary Layer Ingestion and Variable Pitch Fan

Taking a step further in the modelling of the fan stage, a fully circumferential fan with blades can offer other applications. Whilst the cruise segment is only considered in this work, other phases of the missions can be explored and savings potentially improved with variable pitch fans. The main advantages of such fans is their improved operability and opportunity of contributing towards reverse thrust. Whilst this concept is studied on turbofans [9, 10], there could be an opportunity in providing reverse thrust from the aft-end thruster.

Under-Wing Turbofan

In this thesis, turbofan maps have been provided by project's partner from Chalmers University of Technology. The maps are generated based on available data assuming top-of-climb conditions. The installed specific fuel consumption and thrust were then calculated based on nacelle drag estimates. There is merit in the design and computation in CFD of turbofan to which optimisation studies can be performed to improve the aerodynamic performance.

6.3.2 Propulsive Fuselage Concept Performance

Vaster Design Space Exploration

This work emphasises on different positions along the plug-cone which tends to have a primary impact on the performance (see Fig. 4.20). The propulsor size, especially with its flow passage, also impacts considerably the aero-propulsive performance of the concept (see Fig. 4.21). In this work, an optimum is found when the propulsor is integrated at 90% of the fuselage chord and ingests 43% of the boundary layer mass-flow. However, a vaster design space should be explored to refine the region of interest before moving to three dimensional investigations.

Nacelle Geometry and Design Optimisation

A relatively simplified parametrisation of the nacelle and its cowl was undertaken in this work. Whilst no shock have been observed along the outer cowl, drastic

improvements in aerodynamic performance are expected with refinements of the geometry. For example, the use of (intuitive) Class Shape Transformation functions [11, 12] would enable the optimisation of the cowl. Slender plug-cone can also be explored during optimisation where overall, drastic improvements on the benefits can be achieved.

Three Dimensional Aspects

It is known that all benefits reported in this work are optimistic and detrimental behaviours are expected as the aircraft accounts for 3D effects. Implementing an up-sweep angle on the aircraft tail will impact the total pressure distortion at the fan-face, and increase the blade loading. Mounting wings on the fuselage will also contribute to a lower performance with the down-wash experienced by the fan.

Power Transmission and Mass

In this work, changes in masses due to the integration of the boundary layer ingestion propulsor have not been considered. However, it is pre-empted to have an important impact on the overall performance as the nacelle, turbomachinery and transmission components are taken into account. On the latter, a transmission efficiency between the turbofans and thruster is assumed but sizing and thermal studies can also be considered in future work.

Entire Flight Envelope

Only the cruise segment of the short-to-medium range haul is considered here. However, it is equally important to assess the aero-propulsive performance of the aircraft along all other phases of the flight, notably developing an understanding on the impact angle of attacks have on the performance and fuel savings.

6.3.3 Tools and Methods Development

Flow Feature Decomposition

The precedence procedure developed in Ref. [5] to identify different flow features is applied to the design space exploration performed in this thesis on two dimensional configurations. The applicability to three dimensional bodies is yet to be demonstrated. In addition, when moving to 3D and lifting bodies, additional existing methods reviewed in Ref. [1] can be investigated to isolate lift-induced losses.

Disruptive Concepts

The design and aero-propulsive performance assessment framework developed in this thesis focuses on propulsive fuselage concepts. There is an interest in demonstrating the application of the performance evaluation approach to other, and more disruptive, concepts that do not rely on complementary propulsion systems but distributed ones.

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