

Fundamental Concepts of Boundary Layer Ingestion Propulsion

Ioannis Lamprakis*, Drewan S. Sanders[†], and Panagiotis Laskaridis[‡]
Cranfield University, Cranfield, Bedfordshire, England MK430AL, United Kingdom

This work further develops energy-based far-field methods, by introducing Galilean covariance in work-energy relationships of flight. The novelty lies in how decomposition formulations are re-derived from integral forms of the governing laws applicable to moving control volumes. It is shown that aerodynamic performance is best evaluated in a reference where the aircraft moves through the atmosphere. The advantages are clearly demonstrated through the formulation of a hypothesis on Boundary Layer Ingestion (BLI) power savings using a series of simplified flat plate - BLI propulsor configurations. This hypothesis links BLI power savings to the energy content within the boundary layer and the propulsor's ability to attenuate the ingested boundary layer's velocity profile. Extensive numerical studies on both laminar and turbulent flows are carried out to test this hypothesis, examining different levels of wake recovery achieved through a body force model propulsor with varying load distributions. Near-perfect wake attenuation is shown to yield maximum power savings, but only for higher Reynolds number flows, where the influence of aero-propulsive interference on upstream dissipation is minimal. The flat plate findings are extended to a 2D axisymmetric fuselage representation, where baroclinic losses become significant. A maximum power saving of around 8% is achievable at typical cruise conditions for a single-aisle passenger aircraft.

Nomenclature

A_D	=	actuator disk area, m ²
c	=	chord length, m
C_D	=	drag coefficient
C_f	=	skin friction coefficient
C_p	=	pressure coefficient
C_T	=	thrust coefficient
$\mathcal{CS}$	=	control volume surface

*Research Assistant, School of Aerospace, Transport and Manufacturing, ioannis.lamprakis@cranfield.ac.uk

[†]Lecturer, School of Aerospace, Transport and Manufacturing, d.s.sanders@cranfield.ac.uk.

[‡]Professor, School of Aerospace, Transport and Manufacturing.

$\mathcal{V}$	=	control volume
d	=	distance, m
D, D_v	=	total, friction drag, N
e	=	internal energy, J/Kg
$\mathbf{f}$	=	volume specific body force vector, N/m ³
$\mathbf{F}$	=	force vector, N
$\mathbf{f}$	=	body force vector field, N/m ³
$\bar{\bar{\mathbf{I}}}$	=	second order tensor identity matrix
M	=	Mach number
k	=	turbulent kinetic energy, J/Kg
$\hat{n}$	=	unit normal vector
p	=	static pressure, Pa
p_g	=	$= p - p_\infty$, gauge pressure, Pa
PER	=	potential for energy recovery
$\mathcal{P}_S$	=	propulsor shaft power, W
$\mathcal{P}_f$	=	body force propulsor power, W
$\mathcal{PER}$	=	compressibility-corrected potential for energy recovery
$\mathcal{PSC}_{\min}$	=	minimum power saving coefficient
$\mathbf{q}$	=	heat conduction flux vector, J/(Kg · s)
$\dot{q}$	=	volumetric heat addition, J/(Kg · s)
$\mathbf{r}$	=	position vector, m
Re	=	Reynolds number
Re_c	=	chord-based Reynolds number
$\mathcal{S}$	=	surface, m ²
$\bar{\bar{\mathbf{S}}}$	=	rate-of-strain tensor
t	=	time, s
T	=	static temperature, K
$\mathcal{T}$	=	Thrust, N
$\mathcal{V}$	=	volume, m ³
$\mathbf{U}$	=	relative velocity between reference frames, m/s
$\mathbf{V}$	=	$\langle u, v, w \rangle$, velocity in the absolute reference frame, m/s
$\mathbf{V}'$	=	$\langle u', v', w' \rangle$, velocity in the relative reference frame, m/s

V_x	=	stream wise velocity component, m/s
Wh	=	potential work rate, W
$\mathbf{X}$	=	$\langle \hat{x}, \hat{y}, \hat{z} \rangle$, absolute reference frame coordinates
$\mathbf{X}'$	=	$\langle \hat{x}', \hat{y}', \hat{z}' \rangle$, relative reference frame coordinates
δ	=	boundary layer thickness, m
$\dot{E}_a$	=	streamwise kinetic energy outflow rates via Trefftz plane, W
$\dot{E}_k$	=	$\dot{E}_a + \dot{E}_v$, total kinetic energy outflow rates via Trefftz plane, W
$\dot{E}_p$	=	pressure work rates acting on the Trefftz plane, W
$\dot{E}_v$	=	transverse kinetic energy outflow rates via Trefftz plane, W
$\dot{\mathcal{E}}_{TP}$	=	$\dot{E}_k + \dot{E}_p$, Trefftz plane net mechanical energy outflow rate, W
η_{FIP}	=	propulsive efficiency of free-stream ingesting propulsor
α	=	fuselage inclination angle, rad
σ	=	propulsor thickness, m
θ	=	momentum thickness, m
θ^*	=	energy thickness, m
Θ	=	volumetric pressure work rate, W
μ, μ_t	=	laminar and turbulent dynamic viscosities, $(N \cdot s)/m^2$
ν	=	kinematic viscosity, m^2/s
ρ	=	density, kg/m^3
$\bar{\bar{\tau}}$	=	viscous stress tensor, Pa
Φ	=	viscous dissipation rate, W
Φ^*	=	total mechanical loss rate, W

Subscripts and Superscripts

AD, D	=	Actuator Disk quantity
ARF	=	Absolute Reference Frame quantity
B	=	aircraft surface related quantity
BL	=	boundary layer-related quantity
CS	=	control volume surface
$dist$	=	distribution
e	=	boundary layer edge quantity

f	=	body force related quantity
FIP	=	free-stream ingesting propulsor
irr	=	irreversible quantity
jet	=	jet plume region
lam	=	laminar quantity
prop	=	propulsor-related quantity
rev	=	reversible quantity
REF	=	reference quantity
RRF	=	Relative Reference Frame quantity
S0-S4	=	hypothesis scenarios 0-4
shock	=	shock-related quantity
$t, turb$	=	turbulent quantity
TE	=	trailing edge quantity
TP	=	Trefftz plane quantity
wake	=	wake layer-related quantity
∞	=	free-stream quantity
$'$	=	quantity in the relative reference frame

I. Introduction

Boundary Layer Ingestion (BLI) is a concept believed to leverage favourable aerodynamic coupling between the airframe and propulsion systems. A comprehensive review of BLI propulsion by Moirou et al. [1], provides an in-depth discussion on performance accounting, numerical methods and models, experimental apparatus and practices, and various BLI aircraft conceptual designs. Concepts include Blended Wing Bodies [2–4], ultra-wide fuselages [5–7], and tube-and-wing aircraft with a tail-cone concentric BLI propulsor [8–13]. The latter have attracted significant interest and lend themselves to axisymmetric modelling assumptions. This reduces flow-field complexity and enables primary mechanisms of BLI to be investigated, as has been explored in Refs. [14–19]. As reviewed by Moirou et al [1], several works [9–11, 20–30] apply various performance accounting methods to differing levels of modelling fidelity, each comparing performance benefits against a different baseline. The unfortunate outcome is that, despite these research efforts, there remains large uncertainties in the claimed benefits and the interpretations of the underlying physics.

A primary challenge is the ambiguity associated with adopting bookkeeping conventions [31, 32] where thrust is isolated and indirectly represents the useful work required to overcome the airframe’s drag. Thereafter, the total power

required to produce this implied useful work is assessed according to its core, transfer, and propulsive efficiencies [33]. Improvements in aero engine propulsive efficiency have been particularly instrumental in enhancing aircraft performance [34–36], however, the definition of propulsive efficiency is reliant on clear divisions between propulsive and drag streamtubes. Unfortunately, these are inseparable in BLI configurations and it becomes difficult to establish a thrust definition (from bookkeeping conventions) that meaningfully represents useful work*. Therefore, despite a number of alternative, BLI specific, force accounting methods [9, 28, 30], a momentum-based approach is likely to return a thrust that misrepresents useful work and propulsive efficiency. This is evident from a number of studies which calculate propulsive efficiencies in excess of 100% [14, 22, 37–40], or the debatable analogies of Bevilacqua and Yam [41] used to constrain it below that threshold. A popular work-around to this dilemma is the Power Saving Coefficient (PSC) proposed by Smith [42], which compares BLI versus Free-stream Ingesting Propulsor (FIP) power required to achieve the same net vehicle force. Unfortunately, the PSC is circumstantial because it depends on the baseline used for comparison. Strictly speaking, and as explained by Hall et al. [5], there is also no unique way of establishing equivalence between the BLI and FIP propulsor that allows for fair comparisons.

Drela [43] and Sato [44] proposed the Power Balance Method to circumvent these difficulties by approaching aircraft performance using mechanical energy instead of forces. Arntz et al. [45] extended the idea to an exergy-energy balance by incorporating the second law of thermodynamics. Subsequently, Lamprakis [46–49] has demonstrated the validity of both approaches in their application to BLI configurations in adiabatic and non-adiabatic flows. Nonetheless, Drela’s [43] and Arntz’s [45] formulations were both derived in the Relative inertial Reference Frame (RRF), which has its coordinate system fixed to the aircraft. Based on the work of Renard and Deck [50], Sanders and Laskaridis [51] identified that the mathematical terms in both formulations actually represent physical mechanisms whose energy forms are naturally perceived in the Absolute inertial Reference Frame (ARF)[†]. As explained by Mallinckrodt and Leff [52], work-energy relationships, unlike force-momentum, are *Galilean covariant* and not *Galilean invariant*. This means that the work done, and its corresponding energy, change by equivalent amounts depending on the inertial reference frame in which they are viewed. Although treatment in the RRF is valid, it is reliant upon an implied shift to the ARF to contextualise and quantify work-energy relationships. This implied shift is not particularly problematic when analysing conventional aero-propulsive architectures, but becomes unclear, difficult to visualise, and open to errors when considering configurations with close aerodynamic coupling between airframe and propulsor. Therefore, in the authors’ opinion, working directly in the ARF provides greater clarity and rigour in dealing with such complexities. Sanders and Laskaridis’ [51] ARF transformation of the power balance decomposition was limited to steady flow assumption, whereas this paper properly addresses Galilean covariance by directly applying the generalised, time dependent, integral

*A further complication is that the ingested boundary layer’s size is on the same scale as the propulsion system’s inlet. Subsequently, it is impossible to obtain local pressure drag contributions by subtracting the potential flow forces, as would normally be permitted via, what Ref. [31] describes as, Prandtl’s extension to D’Alembert’s paradox.

[†]Dubbed by Renard and Deck [50]

forms of the governing equations in the ARF. This non-trivial matter has been described in detail by Berhouni et al. [53] in the context of exergy analysis[‡], who emphasise the importance of analysing flows from the perspective of the thermodynamic deadstate, in order to arrive at a correct definition for exergy. Although not that dissimilar, there are a few important differences between that presented herein versus that of Ref. [53]. In particular, this work takes the approach of moving control volumes, whereas Berhouni et al. [53] derive their formulations for a control volume fixed in space. The generality of the moving over the fixed control volume approach has some key advantages, which enable it to be readily applied to non-inertial accelerations and other unsteady motion of an aircraft and its components.

Galilean covariance considerations aside, the energy-based approaches have enabled improved quantification and qualification of BLI performance via concepts such as; the potential for energy recovery [15, 51] redefined propulsive efficiency definitions [5, 44], dissipation-based form factor correlations [44] and a full thermo-aerodynamic analysis [46], which is inclusive of thermal energy recuperation aspects. These have, for example, typically been applied to complex BLI configurations like the D8 [5–7, 54]. In such applications it becomes difficult isolate BLI specific benefits from other airframe design adaptations and, in some cases, BLI may actually introduce undesirable interaction effects [14] which detract from, or negate, benefits. As such, analyses of complex architectures offers limited insight into the fundamental principles underpinning BLI physics.

To better interrogate fundamental BLI mechanisms Baskaran et al. [18] presented an energy-based aero-propulsive analysis of simplified 2-D axisymmetric, BLI propelled, fuselage designs. This also featured flat plate studies in laminar flow with and without a wake-filling Body Force Model (BFM). Applicable to incompressible flow, the BFM utilized the Bernoulli equation to determine the source term distribution necessary to fill in the boundary layer's total pressure deficit. The study demonstrated, conceptually, the possibility of achieving greater power savings via improved wake attenuation, but the reported data were limited to a single laminar flow condition (at one Reynolds number) and it was therefore unclear if the good agreement between the Blasius solution ($\approx 21.4\%$) and the large PSC savings reported ($\approx 19\%$) were circumstantial. This paper addresses this knowledge gap and requires a comprehensive study, based on extensive canonical test cases in both laminar and turbulent flow, across a wide range of flow conditions, and for varying levels of wake attenuation, to thoroughly interrogate the mechanisms underpinning BLI aerodynamics.

The first aim of this paper is to clearly demonstrate the significance, necessity, and physical relevance of dealing with aerodynamic work-energy balances in the correct inertial reference frame, particularly with respect to BLI. The second aim is to develop and numerically test a hypothesis on BLI power savings using a series of simplified flat plate–BLI propulsor configurations. This hypothesis introduces a generalized PSC definition, linking BLI power savings to the boundary layer's energy content and the propulsor's ability in attenuating the ingested boundary layer profile. To

[‡]It is noted that this manuscript was submitted for peer review prior to the publication by Berhouni et al. [53], and that these notions regarding appropriate reference frames in such analysis were developed around the same time and from different perspectives.

evaluate this, the hypothesis is tested through CFD simulations of laminar and turbulent flat plate flows, as well as 2D axisymmetric fuselage representations, across a comprehensive range of Reynolds and Mach numbers.

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II. Work-Energy Relationships of Flight

This section closely follows the work-energy relationship theory developed by Sanders [56, 57] for the purposes of analysing ejector powered BLI configurations, as tested in Supporting Understanding of Boundary Layer Ingestion Model Experiment (SUBLIME) project [58].

A. Reference Frame Perspectives

External aerodynamic problems are typically approached using an inertial reference frame fixed to the aerodynamic body in question. Analogous to a wind tunnel experiment, the aerodynamic body is viewed as an obstacle which the oncoming flow must overcome. The body is perceived as being stationary, with the air moving relative to it, and experiences a net force commonly referred to as drag. This has taken advantage of Galilean *invariance* in representing forces in flight. However, from the perspective of this Relative inertial Reference Frame (RRF), *static equilibrium* appears to be achieved *passively* by a force directly opposing this drag. In other words, there is seemingly no *active* effort (work) required to hold the aerodynamic body in place, as can be imagined by visualising a sting/support in a wind tunnel model scenario. Instead, the effort originates in the flow itself, and is supplied by the wind tunnel's fan in this analogy. In keeping with the RRF perspective, the flow is pre-energised and its interaction with the aerodynamic body results in losses that detract from its originating energy supply. From this it can be understood that Galilean *invariance* no longer applies when considering the origins of the effort (work) required for flight.

This idea in the RRF extends to propulsion, and for demonstration purposes it is convenient to think of propulsion as a separate system that provides the opposing force to drag (i.e. thrust). This is achieved by imparting additional momentum to free-stream flow at the cost of some total power consumption. In jet engines, the thermal and transfer efficiencies describe how effectively the total fuel power has been converted into kinetic energy [33]. The propulsive efficiency quantifies how much of that kinetic energy is converted into useful work (thrust power). But, work is defined as the dot product between the force and the distance over which it is acted. Therefore, strictly speaking, the useful work done by the propulsion system's net thrust is zero when analysed from the RRF, because it appears to be stationary. Nonetheless, engineers overcome this by making an implied shift to the ARF, where the balance between thrust and

drag results in *dynamic equilibrium* and useful work is the net thrust acting at the aircraft's flight velocity.

Caution is required when relying on an implied shift between the RRF and ARF, particularly when considering the complexities of highly coupled aerodynamic systems, where the division between thrust and drag is ambiguous, and useful work (thrust power) is not clearly defined. Unlike forces and Newton's second law, work-energy relationships are Galilean *covariant* rather than *invariant* [52], which means that the amount of work (and its energy counterpart) will vary according to the reference frame in which it is perceived. Therefore, it is sensible to adopt a fully consistent ARF approach, which perceives all the energy supply required for flight as emanating from the aircraft itself, and being transferred across the aircraft surfaces (via the no-slip condition) to the atmosphere. Subsequently, the atmosphere is rightly perceived to be an energy sink only, as opposed to the RRF perspective, which may falsely perceive the "moving" atmosphere as an energy source (like in the wind tunnel experiment analogy). Moreover, the ARF helps to reveal causal energy transformation pathways, and can be analysed to identify local flow mechanisms containing available energy that can be harnessed to perform thrust work through strategic propulsion integration, such as BLI.

B. The Governing Equations for Moving Control Volumes

Aircraft external aerodynamic performance analysis is reliant on control volume theory and the associated integral forms of the governing equations. Aerodynamic texts often introduce the governing equations with respect to a control volume that has its boundaries fixed in space (see for example Anderson [59]). Although this approach yields a simplified form of the integral equations suitable for most applications, it does not enable analysis for moving control volumes, as required for the ARF analysis formulation presented herein. Following Leibniz' General Transport Theorem Eq. (1), the integral forms governing mass, linear momentum, mechanical energy, and total energy can be expressed in the forms of Eqs. (2) to (5), respectively.

$$\frac{d}{dt} \iiint_{CV(t)} (\cdot) dV = \iiint_{CV(t)} \frac{\partial}{\partial t} (\cdot) dV + \oint_{CS(t)} (\cdot) \mathbf{V}_{CS} \cdot d\mathbf{S} \quad (1)$$

$$\frac{d}{dt} \iiint_{CV(t)} \rho dV + \oint_{CS(t)} \rho (\mathbf{V} - \mathbf{V}_{CS}) \cdot d\mathbf{S} = 0 \quad (2)$$

$$\frac{d}{dt} \iiint_{CV(t)} \rho \mathbf{V} dV + \oint_{CS(t)} \rho \mathbf{V} (\mathbf{V} - \mathbf{V}_{CS}) \cdot d\mathbf{S} = \oint_{CS(t)} (-p \hat{n} + \bar{\tau} \cdot \hat{n}) dS + \iiint_{CV(t)} \rho \mathbf{f} dV \quad (3)$$

$$\frac{d}{dt} \iiint_{\mathcal{CV}(t)} \left(\frac{\mathbf{V}^2}{2} \rho \right) d\mathcal{V} + \oint_{\mathcal{CS}(t)} \frac{\mathbf{V}^2}{2} \rho (\mathbf{V} - \mathbf{V}_{CS}) \cdot \mathbf{dS} = - \oint_{\mathcal{CS}(t)} \left[p \mathbf{V} - \bar{\bar{\tau}} \cdot \mathbf{V} \right] \cdot \mathbf{dS} + \iiint_{\mathcal{CV}(t)} \left[p \nabla \cdot \mathbf{V} - \bar{\bar{\tau}} : \nabla \mathbf{V} + \rho \mathbf{f} \cdot \mathbf{V} \right] d\mathcal{V} \quad (4)$$

$$\frac{d}{dt} \iiint_{\mathcal{CV}(t)} \rho \left(e + \frac{\mathbf{V}^2}{2} \right) d\mathcal{V} + \oint_{\mathcal{CS}(t)} \left(e + \frac{\mathbf{V}^2}{2} \right) \rho (\mathbf{V} - \mathbf{V}_{CS}) \cdot \mathbf{dS} = \oint_{\mathcal{CS}(t)} \left(-\mathbf{q} - p \mathbf{V} + \bar{\bar{\tau}} \cdot \mathbf{V} \right) \cdot \mathbf{dS} + \iiint_{\mathcal{CV}(t)} \rho (\dot{q} + \mathbf{f} \cdot \mathbf{V}) d\mathcal{V} \quad (5)$$

In Eqs. (2) to (5), $\mathbf{V}$ and $\mathbf{V}_{CS}$ refer to the local flow velocity, and control volume surface velocity, respectively. The integration limits $\mathcal{CV}(t)$ and $\mathcal{CS}(t)$ indicate the movement of the control volume and its surfaces with respect to time t , respectively. The symbols ρ , p , $\bar{\bar{\tau}}$, $\mathbf{f}$, e , $\mathbf{q}$, and $\dot{q}$ represent the flow density, static pressure, viscous stress tensor, body force vector field, internal energy, heat transfer coefficient, and any volumetric heating sources, respectively. The control volume is comprised of infinitesimally small volumes $d\mathcal{V}$, and the normal vector $\hat{n}$ is defined as pointing out of the control volume perpendicular to any surface element, such that $\mathbf{dS} = \hat{n} \cdot d\mathcal{S}$.

The General Transport Theorem, Eq. (1), helps to correctly isolate and account for changes of properties inside the control volume due to the movement of its surfaces. If Eq. (1) were to be substituted back into Eqs. (2) to (5), this would bring the time derivative within the volume integration and return the integral equations to the more typical form presented in most texts. However, it is also evident that this is equivalent to setting $\mathbf{V}_{CS} = 0$, which implies that those typical forms of the integral equations really represent a control volume that is fixed in space relative to the inertial reference frame being used. Therefore, it is clear that Eqs. (2) to (5) represent the governing equations in a fully generalised form that will enable an ARF analysis of aircraft external aerodynamics. Furthermore, Eqs. (2) to (5) are written with respect to some arbitrary inertial reference frame $\mathbf{X} = \langle \hat{x}, \hat{y}, \hat{z} \rangle$, and are related to any other arbitrary inertial reference frame $\mathbf{X}' = \langle \hat{x}', \hat{y}', \hat{z}' \rangle$ travelling at a velocity $\mathbf{U}$ relative to $\mathbf{X}$, via Galilean Transformation where:

$$\mathbf{r} = \mathbf{r}' + \mathbf{U}t', \quad t = t' \quad (6)$$

where $\mathbf{r}$ and $\mathbf{r}'$ are position vectors referencing a point in space as perceived from $\mathbf{X}$ and $\mathbf{X}'$, respectively. Subsequently, the relationship of the local fluid velocity between the two reference frames is given by:

$$\mathbf{V} = \mathbf{V}' + \mathbf{U} \quad (7)$$

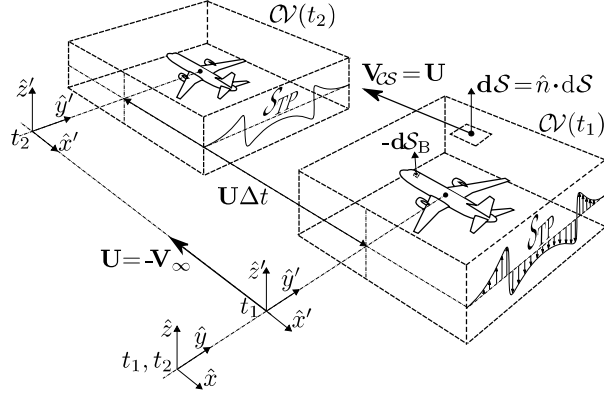


Fig. 1 The motion of an aircraft and its control volume in the ARF, $\mathbf{X} = (\hat{x}, \hat{y}, \hat{z})$, versus the RRF, $\mathbf{X}' = (\hat{x}', \hat{y}', \hat{z}')$.

C. Absolute Reference Frame Formulations

Fig. 1 shows an aircraft flying through the initially quiescent atmosphere, as perceived by the ARF coordinates $\mathbf{X}$, for two instances in time, t_1 and t_2 , and at a constant flight velocity $\mathbf{U} = -\mathbf{V}_\infty$. Also shown, are the RRF coordinates $\mathbf{X}'$, which are fixed relative to the aircraft and seen to change position with time. Velocities in the two references frames are related by Eq. (7). It is convenient to fix the encapsulating control volume relative to the aircraft such that its far-field surfaces move at its flight speed i.e. $(\mathbf{V}_{CS})_{\text{far-field}} = \mathbf{U}$. The front, top, bottom, right, and left far-field planes are assumed to be positioned sufficiently far from the aircraft such that they are immersed in undisturbed flow. Therefore, the only disturbed flow to cross far-field does so via the Trefftz plane $\mathcal{S}_{TP}$. Assuming quasi-steady flow (or periodically unsteady flow, as addressed by Drela [43]) within the moving control volume, enables the momentum balances of Eq. (3) to be simplified and rearranged[§]:

$$\iint (p_g \hat{n} - \bar{\bar{\tau}} \cdot \hat{n}) d\mathcal{S}_B - \iiint \rho \mathbf{f} d\mathcal{V} = - \iint (\mathbf{V}_\rho \mathbf{V}' \cdot \hat{n} + p_g \hat{n} - \bar{\bar{\tau}} \cdot \hat{n}) d\mathcal{S}_{TP} \quad (8)$$

The first term on the left describes the integrated near-field pressure and shear stress forces scrubbing the aircraft's surfaces $\mathcal{S}_B$, and also includes propulsive surfaces moving relative to the aircraft's centre of mass. Alternatively, the second term on the left allows for propulsor representation via body forces. The combined forces on the left hand side equate to the far-field counterpart on the right hand side, which is integrated across the Trefftz plane $\mathcal{S}_{TP}$. Apart from the body forces, and if shear stresses on the Trefftz plane are to be neglected, Eq. (8) is the classical near versus far-field force balance, and is the typical starting point for more sophisticated far-field decomposition methods [43, 45, 60–63]. However, it is important to note from reviewing these studies, that the form of Eq. (8) is normally obtained from the RRF momentum formulation via mathematical substitutions primarily aimed at simply confining the far-field integration to

[§]The static pressure has been substituted for the gauge static pressure, i.e. $p_g = p - p_\infty$ as a further simplification.

the Trefftz plane only[¶] and not necessarily with the intention of transforming the analysis to the ARF. Whereas the derivation presented herein is shown to be a natural and direct outcome of the ARF perspective.

Eq. (8) only deals with forces, and instead a more holistic approach is to analyse aerodynamics in terms of work-energy relationships, as was first proposed by Drela [43], who developed a power-based decomposition from the RRF mechanical energy balance. However, as with the classical far-field force balance derivation, Drela [43] applies similar simplifying substitutions to the mechanical energy balance in the RRF to limit the far-field surface integrations to the Trefftz plane only. Drela [43], does not make any explicit mention that this simplification equates to an effective transformation to the ARF. However, Sanders and Laskaridis [51] made this connection explicitly and argued that contextualising the decomposition from the ARF perspective enables a more natural interpretation of the energy forms described by the various mathematical terms. This was supported via a rather limited mathematical transformation from the steady-flow RRF mechanical energy balance, and did not yet make the connection to use a moving control volume derivation to properly represent the ARF, which was later done by Sanders [56, 57]. The form of Eq. (4) is a significant improvement on this, as it is fully generalised and also readily applicable to unsteady flows too, which is left for consideration and further development in future work. Instead, for the purposes of this paper, Eq. (4) is applied to the moving control volume in Fig. 1, following previous assumptions, and is subsequently rearranged to give^{||}:

$$\begin{aligned}
 & \underbrace{- \iint (p_g \mathbf{V}'_{CS} - \bar{\bar{\tau}} \cdot \mathbf{V}'_{CS}) \cdot d\mathcal{S}_B}_{\mathcal{P}_S} + \underbrace{\iiint \rho \mathbf{f} \cdot \mathbf{V}' d\mathcal{V}}_{\mathcal{P}_f} + \underbrace{\iint (p_g \mathbf{V}_\infty - \bar{\bar{\tau}} \cdot \mathbf{V}_\infty) \cdot d\mathcal{S}_B}_{-\mathbf{F}_B \cdot \mathbf{U}} - \underbrace{\iiint \rho \mathbf{f} \cdot \mathbf{V}_\infty d\mathcal{V}}_{-\mathbf{F}_f \cdot \mathbf{U}} = \dots \quad (9) \\
 & \underbrace{\iint \left(\frac{\mathbf{V}^2}{2} \rho \mathbf{V}' + p_g \mathbf{V} - \bar{\bar{\tau}} \cdot \mathbf{V} \right) \cdot d\mathcal{S}_{TP}}_{\dot{\mathcal{E}}_{TP}} + \underbrace{\iiint -p_g \nabla \cdot \mathbf{V} d\mathcal{V}}_{\Theta} + \underbrace{\iiint \bar{\bar{\tau}} : \nabla \mathbf{V} d\mathcal{V}}_{\Phi}
 \end{aligned}$$

The term $\mathcal{P}_S$ describes the shaft power related to any moving propulsive surface, like a propeller for example, and is the work related to the motion of the control volume surface $\mathcal{S}_B$ relative to the aircraft's centre of mass. This near-field shaft power is confirmed by application of the total energy balance Eq. (5), which indicates that it corresponds to a total enthalpy rise in the flow, as described in Appendix B. The same appendix, also shows that body forces may be used to represent a propulsor instead of actual geometries, and that $\mathcal{P}_f$ is the corresponding power input that can also be shown, from the total energy balance Eq. (5), to equate to the enthalpy rise typically associated with shaft power.

The remaining terms on the left of Eq. (9), $\mathbf{F}_B \cdot \mathbf{U}$ and $\mathbf{F}_f \cdot \mathbf{U}$, denote the pseudo-work^{**} rate of the forces acting on the

[¶]This is achieved by first substituting $\mathbf{V}' = \mathbf{V}' - \mathbf{V}_\infty + \mathbf{V}_\infty$ into the RRF momentum balance formulation (and relying on the corresponding RRF mass balance equating to zero, see Ref. [62]), followed by positioning the other far-field planes sufficiently far away from the aircraft, such that they are immersed in free-stream flow.

^{||}It is noted that this work assumes a fully inclusive, uninterrupted control volume, and therefore does not need to include Drela's [43] " $\mathcal{P}_k$ " term, which is normally used to account for energy fluxes across the interfaces between the aircraft's and propulsor's separated control volumes.

^{**}following Mallinckrodt and Leff's [52] interpretation of work associated with integrated forces applied to the centre of mass of an object

flow (at the aircraft's flight velocity) by the aircraft surfaces $\mathcal{S}_B$ and body forces $\mathbf{f}$, respectively. To maintain dynamic equilibrium, and because quasi-steady flow has been assumed, any imbalance in the combination of these forces must be compensated for. Drela [43] suggests that a net negative streamwise force indicates an excess in power which must be converted to altitude potential energy by the aircraft in steady climb, or vice versa, and so:

$$W\dot{h} = \mathbf{F}_B \cdot \mathbf{U} + \mathbf{F}_f \cdot \mathbf{U} \quad (10)$$

where, W and $\dot{h}$ symbolise the aircraft weight and climb rate, respectively. Therefore, this term can exist on either the left or right side of Eq. (9) depending on whether the climb rate is negative or positive. In other words, if the aircraft is descending, then its altitude potential energy will be transferred to the atmosphere, whereas an ascending aircraft transfers a portion of the propulsive power into raising the aircraft's altitude potential energy instead of transferring it to the atmosphere.

The mechanical energy deposited in the wake via the Trefftz plane is given by $\dot{\mathcal{E}}_{TP}$ in Eq. (9), and may be decomposed into the ARF streamwise kinetic energy $\dot{E}_a$, transverse kinetic energy $\dot{E}_v$, and pressure work $\dot{E}_p$ constituents^{††}:

$$\dot{\mathcal{E}}_{TP} = \iint \left(\underbrace{\rho \frac{u^2}{2} u'}_{\dot{E}_a} + \underbrace{\rho \frac{(v^2 + w^2)}{2} u'}_{\dot{E}_v} + \underbrace{p_g u}_{\dot{E}_p} \right) d\mathcal{S}_{TP} \quad (11)$$

where $\langle u, v, w \rangle$ and $\langle u', v', w' \rangle$, are the ARF and RRF fluid velocity components, respectively.

The remaining terms Θ and Φ on the right of Eq. (9) are both Galilean invariant and are the volumetric pressure work and viscous dissipation, respectively. Θ measures the volumetric mechanical pressure power of the fluid expanding against atmospheric pressure. It is closely related to compressibility (zero for incompressible flow) and spans across regions of strong pressure gradients. It has the opposite sign in comparison to Drela's formulation [43], as its net contribution is deemed to be a loss and is thus placed on the power consumption side of the balance instead. This is in agreement with Sato's [44] and Lamprakis et al. [48] analyses, and only circumstantially shown to be negative where a highly compressive portion of the control volume has been omitted in the analysis due to the propulsor modelling approach taken [51]. In fact Θ may have very large local magnitudes which cancel out globally. This in-turn highlights the need for caution when using the " P_K " term in Drela's formulation [43], which is related to internal integrations of Θ and thus dependent on the chosen locations of its inlet and outlet interface planes. To avoid ambiguity, this work opts for using the shaft power, $\mathcal{P}_S$, and body force power, $\mathcal{P}_f$, which can be directly equated to total enthalpy rise across an adiabatic propulsor representation.

^{††}assuming the shear stress term to be negligible

Unlike Θ , the volumetric viscous dissipation Φ is strictly positive, and its net value is an accumulation of its local contributions. This is advantageous because it enables power consumption in the flow to be spatially decomposed and directly attributed to local flow mechanisms, such as BLs, free-shear layers (wake), jet plumes, and shocks:

$$\Phi = \Phi_{\text{BL}} + \Phi_{\text{wake}} + \Phi_{\text{jet}} + \Phi_{\text{shock}} \quad (12)$$

D. A Note on Including the Second Law of Thermodynamics

Inclusion of the second law of thermodynamics via the Gibbs equation, as performed by Arntz et al. [45], offers a more strict and explicit distinction between reversible and irreversible flow quantities. In so doing, the right hand side of the work-energy relationship is split into energy available to do work (exergy) versus energy that has been destroyed (anergy). In typical adiabatic flows, the difference is the isolation of thermal exergy terms, which could in theory be recuperated by a heat engine. However, in the context of BLI, it is the recovery of mechanical exergy that is of interest and the additional decomposition of the thermal exergy-anergy is not required. Lamprakis [46] has discussed this in detail with respect to the theory of adiabatic and non-adiabatic flows, supported by a boundary layer defect integral analysis method [47], and demonstrated on adiabatic and non-adiabatic unpowered [48] and powered fuselages [49], respectively.

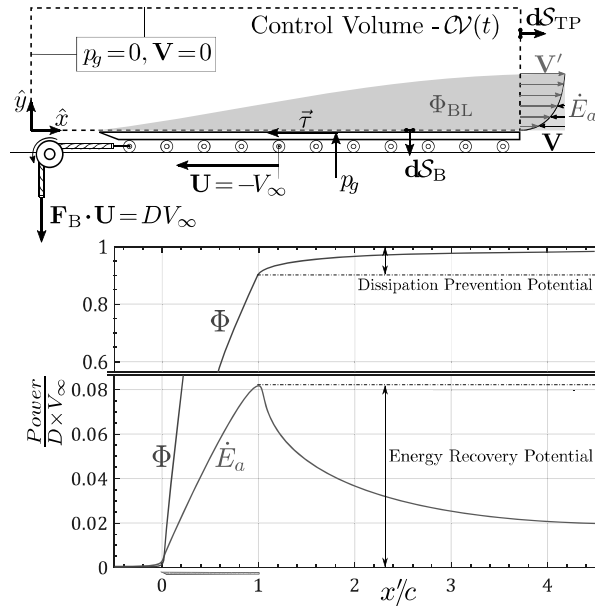


Fig. 2 Kinetic energy, $\dot{E}_a$, versus viscous dissipation, Φ , accumulation for a hypothetical flat plate being pulled through initially quiescent flow by a frictionless pulley system.

III. Available Energy from Flat Plate Aerodynamic Work

A. The Potential for Energy Recovery Hypothesis

This section considers steady, flat plate flows in isolation, where no aerodynamic propulsion is included, and is therefore referred to herein as "unpowered". In this scenario, the mechanical energy balance of Eq. (9) reduces to:

$$DV_\infty \approx \dot{E}_a + \Phi \quad (13)$$

where, for a quasi zero-pressure gradient flow, the wall normal velocity component v and pressure-dependent terms, such as Θ and $\dot{E}_p$, represent second order effects due to the flow streamlines being nearly tangent to the wall and $p \approx p_\infty$, respectively. Thus, the drag power energy exchange mechanisms for this flow are closely approximated by the sum of the streamwise kinetic energy flux $\dot{E}_a$ and the viscous dissipation rates Φ within the control volume.

From the ARF perspective, the unpowered flat plate must still maintain dynamic equilibrium, and requires that a force be applied to it in driving it through the air. Fig. 2 presents a hypothetical scenario, where a flat plate is viewed from the ARF and seen to be pulled through initially quiescent fluid by a frictionless pulley and cart system. The purpose of this diagram is to illustrate clearly the causal work-energy pathways. Firstly, work is done by a continuous force $\mathbf{F}_B$ applied to the rope at a constant velocity equal in magnitude to the flat plate's velocity $\mathbf{U}$. This work is then transferred perfectly to the flat plate, assuming no losses via the frictionless pulley and cart system, in the form of kinetic energy that is itself available to do work. In turn, the flat plate does work on the fluid via the no-slip condition, whereby the initially still fluid is pulled along with the flat plate's motion. Thus, kinetic energy has been imparted directly to the fluid in contact with the surface. Due to this acceleration, a boundary layer forms as a diminishing amount of the kinetic energy reaches those fluid layers that are located increasingly further away from the flat plate's surface. The outwardly diminishing kinetic energy in the boundary layer is due to the imperfect viscous work done between fluid layers in transferring this kinetic energy, and subsequently a large portion of energy is lost irreversibly via local viscous dissipation in raising the fluid's internal energy. This is further described by Renard and Deck's [50] ARF modified skin friction version of Eq. (13), which provides a comprehensive analysis of the intricate work-energy relationships of zero pressure gradient laminar and turbulent flow. Now, considering a vertical survey plane at a fixed $\hat{x}$ axis location, the energy lost via viscous dissipation Φ , ahead of the survey plane, rapidly accumulates as the flat plate passes by. This is accompanied by a much more gradual accumulation in local residual kinetic energy $\dot{E}_a$ left behind by the boundary layer, and reaches a peak once the plate's trailing edge has passed by, as illustrated in Fig. 2. At the instance the trailing edge passes this location, the entire work rate imparted to the flat plate ($\mathbf{F}_B \cdot \mathbf{U} = DV_\infty$) has been transferred to the flow ahead of the survey plane, and exists either as viscous dissipation or residual streamwise kinetic energy. As the plate continues to move beyond the survey plane, there is no more local work transfer to the flow. Therefore, the net residual streamwise kinetic energy that

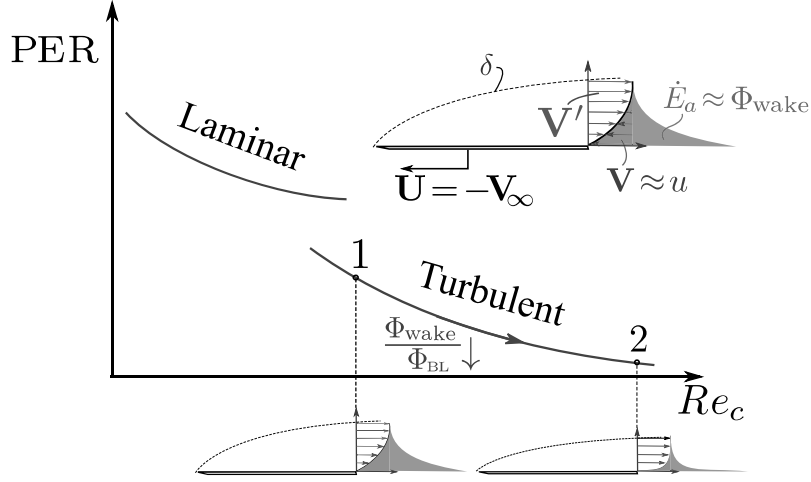


Fig. 3 Hypothesis of PER_{TE} as a function of Re_c with annotated velocity profiles and excess kinetic energy rates for two different Re_c numbers in turbulent flow.

was available energy, gradually decays as it is converted entirely irreversibly to internal energy via viscous dissipation. In theory, the peak net kinetic energy was energy available to do work, and could have potentially been harvested by an appropriate mechanism before decaying irreversibly to heat. It is proposed that BLI propulsion is a mechanism that can harvest this energy whilst also providing the thrust to overcome the friction forces between the flat plate and the fluid. A general Potential for Energy Recovery (PER) metric has been introduced to quantifying this premise by assuming that any residual drag power that has not yet been dissipated ahead of the Trefftz plane, should be available to do work [51]:

$$PER = 1 - \frac{\Phi}{DV_\infty} = \frac{\dot{\mathcal{E}}_{TP} + \Theta}{DV_\infty} \quad (14)$$

In the zero-pressure gradient unpowered flat plate scenario, PER may be simplified to the approximation:

$$PER \approx \frac{\dot{\mathcal{E}}_a}{DV_\infty} \quad (15)$$

Subsequently, a hypothesis may be formulated for the effect of the chords-based Reynolds number Re_c on the PER. The formation of a boundary layer of thickness δ , is viewed differently in the ARF from the more conventional RRF visualisation. This is depicted in the schematics of Fig. 3, which illustrate the ARF and RRF velocity profiles in purple and blue, respectively. The streamwise kinetic energy $\dot{\mathcal{E}}_a$ imparted to the flow via the no-slip condition, is determined according to ARF perspective and scales with u^2 (blue highlighted area), which is subject to the BL's velocity profile shape and subsequently dependent on Re_c . As depicted in Fig. 3, a higher Re_c results in a more concave BL profile, with larger gradients close to the wall and a thinner δ . This means that there is a greater concentration of dissipation occurring closer to the wall, which in turn results in a reduced amount of residual kinetic energy being left behind in the

boundary layer. Subsequently, there is less kinetic energy available to harvest and so PER reduces.

B. Energy Decomposition of Laminar flow

1. Blasius solution

For a zero pressure gradient Blasius flow, the drag power along the flat plate at each streamwise position is measured by the sum of the local streamwise kinetic energy flux and the upstream viscous dissipation, as per Eq. (13). Following the derivations of Drela [43], the kinetic energy is at its maximum value at the trailing edge of the flat plate and is given by:

$$\dot{E}_{aTE} = 0.142\rho_{\infty}V_{\infty}^3cRe_c^{-0.5} \quad (16)$$

The corresponding cumulative viscous dissipation can be written as:

$$\Phi_{TE} = 0.522 \int_0^c \rho_{\infty}V_{\infty}^3Re_c^{-0.5} dx \quad (17)$$

Finally, the stream-wise drag power generation along the flat plate is obtained from the skin friction as:

$$DV_{\infty} = 0.332 \int_0^c \rho_{\infty}V_{\infty}^3Re_c^{-0.5} dx \quad (18)$$

which simplifies to:

$$DV_{\infty} = 0.664\rho_{\infty}V_{\infty}^3Re_c^{-0.5} \quad (19)$$

Subsequently, based on the Blasius solution, the potential for energy recovery at the trailing edge of a laminar flat plate is:

$$PER_{TE}^{Blasius} = 1 - \frac{\Phi_{TE}^{Blasius}}{DV_{\infty}} \equiv \frac{\dot{E}_{aTE}}{DV_{\infty}} \simeq 21.4\% \quad (20)$$

This quantity describes the amount of kinetic energy, as a proportion of work input to overcome drag, that is available for recovery by a propulsive device.

2. CFD analysis

Figure 4 summarizes the energy decomposition along the flat plate and wake corresponding to laminar flows for $Re_c = 5 \times 10^5, 1 \times 10^6$ and four different M_{∞} numbers. The normalised $\dot{E}_a$ distributions in Fig. 4 a) show a clear Re_c dependency which runs counter to the preceding Blasius solution. This is mostly an outcome of the drag being underpredicted by the Blasius solution at the lower Re_c regimes as shown in Fig. 4 f). This deviation is discussed by White [64] and originates from the leading and trailing edge singularities of the flat plate in response to the discontinuous change of boundary conditions in the flow field resulting in localised pressure fields. These singularities and their effect

in the skin friction drag are also addressed in higher order theories with the inclusion of modified skin friction drag formulations [65–67]. From an energy perspective, these singularities are translated, locally, to excess pressure work $\dot{E}_p$ (see Fig. 4 c)) and volumetric compression/expansion work, Θ , the relative magnitude of which is a strong function Mach number as indicated by Fig. 4 c) and d). The rapid decay of the singularities' energy trace with increasing Re_c number in Fig. 4 c) signifies that the higher Re_c regime largely suppresses their presence and that the energy characteristics of the boundary layer more closely resemble the Blasius flow field described by Eq. (13). The discrepancy between the $\dot{E}_p$ magnitude of the two singularities indicates that the presence of the boundary layer flow at the trailing edge partially mitigates the effects of the singularity. For this non-lifting 2-D flow, the $\dot{E}_v$ term merely accounts for the transverse kinetic energy due to the wall-normal flow displacement with significant contributions for the lower Re_c spectrum, for which the boundary layer thickness displacement is larger.

C. Energy Decomposition of Turbulent flow

Figure 5 shows the evolution of the various energy terms of the turbulent flat plate flow for $Re_c = 1 \times 10^6, 1 \times 10^8$ and four different M_∞ numbers. The energy-based characteristics are primarily driven by the $\dot{E}_a$ and Φ evolutions, while the net $\dot{E}_p + \Theta$ singularity spike contributions measure less than 3% of the total power and rapidly subside for the highest Re range, as shown in Fig. 5 c) and d). In contrast with the self-similar Blasius profile, the boundary layer shape of the turbulent flat plate flow is clearly Reynolds number dependent, with higher Re_c values yielding more concave-shaped velocity profiles. This results in a substantial reduction in the rate of change of $\dot{E}_a/dx$ along the flat plate and in a correspondingly smaller energy recovery fraction of the drag power, as shown in Fig. 5 a). The $\dot{E}_v$ variation is a second order term as expected for high Re_c flows ($v \ll V_x$) which rapidly decays downstream of the flat plate and can be omitted from the energy balance without any significant compromise to accuracy. The superposition of the viscous dissipation plots in Fig. 5 e) for different free-stream Mach numbers, signifies that Φ is purely a function of Re_c number due to its effect on the boundary layer shape. Thus, higher Re_c number flows progressively shift larger fractions of dissipation from the wake to the upstream boundary layers, such that the ratio Φ_{wake}/Φ_{BL} decreases, in agreement with the hypothesis described in Fig. 3. It is conceptually useful to analyse the difference between the $Re_c = 1 \times 10^6, 1 \times 10^8$ dissipation curves by considering the relative contributions from their laminar (Φ_{lam}) and turbulent (Φ_{turb}) constituents. These are shown in the subplot of Fig. 5 e) as obtained from the dissipation integrand by considering in isolation the laminar and eddy viscosity components, as discussed in Appendix B. For $Re_c = 1 \times 10^6$, the relative contribution of Φ_{lam} is directly comparable to Φ_{turb} along the flat plate, while the dissipation rate inside the wake region is measured strictly by the turbulent mixing losses. The same reasoning applies for $Re_c = 1 \times 10^8$, however the turbulent dissipation component dominates with a ratio of $\Phi_{turb}/\Phi_{lam} > 2$ at almost every streamwise position. The implication of the increased turbulent dissipation is that for the $Re_c = 1 \times 10^8$ case, an additional $\approx 3.9\%$ energy fraction has been irreversibly dissipated at the trailing edge of the plate. Finally, the CFD skin friction predictions for different Re and

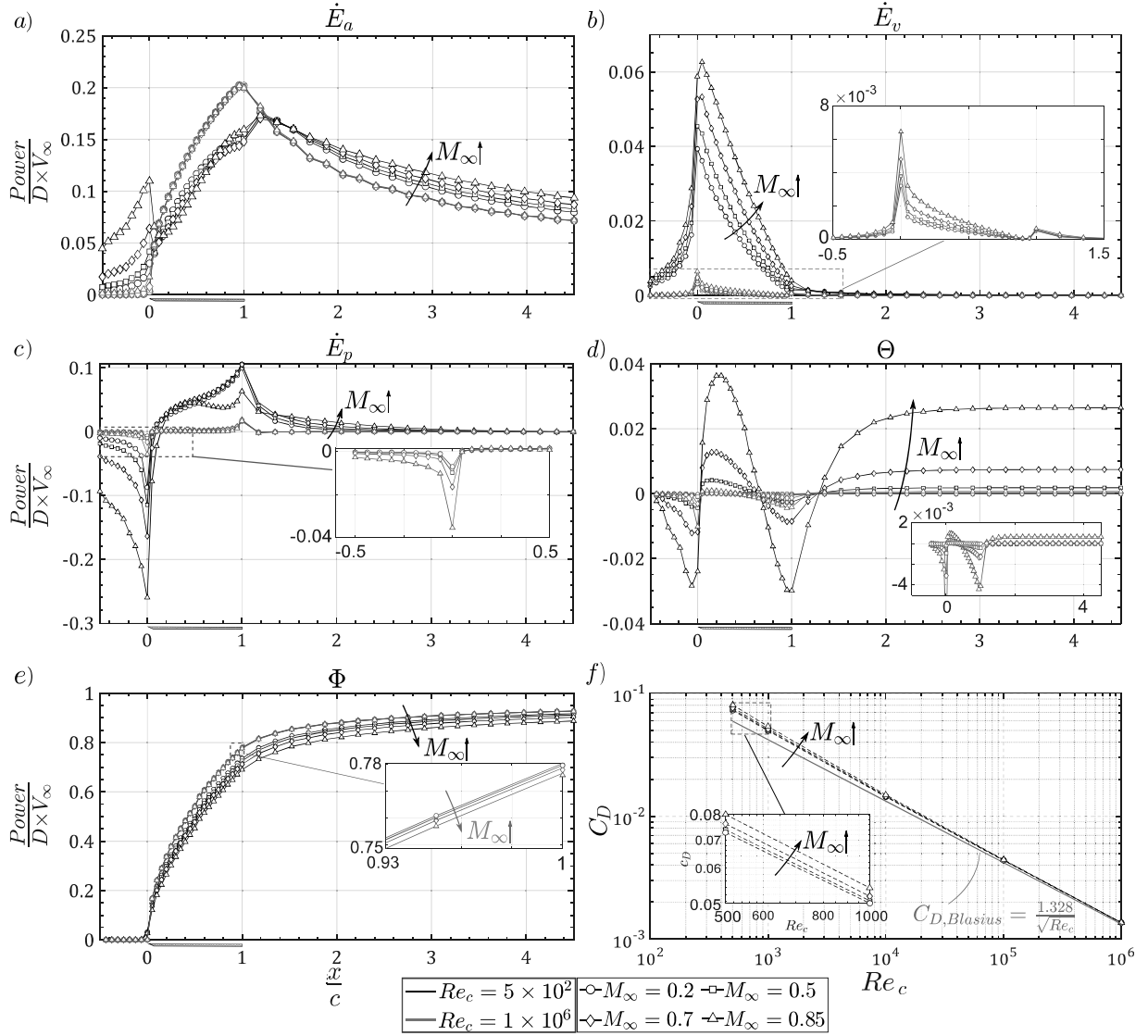


Fig. 4 Laminar flow energy decompositions along the plate ($x/c \leq 1$) and in the wake ($x/c > 1$), as well as C_D (compared against Blasius predictions), for varying M_∞ and Re_c .

Mach numbers (discrete points) along with the skin friction relation of White [68] (black line) are plotted in Fig. 5 f) as a non-dimensional measure of the drag power. The $M_\infty = 0.2$ data show excellent agreement with White's correlation with larger deviations to be noted only in the highest Re_c range mostly due to White's approximation assumptions [68]. Higher freestream compressibility manifests as a downward offset of the curves indicating a "relaxation net effect" due to beneficial thermo-compressible coupling effects arising from the boundary layer self-heating which outweigh the effects of singularities.

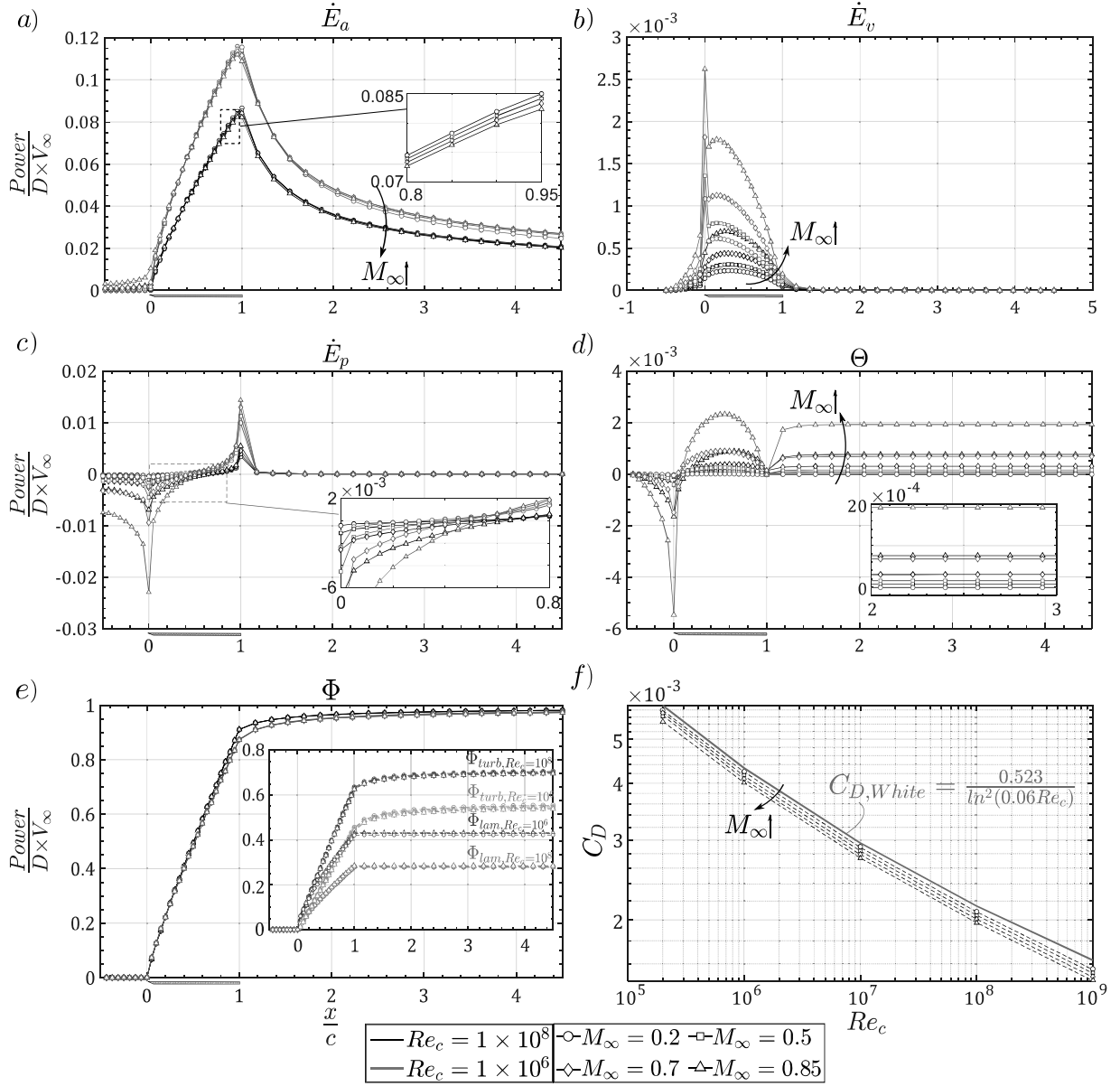


Fig. 5 Turbulent flow energy decompositions along the plate ($x/c \leq 1$) and in the wake ($x/c > 1$), as well as C_D (compared against White's predictions), for varying M_∞ and Re_c .

D. Trailing Edge Potential for Energy Recovery versus Reynolds Number

1. Flat Plate PER Evolution

For a quasi zero-pressure gradient flow, Eq. (14) is plotted in Fig. 6 for a $Re_c = 1 \times 10^6$ in laminar flow. The excess kinetic energy, $PER \approx \dot{E}_a / DV_\infty$, gradually accumulates along the length of the plate, reaching its peak at the trailing edge. The corresponding fraction $PER = 1 - \Phi / DV_\infty$ measures the drag power not yet lost via viscous dissipation. The two curves become coupled at the trailing edge, whereafter the decay in $\dot{E}_{TP}$ matches the accumulation in Φ . Therefore,

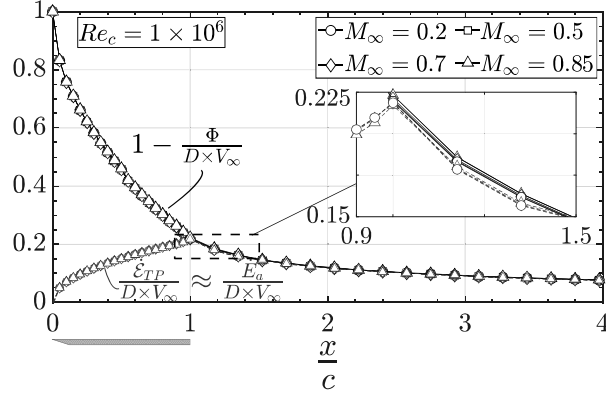


Fig. 6 Laminar flow available energy accumulation versus drag power consumption, along the flat plate and in its wake, at $Re_c = 1 \times 10^6$ and $M_\infty = 0.2, 0.5, 0.7, 0.85$.

the trailing edge location is likely the position of maximum energy recovery (PER_{TE}) and is in agreement with Drela's [43] observations.

2. Laminar and Turbulent Flow PER_{TE} Map

A non-dimensional representation of available energy at the trailing edge, PER_{TE} is summarized in Fig. 7. Qualitative comparison of the laminar and turbulent PER_{TE} curves confirms that laminar flows shift larger portions of dissipation from the body to the wake, yielding higher PER_{TE} values. The laminar PER_{TE} curves exhibit values of $> 30\%$ for the lowest Re_c number regime, thereafter gradually asymptoting to the Blasius $\approx 21.4\%$ prediction for the upper Re_c regime. With reference to the energy decomposition of Fig. 4, the mechanical energy increment between the PER plots and the Blasius prediction is mainly a corollary of the strong pressure boundary work $\dot{E}_p$ and the local Θ generated by the trailing edge singularity. For the upper Re_c laminar flow regime, the effect of the singularities rapidly subsides, such that the governing energy transfer mechanisms are well described by the Blasius power balance form. For turbulent flow, the boundary layer shape is associated with a combination of higher average-velocity gradients and higher effective viscosity that severely penalise the dissipation characteristics of the flow resulting in a substantial downward slope of the PER_{TE} curves, in agreement with the hypothesis graph of Fig. 3. For a fixed $Re_c = 1 \times 10^6$ condition, the excess boundary layer dissipation penalty yields a 43% reduction in the available energy fraction relative to laminar flow. The overlap of the turbulent PER_{TE} plots of different M_∞ indicates that the effect of singularities rapidly decays with increasing Re_c , rendering $\dot{E}_a$ and Φ as the dominant energy terms. For typical airliners at cruise conditions ($Re_c = 1 \times 10^8 - 5 \times 10^8$), Fig. 7 suggests that about 8 – 9% of the fuselage's drag power is available at its trailing edge for recuperation.

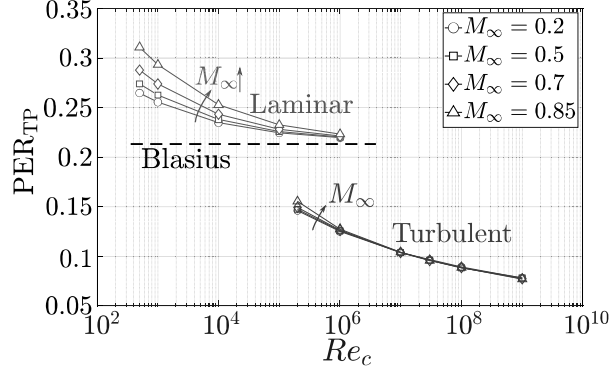


Fig. 7 PER_{TE} map of laminar and turbulent flat plate flows as a function of Re_c for various Mach numbers.

IV. Propelling Flat Plates using Harvested Boundary Layer Energy

A. Hypothesis for Propulsively Harvesting Energy

Representing the propulsor as an actuator disk (AD) defined by a body force vector field $\mathbf{f}$, provides a useful conceptual tool for interrogating BLI energy harvesting in a systematic manner. Fig. 8 depicts five scenarios for aerodynamically powering a flat plate through the flow. For all of these scenarios dynamic equilibrium is imposed, i.e. $Wh = 0$ in Eq. (10), and Eq. (9) reduces to:

$$\mathcal{P}_f = \dot{\mathcal{E}}_{TP} + \Theta + \Phi \quad (21)$$

To explain the five scenarios in Fig. 8, it is convenient to assume a control volume whose far-field boundaries extend infinitely far from the powered flat plate assembly in all directions, such that $\dot{\mathcal{E}}_{TP} \rightarrow 0$. As a result of this assumption,

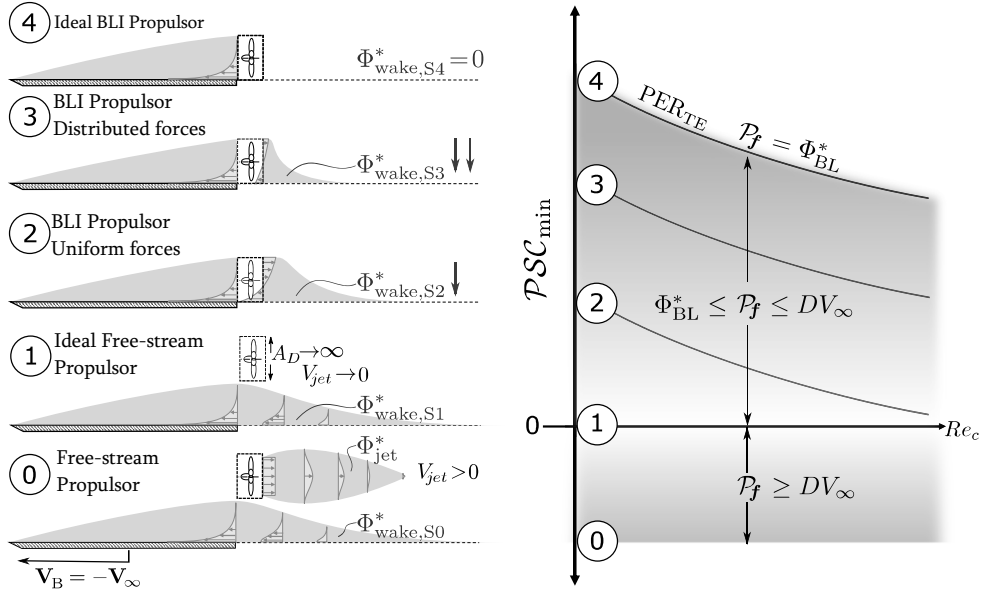


Fig. 8 Hypothesis of energy utilization and anticipated power saving trends versus Re_c .

the net Θ remaining is assumed to be the sum of locally produced irreversibilities that can be lumped together with the local viscous dissipation to give a combined local loss:

$$\Phi^* = \Phi + \Theta_{\text{irr}}, \quad \text{where, } \iiint_{\mathcal{V} \rightarrow \infty} \Theta_{\text{rev}} d\mathcal{V} = 0 \quad (22)$$

These local irreversibilities, Θ_{irr} are synonymous with the Baroclinic power described by Sato [44] and later by Lamprakis [46] and Lamprakis et al [48]. Eq. (21) now simplifies to:

$$\mathcal{P}_f = \Phi_{\text{BL}}^* + \Phi_{\text{wake}}^* + \Phi_{\text{jet}}^* \quad (23)$$

Scenario 0 (S0) in Fig. 8, is analogous of a podded propulsor installation that ingests free-stream flow. The finite diameter propulsor must produce a jet that dissipates viscously, Φ_{jet}^* . Additionally, the flat plate's BL shifts unopposed into the wake, where any available kinetic energy also dissipates viscously, i.e. $DV_\infty = \Phi_{\text{BL}}^* + \Phi_{\text{wake}}^*$. The power consumption of the AD, $\mathcal{P}_f$, must compensate for all this dissipation in order to maintain dynamic equilibrium, and so $\mathcal{P}_{f,S0} > DV_\infty$. The free-stream ingesting propulsor's (FIP) propulsive efficiency is derived from Eq. (9):

$$\eta_{\text{FIP}} = 1 - \frac{\Phi_{\text{jet}}^*}{\mathcal{P}_f} = \frac{\mathbf{F}_f \cdot \mathbf{V}_\infty}{\mathcal{P}_f} \quad (24)$$

Scenario 1 (S1) in Fig. 8 represents an ideal FIP with $\eta_{\text{FIP}} = 100\%$. This hypothetical, ideal FIP would require its diameter to approach infinity such that $\mathbf{V}_{\text{jet}} \rightarrow 0$ and $\Phi_{\text{jet}}^* \rightarrow 0$. Nonetheless, the power consumed by the propulsor in achieving dynamic equilibrium, is $\mathcal{P}_{f,S1} = \Phi_{\text{BL}}^* + \Phi_{\text{wake,S1}}^* = DV_\infty$. Using this as an appropriate baseline, a minimum Power Saving Coefficient for a BLI propulsor can be defined relative to the hypothetically ideal FIP:

$$\mathcal{PSC}_{\min} = \frac{\mathcal{P}_{f,S1} - \mathcal{P}_{f,\text{BLI}}}{\mathcal{P}_{f,S1}} = 1 - \frac{\mathcal{P}_{f,\text{BLI}}}{DV_\infty} \quad (25)$$

This is a minimum power saving because it is compared to a perfect free-stream ingesting propulsor, and it is natural to expect that the power savings will be greater when compared against a more realistic propulsor with its own intrinsic inefficiencies and having $\eta_{\text{FIP}} \leq 100\%$. A similar definition has been independently introduced by Baskaran et al. [18], but additional insight is gained by reformulating Eq. (25) to be a function of the accumulation of losses in the flow. Following the previous assumption, Eq. (22), the $\mathcal{PSC}_{\min}$ can be expressed in terms of changes in Φ^* relative to scenario 1 of Fig. 8:

$$\mathcal{PSC}_{\min} = \frac{(\Phi_{\text{BL,S1}}^* - \Phi_{\text{BL}}^*) + (\Phi_{\text{prop,S1}}^* - \Phi_{\text{prop}}^*) + (\Phi_{\text{wake,S1}}^* - \Phi_{\text{wake}}^*)}{DV_\infty} = \frac{\Delta\Phi_{\text{BL}}^* + \Delta\Phi_{\text{prop}}^* + \Delta\Phi_{\text{wake}}^*}{DV_\infty} \quad (26)$$

Here, the subscript "BL" refers to the losses in the boundary layer between the leading and trailing edge $0 \leq x/c < 1$, "prop" any intrinsic propulsor losses, and "wake" any losses behind the propulsor $x/c > 1$. Therefore, the energy harvesting process is futile if the upstream losses due to aerodynamic airframe/propulsion interaction penalties, in combination with propulsor intrinsic losses, exceed the downstream wake dissipation reduction. Conversely, Eq. (26) indicates that in the absence of Φ_{prop}^* and for fixed upstream BL losses $\Delta\Phi_{\text{BL}}^* \approx 0$, $\mathcal{PSC}_{\text{min}}$ is driven by the wake attenuating capabilities of the BLI propulsor, exclusively.

Scenario 2 (S2) in Fig. 8, assumes that the AD is placed at the trailing edge of the flat plate, and its height matches that of the incoming BL. Here, the body forces are assumed to be uniform along the AD and only a minor level of BL attenuation is achieved in the wake. This partial attenuation means that only a small portion of the available streamwise kinetic energy was harvested before dissipating in the wake. Assuming $\Phi_{\text{prop}}^* = 0$ and $\Delta\Phi_{\text{BL}}^* = 0$, Eq. (26) indicates that $\mathcal{P}_{f,S2} \leq DV_{\infty}$ because $\Delta\Phi_{\text{wake}}^* > 0$.

Scenario 3 (S3) in Fig. 8, distributes the AD's body forces based on the incoming BL profile in an attempt to fully harvest the streamwise kinetic energy and completely attenuate the wake at the trailing edge of the flat plate. However, it is unlikely that instantaneous full attenuation is achievable, and instead the remaining streamwise kinetic energy will go on to dissipate in the wake. Following the same assumptions ($\Phi_{\text{prop}}^* = 0$ & $\Delta\Phi_{\text{BL}}^* = 0$) there is an improvement over scenario 2, i.e. $\mathcal{P}_{f,S3} < \mathcal{P}_{f,S2}$ because $\Phi_{\text{wake,S3}}^* < \Phi_{\text{wake,S2}}^*$.

Finally Scenario 4 (S4) in Fig. 8, represents the hypothetical situation where the distribution in body forces is able to completely harvest the available kinetic energy, and in keeping with previous assumptions ($\Phi_{\text{prop}}^* = 0$ & $\Delta\Phi_{\text{BL}}^* = 0$). In this idealised scenario, no disturbances exist in the wake, which means that dynamic equilibrium is simultaneously achieved, as can be inferred from the right hand side of Eq. (8). Subsequently, the AD's power consumption only need compensate for the energy dissipated in the BL ahead of it, i.e. $\mathcal{P}_{f,S4} = \Phi_{\text{BL},4}^* = \Phi_{\text{BL},1}^*$, and the power saving coefficient of Eq. (25) equates to the potential for energy recovery, i.e. $\mathcal{PSC}_{\text{min}} = \text{PER}_{\text{TE}}$.

In scenarios 2-4 of Fig. 8, it is expected that the amount of power saving is dependent on Re_c , as was described previously with reference to Fig. 3 and supported by the numerical results of Fig. 7. The subsequent aim of this study was to conduct RANS CFD models of scenario 2 and 3 so that the hypothesis of Fig. 8 could be numerically tested, and its assumptions interrogated, as it is likely that the AD will introduce additional aerodynamic interference losses (the propulsor may affect the upstream boundary layer characteristics relative to the unpowered configuration) in trying to harvest energy from the boundary layer.

B. Propulsively Harvesting Laminar Boundary Layer Energy

The AD was first included at the trailing edge of a flat plate^{‡‡} in laminar flow, where uniform and distributed body force vector fields (described in Appendix C) were implemented to test the hypothesised scenarios of 2 and 3 depicted in Fig. 8. The primary objective was to see if the numerical models reflected the $\mathcal{PSC}_{\min}$ trends. The second objective was to test the validity/significance of the assumptions that $\Phi_{\text{prop}}^* = 0$ & $\Delta\Phi_{\text{BL}}^* = 0$.

1. Propulsor's Influence on Viscous Dissipation Accumulation in Laminar Flow

The viscous dissipation accumulation over the flat plate, and into the wake, is depicted in Fig. 9 for laminar flow. A comparison is made between the unpowered flat plate, uniform AD loading, and wake attenuating AD loading, respectively. Fig. 9 (a) examines a single, relatively high, Reynolds number, $Re_c = 1 \times 10^6$, at different free-stream Mach numbers $M_\infty = 0.2, 0.5, 0.7$. Here, Φ_{BL} is observed to be almost completely independent of M_∞ and nearly entirely invariant to the propulsor suction effect, for both uniform and distributed AD loadings. Across the AD, there is a slight "jump" increase in Φ in comparison to the unpowered flat plate, which may be attributed to two causes. The first is the subtle suction effect that the AD has on the BL just ahead of it, where a slight deceleration (relative to the atmosphere) of the flow increases gradients closest to the wall, i.e. $\Delta\Phi_{\text{BL}} < 0$. The second involves intrinsic additional losses incurred by the shear work of the AD's body forces acting on the incoming BL flow, i.e. $\Delta\Phi_{\text{prop}} < 0$. This is an inherent minimum loss that occurs, not necessarily due to limitations or assumptions in the propulsor modelling^{§§} only, but rather due to the shearing that takes place across the boundary layer when it is "forcefully" reshaped to a uniform profile. This is an inherent loss associated with propulsively harvesting the BL's available kinetic energy. In both aspects, the wake attenuating distribution of body forces introduces slightly more dissipation just as the BL is shed into the wake, and is also observed to be marginally more sensitive to the compressibility of the flow, as can be seen by slight increase in accumulated dissipation with M_∞ . Thereafter, the Φ is observed to continue accumulating behind the uniformly loaded AD, which suggests that it was unable to harvest all of the BL's streamwise kinetic energy. Nonetheless, this dissipation accumulation is observed to be lower than that in the wake of the unpowered flat plate, which suggests that at least some BL energy recovery along with a reduction in the AD power $\mathcal{P}_f$ to achieve dynamic equilibrium. On the other hand, the accumulation of Φ just behind the distributively loaded AD (targeting full wake attenuation) is observed to cease completely in the wake. This suggests that almost all of the BL's available streamwise kinetic energy has been harvested and utilised in achieving dynamic equilibrium, thereby significantly reducing the AD power consumption, despite the slight increase in local losses due to the effect of the AD's presence on the BL.

Fig. 9 (b) examines a single, relatively low, Mach number, $M_\infty = 0.2$, at different Reynolds numbers, $Re_c =$

^{‡‡}see Appendix A.A for CFD setup.

^{§§}Viscosity remains activated within the propulsor cell zone. Although it could be deactivated, retaining it allows for an assessment of the inherent losses associated with wake attenuation, as well as any numerical dissipation introduced.

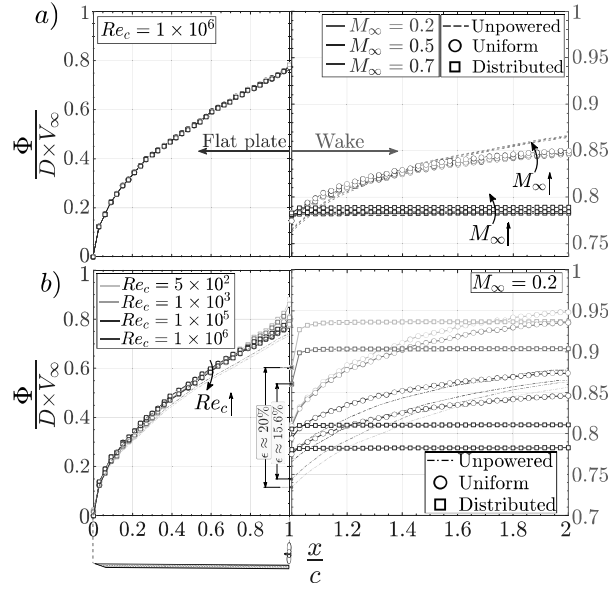


Fig. 9 Powered versus unpowered flat plate's dissipation Φ accumulation in laminar flow for a) Fixed Re_c and varying M_∞ b) Fixed M_∞ and varying Re_c .

$5 \times 10^2, 1 \times 10^3, 1 \times 10^5, 1 \times 10^6$. As anticipated, the rate of viscous dissipation accumulation $d\Phi/dx$ in zero-pressure gradient flow along the unpowered flat plate ($0 \leq x/c \leq 1$), decreases with increasing Re_c . However, in the powered cases, the observed rise in $d\Phi/dx$ ahead of the AD for low Re_c , indicates that these flows are increasingly susceptible to the suction effect of the AD, whereas high Re_c flows are more resistant. As an example, for the lower $Re_c \leq 10 \times 10^3$ regimes, the suction of the distributively loaded AD severely alters the boundary layer shape in its near vicinity by decelerating and stretching it. This results in an abrupt increase of the rate of change of dissipation $d\Phi_{BL}/dx$ and a subsequent upstream dissipation penalty ($\Delta\Phi_{BL} < 0$) measuring $\epsilon \approx 15.6\%$ and $\epsilon \approx 20\%$ for $Re_c = 1 \times 10^3$ and $Re_c = 5 \times 10^2$, respectively, as indicated in Fig. 9 (b). Moreover, this penalty is exacerbated by compressibility effects at higher free-stream Mach numebers, which is summarised in Fig. 10.

Fig. 10 a) and b) depict the effect of M_∞ on the distribution of the volume specific dissipation $\approx \mu(\partial V/\partial y)^2$ across the normalised boundary layer $y/\delta_{REF}^\ddagger$ at two different positions ahead of the AD, $x/c = 0.8$ and $x/c = 0.98$, respectively. The results are plotted for the unpowered, uniformly loaded, and distributively loaded AD corresponding to $Re_c = 1 \times 10^3$ and $M_\infty = 0.2, 0.5, 0.7$, respectively. In general, the AD's suction increases progressively with higher M_∞ , which amplifies the dissipative characteristics of the boundary layer closer to the wall. This amplification becomes particularly high for the distributively loaded, wake attenuating AD, due to the abrupt deceleration of the thinner and higher momentum shear layers adjacent to the wall. From a comparison between the two positions of Fig. 10 a) and b), it is clearly evident to see that this effect becomes far more dominant in the vicinity closest to the AD (i.e $x/c = 0.98$).

$^\ddagger \delta_{REF}$ is the BL thickness of the unpowered flat plate at the same position at $M_\infty = 0.2$

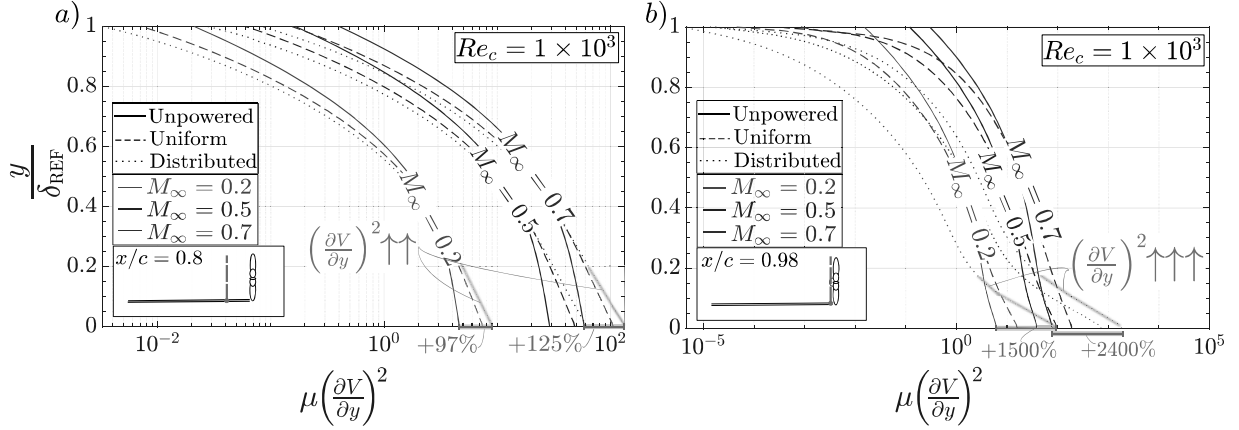


Fig. 10 Propulsor suction effect on the volume specific dissipation distribution across the laminar boundary layer for fixed $Re_c = 1 \times 10^3$, varying M_∞ and two positions: a) $x/c = 0.8$ b) $x/c = 0.98$.

Following this explanation and returning to Fig. 9 (b), the result is that there is sudden, almost, discontinuous, increase in the viscous dissipation accumulation across the AD. At the lower Reynolds numbers, and in particular for the distributively loaded AD, this additional dissipation penalty raises the total accumulated dissipation significantly above that of the unpowered flat plate, despite having attenuated the wake. This implies a highly inefficient streamwise kinetic energy harvesting process whereby, under low Re conditions, the AD's power consumption would exceed that of an ideal free-stream ingesting propulsor, i.e. $\mathcal{P}_f > DV_\infty$ because $\Delta\Phi_{BL} + \Delta\Phi_{prop} \ll 0$. In other words, the dissipation penalties incurred in attenuating the wake exceed the amount of kinetic energy harvested during the process, resulting in a negative power saving $\mathcal{PSC}_{min} < 0$ as per Eq. (26). This runs contrary to the hypothesised graph in Fig. 8, for low Re numbers in laminar flow because the assumptions that $\Delta\Phi_{BL} \approx 0$ and $\Delta\Phi_{prop} \approx 0$ are no longer valid under these conditions. However, the higher Re number flows behaved as anticipated, $\Delta\Phi_{BL} \approx 0$ and $\Delta\Phi_{prop} \approx 0$, and it is shown later, via a similar analysis, that the hypothesis trends seems to hold true for turbulent flows, particularly at low Mach numbers.

2. Propulsor's Influence on Mechanical Energy Outflow in Laminar Flow

The evolution of the drag power-normalised mechanical energy outflow of the unpowered and powered flat plate configurations is summarized in Fig. 11 a) and b) for laminar flow with varying M_∞ and Re_c , respectively. Similar to the preceding dissipation analysis, Fig. 11 a) examines a single, relatively high, Reynolds number, $Re_c = 1 \times 10^6$, at different free-stream Mach numbers $M_\infty = 0.2, 0.5, 0.7$. As observed for $d\Phi/dx$ along the flat plate ($0 \leq x/c \leq 1$), the rate-of-change in mechanical power outflow per unit length, $d\dot{\mathcal{E}}_{TP}/dx$, appears to be relatively independent of M_∞ and insensitive to the AD's suction and associated compressibility effects. However, although $\dot{\mathcal{E}}_{TP}$ is the same between unpowered and powered scenarios, the proportions of its constituents, $\dot{\mathcal{E}}_a$ and $\dot{\mathcal{E}}_p$, are not. In the unpowered case, $\dot{\mathcal{E}}_{TP} \approx \dot{\mathcal{E}}_a$ (because $\dot{\mathcal{E}}_p \approx 0$, as shown in in Fig. 4) and accumulates to a peak value at the trailing edge. However, for the

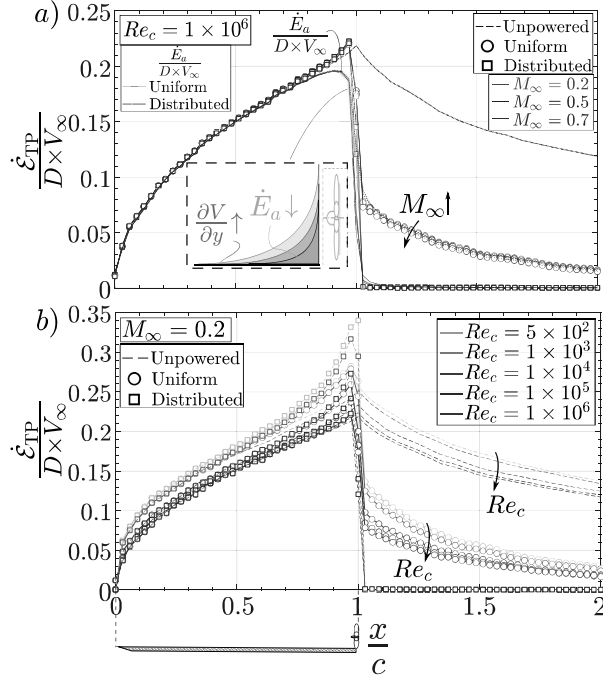


Fig. 11 Powered versus unpowered flat plate's mechanical energy flux $\dot{\mathcal{E}}_{TP}$ in laminar flow for a) Fixed Re_c and varying M_∞ b) Fixed M_∞ and varying Re_c .

powered cases shown in Fig. 11 a), $\dot{E}_p > 0$ as the BL approaches the AD, meaning that $\dot{E}_a$ must have been reduced to maintain the same $\dot{\mathcal{E}}_{TP}$ as the unpowered case. As shown by the sketch in Fig. 11 a), the AD's suction effect promotes a more concave BL profile, thereby reducing $\dot{E}_a$ (and increasing local dissipation), which is now found to have a lower peak slightly ahead of the trailing edge. At this relatively high Re, this mechanism occurs to an almost identical degree for each M_∞ , as is shown by the respective plots. However at lower Re numbers, the flow is more sensitive to the AD's suction effect. Nonetheless, examining and comparing $\dot{\mathcal{E}}_{TP}$ in the wake, shows that the uniformly loaded AD harvests some of $\dot{E}_a$ (with the remainder decaying as it dissipates viscously), whereas the distributively loaded, wake attenuating AD, completely harvests all of $\dot{E}_a$ by eliminating it from the wake. This energy harvesting is also found to be relatively insensitive to M_∞ .

Now turning to Fig. 11 b), which depicts the evolution of $\dot{\mathcal{E}}_{TP}$ for a relatively low constant Mach, $M_\infty = 0.2$, and a range of Re numbers, $Re_c = 5 \times 10^2, 1 \times 10^3, 1 \times 10^5, 1 \times 10^6$, it is clear that the AD's suction effect has a significant impact on $\dot{\mathcal{E}}_{TP}$ and not only $\dot{E}_a$. In accordance with Fig. 7, lower Re numbers result in greater proportions of $\dot{E}_a$, and therefore $\dot{\mathcal{E}}_{TP}$. However, a more curious observation is that the proportion $\dot{\mathcal{E}}_{TP} / DV_\infty$ increases for the powered cases, and for the distributively loaded AD in particular. This gives the misleading impression that there is, proportionally speaking, more energy available to be harvested. The issue here is that the drag power used to normalise these curves, belongs to the unpowered case. But the suction effect, to which low Re numbers are particularly prone, causes higher gradients

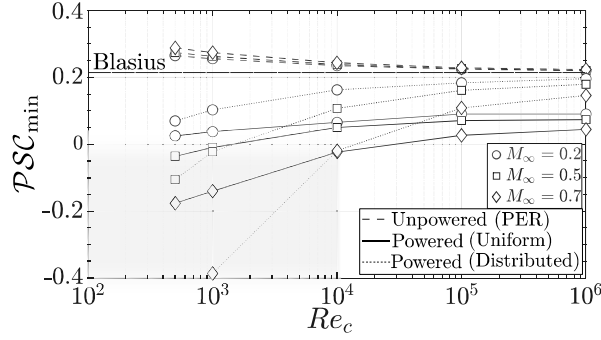


Fig. 12 Powered laminar flat plate flow $\mathcal{PSC}_{\min}$ versus Re_c at different M_∞ 's, for the uniformly and distributively loaded AD compared against the unpowered PER_{TE} .

near the wall, which increases the dissipation accumulation in the BL as was discussed around Fig. 10 b). Therefore, although the absolute amount of $\dot{\mathcal{E}}_{TP}$ may have increased, it has been accompanied by an even larger increase in BL dissipation, i.e. $|\Delta\Phi_{BL}| > |\Delta\dot{\mathcal{E}}_{TP}|$ at $x/c = 1$. It was necessary to normalise the curves by the same "drag power" for consistency, and this emphasises the need to examine both dissipation and mechanical power outflows together before drawing any final conclusions. Subsequently, for laminar flows, it may be understood that the AD's suction effect causes dissipative penalties that more than override the benefits of reduced Re_c prescribed by Fig. 7. Nonetheless this sensitivity to the suction effect diminishes as Re_c is increased, and the benefits of harvesting $\dot{\mathcal{E}}_a$ will begin to outweigh the dissipation penalties. However, once Re_c has been increased sufficiently, such that the BL has become insensitive to the AD's suction effect, then the benefits will start to decrease as the proportion of $\dot{\mathcal{E}}_a$ available for recovery reduces. This leads on to the $\mathcal{PSC}_{\min}$ trends, which are discussed next.

3. Combined Mach-Reynolds Number Effects on $\mathcal{PSC}_{\min}$ in Laminar Flow

The non-dimensional power saving curves of the laminar BLI flat plate configuration are obtained as a function of the chords-based Reynolds number Re_c and for 3 different freestream Mach numbers, as shown in Fig. 12. The superpositioned dashed lines indicate the hypothetical maximum $\mathcal{PSC}_{\min} = PER_{TE}$ attainable via an ideal wake attenuation process along with the hypothetical Blasius limit prediction. The trends obtained from the numerical results differ significantly from those hypothesised in Fig. 8, because the assumptions did not hold under the low Re_c flow cases, particularly at high M_∞ and for the distributively loaded, wake attenuating AD. Instead, the AD's suction effect caused significant additional viscous dissipation in the upstream BL, which was in addition to its own losses due to the irreversible shear work performed on the BL in attempting to attenuate the wake. Subsequently, and with reference to Eq. (26), $\mathcal{PSC}_{\min} < 0$ because $|\Delta\Phi_{BL} + \Delta\Phi_{prop}| > |\Delta\Phi_{wake}|$ where $(\Delta\Phi_{BL} + \Delta\Phi_{prop}) < 0$ and $\Delta\Phi_{wake} > 0$. However, it was observed that the flow became ever more resistant to the AD's suction effect as Re_c was increased, which reduced the associated dissipation penalties and led to a shift in balance where they were eventually outweighed by the wake attenuation gains, i.e. $|\Delta\Phi_{BL} + \Delta\Phi_{prop}| < |\Delta\Phi_{wake}|$. The rise in $\mathcal{PSC}_{\min}$ with continued increase in Re_c , soon

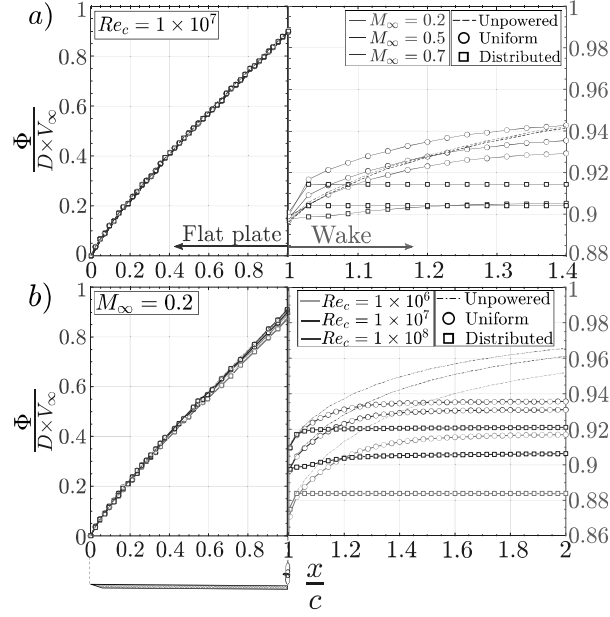


Fig. 13 Powered versus unpowered flat plate's dissipation Φ accumulation in turbulent flow for a) Fixed Re_c and varying M_∞ b) Fixed M_∞ and varying Re_c .

plateaued because of the counteracting mechanism of reduced available energy, as described by the PER trends of Fig. 7. Eventually, as Re_c is raised further, the suction effect on Φ_{BL} becomes negligible and, for the distributively loaded AD at low M_∞ , the $\mathcal{PSC}_{min}$ converges on the PER_{TE} limit. The negative offset of $\mathcal{PSC}_{min}$ with increasing M_∞ eludes to intrinsic compressibility losses incurred by the AD in the shear force work performed in attempting to attenuate the wake, i.e $\Phi_{prop}^* \propto M_\infty$. Finally, the lower $\mathcal{PSC}_{min}$ values for the uniformly loaded AD, at the higher Re_c numbers, clearly shows that the distributively loaded AD extracted and utilised more energy from the BL.

C. Propulsively Harvesting Turbulent Boundary Layer Energy

1. Propulsor's Influence on Viscous Dissipation Accumulation in Turbulent Flow

The turbulent flow evolution of the drag power-normalised dissipation for the unpowered and powered flat plate with uniform and distributed forces, is shown in Fig. 13 a) and b) for varying M_∞ and Re_c numbers, respectively. Fig. 13 (a) examines a single Reynolds number, $Re_c = 1 \times 10^7$, at different free-stream Mach numbers $M_\infty = 0.2, 0.5, 0.7$. Over the flat plate ($0 \leq x/c \leq 1$), the dissipation curves are superimposed over one another, and shown to be completely independent of M_∞ , as well as the propulsor's suction effect. Thus, the upstream dissipation of turbulent flow is shown to be resistant to pressure gradients in agreement with the analysis of Hall et al. [5]. The AD's intrinsic losses cause sharp rise in dissipation just behind the AD, particularly evident in the higher $M_\infty = 0.7$ case. This rise is attributed to a slight delay in the counteracting shear work induced by the deceleration of the flow (wrt the ARF) by the AD acting on the BL. This rise detracts from the gains obtained from preventing wake dissipation, which is particularly noticeable

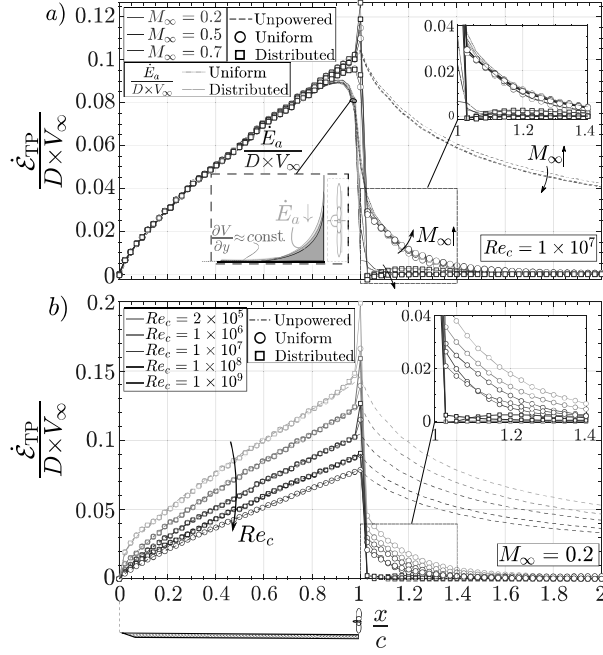


Fig. 14 Powered versus unpowered flat plate's mechanical energy flux $\dot{\mathcal{E}}_{TP}$ in turbulent flow for a) Fixed Re_c and varying M_∞ b) Fixed M_∞ and varying Re_c .

in the case of the uniformly loaded AD propulsion at the high $M_\infty = 0.7$. Nonetheless, there is a clear net gain for all scenarios, but especially for the distributively loaded AD at low M_∞ , which manages to eliminate most of the BL dissipation that would have occurred within the wake. From this it may be inferred that the AD successfully harvested some of the available streamwise kinetic energy, at the cost of some shearing work to achieve attenuation, towards achieving dynamic equilibrium.

Fig. 13 (b) examines a single Mach number, $M_\infty = 0.2$, at different Reynolds numbers $Re_c = 1 \times 10^6, 1 \times 10^7, 1 \times 10^8$. The dissipation along the flat plate is still observed to be independent of the AD's suction effect, confirming that it is fairly resistant to pressure gradients when turbulent (this confirms once again the observations of Hall et al. [5]). Therefore the rate of dissipation accumulation $d\Phi/dx$ is dependent solely on Re_c , which determines the shape of the BL and the proportions of its energy content in terms of dissipation versus kinetic energy. Thereafter behind the AD, it is clearly shown that the both uniformly and distributively loaded AD incur few intrinsic losses at low M_∞ and have successfully prevented further viscous dissipation in the wake. Subsequently, it may be inferred that a large portion of the BL's streamwise kinetic energy was harvested by the AD, particularly in the case of distributive forces, and put towards achieving dynamic equilibrium.

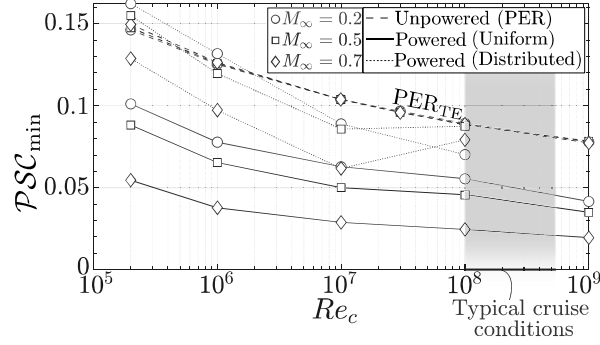


Fig. 15 Powered turbulent flat plate flow $\mathcal{PSC}_{\min}$ versus Re_c at different M_∞ 's, for the uniformly and distributively loaded AD compared against the unpowered PER_{TE} .

2. Propulsor's Influence on Mechanical Energy Outflow in Turbulent Flow

The turbulent flow evolution mechanical energy outflow for the unpowered and powered flat plate with uniform and distributed forces, is shown in Fig. 14 a) and b) for varying M_∞ and Re_c numbers, respectively. Fig. 14 (a) examines a single Reynolds number, $Re_c = 1 \times 10^7$, at different free-stream Mach numbers $M_\infty = 0.2, 0.5, 0.7$. Unlike the viscous dissipation, the mechanical energy outflow is clearly influenced by the propulsor's suction effect. Fig. 13 (a) shows that the amount of streamwise kinetic energy $\dot{E}_a$ is reduced in exchange for pressure boundary work $\dot{E}_p$, which is more pronounced for the distributively loaded AD at higher M_∞ . This suggests that less of the BL's energy is readily available in kinetic energy form and instead the proportion in $\dot{E}_p$ is a contributor to compressibility related losses. Therefore, lower savings should be anticipated at higher M_∞ .

Fig. 14 (b) examines a single Mach number, $M_\infty = 0.2$, at different Reynolds numbers $Re_c = 1 \times 10^6, 1 \times 10^7, 1 \times 10^8$. This plot clearly indicates how there is a greater accumulation in available kinetic energy at lower Re_c , as hypothesised, but that these lower Re_c are more sensitive to the propulsor's suction effect, described above. The BL is thinner at higher Re_c , and therefore less influenced by pressure gradients. Nonetheless, a near perfect extraction of the BL's energy is achieved for the distributively loaded actuator disk versus the uniformly loaded AD. But both do achieve a significant benefit relative to the unpowered case.

3. Combined Mach-Reynolds Number Effects on $\mathcal{PSC}_{\min}$ in Turbulent Flow

The non-dimensional power saving curves of the turbulent BLI flat plate configuration are calculated directly from the power consumption of the AD (see Appendix C) and depicted as a function of the chords-based Reynolds number Re_c and for 3 different freestream Mach numbers, as shown in Fig. 15. The $\mathcal{PSC}_{\min}$ curves now closely resembles the hypothesis graph of Fig. 8 with $\mathcal{PSC}_{\min} > 0$ over the entire regime, and indicate reduction in benefit for increased Re_c and M_∞ . The PER_{TE} curves delimit the upper hypothetical power saving bounds with values substantially lower than that of laminar flow. The power saving of the uniformly loaded AD reduce monotonically with Re_c with the

rate of change $d\mathcal{PSC}/dRe_c$ being imposed by the PER slope. Thus, in the absence of notable upstream dissipation penalties ($\Phi_{BL} - \Phi'_{BL} \approx 0$), the power savings are only function of the residual mechanical energy deposited in the wake (wake dissipation). The source of the vertical offset of the power saving trends with increasing M_∞ is justified from the reduction of the available upstream mechanical energy due to the stronger suction effect of the propulsor and the slight increase in the downstream dissipation (see Fig. 13 a)). The wake attenuator AD significantly increases the power savings, resulting in an upward shift of the corresponding $\mathcal{PSC}_{min}$ curves closer to PER limit. At $Re_c = 2 \times 10^5$, the corresponding $\mathcal{PSC}_{min} \approx 15.7\%$ even exceeds that of PER indicating either a beneficial aerodynamic synergy between the propulsor and the upstream viscous flow ($\Delta\Phi_{BL} > 0$)[§] or numerical inaccuracies affiliated with the source terms and slight net force mismatching. Similar numerical uncertainties are also associated with the monotonicity change of the wake attenuator trends at $Re_c = 1 \times 10^8$ due to contributions from Θ that fluctuate with M_∞ and Re_c in response to the trailing edge singularity and the abrupt attenuation process. A key advantage of the present non-dimensional $\mathcal{PSC}_{min}$ map representation is that it may be used to predict and map potential power savings for a wide range of operating conditions. The blue highlighted region on the graph denotes the typical Re_c range for typical airliners (such as the A320) with power saving estimates of $\mathcal{PSC}_{min} \approx 5\% - 9\%$, rendering BLI propulsion as an attractive means for improved aerodynamic performance.

V. Fuselage Boundary Layer Energy Harvesting

A. Reference Fuselage Geometry and Specifications

An A320 NEO axisymmetric fuselage approximation (fuselage shape and dimensions obtained from Ref. [69]) is selected for the subsequent studies and a comparison of the actual and approximated geometries is illustrated in Fig. 16. The fuselage has an overall length of $c = 37.57m$, a maximum diameter of $D = 3.95m$ and is comprised of an elliptical nose, a cylindrical midbody section and a conical aftbody with a sharp trailing edge. The reference cruise conditions for the subsequent studies are representative of an A320 with a cruising altitude of 37000 ft and a flight Mach number of $M_\infty = 0.78$. Hereafter, all the powered BLI fuselage CFD studies will be carried out at zero net vehicle force conditions for the fuselage-propulsor assembly.

B. Energy Decomposition of the Unpowered Fuselage

The power decomposition transfers among the various energy constituents of the unpowered axisymmetric fuselage are summarized in Fig. 17 for three different Re_c and M_∞ . Here, only the energy transfers in the wake behind the trailing edge are shown, and the evolution of energy terms over the body are omitted for brevity. The negative $\dot{E}_p$ contributions in the near wake region signify that in the presence of strong pressure gradients, the total mechanical

[§]This is permissible from the $\mathcal{PSC}_{min}$ definition (Eq. (26)) and a possible outcome of the interaction between the trailing edge singularity and the quasi discontinuous behaviour of the propulsor.

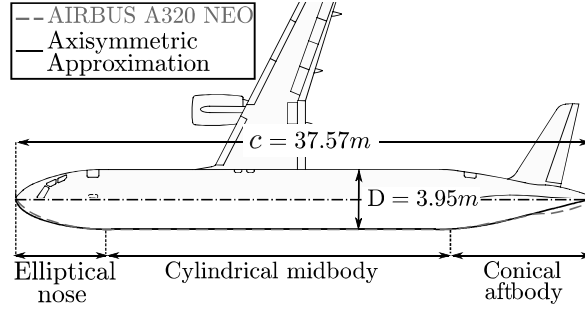


Fig. 16 (Upper half) AIRBUS A320 NEO fuselage dimensions, obtained from Ref. [69]. (Lower half) Axisymmetric approximation for the numerical simulations.

energy outflow leaving the Trefftz plane, Eq. (11), is primarily governed by the energy exchange among its energy constituents, with $\dot{E}_p$ partially negating the seemingly large kinetic energy outflow fraction. The pressure-velocity energy transfer is terminated at a short distance downstream of the trailing edge ($x/c < 0.2$) where the pressure field of the fuselage rapidly subsides ($\dot{E}_p = 0$). The flow then transitions to a free shear layer, similar to a flat plate wake, with its downstream residual excess kinetic energy being gradually converted into dissipation.

For high M_∞ flows, the pressure field of the body favours additional thermo-compressible energy exchange that shifts the $\dot{E}_k$ and $\dot{E}_p$ values at the trailing edge further apart, with a larger $\dot{E}_p$ offset being balanced out by a correspondingly greater Θ contribution. The rapid decay of Θ along the near wake region results, mainly, from the isentropic thermo-compressible energy exchange due to the spatial expansion of the flow ($p_g \nabla \cdot \mathbf{V} < 0$ locally) which eventually yields a net positive (loss) residual power trace Θ_∞ for a fully expanded flow. The origin of Θ_∞ is related to the entropic portion of Θ , i.e the baroclinic power imparted to the flow due to non-isentropic flow compression and expansion with substantial contributions for transonic flows with shock formation or flow fields with significant heat transfer effects, as detailed in Ref. [48]. For the adiabatic flow scenarios examined herein, the compressibility-induced losses represent merely a second-order effect $\Theta < 1\%$ even for the highest M_∞ regime (without shock formation), so that the total mechanical energy loss is governed by viscous dissipation.

From a dissipation perspective, higher Re_c values amplify the relative magnitude and thus power transfer among $\dot{E}_k$, $\dot{E}_p$ and Θ with the net overall effect being reflected as a corresponding increase in the rate of change of dissipation $d\Phi/dx$, in agreement with the flat plate analysis. For fixed Re_c , the effect of increasing M_∞ manifests as a vertical offset of the curves without explicitly affecting the dissipative characteristics ($d\Phi/dx$ same for all curves). Closer inspection of the Θ_∞ residual indicates that its energy fraction approximately corresponds to the dissipation decrement of the respective dissipation curve, relative to the incompressible scenario ($M_\infty = 0.2$). That is, the seeming dissipation reduction benefit for higher M_∞ has been merely manifested in the form of baroclinic power excess. Following Eq. (22) and the notation by Sato [44], a compressibility-corrected dissipation may be introduced as the sum of the dissipation at each wake point

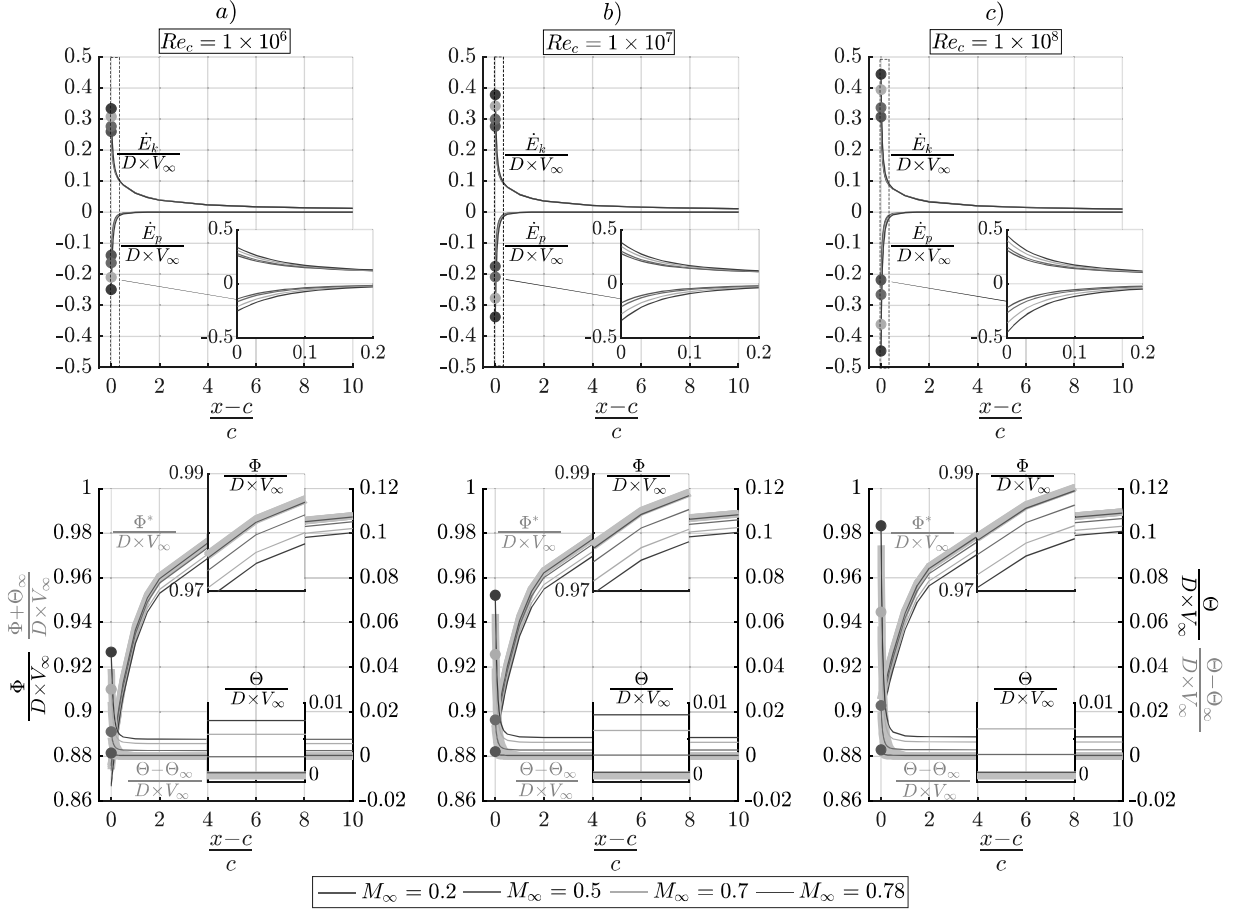


Fig. 17 Unpowered fuselage energy decomposition of $\dot{E}_k$, $\dot{E}_p$ (first row) and Φ , Θ (second row) at various M_∞ and Re_c numbers (columns): a) $Re_c = 1 \times 10^6$ b) $Re_c = 1 \times 10^7$ c) $Re_c = 1 \times 10^8$.

plus the affiliated Θ_∞ residual corresponding to a fully expanded flow:

$$\Phi^* \approx \Phi + \Theta_\infty \quad (27)$$

where Φ^* measures the approximate total mechanical energy loss due to dissipative and baroclinic losses. The compressibility-corrected dissipation curves, (shown in grey for all M_∞) collapse approximately on the incompressible ($M_\infty = 0.2$) dissipation trend for all Re_c numbers, signifying that the Φ^* evolution is compressibility independent. For completeness, the normalised local deviation ($\Theta - \Theta_\infty$) is plotted as a measure of the uncertainty being involved in the Θ_∞ assumption in close proximity of the body. The uncertainty is significantly higher for larger Re_c values, with marginal effect however on the respective Φ^* evolution. The implication is that the major bulk of Θ in the near wake region is possibly associated with isentropic energy exchanges and does not significantly affect the magnitude of the baroclinic constituent, so that the Φ^* approximation generally holds.

C. Accounting for Baroclinic Power Losses within the PER Factor

A slightly modified definition of the original PER metric of Eq. (14) may be introduced to account for compressibility-induced losses, observed within the volumetric pressure work Θ . These irreversibilities, referred to herein as baroclinic losses Θ_{irr} , manifest primarily within the boundary layers experiencing pressure gradients. It is difficult to directly separate out Θ_{irr} from Θ at local points in the flow. However, a good approximation is obtained from a positive volumetric pressure power residual, Θ_{∞} for a fully expanded flow, which offsets the apparent compressibility-induced reduction in the (normalised) dissipation evolution. Thus, the new potential for energy recovery ($\mathcal{PER}$) sifts out the Θ_{∞} contribution and the corresponding Mach number dependency from the energy recovery process, yielding the following expression:

$$\mathcal{PER} = 1 - \frac{\Phi + \Theta_{\text{irr}}}{DV_{\infty}} \approx 1 - \frac{\Phi + \Theta_{\infty}}{DV_{\infty}} = \text{PER} - \frac{\Theta_{\infty}}{DV_{\infty}} \quad (28)$$

where the expression is only valid for S_{TP} positioned behind the trailing edge. The last right-hand side equality of Eq. (28) indicates that the value of $\mathcal{PER}$ is offset, relative to the original definition, by an amount equal to the residual compressibility power imparted to the flow field. For quasi zero-pressure gradient flows, $\Theta \approx 0$, so that the Eq. (28) reduces to Eq. (14). A more rigorous derivation of $\mathcal{PER}$ and discussion on Θ_{∞} implications for subsonic and transonic adiabatic and heated axisymmetric flows is provided by Lamprakis et al. [48] based on the concept of baroclinic power by Sato [44]. For typical aerodynamic flows, $\Theta_{\infty} > 0$, so that $\mathcal{PER} < \text{PER}$ with significant contributions for transonic flows with shock formation and non-negligible heat transfer effects (wall heating) [48].

Fig. 18 shows plots of the trailing edge values of PER_{TE} and $\mathcal{PER}_{\text{TE}}$ curves of the unpowered fuselage as a function of Re_c and for various Mach numbers. The flat plate PER_{TE} predictions (red plots) are also included for comparison along with two distinct points that correspond to cruise conditions. Eq. (28) leads to the collapse of all the plots onto the incompressible $M_{\infty} = 0.2$ curve, signifying the compressibility invariance of the losses, rendering the Re_c as the only relevant flow parameter. The $\mathcal{PER}$ curves show excellent agreement with the corresponding flat plate PER data across the entire Re_c range, confirming that $\approx 9\%$ of the fuselage drag power is available for recovery at cruise conditions. The accuracy of the flat plate predictions is however circumstantial and primarily controlled by the fineness ratio of the fuselage [48], with more streamlined aerodynamic bodies retaining over their surfaces quasi zero-pressure gradient flow dissipative characteristics.

D. A BLI Powered Fuselage

When dealing with close aero-coupling between propulsor and airframe, it is often tempting to try and explain benefits (or seek additional benefits) via near-field forces and C_p distributions, with relevant examples (among others) being Refs. [9, 70, 71]. In these examples, the attempt to use C_p distributions to explain separated near-field propulsor, nacelle, and airframe forces, requires extreme caution and can be misleading. A prime example is from Gray et al. [9]; "To

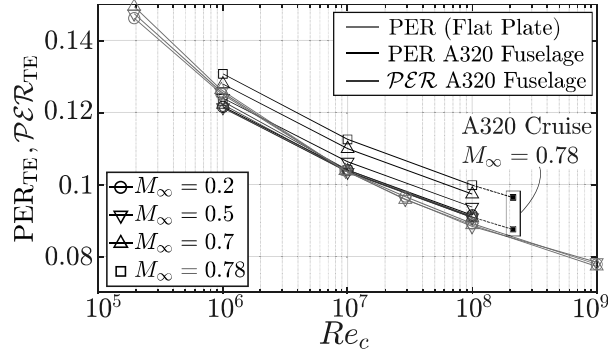


Fig. 18 PER_{TE} and PER_{TE} trends versus Re_c and M_∞ for an axisymmetric A320 fuselage representation, compared against the turbulent flat plate PER_{TE} trends of Fig. 7.

better understand the aerodynamic cause for the change in force coefficient for forces generated by the propulsor, we examined the C_p distribution on the surface of the aft fuselage." Focusing on near-field forces and C_p distributions can lead to potentially misguided design decisions, as demonstrated by Seitz et al. [70] where "the length of the boat tail was increased, to increase the positive axial force on the body due to the higher static pressure in the exhaust of the fuselage fan". Additionally from Blumenthal et al. [8] "The fuselage has an aft-facing surface in this region, so it is possible that an increase in pressure coefficient could provide more thrust". Subsequently, the purpose of this section is threefold. Firstly, to illustrate how seemingly beneficial C_p distributions along the fuselage, induced by the BLI propulsor, can be misleading. Secondly, to demonstrate how the energy-based approach can be used to better explain aero-coupling implications, thereby avoiding misconceptions around leveraging seemingly "beneficial" C_p distributions on the fuselage. Finally, to test and verify the hypothesis that the greatest amount of available energy, described by Eq. (28) and depicted in Fig. 18, can be propulsively harvested at the fuselage's trailing edge.

1. Test Case Description

To demonstrate misconceptions surrounding additional benefits from seemingly favourable C_p distributions, induced on the fuselage surface by the propulsor, various propulsor positions were tested along the tail cone surface, as shown in Fig. 19. The radius of the propulsor was adjusted at each position such that the annulus area of the propulsor was kept constant, in lack of more rigorous means of establishing a "fair" comparison among the various positions. The reference area has been selected to coincide with the cross-sectional area of the boundary layer flow at the trailing edge, such that the T.E. propulsor ingests the entire viscous fluid flow. For all the aft cone positions, the propulsor is modelled as an actuator disk (see details in Appendix C), while a wake attenuator is additionally considered at the trailing edge. Finally, a radial body force component is incorporated for the $x/c = 0.79, 0.86, 0.93$ positions, as described in Appendix A.B such that the ingested flow is accelerated parallel to the local wall curvature thereby mitigating additional separation losses arising from purely axial acceleration.

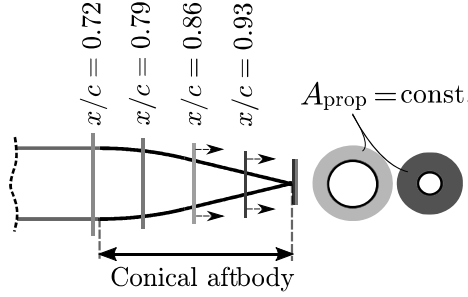


Fig. 19 Different AD locations tested along the fuselage tail-cone. AD area kept constant and equal to the cross-sectional area of the BL at the trailing edge of the unpowered fuselage.

Table 1 Integrated pressure (ϵ_p), skin friction (ϵ_v) and total (ϵ_{tot}) fuselage force changes with respect to the unpowered fuselage drag.

Prop. Position (x/c)	ϵ_p (%)	ϵ_f (%)	ϵ_{tot} (%)
0.72	-67.4	3.0	-2.3
0.79	-121.7	2.5	-6.8
0.86	58.3	1.1	5.4
0.93	154.0	0.4	12
1 (Uniform)	118.7	0.1	9.0
1 (Distributed)	161.1	0.2	12.3

2. *Seemingly favourable near-field fuselage forces*

The effect of the BLI propulsor position on the fuselage pressure coefficient (C_p) and skin friction coefficient (C_f) distributions is shown in Fig. 20. Fig. 20 a) shows that positioning the propulsor in-front of the T.E., raises the net average C_p (versus the unpowered case) over the aft facing tail-cone, which is seemingly advantageous in terms of increasing the forward contribution of fuselage near-field forces. Conversely, the suction effect of positioning the propulsor at the T.E., substantially lowers the C_p over the tail-cone, suggesting a detriment in terms of fuselage near-field forces. From Fig. 20 b), it is observed that acceleration of the flow over the tail-cone serves to increase C_f for all scenarios, thereby penalising the near-field forces experienced by the fuselage. The trade-offs between contributions of integrated pressure and friction forces to the total fuselage force, are tabulated in Table 1 in terms of their respective deviations ϵ_p , ϵ_v and ϵ_{tot} from the unpowered fuselage values. In Table 1, where the propulsor is positioned at $x/c = 0.72$ and $x/c = 0.79$, the seemingly beneficial effect of increased average C_p outweighs the penalty of increased C_f , implying a fuselage "thrust" (-ve value) benefit of 2.3% and 6.8%, respectively. Conversely, the T.E. propulsor positions seemed to increase fuselage "drag" by 9% and 12% for the uniform and wake attenuating body force distributions, respectively. However, the following energy analysis will demonstrate that this is misleading, and that the propulsors positioned at $x/c = 0.72$ and $x/c = 0.79$ were in fact the poorest performers, whereas the trailing edge propulsors were among the best performers.

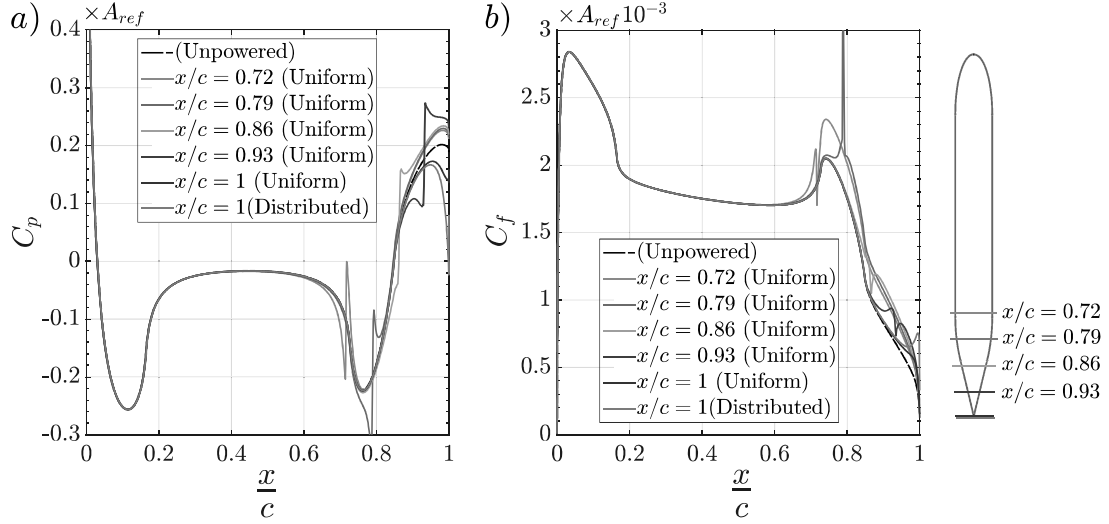


Fig. 20 BLI propulsor position effect on the C_p and C_f evolutions along the fuselage.

3. Energy Decomposition of a BLI Powered Fuselage

The normalised evolution of the power outflow and volumetric loss constituents is summarized in Fig. 21 for the various propulsor positions. Relative to the unpowered fuselage, the presence of the BLI propulsor yields a rapid decay of the mechanical energy constituents $\dot{E}_k$ and $\dot{E}_p$ as shown in Fig. 21 a) while the overlap of the various curves and the $\dot{E}_p$ trend invariance signifies that the energy recovery process is nearly independent of the propulsor position and primarily governed by the excess kinetic energy reduction. The decelerated flow regions (wrt the ARF) forming upstream (due to suction) and downstream of the propulsor manifest locally as an increase in the local rate of change $d\Phi/dx$ which is determined by the propulsor position, as shown in Fig. 21 b). The reason is that for the farther upstream propulsor positions, the boundary layer jet flow is scrubbing on progressively larger wetted airframe surfaces. The airframe dissipation rise results in the upward offset of the wake dissipation curve, even for a seemingly equal wake reduction efficiency, or equivalently same $d\Phi_{wake}/dx$ among the various propulsor positions in Fig. 21 c). The distributed forces achieve, similar to the flat plate studies, a nearly instantaneous wake attenuation with a subsequent $\approx 9\%$ elimination reduction benefit. The Θ evolution rapidly asymptotes to its baroclinic power residual within 0.4 chords, measuring non-negligible losses of up to $\approx 3.2\%$ of the drag power depending on the propulsor position. Relative to the unpowered configuration, the minimum baroclinic penalty is attained for a fully attenuated wake, while the scattering of Θ_∞ with varying propulsor position indicates the possibility of aerodynamic propulsor-airframe synergies for the minimisation of the compressibility loss penalty. For fixed net force conditions, the propulsive power requirement of the propulsor is determined by its volumetric losses Φ^* imparted to the flow as shown in Fig. 21 d). For the two most upstream positions $x/c = 0.72, 0.79$, the normalised mechanical losses exceed the unity threshold, indicating that the airframe-propulsor assembly imparts additional losses, relative to the unpowered configuration. The reason is that the affiliated airframe

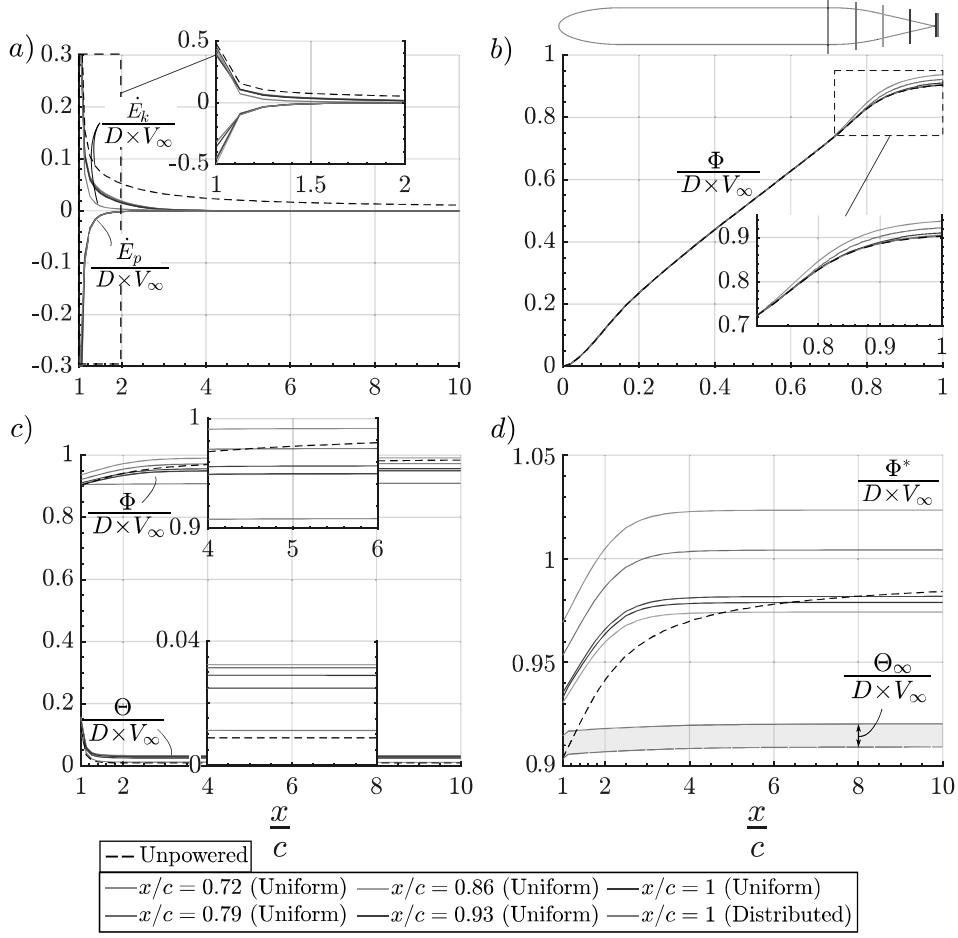


Fig. 21 Evolution of energy constituents for the powered BLI fuselage corresponding to different propulsor positions.

dissipation penalty completely outweighs the wake reduction benefit. The minimum loss is therefore achieved for a fully attenuated wake with the remaining BLI positions attaining similar wake reduction margins among them. This analysis clearly shows that seemingly favourable C_p distributions and near-field forces on the fuselage (see Fig. 20 b) and Table 1) distributions can be quite misleading and should not drive design decisions. Instead, the energy-based approach provides a more reliable way examining and inferring how propulsor-airframe interactions may be favourable or detrimental.

4. $\mathcal{PSC}_{min}$ of a BLI Powered Fuselage

The $\mathcal{PSC}_{min}$ definition of Eq. (26) is adopted to compare the aerodynamic performance of the various airframe-propulsor assemblies at cruise conditions. The results are summarized in Fig. 22 and compared against the $\mathcal{PER}$ values, where it is hypothesised that the $\mathcal{PER}$ definition Eq. (28) effectively represents a hypothetical maximum for $\mathcal{PSC}_{min}$ ^{¶¶}.

^{¶¶}It is noted that both $\mathcal{PER}$ and $\mathcal{PSC}$ account for baroclinic losses, unlike Baskaran et al.'s [18] $\mathcal{PSC}$ definition, and therefore gives a more accurate and better account of the losses associated with pressure gradients and compressibility effects.

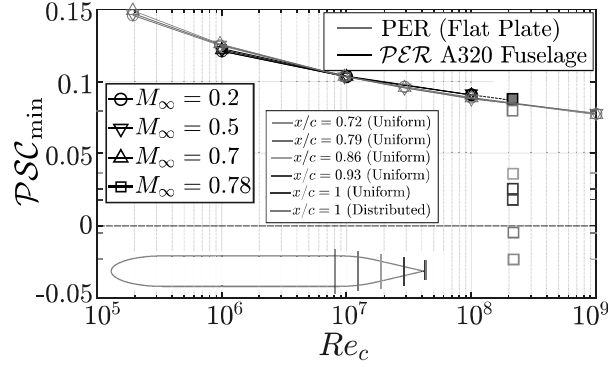


Fig. 22 $\mathcal{PSC}_{\min}$ of the BLI propulsor-fuselage assembly at cruise conditions and for various propulsor positions.

From Eq. (26) it can be seen that $\mathcal{PSC}_{\min}$ is purely a function of the total mechanical losses imparted to the flow. The implication is that seemingly favourable force re-distributions on the airframe, due to the BLI integration, can lead to erroneous design conclusions. An example is the two most upstream positioned propulsors ($x/c = 0.72, 0.79$) in this study, which yield the highest power consumption and lowest $\mathcal{PSC} < 0$ values despite the apparent fuselage force reduction benefit of -2.3% and -6.8% (see Table 1), respectively. With reference to Fig. 21 c) and d), these two propulsor positions are associated with the highest airframe dissipation and total baroclinic loss penalty which outweigh the wake dissipation reduction such that: $\Delta\Phi_{\infty}^* < 0$. On the contrary, the trailing edge wake attenuator propulsor attains a $\mathcal{PSC} \approx 8\%$ value, despite the force penalty of 12.3% , approaching the hypothetical $\mathcal{PER}$ limit with their relative discrepancy being attributed to the propulsor's baroclinic loss penalty as per Fig. 21 c). The remaining propulsor positions achieve power savings of $\sim 2 - 4\%$ with the mid cone positioned ($x/c = 0.86$) propulsor slightly outperforming the corresponding trailing edge (with uniform forces), primarily due to the lower baroclinic losses of the former within the propulsive domain.

VI. Conclusions

This paper introduces a different approach to work-energy relationships of flight by focusing on the ARF, where the aircraft is perceived to be moving through an initially quiescent atmosphere, instead of the (more typical) other way around. The novelty that enabled this was the derivation and implementation of generalised integral forms of governing laws applicable to moving control volumes. This process revealed that nearly all of the current, state-of-the-art, far-field decomposition methods, actually end up with mathematically equivalent ARF forms through their substitutions aimed at limiting the formulations to Trefftz plane integrations only. Whereas the work presented in this paper's aims to change how aerodynamicists visualise the mathematical decomposition terms, by explicitly contextualising their origins directly in terms of the ARF. In particular, one of the paper's contributions was in identifying and highlighting the significance of Galilean covariance and reference frame perspectives in emerging energy-based control-volume methods that are gaining popularity due to their potential in dealing more effectively with highly-integrated airframe-propulsion systems.

As a demonstration, this paper has focused on applying the above to the concept of boundary layer ingestion (BLI). A clear hypothesis was formulated around propulsively harvesting boundary layer energy, which was systematically tested via canonical test cases covering comprehensive ranges of Reynolds and Mach Numbers.

An unpowered flat plate was first modelled in RANS CFD for both laminar and turbulent flows for a wide range of Reynolds and Mach numbers, to interrogate the work done on the flow by the flat plate and the subsequent transformations describing the decay of available energy. It was shown that the available kinetic energy manifestation of the work done on the flow by the flat plate, and quantified by the Potential for Energy Recovery (PER) metric, decreases with increasing Reynolds number because of the more thinner, more pronounced, and locally dissipative BL profile. In the case of laminar flow, $PER > 21\%$ and was highest at the trailing edge. For turbulent flows, substantial local dissipation within the BL reduced the available kinetic energy and the subsequent potential benefits down to $8 - 12\%$, which also decreased with Reynolds number.

An actuator disk (AD), capable of varying levels of wake attenuation, was implemented with the RANS CFD models to investigate whether the energy quantified by PER could indeed be recuperated. A uniformly and distributively loaded AD were both shown to extract available kinetic energy and suppress wake dissipation whilst achieving dynamic equilibrium. The laminar flow suffered from additional BL dissipation losses due to the propulsors suction effect, as well as compressibility losses at higher free-stream Mach numbers, both of which were exacerbated by the distributively loaded AD attempting to attenuate the wake. As a result, low Reynolds number laminar flows consumed more power in comparison to an ideal free-stream ingesting propulsor. The turbulent flow BL's dissipation was found to be insensitive to the suction effect, and the wake attenuating AD achieved power savings that approached the PER maximum hypothesis.

Finally, the developed framework was tested against a more representative axisymmetric A320 fuselage approximation and confirmed the accuracy and validity of the flat plate analysis. The powered fuselage studies featured a BLI propulsor of fixed surface area that was allowed to vary along the aft body in order to examine further potential aero-propulsive synergies and to shed light on misconceptions associated with force and C_p distribution. The results indicated additional baroclinic losses which were subsequently incorporated into a modified PER expression. The powered fuselage studies demonstrated that maximum power saving, approaching PER , could be achieved via a wake attenuating actuator disk at the fuselage trailing edge. Conversely, it was found that locating the propulsor at different position along the tailcone, generated losses that detracted from or, in some cases, mitigated any benefits.

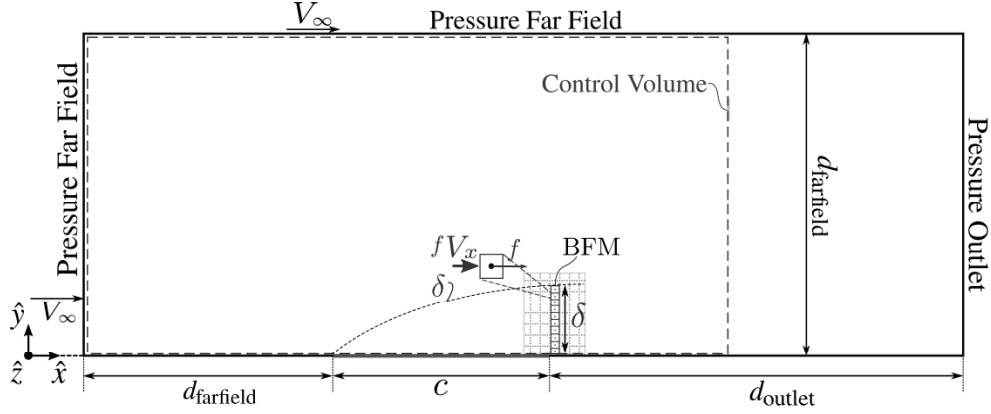


Fig. A1 CFD domain and control volume definitions of the flat plate studies, showing BFM AD representation for powered cases.

A. CFD Setup

A. Flat Plate

The 2D rectangular CFD domain of the unpowered flat plate layout is summarized in Fig. A1. The CFD domain is meshed as a structured grid featuring a wall-normal growth ratio of 1.05 to ensure high numerical resolution of the boundary layer and the wake region. For laminar flow, the near-wall grid density has been fine-tuned via comparison against the Blasius solution while the grids of turbulent computations have been further treated to ensure the conformity of the mesh with the $y^+ < 1$ requirement of the two-equation eddy viscosity $\kappa - \omega$ SST turbulence model. The numerical solutions are obtained using the commercial CFD package ANSYS FLUENT coupled with a second-order Green-Gauss Cell-based gradient scheme. The fluid is modelled as an ideal gas with constant thermal conductivity and a piecewise polynomial variation of the specific heat capacity.

A grid dependence study was conducted alongside a domain size study for the unpowered flat plate, following the procedure described by Celik et al. [72]. For this, the size of the domain was varied to investigate the effect of the boundary conditions on the solution and three different mesh refinement levels were assessed for the selected domain size. Additionally, the turbulence model settings were verified against NASA's flat plate CFD data [73]. Details of the grid dependence study and turbulence model verification can be found in Ref. [74] and are omitted here for brevity.

For powered cases, the propulsor is located at the trailing edge of the flat plate and is modelled as a single column cell zone with specified momentum and energy source terms, as described in Appendix C, to replicate an actuator line with uniform or momentum-filling body force distributions.

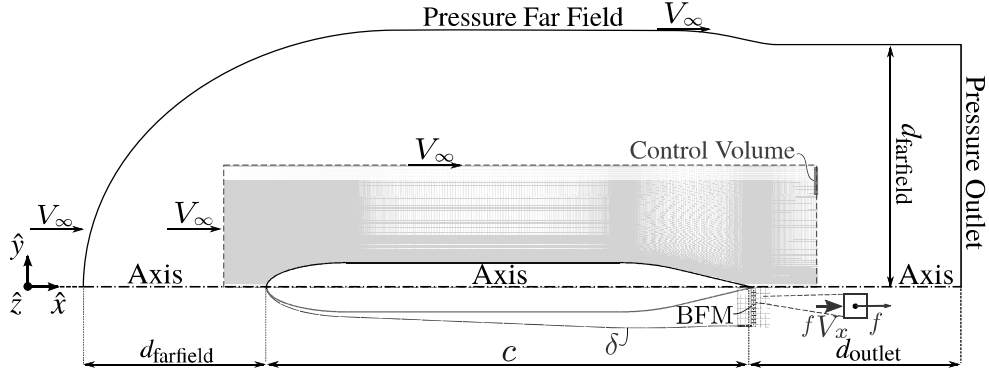


Fig. A2 CFD domain and control volume of the A320 fuselage studies showing the AD BFM representation for powered cases.

B. Axisymmetric Fuselage

The 2D CFD axisymmetric domain for the unpowered and powered fuselage numerical studies is shown in Fig. A2 and follows a similar domain parametrisation, mesh refinement strategy and turbulent modelling setup. The upstream and vertical extent of the pressure far field surfaces are $d_{\text{farfield}} = 30 \times c$, and the downstream location of the pressure outlet boundary are $d_{\text{farfield}} = 80 \times c$.

The numerical setup, including the domain size and mesh refinement strategies for both unpowered and BLI powered fuselages, were adapted from the authors' previous grid dependence studies reported in Refs. [51] and [75] and are omitted for brevity.

For the powered cases, additional radial momentum and energy source terms are included when the propulsor is moved along the curved surfaces of the fuselage aft body to ensure that the source terms are acting in a direction parallel to the ingested flow, as shown in Fig. A3. The direction of the volume specific vector $\mathbf{f}$ is specified to be equal to the local curvature's inclination angle α and its radial f_x component is determined by the net force requirement:

$$f_y = f_x \tan \alpha \quad (\text{A1})$$

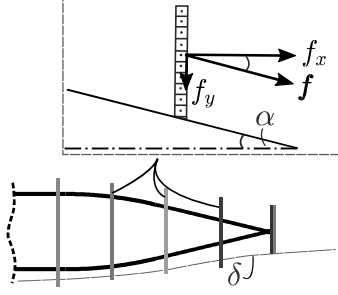


Fig. A3 Radial Body forces applied to three propulsor positions to accelerate the flow parallel to the wall.

B. Laminar and Turbulent Dissipation Expressions

For the two equation $k - \omega$ SST turbulent model used herein, the inclusion of the Reynolds stresses via the Boussinesq's approximation yields the following expression for the shear stress tensor [51].

$$\bar{\tau} = 2(\mu + \mu_t) \left[\bar{S} - \frac{1}{3}(\nabla \cdot \mathbf{V})\bar{I} \right] - \frac{2}{3}\rho k \bar{I} \quad (\text{A2})$$

where $\bar{I}$ the identity matrix of equivalent tensor order. Inserting now Eq. (A2) into the dissipation integrand $(\nabla \cdot \bar{\tau}) \cdot \mathbf{V}$, the following dissipation form is obtained:

$$\Phi = \Phi_{lam} + \Phi_{turb} = 2(\mu + \mu_t) \left[\bar{S} : \bar{S} - \frac{1}{3}(\nabla \cdot \mathbf{V})^2 \right] - \frac{2}{3}\rho k (\nabla \cdot \mathbf{V}) \quad (\text{A3})$$

where μ the laminar viscosity, μ_t the eddy viscosity, k the turbulent kinetic energy per unit mass and $\bar{S}$ the rate-of-strain-tensors expressed as [76]:

$$\bar{S} = \frac{1}{2}[\nabla \mathbf{V} + (\nabla \mathbf{V})^T] \quad (\text{A4})$$

where the superscript T denotes the transpose. The laminar and turbulent dissipation constituents are obtained from Eq. (A3) by isolating the laminar and eddy viscosity contributions, respectively:

$$\Phi_{lam} = 2\mu \left[\bar{S} : \bar{S} - \frac{1}{3}(\nabla \cdot \mathbf{V})^2 \right] \quad (\text{A5})$$

$$\Phi_{turb} = 2\mu_t \left[\bar{S} : \bar{S} - \frac{1}{3}(\nabla \cdot \mathbf{V})^2 \right] - \frac{2}{3}\rho k (\nabla \cdot \mathbf{V}) \quad (\text{A6})$$

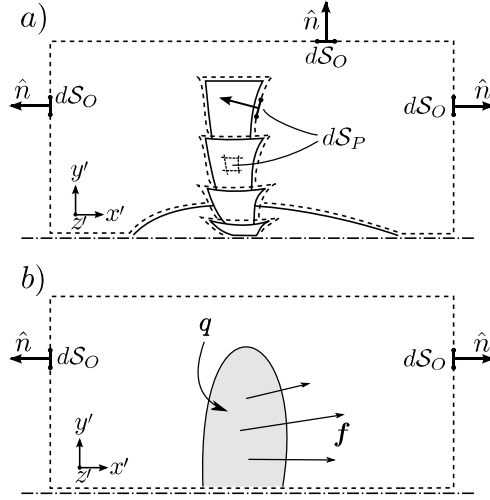


Fig. C4 Control volumes for different propulsion system representations: a) Real blade geometry b) Equivalent source term model.

C. Propulsor Representation and Modelling

A. Forces and Shaft Power

An actual propulsor, Fig. C4 a), can be represented numerically using a body force model, Fig. C4 b). Noting that Fig. C4 is depicted in the RRF, Eq. (3) formulation may be applied to the control volumes of Fig. C4 a) and b) to obtain the forces:

$$\underbrace{\iint_{\mathcal{CS}(t)} (p_g \hat{n} - \bar{\bar{\tau}} \cdot \hat{n}) dS_P}_{\text{Real: } \mathbf{F}_{S_P}} = - \underbrace{\iiint_{\mathcal{CV}(t)} \rho \mathbf{f} d\mathcal{V}}_{\text{BFM: } \mathbf{F}_f} = - \frac{d}{dt} \iiint_{\mathcal{CV}(t)} \rho \mathbf{V} d\mathcal{V} - \iint_{\mathcal{CS}(t)} (\rho \mathbf{V} \mathbf{V}' \cdot \hat{n} + p_g \hat{n} - \bar{\bar{\tau}} \cdot \hat{n}) dS_O \quad (\text{A7})$$

where the unsteady term on the right of the equality may be assumed ignored in quasi-unsteady (or periodically unsteady) flow. Similarly, the total energy balance of Eq. (5) can be applied to obtain the power of the two representations:

$$\underbrace{- \iint_{\mathcal{CS}(t)} (p_g \mathbf{V}'_{CS} - \bar{\bar{\tau}} \cdot \mathbf{V}'_{CS}) \cdot \mathbf{dS}_P}_{\text{Real: } \mathcal{P}_S} = \underbrace{\iiint_{\mathcal{CV}(t)} \rho (\dot{q} + \mathbf{f} \cdot \mathbf{V}') d\mathcal{V}}_{\text{BFM: } \mathcal{P}_f} = \dots \quad (\text{A8})$$

$$\frac{d}{dt} \iiint_{\mathcal{CV}(t)} \rho \left(e + \frac{\mathbf{V}'^2}{2} \right) d\mathcal{V} + \iint_{\mathcal{CS}(t)} \left[\left(e + \frac{\mathbf{V}'^2}{2} \right) \rho \mathbf{V}' + p \mathbf{V}' - \bar{\bar{\tau}} \cdot \mathbf{V}' + \mathbf{q} \right] \cdot \mathbf{dS}_O$$

where the near-field power for the real propulsor is obtained because of the relative motion of the control volume surfaces draping its geometry, thereby further emphasising the requirement of the moving control volume formulation. If adiabatic and quasi-steady (periodically unsteady) is assumed, then the far right equality of Eq. (A8) is simply the

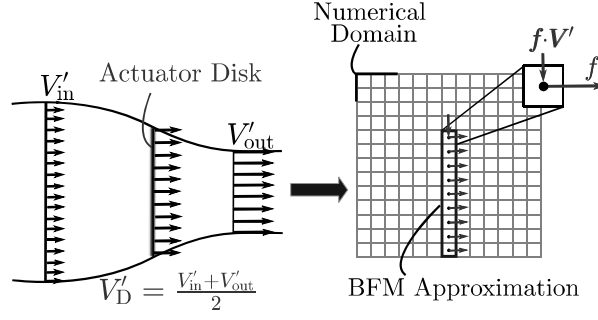


Fig. C5 BFM AD line representation modelled numerically as a single column of cells with volumetric momentum and energy sources.

change in total enthalpy across the control.

B. Actuator Disk Approximation

The BFM is used to create an actuator disk propulsor in the numerical simulations, as depicted in Fig. C5, which has been verified against data from van Kuik's [77] models for quasi-incompressible 2D axisymmetric flow at $M_\infty = 0.2$ and for a thrust coefficient of $C_T = 16/9$, defined as:

$$C_T = \frac{\mathcal{T}}{\frac{1}{2}\rho_\infty A_D V_\infty^2} \quad (\text{A9})$$

where $\mathcal{T}$ the thrust of the BFM propulsor as per Eq. (A7), A_D the actuator disk surface area and $|\Delta p_D|$ the static pressure jump across the disk. Numerical solutions have been obtained for 2D axisymmetric inviscid and viscous turbulent** flows to test the consistency of the model. Figure C6 shows the comparison of the inviscid and viscous flow AD BFM predictions of the normalised axial (V'_x), radial (V'_r), total ($|V'|$) velocity components along with the streamwise pressure axial pressure distribution against numerical data by van Kuik [77]. The flow field changes are nearly independent of the flow type (inviscid, viscous) and show excellent agreement of $< 2\%$ for all scenarios.

C. Actuator Disk for Wake Attenuation

Introducing the thickness of the BFM propulsor, σ , the magnitude of the propulsive force, F_f and the distributed body forces, f_{dist} may be related to the stream-wise momentum deficit, as described in Eq. (A10).

$$F_f = \iiint f_{\text{dist}} dV = \int_0^\delta \sigma f_{\text{dist}} dS = \int_0^\delta \underbrace{\rho \frac{V'(V' - V_\infty)}{\sigma}}_{f_{\text{dist}}} dS \quad (\text{A10})$$

Equation (A10) uniquely relates the body force distribution across the propulsor to the local momentum deficit of the boundary layer as illustrated in Fig. C7, allowing for an approximation that numerically replicates the ideal wake

**The $\kappa - \omega$ SST model has been used for turbulent computations.

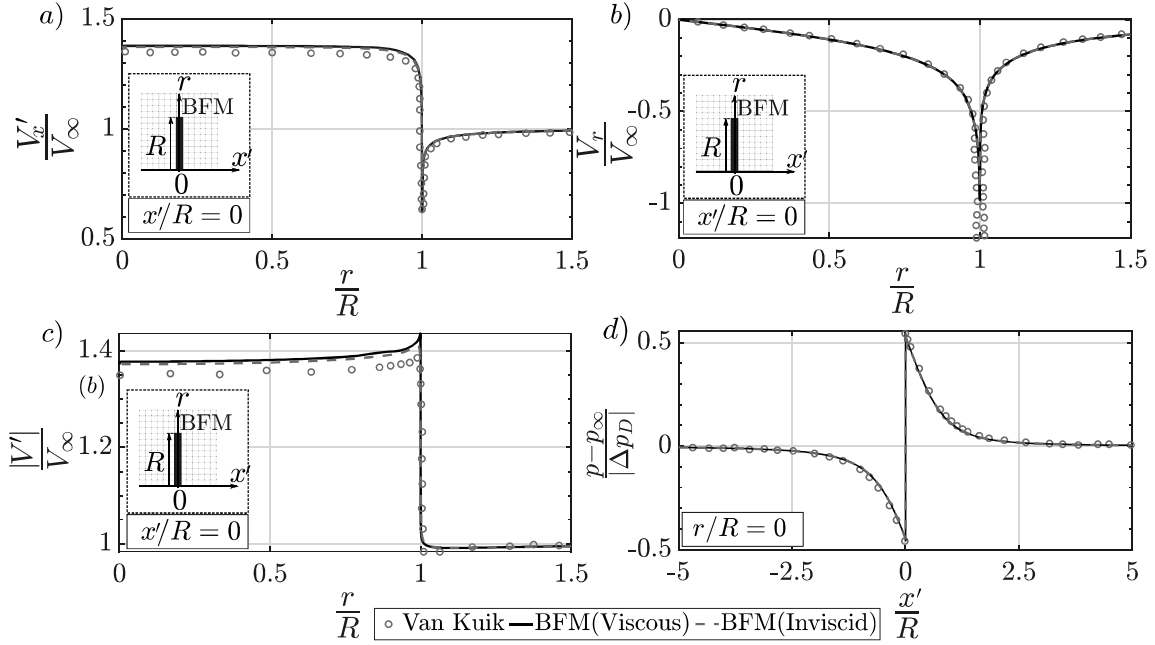


Fig. C6 Inviscid and viscous BFM AD representation for free-stream ingestion compared against Van Kuik [77] for a thrust coefficient of $C_T = 16/9$.

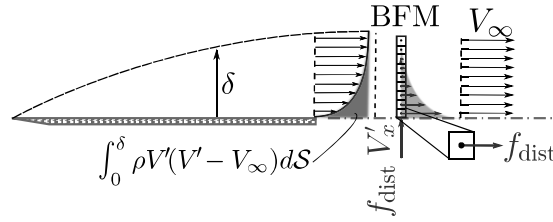


Fig. C7 Schematic depicting a wake filling AD achieved using the BFM in CFD.

attenuation scenario.

Acknowledgments

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CRediT Author Contribution Statement

Ioannis Lamprakis: Conceptualisation, Methodology, Validation, Formal Analysis, Data Curation, Writing - Original Draft, Writing - Review & Editing, Visualisation. **Drewan S. Sanders:** Conceptualisation, Methodology, Validation, Formal Analysis, Data Curation, Writing - Original Draft, Writing - Review & Editing, Visualisation, Supervision. **Panagiotis Laskaridis:** Conceptualisation, Writing - Review & Editing, Supervision, Project Administration, Funding Acquisition.

References

- [1] Moirou, N. G., Sanders, D. S., and Laskaridis, P., “Advancements and prospects of boundary layer ingestion propulsion concepts,” *Progress in Aerospace Sciences*, Vol. 138, 2023, p. 100897. <https://doi.org/https://doi.org/10.1016/j.paerosci.2023.100897>.
- [2] Felder, J., Kim, H., and Brown, G., “Turboelectric Distributed Propulsion Engine Cycle Analysis for Hybrid-Wing-Body Aircraft,” AIAA Paper 2009-1132, January 2009. <https://doi.org/10.2514/6.2009-1132>.
- [3] Arntz, A., and Atinault, O., “Exergy-Based Performance Assessment of a Blended Wing–Body with Boundary-Layer Ingestion,” *AIAA Journal*, Vol. 53, No. 12, 2015, pp. 3766–3776. <https://doi.org/10.2514/1.J054072>.
- [4] Ochs, S. S., Tillman, G., Joo, J., and Voytovych, D. M., “Computational Fluid Dynamics-Based Analysis of Boundary Layer Ingesting Propulsion,” *Journal of Propulsion and Power*, Vol. 33, No. 2, 2017, pp. 522–530. <https://doi.org/10.2514/1.B36069>.
- [5] Hall, D. K., Huang, A. C., Uranga, A., Greitzer, E. M., Drela, M., and Sato, S., “Boundary Layer Ingestion Propulsion Benefit for Transport Aircraft,” *Journal of Propulsion and Power*, Vol. 33, No. 5, 2017, pp. 1118–1129. <https://doi.org/10.2514/1.B36321>.
- [6] Uranga, A., Drela, M., Greitzer, E. M., Hall, D. K., Titchener, N. A., Lieu, M. K., Siu, N. M., Casses, C., Huang, A. C., Gatlin, G. M., and Hannon, J. A., “Boundary Layer Ingestion Benefit of the D8 Transport Aircraft,” *AIAA Journal*, Vol. 55, No. 11, 2017, pp. 3693–3708. <https://doi.org/10.2514/1.J055755>.
- [7] Uranga, A., Drela, M., Hall, D. K., and Greitzer, E. M., “Analysis of the Aerodynamic Benefit from Boundary Layer Ingestion for Transport Aircraft,” *AIAA Journal*, Vol. 56, No. 11, 2018, pp. 4271–4281. <https://doi.org/10.2514/1.J056781>.
- [8] Blumenthal, B. T., Elmilgoui, A. A., Geiselhart, K. A., Campbell, R. L., Maughmer, M. D., and Schmitz, S., “Computational Investigation of a Boundary-Layer-Ingestion Propulsion System,” *Journal of Aircraft*, Vol. 55, No. 3, 2018, pp. 1141–1153. <https://doi.org/10.2514/1.C034454>.
- [9] Gray, J. S., Mader, C. A., Kenway, G. K. W., and Martins, J. R. R. A., “Modeling Boundary Layer Ingestion Using a Coupled Aeropropulsive Analysis,” *Journal of Aircraft*, Vol. 55, No. 3, 2018, pp. 1191–1199. <https://doi.org/10.2514/1.C034601>.
- [10] Gray, J. S., Mader, C. A., Kenway, G. K. W., and Martins, J. R. R. A., “Coupled Aeropropulsive Optimization of a Three-Dimensional Boundary-Layer Ingestion Propulsor Considering Inlet Distortion,” *Journal of Aircraft*, Vol. 57, No. 6, 2020, pp. 1014–1025. <https://doi.org/10.2514/1.C035845>.

- [11] Yildirim, A., Gray, J. S., Mader, C. A., and Martins, J. R. R. A., “Boundary-Layer Ingestion Benefit for the STARC-ABL Concept,” *Journal of Aircraft*, Vol. 59, No. 4, 2022, pp. 896–911. <https://doi.org/10.2514/1.C036103>.
- [12] Secchi, M., Lacava, P. T., Trapp, L. G., and Gama Ribeiro, R. F., “Evaluation of a Regional Aircraft with Boundary Layer Ingestion and Electric-Fan Propulsor,” *Journal of Aircraft*, Vol. 58, No. 6, 2021, pp. 1204–1215. <https://doi.org/10.2514/1.C035932>.
- [13] Della Corte, B., van Sluis, M., Gangoli Rao, A., and Veldhuis, L. L. M., “Aerodynamic Performance of an Aircraft with Aft-Fuselage Boundary-Layer-Ingestion Propulsion,” *Journal of Aircraft*, Vol. 59, No. 4, 2022, pp. 1054–1070. <https://doi.org/10.2514/1.C036596>.
- [14] Lv, P., Rao, A. G., Ragni, D., and Veldhuis, L., “Performance Analysis of Wake and Boundary-Layer Ingestion for Aircraft Design,” *Journal of Aircraft*, Vol. 53, No. 5, 2016, pp. 1517–1526. <https://doi.org/10.2514/1.C033395>.
- [15] Mutangara, N. E., Smith, L., Craig, K. J., and Sanders, D. S., “Potential for Energy Recovery of Unpowered Configurations Using Power Balance Method Computations,” *Journal of Aircraft*, Vol. 58, No. 6, 2021, pp. 1364–1374. <https://doi.org/10.2514/1.C036172>.
- [16] Mutangara, N. E., Smith, L., Craig, K. J., and Sanders, D. S., “Potential for Energy Recovery from Boundary-Layer Ingesting Actuator Disk Propulsion,” *Journal of Aircraft*, Vol. 61, No. 2, 2024, pp. 611–624. <https://doi.org/10.2514/1.C037294>.
- [17] Della Corte, B., van Sluis, M., Veldhuis, L. L. M., and Gangoli Rao, A., “Power Balance Analysis Experiments on an Axisymmetric Fuselage with an Integrated Boundary-Layer-Ingesting Fan,” *AIAA Journal*, Vol. 59, No. 12, 2021, pp. 5211–5224. <https://doi.org/10.2514/1.J060570>.
- [18] Baskaran, P., Corte, B. D., van Sluis, M., and Rao, A. G., “Aeropropulsive Performance Analysis of Axisymmetric Fuselage Bodies for Boundary-Layer Ingestion Applications,” *AIAA Journal*, Vol. 60, No. 3, 2022, pp. 1592–1611. <https://doi.org/10.2514/1.J060362>.
- [19] Moirou, N., Mutangara, N., and Sanders, D., “Fundamental considerations in the design and performance assessment of propulsive fuselage aircraft concepts,” *The Aeronautical Journal*, 2024, p. 1–26. <https://doi.org/10.1017/aer.2024.124>.
- [20] Mutangara, N. E., Sanders, D. S., Laskaridis, P., Hart, P. L., and Schmitz, S., “A Unified Partial Pressure Field and Velocity Decomposition Approach toward Improved Energetic Aerodynamic Force Decompositions,” *57th 3AF International Conference on Applied Aerodynamics, Bordeaux, France*, 29-31 March, 2023.
- [21] Hart, P. L., Mutangara, N. E., Sanders, D. S., and Schmitz, S., “Thrust/Drag Decomposition using Partial Pressure Fields,” *57th 3AF International Conference on Applied Aerodynamics, Bordeaux, France*, 29-31 March, 2023.
- [22] Plas, A., Crichton, D., Sargeant, M., Hynes, T., Greitzer, E., Hall, C., and Madani, V., “Performance of a Boundary Layer Ingesting (BLI) Propulsion System,” *AIAA Paper 2007-450*, January 2007. <https://doi.org/10.2514/6.2007-450>.
- [23] Bijewitz, J., Seitz, A., Hornung, M., and Isikveren, A. T., “Progress in Optimizing the Propulsive Fuselage Aircraft Concept,” *Journal of Aircraft*, Vol. 54, No. 5, 2017, pp. 1979–1989. <https://doi.org/10.2514/1.C034002>.

- [24] Goldberg, C., Nalianda, D., Laskaridis, P., and Pilidis, P., “Installed Performance Assessment of an Array of Distributed Propulsors Ingesting Boundary Layer Flow,” *Journal of Engineering for Gas Turbines and Power*, Vol. 140, No. 7, 2018. <https://doi.org/10.1115/1.4038837>.
- [25] Valencia, E., Hidalgo, V., Nalianda, D., Laskaridis, P., and Singh, R., “Discretized Miller approach to assess effects on boundary layer ingestion induced distortion,” *Chinese Journal of Aeronautics*, Vol. 30, No. 1, 2017, pp. 235–248. <https://doi.org/10.1016/j.cja.2016.12.005>.
- [26] Valencia, E., Alulema, V., Rodriguez, D., Laskaridis, P., and Roumeliotis, I., “Novel fan configuration for distributed propulsion systems with boundary layer ingestion on an hybrid wing body airframe,” *Thermal Science and Engineering Progress*, Vol. 18, 2020, p. 100515. <https://doi.org/10.1016/j.tsep.2020.100515>.
- [27] Liu, C., Ihiabe, D., Laskaridis, P., and Singh, R., “A preliminary method to estimate impacts of inlet flow distortion on boundary layer ingesting propulsion system design point performance,” *Proceedings of the Institution of Mechanical Engineers, Part G: Journal of Aerospace Engineering*, Vol. 228, No. 9, 2014, pp. 1528–1539. <https://doi.org/10.1177/0954410013496750>.
- [28] Habermann, A. L., Bijewitz, J., Seitz, A., and Hornung, M., “Performance bookkeeping for aircraft configurations with fuselage wake-filling propulsion integration,” *CEAS Aeronautical Journal*, Vol. 11, 2020, p. 529–551. <https://doi.org/10.1007/s13272-019-00434-w>.
- [29] Habermann, A. L., Gokhale, A., and Hornung, M., “Numerical investigation of the effects of fuselage upsweep in a propulsive fuselage concept,” *CEAS Aeronautical Journal*, Vol. 12, 2021, pp. 173–189. <https://doi.org/10.1007/s13272-020-00487-2>.
- [30] Gray, J. S., Mader, C. A., Kenway, G. K., and Martins, J. R. R. A., “Approach to Modeling Boundary Layer Ingestion using a Fully Coupled Propulsion-RANS Model,” AIAA Paper 2017-1753, January 2017. <https://doi.org/10.2514/6.2017-1753>.
- [31] MIDAP Study Group, “Guide to In-Flight Thrust Measurement of Turbojets and Fan Engines,” Advisory Group for Aerospace Research and Development, AGARDograph No. 237, January 1979.
- [32] E-33 In Flight Propulsion Measurement Committee, “In-Flight Thrust Determination,” International Society of Automotive Engineers, AIR1730, December 2004.
- [33] Rolt, A. M., and Whurr, J. R., “Optimizing propulsive efficiency in aircraft with boundary layer ingesting distributed propulsion,” International Symposium on Air Breathing Engines, ISABE-2015-20201, October 2015.
- [34] Guha, A., “Optimum Fan Pressure Ratio for Bypass Engines with Separate or Mixed Exhaust Streams,” *Journal of Propulsion and Power*, Vol. 17, No. 5, 2001, pp. 1117–1122. <https://doi.org/10.2514/2.5852>.
- [35] Guha, A., Boylan, D., and Gallagher, P., “Determination of optimum specific thrust for civil aero gas turbine engines: a multidisciplinary design synthesis and optimisation,” *Proceedings of the Institution of Mechanical Engineers, Part G: Journal of Aerospace Engineering*, Vol. 227, No. 3, 2013, pp. 502–527. <https://doi.org/10.1177/0954410011435623>.

- [36] Epstein, A. H., “Aeropropulsion for commercial aviation in the twenty-first century and research directions needed,” *AIAA journal*, Vol. 52, No. 5, 2014, pp. 901–911. <https://doi.org/https://doi.org/10.2514/1.J052713>.
- [37] Küchemann, D., and Weber, J., “Installation of Jet Engines,” *Aerodynamics of Propulsion*, McGraw-Hill, London, England, U.K., 1953, 1st ed., pp. 185–209.
- [38] Betz, A., “Interference between Propeller and Vehicle; The Ducted Propeller,” *Introduction to the Theory of Flow Machines*, Pergamon Press, London, England, U.K., 1966, 1st ed., pp. 215–220.
- [39] Thurston, S., and Evanbar, M. S., “Efficiency of a propulsor on a body of revolution-inducting boundary-layer fluid,” *Journal of Aircraft*, Vol. 3, No. 3, 1966, pp. 270–277. <https://doi.org/10.2514/3.43737>.
- [40] Davidson, I. M., “Some Notes on Aircraft Propulsion by Wake Regeneration,” *Annals of the New York Academy of Sciences*, Vol. 154, No. 2, 1968, pp. 641–651. <https://doi.org/https://doi.org/10.1111/j.1749-6632.1968.tb15219.x>.
- [41] Bevilaqua, P., and Yam, C., “Propulsive Efficiency of Wake Ingestion,” *Journal of Propulsion and Power*, Vol. 36, No. 4, 2020, pp. 517–526. <https://doi.org/10.2514/1.B37695>.
- [42] Smith Jr., L. H., “Wake ingestion propulsion benefit,” *Journal of Propulsion and Power*, Vol. 9, No. 1, 1993, pp. 74–82. <https://doi.org/10.2514/3.11487>.
- [43] Drela, M., “Power Balance in Aerodynamic Flows,” *AIAA Journal*, Vol. 47, No. 7, 2009, pp. 1761–1771. <https://doi.org/10.2514/1.42409>.
- [44] Sato, S., “The power balance method for aerodynamic performance assessment,” Ph.D. thesis, Massachusetts Institute of Technology, 2012.
- [45] Arntz, A., Atinault, O., and Merlen, A., “Exergy-Based Formulation for Aircraft Aeropropulsive Performance Assessment: Theoretical Development,” *AIAA Journal*, Vol. 53, No. 6, 2015, pp. 1627–1639. <https://doi.org/10.2514/1.J053467>.
- [46] Lamprakis, I., “Energy-Based Thermo-Aero-Propulsive Flight Vehicle Performance Assessment,” *Energy-Based Aerodynamic Principles of Adiabatic and Heated Boundary Layer Ingestion*, Ph.D. Thesis, Cranfield University, 2023, Chap. 2, pp. 20–93.
- [47] Lamprakis, I., “Defect Force and Viscous Mechanical Energy Generation in Aerodynamic Flows,” *Energy-Based Aerodynamic Principles of Adiabatic and Heated Boundary Layer Ingestion*, Ph.D. Thesis, Cranfield University, 2023, Chap. 3, pp. 94–171.
- [48] Lamprakis, I., Sanders, D. S., and Laskaridis, P., “Energy-Based Aerodynamic Loss and Recovery Characteristics of Adiabatic and Heated Fuselages,” *Journal of Aircraft*, Vol. 0, No. 0, 0, pp. 1–19. <https://doi.org/10.2514/1.C037246>.
- [49] Lamprakis, I., “Energy-Based Aerodynamic Principles of Adiabatic and Heated Boundary Layer Ingestion,” *Energy-Based Aerodynamic Principles of Adiabatic and Heated Boundary Layer Ingestion*, Ph.D. Thesis, Cranfield University, 2023, Chap. 5, pp. 235–351.

- [50] Renard, N., and Deck, S., “A theoretical decomposition of mean skin friction generation into physical phenomena across the boundary layer,” *Journal of Fluid Mechanics*, Vol. 790, 2016, p. 339–367. <https://doi.org/10.1017/jfm.2016.12>.
- [51] Sanders, D. S., and Laskaridis, P., “Full-Aircraft Energy-Based Force Decomposition Applied to Boundary-Layer Ingestion,” *AIAA Journal*, Vol. 58, No. 10, 2020, pp. 4357–4373. <https://doi.org/10.2514/1.J058695>.
- [52] Mallinckrodt, A. J., and Leff, H. S., “All about work,” *American Journal of Physics*, Vol. 60, No. 4, 1992, pp. 356–365. <https://doi.org/10.1119/1.16878>.
- [53] Berhouni, I., Bailly, D., and Petropoulos, I., “On the Definition of Exergy in the Field of Aerodynamics,” *AIAA Journal*, Vol. 61, No. 10, 2023, pp. 4356–4366. <https://doi.org/10.2514/1.J062833>.
- [54] Uranga, A., Drela, M., Greitzer, E., Titchener, N., Lieu, M., Siu, N., Huang, A., Gatlin, G. M., and Hannon, J., “Preliminary Experimental Assessment of the Boundary Layer Ingestion Benefit for the D8 Aircraft,” AIAA Paper 2014-0906, January 2014. <https://doi.org/10.2514/6.2014-0906>.
- [55] Lamprakis, I., Sanders, D. S., and Laskaridis, “APROCON WP1.1a Final Deliverable - Boundary Layer Ingestion Use Case,” *Technical Report, APROCON Project, UKRI Project Reference Number 113092*, April 2020.
- [56] Sanders, D. S., “Aircraft Aerodynamic Work-Energy Relationships with Mass Source Considerations,” Technical Report RP0028, Cranfield University, Cranfield, United Kingdom, June 2023.
- [57] Sanders, D. S., “SUBLIME Deliverable 14 Report: Aircraft level assessment including uncertainty propagation,” Technical report, Cranfield University, Cranfield, United Kingdom, June 2023.
- [58] “SUBLIME Project, SUBLIME,” <https://sublime-cleansky.com>, 2023. [retrieved 26/12/2024].
- [59] Anderson, J. D., “Aerodynamics: Some Fundamental Principles and Equations,” *Fundamentals of Aerodynamics*, McGraw-Hill Series in Aeronautical and Aerospace Engineering, McGraw-Hill, 2011, Chap. 2, 5th ed., pp. 125–134.
- [60] Paparone, L., and Tognaccini, R., “Computational Fluid Dynamics-Based Drag Prediction and Decomposition,” *AIAA Journal*, Vol. 41, No. 9, 2003, pp. 1647–1657. <https://doi.org/10.2514/2.7300>.
- [61] Destarac, D., and van der Vooren, J., “Drag/thrust analysis of jet-propelled transonic transport aircraft; Definition of physical drag components,” *Aerospace Science and Technology*, Vol. 8, No. 6, 2004, pp. 545–556. <https://doi.org/10.1016/j.ast.2004.03.004>.
- [62] Drela, M., “Aerodynamic Force Analysis,” *Flight Vehicle Aerodynamics*, The MIT Press, Cambridge, Massachusetts, 2014, Chap. 5, pp. 99–102.
- [63] McLean, D., “Drag and Propulsion,” *Understanding Aerodynamics*, Wiley, West Sussex, England, 2013, Chap. 6, 1st ed., pp. 191–251. <https://doi.org/10.1002/9781118454190>.
- [64] White, F., “Laminar Boundary Layers,” *Viscous Fluid Flow*, McGraw-Hill international edition, McGraw-Hill, 2006, Chap. 4, 3rd ed., pp. 215–328.

- [65] Goldstein, S., “Concerning some Solutions of the Boundary Layer Equations in Hydrodynamics,” *Mathematical Proceedings of the Cambridge Philosophical Society*, Vol. 26, No. 1, 1930, p. 1–30. <https://doi.org/10.1017/S0305004100014997>.
- [66] Stewartson, K., “On the flow near the trailing edge of a flat plate II,” *Mathematika*, Vol. 16, No. 1, 1969, p. 106–121. <https://doi.org/10.1112/S0025579300004678>.
- [67] Messiter, A. F., “Boundary-Layer Flow Near the Trailing Edge of a Flat Plate,” *SIAM Journal on Applied Mathematics*, Vol. 18, No. 1, 1970, pp. 241–257. <https://doi.org/10.1137/0118020>.
- [68] White, F., “Compressible Boundary Layer Flow,” *Viscous Fluid Flow*, McGraw-Hill international edition, McGraw-Hill, 2006, Chap. 7, 3rd ed., pp. 505–566.
- [69] Airbus S.A.S., “Airbus A320 - Aircraft Characteristics, Airport and Maintenance Planning,” Technical Data Support and Services, 31707 Blagnac, Cedex, France. Issue: Sep 30/85, Rev: Dec 01/20, December 2020.
- [70] Seitz, A., Habermann, A. L., and van Sluis, M., “Optimality considerations for propulsive fuselage power savings,” *Proceedings of the Institution of Mechanical Engineers, Part G: Journal of Aerospace Engineering*, Vol. 235, No. 1, 2021, pp. 22–39. <https://doi.org/10.1177/0954410020916319>.
- [71] Habermann, A. L., Gokhale, A., and Hornung, M., “Numerical investigation of the effects of fuselage upsweep in a propulsive fuselage concept,” Vol. 12, No. Issue 1, 2021, pp. 172–189. <https://doi.org/10.1007/s13272-020-00487-2>.
- [72] Celik, I. B., Ghia, U., Roache, P. J., Freitas, C. J., Coleman, H., and Raad, P. E., “Procedure for Estimation and Reporting of Uncertainty Due to Discretization in CFD Applications,” *Journal of Fluids Engineering*, Vol. 130, No. 7, 2008, p. 078001. <https://doi.org/10.1115/1.2960953>.
- [73] NASA, “SST-Vm Expected Results - 2D Zero Pressure Gradient Flat Plate,” https://turbmodels.larc.nasa.gov/flatplate_sst.html, 2021. Accessed: Nov. 7, 2024.
- [74] Lamprakis, I., “Energy Decomposition of Viscous Flow Over a Flat Plate for Boundary Layer Ingestion Assessment,” MSc Thesis, Cranfield University, 2018.
- [75] Lamprakis, I., Sanders, D. S., and Laskaridis, P., “Energy-Based Aerodynamic Loss and Recovery Characteristics of Adiabatic and Heated Fuselages,” *Journal of Aircraft*, Vol. 60, No. 6, 2023, pp. 1947–1964. <https://doi.org/10.2514/1.C037246>.
- [76] Batchelor, G. K., “Equations Governing the Motion of a Fluid,” *An introduction to fluid dynamics*, Cambridge University Press, Cambridge, England, U.K., 2010, Chaps. 3 - Equations Governing the Motion of a Fluid, pp. 131–175.
- [77] van Kuik, G., “Analysis of Froude’s actuator disc flows,” *The Fluid Dynamic Basis for Actuator Disc and Rotor Theories*, IOS Press, Amsterdam, The Netherlands, 2018, Chap. 5, 1st ed., pp. 43–61.



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Lamprakis, Ioannis; Sanders, Drewan S.; Laskaridis, Panagiotis

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