BODY-FORCE MODELLING OF MULTIPLE DISTRIBUTED PROPULSORS WITH BOUNDARY LAYER INGESTION

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ABSTRACT
A coupled wing-propulsor configuration is modelled in order to extract the coupled lift, drag or thrust coefficients and to investigate a methodology for in-flight energy recovery via loaded windmilling. The baseline wing geometry and flight conditions of the configuration are representative of the Daher TBM900 aeroplane. The propulsors are modelled using RANS Body Force Modelling (BFM) in order to analyse the non-homogenous incoming flow at computation cost somewhat coherent with preliminary design. The main coupled phenomena such as Boundary Layer Ingestion (BLI), suction-side flow acceleration, propulsor-to-propulsor influence, lateral loads and increased operability are discussed; numerical modifications to the baseline Hall-Thollet model are introduced, providing control-by-power of the propulsor. In continuation of previous in-house efforts, the simulations were carried out for a typical takeoff case. The obtained results provide preliminary evidence for the possibility of energy recovery by loaded windmilling, which lays groundwork for further investigations at other flight conditions and more complex configurations.

KEYWORDS
Body-Force Modelling, Aero-Propulsive Performance, Preliminary Design, Boundary-Layer Ingestion

NOMENCLATURE
BFM : Body-Force Modelling
\( \rho \) : Fluid density
\( \vec{V} \) : Velocity
\( \Omega \) : Rotational speed
\( \vec{F} \) : Force produced by pressure and viscosity
\( \vec{T} \) : Thrust

INTRODUCTION
The growing demand for environmentally-acceptable means of transportation has been driving the aeronautical industry actors to investigate a variety of technological concepts beyond the conventional "Tube and Wing (with podded gas-turbine engines)" paradigm. (Kellari, Crawley, and Cameron 2017) Two disruptive ideas are Boundary Layer Ingestion (BLI) and Distributed Propulsion (DP). (Bijewitz, Seitz, and Hornung 2016) While the two are a priori separate concept families, they are often employed together since the DP involves a greater number of small propulsors whose distribution over the airframe can enable different levels of absorption of portions of the upstream boundary layer. Moreover, vehicle-level performance gains from aero-
propulsive synergies can potentially contribute to offsetting the penalising effects of the (hybrid-)electric propulsive systems (Kim, Perry, and Ansell 2018) which are often regarded as enabler for zero-emissions flying, but whose power densities still make them prohibitive for large-scale airborne applications. To carry such philosophy of functional synergies a step further - in addition to enabling aerodynamic vehicle-level gains, the DP could also be functionally related to the aeroplane control and stability characteristics. Given that the DP propulsors would likely be powered by an electrically distributed power system, the "one engine inoperative" failure mode would not necessarily be the relevant limiting sizing case anymore.

Furthermore, in the previous work by the current authors, where the contribution presented in this paper was firstly demonstrated (Benichou et al. 2020), a notable behaviour was observed. Simulations indicated that distorted flow at the inlet of a BLI distributed propulsor going through the propulsor assembly results in a lateral force component at the propulsor nozzle. While already by itself this is a valuable observation, it does not provide any insight into how such emergent effects could be fully leveraged with multi-propulsor arrangements, notably with the DP which intrinsically relies on various layouts of two or more adjacent propulsors under mutual influence.

If the most beneficial concepts are to be revealed to the designer, it is crucial to render the possible system synergies transparent in early design. That is, the preliminary-design level methods need to contain all the necessary information to capture the complex correlations such as the ones described previously. However, the sheer number of the different ways one could configure airframe aerodynamics with distributed propulsion and various types of hybrid-electric propulsive architectures over different flight profiles opens a tremendously big design space. Confident discrimination between different possible solutions within huge possibility space requires very fast computing methods. The authors have been developing one such low-fidelity method for aero-propulsive conceptual design (Bommidala et al. 2022), but due to lack of real-life experience to validate and/or calibrate the models - intermediary solutions are sought. One such approach is the one presented in the current paper.

In particular, we present a Body Force Modelling (BFM) approach, employed for modelling propulsors of an aero-propulsive assembly. This choice allows for an acceptable trade-off between computation costs and fidelity of the results. The BFM methods date back to the 1990s; they consist of replacing the blades by their mean azimuthal effect on the flow. The modelling used in this paper is taken from Hall and Thollet (Thollet 2017).

The case study presented in this article was carried out on an aeroplane model similar to a Daher TBM900. In particular, the explored propulsive concept was developed by replacing the single nose-mounted propeller with eight smaller ducted fans - four on each wing - mounted on the suction side at the trailing edge. Such setup enables studying the coupling between the propulsor behaviour and the wing aerodynamics, along with the coupling between neighbouring propulsors. To that end, the article is elaborated as follows: the first chapter presents the theoretical background for the models employed in this study; then, the details of the study at hand is provided; this is followed by the summary of the results for the different investigated operating scenarios.

THEORETICAL PRINCIPLES

Body-Force Modelling

The core idea of Body-Force Modelling (BFM) is to substitute the physical presence (i.e. explicit model) of the blades with volume sources that represent the mean azimuthal effect that
the blades exert on the flow. (Fig. 1) With this method all the phenomena of length scale lower than the blade-to-blade distance are not resolved. However, it allows for much lighter simulations since the need to mesh the turbomachine blade rows is eliminated. In BFM, the source terms depend on both local flow and local geometry of the blade. This allows for the simulation of non-homogeneous flows and their interaction with the propulsors without resorting to URANS simulations, which is why this approach was chosen to conduct the study. Further insight into expressions for the source terms can be found in the literature, see Thollet et al. 2016, Reichstein 2009, Hale and OBrien 1997 and Gong 1999.

![Figure 1: Illustration of the BFM: the blade passage is modelled by its mean effect on the flow. (Dufour and Thollet 2016)](image)

The formulation used in the paper was developed by Thollet (Thollet et al. 2016), who had in turn improved on model by Hall et al. (Hall, Greitzer, and Tan 2016):

\[
\frac{\partial \rho}{\partial t} + \nabla \cdot (\rho \vec{V}) = B \tag{1}
\]

\[
\frac{\partial \rho \vec{V}}{\partial t} + \nabla \cdot (\rho \vec{V} \cdot \nabla) + \nabla P = \rho(\vec{f}_n + \vec{f}_p) + B \vec{V} \tag{2}
\]

\[
\frac{\partial \rho e_t}{\partial t} + \nabla \cdot (\rho h_t \vec{V}) = E_{source} + B h_t \tag{3}
\]

Where the different terms are described as follows:

- **B** = \(-\frac{1}{b}(\rho \vec{V} \cdot \nabla b)\) is the mass source term
- **\vec{f}_n** = \(K_{Mach} \frac{1}{2} W^2 2\pi \frac{\delta}{\pi h [\eta]}\) is the normal force applied to the flow, with a corection term \(K_{Mach}\) for compressible effects
- **\vec{f}_p** = 0.0592 \(Re^{-0.2} \frac{W^2}{\delta h [\eta a]}\) is the parallel force, corresponding to losses and viscosity
- **E_{source}** = \(\rho r \Omega (f_n, \theta + f_p, \theta)\) is the volume energy source, derived from Euler’s theorem
- **b** is the blockage term defined as shown on the right of figure 1

In this formulation of the BFM, only the geometry of the blade is necessary to create the source terms, no previous CFD simulations are needed to extract coefficients. These source terms depends on the blockage factor \(b\) and the normal vector on the surface of the simulated blade.
Windmilling and Control by Power

As the above equations indicate, the main way to control the body forces at the user’s disposal is to vary the rotation speed \( \Omega \), which will also control the relative velocity \( \vec{W} \). However, in the aeroplane preliminary design and power consumption characterisation of the whole system, it is more relevant to control the power delivered to the propulsor. Indeed, in the broader framework of the presented study, the goal was to determine if the baseline TBM900 aeroplane retrofitted with a wing-mounted BLI DP concept would be able to carry out a standard mission with the same power and energy characteristics as the baseline aeroplane.

Using an analysis by Dufour (Dufour and Thollet 2016), an angular momentum based equation can be derived to describe the evolution of the rotational speed based on the power received by the propeller:

\[
\frac{d\Omega}{dt} = \frac{\dot{W}_{fan} - \dot{W}_{fluid}}{\Omega J_\Theta}
\] (4)

Where the term \( J_\Theta \) represents the inertia of the rotating parts. The above differential can be approximated as follows:

\[
\Omega_{n+1} = \Omega_n - \frac{\delta t}{J_\Theta \Omega_n} (\dot{m} \Delta h_t - \dot{W})
\] (5)

The relaxation term needs to be physical in the case of an unsteady simulation, but can be regarded as a purely numerical constant otherwise:

\[
\Omega_{n+1} = \Omega_n - K (\Delta h_t - \frac{\dot{W}}{\dot{m}})
\] (6)

Where \( K \) is the aforementioned relaxation factor that was tuned for the convergence of the calculation (here around 0.005). The simulation does not appear to be too sensitive to this value. \( \dot{W} \) is the power command for the propulsor (positive for a net increase of the energy in the rotor), \( \Delta h_t \) is the computed enthalpy rise through the rotor, and \( \dot{m} \) is the mass flow. Thus, if the power command is less than the actual energy output, the rotational speed will decrease until both terms are equal. By assigning null or negative value to \( \dot{W} \), free and loaded windmilling situations can be attained respectively.

STUDY SETUP

Aero-Propulsive Assembly Description

The presented aero-propulsive assembly was conceived to be representative of a concept that could be envisioned for a small aeroplane like the TBM900. The original MS-0313 wing root profile of the TBM900 was kept and extruded to make a constant-section basic wing structure for the current model. The current aero-propulsive assembly design employs 8 propulsors - 4 on each wing - whose total combined power would match the power provided by the Pratt&Whitney PT6 engine to the baseline aircraft. The propulsor is composed of a rotor and a stator designed by an in-house program whose purpose is to generate basic blades that generate one eighth of the thrust of the baseline aeroplane whilst consuming one eighth of its power.

The resulting unitary aero-propulsive segment is shown in Fig. 2; the base profile chord is 2m long and the segment is 0.375m wide. It was designed in order to enable modular stacking of as many of these segments as necessary, to conduct studies such as the one described in the following paragraph. As such, the nacelle and the wing are symmetrical with respect to the
center plane, and the walls have continuous tangency constraints in order to avoid sharp angles and corners.

![Figure 2: Left: Visualisation of the investigated aero-propulsive assembly consisting of two identical unitary segments; Right: Description of the different coordinate systems for the analysis of the aero-propulsive coupling (Benichou et al. 2020).](image)

**Numerical Setup**

The simulations were carried out using StarCCM+ 13.04. This software was chosen for two main reasons. Firstly, the versatility of its unstructured mesher allows meshing of complex 3D shapes such as e.g. the transition between the nacelle, the propulsor and the wing for the current case. However, this capability comes at the cost of controllability, i.e. it is more difficult to have control over the mesh in the geometrically complicated areas (such as the junction between the propulsor and the wing) without a drastic increase the number of cells. Secondly, the simple capability of addition of User Field Functions that compute custom scalar or vector fields facilitates creation of the source terms needed in the Body Force Modelling.

All simulations were done using a 3D RANS steady solver with a second order implicit scheme; the turbulent flow was simulated using the $k - \omega$ SST model with the second order Gamma-ReTheta transition. The air was modeled as an ideal gas following Sutherland’s law for the viscosity. Since capturing the interaction between the propulsor and the boundary layer is one of the main objectives of this study, the mesh was constructed in order to have a $y^+ \sim 1$ on every wall in the domain.

Given that the studied configuration is a combination of a propulsor and a wing, the fluid domain needs to have a large extension in order to capture the jet and the aerodynamic forces on the physical (airframe) surfaces. It was chosen to have a rectangular domain with 5 chords upstream, above and below, and 20 chords downstream of the aero-propulsive assembly. For the single-propulsor configuration, the width is equal to 0.375m, which corresponds to the diameter of the fan and its cowling. Such simulations are composed of 4 domains: the rotor, the stator, the regions in-between the two, and the exterior. The source terms described above were applied in the rotor and stator domains (red and dark blue zones depicted in figure 3 as well as the yellow zones in figure 2). On the lateral boundaries of the domain, a periodic condition was applied. Thus, the simulation is analogous to an infinite wing with an infinite number of propulsors integrated on the suction side. For reference, the total number of cells is approximately 17 million and the computational cost is around 2000 CPU hours for a single operating point simulation on one single-segment. In comparison with a equivalent URANS case, the cell number is much smaller in the bladed area while everywhere else the number of cells is approximately the same. The number of CPU hours is considerably smaller than for an equivalent URANS
case. A more comprehensive description of the meshing method and criteria can be found on the previous paper from the authors (Benichou et al. 2020).

For the bi-propulsor simulations, the domain width was doubled in order to fit the two segments. Consequently, this simulation consisted of 7 domains shown on figure 3: 2 rotors in dark blue, 2 stators in red, 2 intermediate spaces in yellow and 1 exterior domain in light blue. The number of cells and the computational cost are doubled with respect to the single-segment case.

![Figure 3: Visualisation of the mesh on the $X_1Z_1$ plane at the center of the propulsor.](image)

**Conservativity Correction**

An issue that might arise is the non-conservation of the mass flow across the rotor and the stator. Indeed, the mass source term $B$ from Eq.1 has theoretically a null global production, but the discretisation can result in an imbalance and the creation (or destruction) of mass flow. During the early simulations, the exiting mass flow was around 1% higher than the one at the inlet. In order to correct this numerical error, the term $B$ was slightly modified as follows:

$$B_B = B - \frac{1}{V} \int_V B dV$$

Where $V$ is the volume of the space in which the source terms are applied. The goal was to subtract the mean value of the mass flow production in the domain (rotor or stator) from the local value of $B$. By construction, the total contribution would be naught and the conservativity would now be within 0.01% of the mass flow. This modification effectively changes the geometry of the blade by increasing (or decreasing) its thickness. Since the term $\frac{1}{V} \int_V B dV$ is very small, the overall shape of $B_B$ is very similar to the original $B$ and it does not impact the simulations results.

**Lift, Drag and Thrust**

In order to analyse and assess the performance of the aero-propulsive configuration at hand, a classical resultant force decomposition into independent lift, drag and thrust contributions is no longer relevant. The nature of the forces that are applied on this segment is roughly three-fold: pressure forces, viscous forces, and momentum increase due to the work added by the propulsor. The first two contributions can be obtained by integrating the pressure and the friction on all wet surfaces noted $\overline{S_F}$ (Eq.8), while the third one comes from the integration of the generated thrust between the rotor inlet and the stator outlet noted $\overline{S_T}$ (Eq.9). Indeed, in BFM, the blades are not physically present in the fluid domain. As such, the variations of
momentum they subject the flow to can only be obtained by integrating the dynalpy between the inlet and the outlet of the propulsor. The resulting forces are then decomposed as a streamwise and upward-oriented stream-normal component (relative to the free-stream flow), in order to draw a comparison with the usual lift and drag of a wing (AGARD 1979) as shown in Figure 2.

The described force decomposition is given in the following equations:

\[
T = \int_{S_F} (p \nabla \cdot \nabla t + (p - p_\infty))dS_T
\]  \hspace{1cm} (9)

From the above, it follows that the streamwise component can be negative, meaning that the segment accelerates in forward direction by producing more thrust than drag. The coefficients (Eqs.10 and 11) are normalised conventionally using the dynamic pressure and the reference surface area \(S_{\text{ref}}\) defined as a rectangular surface spanned by the chord and the width of the aero-propulsive segment (or double the width for the bi-propulsor simulations).

\[
C_L = \frac{(\vec{F} + \vec{T}) \cdot \vec{Y}_0}{\frac{1}{2} \rho_\infty V_\infty^2 S_{\text{ref}}}
\]  \hspace{1cm} (10)

\[
C_D = \frac{(\vec{F} + \vec{T}) \cdot \vec{X}_0}{\frac{1}{2} \rho_\infty V_\infty^2 S_{\text{ref}}}
\]  \hspace{1cm} (11)

A comprehensive analysis of these phenomena can be found in the previous paper by the current authors (Benichou et al. 2020).

WING-PROPULSOR COUPLING: RESULTS

Test Matrix

The investigated cases are summarised in Tables 1 and 2. All the upstream conditions are representative of the take-off of a TBM900 with \(P_\infty = 101325\) Pa and \(T_\infty = 300K\) for the atmospheric conditions, with a Mach number \(M_\infty = 0.11\). The angle of attack is set to 0°.

<table>
<thead>
<tr>
<th>Case name</th>
<th>Imposed condition for propulsor</th>
<th>Imposed value</th>
</tr>
</thead>
<tbody>
<tr>
<td>(\Omega_N)</td>
<td>Rotor speed</td>
<td>13 900 rpm</td>
</tr>
<tr>
<td>(\Omega_{WM})</td>
<td>Rotor power</td>
<td>0 kW</td>
</tr>
</tbody>
</table>

Table 1: Summary of the simulations for the single-segment case.

The single-segment simulations are the focus of the precedent paper by the authors (ibid.) and are solely used here to add some degree of validation to the double-segment computations.

For the investigation of the mutual influence of the propulsors, the last case \(\Omega_{WM}/P_{100\%}\) is used to do a sweep in angle of attack from 0° to 16°.

Coupled Behaviour of the Adjacent Propulsors

The simulations presented in this paper aim to study the mutual interactions between the propulsors. The computational domain was changed to include two aero-propulsive segments whose respective power setting could be controlled independently. The currently presented
setup makes it possible to impose different fan power settings and analyse the overall behaviour of the resulting flow. As for the unitary aero-propulsive assembly case, periodicity condition was imposed on the lateral boundaries of the domain, which means that in this case the simulation represented an infinite wing composed of double-segment blocks. Consequently, these simulations were evidently twice as big in term of memory and costly in CPU hours as the previous ones; the mesh target sizes used were unchanged, and the number of cells was approximately doubled. Table 3 summarises a selection of the results from the simulations outlined in the test matrix (Tables 1 and 2).

<table>
<thead>
<tr>
<th>Case name</th>
<th>Imposed conditions for propulsor 1 and 2</th>
<th>Imposed value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\Omega_N/\Omega_N$</td>
<td>Speed</td>
<td>13 900 rpm</td>
</tr>
<tr>
<td>$\Omega_{WM}/\Omega_N$</td>
<td>Power for 1 and Speed for 2</td>
<td>0 kW and 13 900 rpm</td>
</tr>
<tr>
<td>$\Omega_{WM}/\Omega_{WM}$</td>
<td>Power</td>
<td>0 kW for both</td>
</tr>
<tr>
<td>$P_{10%/P_{10%}}$</td>
<td>Power</td>
<td>12.3 kW for both</td>
</tr>
<tr>
<td>$P_{100%/P_{100%}}$</td>
<td>Power</td>
<td>123 kW for both</td>
</tr>
<tr>
<td>$\Omega_{WM}/P_{100%}$</td>
<td>Power</td>
<td>0 kW and 123 kW</td>
</tr>
</tbody>
</table>

Table 2: Summary of the simulations for the bi-propulsor case.

<table>
<thead>
<tr>
<th>Case name</th>
<th>$\Omega_1/\Omega_2$ (RPM)</th>
<th>$\dot{m}_1/\dot{m}_2$ (kg/s)</th>
<th>$\Delta h_{t1}/\Delta h_{t2}$ (kJ/kg)</th>
<th>$P_1/P_2$ (kW)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$\Omega_N$</td>
<td>13 900</td>
<td>14.7</td>
<td>8.4</td>
<td>123</td>
</tr>
<tr>
<td>$\Omega_{WM}$</td>
<td>2 650</td>
<td>4.1</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>$\Omega_N/\Omega_N$</td>
<td>13 900/13 900</td>
<td>14.7/14.7</td>
<td>8.4/8.4</td>
<td>123/123</td>
</tr>
<tr>
<td>$\Omega_{WM}/\Omega_N$</td>
<td>3 300/13 900</td>
<td>5/14.3</td>
<td>0/9.4</td>
<td>0/135</td>
</tr>
<tr>
<td>$\Omega_{WM}/\Omega_{WM}$</td>
<td>2 650/2 650</td>
<td>4.1/4.1</td>
<td>0/0</td>
<td>0/0</td>
</tr>
<tr>
<td>$P_{10%/P_{10%}}$</td>
<td>6 500/6 500</td>
<td>7.7/7.7</td>
<td>1.6/1.6</td>
<td>12.3/12.3</td>
</tr>
<tr>
<td>$P_{100%/P_{100%}}$</td>
<td>13 900/13 900</td>
<td>14.7/14.7</td>
<td>8.4/8.4</td>
<td>123/123</td>
</tr>
<tr>
<td>$\Omega_{WM}/P_{100%}$</td>
<td>3 200/13 400</td>
<td>5/14</td>
<td>0/9</td>
<td>0/123</td>
</tr>
</tbody>
</table>

Table 3: Results of the simulations. $\Omega$ is the rotational speed, $\dot{m}$ is the mass flow, $\Delta h_t$ the rise of enthalpy through the propulsor and $P$ the power setting of the given propulsor.

The following section will firstly compare the results of single-segment simulations with their uniform-power double-segment counterparts. Following this pseudo-validation step, we went on to analyse the double-segment with asymmetrical power settings, as presented in the second part of the section.

In the framework of preliminary validation of these simulations, the first computations consisted in setting the same operating point for the two propulsors, and then comparing the results with the ones from the single-segment case. Comparison between cases $\Omega_N$ and $\Omega_N/\Omega_N$; and $\Omega_{WM}$ and $\Omega_{WM}/\Omega_{WM}$ provides evidence of the coherence between the single and double-segment simulations. Indeed, the mass flow, the power consumption, and the predicted free-windmilling velocity are almost identical, as well as the lift and drag coefficient (not shown here for clarity). These results provide some confidence going further with asymmetrical power settings.

Cases $\Omega_{WM}/\Omega_N$ and $\Omega_{WM}/P_{100%}$ highlight the coupling of the flow between adjacent segment. It shows that in the case of asymmetrical power-setting combination, the mass flow is
higher in the windmilling propulsor and lower in the full-power one than compared to their baseline counterpart (case $\Omega_{WM}$ and $\Omega_N$). Some of the airflow is forced in the windmilling fan by the suction of the other. Thus, with the same power input, the powered fan experiences a higher load on its blades. Comparison between lines $\Omega_{WM}/\Omega_N$ and $\Omega_{WM}/P_{100\%}$ provides compelling evidence that the control-by-power of the BFM model allows to keep control over the power consumption. In the case of $\Omega_{WM}/\Omega_N$, the power needed to keep the baseline rotational speed is higher since some of the work is used by the windmilling rotor (and thus the windmilling speed is higher). Case $\Omega_{WM}/P_{100\%}$ is the counterpart by highlighting the lower rotational speed of the powered rotor caused by the presence of the windmilling propulsor.

Since this was preliminary work, dedicated to the methodology assessment, only four bi-propulsor simulations were run with different angles of attack. Even though the Body Force Modelling reduces drastically the number of cells in the rotor and stator, the rest of the simulated domain needs a fine mesh, which is the main contributor to the duration of the simulations.

We can also consider other global indicators such as lift or drag coefficient. Figure 4 shows the variations of the lift coefficient as defined in Eq.10 for asymmetrical settings of the bi-propulsor case versus the single propulsor. For the rest of this paper, the bi-propulsor results correspond to the $\Omega_{WM}/P_{100\%}$ case at four different angles of attack. A flow field is presented figure 5 to provide some visualisation of the explored configuration. The single-propulsor corresponds to $P_{100\%}$ in Figure 4 (a) and $\Omega_{WM}$ in (b). The mutual interactions of the propulsors at different power settings can then be investigated.

Firstly, we can study the segment independently. In Figure 4 (a), we compare the single-segment of $\Omega_N$ single-segment against the segment at nominal power of the bi-propulsor simulation. This highlight that the coupling effect continues to take place at different angles of attack, with the windmilling segment absorbing some of the power and thus decreasing the lift of the powered segment. On the contrary, Figure 4 (b) compares $\Omega_{WM}$ to the windmilling segment of the bi-propulsor simulation. On this case, the lift is higher is the asymmetrical configuration since there is a power source next to it.

We can also study the mean performance of the bi-propulsors simulation compared to the uniform wing at different fractions of the rotational speed (power fraction is not employed here because these simulations date back from before the method was formulated). The values
summarised in Figure 6 indicate what is the equivalent of the bi-propulsor in regards to a mono-propulsor. From the point of view of the lift coefficient, the bi-propulsor is practically coincident with the 50%Ω mono-propulsor trend. Given that a major part of the lift is generated by the upper-surface suction effect, having only half the propulsive power generates as much lift as the single propulsor at half nominal speed. However, on the drag coefficient, the bi-propulsor is closer to a rotational setting of 75%Ω. This is due to the fact that the lift is in part created by the suction while the drag of the windmilling fan is underestimated without the mechanical losses simulated. Finally, the mass flow indicates a midpoint between 75%Ω and 50%Ω. Indeed, some of the flow in the windmilling propulsor come from the upstream velocity. These results show potential for energy recovery in flight, with a possibility to load the rotors until $C_D$ become null or even negative.

**CONCLUSIONS AND PERSPECTIVES**

The paper describes application of a Body Force Model to model aero-propulsive behaviour of a highly integrated propulsors-airframe (wing and nacelle) assembly with computation cost compatible with preliminary and early detailed design. The particular contribution of the presented work is in new boundary condition formalism which enables control of the propulsor operation through power manipulation rather than rotating speed, as it has been the case so far. The described formalism was employed to examine the traditionally non-linear coupled behaviour of aero-propulsive assemblies, this time for a configuration consisting of a double aero-propulsive wing segment. The configuration was simulated at non-symmetric conditions, with particular aim to investigate the behaviour in the case of one of the propulsors operating at windmilling mode. The obtained preliminary results indicate that a possibility for power recovery via loaded windmilling might be possible and need to be investigated. The coupling between two propulsors at different power settings was also highlighted and a more complete parametric study can reveal new interesting synergies.

This work presents the next step towards a better visibility of the functional synergies enabled by the airframe-propulsor configuration composed of an array of ducted propulsors mounted in the boundary layer zone of the wing suction surface. Aerodynamics and handling qualities enabled by combined propulsor-airframe behaviour need to be rendered transparent in
the overall aeroplane design schemes, which are beyond the scope of this paper. To that end, the next step in this research work would consist of creation of a full wing model with a full array of propulsors which could be independently simulated at any flight conditions of interest. The experience summarised in this paper tells us that even at this lower-fidelity level of modelling, the CPU cost relative to the number of possible configurations of interest would be completely out of reach of any preliminary design effort. Nevertheless, the presented method can be used to identify preliminary tendencies as well as to calibrate low-level decision-making methods used in preliminary aero-propulsive configuration selection.

While simulation of a full finite wing-propulsor configuration is currently not envisioned since the associated computation cost would be prohibitive for such endeavours, preliminary feasibility studies of a finite wing with 4 independently controlled distributed propulsors were carried out. Notwithstanding rather extensive meshing, such calculations seem perfectly feasible if CPU constraint is alleviated. Furthermore, the experience gathered from the current and the preceding works does not seem to favour any optimisation efforts of such configuration using the presented frameworks, be it for a unitary or more complicated aero-propulsive segments. Creation of such geometries involves a lot of variables, for both the propulsor and the wing but mainly for the transition zone between the nacelle, the fan and the aerofoil, and more work needs to be invested in definition of meaningful free variables that define such configurations, if any optimisation is to be undertaken in the long run.

Figure 6: Comparison of the lift coefficient (a), the drag coefficient (b), the mass flow (c) and the polars (d) at multiple angles of attack.
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