

Article

Improvement of Take-Off Performance for an Electric Commuter Aircraft Due to Distributed Electric Propulsion

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Abstract: The need for environmentally responsible solutions in aircraft technology is now considered the priority for global challenges related to the limited supply of traditional fuel sources and the potential global hazards associated with emissions produced by traditional aircraft propulsion systems. Several projects, including research into highly advanced subsonic aircraft concepts to drastically reduce energy or fuel usage, community noise, and emissions associated with aviation, are currently ongoing. One of the proposed propulsion concepts that address European environmental goals is distributed electric propulsion. This paper deals with the detailed aerodynamic analyses of a full-electric commuter aircraft with fuel cells, which expects two primary electric motors at the wing tip and eight other electric motors distributed along the wingspan as secondary power sources. The main objective was the numerical estimation of propulsive effects in terms of lift capabilities at take-off conditions to quantify the possible reduction of take-off field length. However, the aircraft was designed from scratch, and therefore a great effort was spent to design both propellers (for the tip and distributed electric motors) and the wing flap. In this respect, several numerical tests were performed to obtain one of the best possible flap positions. This research work estimated a reduction of about 14% of the take-off field length due to only the propulsive effects. A greater reduction of up to 27%, if compared to a reference conventional commuter aircraft, could be achieved thanks to a combined effect of distributed propulsion and a refined design of the Fowler flap. On the contrary, a significant increment of pitching moment was found due to distributed propulsion that may have a non-negligible impact on the aircraft stability, control, and trim drag.

Keywords: DEP; take-off performance; full-electric aircraft; CFD-RANS

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1. Introduction

In the last few years, great effort has been dedicated to the hybridization of propulsion systems for aviation to address European environmental goals of reducing pollutant emissions and noise. However, to reach near-zero emissions target, it was necessary to overcome the technological limits of the battery in terms of energy and power densities, in view of the maximum take-off weight limit imposed by the regulation, which could be possible only considering hydrogen as an alternative power source. In this respect, an interesting example of a feasibility study has been performed within the framework of the ELICA project [1]. Readers could refer to [2–4] for the conceptual and preliminary design of the hybrid configuration with distributed electric propulsion (DEP), including the analysis of other enabling technologies, to [5] for market study and the TLARs definition, and to [6] for the design of the full-electric configuration with hydrogen fuel cells (basically the configuration analyzed in this paper). These research papers describe the effort spent by ELICA partners to design a new commuter aircraft able to transport 19 passengers for 200 nautical miles as a typical mission. However, it is important to underline that this kind of aircraft is considered a laboratory for other domains in clean aviation [7]. Indeed, to

identify the most efficient aircraft architecture, different propulsion and aircraft configurations were assessed in trade-offs; different levels of hybridization and different primary energy sources were explored, including options for a thermal engine or a fuel cell as part of the hybrid (or full-electric) configuration. In parallel, technologies and solutions that can shorten time-to-market and affordability were investigated as well. For the ELICA case, DEP has been identified as the most promising technology to achieve the best performance in the take-off phase for a full-electric configuration with fuel cells configuration, with an expected entry-into-service of 2035. According to current regulations, the beneficial aero-propulsive effects cannot be exploited for the approach and landing phases. However, some thoughts on potential changes to regulations related to high-lift propeller aircraft have been drafted [8]. The configuration expects two primary electric motors at the wing tip, and eight electric motors distributed along the wingspan as a secondary power source. The partial turbo-electric architecture with fuel cells was selected in order to be able to place larger propellers at the wing tips for reducing the induced drag.

Concerning the environmental goals, the estimated noise reduction is about 70%, while as far as pollutant emissions are concerned, this concept has the potential to be zero-emission (−100% of emissions compared with conventional configuration), at least concerning its operative life [6]. In fact, the only fuel cell emission is water vapor, and hydrogen production can potentially be a negative environmental impact. However, safety and ground handling problems associated with the use of hydrogen must be solved. However, DEP effects are not limited to aerodynamics but range from structural aspects [9,10] to the design of batteries [11], involving issues in control as well [12].

In greater detail, DEP technology uses multiple propulsors driven by electric motors distributed about the airframe to yield a beneficial aerodynamic–propulsion interaction. NASA has been working on such innovative configurations since 2014 within the X-57 programme [13–15]. In principle, the beneficial effect of the propeller–fluid interaction allows for obtaining a higher lift capability for the wing. Indeed, among the several benefits, the use of DEP involves a large reduction of the wing area, decreasing the friction drag [16,17], a higher cruise lift coefficient (close to the maximum efficiency point), less gust/turbulence sensitivity, and comparable take-off and landing speed [18]. One of the inherent features of a DEP-enabled aircraft is the tight integration of the propulsion system into the wing-body surfaces of the aircraft [19]. As reported by Kim [20], the benefits of aero-propulsive coupling can be broken into several categories. Firstly, a wide range of vehicle configurations which have been developed claim propulsive efficiency benefits due to boundary-layer ingestion. Secondly, the strategic placement of propulsors can reduce vehicle drag through a variety of mechanisms, including wake-filling and vortex suppression. Finally, various applications have been developed which make use of the propeller or fan slipstream interacting with an aerodynamic surface to produce some form of enhanced lift or control authority. Additionally, the thrust stream of distributed electric propulsors can be utilized to enable new capabilities in vehicle control, including reducing requirements for traditional control surfaces and increasing the tolerance of the vehicle control system to engine-out or propulsor-out scenarios [21].

Obviously, these advantages must be quantified and verified case by case since they highly depend on the configuration, in particular concerning drag reduction, and on the speed in the case of the propeller blowing effect related to lift coefficient increment. For example, considering the work of [22,23], they estimate an amplification of the maximum lift coefficient of about three times for the flapped configuration in landing for the DEP wing, whereas [24,25] agree that an induced drag reduction of up to 15% could be achievable thanks to the wing-tip mounted propellers, depending on the wing platform and the lift coefficient. This output is based on the theory reported in refs. [26,27]. Numerical results that will be shown in the following sections demonstrated that for the ELICA case study, the increment in lift coefficient would be pretty much lower than that NASA predicted due to the higher take-off speed which reduced the blowing effect of the propellers. Moreover, the drag reduction due to the wing-tip propeller was practically undermined by the fact

that the distributed propellers also work in cruise, unlike the NASA X-57 where the DEP is enabled just in take-off [28] (it is possible because the energy required for the flight mission is lower since the aircraft must transport only 4 passengers instead of 19 like the ELICA configuration).

To sum up what has been stated above, this paper aims to estimate the possible benefit of distributed electric propulsion for a commuter aircraft which is able to transport 19 passengers for 200 nautical miles for a typical mission. The references reported above refer to smaller aircraft (for example, the NASA X-57 retrofitted a TECNAM P2006T, which transports four passengers). This aspect is crucial because the propulsive effects highly depend on the forward speed of the aircraft during each mission phase. So, the research question which lies behind this research could also be expressed as follows: how do propulsive effects impact the low-speed performance of a commuter aircraft? What is the possible take-off field length reduction related to the distributed propeller wing interaction?

Studies have indicated that the runway length for an initial twin-engine aircraft can be reduced using the DEP system by up to 80% [29] by reaching the extreme values of the lift coefficient [30]. However, also in these cases, the aircraft considered could transport fewer passengers than the ones considered for the ELICA configuration. Another interesting question is related to the global effect on the aircraft design of the DEP technology, in particular in terms of wing design. The configuration herein considered expects a 20% reduction of the wing surface if compared with a conventional reference configuration as described in [6]. Data reported by Hospodář et al. [31], related to a nine-seat light airplane, estimated a possible reduction of wing surface of about 35%, which involves a possible reduction of 6% block fuel. This estimation seems to be too optimistic if compared with the weight estimation carried out by Ongut et al. in [32] for the ELICA configuration, where the preliminary estimation was also refined by means of FEM analysis. In this work, the wing weight of the DEP configuration is slightly different with respect to the conventional reference configuration (+5%), even if the wing surface is reduced by 20%. Therefore, it was impossible to reach such a high block fuel reduction just for the DEP.

Regarding the numerical analysis, CFD-RANS calculations were carried out by means of Simcenter STAR-CCM+ software, developed by Siemens Digital Industries Software based in Aktiengesellschaft Werner-von-Siemens-Straße 1 80333 Munich, Germany, using the University of Naples Federico II computing cluster [33]. The numerical model has been built by following the best practices suggested by NASA [34] and Siemens Simcenter support [35]. Moreover, the numerical model is based on the knowledge acquired during the past experience of the DAF research group of the University of Naples [24,36], thanks to their involvement in other European and national projects such as PROSIB [37] and IRON [38].

In order to answer the research questions previously mentioned, it was necessary to design the propeller and the flap since the aircraft had been designed from scratch. The high-lift propellers were designed aiming to maximize the wing-propellers interaction. According to Patterson [39], to improve the axial velocity and the high-lift capability, a near-uniform axial velocity must be produced after the propeller. Such kind of distribution is different from that derived from the Goldestein [40], based on the solution of the Betz condition for minimum energy loss [41]. In greater detail, the distribution indicated hereinafter in this paper as the minimum induced loss (MIL) is based on the practical solution reported by Larranbee in [42], modified by Adkins and Liebeck [43], and generalized by Wald [44], which was the common approach for propeller design before the introduction of DEP technology.

Concerning the flap design, a fowler flap was chosen, which is a single-slotted flap with a combined rearward translation towards the trailing edge position. Usually, it is difficult to define the proper shape of the flap slot based on general assumptions. Indeed, the flow around a wing section with a deflected slotted flap is very complicated, and no theory has been developed to predict the aerodynamic characteristics. Consequently, the information required for design is obtained entirely by empirical methods. In particular, the

design is based on data reported in well-known references such as Abbott [45], particularly for the effect of lip shape (the extremal part of the main wing), the flap chord ratio, and flap positioning on maximum lift coefficient. Other indications regarding flap positioning, such as for gap, overlap, and deflection for general aviation aircraft, were taken from Gudmundsson [46]. The airfoil chosen for the wing was NACA 23018 for the root and kink station and NACA 23015 for the wing tip. Information about movables for the laminar airfoil, but for a maximum thickness of 12% (NACA 23012), can be found in [47,48]. They were useful in finding out the range of values that must be considered to reach the possible highest maximum lift coefficient. In this respect, data reported in [49,50] for GA-W1 and GA-W2 airfoils were analyzed since they were also investigated by Viken et al. [51] as the first choice for NASA X-57 aircraft (airfoils were subsequently modified and ad hoc designed for that aircraft). Finally, considering general aviation, seeking the optimized position of the flap, the best practice acquired by the University of Naples DAF research group on similar commuter aircraft, as reported in Corcione et al., was taken into account [52].

Further details about flap and propeller design and tools used for calculations are reported in Section 2. The main results about aerodynamics and the impact of DEP on aircraft performance will be shown in Section 3, while some comments and remarks will be drawn in Section 4.

2. Materials and Methods

The configuration considered in this work is full-electric with fuel cells, with expected entry-into-service in 2035. It expects two primary electric motors at the wing tip and eight electric motors distributed along the wingspan as a secondary power source. A sketch of the configuration is shown in Figure 1, while reference data are provided in Table 1. The external geometry was obtained by means of the JPAD Modeller, the first software of the JPAD family conceived to support preliminary aircraft design workflows, developed by SMARTUP Engineering [53].



Figure 1. A sketch of aircraft geometry, commuter aircraft 19 pax, obtained by means of JPAD.

Table 1. Relevant data of innovative aircraft configuration.

Wing	
S_w	33.94 m ²
AR	13
root chord	1.851
Kink position	2.88 m
TR	0.65
MAC	1.645 m
LE sweep angle	0 deg
TE sweep angle (kink-tip)	4 deg
wing incidence	3 deg
tip washout	−3 deg
Airfoil (root, kink, tip)	NACA 23018/18/15
Fuselage	
Length	18 m
Cabin Diameter	2.150 m
Cabin Height	2.262 m
Horizontal tail	
S_h	10.6 m ²
AR_h	4.6
root chord	1.76
TR	0.72
LE sweep angle	8 deg
Airfoil	NACA 0012
Vertical tail	
S_v	10.6 m ²
AR_v	1.6
root chord	3.345 m
TR	0.37
LE sweep angle	45 deg
Airfoil	NACA 0012
Weight and Balance	
MTOM	8139 kg
CG (%mac)	[30–40%, 0.0, −25%] Wing reference frame

2.1. Aerodynamic Numerical Model

As mentioned before, the software used for this numerical study was STAR-CCM+. It is a commercial CFD software package consisting of CAD/geometry manipulation tools, a grid generator, a flow solver, and post-processing tools. The framework allows users to perform the analysis from beginning to end within STAR-CCM+. The grid generator is robust and capable of creating grids of various topologies, including polyhedral, tetrahedral, and cartesian. Any of the mentioned topologies can be combined with a prism layer mesh to adequately capture the boundary layer. STAR-CCM+ solves Reynolds-averaged Navier–Stokes equations in the cell-centered, finite-volume formulation [34,54]. A polyhedral grid topology was the type selected for this study. High-fidelity calculation is based on a reliable numerical model whose main parameters are reported in Table 2, while flight conditions data are in Table 3. The total cell number is around 9 million, with a reference (base) size of 0.5 m (less than 3% of the model reference length). Refinements were located in the most interesting parts, such as lifting surfaces (wing, horizontal and vertical tailplane), in particular at the trailing edges and tips, in the wing-fuselage junction, and at the fuselage nose (see also Figure 2). The total height of the prism layer was determined using the turbulent boundary layer thickness of the flat plate, and the wall spacing for the coarse grid was again determined based on a y^+ value of 1.0 computed from the free stream

condition, in line with [34]. The Spalart–Allmaras model was chosen as the turbulence model. The numerical domain was externally bound by a block, representing the far-field, and internally bound by the aircraft surfaces. The block used to simulate the volume of fluid around the aircraft was sized opportunely to ensure the restoration of asymptotic conditions in the boundaries of the domain. In order to judge convergence, the lift and drag coefficients were monitored. In Figure 3, is possible to see how the aerodynamic coefficients reached an unchanging state with less than 1000 iterations. Further information about mesh quality could also be derived from Figure 4 about wall y^+ .

Table 2. Mesh and numerical model parameters.

Mesh Type	Unstructured (Polyhedral Cells)
Number of cells	~8,500,000
Number of prism layer	33
Wall distance of the first cell	6×10^{-6} m
Turbulence models	Spalart–Allmaras (SA)
Flow model	Ideal gas
Solver	Steady Coupled implicit
Number of iterations per AoA	1000
Boundary conditions	Inflow: Free Stream Outflow: Pressure Outlet

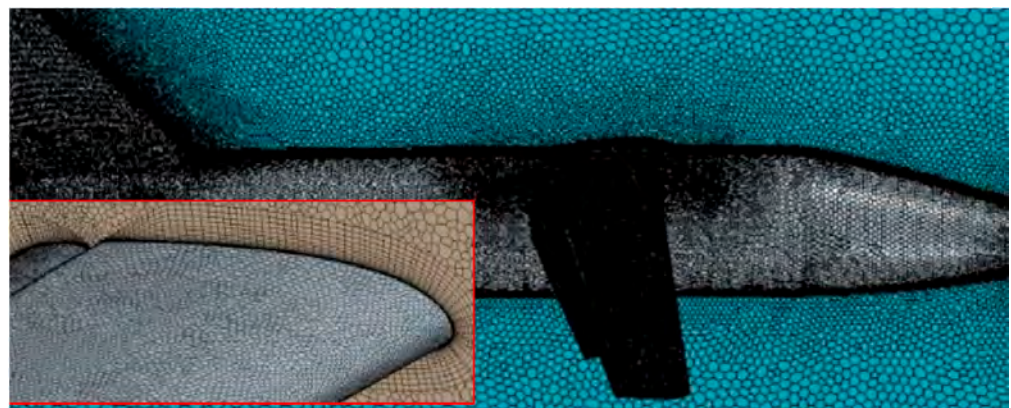


Figure 2. Polyhedral mesh: symmetry plane near aircraft.

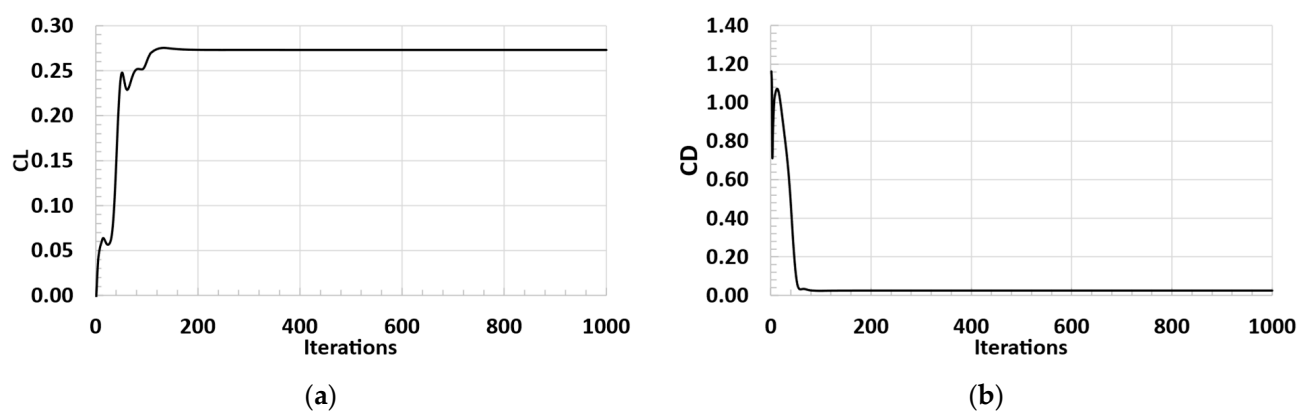


Figure 3. Convergence plot for lift coefficient (a) and drag coefficient (b). Cruise condition, $M = 0.32$, $Re = 9.2$ Mil.

Table 3. Flight conditions.

	Low Speed	Climb	Cruise
Mach number	0.15	0.26	0.317
Reynolds number	5.7×10^6	8.7×10^6	9.2×10^6
Reference altitude	S/L	1500 m	3048 m
Reference density	1.225 kg/m ³	1.058 kg/m ³	0.909 kg/m ³
Speed of sound	340.3 m/s	334.5 m/s	328.4 m/s

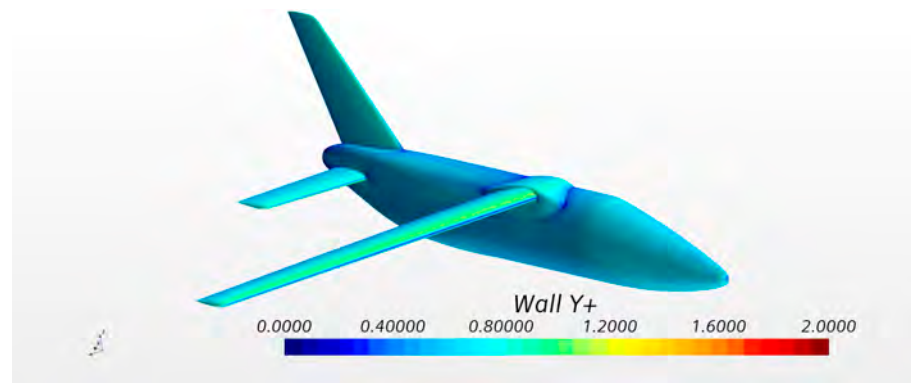
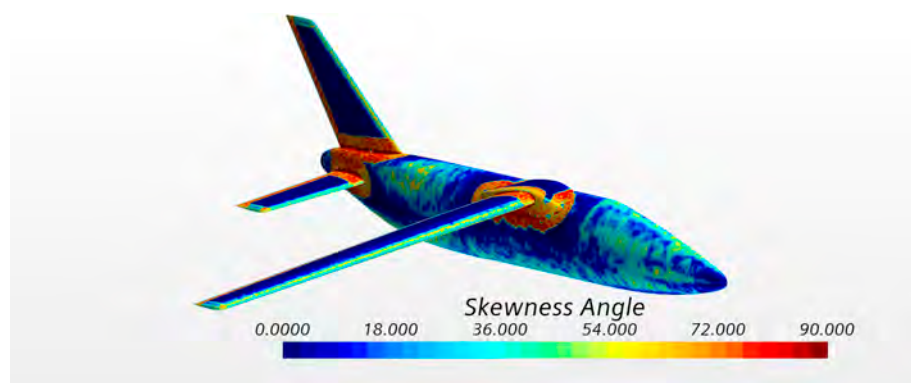
**Figure 4.** Wall Y+ map, Star-CCM+, cruise condition, $M = 0.32$, $Re = 9.2$ Mil.

Figure 5 shows the surface map of the mesh skewness angle. The skewness angle is the angle between the face area vector (face normal) and the vector connecting the two cell centroids. This skewness measure is designed to reflect whether the cells on either side of a face are formed in such a way as to permit diffusion of quantities without these quantities becoming unbound. An angle of zero indicates a perfectly orthogonal mesh [54]. As depicted by Figure 5, this parameter is almost zero on lifting surfaces that are the target regions of this research work. As expected, in the intersection between aircraft components and where surfaces exhibit curvatures having different directions, the skewness angle is inevitably larger than zero.

**Figure 5.** Skewness angle map, Star-CCM+, cruise condition, $M = 0.32$, $Re = 9.2$ Mil.

Regarding the 2D investigations performed to find out the best flap position, a dedicated mesh was defined. Figure 6 shows the considered airfoil located at the inner flap station (12% of the wingspan) with a deployed flap. Relevant data for analysis are collected in Table 4.

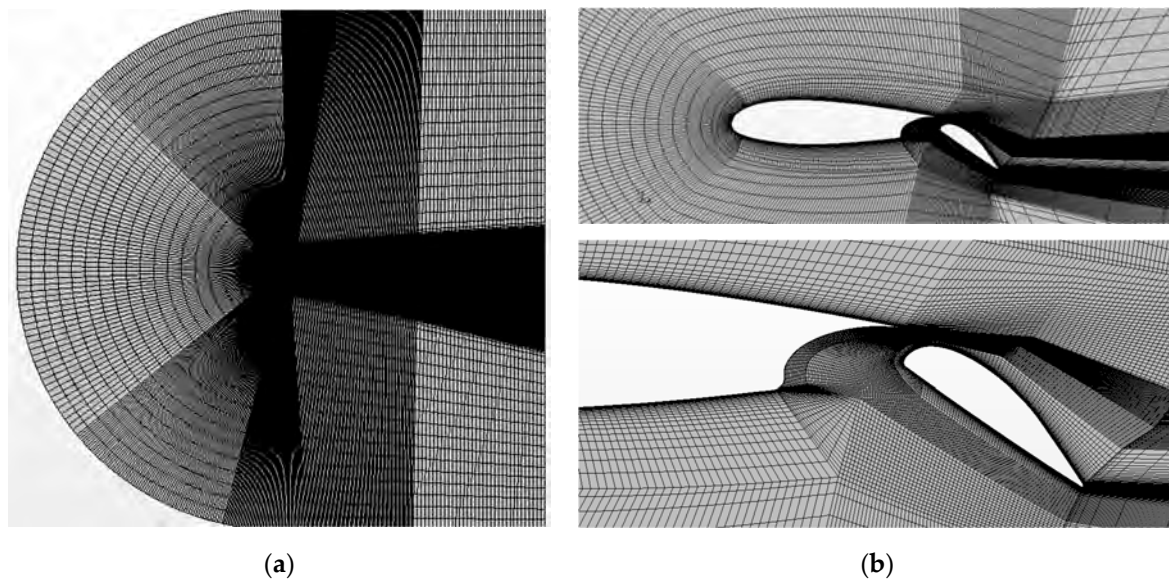


Figure 6. Two-dimensional mesh for flapped airfoil analysis: (a) Global overview of C-shape domain; (b) mesh details around the airfoil and flap.

Table 4. Relevant data for 2D analysis.

Low-Speed Conditions	
Mesh type	2D-Structured
Number of cells	86,179
Mach number	0.15
Reynolds number	5.7×10^6
Speed of sound	340.3 m/s
Number of iterations per AoA	~1000

Finally, the propellers were simulated using the virtual disk model. As reported in [55], it is based upon the principle of representing propellers, turbines, rotors, fans, and so on as an actuator disk. Among several methodologies for the implementation of the action of the actuator disk, the blade element method was chosen. This models the spinning rotor as a distribution of momentum sources. The strength of the source terms and their variations are interactively determined by the rotor geometry and the local velocity field. Although the blade geometry is not explicitly resolved, it was required to specify the blade geometry in terms of the chord and twist variations along the rotor radius. Details about the propeller designs and results are shown in the next section.

2.2. Propeller Design

In general, the stream tube behind a propeller in which the velocity of the axial flow is higher than the undisturbed flow and a rotational velocity is present is called the propeller slipstream. Aircraft components which are located behind the propeller experience the slipstream as a variation in the oncoming airflow, which have no parallel streamlines and different pressure distribution (consequently lift, drag, and pitching moment). In general, all effects coming from the slipstream interaction with aircraft components are defined as indirect effects. On the contrary, direct effects are related to the aerodynamic forces exerted by and on the propeller: the thrust T and the normal force N . The last is orthogonal to the thrust line, and it is generated in non-axial flows. Both produce a pitching moment whose value depends on the distance of the aircraft's center of gravity to the thrust line and propeller disk.

In this work, the indirect propulsive effects were evaluated for take-off conditions, aiming to obtain what will be the possible gain derived from the DEP configuration in

terms of aircraft lift capabilities. In this respect, the first step was the design of the propeller, starting from the power requirements provided by the mission analysis carried out during the conceptual design phase (see Table 5). Propeller design was performed by means of XROTOR, which is an interactive program for the design and analysis of ducted and free-tip propellers and windmills. The design procedure allows the calculation of rotor chord and blade angle (c/R , β) distributions to achieve a minimum induced loss (MIL) circulation distribution [56]. Minimum induced loss propellers are the rotor analogues of elliptically loaded wings, characterized by minimum kinetic energy loss for a specified disc loading, number of blades, and flight (or wind) speed; they approximate the actuator disk, given the constraints of single rotation and blade number [57]. For the design of the DEP propellers, a dedicated MATLAB tool was used [58], which implements Patterson's theory [39,59] for the design of the propellers and exploited XROTOR for the analysis.

Table 5. Power requirements for each flight phase.

	Altitude	Speed	Shaft Power TIP	Shaft Power DEP
Take-Off	0 m	51 m/s	217 kW	126 kW
Climb	1500 m	94 m/s	225 kW	131 kW
Cruise	3000 m	104 m/s	221 kW	129 kW
Descent	1500 m	66 m/s	32 kW	49 kW
Landing	0 m	54 m/s	16 kW	6 kW

Indeed, distributed propellers are exploited to augment the maximum lift capabilities of a wing in high-lift conditions, specifically at take-off, to shorten the required length or to meet a specific distance with a smaller wing area. To this purpose, DEP propellers have been designed to maximize the blowing effect. On the other hand, in cruise conditions, the required thrust is provided only by TIP propellers; thus, they must be designed to maximize their efficiency.

As already stated, the starting point for the propeller design was the power requirements defined during the preliminary design phase within the ELICA project framework. Table 5 summarizes the power requirements for each flight phase and other data of interest for the simulations, such as the speed and altitude.

Finally, for the sake of clarity, the definition of thrust, power, torque coefficients, and of advance ratio and propeller efficiency is reported in the following Equations (1)–(4), where ρ is the flow density, n the round per seconds, D is the propeller diameter, T the thrust force, P the shaft power, and V is the forward speed. Moreover, in Figure 7, the scheme of blade geometry is drawn: α is AoA for the blade element, β is the geometric pitch angle, φ is the helix angle, V_t is the tangential velocity component, V_a is the axial velocity component, and W is the resulting velocity.

$$C_T = T/(\rho n^2 D^4), \text{ Thrust coefficient,} \quad (1)$$

$$C_P = P/(\rho n^3 D^5), \text{ Power coefficient,} \quad (2)$$

$$J = V/nD, \text{ Advance ratio,} \quad (3)$$

$$\eta = C_T/C_P J, \text{ Propeller efficiency.} \quad (4)$$

Concerning the airfoil, M114 [60] was chosen for DEP propellers, while for the tip propeller, an SDA 1075 was considered [61].

Blade geometries of both TIP and DEP propellers are reported in Table 6 and represented in Figures 8 and 9. To describe the influence of the blade geometry on the flow, lift and drag coefficients for two-dimensional cross-sections of the blade at successive locations on the blade had to be provided. These data were obtained by means of the XFOIL tool [62] and reported in Table 7.

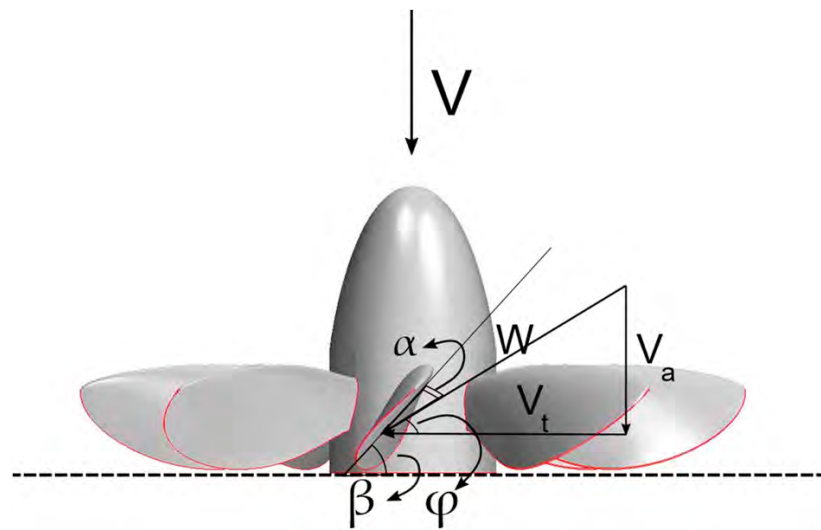


Figure 7. Blade geometry definition and velocity of interest: α is AoA for the blade element, β is the geometric pitch angle, ϕ is the helix angle, V_t is the tangential velocity component, V_a is the axial velocity component, and W is the resultant velocity.

Table 6. DEP and TIP propeller geometries (r , blade station, R , blade radius, c , blade local chord).

DEP Propeller			TIP Propeller		
r/R	c/R	Blade Pitch Angle β (deg)	r/R	c/R	Blade Pitch Angle β (deg)
0.101	0.04	80.70	0.106	0.031	82.63
0.103	0.042	80.36	0.129	0.037	77.97
0.111	0.049	79.35	0.165	0.048	70.79
0.124	0.061	77.73	0.207	0.059	63.49
0.142	0.079	75.59	0.253	0.068	56.96
0.165	0.105	73.04	0.3	0.072	51.36
0.192	0.12	69.1	0.347	0.074	47.25
0.224	0.129	64.58	0.394	0.074	43.78
0.259	0.126	59.52	0.44	0.073	40.85
0.298	0.113	54.38	0.485	0.071	38.37
0.34	0.103	49.81	0.529	0.068	36.26
0.384	0.094	45.76	0.572	0.066	34.44
0.43	0.086	42.21	0.614	0.063	32.87
0.478	0.079	39.1	0.654	0.061	31.51
0.526	0.073	36.4	0.692	0.058	30.33
0.575	0.068	34.07	0.728	0.056	29.28
0.623	0.063	32.06	0.763	0.053	28.37
0.671	0.059	30.34	0.795	0.051	27.56
0.717	0.056	28.86	0.826	0.048	26.85
0.761	0.053	27.61	0.854	0.045	26.23
0.803	0.05	26.56	0.88	0.042	25.68
0.841	0.047	25.7	0.903	0.038	25.21
0.877	0.045	25	0.924	0.035	24.81
0.908	0.042	24.47	0.943	0.031	24.47
0.936	0.039	24.1	0.959	0.026	24.2
0.958	0.035	23.89	0.972	0.022	23.99
0.976	0.03	23.83	0.983	0.018	23.83
0.989	0.023	23.9	0.991	0.013	23.73
0.997	0.013	24.02	0.997	0.008	23.68
0.999	0.006	24.09	0.999	0.005	23.65

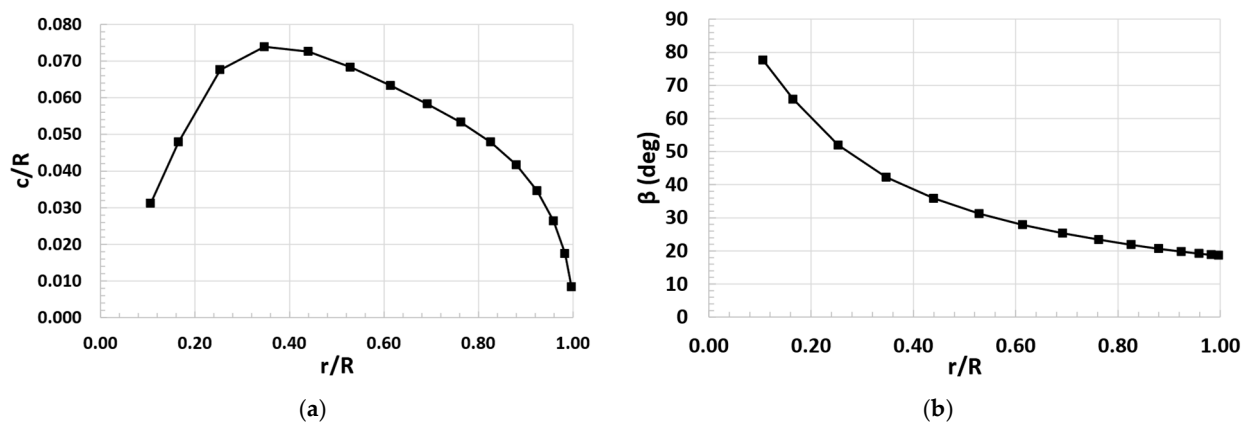


Figure 8. TIP propeller designed with MIL approach. (a) Chord distribution. (b) Pitch angle distribution.

Table 7. Aerodynamic data of M114 and SDA1075 blade airfoil, XFOIL $M = 0.15$, $Re = 3 \times 10^6$.

M114 Airfoil			SDA1075 Airfoil		
AoA	cl	cd	AoA	cl	cd
−6	0.257	0.00818	−6	−0.467	0.0073
−5	0.382	0.00697	−5	−0.362	0.0068
−4	0.51	0.00641	−4	−0.256	0.0064
−3	0.636	0.00628	−3	−0.151	0.0059
−2	0.762	0.00604	−2	−0.045	0.0056
−1	0.88	0.00617	−1	0.061	0.0053
0	1.001	0.0063	0	0.167	0.0051
1	1.12	0.00656	1	0.273	0.005
2	1.239	0.00668	2	0.378	0.005
3	1.358	0.00699	3	0.478	0.0048
4	1.475	0.00732	4	0.591	0.0048
5	1.59	0.00779	5	0.714	0.0054
6	1.704	0.00825	6	0.842	0.0061
7	1.815	0.00867	7	0.959	0.0071
8	1.921	0.00959	8	1.082	0.0085
9	2.019	0.01055	9	1.21	0.01
10	2.083	0.0118	10	1.317	0.0113
11	2.101	0.0232	11	1.389	0.0127
12	2.084	0.02084	12	1.458	0.0141
13	2.03	0.02262	13	1.494	0.0158
14	1.951	0.03242	14	1.535	0.0179
15	1.844	0.03692	15	1.57	0.0211
16	1.711	0.04214	16	1.597	0.0261
17	1.562	0.04865	17	1.605	0.0341
18	1.408	0.05684	18	1.598	0.0452
19	1.257	0.06743	19	1.569	0.0605
20	1.112	0.08102	20	1.509	0.0816

Table 8 summarizes the design operating points for both DEP and TIP propellers.

Table 8. Propellers take-off operating points. Both DEP and TIP propellers have a right-handed rotation.

	D (m)	J	C_T	n (rpm)	Pitch Angle @.75 r/R (deg)	Efficiency	Power (kW)
DEP	1.89	0.89	0.138	2000	28.4	0.82	126
TIP	2.54	0.78	0.1015	1600	23.8	0.86	217

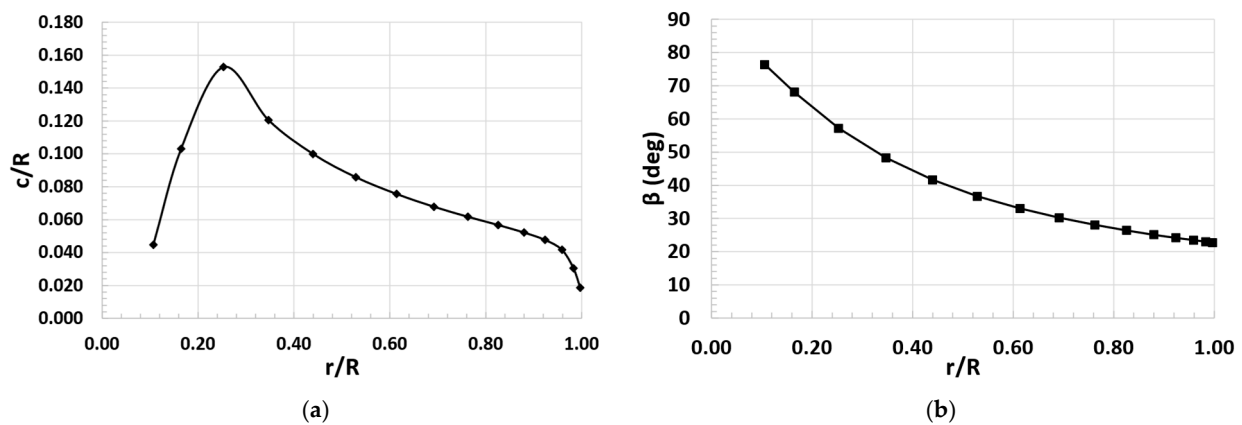


Figure 9. DEP propeller designed with Patterson's approach. (a) Chord distribution. (b) Pitch angle distribution.

To clarify the different aerodynamic behaviour of a propeller designed according to MIL or Patterson's approach, Figure 10 compares the profile of the axial induced velocity of the considered DEP propeller with a propeller having the same diameter and absorbing the same shaft power but designed with the MIL approach. According to the Patterson theory, to maximize the blowing effect across the propeller disk, the radial distribution of the axial velocity must be as uniform as possible.

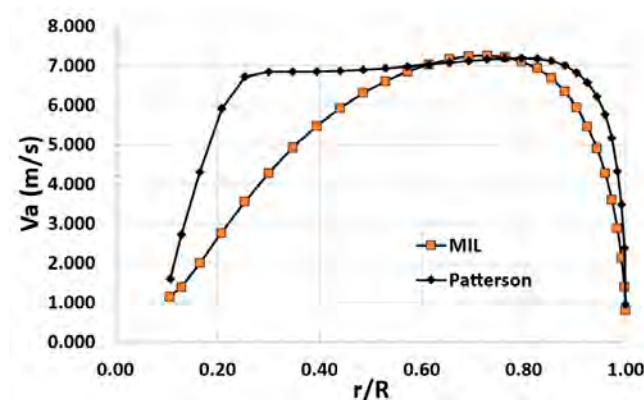


Figure 10. Induced axial speed V_a (m/s) for DEP propeller designed following Patterson approach and the equivalent one designed following the MIL approach.

Finally, in the following Figures 11–13, the propeller maps of efficiency, thrust, and power coefficients for both tip and distributed propellers are reported.

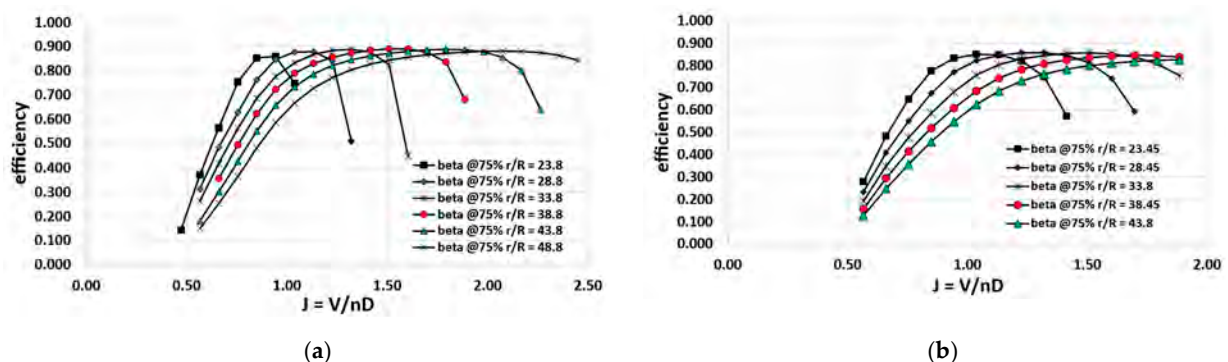


Figure 11. Propellers efficiency map (different blade pitch angles). (a) Tip propeller. (b) DEP propeller.

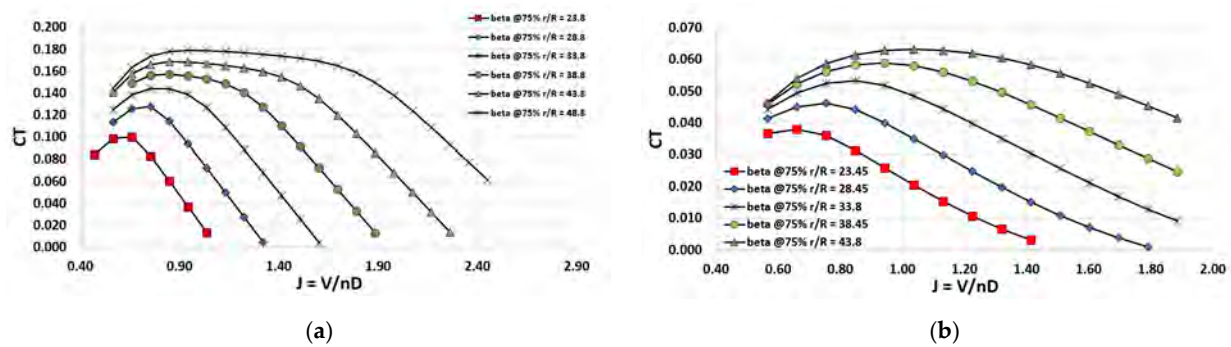


Figure 12. Propellers thrust coefficient map (different blade pitch angles). (a) Tip propeller. (b) DEP propeller.

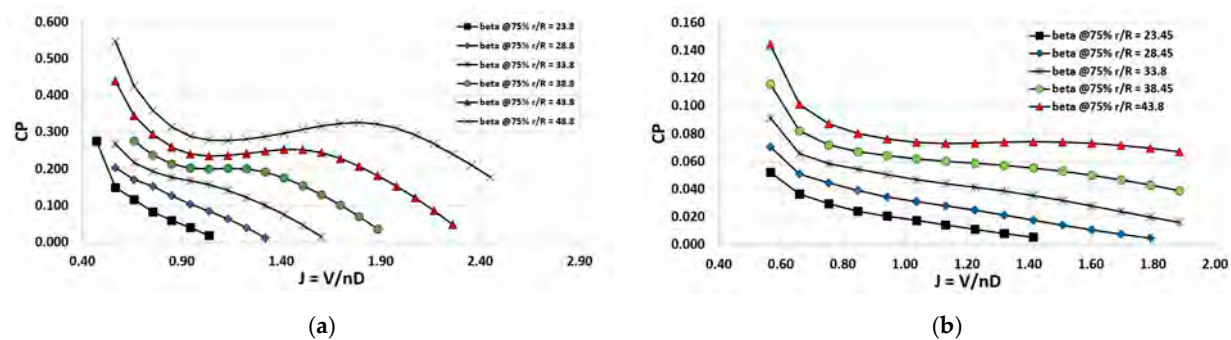


Figure 13. Propeller power coefficient map (different blade pitch angles). (a) Tip propeller. (b) DEP propeller.

2.3. Flap Settings Design

In order to estimate low-speed performance, it was necessary to design the flap settings. As for propeller design, the starting point for the flap design was the indications coming from the preliminary design phase performed during the ELICA project. Indeed, in that design phase, to obtain a certain lift gain in low-speed conditions (take-off and landing), the extension in chord, span, and flap deflections were estimated by means of semi-empirical methods. A single-slotted Fowler flap was assumed (see Table 9).

Table 9. Main flap characteristics.

Type	Fowler
Flap/Wing chord ratio	0.3
Inner station	12% wingspan
Outer station	80% wingspan
Airfoil (root, kink, tip)	NACA 23018/18/15

However, there was not any information about the flap position. Therefore, it was necessary to find out the best position of the flap in both low-speed conditions. For a Fowler flap, the position could be identified by two parameters (see Figure 14):

- gap, which is the vertical distance between the lip of the main wing and the flap;
- overlap, which is the longitudinal distance between the lip of the main wing and the foremost point of the flap.

The geometry was obtained by means of a JPAD Modeller [53,63], which is able to design many kinds of trailing-edge flaps: plain flaps, single or multiple slotted flaps, split flaps, and Fowler flaps.

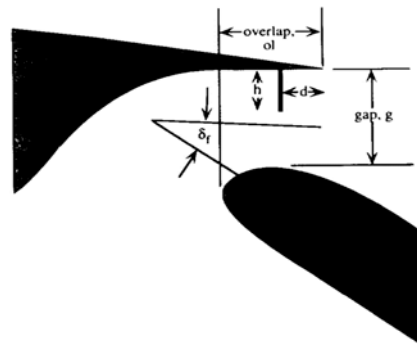


Figure 14. Flap positioning main parameters: gap and overlap definition.

Several numerical tests were performed on different flap positions in order to find out the most promising one in terms of maximum achievable lift coefficient at landing deflection, the latter being the most demanding condition. According to the 2D numerical results reported in Table 10 and the lift curves of Figure 15, two promising positions were identified:

- gap 3%, overlap 2%, flap deflection 35 deg;
- gap 2.5%, overlap 2.7%, flap deflection 30 deg.

Table 10. Numerical results of 2D analysis at different flap positions, landing condition, $M = 0.15$, $Re = 5.7 \times 10^6$.

Gap	Overlap	Deflection	$C_{l \max}$	$C_{l \alpha}$	C_{l0}
3%	0%	35 deg	2.88	0.108	1.53
3%	−2%	35 deg	2.51	0.105	1.22
3%	2%	35 deg	3.56	0.107	1.68
3%	3%	35 deg	3.52	0.108	1.69
2%	0%	35 deg	3.11	0.108	1.46
4%	2%	35 deg	2.99	0.106	1.50
2.5%	2.7%	35 deg	2.98	0.097	1.64
2.5%	2.7%	30 deg	3.45	0.108	1.94

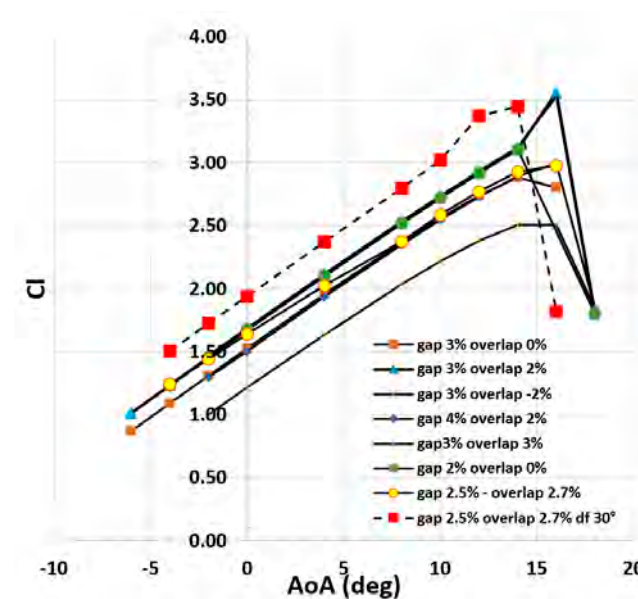


Figure 15. Numerical results in terms of lift coefficient curves of 2D analysis at different flap positions, landing condition, $M = 0.15$, $Re = 5.7 \times 10^6$.

Both configurations provide a similar maximum lift coefficient, but the second one exhibits a larger lift increment at zero angles of attack. The latter should be the driver parameter since, during the take-off and landing maneuvers, the feasible attainable angle of attack is significantly lower than the maximum achievable one (corresponding to the maximum lift coefficient).

These geometries were also analyzed in three-dimensional simulations. The results of the three-dimensional CFD analyses will be shown and discussed in Section 3.

3. Results

In this section, the main results dealing with the aerodynamic analyses of the complete aircraft will be shown. Firstly, the 3D analyses in power-off conditions will be presented, and then the propulsive effects of a DEP in take-off conditions will be illustrated, paying particular attention to the impact the propeller's blowing has on the take-off performance. Moreover, some considerations about how the aero-propulsive interaction affects aircraft stability and control will be addressed.

3.1. Low-Speed Aerodynamic Analysis Results (Power-off)

The first numerical investigation dealt with the aerodynamic characterization of the complete aircraft in power-off conditions for both take-off and landing flap settings.

The first CFD runs highlighted the need for further refinement of the wing–fuselage intersection in order to avoid undesired flow separations magnified by the flow perturbation due to flap deflection (see Figure 16). In this respect, Figure 17 shows the skin friction contours in order to visualize the behavior of the flow at a high angle of attack (AoA equal to 14 deg). The deep-blue area in the maps indicates a flow separation. The positive effect of the fillet on the flow separation is clearly visible.



Figure 16. Wing–fuselage fairing geometries without (a) and with (b) fillet.

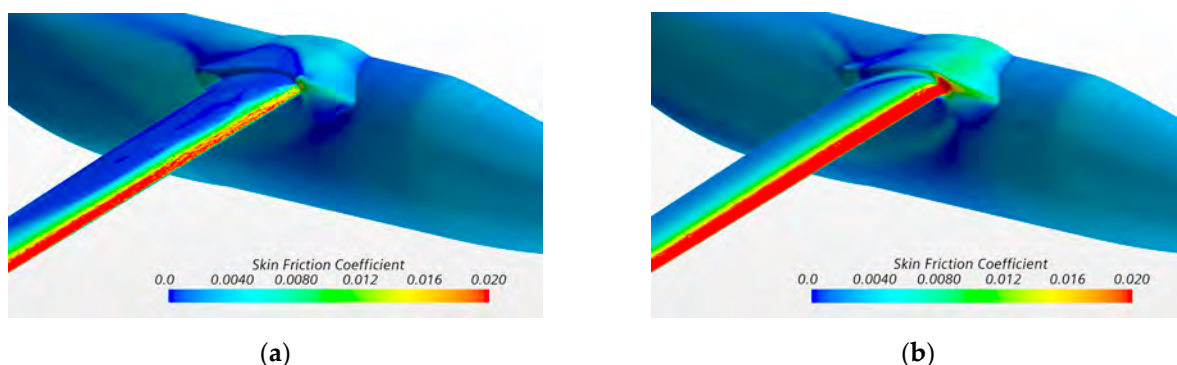


Figure 17. Effect of the wing–fuselage fillet on the flow separation (skin friction map), AoA equal to 14 deg. (a) Geometry without fillet. (b) Geometry with fillet.

Numerical results confirmed that a gap value of 2.5%, an overlap value of 2.7%, and a deflection of 30 deg was the most promising solution in terms of maximum lift coefficient. Indeed, the maximum achievable lift coefficient was about 3, as shown in Figure 18. In the same figure, the analysis performed for the take-off condition is also reported. In this respect, starting from the best flap positioning for the landing and according to a plausible fowler flap handling mechanism, the take-off flap (considering the deflection angle of 15 deg) gap and overlap were 3 % and 0%, respectively (Tables 11 and 12 summarize the suggested flap position for take-off and landing).

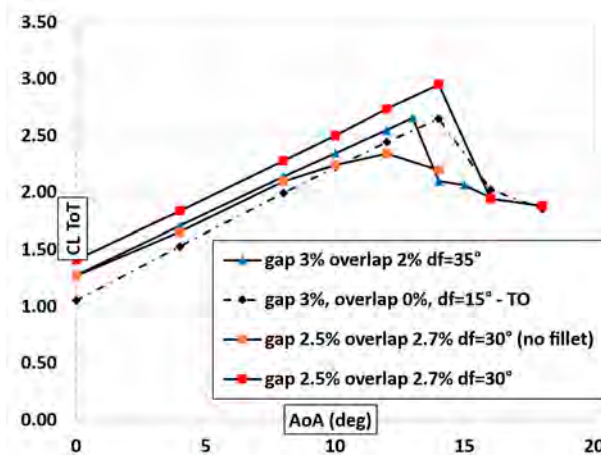


Figure 18. Numerical results of 3D simulations in terms of global lift coefficient for different flap positions, landing condition, $M = 0.15$, $Re = 5.7 \times 10^6$.

Table 11. Flap position take-off.

Take-Off	
Gap	3% chord
Overlap	0% chord
Deflection	15 deg

Table 12. Flap position landing.

Landing	
Gap	2.5% chord
Overlap	2.7% chord
Deflection	30 deg

A comparison of the three curves of the global lift coefficient for take-off and landing conditions is shown in Figure 19. In the same figure, the curve related to the clean configuration is also reported (no flap deployed). Furthermore, drag polar and pitching moment curves are reported in Figure 20. In Table 13, the numerical results are reported in terms of force coefficients.

Table 13. Numerical results for low-speed conditions, untrimmed maximum lift coefficient, moment coefficient calculated w.r.t. CG pos. (31% mac), $M = 0.15$, $Re = 5.7 \times 10^6$.

	C_{Lmax}	$C_{L\alpha}$	C_{L0}	$C_{M\alpha}$	C_{M0}
Clean	1.57	0.110	0.27	−0.053	0.118
Take Off	2.65	0.118	1.05	−0.045	0.100
Landing	2.95	0.108	1.41	−0.039	0.130

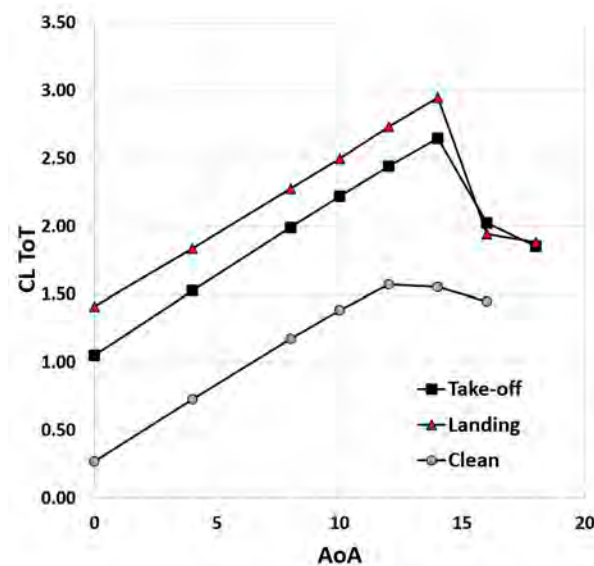


Figure 19. Total lift coefficient curves for clean and flapped (take-off and landing setup) wing configurations, low-speed conditions, $M = 0.15$, $Re = 5.7 \times 10^6$.

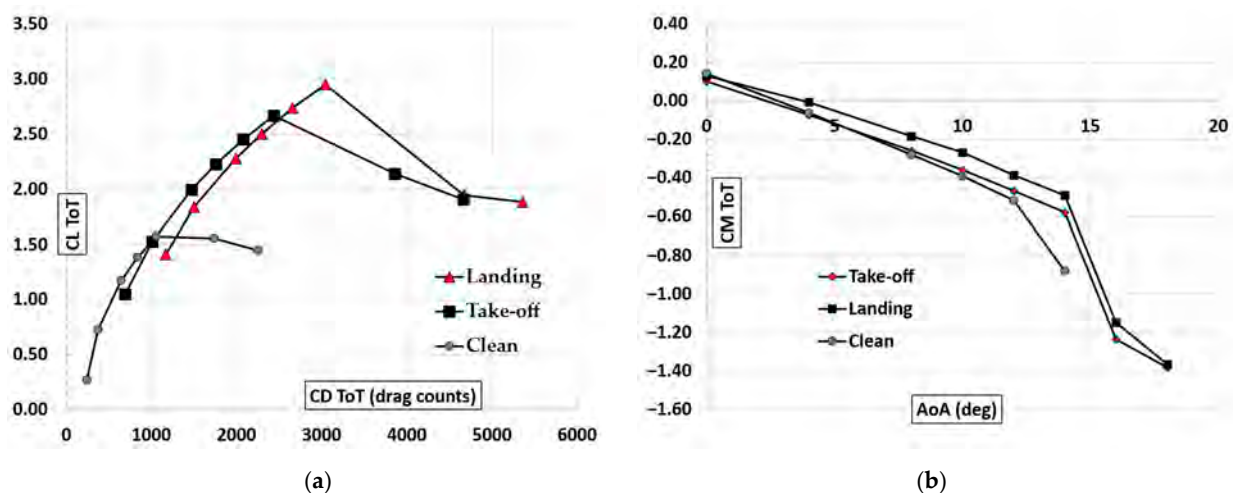


Figure 20. Drag and pitching moment curves, take-off and landing condition, $M = 0.15$, $Re = 5.7 \times 10^6$. (a) Drag polar (drag expressed in counts). (b) Pitching moment coefficient w.r.t. CG pos. (31% mac).

It is worth noting that the value of the maximum lift coefficient achieved in the take-off condition (2.65) is pretty much higher with respect to that assumed during the conceptual design phase estimated with the vortex lattice method (VLM) [6], equal to 1.9. Consequently, the take-off performance was positively affected by this increment, with a relatively reduced field length. The same applies to the landing phase, where the estimated maximum lift coefficient was 2.27 instead of 2.95. The reason behind this discrepancy lies not only in the different fidelity of the tool used for the calculation, CFD instead of VLM, but also because, in this case, the flap was designed and optimized while in the previous case reported in [6], the lift increment due to flap was just estimated by means of the semi-empirical method.

However, the lift coefficient values reported in Table 13 refer to an untrimmed condition, let us say, a condition where the equilibrium about the y-axis was not guaranteed. As suggested by EASA [64,65], the appropriate balancing horizontal tail loads must be accounted for in a rational or conservative manner. In this case, it was estimated starting from the total pitching moment curve drawn in Figure 20b, and in particular, it was considered the pitching moment coefficient value at a high angle of attack near the stall condition

since the considered forward speed for take-off was assumed equal to 1.13 of the 1 g stall speed. Considering that value and the volumetric coefficient V_H defined as in Equation (5), where l_h is the distance between aerodynamic center of the tailplane and the CG position, the negative lift coefficient of the tailplane was computed by means of Equation (6).

$$V_H = \frac{l_h}{\text{mac}} \frac{S_H}{S_w}, \quad (5)$$

$$\Delta C_{LH} = \frac{C_{M \text{ CG}}}{V_H} \frac{S_H}{S_w}. \quad (6)$$

Regarding the actual maximum lift coefficient values achievable by the aircraft in take-off, it could be estimated by subtracting the ΔC_{LH} calculated with Equation (6) from the values reported in Table 13. The updated values are reported in Table 14.

Table 14. Numerical results for low-speed conditions, trimmed maximum lift coefficient, moment coefficient calculated w.r.t. CG pos. (31% mac), $M = 0.15$, $Re = 5.7 \times 10^6$.

	$C_{L\text{max}}$	$C_{M \text{ cg}}$ (@AoA = 14 Deg)	V_H	ΔC_{LH}	$C_{L\text{max trimmed}}$
Take-Off	2.65	−0.593	1.5	−0.12	2.53
Landing	2.95	−0.491	1.5	−0.10	2.85

3.2. Investigation of Propulsive Effects on the Aircraft Aerodynamics at Take-Off

Regarding the assessment of propulsive effects in the take-off condition, in order to quantify the possible gain in terms of lift capabilities which could involve a reduction of take-off field length, the lift coefficient curve, the drag polar, and the pitching moment coefficient curve are reported in Figures 21 and 22, respectively, and compared with respect to the power-off case. In Figure 23, the flow around the aircraft is visualized in terms of vorticity induced by propellers, with the skin friction map on the body and with pressure coefficient plots related to wing sections located behind the propellers. The numerical results showed that the maximum lift coefficient jump due to propulsive effects was about 0.3 for untrimmed conditions (Table 15).

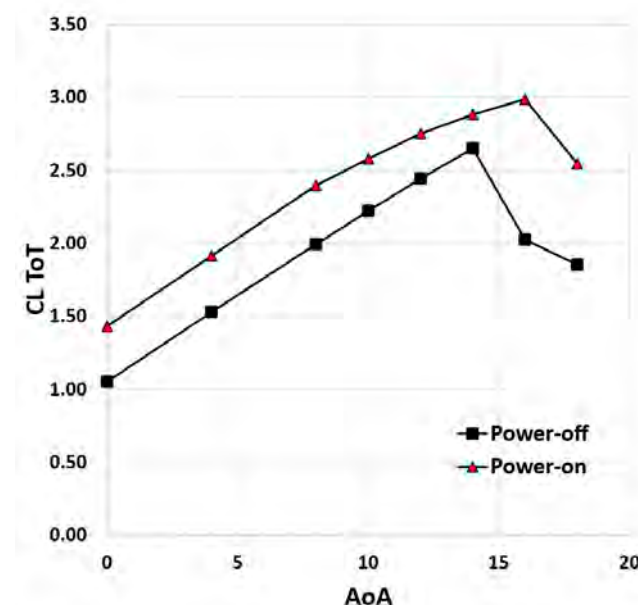


Figure 21. Total lift coefficient curves for power-off and power-on take-off conditions, $M = 0.15$, $Re = 5.7 \times 10^6$, $J_{TIP} = 0.78$, $rpm_{TIP} = 1600$, $J_{DEP} = 0.89$, $rpm_{DEP} = 2000$.

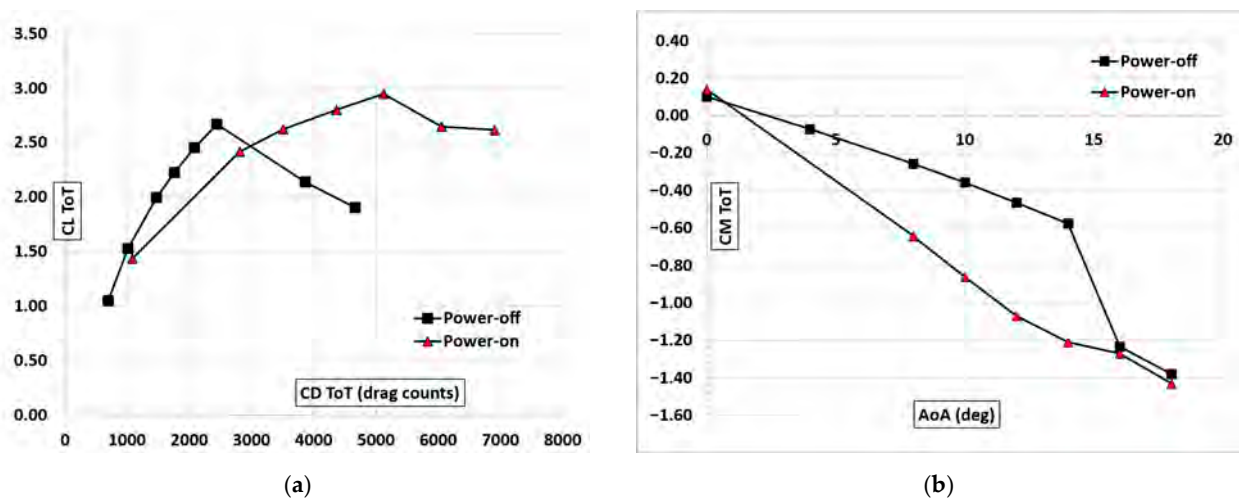


Figure 22. Drag and pitching moment curves for power-off and power-on take-off conditions, $M = 0.15$, $Re = 5.7 \times 10^6$, $J_{TIP} = 0.78$, $rpm_{TIP} = 1600$, $J_{DEP} = 0.89$, $rpm_{DEP} = 2000$. (a) Drag polar (drag expressed in counts). (b) Pitching moment coefficient w.r.t. CG pos. (31% mac).

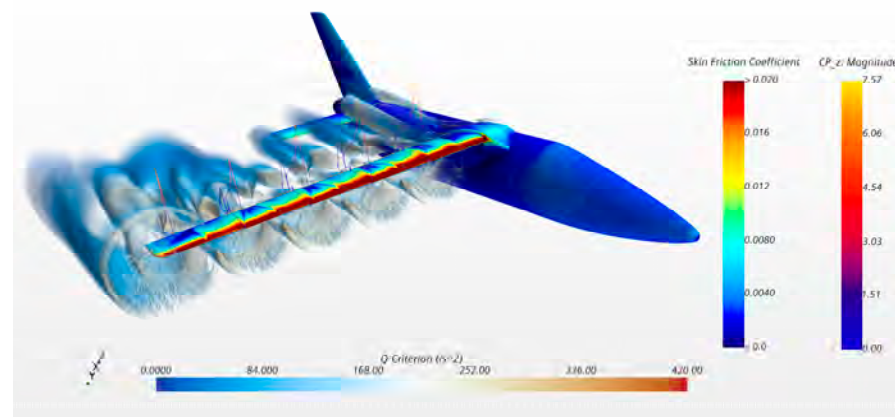


Figure 23. Vorticity scene with the body skin friction contour and plot of pressure coefficient for sections located behind the propeller, AoA = 10 deg, take-off power-on, $M = 0.15$, $Re = 5.7 \times 10^6$, $J_{TIP} = 0.78$, $rpm_{TIP} = 1600$, $J_{DEP} = 0.89$, $rpm_{DEP} = 2000$.

Table 15. Numerical results for take-off power-on condition, untrimmed maximum lift coefficient, moment coefficient calculated w.r.t. CG pos. (31% mac), $M = 0.15$, $Re = 5.7 \times 10^6$, $J_{TIP} = 0.78$, $rpm_{TIP} = 1600$, $J_{DEP} = 0.89$, $rpm_{DEP} = 2000$.

Take Off	C_{Lmax}	$C_{L\alpha}$	C_{L0}	$C_{M\alpha}$	C_{M0}
power-off	2.65	0.118	1.05	−0.045	0.100
power-on	2.95	0.123	1.43	−0.098	0.139

Similarly to power-off, the numerical results reported in Figures 21 and 22 and resumed in Table 15 refer to an untrimmed condition. Following the same approach as the previous section, the trimmed maximum lift coefficient was estimated and reported in Table 16. In this case, the effect of the DEP on the pitching moment coefficient was pretty much higher than in the power-off case (see Figure 22b). Therefore, the negative lift coefficient that the tailplane should provide to equilibrate the aircraft was higher (in terms of absolute value) compared with the power-off case, resulting in a higher penalty on the maximum lift coefficient achievable. These effects partially mitigate the DEP advantages in terms of the total maximum lift coefficient of the aircraft.

Table 16. Numerical results for take-off, trimmed maximum lift coefficient, moment coefficient calculated w.r.t. CG pos. (31% mac), power-on conditions: $J_{TIP} = 0.78$, $rpm_{TIP} = 1600$, $J_{DEP} = 0.89$, $rpm_{DEP} = 2000$. $M = 0.15$, $Re = 5.7 \times 10^6$.

Take Off	C_{Lmax}	$C_{M\ cg}$ (@AoA = 14 Deg)	V_H	ΔC_{LH}	$C_{Lmax\ trimmed}$
power off	2.657	−0.593	1.5	−0.12	2.53
power on	2.95	−1.210	1.5	−0.25	2.70

Another interesting aspect revealed by numerical results was the impact of the forward speed reduction on the propulsive effects. Indeed, it has been stated above that thanks to a careful flap design, the configuration could reach a higher maximum lift coefficient at take-off condition with respect to what was assumed during the first conceptual design phase of the aircraft ($C_{Lmax\ TO} = 2.55$ instead of 1.9). It was possible to define a new take-off forward speed ($V_{TO} = 44$ m/s instead of 51 m/s) based on the high-fidelity power-off estimation of the maximum lift coefficient. Numerical results are reported in Figures 24 and 25 and Table 17. With this assumption, the blowing effects were higher since the same propeller induced an axial velocity of about 6–7 m/s (see Figure 10) at a lower forward speed. Since the lift force increment depends on the ratio of induced axial velocity and the forward speed, the jump in the maximum lift coefficient value was higher than the previous calculations, and the coefficient achieved a value of 3.24 (untrimmed, instead of 2.95).

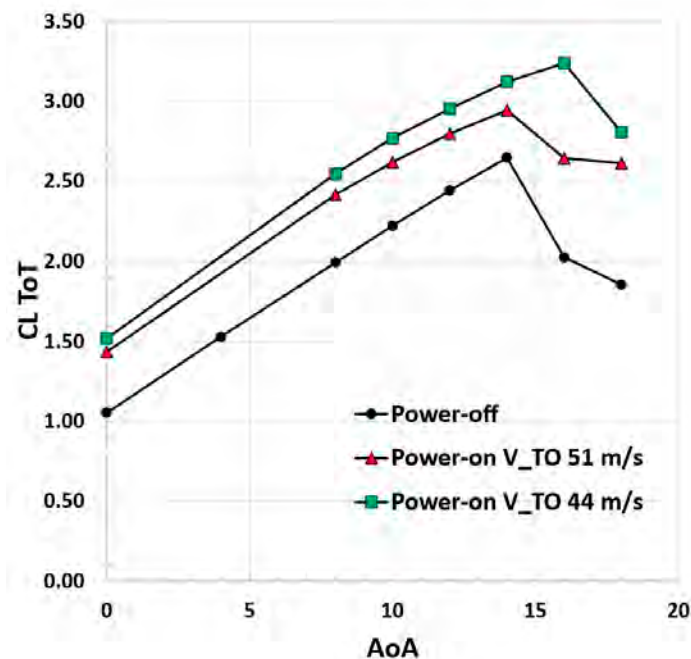


Figure 24. Total lift coefficient curves for power-off and power-on take-off conditions, $V_{TO} = 44$ m/s and $V_{TO} = 51$ m/s, $Re = 5.7 \times 10^6$, $J_{TIP} = 0.78$, $rpm_{TIP} = 1600$, $J_{DEP} = 0.89$, $rpm_{DEP} = 2000$.

Table 17. Numerical results for take-off power-on condition, untrimmed maximum lift coefficient, moment coefficient calculated w.r.t. CG pos. (31% mac), $V_{TO} = 44$ m/s and $V_{TO} = 51$ m/s, $Re = 5.7 \times 10^6$, $J_{TIP} = 0.78$, $rpm_{TIP} = 1600$, $J_{DEP} = 0.89$, $rpm_{DEP} = 2000$.

Take Off	C_{Lmax}	$C_{L\alpha}$	C_{L0}	$C_{M\alpha}$	C_{M0}
power-off	2.65	0.118	1.05	−0.045	0.100
power-on	2.95	0.123	1.43	−0.098	0.139
power-on (reduced V_{TO})	3.24	0.129	1.52	−0.098	0.138

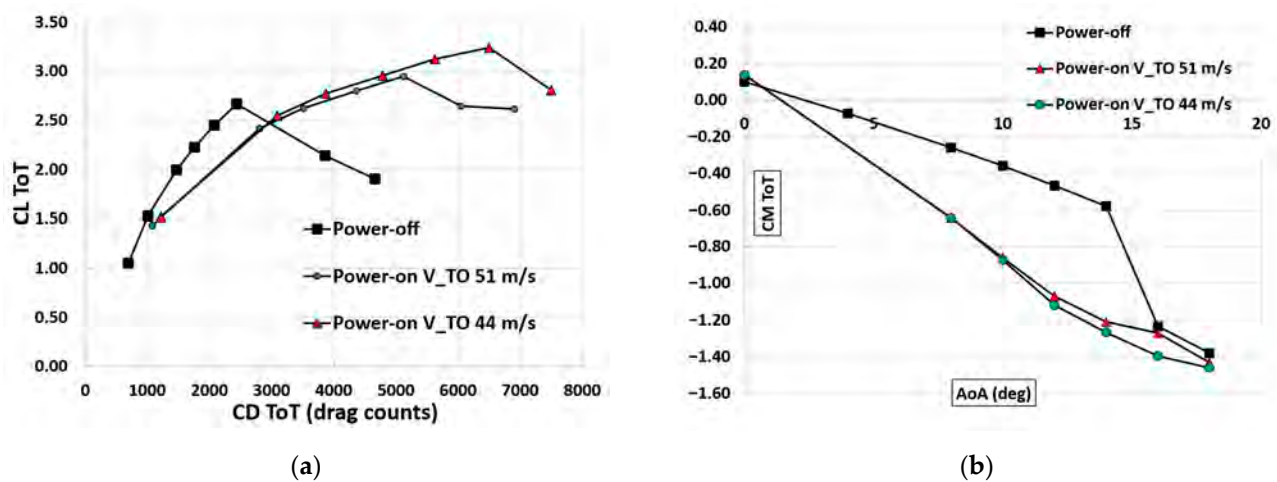


Figure 25. Drag and pitching moment curves, take-off condition $V_{TO} = 44$ m/s and $V_{TO} = 51$ m/s, $Re = 5.7 \times 10^6$, $J_{TIP} = 0.78$, $rpm_{TIP} = 1600$, $J_{DEP} = 0.89$, $rpm_{DEP} = 2000$. (a) Drag polar (drag expressed in counts). (b) Pitching moment coefficient w.r.t. CG pos. (31% mac).

Applying the same process as before, a promising value of 3.0 was estimated for the trimmed maximum lift coefficients (see Table 18) with a possible improvement of take-off performance in terms of field length reduction.

Table 18. Numerical results for take-off power-on condition, trimmed maximum lift coefficient, moment coefficient calculated w.r.t. CG pos. (31% mac), $V_{TO} = 44$ m/s and $V_{TO} = 51$ m/s, $Re = 5.7 \times 10^6$, $J_{TIP} = 0.78$, $rpm_{TIP} = 1600$, $J_{DEP} = 0.89$, $rpm_{DEP} = 2000$.

Take Off	C_{Lmax}	$C_{M_{cg}}$ (@AoA = 14 Deg)	V_H	ΔC_{LH}	C_{Lmax} trimmed
power off	2.67	−0.593	1.5	−0.12	2.53
power on	2.95	−1.210	1.5	−0.25	2.70
power-on (reduced V_{TO})	3.24	−1.268	1.5	−0.26	2.98

In this respect, these values of maximum lift coefficient were exploited to calculate the take-off field length with and without propulsive effects, also considering the effect of the forward speed reduction. It is important to remember that a reduction of take-off field length is crucial for the economic and operative attractiveness of this innovative configuration with respect to the conventional one. The considered reference aircraft was the greener aircraft designed by Piaggio Aerospace, whose main characteristics and performance are collected in [66]. In Table 19, the take-off distance, defined as the 115% of the horizontal distance along the take-off path from the start of the take-off to the point at which the airplane is 11 m (35 ft) above the take-off surface, as specified by CS 23–59, is presented [67]. The estimation of this distance was obtained by means of a simplified approach suggested by Roskam [68]. What is relevant in Table 19 is the last column, where the percentage reduction of the field length obtained thanks to the propulsive effect is reported. For the reference aircraft, the maximum lift coefficient is based on a well-educated assumption, while other data for weight, geometry, and the propulsive system are derived by [66]. As expected, the great increase in lift capability due to DEP involved a large reduction of the take-off distance, despite the relative increment in induced drag. However, the advantage of the configuration herein considered was derived mostly from the flap design, which allowed us to reach a significant increment of maximum lift coefficient. In fact, comparing only the power-off and on results, the net reduction of distance went from 5% to 13% (instead of 14% and 27% reported in Table 19), depending on the take-off forward speed.

Table 19. DEP effect on the take-off distance.

Take Off	C_{Lmax} trimmed	Take-Off Stall Speed (m/s)	Take-Off Distance (m)	$\Delta\%$
Reference	2.0	40.37	507	
Power-off	2.53	38.96	435	−14%
Power-on	2.70	37.72	407	−19%
Power-on (reduced V_{TO})	2.98	35.90	365	−27%

4. Conclusions

This paper dealt with the prediction of propulsive effects on the maximum lift capabilities of a full-electric commuter aircraft with fuel cells. The configuration had two primary electric motors at the wing tip and eight other electric motors distributed along the wingspan as secondary power sources. The high-fidelity investigations were mainly focused on low-speed conditions, in particular on take-off, in order to quantify the possible gain in terms of aircraft lift capabilities related to propulsive effects coming from the electric distributed propulsion architecture.

An exploration of the design variables of the flap was accomplished to identify the best deflection angles, gap, and overlap values targeting the best high-lift characteristics in landing configuration. High-fidelity numerical analyses were performed through the CFD-RANS solver performed on the complete aircraft in both landing and take-off settings. The innovative configuration was able to reach a higher maximum lift coefficient if compared to a conventional reference aircraft, being the greener aircraft designed by Piaggio Aerospace. In fact, the innovative configuration reached a maximum trimmed lift coefficient of about 2.5 instead of 2.0 (power-off condition).

Once the aerodynamics in power-off conditions were assessed, the study focused on the evaluation of the propulsive effects on the aircraft aerodynamics under take-off conditions. Before assessing the interaction of the propellers with the aircraft, the design of both the DEP and the TIP propellers was faced. Two sets of propellers were designed. The TIP propeller was designed to match the required spec points according to the minimum induced loss approach (MIL), whereas the DEP propellers were designed to reach an almost constant axial induction profile across the propeller blades in order to obtain the best blowing effect.

Once the propeller's design was accomplished, the propulsive effects on the take-off aircraft configuration were fulfilled by means of high-fidelity CFD analyses. The trimmed maximum lift coefficient increased by about 0.2 (2.70 instead 2.53). The drawback was the large increment of the pitching moment coefficient with respect to the power-off case, resulting in a higher penalty on the maximum achievable lift coefficient. This effect could partially reduce the distributed propulsion advantages and could resolve potential issues related to higher trim drag.

A higher blowing effect of the propellers was observed considering the lower take-off forward velocity due to a higher maximum lift coefficient provided by the designed flap (the first set of simulations was based on the take-off stall speed coming from the maximum lift coefficient estimated through a VLM approach). The lift increment could be 0.5 instead of 0.2 if the forward speed was assumed to be equal to 44 m/s instead of 51 m/s and the trimmed maximum lift coefficient achievable was about 3.0.

Thanks to the propulsive effects, a potential take-off field length reduction of 16% can be achieved. Compared with the reference conventional aircraft, the take-off field length turned out to be 27% thanks to the combined contributions of the flap design improvements and propulsive effects, 14% and 13%, respectively. This paper also highlighted how the flap design had a crucial role regarding the improvement of low-speed performance; therefore, only combined optimization of the flap and propeller could allow us to maximize the take-off field length reduction.

On balance, distributed electric propulsion seems to be an effective enabling technology for future commuter electric aircraft. Indeed, propellers could induce an axial velocity which could be equal to or higher than 10% of the forward speed and, therefore, could improve the lift capabilities at take-off. In the NASA X-57 case, the increment in terms of lift coefficient due to propulsion was estimated to be higher since the take-off speed is much lower than that of a commuter aircraft, such as that analyzed in this paper. Furthermore, the effect of the increment of pitching moment due to propulsion on the aircraft stability and control should be thoroughly assessed. From a wider perspective, other aspects should also be considered, such as the acquisition and maintenance costs of such an innovative electric propulsion system and the regulatory constraints related to the landing phase (currently, the propulsion could be exploited only during take-off). As far as this paper is concerned, this technology drastically enhances the take-off performance for the commuter aircraft considered, and therefore it could be a solution to address European environmental goals, in particular, the so-called near-zero emissions target.

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Conflicts of Interest: The authors declare no conflict of interest.

Abbreviations

The following abbreviations and symbols are used in this manuscript.

AC or A/C	Complete aircraft configuration
AoA, α	Angle of attack
AR	Aspect ratio
$C(\cdot)\alpha$	Derivative of the aerodynamic coefficient with respect to α
CG	Center of gravity
CFD	Computational fluid dynamic
C_D, C_d	Drag coefficient
C_L, C_l	Lift coefficient
$C_{L\max}$	Maximum lift coefficient
C_{L0}	Lift coefficient at an angle of attack of 0 deg
C_M or C_{MCG}	Pitching moment coefficient with respect to the centre of gravity
C_P	Power coefficient
C_T	Thrust coefficient
D, d	Diameter
DAF	Design of aircraft and flight technologies
DEP	Distributed electric propulsion
J	Advance ratio
JPAD	Java program for aircraft design
l_H	Longitudinal distance between the tail aerodynamic center and CG
LE	Leading edge

LND	Landing
M	Mach number
MAC, mac	Mean aerodynamic chord
MIL	Minimum induced loss
MTOM	Maximum take-off mass
P	Power
Q	Torque
Re	Reynolds number
RPM orn	Round per minute
RANS	Reynolds average Navier–Stokes
S(.)	Component surface
T	Thrust
TO	Take-off
TR	Taper ratio
V, V ₀	Forward speed
V _a	Axial speed
V _H	Horizontal tailplane volumetric coefficient
V _s	Stall speed
V _t	Tangential speed
V _{TO}	Take-off forward speed
β	Blade pitch angle
η	Propeller efficiency
ρ	Air density

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