

Coupled propulsive and aerodynamic analysis of an installed ultra-high bypass ratio powerplant at high-speed and high-lift conditions

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To achieve the targets proposed in the Flightpath 2050 for the aviation industry, more efficient propulsive systems are required. One possible solution is to increase the bypass ratio of the engines to increase the propulsive efficiency and reduce the specific fuel consumption. However, larger fan diameters are expected for these configurations, which results in an increase in the aerodynamic coupling between the powerplant and the airframe. The aim of this work is to develop and demonstrate a thrust and lift matching methodology for installed powerplants using a coupled aero-propulsive model. As a proof of concept, the aerodynamic performance of an ultra-high bypass ratio powerplant integrated with the airframe was evaluated across different flight conditions. This includes high-lift operating conditions such as end of runway; and high-speed conditions such as mid cruise. To evaluate the aerodynamic performance of the propulsion integration a combined assessment of the airframe and powerplant aerodynamics is required using computational fluid dynamics (CFD). The integration of the powerplant with the airframe has the potential to change the engine requirements across the aircraft operational envelope. To account for this the aerodynamic analysis is coupled with a turbomachinery model to adjust the engine thermodynamic conditions at a given operating point.

I. Nomenclature

Roman symbols

A_{hi}	=	Highlight area
A_{∞}	=	Area of the streamtube at the freestream
C_L	=	Lift coefficient
C_{local}^{wing}	=	Local cord of the wing
C_V^*	=	Modified velocity coefficient
D	=	Drag
D_{af}	=	Airframe drag
D_{nac}^*	=	Modified nacelle drag force
F_{Gi}	=	Stream Gauge Forces
$F_{N,MC}$	=	Reference nominal thrust force at mid cruise conditions
$F_{N,TO}$	=	Reference nominal thrust force during take-off conditions
g	=	Earth's gravity
GPF^*	=	Modified Gross Propulsive Force
h	=	Altitude
L	=	Lift
$\dot{m}$	=	Mass flow rate

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$\dot{m}_{fuel}$	=	Fuel mass flow rate
N	=	Rotation time
s_g	=	Required runway length
S_{ref}	=	Reference cross sectional area of the aircraft
T	=	Thrust
$T_{t,BP}$	=	Total temperature in the bypass duct
$T_{t,CR}$	=	Total temperature in the core duct
V_∞	=	Freestream velocity

Greek symbols

$\alpha_{A/F}$	=	Airframe angle of attack
α_{eng}	=	Engine angle of attack
α_{up}	=	Up-wash angle
γ	=	Climb angle
θ	=	Pitch angle
θ_{droop}	=	Nacelle droop angle
θ_{scarf}	=	Nacelle scarf angle
θ_j	=	Surface forces
Ω_{pitch}	=	Engine pitch angle
Ω_{toe}	=	Engine toe angle
ϕ_{nac}	=	Nacelle drag force component
ϕ_{pre}	=	Pre-entry stream drag force component
ϕ_{pylon}	=	Pylon drag force component
ρ_∞	=	Ambient density

Acronyms

BPR	=	Bypass ratio
CNPR	=	Core Nozzle Pressure Ratio
EoR	=	End of runway
FF	=	Free-Flying cases
FNPR	=	Fan Nozzle Pressure Ratio
GCI	=	Grid Convergence Index
HL-CRM-TO	=	High-Lift Common Research Model at take-off
HS-CRM	=	High-Speed Common Research Model
MC	=	Mid cruise
MFCR	=	Mass Flow Capture Ratio
MTOW	=	Maximum take-off weight
NPF	=	Net Propulsive Force
NVF	=	Net Vehicle Force
ROC	=	Rate of climb
SFC	=	Specific fuel consumption
TRET	=	Turbine rotor entry temperature
UHBR	=	Ultra-High Bypass Ratio engine

II. Introduction

To achieve the targets proposed in the Flightpath 2050 [1] for the aviation industry, more efficient propulsive systems are required. One possible solution is to increase the bypass ratio (BPR) of the civil turbofan engines to increase the propulsive efficiency and reduce the specific fuel consumption [2]. However, as a consequence of the increase on BPR, larger fan diameters are expected for these configurations. This is expected to increase the mass and drag of the housing components of the powerplant (intake, nacelle and exhaust) [3]. Under these circumstances, more compact designs of the housing components are required to reduce the overall drag contribution of the powerplant integration [4].

However, this requires a detailed design of the fan cowl and exhaust aerodynamics to maximise the fuel savings. The design and optimisation approaches have been mostly focussed on the study of the nacelle and exhaust in isolation [5–7]. Nevertheless, the increase on the fan diameter will require a closer integration of the propulsion system within the airframe. This is expected to increase the aerodynamic coupling between the engine and the airframe [8, 9]. Thus, to evaluate the aerodynamic performance of the integrated powerplant design a combined assessment of the airframe and powerplant aerodynamics is required.

To meet the envisaged requirements for the aviation industry [1], the design of the next generation of civil aero-engines is a multi-disciplinary and multi-point design problem. Design considerations include aspects related to both Design-Point (DP), as well as Off-Design (OD) performance at key points within the aircraft operational envelope. These considerations include thrust requirements at take-off, engine life based on climb requirements, and specific fuel consumption at cruise. As a result, the engine design process needs to account for aspects related to the aerodynamic behaviour of the coupled engine-airframe system, as well the thermodynamic performance of the incorporated engine cycle [8]. The increase in the aerodynamic coupling between the propulsion system and the airframe could potentially result in a greater impact of the powerplant integration on the overall aircraft aerodynamic performance. This could affect the propulsive requirements for the aircraft at DP and OD conditions [10], which may modify the engine design and operation requirements. Thus, it is of interest to incorporate the impact of the propulsion integration on the overall aircraft performance into the engine design process with a coupled aerodynamic and propulsive model. Coupled aero-propulsive approaches based on computational fluid dynamics (CFD) have been studied at design point for highly-coupled systems such as boundary layer ingestion propulsors in works such as Gray et al [11] and Yildirim et al. [12]; and over-wing installed ultra-high bypass ratio (UHBR) nacelles by Kaiyoom et al. [13]. However, to the authors' knowledge there is no previous work that included a coupled aero-propulsive analysis for under-wing installed UHBR engines across DP and OD conditions, such as take-off. Thus, as a key contribution, this work presents a methodology to adjust the thrust and lift requirements at different operating conditions (mid cruise and end of runway) of the aircraft based on the CFD assessed aerodynamic performance of the coupled airframe and powerplant system.

In preliminary design stages, the aerodynamic performance of the powerplant is sometimes approached by the design of the housing components (intake, nacelle, and exhaust) in isolation, due to the increase of the computational cost for the coupled engine-airframe design strategies. Thus, there is a limited amount of information about the integration effects on the aerodynamic performance of the powerplant for UHBR engines. Tejero et al [9] and Sánchez-Moreno et al [14] analysed the nacelle aerodynamics for integrated powerplants under cruise conditions. Goulos et al [8] also carried out an evaluation of the impact of the engine installation position in the overall aerodynamics of the aircraft. For other design conditions, such as take-off, climb, descent and landing, the available information in the open literature is also quite limited. Rao et al [15] investigated the effects of descent conditions in the exhaust performance of a high bypass ratio engine. Subbian et al also evaluated the aerodynamics of UHBR engines for landing [16] and take-off conditions [17]. Nevertheless, to the authors' knowledge, a comprehensive comparison of the aerodynamic performance of the powerplant across the different stages of the aircraft flight has not been performed.

The aim of this work is to develop and demonstrate a thrust and lift matching methodology for installed powerplants using a coupled aero-propulsive model based on CFD. A one-dimensional engine model is used to adjust the engine thermodynamic conditions to the thrust requirements of the installed powerplant. As a proof of concept, the aerodynamic performance of an UHBR powerplant integrated with the airframe was evaluated across different flight conditions, such as end of runway, and mid cruise. This involves the analysis of the aerodynamics of a high-speed long-range airframe [18] and the equivalent high-lift airframe based on the work of Lacy and Clark [19] under take-off conditions. An analysis of the effects of the aerodynamic interference between the airframe and the powerplant is also performed by the comparison of the aerodynamics of the installed powerplant with the powerplant and the airframe in isolation. This results in a multi-disciplinary design approach to evaluate the key requirements for the powerplant design and integration across the different stages of flight.

III. Methodology

A. Airframe and powerplant configurations

Two airframe configurations are used (Fig. 1) based on the NASA Common Research Model (CRM)[18–20]. First, a take-off airframe based on the High-Lift Common Research Model [19] configuration (HL-CRM-TO) is used for the end of runway operating conditions. Second, the High-Speed Common Research Model (HS-CRM) [18] is used for the mid cruise operating condition. Both configurations share the same fuselage and horizontal stabiliser. In

both cases, the vertical stabiliser is not included. The HL-CRM-TO includes three high-lift devices (Fig. 1): one single body flap for outboard and inboard, an inboard slat, and an outboard slat. The fairings of the high-lift devices are not included in the airframe model. The inboard and outboard slats are deployed at 22 degrees, and the flaps are deployed at 25 degrees as defined by Lacy and Clark [19].

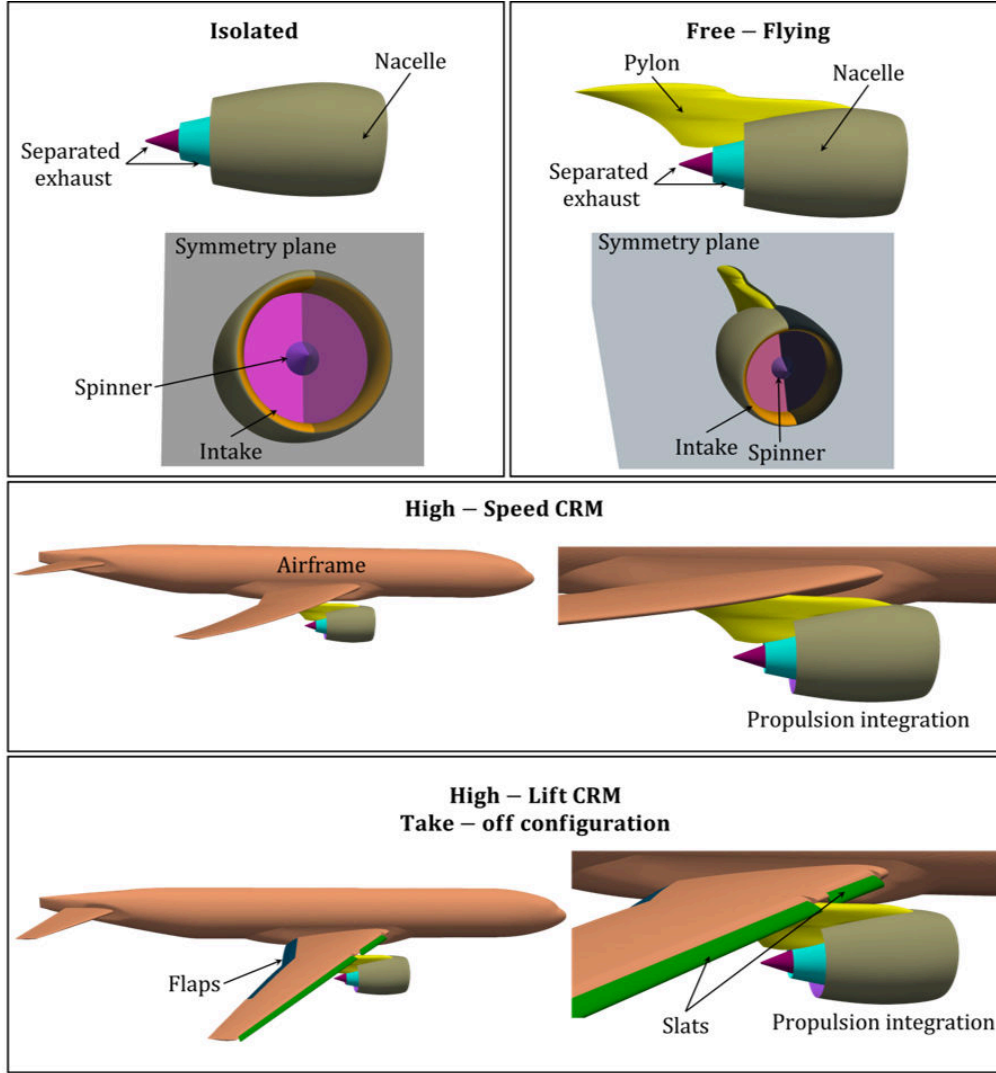


Fig. 1 Summary of the airframe and powerplant configurations

The powerplant configuration comprises the housing components of the powerplant (intake, nacelle, and exhaust) and the pylon (Fig. 1). The powerplant is studied in isolation or integrated into the airframe (installed). The isolated powerplant configurations are divided into two categories: an uninstalled powerplant without pylon (isolated), and an uninstalled powerplant with pylon (free-flying). The uninstalled configurations are applied to evaluate the impact of the integration of the powerplant on the aerodynamics of the aircraft.

A short nacelle is used ($l_{nac}/r_{hi} = 3.1$) to reduce the drag of the ultra-high bypass ratio engine [4]. The nacelle was selected from the non-dominated designs of a multi-objective optimisation approach [6, 21] of the aerodynamic performance of the nacelle in isolation (i.e., without pylon and airframe). The intake was drooped and scarfed (Fig. 2) to improve the alignment of the powerplant with the flow. Also, a separate exhaust design is included based on a multi-objective optimisation of the exhaust performance [22]. A bespoke pylon design is included in this configuration, and areas of the exhaust are corrected to compensate for the pylon blockage effects [8].

A single installation position of the powerplant is considered. The engine position is defined as the vertical

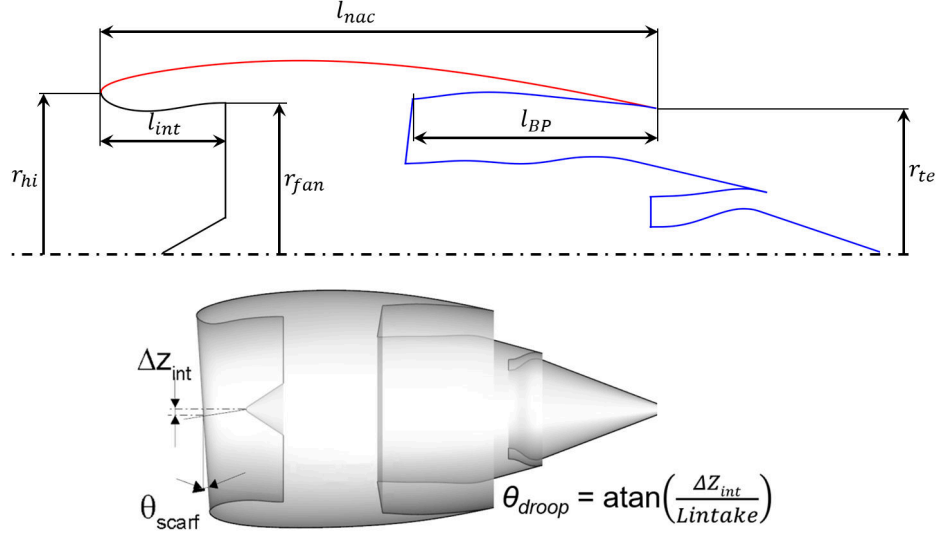


Fig. 2 Definition of the main dimensions of the housing components

($\zeta = z/C_{local}^{wing}$) and horizontal ($\xi = x/C_{local}^{wing}$) distance of the nacelle trailing edge with respect to the wing leading edge at the installation point (Fig. 3). The lengths are normalised with the local chord of the wing at the installation point. For this work, the nacelle trailing edge horizontal location was set to be coincident with the wing leading edge ($\xi = 0$). The nacelle is positioned vertically at $\zeta = 0.15$. Based on the trailing edge of the top line of the nacelle, the powerplant is also pitched (Ω_{pitch}) in 1.75° and toed (Ω_{toe}) in 2.25° towards the inboard side of the airframe (Fig. 3) based on the position of the NASA CRM[18]. The vertical position was selected to maintain the same approximate minimum gap between the deflected slat and the nacelle surface. Consequently, given the large nacelle diameter, the vertical offset (ζ) is relatively large. Nevertheless, this installation position is considered adequate for the demonstration of the thrust matching methodology.

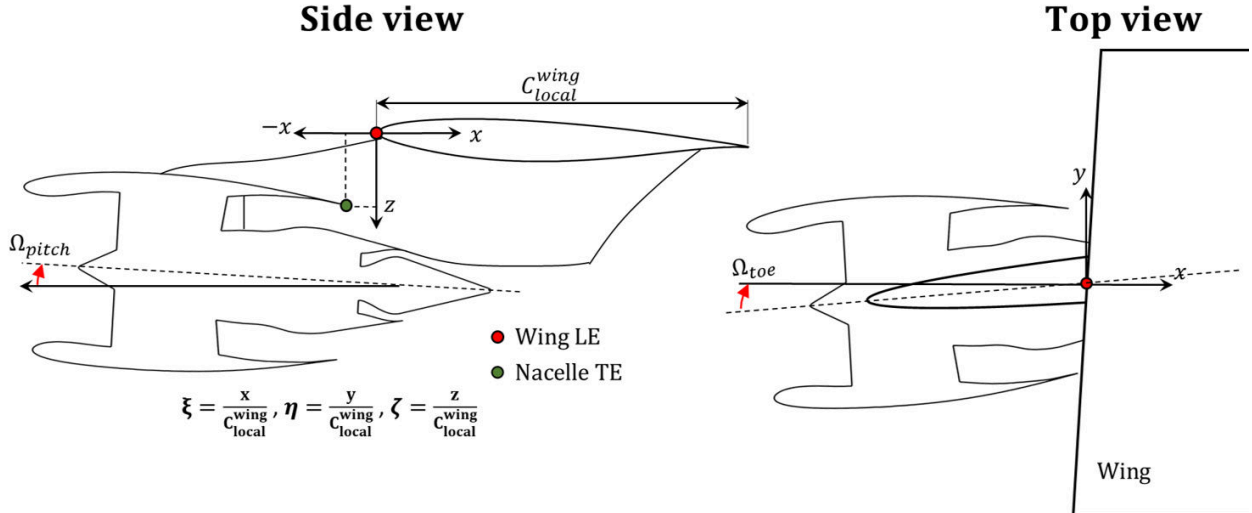


Fig. 3 Schematic of the position and main angles of the powerplant relative to the wing leading edge

B. Performance bookkeeping

An established industrial method based on near-field momentum [8, 14, 23] is used to evaluate the aerodynamic performance of the engine-airframe system. Five coordinate systems are defined to evaluate the performance of the airframe and the powerplant: the horizon axes, the aircraft body axes, the aerodynamic axes (or climb path), the engine axes, and the local flow axes (Fig. 4). The lift and drag are defined based on the aerodynamic axes. The different coordinate systems are used to define the main angles for the aircraft operation (pitch, climb, and airframe angle of attack), and the engine (engine angle of attack, engine pitch, and wing up-wash angle). The airframe angle of attack ($\alpha_{A/F}$) is defined as the angle between the flight path and the aircraft horizontal axis. At cruise operating conditions, the climb angle (γ) is zero, while at other conditions it is defined with the rate of climb (ROC). The engine angle of attack (α_{eng}) is composed by the airframe angle of attack, the engine pitch (Fig. 3), and the local up-wash angle (α_{up}). To estimate the up-wash angle, a streamline is sampled from the stagnation point of the wing without the powerplant at the equivalent engine central plane. The local flow angle (α_{local}) is then evaluated at the axial position of the engine highlight (Fig. 4).

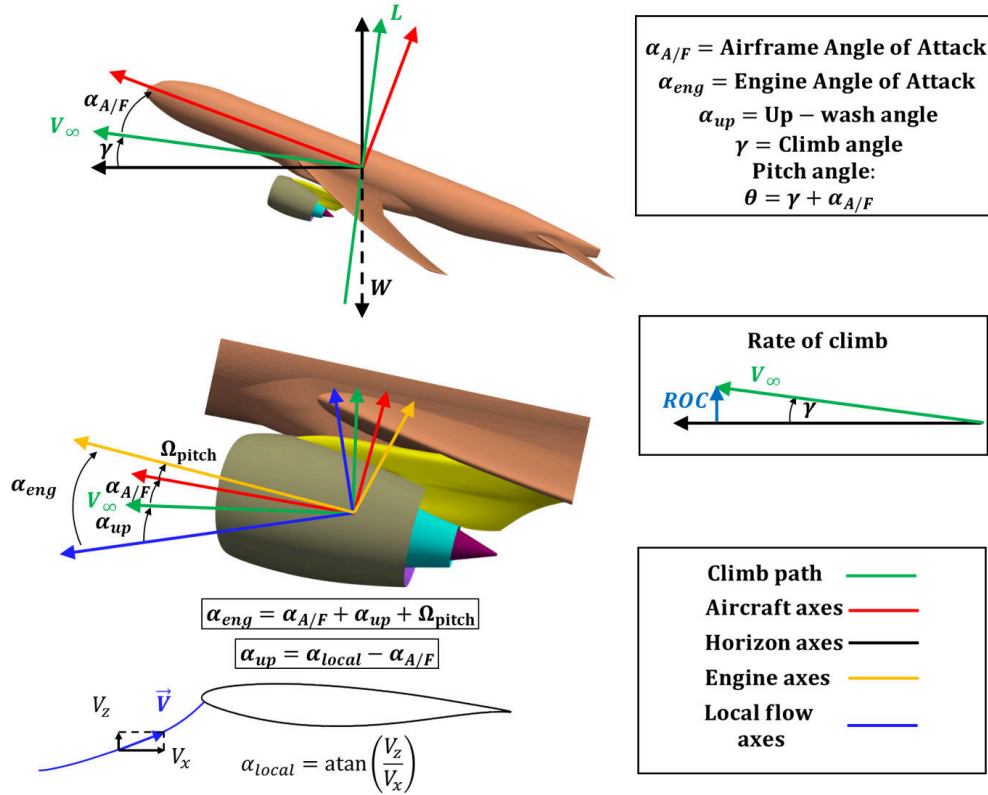


Fig. 4 Definition of the reference frames and the main angles applied

To obtain the net forces of the system the thrust-drag decomposition (Fig. 5) is based on the modified near-field momentum formulation [23]. A modified drag force for the nacelle (D_{nac}^*) is defined (Eq. 1) by excluding the post-exit forces (ϕ_{post}). This formulation includes the drag forces (Eq. 2) of the pre-entry streamtube (ϕ_{pre}), the surface drag of the fan cowl (ϕ_{nac}), and the drag contribution of the pylon (ϕ_{pylon}) in the free-flying (Fig. 1) and installed cases [8].

$$D_{nac}^*|_D = \phi_{pre} + \phi_{nac} + \phi_{pylon} \quad (1)$$

$$\phi_{pre}|_D = -F_{G0} + F_{G2} - (\theta_{spin} + \theta_{intk}) \quad (2)$$

The propulsive contribution of the powerplant is accounted based on the engine cycle requirements and the exhaust performance as defined by Goulos et al [8]. The exhaust performance is evaluated based on the modified velocity coefficient, C_V^* (Eq. 3). This metric is a function of the modified gross propulsive force of the system (GPF^*) obtained from the CFD evaluation, and the ideal propulsive force (IPF). The IPF is a function of the actual mass flow rates of

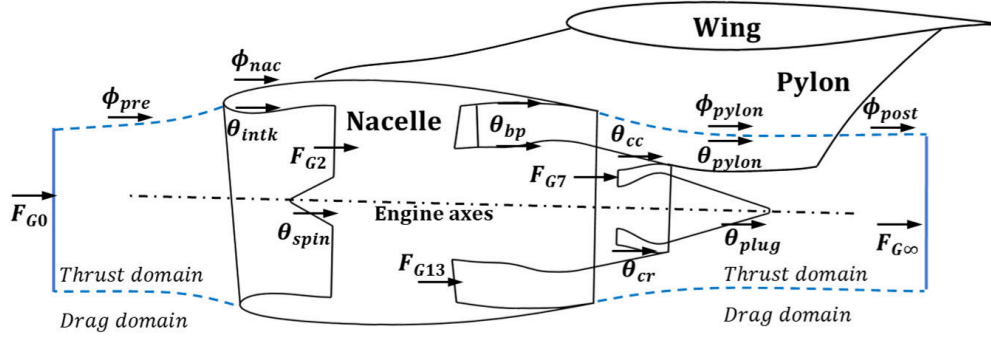


Fig. 5 Definition of the force decomposition for the modified near-field method, based on Sánchez-Moreno et al [14] and Goulos et al [8]

the bypass ($\dot{m}_{BP}$) and the core ($\dot{m}_{CR}$) and the ideal velocities, V^{Ideal} , in each duct (Eq. 5). The V^{Ideal} is obtained from the total temperature and the nozzle pressure ratio ($\lambda = P_t/p_\infty$) in each of the exhaust ducts. The C_V^* definition is independent of changes in the mass flow rate in the exhaust due to exhaust suppression [8]. In addition, since the C_V^* is obtained based on the CFD evaluation of the system, this definition also includes the installation and interference effects on the exhaust performance. Hence, this metric is used to scale the propulsive force based on the mass efflux of the engine model ($\dot{m}^{cycle}$) to couple the CFD simulation with the engine performance. The modified gross propulsive force is corrected to obtain the propulsive contribution of the installed powerplant (Eq. 6).

$$C_V^*|_D = \frac{GPF^*|_D}{IPF} = \frac{GPF^*|_D}{\dot{m}_{BP}V_{BP}^{Ideal} + \dot{m}_{CR}V_{CR}^{Ideal}} \quad (3)$$

$$GPF^*|_D = (F_{G13} + F_{G7})|_D - (\theta_{bp} + \theta_{cc} + \theta_{cr} + \theta_{plug} + \theta_{pylon})|_D \quad (4)$$

$$V^{Ideal} = \sqrt{\frac{2\gamma RT_t}{\gamma - 1} \left(1 - \left(\frac{1}{\lambda} \right)^{\frac{\gamma-1}{\gamma}} \right)} \quad (5)$$

$$GPF^*|_D^{Cycle} = C_V^*|_D \left(\dot{m}_{BP}^{Cycle} V_{BP}^{Ideal} + \dot{m}_{CR}^{Cycle} V_{CR}^{Ideal} \right) \quad (6)$$

The modified drag force for the nacelle (D_{nac}^*) is balanced with the modified Gross Propulsive Force ($GPF^*|_D^{Cycle}$) of the powerplant and the inlet momentum force (F_{G0}) to define the Net-Propulsive Force (NPF) of the propulsive system (Eq. 7). This term summarises the net force contribution of the powerplant and the housing components. The NPF is directly defined in the drag direction of the aerodynamic axis, marked with the sub-index D . In the cases where the powerplant is installed, the propulsive forces can be balanced with the airframe lift and drag forces ($L_{A/F}$ and $D_{A/F}$) to define the Net Vehicle Force (NVF). This is used to define the NVF in the lift direction, $NVF|_L$, (Eq. 8) and the NVF in the drag direction, $NVF|_D$, (Eq. 9). The $NVF|_D$ is also equivalent to the difference between the total thrust contribution of the engine (T) and the absolute value of the aircraft drag (D). In an operating condition without any acceleration or climb of the aircraft (e.g., mid-cruise conditions), the $NVF|_D$ should be equal to zero. Any increase of $NVF|_D$ at these operating conditions is considered as an excess thrust. Thus, the power input of the powerplant can be reduced which leads to savings in the fuel burn. Hence, this metric can be used as a surrogate of fuel burn to compare the performance of different powerplant integration designs under static flight. In operating conditions under climb or acceleration, the $NVF|_D$ is not equal to zero. Thus, the evaluation of the overall benefit of the powerplant integration design should be compared with the target acceleration. The $NVF|_L$ is equivalent to the total lift force of the aircraft. As such, it can be normalised to obtain the lift coefficient of the aircraft (C_L) and decompose between the airframe, the powerplant aerodynamics, and the propulsive components of the lift (Eq. 10).

$$NPF|_D = GPF^*|_D^{Cycle} - F_{G0} - D_{nac}^* \quad (7)$$

$$NVF|_L = L_{A/F} + L_{nac}^* + GPF^*|_L \quad (8)$$

$$NVF|_D = NPF|_D - D_{A/F} = T - D \quad (9)$$

$$C_L = \frac{NVF|_L}{q_\infty S_{ref}} = \frac{L_{A/F} + L_{nac}^* + GPF^*|_L}{\frac{1}{2}\rho_\infty V_\infty^2 S_{ref}} = C_{L,A/F} + C_{L,nac}^* + C_{L,GPF}^* \quad (10)$$

C. Computational fluid dynamics methodology

A compressible, viscous computational fluid dynamics approach is used to evaluate the aerodynamic performance of the different powerplant and airframe configurations (Fig. 1). ANSYS Fluent [24] is applied as the CFD solver. Two CFD approaches are combined to optimise the simulation performance for the different aircraft configurations: a pressure-based CFD approach, and a density based CFD approach. The pressure-based CFD approach is used in the cases with the HL-CRM-TO airframe. A segregated pressure-based approach with the SIMPLE algorithm is used [24]. A Reynolds-average Navier-Stokes approach is used with a second-order up-wind scheme for the spatial discretisation. The density-based CFD approach is used in the cases with the HS-CRM airframe, the free-flying, and the isolated engine cases. A Favre-averaged Navier-Stokes approach is used with a second order up-wind scheme for the spatial discretisation.

In both CFD approaches, the Green-Gauss Node Based scheme is applied for the gradient definition. As the turbulence closure model, the $k-\omega$ SST model [25] is applied. Air is used as the working fluid with an ideal gas model. The air properties at a given altitude are obtained based on the International Standard Atmosphere model [26]. Sutherland's law is used to calculate the changes on dynamic viscosity with the temperature.

The domain and boundary conditions are also consistent between the pressure-based and the density-based CFD approaches. The computational domain is a hemisphere with a symmetry plane. The symmetry plane is placed at the central plane of the airframe in the installed engine-aircraft configurations and on the engine central plane on the free-flying and isolated powerplant configurations (Fig. 6). The domain has a radius of 100 times the reference chord length based on the recommendations of the 4th NASA Drag Prediction Workshop [20]. Free-stream boundary conditions for the far field boundaries of the domain (Fig. 6), based on the altitude and the flight Mach number. The angle of attack of the airframe ($\alpha_{A/F}$) is defined at this boundary based on the airframe orientation towards the flow. No-slip adiabatic walls are applied for the surfaces of the powerplant and the airframe. A target mass flow boundary condition with strong pressure averaging [24] is imposed at the fan face (Fig. 6). The target mass flow is controlled by the mass flow capture ratio, MFCR (Eq. 11). Pressure inlet boundary conditions are applied on the inlet boundaries of the bypass and the core (Fig. 6). The total pressure at these boundaries is defined by the Fan Nozzle Pressure Ratio (FNPR) in the bypass duct, and the Core Nozzle Pressure ratio (CNPR) in the core duct. The total temperature at these boundaries is also prescribed.

$$MFCR = \frac{A_\infty}{A_{hi}} = \frac{\dot{m}}{\rho_\infty V_\infty A_{hi}} \quad (11)$$

A consistent surface refinement is applied for all the airframe and powerplant configurations. Structured inflation layers are defined normal to the wall boundaries to cover the boundary layer regions. The inflation layer is maintained at the pressure inlets and the fan face to keep mesh continuity. The size of the first layer and the growth ratio at these boundaries is corrected to improve the numerical stability of the solver. In the wall boundaries a y^+ value near unity is applied for all the CFD approaches. The remaining domain is defined with unstructured groups of tetrahedra. Local refinement boxes are applied in the regions of interest near the powerplant integration and the wing of the airframe (Fig. 7). The mesh densities of the different configurations are summarised in Table 1.

The mesh resolution and CFD methodology for the powerplant was validated based on the Dual Separate Flow Reference Nozzle (DSFRN) by Goulos et al [8] and Hueso-Rebassa [27]. For the fan cowl, the methodology was also validated by Tejero et al [6] for aero-engine nacelles. The airframe mesh methodology was validated by Goulos et al [8] based on the high-speed CRM configuration. Hueso-Rebassa [27] performed an analysis of the sensitivity of the computational approach to the grid resolution, based on the approach defined by Celik et al [28] for the powerplant and airframe meshes at mid-cruise conditions. For an analogous mesh definition, a Grid Convergence Index (GCI) of approximately 0.16% in NPF was identified for the powerplant mesh on a free-flying configuration. The mesh methodology for the airframe predicted the lift coefficient with a GCI of 0.64%, and the drag coefficient with a GCI of 1.47%. The sensitivity of the solution to the mesh resolution was also evaluated by Hueso-Rebassa [27] for the

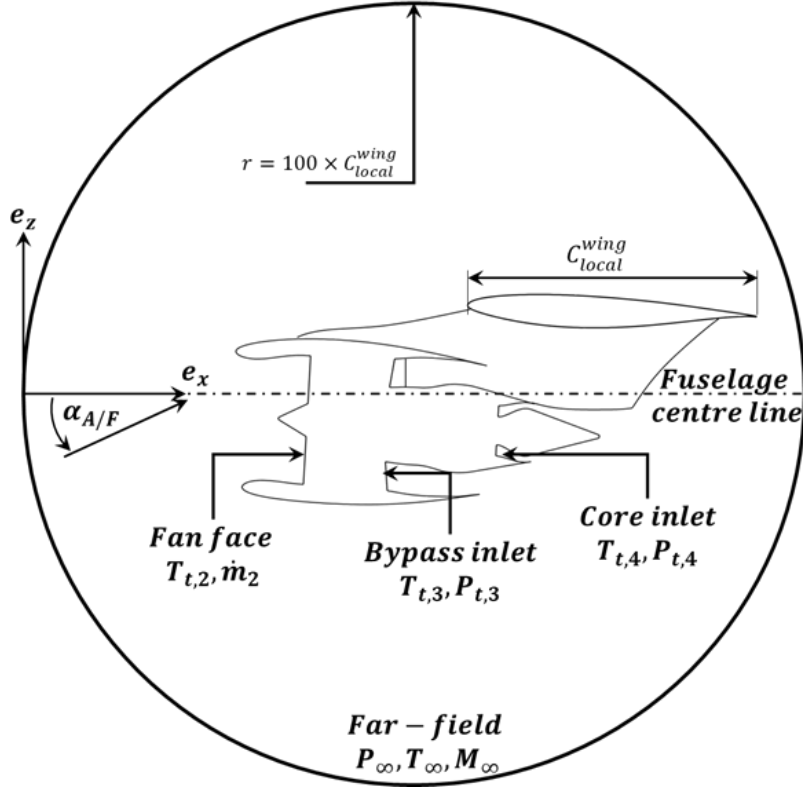


Fig. 6 Summary of the boundary conditions for the installed powerplant configurations. Domain and boundary conditions equivalent to the free-flying and isolated configurations

Table 1 Summary of the mesh resolution for the airframe and powerplant configurations of the present work

Summary of the mesh resolution for the airframe and powerplant configurations						
Configuration	Isolated	Free Flying	HS-CRM Installed Powerplant	HL-CRM-TO Installed Powerplant	HS-CRM Isolated airframe	HL-CRM-TO Isolated airframe
Million Elements	55	66	145.3	263.2	24.0	126.6

powerplant installed into the HS-CRM airframe. A GCI of 0.34% was obtained for the NPF. The airframe Drag, $D_{A/F}$ had a GCI of 1.57% for an equivalent mesh methodology.

For the HL-CRM-TO configurations, a mesh resolution analogous to the cruise cases is used for the powerplant. On the airframe, the mesh resolution was defined based on the guidelines of the 4th AIAA High-Lift Drag Prediction Workshop [20]. To verify the validity of the HL-CRM-TO meshing approach, the selected mesh resolution (263.2×10^6 elements) was compared with results from a coarser mesh (88.5×10^6 elements) at sea level, a flight Mach number of 0.25, fixed engine operating conditions, and $\alpha_{A/F}$ of 12.5° . A difference in NPF of 0.016% was observed between both cases, based on the reference mesh value. The NVF increased by 1.97% from the coarse to the current mesh, based on the reference mesh value. This was mostly due to the reduction of the airframe drag by 1.21%, based on the reference mesh value. Nevertheless, a consistent lift value for the full airframe and powerplant system is observed with a reduction of 0.006% in the lift coefficient. While the NPF and aircraft C_L are relatively consistent between both meshes, the changes on NVF and $D_{A/F}$ are more significant. Thus, the finer mesh is used in the present studies.

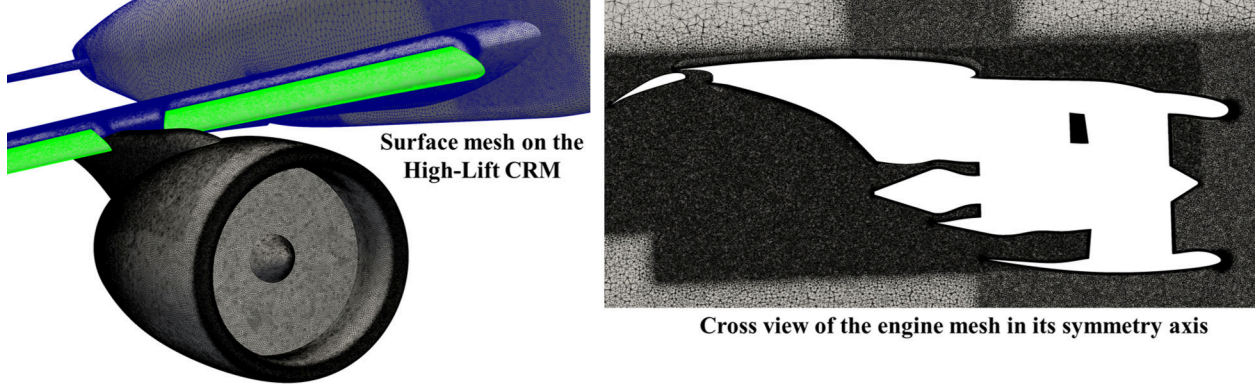


Fig. 7 Details of the mesh in the HL-CRM configuration with the installed powerplant

IV. Definition of the operating conditions

Two main operating points are evaluated in this work: take-off, and mid-cruise (MC). The take-off operating point is a transient condition. To simplify this analysis, the take-off condition has been reduced to the end of runway (EoR). This point is evaluated at steady CFD conditions to understand the aerodynamics of the installed powerplant. The EoR condition is defined as the point previous to the lift off from the ground where the engine is operating at the Maximum take-off thrust rating .

A. Mid cruise conditions

At mid cruise conditions (Table 2), the HS-CRM airframe is used (Fig. 1). The flight altitude is 35,000 feet with a cruise Mach number of $M_\infty = 0.85$. Based on the HS-CRM [20] requirements, the performance of the aircraft is evaluated at a target lift value of $C_L = 0.5$. For this purpose, the analyses were conducted at two airframe angles of attack (2.0° and 2.5°). Then, the NVF of the aircraft is interpolated for the target lift.

To define the powerplant operating conditions (i.e., $FNPR$, $CNPR$, $MFCR$, $T_{t,BP}$, and $T_{t,Cr}$), a dual spool geared turbofan model is used. This model was defined based on general technology assumptions and does not correspond with any existing or future engine configuration. The reference thrust at cruise ($F_{N,MC}$) is selected based on top-level aircraft requirements (airframe drag, flight Mach number, altitude...) for long-range, wide-body, twin-engine configurations. The overall engine dimensions and bypass ratios were defined based on a representative Ultra-High Bypass Ratio engine. To evaluate the changes on operating conditions from the design point, TURBOMATCH [29] is used. This is analysed in further detail in Section IV.C.

The HS-CRM is a simplified airframe model that does not include some important contributors to the airframe drag, such as the vertical tail or the wing fairings. As a partial solution, based on the isolated HS-CRM airframe at the nominal C_L , an additional 5% of the airframe drag is deduced from the NVF value [30]. This value is introduced as an assumption to enable the current work. However, the capabilities develop within this work could be adapted for a more representative airframe geometry (i.e., including fairings, vertical fin, and other elements) to provide a more accurate representation of the changes in NVF . In addition, since the engine is sized based on the top-level requirements, some excess or deficit on NVF should be expected when the powerplant is installed.

Table 2 Summary of the preliminary operating conditions based on the top-level aircraft requirements. Net Vehicle Force values scaled based on the reference thrust (F_N) at each operating point

Summary of operating conditions							
	h [ft]	M	MFCR	$\alpha_{A/F}$	C_L	$\frac{NVF}{F_N}$	Airframe
Mid Cruise	35,000	0.85	0.674	2.0-2.5	0.5	0.0	HS-CRM
End of Runway	0.0	0.25	1.603	9-10	1.842	0.56	HL-CRM-TO

B. Take-off conditions

The EoR point is used to estimate the Maximum take-off Weight (MTOW) of the aircraft based on the lift provided by the airframe at this operating condition. A Mach number of 0.25 and sea level conditions are assumed. The lift-off velocity is estimated to be 1.1 times the stall velocity [31]. The take-off angle of attack was defined based on tail-strike clearance requirement of the NASA CRM, with a maximum angle of attack of about 9.25° at EoR. This constrains the maximum lift coefficient achieved at EoR conditions ($C_{L,max}$) to the C_L value at this angle of attack. However, for the CFD simulations the ground plane was not included. The climb angle (γ) is set to zero at this point. To obtain the value of the lift at a given angle of attack, the HL-CRM-TO configuration with the installed powerplant was evaluated at sea-level conditions and various angles of attack with fixed engine operating conditions for the required thrust level at take-off, $F_{N,TO}$ (Fig. 8).

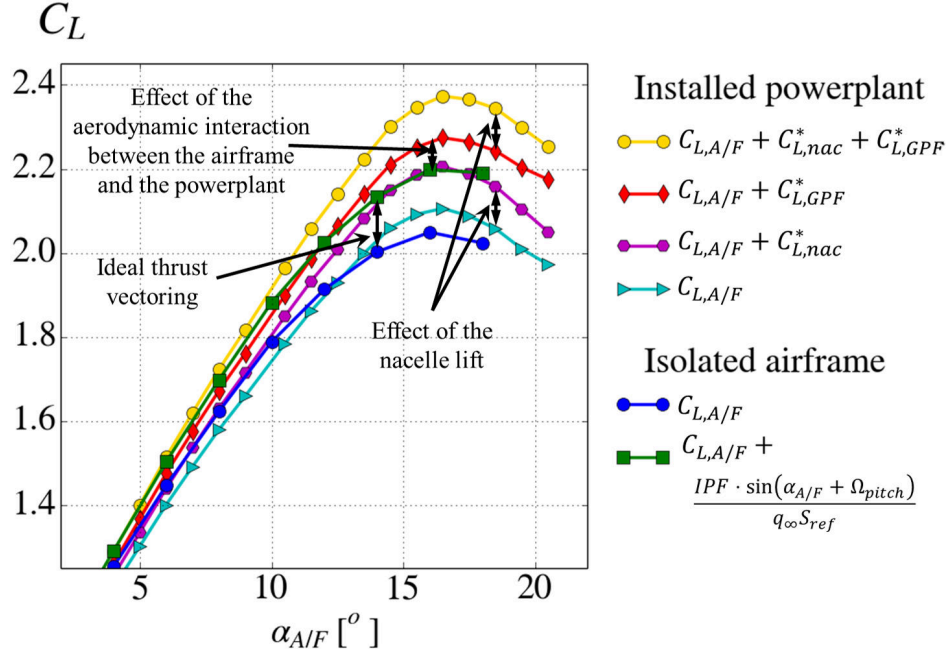


Fig. 8 Predicted C_L with the variation of $\alpha_{A/F}$ for the powerplant installed, and the HL-CRM-TO airframe in isolation. Operating at $M=0.25$ and sea level conditions

The variation of the lift with $\alpha_{A/F}$ for the installed powerplant and the isolated HL-CRM-TO airframe were compared to evaluate the impact of the aerodynamic interaction between the powerplant and the airframe (Fig. 8). The lift of the aircraft (C_L) was decomposed into the individual contributions of the airframe ($C_{L,A/F}$), the powerplant ($C_{L,nac}^*$), and the gross propulsive force ($C_{L,GPF}^*$). The contribution to the lift of the ideal propulsive force ($IPF \cdot \sin(\alpha_{A/F} + \Omega_{pitch})$) was added to the $C_{A/F}$ of the isolated HL-CRM-TO airframe. This is used to account for the thrust vectoring in a preliminary analysis, and to identify the discrepancies between a preliminary lift estimation and the aerodynamics of an installed powerplant. In most of the preliminary design tools, the lift will be predicted based on a similar uncoupled manner, not accounting for the aerodynamic interaction between the powerplant and the airframe. Thus, this comparison is one of the initial steps for the development of an approach to assess the coupled airframe and powerplant system and to identify the effects of their aerodynamic interaction.

The C_L for the installed powerplant is higher than the C_L of the isolated airframe with the ideal gross propulsive force contribution (Fig. 8). The difference in the C_L increases with $\alpha_{A/F}$, with differences varying from ~ 0.01 at $\alpha_{A/F} = 5^\circ$ to up to ~ 0.18 near the stall point ($\alpha_{A/F} = 16.5^\circ$). This difference in the C_L is a consequence of the combination of the nacelle lift contribution, the GPF^* , and the aerodynamic interaction between the powerplant and the airframe. The nacelle lift contributes in about $0.028 - 0.1$ to the overall aircraft C_L and the contribution increases with $\alpha_{A/F}$. The interaction between the powerplant and the nacelle has a negative impact on the C_L at $\alpha_{A/F}$ below 12° (Fig. 8), but it increases the aircraft lift near the stall point. This is also reflected by the increase of the $C_{L,A/F}$ of the installed powerplant at higher angles of attack (Fig. 8). Nevertheless, it is acknowledged that the operating conditions close to

the stall point are near to the performance limit of the RANS models. Thus, some of the differences in the C_L between the airframe with the installed powerplant and the airframe in isolation could be a consequence of the limitations of the CFD methodology.

The differences in C_L have an impact on the preliminary estimation of the MTOW. If the MTOW is estimated based on the isolated HL-CRM-TO, the $MTOW$ decreases approximately by 12.5% compared with the case with the installed powerplant configuration at $C_{L,max}$. At the EoR operating condition ($\alpha_{A/F} = 9.25^\circ$), the MTOW of the installed powerplant case is $\sim 3\%$ greater than the isolated airframe model. Given that the case with the installed powerplant is a more complete aerodynamic model, the value of MTOW at this condition was selected to determine the NVF required by the aircraft at EoR.

To evaluate the NVF requirement at EoR, a required runway length (s_g) of 3,000 meters was assumed [32, 33]. This is a representative value to demonstrate the thrust-matching capability at take-off. The lift-off velocity is estimated to be 1.1 times the stall velocity [31]. With this assumption, the required runway length for take-off can be evaluated with the equation 12). A rotation time (N) of three seconds is assumed [31]. The friction coefficient of the runway (μ_r) is fixed to a value of 0.8, representative a dry runway [31]. The maximum lift coefficient ($C_{L,max}$) is fixed based on the value at $\alpha_{A/F}$ for EoR to maintain the tail clearance requirements. Based on equation 7, the NVF can be assumed as the difference between the drag and the thrust contributions of the aircraft. Thus, the NVF at EoR, with respect to the reference nominal thrust at take-off ($F_{N,TO}$) is obtained with the equation 13. This results in a required $NVF|_{EoR}/F_{N,TO}$ of 0.56.

$$s_g = \frac{1.1^2(MTOW/S_{ref})}{\rho_\infty g C_{L,max} ((T - D)/MTOW - \mu_r (1 - L/MTOW))} + 1.1N \sqrt{\frac{2MTOW}{\rho_\infty C_{L,max} S_{ref}}} \quad (12)$$

$$\frac{NVF|_{EoR}}{F_{N,TO}} = \frac{MTOW}{F_{N,TO}} \frac{1.1^2(MTOW/S_{ref})}{\rho_\infty g C_{L,max} \left(s_g - 1.1N \sqrt{\frac{2MTOW}{\rho_\infty C_{L,max} S_{ref}}} + \mu_r (1 - L/MTOW) \right)} \quad (13)$$

The MTOW and the runway length requirement assumptions are used to size the aircraft and thrust requirements. However, the engine initially was sized based on the preliminary aircraft design requirements for a long-range, wide-body, twin-engine configuration. This results on an imbalance on the NVF with the target predicted for the selected runway length. To account for this, the engine thrust levels are adjusted at the EoR condition based on the CFD results. The methodology to adjust the thrust settings is explained in detail on the next section. The NVF requirement at EoR (Table 2) is used to identify the operating conditions of the engine at this operating point.

As for the HS-CRM airframe, the HL-CRM-TO configuration used in this work does not include some of the main contributors to the drag during take-off, such as the landing gear, the fairings or the vertical tail. To account for this, the drag of the airframe is adjusted based on the drag of the isolated HL-CRM-TO airframe at the target angle of attack (Table 2). At EoR, the value of the airframe drag is increased by 10% [30] to account for the landing gear and the other elements. This is a preliminary estimation of the airframe drag at EoR to demonstrate the current methodology. A more accurate definition of the airframe drag and the propulsive requirements of the powerplant could be included if a more representative airframe was used.

C. Thrust and lift matching

The CFD evaluation of the aircraft at a given angle of attack results in some differences with respect to the target C_L and the target NVF (Table 2). Thus, the angle of attack and engine operating conditions have to be trimmed to match the aircraft operating requirements at a given flight condition. The C_L trim is achieved by the CFD evaluation of two $\alpha_{A/F}$ which yield values of C_L below and above the target value. The angle of attack and all the aircraft forces are interpolated to the target C_L based on these two angles. However, to apply the propulsive trim a propulsion model coupled with the CFD aerodynamic approach is required. Hence, a thrust and lift matching approach was developed to couple the aero-propulsive effects (Fig. 9).

The thrust and lift matching strategy for the installed powerplant configurations is based in two levels (Fig. 9). First, the lift is matched for a given set of engine operating conditions (i.e., $FNPR$, $CNPR$, $MFCR$, $T_{t,BP}$, and $T_{t,Cr}$) by the CFD evaluation of two angles of attack (AoA_1 and AoA_2). In an intermediate step, the mass flow in the engine fan face is matched to the values of the mass flow in the exhaust. This accounts for the local suppression effects in the exhaust [8]. However, the effect of the exhaust suppression is not directly coupled with the engine model, but included into the aerodynamic model to maintain the mass flow conservation. After the evaluation of the two angles of attack, the

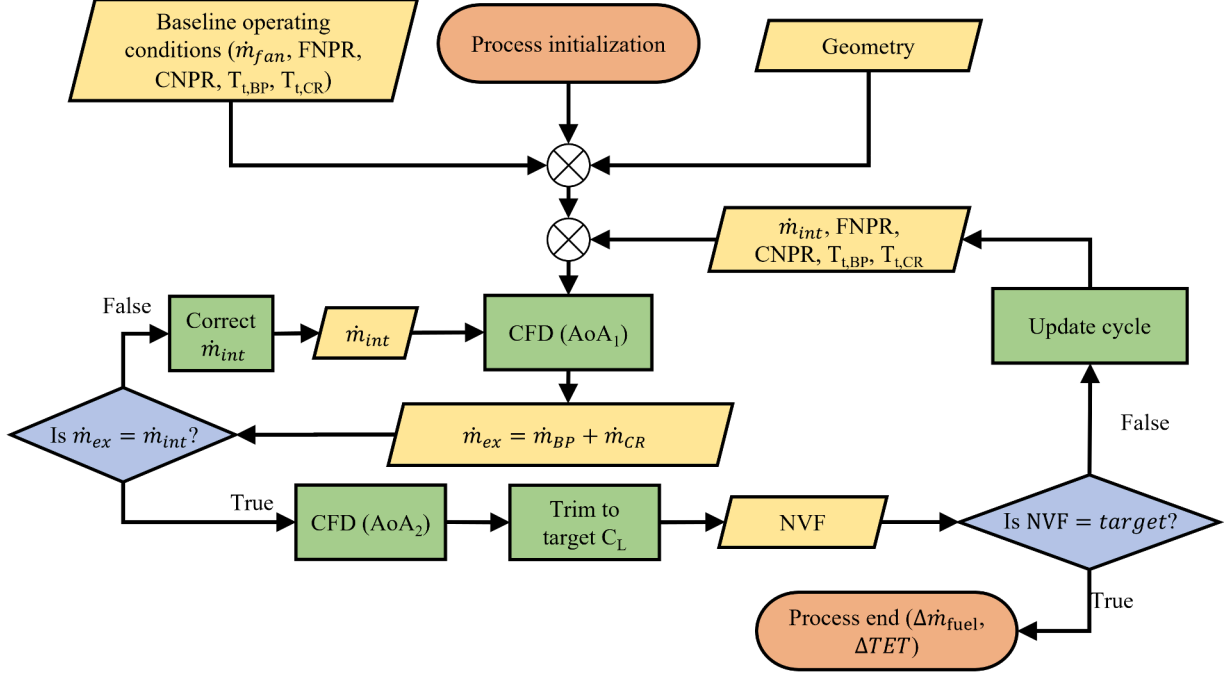


Fig. 9 Work-flow for the turbomachinery re-matching with the aerodynamic performance of the integrated powerplant at cruise conditions

$NVF|_D$ at the target C_L of the aircraft (airframe, powerplant and propulsive contribution) is obtained. In the second level, the thrust is adjusted based on the target $NVF|_D$ required. This involves the application of a one-dimensional engine model to define the new engine operating conditions based on the new thrust requirements. With the adjusted engine operating conditions, the first step is repeated until the difference in $NVF|_D$ with the target value (NVF_{target}) is below a given threshold. For this work, a threshold of 0.25% of the nominal thrust at the flight operating condition was selected. However, the simple adjustment of the engine operating conditions with a one-dimensional model does not account for possible non-linear changes on the $NVF|_D$ due to the powerplant and airframe aerodynamic performance. To address this issue, after the first iteration of the engine operating conditions, the subsequent iterations are adjusted based on a Regula Falsi method [34].

For the one-dimensional engine model, a two-spool geared ultra-high bypass ratio engine with fixed dimensions is used. The engine performance is evaluated with TURBOMATCH [29]. Based on the design point of the engine at mid cruise conditions, it is required to evaluate off-design thrust variations ($\Delta T = NVF - NVF_{target}$) to determine the engine operating conditions at different thrust levels. These thrust variations are based on the increase or reduction of the $NVF|_D$ based on the CFD simulation. The excess thrust in the CFD model is equivalent to a reduction of the thrust input in the TURBOMATCH model (Fig. 10). For the present work, thrust variations between -15% to 15% of the nominal thrust were evaluated (Fig. 10) for mid cruise operating conditions ($F_{N,MC}$) and end of runway operating conditions ($F_{N,TO}$). An almost linear monotonic evolution of the engine design variables is observed with the changes of thrust for both operating conditions (Fig. 10). In addition to the engine operating conditions for the coupled aerodynamic approach, other relevant engine metrics can be extracted based on the changes in the thrust requirements. For example, the fuel flow ($\dot{m}_{fuel}$) and the turbine rotor entry temperature (TRET). The fuel flow is most important under cruise conditions where the engine is operating during most of flight, while the TRET is critical to determine the life of the engine at the most demanding operating conditions such as end of runway.

Based on the one-dimensional engine analysis for this sample engine at mid cruise conditions, the fuel flow ($\dot{m}_{fuel}$) decreases approximately by 0.8% per 1% reduction from the nominal thrust (Fig. 10). This results in a net increase on the Specific Fuel Consumption (SFC), since the thrust decreases at a higher rate than the fuel flow. This is because the changes on thrust are evaluated as off-design points with respect to the reference engine. Future work may involve the optimization of the engine cycle for minimum SFC at cruise at different thrust levels. This enables the introduction of

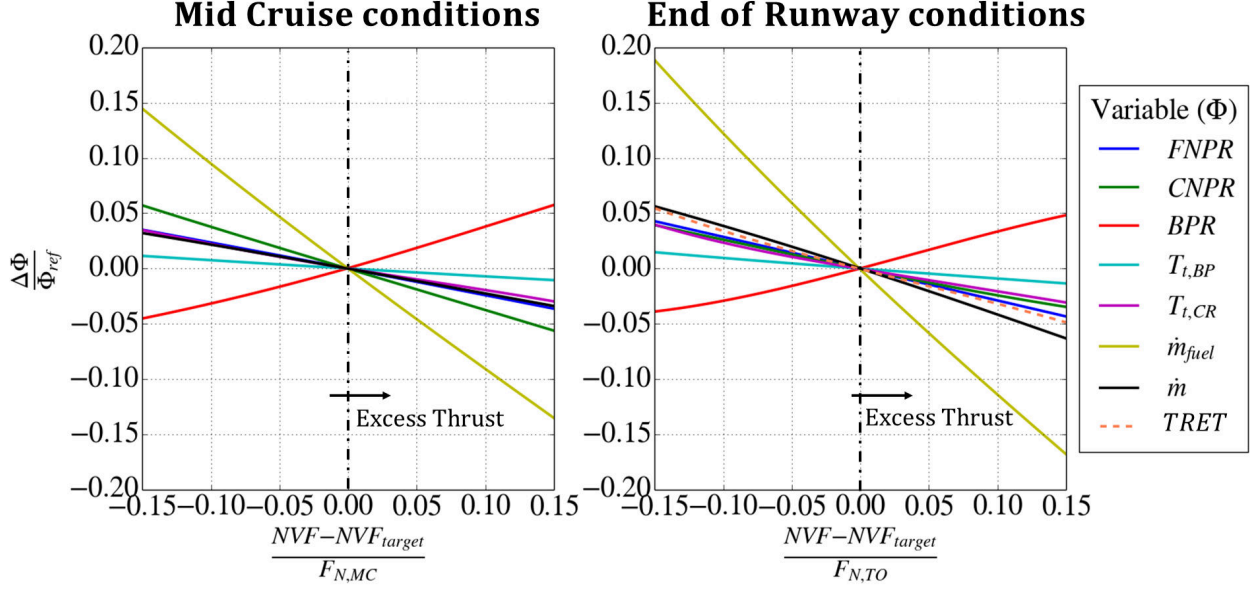


Fig. 10 Changes on engine operating conditions based on the net vehicle force of the CFD approach. Exchange rates of the main cycle variables with respect to the preliminary design variables. Values for Mid Cruise (left), and End of Runway (right) operating conditions

the aerodynamic performance of the propulsion integration as an additional constraint on the engine design process.

At EoR conditions, the metric of interest is the TRET. This variable is linked with the overall life of the engine [35]. For this particular engine model based on the baseline operating point, if the thrust increases by 1% of the nominal thrust at EoR ($F_{N,TO}$), the TRET rises in approximately 0.3%.

V. Mid-cruise operating conditions

A. Demonstration of the coupled aero-propulsive model

The thrust matching approach was applied for a target NVF of zero at mid-cruise conditions (Table 2). The case was started from the nominal thrust requirement at mid-cruise ($F_{N,MC}$). Two additional iterations (Fig. 11) were required to achieve the required tolerance (within $\pm 0.25\%$ of $F_{N,MC}$). The NVF at the final iteration was approximately -0.001% of $F_{N,MC}$. From the initial engine operating condition, the thrust requirement of the powerplant was reduced a $\sim 4.9\%$ of $F_{N,MC}$. This number will vary if a more representative airframe design is applied (e.g., including fairings, horizontal tail). The engine model and the initial thrust prediction will also have a significant impact on this value. In this particular example, there was a particularly large difference between the thrust requirement of the aircraft and the defined preliminary nominal thrust requirement of the powerplant ($F_{N,MC}$). Consequently, some notable changes on the engine operating conditions are obtained from the thrust matching process (Fig. 11). For example, the thrust reduction is equivalent to a $\sim 4.45\%$ reduction on the fuel flow required. It is acknowledged that these variations are not representative of a more refined coupled aero-propulsive system. Nevertheless, this is a successful demonstration of the thrust and lift matching capability developed in this work. This also highlights the potential benefits of including the aerodynamic performance of the powerplant integration into the preliminary design of the engine cycle. Eventually, this could be used to optimise the engine cycle (i.e., minimize the SFC) based on a more representative target thrust level. This could also facilitate a concurrent design approach between the airframe and the powerplant to improve the performance of the final product.

The change in the thrust required at mid-cruise is translated into changes on the engine operating conditions (Fig. 11). For example, the $MFCR$ is reduced from 0.674 at the initial point to 0.666 at the final iteration. Also, the $FNPR$ and the $CNPR$ decrease $\sim 1.2\%$ and $\sim 1.8\%$, respectively. These changes in the engine operating conditions also have a moderate effect in the aerodynamic performance of the housing components (intake, nacelle, and exhaust) and the

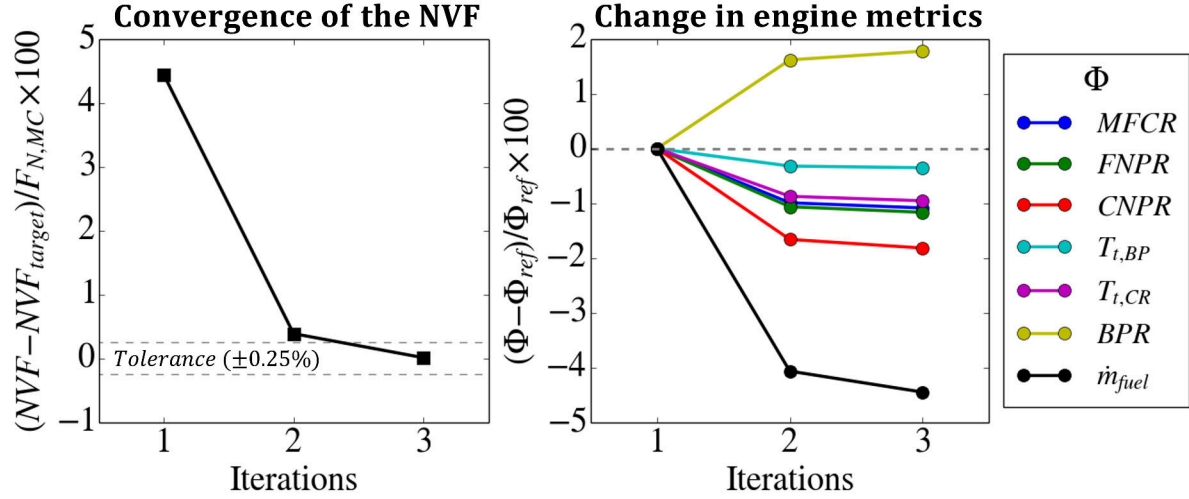


Fig. 11 Convergence of the NVF (left), and change on the engine operating conditions (right) with the thrust matching method at mid cruise operating conditions

airframe (Fig. 12). Nevertheless, the majority of the variation in the NVF during the thrust matching process is due to changes in the GPF^* (Fig. 11), with a reduction of $\sim 4.36\%$ of $F_{N,MC}$ at the final iteration. The thrust trimming has a negligible effect in the drag of the airframe ($D_{A/F}$) and the modified nacelle drag (D_{nac}^*) with a maximum variation from the initial condition of -0.149% and $\sim 0.045\%$ of $F_{N,MC}$, respectively. Thus, the aircraft and powerplant aerodynamics remains relatively insensitive to the changes on the engine operating conditions, and the nacelle is not highly affected by the reduction in the $MFCR$. At the same time, the reduction of the GPF^* also affect the contribution of the airframe and the powerplant to the lift (Fig. 12). The contribution of the GPF^* to the lift decreases by about 0.38% of $F_{N,MC}$. However, this is compensated with the increase on the nacelle and the aircraft lift by the trimming process, with small changes in the angle of attack.

B. Aerodynamic performance at mid-cruise operating conditions

To understand the impact of the powerplant integration with the aircraft on the aerodynamic performance, the free-flying and the isolated powerplant configurations (Fig. 1) were also evaluated at the equivalent engine angle of attack, α_{eng} , (Fig. 4) and engine operating conditions. An up-wash angle of 1° was used, based on the aerodynamics of the HS-CRM airframe in isolation. Over the fan cowl, the pressure distribution changes substantially when the installed powerplant is compared with the free-flying and isolated configurations (Fig. 13). The changes in the aerodynamics of the installed powerplant result from the combination of the installation toe angle, and the aerodynamic interaction between the airframe and the powerplant. A clear difference in the isentropic Mach number distribution is observed between the outboard and inboard side of the fan cowl (Fig. 13). This results in a change on the predicted aerodynamic performance of the powerplant between the installed and the free-flying configurations. These effects are also present for the pylon and the airframe. A shock wave is observed in the inboard side of the pylon. This shock affects the flow field over the pressure side of the airframe and modifies the pressure distribution with respect to the isolated airframe. This results on changes in the airframe drag. At the same time, the presence of the pylon also affects the local aerodynamics over the fan cowl. This can be observed when the free-flying configuration is compared with the isolated powerplant (Fig. 13). The shock wave distribution and intensity are affected by the presence of the pylon and the local blockage effects. Overall, the notable differences in the aerodynamics between the installed powerplant and the other approaches mean that the aerodynamic interaction between the airframe and the powerplant should be considered for the aerodynamic design and evaluation of the propulsion integration.

For this powerplant design, this variation in the engine operating conditions have a moderate effect on the lift and drag (Fig. 12). However, the housing components were not optimised for the updated engine operating conditions. So, further deviations of the original design point could result in penalties in the aerodynamic performance. Thus, for future work it is also of interest to include the optimisation and design of the housing components into this design process.

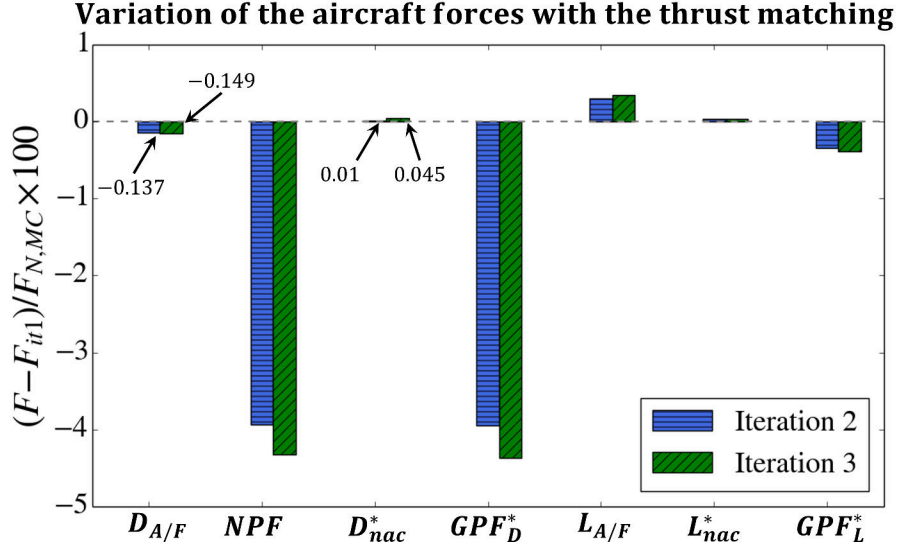


Fig. 12 Changes in the forces over the aircraft with the thrust matching method at the mid cruise operating condition

This requires the optimisation of the installed powerplant. However, this increases the computational cost in comparison with other approaches to optimise the housing components in isolation [21, 22]. Thus, advanced design techniques such as multi-fidelity methods and dimensionality reduction [9] should be explored to enable the concurrent optimisation of the powerplant integration and the engine cycle.

VI. End of runway operating conditions

Based on the preliminary estimation of the end of runway operating conditions (Section IV.B), the lift and thrust matching methodology was applied for a target $NVF|_D$ of 56% of the nominal thrust at take-off ($F_{N,TO}$). From the initial evaluation, two additional iterations were required to achieve the required tolerance of 0.25% of $F_{N,TO}$. For this particular example, an excess in thrust of 0.12% of $F_{N,TO}$ was achieved. From the initial thrust value, the thrust was reduced by approximately a 3.43% of $F_{N,TO}$. Based on the preliminary thrust values, this reduces the maximum TRET in about 0.9%. It is acknowledged that this value is particularly large, and it is not representative of a more refined thrust matching of the powerplant. This large imbalance results from an overestimation of the preliminary airframe drag and engine thrust requirements at EoR. Nevertheless, despite the initial overestimation of the thrust requirement at EoR, this is a successful demonstration of the thrust and lift matching approach with the coupled engine model under take-off conditions. While the required thrust and TRET will vary if a more representative airframe is applied (e.g., a more detailed definition of the landing gear drag, and the flap and slat fairings), the current reduction in the thrust requirement and TRET highlight the advantages of including a coupled aero-propulsive approach on the engine design process. The thrust and lift matching methodology could be applied to different critical off-design conditions, such as higher take-off altitudes, higher altitudes, or shorter runway lengths; in order to determine the operational limits of the engine based on the aerodynamic performance of the powerplant integration.

The reduction in the thrust of the powerplant was achieved by moderate changes in the engine operating conditions. The $MFCR$ decreases in $\sim 1.2\%$ of the initial value. At the same time the $FNPR$ and the $CNPR$ decrease in about 0.9% and 0.7%, respectively. This leads to a notable reduction of the GPF^* to match the target NVF (Fig. 14). The airframe and the nacelle drag remain relatively unaltered with an increase of about 1.1% of the $F_{N,TO}$ from the initial point. At the same time, the reduction in GPF^* results in a reduction of the propulsive force to the aircraft lift (Fig. 14). This is compensated by an increase on the airframe lift with small changes in $\alpha_{A/F}$. The nacelle drag remains virtually unaltered during the thrust matching process.

The free-flying and the isolated powerplant configurations (Fig. 1) were also evaluated at the equivalent engine angle of attack, α_{eng} , (Fig. 4) and engine operating conditions. At EoR conditions, an up-wash angle of 5° was

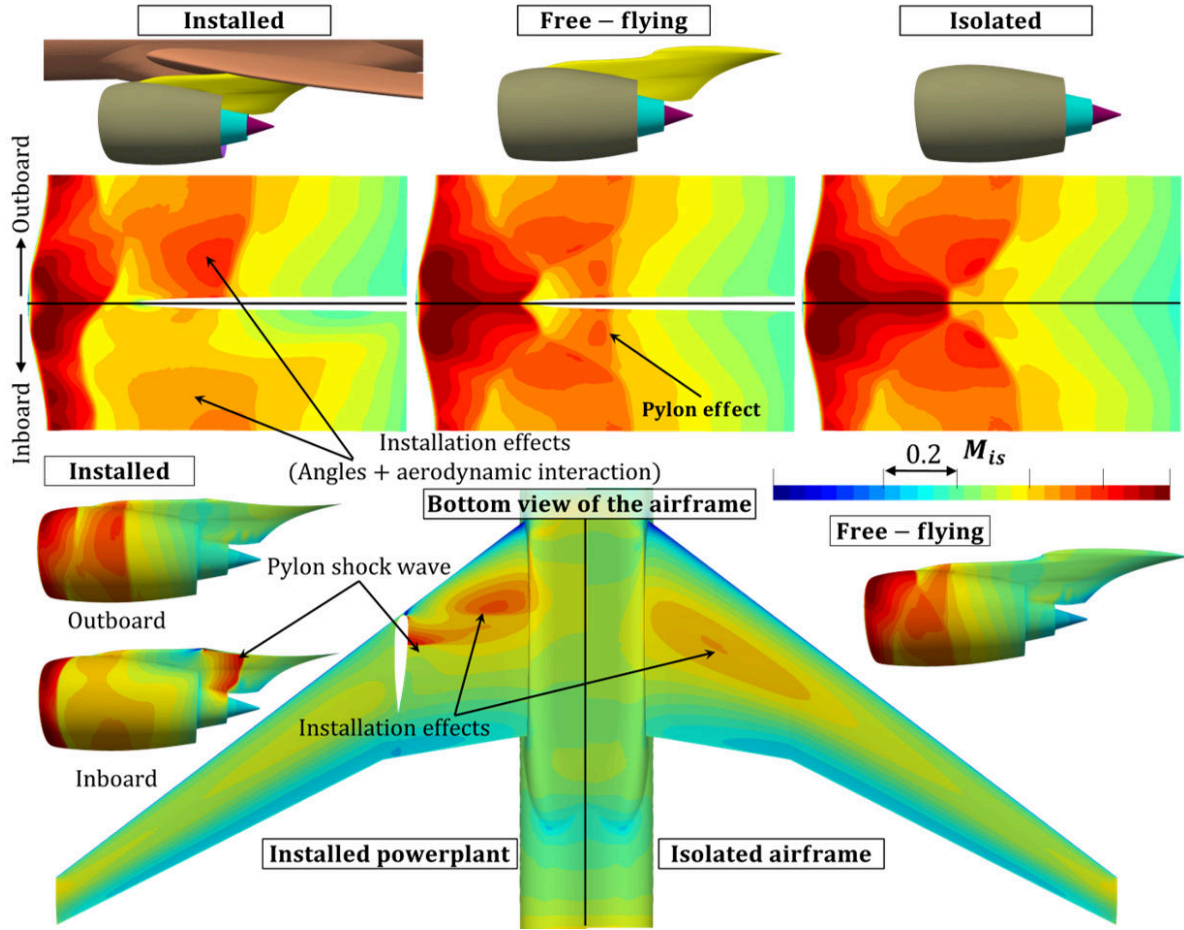


Fig. 13 Isentropic Mach number over the HS-CRM airframe (bottom view) with and without the powerplant installed, and over the nacelle for the three powerplant configurations: installed, free-flying, and isolated. Nacelle surface represented in an unwrapped manner. Mid cruise conditions applied with a fixed angle of attack of 2.0°

used for the free-flying and isolated configurations based on the aerodynamics of the isolated HL-CRM-TO airframe. The pressure distributions over the fan cowl change significantly when the installed powerplant is compared with the free-flying and isolated configurations. As a consequence, a notable change in the contribution of the pressure force to the thrust and drag is observed between the inboard and the outboard side of the fan cowl (Fig. 15). The pressure force contribution to the thrust direction increases on the nacelle afterbody in the inboard side. This is due to the aerodynamic interaction between the powerplant and the airframe, and the installation toe angle. The thrust contribution from the nacelle forebody of the installed powerplant increases with respect to the free-flying and isolated cases, due to the local increase in the suction. This is due to the effect of the flow circulation of the wing and the slats. In the free-flying and isolated models, the up-wash angle was accounted based on the isolated HL-CRM-TO fuselage. However, this preliminary analysis does not fully capture the aerodynamic interaction effects that result from the fully installed model. Similarly, the presence of the powerplant modifies the local contribution of the pressure forces to the airframe drag (Fig. 15). This also affects the overall force balance of the aircraft and powerplant system under take-off conditions. Thus, for the aerodynamic analysis of the powerplant in take-off conditions such as the end of runway, it is necessary to evaluate the powerplant installed into the airframe.

In addition to the changes in the forces over the airframe and the powerplant, the installed configuration also captures relevant aerodynamic features that are neglected in the free-flying and isolated models (Fig. 16). This is another reason to evaluate the aerodynamics of the powerplant installed into the airframe under high-lift conditions, rather than in isolation. For example, a corner flow separation emerges in the inboard side of the installed nacelle at the joint

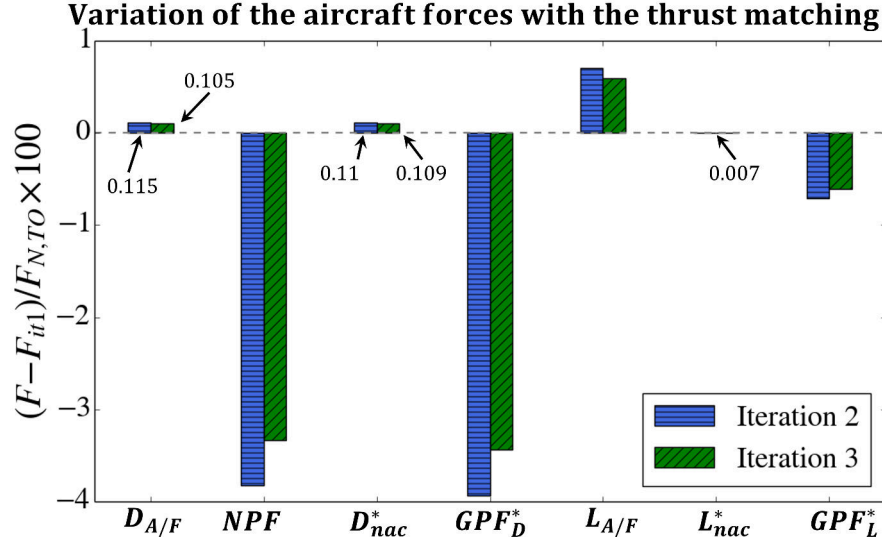


Fig. 14 Changes in the forces over the aircraft with the thrust matching method at the end of runway operating condition

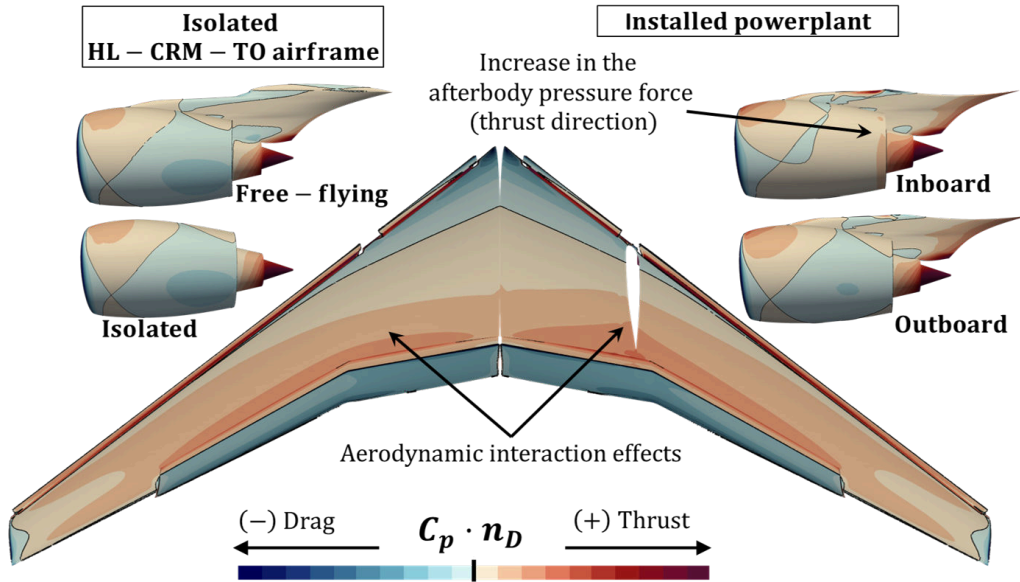


Fig. 15 Pressure force projected on the drag axis of the airframe. Distributions over the powerplant for the installed, free-flying and isolated cases; and over the wings for the installed powerplant and the isolated HL-CRM-TO airframe. End of runway operating conditions after thrust matching

with the pylon. This feature is not captured with the isolated and free-flying approaches. This inboard separation is a consequence of the aerodynamic interaction with the inboard slat, which increases the pressure at the nacelle afterbody and generates a local adverse pressure gradient. Also, the powerplant interaction with the airframe leads to local changes in the flow over the wing, when compared with the isolated HL-CRM-TO airframe (Fig. 16). There is a local reduction of the local wall shear stress over the wing leading edge in the installed configuration. This is due to local changes in the flow acceleration due to the presence of the powerplant. At the same time, the interaction between the pylon and the leading edge of the wing results in a local separation near the intersection. This separation affects the local

aerodynamics over the powerplant and the wing.

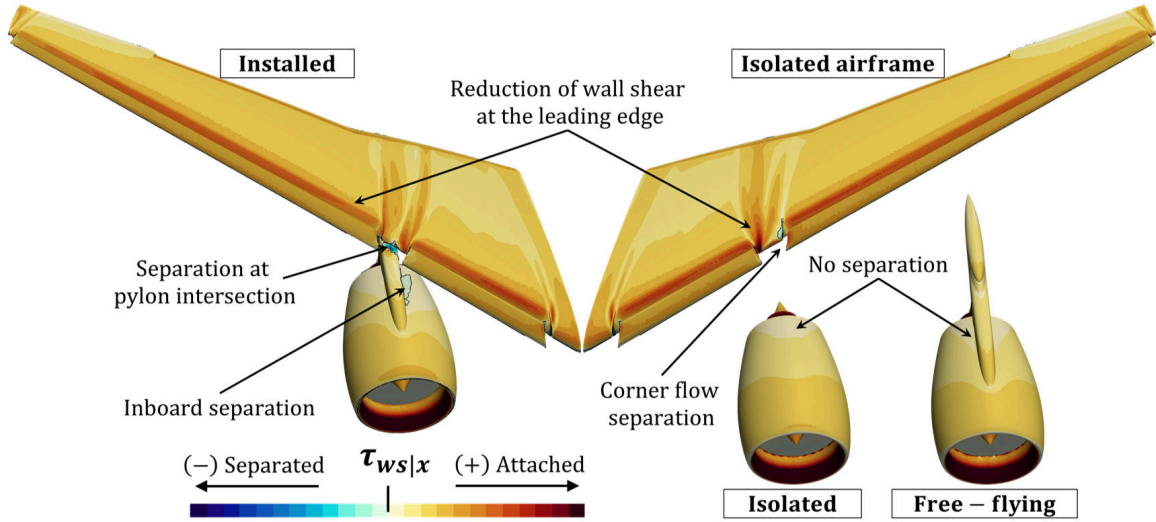


Fig. 16 Axial wall shear stress over the HL-CRM-TO wing for the installed and the isolated airframe configurations, and over the powerplant for the installed, free-flying and isolated cases. End of runway operating conditions after thrust matching

VII. Conclusions

A methodology to evaluate the aerodynamic performance of installed powerplants with a coupled propulsion model was developed. This included a thrust and lift matching capability. To demonstrate this capability, the aerodynamics of an installed ultra-high bypass ratio powerplant have been studied under mid-cruise and end of runway operating conditions. A CFD approach for the systematic evaluation of the aerodynamic performance of the propulsion integration at high-speed and high-lift conditions was defined. This included the study of the powerplant installed into the airframe as well as the airframe and the powerplant in isolation. The NASA Common Research Model (CRM) was used as the reference airframe, with the high-speed (HS-CRM) version of the airframe used for mid cruise conditions and the high-lift configuration for take-off (HL-CRM-TO) was used for the end of runway conditions.

A one-dimensional engine model was coupled with the aerodynamic approach. This model was based on a two-spool, ultra-high bypass ratio, geared turbofan. The initial thrust requirements at mid cruise and take-off were estimated based on sample top-level aircraft requirements for a long-range wide body airframe, such as the NASA CRM. The engine operating conditions were obtained for a range of thrust requirements at mid cruise and take-off, in order to correct the NVF of the aircraft in the CFD model. A structured methodology to match the lift and thrust requirements of the aircraft was developed based on the coupled aero-propulsive model. The engine model was also used to evaluate the variation of metrics of interest for the engine with the thrust, such as the fuel flow at cruise and the turbine rotor entry temperature (TRET) at end of runway conditions. As a proof of concept, the coupled aero-propulsive model was applied to study the aerodynamic performance of the installed powerplant at mid cruise and end of runway conditions. While similar approaches have been applied under cruise conditions, to the authors' knowledge this is the first time this approach has been also applied under take-off conditions.

The aerodynamics of the installed powerplant were compared with the aerodynamics of the airframe and the powerplant in isolation in order to understand the effects of the powerplant integration with the airframe. Notable changes in the flow topology were observed between the installed and isolated approaches due to the aerodynamic interaction between the engine and the airframe at mid-cruise and end of runway conditions. These changes result in notable changes in the predicted aerodynamic performance of the powerplant. Thus, to analyse the aerodynamic performance of the powerplant, a model that includes the installed powerplant into the airframe should be used.

As a preliminary evaluation of the effects of the powerplant integration under high-lift conditions, the lift profile of the aircraft at end of runway conditions was obtained for different angles of attack up to the airframe stall. The

lift profiles of the installed powerplant model were compared with the lift of the HL-CRM-TO airframe in isolation. The installed powerplant configuration had a significant higher lift than the isolated airframe near the stall point. This increase in lift is a consequence of the lift contribution of the nacelle and the aerodynamic interaction effects between the airframe and the powerplant.

Acknowledgments

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Data availability

Due to commercial confidentiality agreements the supporting data is not available.

References

- [1] Commission, E., for Mobility, D.-G., Transport, for Research, D.-G., and Innovation, *Flightpath 2050 : Europe's vision for aviation : maintaining global leadership and serving society's needs*, Publications Office, 2011. <https://doi.org/doi/10.2777/50266>.
- [2] 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>, URL <https://arc.aiaa.org/doi/10.2514/2.5852>.
- [3] Daggett, D. L., Brown, S. T., and Kawai, R. T., "Ultra-efficient Engine Diameter Study (No. NASA/CR—2003-212309)," Tech. rep., NASA, 2003.
- [4] Peters, A., "Ultra-short nacelles for low fan pressure ratio propulsors," Phd thesis, Massachusetts Institute of Technology, 2014.
- [5] Tejero, F., Robinson, M., MacManus, D. G., and Sheaf, C., "Multi-objective optimisation of short nacelles for high bypass ratio engines," *Aerospace Science and Technology*, Vol. 91, 2019, pp. 410–421. <https://doi.org/10.1016/j.ast.2019.02.014>, URL <https://doi.org/10.1016/j.ast.2019.02.014>.
- [6] Tejero, F., MacManus, D., Matesanz García, J., Swarthout, A., and Sheaf, C., "Towards the design and optimisation of future compact aero-engines: intake/fan cowling trade-off investigation," *International Journal of Numerical Methods for Heat & Fluid Flow*, 2022. <https://doi.org/10.1108/HFF-06-2022-0366>, URL <https://www.emerald.com/insight/content/doi/10.1108/HFF-06-2022-0366/full/html>.
- [7] Goulos, I., Stankowski, T., Otter, J., MacManus, D., Grech, N., and Sheaf, C., "Aerodynamic Design of Separate-Jet Exhausts for Future Civil Aero-engines - Part I: Parametric Geometry Definition and Computational Fluid Dynamics Approach," *Journal of Engineering for Gas Turbines and Power*, Vol. 138, No. 8, 2016, pp. 1–14. <https://doi.org/10.1115/1.4032649>.
- [8] Goulos, I., Otter, J., Tejero, F., Hueso Rebassa, J., MacManus, D., and Sheaf, C., "Civil turbofan propulsion aerodynamics: Thrust-drag accounting and impact of engine installation position," *Aerospace Science and Technology*, Vol. 111, 2021, p. 106533. <https://doi.org/10.1016/j.ast.2021.106533>, URL <https://linkinghub.elsevier.com/retrieve/pii/S1270963821000456>.
- [9] Tejero, F., MacManus, D., Hueso-Rebassa, J., Sanchez-Moreno, F., Goulos, I., and Sheaf, C., "Aerodynamic optimisation of civil aero-engine nacelles by dimensionality reduction and multi-fidelity techniques," *International Journal of Numerical Methods for Heat & Fluid Flow*, 2022. <https://doi.org/10.1108/HFF-06-2022-0368>, URL <https://www.emerald.com/insight/content/doi/10.1108/HFF-06-2022-0368/full/html>.
- [10] Swarthout, A., MacManus, D., Tejero, F., Matesanz-García, J., Boscagli, L., and Sheaf, C., "A Comparative Assessment of Multi-Objective Optimisation Methodologies for Aero-Engine Nacelles," *33rd Congress of the International Council of the Aeronautical Sciences (ICAS)*, Stockholm, Sweden, 2022.
- [11] Gray, J. S., Mader, C. A., Kenway, G. K., and Martins, J. R., "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>.
- [12] Yildirim, A., Gray, J. S., Mader, C. A., and Martins, J. 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>, URL <https://arc.aiaa.org/doi/10.2514/1.C036103>.

- [13] Abdul Kaiyoom, M. A. S., Yildirim, A., and Martins, J. R., "Coupled Aeropropulsive Design Optimization of an Over-Wing Nacelle Configuration," *AIAA SCITECH 2023 Forum*, American Institute of Aeronautics and Astronautics, National Harbor, MD & Online, 2023. <https://doi.org/10.2514/6.2023-0327>, URL <https://arc.aiaa.org/doi/10.2514/6.2023-0327>.
- [14] Sanchez Moreno, F., MacManus, D., Hueso Rebassa, J., Tejero, F., and Sheaf, C., "Optimization of installed compact and robust nacelles using surrogate models," *33rd Congress of the International Council of the Aeronautical Sciences*, ICAS, Stockholm, Sweden, 2022.
- [15] Rao, A. N., Goulos, I., and MacManus, D. G., "Impact of installation on a civil large turbofan exhaust system at idle descent conditions," *Aerospace Science and Technology*, Vol. 119, 2021, p. 107125. <https://doi.org/10.1016/j.ast.2021.107125>, URL <https://linkinghub.elsevier.com/retrieve/pii/S1270963821006350>.
- [16] Subbian, G., Magrini, A., Buosi, D., Radespiel, R., Ponza, R., and Benini, E., "Investigation of HL-CRM Aerodynamics With a UHBPR Nacelle in Powered-on Condition," *AIAA Propulsion and Energy 2021 Forum*, American Institute of Aeronautics and Astronautics, Virtual Event, 2021. <https://doi.org/10.2514/6.2021-3547>, URL <https://arc.aiaa.org/doi/10.2514/6.2021-3547>.
- [17] Subbian, G., Magrini, A., Benini, E., Buosi, D., Ponza, R., and Radespiel, R., "RANS Analysis of HL-CRM at Take-off and Landing Configurations with different Flap Deflections and Engine Settings using DLR-TAU Code," *AIAA SCITECH 2022 Forum*, American Institute of Aeronautics and Astronautics, San Diego, CA & Virtual, 2022. <https://doi.org/10.2514/6.2022-0048>, URL <https://arc.aiaa.org/doi/10.2514/6.2022-0048>.
- [18] Vassberg, J., Dehaan, M., Rivers, M., and Wahls, R., "Development of a Common Research Model for Applied CFD Validation Studies," *26th AIAA Applied Aerodynamics Conference*, American Institute of Aeronautics and Astronautics, Honolulu, Hawaii, 2008. <https://doi.org/10.2514/6.2008-6919>, URL <https://arc.aiaa.org/doi/10.2514/6.2008-6919>.
- [19] Lacy, D. S., and Clark, A. M., "Definition of Initial Landing and Takeoff Reference Configurations for the High Lift Common Research Model (CRM-HL)," *AIAA AVIATION 2020 FORUM*, American Institute of Aeronautics and Astronautics, Virtual Event, 2020. <https://doi.org/10.2514/6.2020-2771>, URL <https://arc.aiaa.org/doi/10.2514/6.2020-2771>.
- [20] NASA.gov, "4th AIAA CFD Drag Prediction Workshop," , 2009. URL <https://aiaa-dpw.larc.nasa.gov/Workshop4/workshop4.html>.
- [21] Tejero, F., MacManus, D. G., Sanchez-Moreno, F., and Sheaf, C., "Neural network-based multi-point, multi-objective optimisation for transonic applications," *Aerospace Science and Technology*, Vol. 136, 2023, p. 108208. <https://doi.org/10.1016/j.ast.2023.108208>, URL <https://linkinghub.elsevier.com/retrieve/pii/S1270963823001050>.
- [22] Goulos, I., Otter, J., Stankowski, T., MacManus, D., Grech, N., and Sheaf, C., "Design optimisation of separate-jet exhausts for the next generation of civil aero-engines," *Aeronautical Journal*, Vol. 122, No. 1256, 2018, pp. 1586–1605. <https://doi.org/10.1017/aer.2018.95>.
- [23] MIDAP Study group, "Guide to in-Flight Thrust Measurement of Turbo- jets and Fan Engines. Agardograph No. 237, AG-237," Tech. rep., AGARD (Advisory Group for Aerospace Research and Development), Neuilly sur Seine, France, 1979.
- [24] ANSYS INC, "ANSYS Fluent User's Guide. Release 19.1," Tech. rep., ANSYS Inc, Canonsburg, PA, USA, 2019. URL ansyshelp.ansys.com.
- [25] Wilcox, D. C., *Turbulence Modelling for CFD*, 3rd ed., DCW Industries, Inc, San Diego, California, 2006.
- [26] ISO International Standard 2533-1975, "Standard Atmosphere First Edition," Tech. rep., ISO, Geneva, Switzerland, 1978.
- [27] Hueso-Rebassa, J., "Design, Performance and Aerodynamics of Non-Axisymmetric Exhaust Systems for Civil Aero-Engines," Phd thesis, Cranfield University, 2022.
- [28] 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, Transactions of the ASME*, Vol. 130, No. 7, 2008, pp. 0780011–0780014. <https://doi.org/10.1115/1.2960953>.
- [29] MacMillan, W. L., "Development of a modular type computer program for the calculation of gas turbine off design performance," Phd thesis, Cranfield University, 1974.
- [30] Jobe, C. E., "ADA326842. Prediction of Aerodynamic Drag," Tech. rep., Air Force Wright Aeronautical Labs, Patterson AFB, OH, 1984. URL <https://apps.dtic.mil/sti/citations/ADA326842>.
- [31] Anderson, J., *Aircraft performance & design*, international ed., McGraw-Hill, London, UK, 1999.

- [32] Airbus S.A.S, “A350 - Aircraft characteristics airport and maintenance planning. AC. Revision No 10 - Jul 01/21,” Tech. rep., Airbus, Blagnac, France, 2021.
- [33] Airplanes, B. C., “777-9 Airplane Characteristics for Airport Planning. D6-86073. Revision E,” Tech. rep., Boeing Commercial Airplanes, Seal Beach, CA, 2023.
- [34] Burden, R. L., and Faires, J. D., *Numerical Analysis*, 9th ed., Brooks/Cole, Cengage Learning, 2010.
- [35] Cumpsty, N., and Heyes, A., *Jet Propulsion. A simple guide to the aerodynamics and thermodynamic design and performance of jet engines*, 3rd ed., Cambridge University Press, New York, 1998.



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