



## **INTER IIT TECH MEET 14.0**

### **End-Term Report**

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### **TEAM 58**

#### **Powered Lift Wing Design Using Externally Blown Flaps with Distributed Electric Propulsion**

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## Executive Summary

This report presents the complete aerodynamic design, analytical modelling, and computational evaluation of a powered-lift wing operating under an Externally Blown Flap (EBF) configuration integrated with Distributed Electric Propulsion (DEP). The primary objective is to achieve Short Take-Off and Landing (STOL) capability at low freestream velocities (20 m/s) by exploiting propeller-induced slipstream energisation to attain exceptionally high lift coefficients in the range of  $C_L = 6\text{--}8$ . Such performance levels lie far beyond the capabilities of passive aerodynamics, thereby necessitating a tightly coupled propulsion–aerodynamics design framework. Through the integration of semi-empirical theory, inverse-design formulation, and high-fidelity CFD, this project delivers a validated aerodynamic configuration and a robust methodology for future prototype realisation.

## Initial Calculation for Wing Sizing

Preliminary wing sizing calculations were conducted to ensure consistency across the analytical and CFD evaluations. The design was governed by mission constraints, environmental parameters, and target aerodynamic performance.

### Design Constraints and Environmental Constants

- Gravitational Acceleration:  $g = 9.81 \text{ m/s}^2$
- Air Density (Sea Level):  $\rho_{sl} = 1.225 \text{ kg/m}^3$
- Air Density (Cruise):  $\rho_{cruise} = 0.6292 \text{ kg/m}^3$
- Maximum Take-Off Weight:  $W = 15.0 \text{ kg}$
- Take-off Velocity:  $V_{TO} = 20.0 \text{ m/s}$
- Cruise Velocity:  $V_{cruise} = 80.0 \text{ m/s}$
- Target Powered Lift Coefficient:  $C_{L,TO} = 6.5$
- Assumed Aspect Ratio:  $AR = 10$

### Initial Sizing Calculations

The required lift force is:

$$L = W \cdot g = 147.15 \text{ N}$$

Using a thrust-to-weight ratio between 0.5 and 0.65, the thrust envelope is:

$$T_{min} = 73.58 \text{ N}, \quad T_{max} = 95.65 \text{ N}$$

## Dynamic Pressure and Wing Area

$$q_{TO} = 0.5 \rho_{sl} V_{TO}^2 = 245 \text{ Pa}$$

With  $C_{L,TO} = 6.5$ :

$$S = \frac{L}{q_{TO} C_{L,TO}} \approx 0.0924 \text{ m}^2$$

## Wing Geometry Derivation

$$b = \sqrt{AR \cdot S} \approx 0.961 \text{ m}, \quad c = \frac{S}{b} \approx 0.096 \text{ m}$$

## Operational Reynolds Numbers

$$Re_{TO} \approx 1.3 \times 10^5, \quad Re_{cruise} \approx 2.7 \times 10^5$$

These estimates provide the geometric and flow conditions required for selecting the airfoil (Selig SI223) and for constructing physically accurate CFD boundary conditions.

## Theoretical Modelling: The Modified Kuhn's Method

A core achievement of the analytical phase was the adaptation of Richard E. Kuhn's semi-empirical powered-lift method (NASA TN D-3584) into a generative inverse-design tool. While the standard Kuhn formulation predicts lift ( $C_L$ ) for a fixed geometry, the design task required determining the optimal wing chord ( $c_w$ ) to sustain a target lift coefficient of  $C_L = 8$  at STOL speeds (20 m/s).

To achieve this, the method was analytically inverted by parameterising all geometric variables as functions of the wing chord,  $c_w$ . A significant challenge in this inversion arises from the non-linear dependency of the slipstream turning angle,  $\theta$ . Since the turning efficiency is a function of the chord-to-diameter ratio ( $c/D$ ),  $\theta$  becomes intrinsically linked to the unknown chord length. This creates a circular dependency, necessitating a numerical solution to the derived implicit sizing equation:

$$c_w^2 = \frac{T_{tot} \sin(\theta(c_w) + \alpha)}{q(AR)(C_L - C_{L,off})} \left( 1 + \frac{k}{\sqrt{1 + \frac{4T_{tot}N}{q\pi(AR)^2 c_w^2}}} \right) \quad (1)$$

where  $\theta(c_w)$  represents the turning angle,  $T_{tot}$  is the total thrust, and  $k \approx 1.8$  is the empirical circulation factor.

The solver was initialized with the design constraints of  $N = 6$  propellers, a thrust-to-weight ratio of approximately 0.6, and a flap deflection of  $17.5^\circ$ . The algorithm converged on a required wing chord of **0.096 m**. This deterministic sizing ensured that the baseline geometry for subsequent CFD analysis was physically grounded in established powered-lift theory.

Table 1: Assumed Parameters and Inverse Kuhn's Solver Results

Parameter	Value
<i>Assumed Design Parameters</i>	
Aspect Ratio ( $AR$ )	10
Number of Propellers ( $N$ )	6
Propeller Diameter ( $D_p$ )	0.16 m
Thrust per Propeller (APC Data)	12.5 N
Flap-to-Wing Chord Ratio ( $c_f/c_w$ )	0.30
Angle of Attack ( $\alpha$ )	45°
Flap Deflection ( $\delta_f$ )	17.5°
Target Lift Coefficient ( $C_L$ )	8.0
Freestream Velocity ( $V_\infty$ )	20 m/s
<i>Inverse Formulation Results</i>	
Calculated Wing Chord ( $c_w$ )	<b>0.0961 m</b>
Verified ( $C_L$ ) using direct Kuhn	<b>8.48</b>
Resulting Wing Span ( $b$ )	0.961 m
Wing Planform Area ( $S$ )	0.0924 m <sup>2</sup>
Total Thrust Required ( $T_{tot}$ )	75.0 N

## Transition to End-Term Work

Building upon the analytical foundations, the end-term phase focused on refining the aerodynamic configuration using high-fidelity CFD simulations. Major developments included:

- Selection of the S1223 airfoil for superior powered-lift performance and enhanced slipstream responsiveness.
- Implementation of a full CFD workflow in ANSYS Fluent, including blown-wing domain construction, slipstream modelling using velocity inlets, and mesh refinement.
- Parametric variation of angle of attack (0°–50°), flap deflection (0°–21°), and effective slipstream velocity (45.8 m/s for take-off, 87.6 m/s for cruise).
- Use of a half-wing symmetry model, with all aerodynamic forces scaled by a factor of two to obtain full-span performance estimates.

These simulations established the aerodynamic behaviour across the operating envelope and validated the ability of the selected geometry to meet the STOL performance targets.

The combined analytical modelling, inverse-design methodology, and high-fidelity CFD simulations culminate in a validated and computationally efficient powered-lift wing configuration. The final design—featuring a high-aspect-ratio wing, S1223 airfoil, single-slotted flap, and distributed slipstream energisation—demonstrates the capability to reliably achieve the extreme lift coefficients required for STOL operation while maintaining aerodynamic efficiency and structural practicality. The methodology presented in this report forms a comprehensive and extensible framework for the continued

development of blown-wing aircraft and establishes a strong foundation for prototype fabrication, propulsion integration, and experimental flight testing.

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## List of Symbols

<b>Symbol</b>	<b>Description</b>
$C_L$	Coefficient of lift (dimensionless)
$C_D$	Coefficient of drag (dimensionless)
$\rho$	Air density
$V$	Free stream velocity
$\alpha$	Angle of attack
$T$	Thrust
$W$	Weight
$\eta_p$	Propeller Efficiency
$\theta$	Angle of Deflection of Slipstream
$\delta_f$	Flap Deflection Angle
$\theta_w$	Slipstream Deflection by Wing
$\theta_f$	Slipstream Deflection by Flap
$N$	Number of Propellers
$S_p$	Propeller Disc Area
$AR$	Aspect Ratio
$q$	Freestream Dynamic Pressure
$c_w$	Length of wing chord
$c_f$	Length of flap chord
$\delta_e$	Angle between the trailing edge and the thrust direction
$D$	Diameter of the propeller

## I Introduction

With rapid advancements in air mobility and a growing demand for sustainable, short-range transportation, there is an increasing need for aircraft capable of operating from constrained take-off and landing areas. Applications within **Urban Air Mobility (UAM)** and **Regional Air Mobility (RAM)** demand configurations that can generate high lift at low airspeeds while maintaining safety, efficiency, and compactness. Conventional fixed-wing aircraft depend on long runways to build the required dynamic pressure for lift generation and are therefore unsuitable for dense urban environments or short-field operations.

To overcome these limitations, next-generation aircraft must rely on **powered-lift configurations**, where the propulsion system directly enhances aerodynamic performance. Lift augmentation methods can be classified broadly into passive techniques (such as high-lift airfoils, flaps, and slats) and active techniques (e.g., blown flaps, circulation control wings, and Upper Surface Blowing). Among these, **Externally Blown Flap (EBF)** and **Upper Surface Blowing (USB)** configurations are particularly compatible with modern electric propulsion systems. In such systems, the propeller slipstream flows over the upper surface of the wing and flap, adhering to the contour due to the **Coandă effect**. This results in augmented circulation, delayed flow separation, and significantly enhanced lift coefficients—capabilities critical for Short Take-Off and Landing (STOL) operations.

A key refinement introduced in the final design is the replacement of the Eppler E423 airfoil with the **Selig S1223** profile. While the E423 performs well at low Reynolds numbers, preliminary mapping showed that the S1223 offers superior maximum lift capabilities and maintains attached flow at much higher angles of attack. This property is essential for exploiting the full potential of blown-wing lift augmentation, especially under aggressive take-off conditions where geometric angles of attack may reach  $40^\circ$ – $50^\circ$ .

Additionally, due to topological meshing limitations encountered during the mid-term CFD studies, the computational framework was transitioned to **ANSYS Fluent**. This migration enabled advanced hybrid meshing strategies and more robust solver configurations capable of accurately capturing the complex propeller–wing interaction physics. The final-phase simulations presented in this report utilize these enhanced modelling capabilities and provide high-fidelity validation of the aerodynamic performance of the EBF–DEP configuration.

In summary, this report builds upon the conceptual foundation established in the mid-term phase and presents a refined aerodynamic design incorporating advanced airfoil selection, improved modelling methodology, and validated high-lift performance. The objective remains consistent: to demonstrate that a distributed electric blown-wing system can meet the stringent lift requirements of a STOL aircraft while operating at low air velocities and without relying on mechanically complex VTOL architectures.

## 2 Concept Overview

The core objective of this problem statement is to design a wing for a **fixed-wing aircraft** capable of achieving **Short Take-Off and Landing (STOL)** performance through **powered lift**, without relying on **Vertical Take-Off and Landing (VTOL)** systems or complex tilting mechanisms. In this design, the **propulsion system** is not limited to providing thrust alone but also contributes directly to the generation of **aerodynamic lift**. This integration fundamentally changes the conventional approach to aircraft design, requiring the propulsion and aerodynamic systems to be treated as a **coupled unit** rather than independent components. The design therefore aims to achieve **high lift performance** during take-off and landing while maintaining **efficient cruise characteristics**, ensuring that the aircraft performs effectively across distinct flight regimes.

At low flight speeds, the available airflow energy is limited, and the boundary layer tends to separate easily, resulting in stall and a rapid loss of lift. Conventional aerodynamic techniques such as larger wings, high-lift flaps, or slats can only achieve maximum lift coefficients in the range of about **2 to 3**, which is insufficient for the lift demands of STOL operations. Increasing the wing area to overcome this limitation leads to higher drag and reduced cruise efficiency. Hence, a **powered-lift approach** that enhances lift aerodynamically without structural or mechanical complications offers a more effective and efficient solution.

The **blown-wing concept** satisfies this requirement by using **propeller or EDF slipstreams** to energize the airflow over the wing and flaps, delaying flow separation and significantly increasing lift. In this configuration, multiple small propellers are distributed along the wing's leading edge, producing a uniform, accelerated airflow across the lifting surface. This interaction enables high lift during take-off and landing while maintaining efficient cruise characteristics. The **distributed propellers** also provide improved control through **differential thrust**, enhancing handling qualities during low-speed operations. Overall, the blown-wing configuration provides the most practical and aerodynamically efficient solution to meet the problem statement's objectives, merging the advantages of **distributed electric propulsion** and **powered lift** without the structural complexity of VTOL systems.

### 2.1 Design Targets and Performance Requirements

- **Lift Coefficient Target ( $C_L$ ): 6.5**, with an operational **envelope of 6–8**.
- **Lift-to-Drag Ratio (L/D):**
  - $> 5$  with flaps deployed (take-off and landing modes)
  - $> 20$  without flaps (cruise mode)
- **Minimum Lift: 15 kg at 20 m/s** freestream velocity.
- **Freestream Velocity Requirements:**
  - With flaps deployed (**take-off mode**): **20 m/s**

- With flaps retracted (**cruise mode**): **80 m/s**
- **Thrust-to-Weight Ratio Envelope:** **0.5 < T/W < 0.65**
- **Minimum Take-Off Weight (MTOW):** **15 kg**

## 3 Rationale for Using Propellers Instead of Electric Ducted Fans

The choice of propulsive device is a critical aspect of any powered-lift or distributed electric propulsion (DEP) aircraft concept. For the present STOL configuration, **propeller-based propulsion** has been selected over electric ducted fans (EDFs) due to superior aerodynamic efficiency, lift augmentation potential, and integration feasibility in low-speed flight regimes. The following subsections outline the rationale behind this selection.

### 3.1 Aerodynamic Rationale

Propeller-driven systems are inherently more effective in low-speed, high-lift flight regimes. A propeller accelerates a relatively large mass of air at a moderate velocity, producing a broad, low-speed slipstream that interacts favorably with the wing and flap surfaces. This distributed slipstream energizes the boundary layer, delays flow separation, and significantly increases the effective lift coefficient ( $C_{L,max}$ )—a primary requirement for powered-lift and STOL performance.

In contrast, EDFs accelerate a smaller mass of air to high velocity, resulting in a narrow jet with limited lateral spread. Such a jet contributes primarily to thrust rather than lift, as the localized high-velocity core does not provide uniform blowing across the wing span. Consequently, EDFs are aerodynamically better suited to compact, high-speed applications where lift augmentation is secondary to thrust generation, whereas propellers are optimal for distributed, low-speed, lift-enhancing configurations.

### 3.2 Power and Efficiency Considerations

Propeller systems exhibit higher propulsive efficiency in low- to moderate-speed flight due to their lower disk loading and higher mass flow per unit thrust. Propellers satisfy this condition by imparting small velocity increments over large disk areas, minimizing kinetic energy losses in the slipstream. This makes them ideal for electrically powered aircraft, where available energy and power density are constrained.

EDFs, by comparison, operate with very high disk loading and require substantially greater shaft power to produce equivalent thrust. Their smaller fan diameters and higher rotational speeds increase mechanical losses and motor heating, leading to reduced overall system efficiency and more complex thermal management. These factors make EDFs less favorable for DEP systems designed for low-speed, high-lift operations.

### 3.3 Integration with the Wing and DEP System

In distributed propulsion architectures, propellers integrate naturally with the wing structure. Multiple small-diameter propellers distributed along the wing span generate overlapping slipstreams, producing nearly continuous blowing across the wing and flap surfaces. This configuration enhances both lift and control authority while ensuring consistent boundary-layer re-energization during take-off and landing.

Conversely, EDFs produce highly concentrated, high-speed jets with limited spatial coverage, yielding negligible aerodynamic interaction with the wing surface. As a result, EDF-based configurations would fail to provide effective lift augmentation or flow control, defeating the purpose of a blown-wing powered-lift concept.

### 3.4 Structural and Operational Advantages

Propeller systems offer several mechanical and operational advantages. They are structurally simpler, lighter, and more adaptable to modular DEP configurations, where each unit can be driven by an independent electric motor and controller. This distributed architecture inherently provides redundancy and fault tolerance, enhancing flight safety.

EDFs, on the other hand, operate at very high rotational speeds, imposing greater centrifugal and vibrational loads on nacelles and mounts. They require tighter manufacturing tolerances, precise balancing, and often active cooling systems, increasing both mechanical complexity and system mass. Additionally, EDFs generate higher noise levels compared to propellers, which may pose challenges for urban or small-airfield operations.

### 3.5 Design Suitability and Mission Alignment

The mission objectives of the present design emphasize low-speed, high-lift performance, efficient power utilization, and smooth aerodynamic integration with the wing. Propellers directly support these goals by producing large, slow-moving slipstreams that effectively energize the airflow over the wing and flap systems, thereby achieving substantial lift augmentation with limited power expenditure.

In contrast, EDFs, though compact and capable of generating high static thrust, are optimized for higher-speed aerodynamic conditions. Their high exit velocities and limited mass flow yield poor lift augmentation and low propulsive efficiency at low speeds. Therefore, propeller-driven distributed electric propulsion provides a more effective and energy-efficient solution aligned with the performance and mission requirements of the current STOL design.

## 4 Overview of Passive Lift-Augmentation Methods

Passive lift-augmentation techniques rely on geometric modification and aerodynamic shaping of the lifting surfaces to enhance lift without the addition of external energy to the flow. These methods

improve aerodynamic performance by manipulating pressure distribution, delaying flow separation, and increasing effective camber or circulation around the airfoil. The primary categories of passive lift-augmentation mechanisms include **leading-edge devices**, **trailing-edge flaps**, **boundary-layer control through surface shaping**, and **high-lift airfoil design**.

#### 4.1 Leading-Edge Devices

Leading-edge devices, such as slats, Krueger flaps, and drooped noses, are designed to increase the stall angle of attack and maintain attached flow over the upper surface at high lift coefficients. A leading-edge slat functions by forming a slot that re-energizes the boundary layer on the main wing, delaying flow separation during high angles of attack. The circulation and lift increase arise from improved pressure recovery and boundary-layer mixing. Typical lift coefficients for multi-element configurations using leading-edge slats can reach  $C_L \approx 2.5\text{--}3.0$  before stall. However, their mechanical deployment and limited incremental lift relative to powered methods restrict their suitability for extreme STOL applications.

#### 4.2 Trailing-Edge High-Lift Systems

Trailing-edge devices, including single, double, and triple-slotted flaps, increase the effective camber and chord of the wing, significantly augmenting lift during take-off and landing. Multi-element flaps exploit the pressure recovery between successive elements to achieve high lift coefficients while maintaining attached flow. For instance, a triple-slotted Fowler flap system can achieve  $C_L$  values up to 4 in clean aerodynamic conditions. Nevertheless, these systems provide diminishing returns as flap deflection increases beyond the point of flow separation. In addition, they rely entirely on aerodynamic camber effects, limiting their lift-to-drag performance in low-speed STOL regimes where flow energy addition becomes essential.

#### 4.3 Boundary-Layer Shaping and Vortex Generators

Passive boundary-layer control methods such as vortex generators, fences, and microvanes introduce controlled streamwise vortices that enhance momentum exchange within the boundary layer. These devices delay separation and improve flow attachment, particularly on swept or highly cambered surfaces. Although they effectively improve stall characteristics and enhance controllability, their lift increment is modest ( $\Delta C_L \approx 0.2\text{--}0.3$ ), and they introduce additional parasitic drag at cruise conditions.

#### 4.4 High-Lift Airfoil Design

Airfoil sections specifically optimized for high-lift performance employ increased maximum camber, favorable pressure recovery characteristics, and delayed transition to turbulence. These designs, such as the NACA 230 series and NASA SC(2)-series high-lift profiles, provide inherent lift advantages without movable elements. However, the lift augmentation achieved is static and limited compared

to configurations employing flow energization. Furthermore, the high camber required for passive lift enhancement often results in adverse drag penalties at cruise.

#### 4.5 Summary of Passive Approaches

In summary, passive lift-augmentation methods enhance lift through aerodynamic shaping, boundary-layer control, and camber manipulation, but their performance is constrained by flow separation at large angles of attack. While effective for conventional take-off and landing operations, they are insufficient for achieving the high lift coefficients ( $C_L > 5$ ) required in advanced short take-off and landing (STOL) or distributed propulsion concepts. Consequently, the present study explores **active lift-augmentation techniques**-methods that introduce additional energy or momentum into the flow-to achieve substantially higher lift performance. The various active techniques are discussed in the following section.

### 5 Overview of Active Lift-Augmentation Methods

Active lift-augmentation techniques are employed to increase the effective lift coefficient of an aircraft beyond that attainable by conventional high-lift devices. These methods utilize the addition of energy or momentum into the flow field to delay separation, enhance circulation, or redirect momentum. The principal active lift-augmentation concepts developed for powered-lift and short take-off and landing (STOL) aircraft include **mechanical thrust vectoring**, **upper-surface blowing (USB)**, **circulation control (CC)**, and **externally blown flaps (EBF)**. Each method operates on a distinct aerodynamic principle and presents different trade-offs in complexity, efficiency, and compatibility with distributed propulsion systems.

#### 5.1 Mechanical Thrust Vectoring

Mechanical thrust vectoring achieves lift augmentation by physically deflecting the propulsion system or nozzle to impart a vertical component of thrust. The thrust vector is typically varied by rotating propeller pods, swiveling jet nozzles, or using movable vanes in the exhaust stream. This method can produce very high lift coefficients, particularly in vertical or short take-off configurations (e.g., the Harrier and F-35B). However, the approach introduces significant mechanical complexity, mass penalties, and control-coupling challenges. In addition, thrust vectoring violates fixed-thrust-axis constraints in conventional STOL design frameworks, making it unsuitable for applications where propulsion alignment must remain fixed.

#### 5.2 Upper-Surface Blowing (USB)

The upper-surface blowing concept directs a high-speed jet or propeller slipstream over the upper surface of the wing, where it adheres to the contour due to the Coandă effect. The attached jet entrains ambient air and accelerates the boundary layer, delaying flow separation and increasing suction on

the upper surface. USB systems, exemplified by the Boeing YC-14 and NASA QSRA, are capable of substantial lift augmentation with moderate mechanical complexity. However, they exhibit strong viscous interactions, high thermal loads in the case of jet exhausts, and complex flow curvature that complicates analytical modeling. The sensitivity of the Coandă attachment to Reynolds number and jet temperature also limits its scalability to propeller-driven systems.

### 5.3 Circulation Control (CC)

Circulation control utilizes a high-velocity jet, typically ejected tangentially through a Coandă-shaped trailing-edge surface, to enhance lift by augmenting circulation around the airfoil. The continuous blowing over the curved trailing edge effectively delays separation and increases the lift coefficient without requiring large mechanical deflections. Despite its aerodynamic effectiveness, the system demands a continuous supply of high-pressure air, complex internal ducting, and precise flow control, leading to significant penalties in system integration, weight, and energy efficiency—particularly in electrically powered aircraft architectures.

### 5.4 Externally Blown Flap (EBF)

In the externally blown flap configuration, the propeller or fan slipstream impinges directly on a deflected flap located downstream of the propeller disk. The slipstream is turned downward by the flap, generating lift through the vertical projection of the deflected momentum and by increasing the dynamic pressure on the wing's aft sections. The EBF concept provides lift augmentation primarily through aerodynamic momentum redirection rather than mechanical vectoring, making it inherently compatible with fixed-thrust-axis constraints. Historically, this mechanism has demonstrated excellent performance in aircraft such as the Breguet 941 and the Doak VZ-4, achieving lift coefficients exceeding five while maintaining attached flow at large flap deflections.

## 6 Justification for Using EBF Concept with DEP

Based on the objectives outlined in the problem statement, the present design adopts an **Externally Blown Flap (EBF)** configuration integrated with **Distributed Electric Propulsion (DEP)**. This combination enables significant lift augmentation through aerodynamic deflection of propeller slipstreams while maintaining a fixed-wing layout and avoiding the mechanical complexity of thrust vectoring or tilt mechanisms.

### 6.1 Physical Basis of the Externally Blown Flap Mechanism

In the externally blown flap concept, the propeller slipstream passes directly over the wing trailing-edge region and impinges on a deflected flap. The high-momentum slipstream is turned downward by the flap, thereby generating an additional lift component through the vertical projection of the deflected

momentum and by increasing the effective dynamic pressure on the flap and aft wing sections. The total lift increment may be expressed as

$$\Delta L = \dot{m}(V_2 \sin \theta - V_\infty \sin \alpha), \quad (2)$$

where  $\dot{m}$  is the mass flow through the propeller disk,  $\theta$  is the slipstream turning angle, and  $\alpha$  is the angle of attack. This mechanism enhances lift both by direct momentum deflection and by augmenting the circulation on the aft portion of the wing.

Because the slipstream remains attached and energized over the flap surface, the externally blown configuration delays separation at high flap deflections, enabling high lift coefficients ( $C_L > 5$ ) without flow detachment-behavior consistent with historical STOL aircraft such as the Breguet 941 and NASA QSRA.

## 6.2 Advantages of Externally Blown Flaps with Distributed Propulsion

The use of distributed electric propulsion (DEP) significantly amplifies the aerodynamic benefits of the externally blown flap concept. Multiple small-diameter propellers positioned along the wing span generate overlapping slipstreams that uniformly energize the boundary layer, producing a quasi-continuous distribution of high-momentum flow over the flap. This provides several key advantages:

- **Enhanced lift augmentation:** The distributed slipstreams increase the effective dynamic pressure over a larger spanwise extent of the wing, reducing spanwise lift gradients and improving overall lift effectiveness.
- **Boundary-layer re-energization:** Continuous propeller slipstreams prevent flow separation at high flap deflections by maintaining attached flow and increasing local Reynolds number, thus extending the linear lift regime.
- **Improved control authority and redundancy:** DEP enables fine-grained thrust modulation across the span, offering differential thrust control for pitch, roll, and yaw moments without the need for mechanical vectoring surfaces.
- **System integration:** Electrically driven propellers allow for compact nacelle design, reduced mechanical complexity, and flexible placement close to the wing surface-conditions favorable for maximizing slipstream–flap interaction efficiency.

Furthermore, the EBF-DEP configuration avoids the high thermal loads and viscous jet–surface interactions characteristic of upper-surface blowing (USB) and the mechanical complexity of thrust-vectoring systems. It retains the core advantage of powered lift through aerodynamic momentum redirection while remaining mechanically simple and energy-efficient within the distributed electric propulsion framework.

The EBF–DEP architecture therefore represents the optimal compromise between aerodynamic performance and system integration. It provides the lift augmentation benefits of a powered-lift system

without violating non-thrust-vectoring constraints, while remaining compatible with distributed electric propulsion and scalable for various wing spans and power loadings.

## 7 Theoretical Models for EBF Systems

This section details the theoretical models used to analyze the Propeller Blown Wing / Externally Blown Flap (EBF) configuration. The analysis begins with foundational analytical and semi-empirical formulations and transitions to the modern non-dimensional parameters used for computational analysis.

### 7.1 Foundational Analytical and Semi-Empirical Models

A number of analytical and semi-empirical formulations have been developed to predict the aerodynamic characteristics of EBF configurations. These models attempt to represent the complex aerodynamic interaction between the propeller slipstream, the wing, and the deflected flap using simplified physical assumptions.

#### 7.1.1 Simple Momentum Theory Models

The earliest mathematical representations of EBF aerodynamics were based directly on momentum theory. The propeller slipstream is treated as a jet with uniform velocity  $V_s$  and mass flow rate  $\dot{m}$ , which impinges on a flap and is deflected by an angle  $\theta$ . The resulting change in vertical momentum produces an incremental lift force:

$$\Delta L = \dot{m}(V_s \sin \theta - V_\infty \sin \alpha), \quad (3)$$

while the longitudinal force (drag) increment is

$$\Delta D = \dot{m}(V_\infty \cos \alpha - V_s \cos \theta). \quad (4)$$

This formulation, sometimes referred to as the *deflected slipstream model*, provides first-order estimates but neglects aerodynamic coupling and pressure distribution changes.

#### 7.1.2 Spence's Jet Flap Theory

To capture the circulation augmentation produced by the slipstream, Spence's Linear Jet Flap Theory [1] provides a baseline 2D estimate. This model predicts the sectional lift coefficient ( $c_l$ ) as a function of angle of attack ( $\alpha$ ), flap deflection ( $\delta_F$ ), and jet momentum ( $c_J$ ).

- **Combined Lift Formula:**

$$c_l = \frac{\partial c_l}{\partial \alpha} \alpha + \frac{\partial c_l}{\partial \delta_F} \delta_F \quad (5)$$

- **Lift-Curve Slope (Effect of  $\alpha$ ):**

$$\frac{\partial C_L}{\partial \alpha} = 2\pi(1 + 0.151\sqrt{C_J} + 0.219C_J) \quad (6)$$

- **Flap Effectiveness (Effect of  $\delta_F$ ):**

$$\frac{\partial C_L}{\partial \delta_F} = 2\sqrt{\pi C_J}(1 + 0.151\sqrt{C_J} + 0.139C_J)^{\frac{1}{2}} \quad (7)$$

Note: This linear theory tends to over-predict lift as it assumes 100% efficient flow turning, but it serves as a foundational theoretical model for circulation-enhanced flow [2].

### 7.1.3 Jet-Turning Control-Volume Models (Sforza, 1966)

Sforza introduced a more comprehensive control-volume model by explicitly evaluating the turning of the slipstream [3]. The approach considers the conservation of mass, momentum, and energy. The lift and drag coefficients are expressed as:

$$C_L = C_{L,\text{off}} + \frac{2T}{\rho V_\infty^2 S} \sin(\theta + \alpha), \quad (8)$$

$$C_D = C_{D,\text{off}} + \frac{2T}{\rho V_\infty^2 S} (1 - \cos(\theta + \alpha)), \quad (9)$$

where  $T$  is the total propeller thrust. This formulation connects the lift increment to thrust and turning angle but neglects viscous losses.

### 7.1.4 Brown and Michael (1961) Semi-Empirical Model

Brown and Michael proposed a semi-empirical model that separates the lift contribution into pressure-induced and momentum-induced components [4]. The incremental lift coefficient is given by:

$$\Delta C_L = \frac{2C'_T}{S/S_p} \sin(\theta + \alpha) + \beta \frac{S_p}{S} \left( \frac{V_s}{V_\infty} - 1 \right), \quad (10)$$

where  $C'_T$  is the nondimensional thrust coefficient,  $S_p$  the propeller disk area, and  $\beta$  an empirical coupling parameter.

### 7.1.5 Kuhn's Semi-Empirical Model (1959)

The present study uses the semi-empirical model developed by Richard E Kuhn of NASA Langley Research Center. Below, we present a theoretical background of the Model and justify our rationale for selecting it to model our powered wing design.

Aerodynamic modelling of powered or blown wings requires capturing the coupled effects of the propeller slipstream, lifting surface, and high-lift devices with computational efficiency. The semi-

empirical method of Richard E. Kuhn [5] provides an optimal balance of accuracy and efficiency, using momentum theory with empirical corrections to predict lift and longitudinal forces in deflected-slipstream conditions.

### Physical Relevance and Underlying Theory

Kuhn's method models the total aerodynamic forces as the sum of contributions from three distinct flow components:

1. the conventional power-off wing,
2. the direct lift generated by thrust deflection, and
3. the incremental lift arising from increased mass flow through the slipstream.

Kuhn's method represents a major advancement in EBF modeling by combining the momentum-balance approach with empirical correction factors derived from extensive NASA and NACA experimental data. The governing equation for the lift coefficient is expressed as:

$$C_L = C_{L,\text{off}} + F_T C'_T \sin(\theta + \alpha) + k F_T C'_T \sin(\theta + \alpha) \left( 1 + \frac{C'_T S}{N S_p} \right)^{-1/2} \quad (\text{II})$$

where  $F_T$  is the thrust recovery factor,  $k$  an empirically determined constant (typically  $k \approx 1.8$ ),  $N$  the number of propellers, and other symbols retain their previous meanings. This method captures both the direct thrust deflection and the mass-flow increase effects, providing accurate lift predictions (within 10% of experimental data) across a wide range of flap deflections and thrust coefficients. Kuhn's model remains the standard semi-empirical reference for deflected-slipstream systems.

### Validation and Applicability to the Present Configuration

Kuhn's method has been extensively validated for propeller-driven short take-off and landing (STOL) aircraft, notably the *Breguet 941*, which features large-chord triple-slotted flaps positioned within the slipstream of four turboprop engines. The semi-empirical predictions demonstrated less than 10% deviation in lift coefficients when compared with both wind-tunnel and flight data up to the onset of stall. Similar validation against CFD simulations for the NASA *X-57 Maxwell* distributed electric propulsion configuration further confirmed the robustness of the approach for modern distributed-propulsion systems.

Given that the present powered wing concept exhibits a comparable aerodynamic mechanism-lift enhancement through propeller slipstream deflection over a high-lift flap system-Kuhn's method provides a reliable means to predict performance across a wide range of thrust coefficients, flap deflections, and propeller geometries.

## Computational Efficiency and Suitability for Conceptual Design

High-fidelity CFD or vortex-lattice coupling methods, though accurate, are computationally prohibitive for conceptual design iterations. Kuhn's framework, being semi-empirical, requires only basic geometric and thrust parameters ( $c_f/D, \delta_f, C'_T, \theta, F_T$ ) to yield rapid estimations of  $C_L$  and  $C_D$ . This allows efficient trade-off analysis between propeller placement, flap sizing, and distributed thrust levels—an essential requirement in early-stage design of powered-wing systems.

The semi-empirical formulation proposed by Kuhn has been widely adopted in preliminary aircraft design tools for the estimation of powered-lift aerodynamic characteristics. It has also been successfully implemented in professional aircraft design environments such as the Onera–Airbus–Dassault (OAD) Aircraft Design Software (ADS), demonstrating its suitability for integration within conceptual design frameworks.

## Known Limitations and Controlled Assumptions

Kuhn's method assumes fully developed slipstream at the wing and neglects swirl-induced spanwise flow effects. It is valid primarily in the unstalled regime, where the flow remains attached over the flaps. Despite these simplifications, the method accurately reproduces key aerodynamic trends, including:

- the linear increase of  $C_L$  with thrust coefficient up to stall,
- the shift in effective lift curve slope due to slipstream momentum addition, and
- the degradation of longitudinal force prediction at very high deflections.

These limitations are well characterized and manageable within the design space of the current study.

## 8 Airfoil Selection Criteria

The selection of a suitable airfoil is a critical step in the aerodynamic design of a low-Reynolds-number, high-lift UAV wing. The performance of an airfoil directly influences stall speed, takeoff distance, and overall aerodynamic efficiency. Since the operating Reynolds number for our wing (chord = 0.096 m) ranges from approximately  $1.5 \times 10^5$  during takeoff to  $3 \times 10^5$  at cruise, the chosen airfoil must perform effectively within the low-Reynolds-number regime while providing high lift.

To identify the most suitable candidate, four high-lift airfoils were evaluated:

- Eppler E216
- Eppler E423
- NACA 4415
- Selig S1223

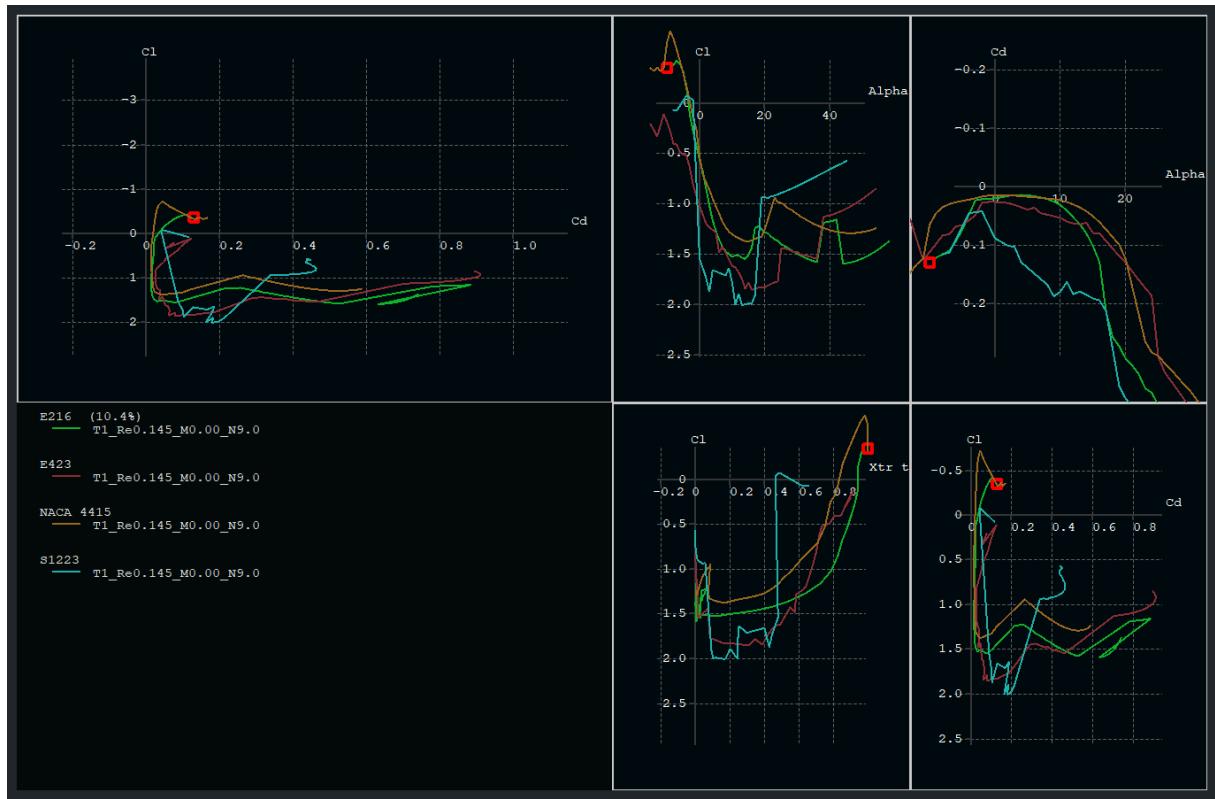


Figure 1: Airfoil Polars

These airfoils were analyzed in XFLR<sub>5</sub> using 2D viscous analysis at  $Re = 1.5 \times 10^5$ , representing the critical low-speed takeoff condition. The results compared lift coefficient ( $C_L$ ), drag coefficient ( $C_D$ ),  $C_L - C_D$  performance, and stall behavior across varying angles of attack. The comparison plots from the analysis are shown in figure 1.

## 8.1 Selection Criteria Used

### 8.1.1 High Lift Coefficient ( $C_{L \max}$ ) at Low Reynolds Numbers

The wing must produce sufficient lift at low speed to enable STOL (Short Takeoff and Landing) capability. Thus, the primary criterion is a high  $C_{L \max}$  at  $Re \approx 1.5 \times 10^5$  (takeoff condition).

From the analysis:

- **S1223** exhibits the highest  $C_L$  across the entire  $\alpha$  range, achieving significantly greater lift than E423 and NACA 4415.
- **E423** shows good performance but remains consistently below S1223.
- **NACA 4415** and **E216** underperform at low Reynolds numbers.

### 8.1.2 $C_L/C_D$ Ratio (Aerodynamic Efficiency)

Efficient climb and cruise require low drag for a given lift.

- **SI223** demonstrates the best  $C_L/C_D$  ratio, indicating high aerodynamic efficiency even at low airspeeds.
- **E423** shows reasonable efficiency but remains inferior to **SI223**.
- **NACA 4415** and **E216** generate higher drag and lower aerodynamic efficiency.

### 8.1.3 Stall Characteristics

The airfoil should maintain lift up to moderate angles of attack and exhibit a gradual stall.

- **SI223** shows delayed stall around  $15^\circ$  and smoother loss of lift.
- **E423** exhibits a sharper stall, especially at low Reynolds numbers.
- **E216** and **NACA 4415** stall earlier and more abruptly.

### 8.1.4 Suitability for Flaps and Powered-Lift (DEP)

Since the final design integrates distributed electric propulsion (DEP) and flap-based high-lift augmentation, the airfoil must respond well to additional camber.

- **SI223** is widely documented as being highly responsive to flap deflection and blown-air (prop-wash) due to its large camber and thickness.
- **E423** performs well with high-lift devices but offers lower baseline lift compared to **SI223**.
- **NACA 4415** and **E216** offer limited margin for further lift enhancement.

## 8.2 Final Airfoil Selection: Selig SI223

Based on the above criteria and the comparative analysis results, the **Selig SI223 airfoil** is selected for the wing.

### Justification

- It provides the highest lift coefficient at the operating Reynolds numbers.
- It demonstrates the best lift-to-drag ratio, crucial for takeoff and climb performance.
- It offers superior stall behavior, with gradual stall and delayed separation.
- It is specifically designed for low-Re, high-lift UAV applications, making it well-suited for our STOL and DEP-augmented concept.

Thus, the **SI223** represents the optimal choice for achieving both maximum lift during takeoff and good aerodynamic efficiency during cruise, making it the best airfoil for the mission requirements.

## 9 Hyperparameter Study

To rapidly narrow down the expansive design space of the blown wing, a semi-empirical hyperparameter study was conducted prior to high-fidelity CFD simulations. While Reynolds-Averaged Navier-Stokes (RANS) simulations provide high accuracy, their computational cost makes them unsuitable for evaluating thousands of geometric and propulsive combinations.

To address this, a Python-based iterative solver was developed based on **Kuhn's Method** for powered lift. This approach allowed for the screening of over 36,000 design configurations to identify a geometry that satisfies both efficient cruise and high-lift takeoff requirements.

### 9.1 Theoretical Framework: Kuhn's Semi-Empirical Method

Kuhn's method is a momentum-theory-based approach used to estimate the lift and drag increments due to propeller slipstream interaction with flaps. Unlike potential flow theory which neglects viscosity, Kuhn's method incorporates empirical factors derived from wind tunnel testing to account for the complex interaction between the high-velocity slipstream and the flap surface.

The core principle relies on separating the aerodynamic forces into three distinct components: the power-off wing aerodynamics, the direct thrust contribution, and the "super-circulation" lift augmentation. The total lift coefficient ( $C_{L,tot}$ ) is calculated as:

$$C_{L,tot} = C_{L,off}(\alpha) + \left(\frac{F}{T}\right) C'_T \sin(\theta + \alpha) + k \left(\frac{F}{T}\right) C'_T \sin(\theta + \alpha) \frac{I}{\sqrt{I + \frac{C'_T S}{NS_p}}} \quad (12)$$

Where:

- $C_{L,off}$ : Baseline lift of the wing-flap system (derived from Xfoil polars of the S1223 airfoil).
- $\theta$ : The total turning angle of the slipstream, which is a function of the flap deflection ( $\delta_f$ ) and the flap-to-wing chord ratio ( $c_f/c_w$ ).
- $F/T$ : The **Thrust Recovery Factor**, an empirical efficiency metric that accounts for turning losses and friction. It typically decreases as the turning angle increases.
- $C'_T$ : The thrust coefficient based on the free-stream dynamic pressure.
- $NS_p$ : The total actuator disk area of the distributed propellers.

The method explicitly checks for flow separation by comparing the calculated turning angle ( $\theta$ ) against a physical maximum limit ( $\theta_{max}$ ). If the required turning exceeds this limit, the configuration is flagged as stalled.

## 9.2 Methodology: Mission-Profile-Driven Optimization

A distinct feature of this study was the implementation of a *Mission-Profile Optimization* strategy. Rather than optimizing solely for maximum lift at takeoff, the algorithm enforces a rigorous two-stage filter to ensure the wing remains efficient during the cruise phase.

The hyperparameter search space was expanded to cover aggressive high-lift conditions for takeoff and efficient low-alpha conditions for cruise:

- **Aspect Ratio (AR):** 7, 7.5, 8, 8.5, 9, 9.5, 10
- **Wing Chord ( $c_w$ ):** 0.065 m to 0.100 m
- **Propeller Count ( $N$ ):** 6, 8
- **Flap Deflection ( $\delta_f$ ):** 0° to 60° (in 5° increments)
- **Takeoff AoA ( $\alpha_{TO}$ ):** 10°, 15°, 20°, 25°, 30°, 35°, 40°, 45°, 50°
- **Cruise AoA ( $\alpha_{Cr}$ ):** 0°, 0.5°, 1.0°, 1.5°, 2.0°, 2.5°, 3.0°, 3.5°, 4.0°

The optimization logic proceeded as follows:

### 9.2.1 Stage 1: Cruise Feasibility Check

Before evaluating the complex powered-lift mechanics, the algorithm first verifies if the candidate wing geometry can sustain level flight at the design cruise velocity ( $V_{cruise} = 80$  m/s) while meeting the high-efficiency target. This acts as a preliminary filter to discard geometries that are fundamentally inefficient.

1. **Required Lift Coefficient ( $C_{L,cruise}$ ):** For steady level flight, lift must equal the aircraft weight ( $W = 15$  kg). The required lift coefficient is derived from the standard lift equation:

$$C_{L,cruise} = \frac{2W}{\rho V_{cruise}^2 S} \quad (13)$$

where  $\rho$  is the air density at altitude and  $S$  is the wing planform area for the specific geometry being tested.

2. **Total Drag Estimation ( $C_{D,cruise}$ ):** The total drag coefficient is estimated by summing the parasitic and induced drag components:

$$C_{D,cruise} = C_{d,profile} + C_{D,induced} = C_{d,profile}(\alpha) + \frac{C_{L,cruise}^2}{\pi \text{totAR} \cdot e} \quad (14)$$

- **Profile Drag ( $C_{d,profile}$ ):** This value is obtained by interpolating the 2D polars of the Si223 airfoil (simulated in Xfoil) at the calculated  $C_{L,cruise}$ . It accounts for skin friction and pressure drag on the airfoil section.

- **Induced Drag ( $C_{D,induced}$ ):** This term accounts for the drag penalty caused by the generation of lift over a finite wing span (wingtip vortices). It is inversely proportional to the Aspect Ratio ( $AR$ ), meaning higher aspect ratio wings pay a smaller drag penalty for the same amount of lift. The Oswald efficiency factor ( $e$ ) was assumed to be 0.8 for this conceptual phase.
3. **Efficiency Filter:** The final Lift-to-Drag ratio is calculated as  $(L/D)_{cruise} = C_{L,cruise}/C_{D,cruise}$ . Any geometry yielding an  $(L/D)_{cruise} < 20$  is flagged as inefficient and immediately discarded from the study, preventing computational resources from being wasted on designs that cannot meet range requirements.

### 9.2.2 Stage 2: Takeoff Optimization (Kuhn's Loop)

Geometries that successfully passed the cruise efficiency filter were subsequently subjected to a rigorous powered-lift evaluation at takeoff conditions ( $V_{takeoff} = 20 \text{ m/s}$ ). This stage involved a granular sweep over the operational parameters to identify the limits of the high-lift system.

1. **Parameter Sweep:** The solver iterated through a comprehensive range of variables to find the optimal takeoff configuration:
  - **Flap Geometry:** The flap chord ratio ( $c_f/c_w$ ) was varied between 0.27 to 0.32 to explore structural limits.
  - **Angle of Attack ( $\alpha_{TO}$ ):** Tested from  $10^\circ$  to  $50^\circ$  to find the maximum usable lift before stall.
  - **Flap Deflection ( $\delta_f$ ):** Tested from  $0^\circ$  to  $21^\circ$  to maximize super-circulation.
2. **Physical Stall Constraint (Theta Limit):** To ensure the predicted lift was physically achievable, the solver utilized imported experimental data defining the maximum possible slipstream turning angle ( $\theta_{max}$ ) as a function of the flap-chord-to-propeller-diameter ratio ( $c_f/D$ ). For every iteration, the calculated total turning angle ( $\theta_{tot}$ ) was compared against this empirical limit. Any configuration where  $\theta_{tot} > \theta_{max}$  was flagged as stalled (flow separation) and immediately discarded. This critical step prevented the selection of mathematically favorable but aerodynamically impossible geometries.
3. **Performance Check:** The solver calculated the final powered lift coefficient ( $C_{L,TO}$ ) and lift-to-drag ratio ( $L/D_{TO}$ ) for all non-stalled cases. A configuration was marked as a "PASS" only if it met the aggressive lift target:

$$C_{L,TO} > 6.5 \quad \text{and} \quad L/D_{TO} > 5.0 \quad (15)$$

### 9.3 Qualitative Trends and Analysis

The hyperparameter study revealed several critical aerodynamic trends that guided the final design selection:

#### 9.3.1 Sensitivity to Flap Chord Ratio ( $c_f/c_w$ )

The simulation data indicated a strong positive correlation between the flap chord ratio and the achievable turning angle ( $\theta$ ). Designs with  $c_f/c_w < 0.25$  struggled to turn the flow sufficiently to generate the target lift. However, ratios exceeding 0.35 frequently violated the  $\theta_{max}$  stall limit in Kuhn's model, leading to massive separation. The chosen ratio of 0.30 (30%) represented the optimal trade-off, maximizing super-circulation without inducing premature stall.

#### 9.3.2 Impact of Distributed Propulsion ( $N = 6$ vs $N = 8$ )

Comparing the 6-propeller and 8-propeller configurations, the  $N = 6$  case provided a more favorable balance for this specific wing span (1 m). While increasing propeller count generally improves the "immersion" of the wing in the slipstream, at this scale,  $N = 8$  resulted in excessively small propeller diameters. Larger propellers ( $N = 6$ ) provided a more coherent slipstream and better thrust recovery factors ( $F/T$ ) in the simulation results.

#### 9.3.3 The Cruise-Takeoff Conflict

A clear Pareto frontier was observed between cruise efficiency and takeoff lift. Lower aspect ratio wings ( $AR < 8$ ) performed exceptionally well in powered lift due to larger chord lengths (accommodating larger flaps). However, these designs consistently failed the Stage 1 cruise filter due to high induced drag. The solver converged on the highest tested Aspect Ratio ( $AR = 10$ ) because it was the only geometric class that could comfortably meet the  $L/D_{cruise} > 20$  requirement while still generating sufficient powered lift through high flap deflections.

### 9.4 Final Configuration Selected for CFD

The hyperparameter study converged on a high-aspect-ratio configuration ( $AR = 10$ ) with a distributed propulsion system of 6 propellers. Based on the highest takeoff  $C_L$  achieved among the valid cruise-capable designs, the geometric parameters listed in Table 2 were frozen for the subsequent high-fidelity CFD analysis.

Table 2: Optimized Wing Configuration from Hyperparameter Study

Parameter	Value
Aspect Ratio ( $AR$ )	10
Wing Span ( $b$ )	0.96 m
Wing Chord ( $c_w$ )	0.096 m
Propeller Count ( $N$ )	6
Flap Chord Ratio ( $c_f/c_w$ )	0.30 (30%)

#### 9.4.1 Limitations and Qualitative Nature of the Study

It is critical to note that the quantitative results of this semi-empirical study, particularly the cruise efficiency metrics, represent an idealized upper bound. A primary limitation was the reliance on power-off polar data for the S1223 airfoil to estimate profile drag ( $C_{d,profile}$ ).

In the actual blown-wing configuration, the interaction between the high-velocity slipstream and the wing surface significantly alters the boundary layer, likely increasing skin friction and pressure drag beyond the values predicted by standard power-off polars. Consequently, the solver predicted exceptionally low drag coefficients, leading to artificially inflated lift-to-drag ( $L/D$ ) ratios during the cruise phase.

Therefore, the hyperparameter study was utilized not as a definitive tool for performance prediction, but as a **qualitative design filter**. Its primary function was to identify geometric families (e.g.,  $AR \approx 10$ ,  $N = 6$ ) that were robust enough to pass both low-speed lift and high-speed efficiency gates, providing a physically grounded starting point for the computationally expensive RANS CFD simulations.

## 10 Choice of Propeller

### Nomenclature:

The data for the propellers was obtained from Advanced Precision Composites (APC) propellers. Their website has an extensive for different configurations of propellers.

As stated in the APC propeller nomenclature, the size of each propeller is identified by two numbers separated by an 'X'. The first number indicates the diameter of the propeller in inches and the second number indicates the pitch of the propeller in inches. The letter after the second number indicates the type of the propeller ('E' for electric, only letter relevant to our use case).

For example, consider the propeller 'PER<sub>3</sub>\_13X7E'. This implies that the diameter of the propeller is 13 in, its pitch is 7 in and it is an electric propeller.

A script was written which goes through the database of all the propellers, and based on the  $D$  obtained from the calculation of  $c_w$ , we find the propeller with the corresponding diameter and an operational velocity of 20 m/s, which is around 45 mph. In case an exact value corresponding to the velocity is not

present, we perform linear interpolation to obtain that value, and as a result we get the required value at 20 m/s.

**Note:** All propellers in the database are two-bladed. For considering three-bladed or four-bladed propellers, a simple modification can be made to the data. The conversion is made by matching the two-blade torque for the specified model speed and RPM conditions. Multiply D by **0.9 (for three-blades)** or **0.84 (for four-blades)** to convert the propeller.

## II Actuator Disk Modelling of the Propellers

### II.1 Rationale for Using the Actuator Disk Approximation

To model the aerodynamic influence of the distributed propellers without explicitly resolving rotating blades, the **Actuator Disk Theory (ADT)** was employed. In this formulation, each propeller is represented as a thin, momentum-adding disk that imparts pressure jump and accelerates the flow across its surface. This approach accurately captures the induced velocities and slipstream characteristics essential for blown-wing aerodynamics, while significantly reducing computational cost compared to full blade-resolved CFD.

The actuator disk imposes a momentum source consistent with Rankine–Froude theory. For an axial-flow propeller, the thrust is given by

$$T = \dot{m}(V_e - V_\infty),$$

which may be rewritten in the widely used actuator disk form:

$$T = \frac{1}{2} \rho A (V_e^2 - V_\infty^2),$$

where  $\rho$  is air density,  $A$  is the disk area,  $V_\infty$  is freestream velocity, and  $V_e$  is the exit slipstream velocity downstream of the propeller.

In the present study, this formulation was used to prescribe the appropriate axial momentum source in each propeller disk region of the CFD domain.

### II.2 Propeller Selection and Blade Count Equivalence

The aircraft uses **four-bladed 14 cm diameter propellers**. To apply available APC performance data, which is reported exclusively for two-bladed propellers, an equivalent-disk representation was required.

A four-bladed propeller of diameter  $D_4 = 0.14$  m may be approximated as a two-bladed propeller of increased diameter such that equal disk loading is preserved. Using the blade-count equivalence

relation,

$$D_2 = \frac{D_4}{0.84}$$

= 0.1666 m,

corresponding to an effective diameter of **6.56 inches**. The closest matching propeller available in APC data was the **PER3\_6x6E**, whose aerodynamic coefficients were used for thrust–RPM estimation.

Using this dataset, the required thrust of

$$T_{TO} = 16 \text{ N} \quad \text{and} \quad T_{cruise} = 12 \text{ N}$$

is achieved at an approximate rotational speed of

$$\text{RPM} \approx 25,000\text{--}26,000,$$

### II.3 Computation of Exit Velocity Using Actuator Disk Theory

The exit velocity downstream of the propeller was computed using the actuator disk relation:

$$T = \frac{1}{2} \rho A (V_e^2 - V_\infty^2).$$

Rearranging,

$$V_e = \sqrt{V_\infty^2 + \frac{2T}{\rho A}},$$

where the disk area is

$$A = \frac{\pi D^2}{4} = \frac{\pi (0.14)^2}{4} = 0.0154 \text{ m}^2.$$

#### II.3.1 Take-off Condition ( $V_\infty = 20 \text{ m/s}$ , $T = 16 \text{ N}$ )

Assuming  $\rho = 1.225 \text{ kg/m}^3$ ,

$$V_e = \sqrt{20^2 + \frac{2(16)}{1.225 \times 0.0154}} = \sqrt{400 + 1698.0} = \sqrt{2098.0} = 45.8 \text{ m/s.}$$

#### II.3.2 Cruise Condition ( $V_\infty = 80 \text{ m/s}$ , $T = 12 \text{ N}$ )

$$V_e = \sqrt{80^2 + \frac{2(12)}{1.225 \times 0.0154}} = \sqrt{6400 + 1274.4} = \sqrt{7674.4} = 87.6 \text{ m/s.}$$

These values were used as the momentum-source reference velocities in the actuator disk regions of the CFD model. The resulting slipstream profiles matched expected accelerated jet behaviour and produced consistent lift augmentation trends in the wing–flap system.

## 12 Wing Design Methodology

The wing for this STOL aircraft is designed to maximise low-speed lift capability, provide predictable stall behaviour, and maintain effective roll control during high lift-device deployment. The design approach prioritises aerodynamic stability, structural simplicity, and ease of manufacturing, which are crucial for a short take-off and landing (STOL) configuration.

### 12.1 Design Philosophy

For STOL operations, the critical sizing condition occurs during take-off and landing, where the aircraft must generate sufficient lift at low speeds. Accordingly, the wing is conceptually sized using the maximum lift capability achievable with deployed high-lift devices. This ensures the aircraft maintains adequate stall margin and achieves the low-speed performance required for short runway operations.

### 12.2 Planform Selection

A rectangular, unswept, untapered wing planform was selected. This choice is supported by several theoretical advantages:

- **Benign stall characteristics:** Rectangular wings stall at the root first, preserving aileron effectiveness near the tips and ensuring safer, more predictable handling near stall.
- **Simplified aerodynamic behaviour:** The constant chord produces a nearly uniform spanwise lift distribution, simplifying aerodynamic modelling and improving the integration of high-lift devices.
- **Ease of fabrication:** Straight leading and trailing edges reduce manufacturing complexity and structural variability.
- **No sweep required:** Sweep is advantageous primarily for transonic or high-speed flight; for low Reynolds number STOL performance, it offers no aerodynamic benefit while adding structural weight and complexity.
- **Structural efficiency:** A constant-chord wing allows straightforward spar design with predictable bending and torsional loading.

### 12.3 High-Lift System: Flaps and Flaperons

To achieve the enhanced low-speed lift demanded by STOL performance, the wing incorporates a combination of single-slotted flaps and flaperons.

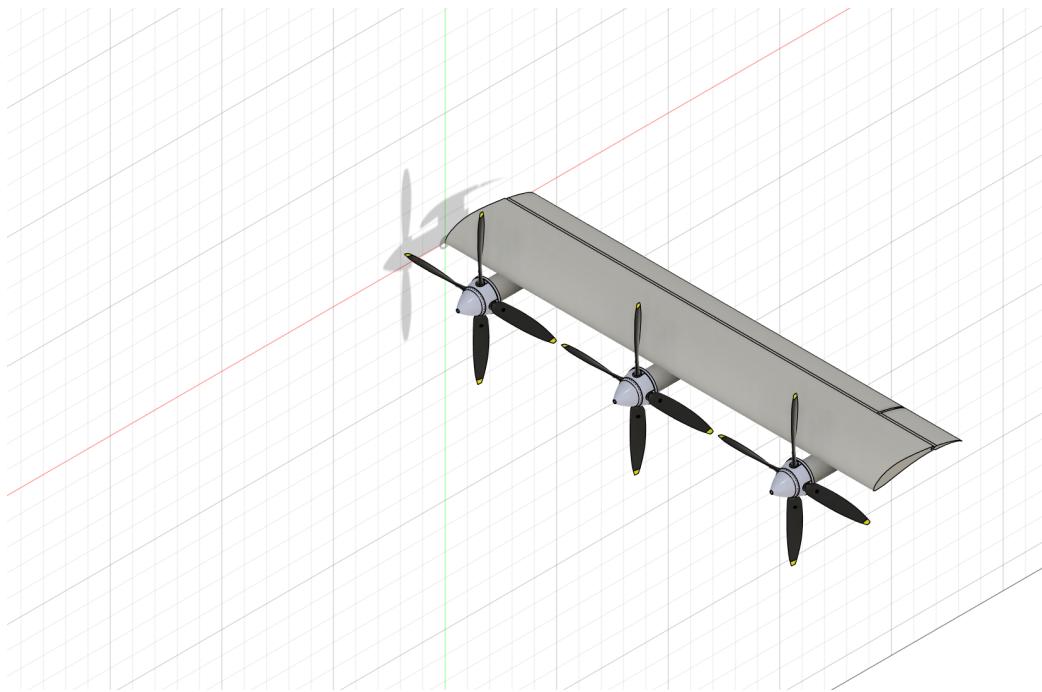


Figure 2: CAD model of half wing section

### 12.3.1 Single-Slotted Flaps

Single-slotted flaps were chosen due to their favourable lift-enhancing behaviour:

- The slot re-energises the boundary layer, delaying flow separation and increasing the maximum lift coefficient.
- They offer a high lift increment without the mechanical complexity of multi-element or Fowler flap systems.
- Their aerodynamic characteristics are well understood, allowing predictable lift and pitching moment increments.

### 12.3.2 Flaperons

Flaperons (combined flaps and ailerons) are implemented on the outboard section of the wing to preserve roll authority during flap deployment:

- They reduce system weight and mechanical complexity by combining two functions into a single surface.
- Differential deflection provides effective roll control even at high flap settings, where conventional ailerons may lose effectiveness.
- They allow seamless integration of both symmetric (flap) and differential (aileron) control for improved low-speed handling qualities.

## 12.4 Aerodynamic and Stability Considerations

The use of a rectangular planform combined with inboard flaps and outboard flaperons ensures:

- Root-first stall progression, maintaining controllability during slow flight.
- Improved roll effectiveness due to larger moment arm of outboard control surfaces.
- Reduced risk of tip stall, which is especially important for STOL operations where the aircraft frequently operates near maximum lift.

## 12.5 Structural and Operational Rationale

A straight, constant-chord wing simplifies spar design and reduces structural weight. The shorter span typical of STOL aircraft limits bending moments, further enhancing structural efficiency. The simplicity of the planform and high-lift mechanisms also reduces maintenance demands and improves reliability in repeated short-field operations.

# 13 Preliminary Structural Analysis of Wing

## 13.1 Scope and assumptions

A preliminary structural analysis was performed for a wing of total span  $b = 0.96\text{ m}$  and chord  $c = 0.096\text{ m}$ . Due to geometric symmetry, each half-wing of length  $b/2 = 0.48\text{ m}$  was analysed independently as a cantilever beam fixed at the root. The aerodynamic loading was taken from CFD results at the design operating condition, yielding a total wing lift of  $L = 78.025\text{ N}$ . Thus, each half-wing carries:

$$L_{\text{half}} = 78.025\text{ N}.$$

Material properties assumed for preliminary sizing: carbon–epoxy composite with  $E = 70\text{ GPa}$ , density  $\rho = 1600\text{ kg/m}^3$ , and conservative allowable bending stress  $\sigma_{\text{allow}} = 200\text{ MPa}$ . The serviceability limit for deflection was selected as  $b/200 = 4.8\text{ mm}$ .

## 13.2 Loads and primary bending

Uniform distributed lift on one half-wing:

$$w = \frac{L_{\text{half}}}{b/2} = \frac{78.025}{0.48} = 162.55\text{ N/m}.$$

Root shear force:

$$V_{\text{root}} = w \left( \frac{b}{2} \right) = 162.55 \times 0.48 = 78.025\text{ N}.$$

Root bending moment:

$$M_{\text{root}} = \frac{w(b/2)^2}{2} = \frac{162.55 \times 0.48^2}{2} = 18.75 \text{ N} \cdot \text{m}.$$

### 13.3 Required section modulus and stiffness

Required section modulus:

$$Z_{\text{req}} = \frac{M_{\text{root}}}{\sigma_{\text{allow}}} = \frac{18.75}{200 \times 10^6} = 9.38 \times 10^{-8} \text{ m}^3.$$

Required second moment of area for target tip deflection  $\delta_{\text{allow}} = 4.8 \text{ mm}$ :

$$I_{\text{req}} = \frac{w(b/2)^4}{8E\delta_{\text{allow}}} = 3.17 \times 10^{-9} \text{ m}^4.$$

### 13.4 Structural topology and sizing

The structural layout consists of a thin-walled CFRP skin box reinforced with spars.

- **Skin box:** Airfoil maximum thickness  $\approx 0.22c = 0.02112 \text{ m}$ . Solving for  $I_{\text{req}}$  yields required wall thickness:

$$t_{\text{skin}} \approx 1.35 \text{ mm}.$$

- **Spars (five total):** Five CFRP tubular spars, sized at wall thickness  $t_{\text{spar}} = 1.0 \text{ mm}$  and outer diameter  $D = 10 \text{ mm}$ , provide per-spar second moment:

$$I_{\text{spar}} = 2.73 \times 10^{-10} \text{ m}^4.$$

Total bending stiffness:

$$I_{\text{total}} = I_{\text{box}} + 5I_{\text{spar}} \approx 3.33 \times 10^{-9} \text{ m}^4.$$

- **Estimated mass:** Skin + spars  $0.55\text{--}0.65 \text{ kg}$  per half-wing (entire wing  $1.1\text{--}1.3 \text{ kg}$  before fittings).

### 13.5 Stress and deflection evaluation

Maximum bending stress:

$$\sigma_{\text{max}} = \frac{M_{\text{root}}(h/2)}{I_{\text{total}}} = \frac{18.75 \times 0.01056}{3.33 \times 10^{-9}} \approx 59.4 \text{ MPa} \ll 200 \text{ MPa}.$$

Predicted tip deflection:

$$\delta_{\text{tip}} = \frac{w(b/2)^4}{8EI_{\text{total}}} \approx 4.56 \text{ mm} < 4.8 \text{ mm}.$$

Thus, both strength and stiffness criteria are satisfied with margin.

Drag-induced torsion is comparatively small due to low drag force and the presence of five spars, which significantly increases torsional rigidity.

## 14 CFD Methodologies/Strategies Explored

We explored the findings of this paper, "Selection of Propeller-Wing Configuration on Blown Wing Aircraft", to establish a robust and computationally feasible methodology for our own preliminary CFD analysis. The paper's authors faced a similar challenge: exploring a vast design space where full-fidelity, transient simulations of all configurations are computationally prohibitive. Their systematic validation of simplified models provides a clear and rigorous blueprint for our approach.

This following section of the report details the theoretical foundations, key assumptions, and procedural steps, as derived from the paper, that justify our use of a 3D/2D, steady-state actuator disk model with a uniform pressure rise (no swirl)

### 14.1 Theoretical Foundation

Any valid Computational Fluid Dynamics simulation of this problem statement must capture the two essential physical phenomena that govern the performance of a blown wing:

1. The mechanism of Lift Augmentation
2. The unique Mechanism of stall

#### 14.1.1 Primary Aerodynamic Principle: Lift Augmentation

The fundamental benefit of a blown wing is the "Blowing" of the wing with the high velocity propeller jet, which increases the dynamic pressure over the airfoil. The paper presents the theoretical relationship for the augmented lift coefficient ( $C_{L,blown}$ ) for an infinitely large jet as:

$$C_{L,blown} = C_{L,unblown} \frac{V_j^2}{V_\infty^2} \quad (16)$$

This equation dictates that the augmented lift scales with the square of the jet velocity ( $V_j$ ) relative to the freestream velocity ( $V_\infty$ ). For a fixed thrust, a smaller jet area results in a higher  $V_j$ , theoretically offering a path to exceptionally high lift coefficients. Our CFD model must, at a minimum, be able to capture this fundamental addition of energy and its effect on lift.

#### 14.1.2 Stall Mechanism

The paper's most critical insight is its identification of the stall mechanism. This is not a traditional high-angle-of-attack flow separation. Instead, it is a sudden and catastrophic loss of lift that occurs when the wing, due to a change in angle of attack or flap angle, moves outside the propeller's jet.

1. **Mechanism:** When the jet passes just above the wing's upper surface, it causes a "sudden rise in diffusion on the rear of the suction surface", which in practice results in boundary layer separation and stall. The paper quantifies this using a Diffusion Factor (DF) in Figure 8:

$$DF = \frac{V_{peak} - V_{TE}}{V_\infty}$$

This stall is therefore a geometrically-driven event. The paper explicitly confirms this: "It can be seen that the stall condition, in all three cases, occurs at the point where the jet just moves above the wing". To avoid this, the paper presents a simple geometric inequality that must be satisfied:

$$\frac{D_p}{2} - y_p \geq f(x_p)$$

where  $y_p$  is the propeller's vertical position and  $f(x_p)$  is the stagnation streamline coordinate.

2. **Implication:** Therefore, our model's primary requirement is not necessarily to capture the fine-scale turbulence of rotation, but to accurately predict the jet's trajectory, height, and interaction with the wing's upper surface, as this is what dictates the operational stall boundary.

## 15 Comparison of CFD Model Fidelities

The reference paper, "Selection of Propeller-Wing Configuration on Blown Wing Aircraft," validates its primary 2D methodology by first comparing three models of increasing complexity. This comparison is essential for understanding the trade-offs between computational cost and physical accuracy.

### 15.1 Description of the Models

The three models investigated by the authors are (as shown in Fig. 3):

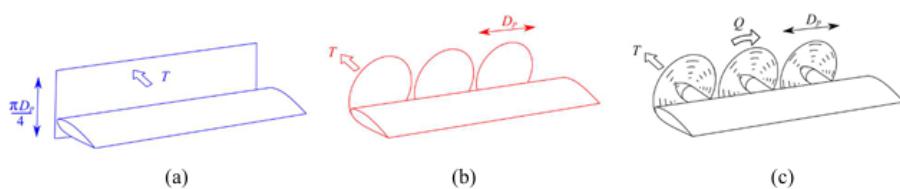


Figure 2. Levels of model fidelity, (a) 2D actuator disc with uniform pressure rise, (b) 3D actuator disc with uniform pressure rise, (c) 3D actuator disc with radial work profile and exit swirl.

Figure 3: Different Models used for simplifying CFD simulations as per referred paper.

**Case A: 2D Actuator Disc** This is a 2D simulation using an actuator disc with a uniform pressure rise. It represents the highest level of simplification, reducing the problem to a two-dimensional slice.

**Case B: 3D Actuator Disc (Uniform Pressure Rise)** This model advances to 3D, modeling a 3D actuator disc with a uniform pressure rise. This case correctly captures the three-dimensional geometry of the wing and the spanwise non-uniformity of the jet's height but **omits the rotational effects (swirl)** of the propeller.

**Case C: 3D Actuator Disc (Swirl & Radial Profile)** This is the highest-fidelity model used in the validation, modeling a 3D actuator disc with a non-uniform radial work profile and exit swirl. This case simulates the tangential forces ( $F_\theta$ ) applied to the fluid, which are generated by a rotating propeller.

## 15.2 Key Findings from the Model Comparison

By comparing these three cases, the authors were able to isolate the physical impact of 3D geometry and rotational swirl. The results (shown in Fig. 4) are the cornerstone of our methodological justification:

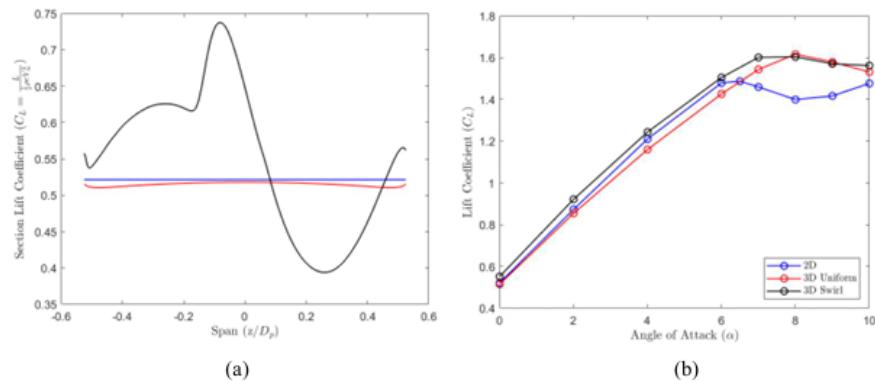


Figure 3: (a) Spanwise lift distribution, (b) Coefficient of lift plotted against angle of attack

Figure 4: Comparison of the three simulation Cases.

- **Effect of 3D Geometry (Case A vs. B):** The spanwise non-uniformity in jet height (present in 3D but not 2D) was found to have "little effect on the spanwise loading" and only a "second order effect on the lift distribution", provided the wing remains submerged in the jet.
- **Effect of Swirl (Case B vs. C):** The introduction of swirl (Case C) "has a significant effect on the spanwise lift distribution". This is visually evident in the non-uniform '3D Swirl' line in Fig. 3a.
- **Impact on Key Performance Metrics (The Critical Finding):** Despite the significant change in spanwise load distribution, the paper explicitly states that the introduction of swirl "was found to have **only a second order effect** on the gradient of the lift coefficient with angle of attack and on the angle of attack at which stall occurs".
- **Maximum Lift Agreement:** Across all three models, from the simplest 2D to the most complex 3D with swirl, the "maximum lift agrees to within 7%".

## 16 Computational Fluid Dynamics (CFD) Methodology

To investigate the aerodynamic augmentation enabled by Distributed Electric Propulsion (DEP) based Externally Blown Flap (EBF) systems, a high-fidelity RANS CFD framework was developed using ANSYS Fluent. The primary focus of this numerical study was to reproduce the fundamental blown-wing flow physics—namely flow reattachment over highly deflected flaps driven by high-momentum jet slipstreams—while maintaining computational tractability suitable for iterative design refinement.

### 16.1 Adopted Simulation Strategy: Actuator Disk-Equivalent Modeling

In real flight, rotating propeller blades generate thrust and accelerate the flow, creating a slipstream that envelopes the wing. However, full blade-resolved CFD models employing rotating reference frames or overset meshes impose significant grid and time-step penalties. To avoid this computational burden, blowing effects were instead modeled using a *3D actuator disk-equivalent momentum injection* approach.

This strategy is strongly supported in existing literature, wherein uniform-pressure-rise actuator discs have been shown to accurately predict the *first-order lift augmentation trends* of blown wings. As demonstrated in Fig. 2(b) of the referenced study, the 3D uniform actuator disc captures the essential aerodynamic effect of embedding the wing within the slipstream while neglecting secondary influences such as swirl and radial work variation. Importantly, it also reproduces the key physical insight that *stall onset corresponds to the jet lifting off the suction surface*, rather than traditional boundary-layer limitations.

Thus, slipstream dynamics were represented using high-velocity inlet patches imposed directly at the propeller disk plane, enabling computational focus on the interaction between the energized jet and the blown flap.

### 16.2 Domain Construction and Inlet Patch Implementation

The aerodynamic model was developed in Autodesk Fusion by extruding the Selig S1223 airfoil to form a three-dimensional wing and creating a hinged flap. In *DesignModeler*, the flap rotation was parameterized to enable analysis over a range of flap deflection angles while keeping all other geometric properties such as chord length, span, and slot gap constant. To reduce computational cost while retaining physical accuracy, **only half of the wing was modelled using a symmetry boundary condition at the wing root**. As a result, all computed aerodynamic loads (lift and drag) obtained from the half-wing simulations were **scaled to full-wing values by multiplying the force results by a factor of two**.

A rectangular computational domain was constructed around the wing and sized sufficiently large to capture wake development without artificial confinement. To model the influence of propeller slipstream, three circular patches corresponding to the propeller disk footprints were drawn on the inlet boundary at realistic spacing based on the physical wing layout. A face-split operation divided the inlet surface into two independent inlet zones: the first representing the freestream inflow at 20 m/s,

and the second consisting of the three propeller inlets where jet velocities of 45.8 m/s (take-off) and 87.6 m/s (cruise) were assigned. These inflow conditions were derived from actuator disc theory. The wing was positioned approximately 0.02 m downstream of the inlet patches to reflect practical blown-wing arrangements in which propellers are mounted close to the wing surface to maximise slipstream interaction.

The propeller inlet region size and spatial locations were defined as adjustable parameters, allowing systematic variation of propeller disk diameter and spacing. Thus, together with flap deflection angle, these inlet patch parameters were the only geometry-related variables modified in the parametric study. Boolean subtraction was used to embed the wing into the flow domain, followed by the creation of named selections for boundary condition assignment. The top and bottom surfaces of the domain were treated as no-slip walls, the wing-root plane was assigned as a symmetry boundary (enabling the half-wing simulation), the lateral far-field boundary was defined as a velocity inlet supplying the freestream direction and magnitude, the wing surfaces were set as no-slip walls, and the downstream face was modelled as a pressure outlet. Modelling the propeller footprints as velocity inlets rather than employing a rotating blade model allowed slipstream momentum to be injected directly into the flow, providing an efficient yet physically meaningful representation of blown-wing aerodynamics.

This inlet-patch slipstream modelling approach, combined with a half-wing computational setup and force scaling methodology, therefore provides a computationally efficient and accurate strategy for simulating propeller–wing interaction and boundary-layer energisation in blown-lift configurations.

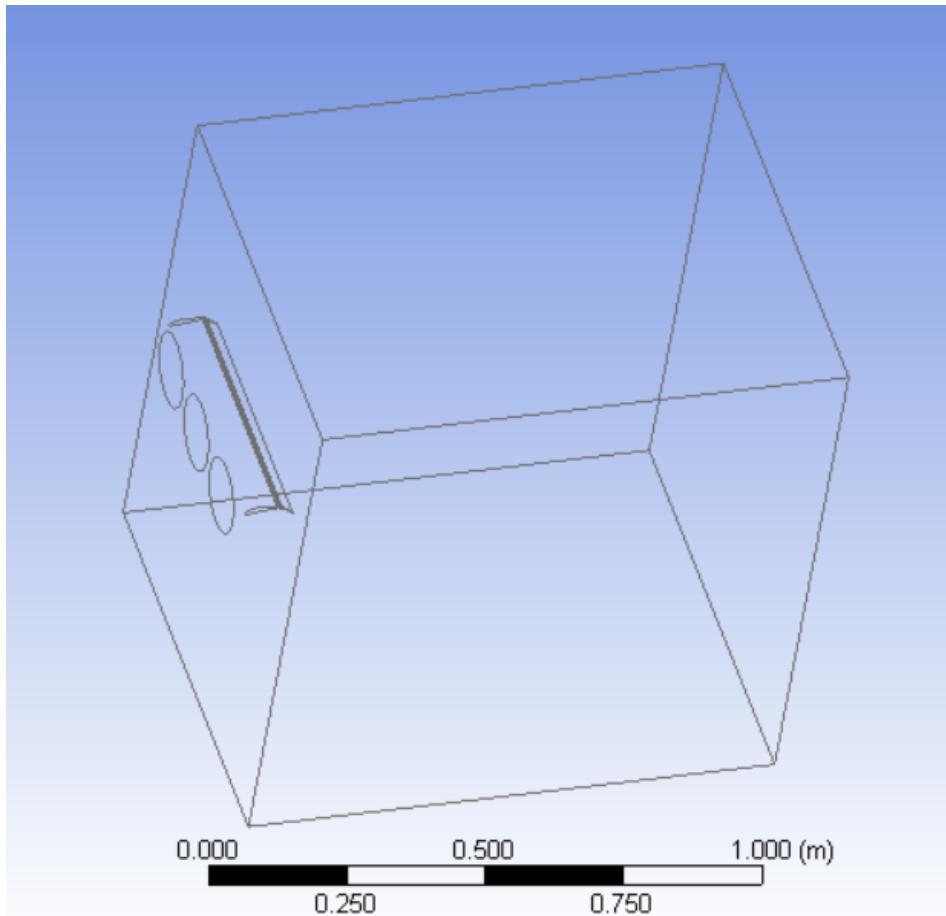


Figure 5: Computational flow domain showing the a half wing, flap, and propeller patch inlet configuration.

### 16.3 Discretization and Mesh Generation Strategy

The computational domain was discretized using an unstructured tetrahedral mesh generated through the patch-conforming meshing method. The *Tetrahedron* meshing approach was selected as it provides suitable flexibility for complex wing–flap geometries and ensures proper element transition around curved surfaces. Curvature and proximity-based refinement controls were enabled to preserve geometric fidelity around the leading edge, trailing edge, and flap hinge region. A global element size of 22 mm was adopted, producing a high-quality volumetric mesh with the final node and element counts reported in the Mesh Independence section.

Mesh quality was evaluated through standard quantitative mesh metrics, including skewness, orthogonal quality, and element quality distributions. These metrics were analyzed using histogram plots generated in ANSYS Mesh, as shown in Figs. 6–8. The majority of elements fall within acceptable quality thresholds (low skewness, high orthogonal and element quality), demonstrating that the generated mesh provides stable discretization characteristics suitable for accurate RANS-based aerodynamic simulation.

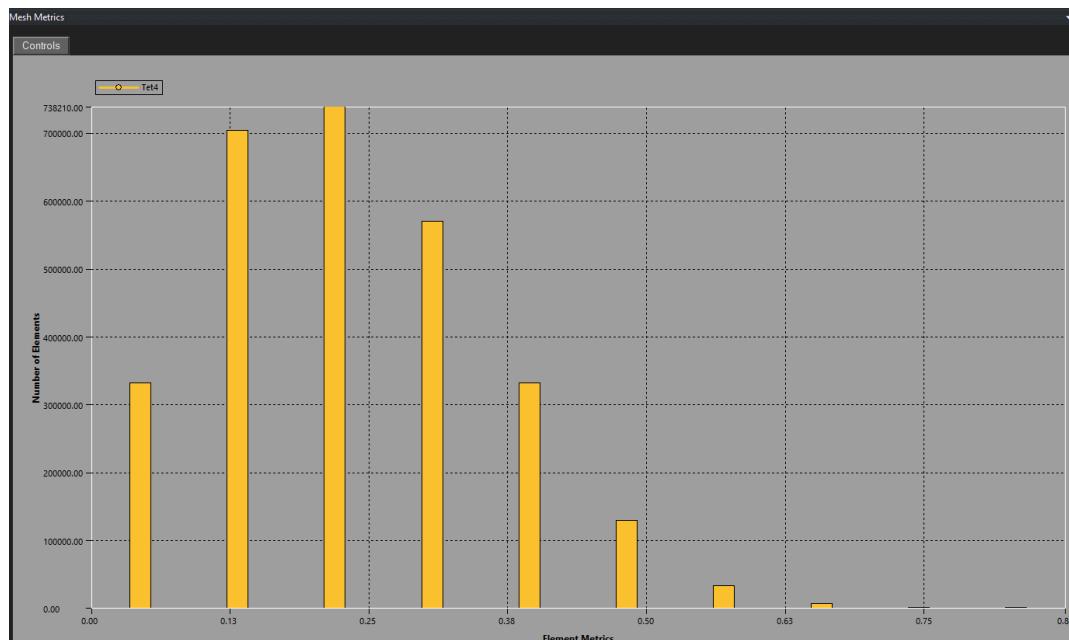


Figure 6: Skewness mesh metric distribution for the final tetrahedral mesh.

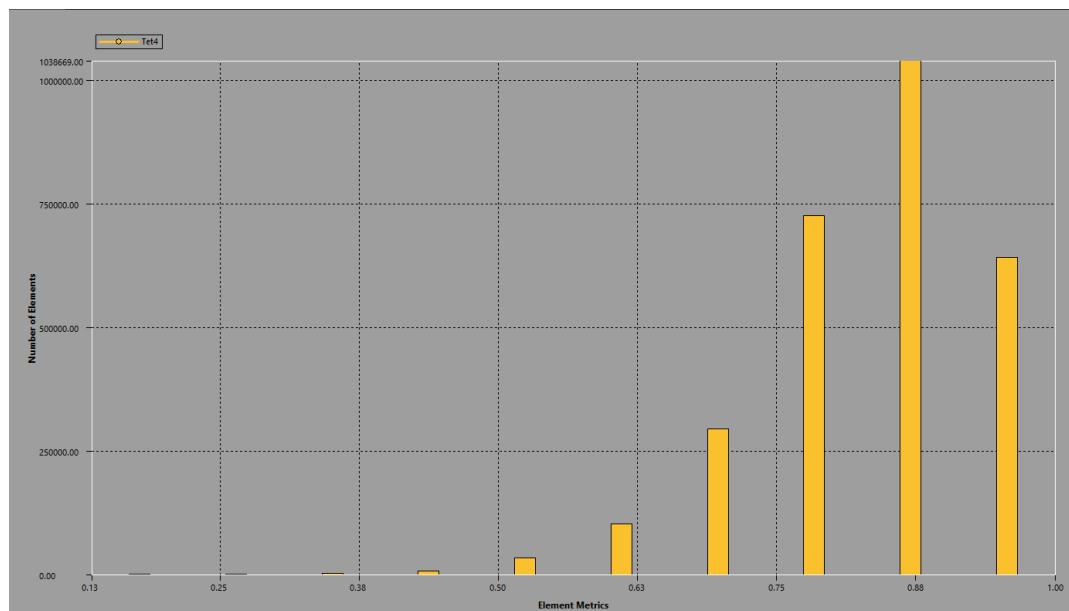


Figure 7: Element quality metric plot for the final mesh.

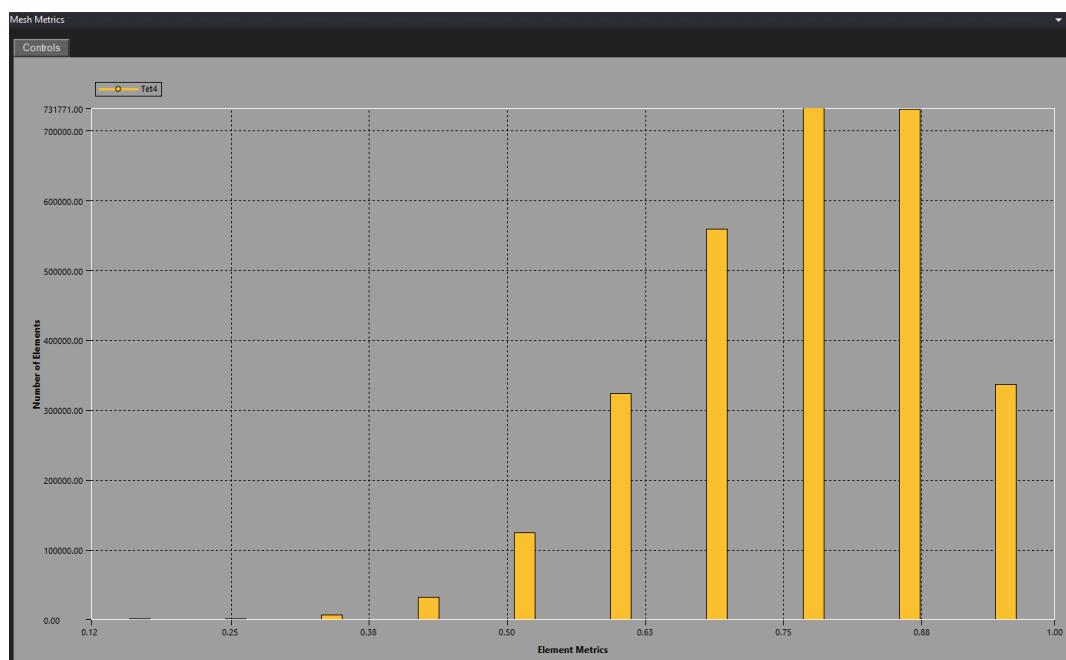


Figure 8: Orthogonal quality mesh metric distribution.

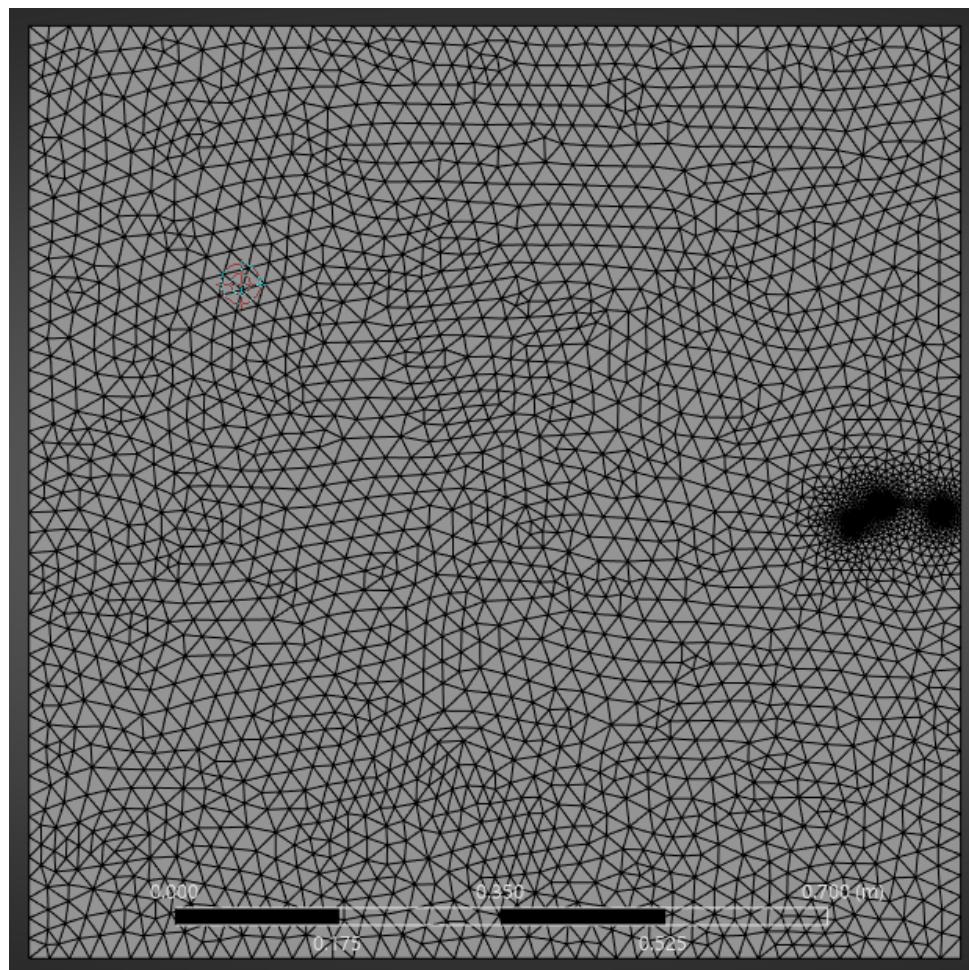


Figure 9: Surface and volume mesh

The mesh size selection was finalized based on these quality evaluations and subsequent aerodynamic sensitivity assessment performed through a mesh independence study.

## 16.4 Mesh/Grid Independence Study

A mesh independence study was carried out to assess the influence of mesh density on the simulation results. Three mesh levels were considered: Coarse, Fine, and Very Fine. For each mesh level, the number of nodes, elements, lift force, and drag force were recorded. The percentage error in lift and drag forces was computed relative to the coarse mesh results.

The coarse and fine meshes were each simulated for 1000 solver iterations, during which the aerodynamic forces stabilised and the continuity residuals decreased to the order of  $10^{-3}$ , indicating adequate convergence.

The very fine mesh required substantially higher computational time, with approximately 500 iterations taking nearly six hours of computation. Nevertheless, the solution exhibited stable force histories and residual levels comparable to the coarser meshes. Thus, despite the reduced iteration count, the results from all three mesh levels were sufficiently converged to allow a meaningful comparison in the mesh-independence assessment.

Table 3 presents the results obtained. It can be observed that refining the mesh beyond the coarse configuration yields minimal improvement in the predicted forces. The maximum percentage error in lift force was found to be 0.489%, while the drag force exhibited a maximum error of 1.005%. Since the differences between the fine and very fine meshes are marginal, and considering the significant increase in computational cost with mesh refinement, the coarse mesh is deemed sufficient for further simulations.

Table 3: Mesh Independence Study Results

Mesh Quality	Element Size (mm)	Nodes	Elements	Lift Force (N)	Drag Force (N)	Percentage Error (%)
Coarse	22	517947	2837281	74.228	13.922	0 (reference)
Fine	8	1005493	5537052	74.239	13.928	0.0148 (Lift), 0.04309 (Drag)
Very Fine	2	3901557	22017579	73.865	13.782	0.4890 (Lift), 1.0056 (Drag)

Figures 10, 11, and 12 show the mesh distribution for the three levels of refinement.

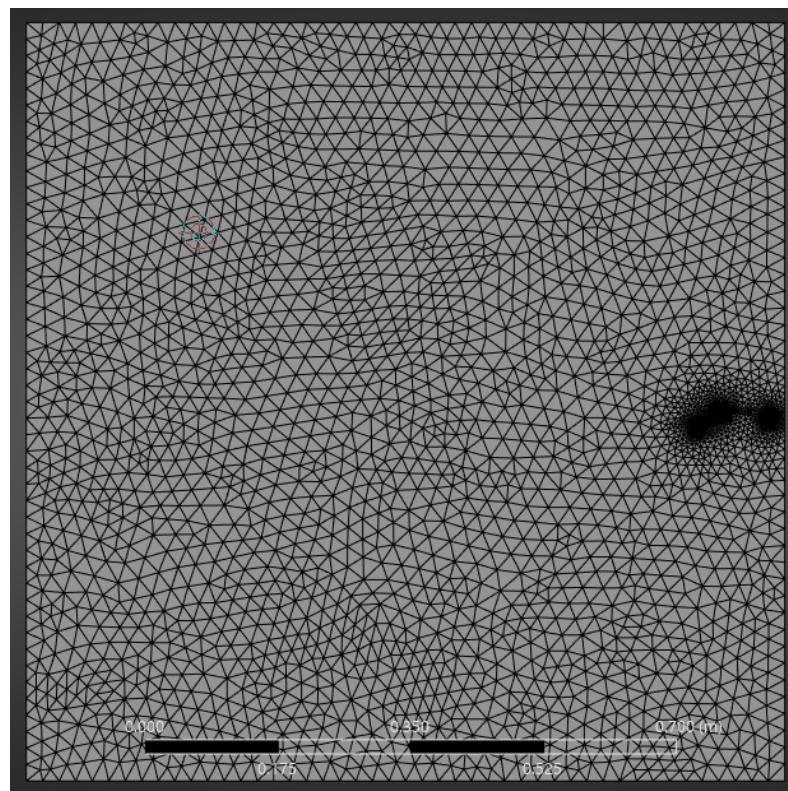


Figure 10: Coarse Mesh

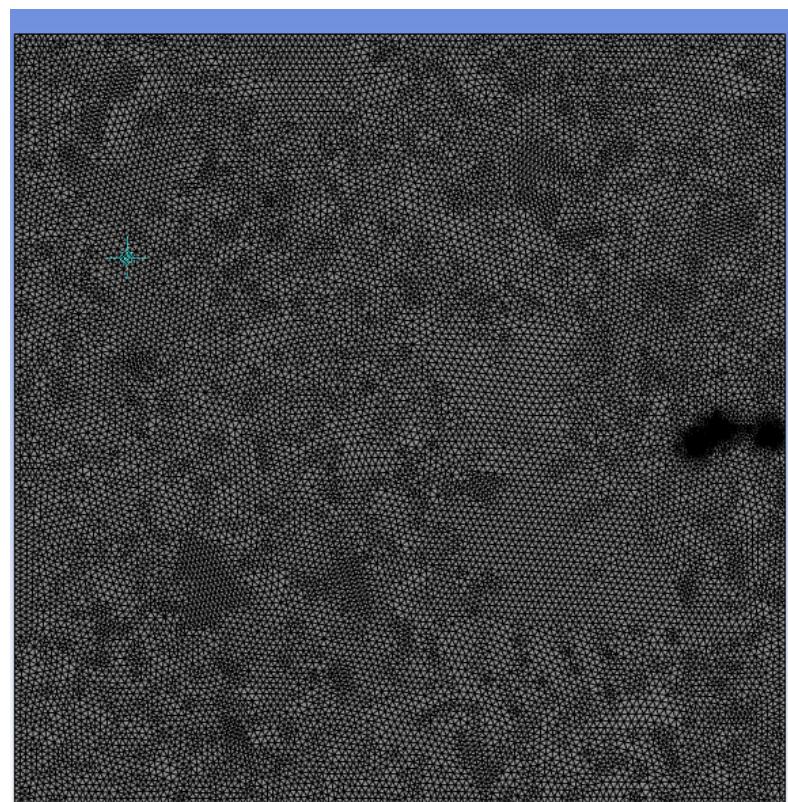


Figure 11: Fine Mesh

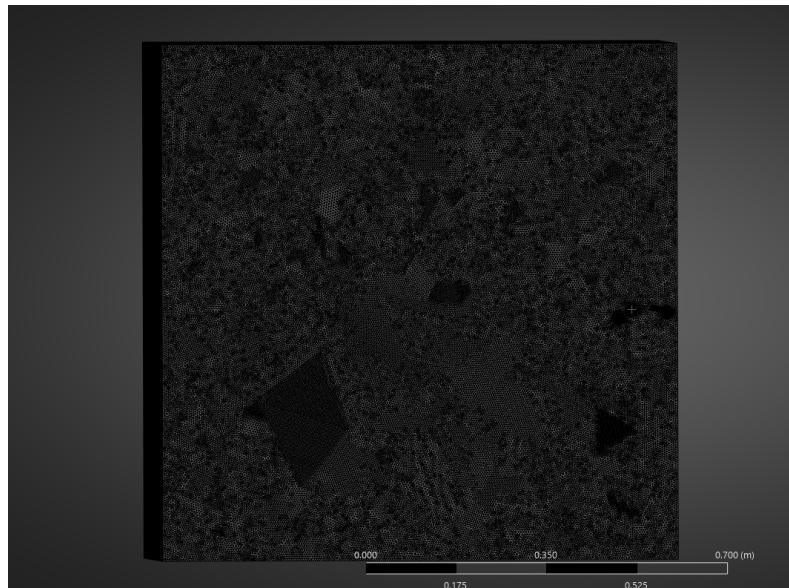


Figure 12: Very Fine Mesh

## 16.5 Solver Setup and Convergence Assessment

The simulations were conducted using a steady-state, pressure-based solver suitable for incompressible flow ( $Ma < 0.3$ ). Second-order spatial discretization was employed for momentum and turbulence transport equations. The  $k-\omega$  SST model was selected due to its robust performance in resolving boundary-layer behavior under strong adverse pressure gradients, a defining feature of deflected high-lift surfaces.

Convergence was guided by a combination of residual minimization ( $10^{-6}$  target) and force monitor stabilization. Final solutions were accepted only after aerodynamic coefficients plateaued and continuity residuals reached the order of  $10^{-3}$  confirming fully developed slipstream-surface interaction.

## 17 Parametric Studies for Take-off

### 17.1 Study on Propeller Vertical Location

To quantify the sensitivity of the blown-wing system to propeller placement, a parametric study was performed in which the vertical location of the propeller axis was varied relative to the airfoil centreline. Three configurations were evaluated: the propeller located 0.06 m, 0.03 m, and 0.001 m below the airfoil centreline. The remaining conditions were held fixed (Angle of Attack =  $45^\circ$ , flap deflection =  $17.5^\circ$ ). The mid-plane flow field was extracted at  $x = 0.24$  m, passing through the centre of the middle propeller.

Table 4: Aerodynamic forces for different propeller vertical locations.

AoA	Flap Deflection (°)	Lift Force (N)	Drag Force (N)	Prop_Vert (m)
45	17.5	47.744	2.229	0.06
45	17.5	67.030	9.570	0.03
45	17.5	78.025	13.550	0.001

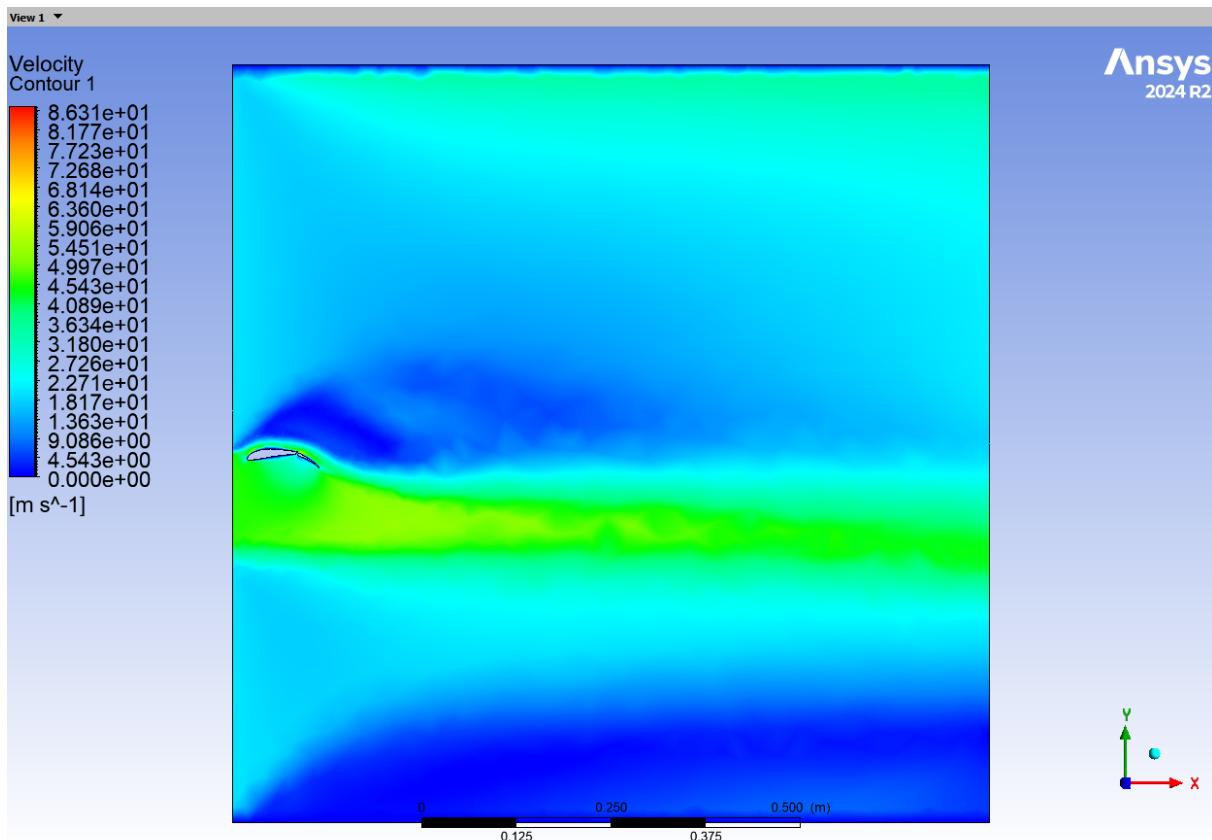


Figure 13: Velocity field for propeller located 0.06 m below wing centreline. The jet remains largely undeflected and does not contribute significantly to lift.

The results show a clear trend: **as the propeller is moved closer to the airfoil surface, the lift generated by the blown wing increases substantially.** When the propeller is located far below the wing (0.06 m), the jet momentum does not effectively attach to or interact with the upper surface of the airfoil. This results in minimal jet deflection by the wing, weaker circulation augmentation, and consequently reduced lift. The velocity contour (Fig. 13) confirms that the jet core remains almost horizontal, passing below the wing without energizing the boundary layer.

As the propeller is raised to 0.03 m below the centreline, the jet begins to interact more strongly with the wing surface. The onset of meaningful Coandă-like deflection is observed, accompanied by increased suction on the upper surface. This results in a  $\sim 40\%$  increase in lift relative to the 0.06 m case.

When the propeller is positioned very close to the wing (0.001 m), the jet fully attaches to the airfoil surface. Strong deflection and re-energisation of the flow occur, producing the highest circulation

and the maximum lift among the tested configurations. This behaviour is consistent with blown-wing theory: the closer the propeller slipstream is to the aerodynamic surface, the more effectively the injected momentum can be converted into useful lift.

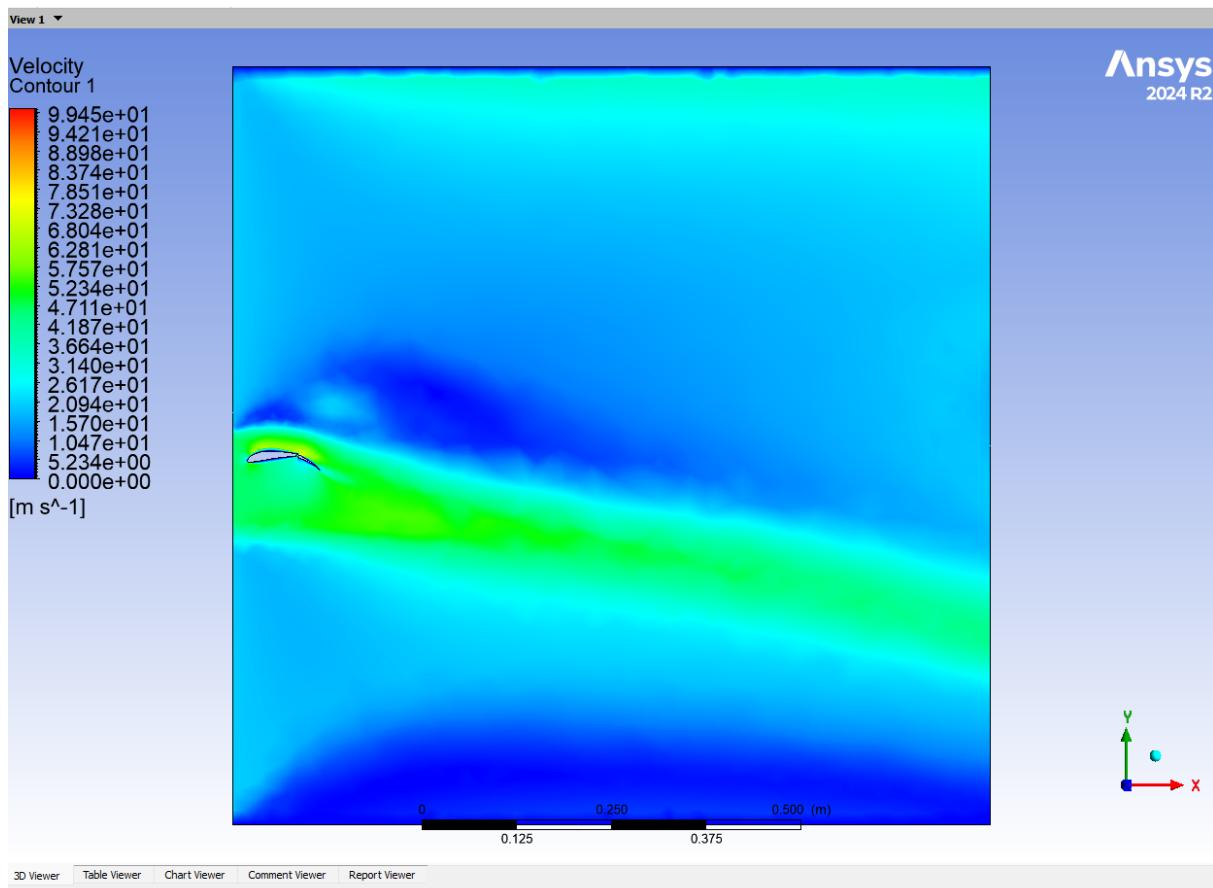


Figure 14: Velocity field for propeller located 0.03 m below centreline. Partial jet deflection increases circulation and lift.

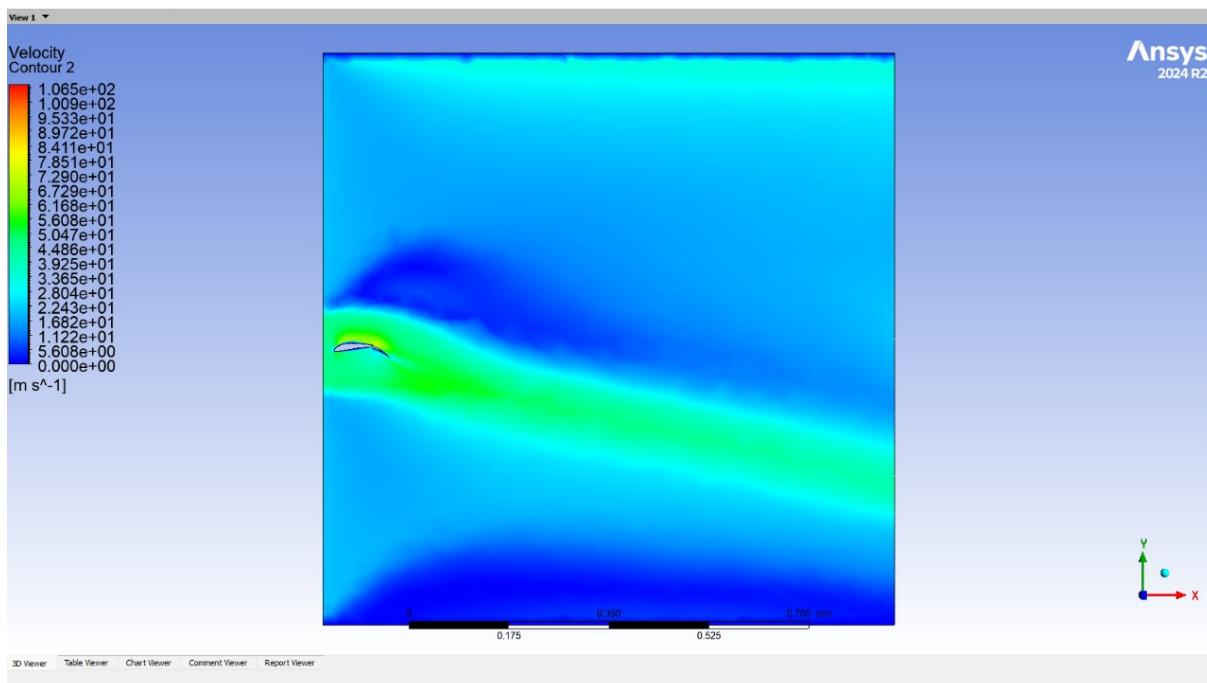


Figure 15: Velocity field for propeller located 0.001 m below centreline. The slipstream fully attaches to the wing, producing maximum lift.

Overall, this parametric study demonstrates that **propeller vertical placement is a critical design parameter for blown-wing aircraft**. A small change in propeller height leads to a large difference in jet-wing interaction and consequently in lift generation. The final configuration selected for the complete 3D study was therefore the 0.001 m distance, which provided the strongest aerodynamic benefit while remaining compatible with ANSYS parametrisation constraints.

## 17.2 Study on Effect of Angle of Attack at Fixed Flap Deflection

A parametric study was performed to quantify the influence of angle of attack (AoA) on the aerodynamic performance of the blown-wing configuration. The propeller vertical offset was fixed at  $\text{prop\_vert} = 0.001 \text{ m}$ , and the flap deflection was held constant at  $\phi = 17.5^\circ$ . This flap angle was selected based on a preliminary study conducted on the baseline S1223 airfoil, which showed that its maximum lift coefficient occurs near a flap deflection of approximately  $17.5^\circ$ .

The freestream velocity was  $V_\infty = 20 \text{ m/s}$ , and air density was  $\rho = 1.225 \text{ kg/m}^3$ . The reference area was taken as the half-wing planform area,

$$S_{\text{ref}} = c \left( \frac{b}{2} \right) = 0.096 \times 0.48 = 0.04608 \text{ m}^2,$$

and the dynamic pressure was

$$q = \frac{1}{2} \rho V_\infty^2 = 245 \text{ Pa.}$$

The nondimensional coefficients were computed using  $qS_{\text{ref}} = 11.2896 \text{ N}$ .

Table 5 summarises the lift and drag forces obtained from CFD for the entire wing, along with the derived lift and drag coefficients and aerodynamic efficiency  $L/D$ .

Table 5: AoA sweep at fixed flap deflection  $\delta = 17.5^\circ$  and propeller vertical offset 0.001 m. (Lift and Drag values doubled)

AoA ( $^\circ$ )	Flap ( $^\circ$ )	$L$ (N)	$D$ (N)	$C_L$	$C_D$	$L/D$
0	17.5	121.878	30.712	5.398	1.360	3.969
10	17.5	133.652	29.360	5.919	1.300	4.552
20	17.5	144.556	28.246	6.402	1.251	5.118
30	17.5	151.956	27.524	6.730	1.219	5.523
35	17.5	154.444	27.340	6.840	1.211	5.648
40	17.5	155.254	27.170	6.876	1.203	5.713
45	17.5	156.050	27.100	6.911	1.200	5.759
50	17.5	151.618	26.908	6.715	1.192	5.633

The results demonstrate a clear monotonic increase in lift with AoA up to approximately  $45^\circ$ , where the maximum lift  $L = 78.025$  N (corresponding to  $C_L \approx 6.91$ ) and maximum aerodynamic efficiency  $L/D \approx 5.76$  are obtained. Beyond  $45^\circ$ , the lift decreases slightly, suggesting that the ability of the propeller slipstream to energise the boundary layer has begun to saturate. At very high AoA the adverse pressure gradient grows rapidly, and the blown jet may no longer remain attached over the suction surface or may detach from the flap surface, reducing the net lift.

In conventional unblown wings, stall typically occurs around  $12^\circ - 15^\circ$  due to boundary-layer separation. However, in a blown-wing configuration the propeller slipstream supplies additional momentum to the suction-side boundary layer, delaying separation significantly and enabling the wing to operate at AoA as high as  $45^\circ$  while still producing increasing lift. This behaviour is consistent with externally blown flap systems and other powered-lift concepts.

## Conclusion

The AoA sweep revealed that the optimum operating condition for this configuration occurs at  $45^\circ$  AoA, where both the lift coefficient and aerodynamic efficiency are maximised. The decline in lift beyond this angle indicates the onset of jet detachment and weakened boundary-layer re-energisation, marking the practical limit of the blown-wing benefit under the tested conditions.

### 17.3 Study on Effect of Flap Deflection at Optimal AoA = $45^\circ$

After identifying  $45^\circ$  as the optimal angle of attack for the blown-wing configuration, a secondary parametric study was conducted to examine the sensitivity of aerodynamic performance to flap deflection at this AoA. Based on the earlier airfoil-level analysis, a flap deflection of  $17.5^\circ$  was expected to provide high lift, but this test verifies whether the optimum persists when the complete blown-wing system is operated at high incidence.

The test matrix consisted of flap deflections of  $15^\circ$ ,  $17.5^\circ$ , and  $20^\circ$ . The same reference area ( $S = 0.04608 \text{ m}^2$ ) and dynamic pressure ( $q = 245 \text{ Pa}$ ) were used as in the previous subsection.

Table 6 summarises the measured lift and drag forces at  $\text{AoA} = 45^\circ$  together with the derived aerodynamic coefficients for the whole wing.

Table 6: Flap deflection sweep at  $45^\circ$  AoA and  $\text{prop\_vert} = 0.001 \text{ m}$ .

AoA ( $^\circ$ )	Flap ( $^\circ$ )	$L$ (N)	$D$ (N)	$C_L$	$C_D$	$L/D$
45	15.0	152.524	24.542	6.757	1.087	6.216
45	17.5	156.05	27.1	6.911	1.200	5.759
45	20.0	155.572	29.154	6.894	1.291	5.335

The results show that a flap deflection of  $17.5^\circ$  continues to produce the highest lift at the operating condition of  $45^\circ$  AoA. Although the configuration with  $\phi = 15^\circ$  exhibits the highest aerodynamic efficiency ( $L/D \approx 6.22$ ), it produces a lower lift coefficient ( $C_L \approx 6.76$ ) compared to the nominal case. The configuration with  $\phi = 20^\circ$  shows a small decrease in lift relative to  $\phi = 17.5^\circ$  and also incurs a noticeable drag penalty, leading to the lowest  $L/D$  ratio among the three.

These trends are physically consistent: at very high AoA, an excessively large flap deflection increases camber and adverse pressure gradients, which can lead to partial flow detachment even under blown conditions. Conversely, too small a flap deflection reduces effective circulation enhancement. The flap angle of  $17.5^\circ$  therefore represents a balance between added camber and controllable flow attachment, resulting in the highest achievable lift in this configuration.

## Conclusion

The flap deflection of  $17.5^\circ$  remains the optimal choice at the operating AoA of  $45^\circ$ , producing the maximum lift coefficient  $C_L \approx 6.91$ . Both lower ( $15^\circ$ ) and higher ( $20^\circ$ ) flap deflections result in reduced lift or increased drag, confirming that the optimum identified in the baseline airfoil study also holds for the full blown-wing system.

## 18 Parametric Study for Cruise

To evaluate the aerodynamic behaviour of the blown-wing configuration during cruise, a dedicated study was conducted at a freestream velocity of  $V_\infty = 80 \text{ m/s}$  and an altitude of  $1000 \text{ m}$ , corresponding to an air density of  $\rho = 1.112 \text{ kg/m}^3$ . The flight condition considered was level cruise at  $\text{AoA} = 0^\circ$ . Two flap settings were examined: a clean configuration ( $\phi = 0^\circ$ ) and a deflected configuration ( $\phi = 15^\circ$ ). The dynamic pressure for cruise was computed as

$$q = \frac{1}{2}\rho V_\infty^2 = 0.5 \times 1.112 \times 80^2 = 3558.4 \text{ Pa},$$

and the reference area is

$$S = 0.04608 \text{ m}^2, \quad qS = 164.21 \text{ N.}$$

Table 7: Cruise performance at AoA =  $0^\circ$  for two flap configurations.

AoA ( $^\circ$ )	Flap ( $^\circ$ )	$L$ (N)	$D$ (N)	$C_L$	$C_D$	$L/D$
0	0	444.32	56.086	1.355	0.171	7.92
0	15	696.822	117.93	2.122	0.359	5.91

The clean configuration ( $\phi = 0^\circ$ ) provides significantly better aerodynamic efficiency, with  $L/D \approx 7.92$ . Although the case with  $\phi = 15^\circ$  generates higher lift ( $C_L \approx 2.12$ ), the drag penalty nearly doubles, reducing efficiency to  $L/D \approx 5.91$ . This behaviour is consistent with classical aerodynamic theory: during cruise, high lift is not required, and flap deployment unnecessarily increases camber and pressure drag, degrading efficiency.

## Conclusion

For cruise at 80 m/s and 1000 m altitude, the clean wing ( $\phi = 0^\circ$ ) is the optimal configuration. Flap deflection should not be used in cruise, as it increases drag without providing any operational benefit. In addition to the aerodynamic efficiency considerations, it is important to note that the predicted cruise lift values exceed the aircraft weight (444–697 N compared to  $W = 147$  N). This outcome does not indicate a modelling issue; rather, it reflects the fact that the CFD simulations were conducted at a fixed geometric angle of attack of  $\alpha = 0^\circ$  without allowing the aircraft to trim itself. In actual flight, the vehicle would not operate at such a high effective lift coefficient in cruise. Instead, the aircraft would naturally pitch downward until the net lift exactly balances its weight.

This trimming action is achieved aerodynamically through the horizontal stabilizer. A downward aerodynamic force generated by the tail produces a nose-down pitching moment, reducing the wing's effective angle of attack until the lift matches the weight. Thus, although the CFD data at fixed  $\alpha$  predict  $C_L$  values well above those required for level cruise, a real aircraft would fly at a lower trimmed angle of attack resulting in  $L = W$  and substantially lower drag. This behaviour is consistent with classical longitudinal stability theory: the wing generates excess lift at high dynamic pressure, and the tail provides the corrective moment necessary to achieve a trimmed, steady equilibrium state.

Therefore, the large lift values observed in the fixed-AoA CFD runs should be interpreted as part of the untrimmed aerodynamic envelope rather than the true cruise operating point. The actual trimmed cruise condition will occur at a lower effective angle of attack and with significantly reduced drag.

## 19 Aerodynamic Center Considerations

Although the present study is primarily focused on lift and drag characteristics, it is useful to briefly discuss the expected behaviour of the aerodynamic center (AC) for context in future stability analysis. For a conventional, unswept wing of moderate aspect ratio, the aerodynamic center typically lies near the quarter-chord position and remains approximately fixed with angle of attack. This classical result forms the basis for most preliminary aircraft stability assessments.

In blown-wing configurations, however, the flow field is altered by both flap deflection and the propeller slipstream. These effects modify the pressure distribution over the upper surface, especially near the flap region, and can influence the effective location of the AC. While the present CFD simulations were limited to force predictions and do not include pitching-moment extraction, it is generally understood that high-lift devices and slipstream-energised flow can change the aerodynamic load distribution compared to a clean wing.

This qualitative understanding is sufficient for the scope of this work. A detailed determination of the AC location is not required at this stage but will be important in subsequent stability and control analyses, particularly for sizing the horizontal stabilizer and ensuring adequate trim capability across the operating envelope.

## 20 Final Blown-Wing Design and Performance Assessment

This section summarises the final blown-wing configuration selected after the mathematical modelling, CFD simulations, and parametric studies conducted in this work. The design parameters were refined to satisfy the take-off lift requirement, geometric constraints, and aerodynamic performance objectives of the aircraft.

### 20.1 Wing Geometry and Propulsion Layout

The wing employs the Selig S1223 high-lift airfoil with a chord length of

$$c = 0.096 \text{ m},$$

and a total wingspan of

$$b = 0.96 \text{ m},$$

resulting in an aspect ratio of

$$AR = \frac{b}{c} = 10.$$

A single-slotted flap is incorporated into the trailing edge to enhance low-speed lift performance. The flap length is

$$c_f = 0.3c = 0.0288 \text{ m},$$

corresponding to 30% of the chord. A geometric gap of

$$g = 3 \text{ mm}$$

is maintained between the main element and the flap to allow high-momentum flow to pass through the slot. This configuration promotes boundary layer re-energisation and delays flow separation during high flap deflection operations, particularly under blown-wing conditions.

Each half-wing integrates three propellers of diameter  $D = 0.14 \text{ m}$ , spaced uniformly at  $0.02 \text{ m}$  intervals. The inboard and outboard propellers are positioned  $0.09 \text{ m}$  and  $0.07 \text{ m}$  from the fuselage junction and wingtip, respectively. The propeller axes are mounted  $0.001 \text{ m}$  below the airfoil centreline and located  $0.020 \text{ m}$  downstream of the leading edge. During take-off each propeller generates a static thrust of  $16 \text{ N}$ , while in cruise the the thrust is reduced to  $12 \text{ N}$ .

Using actuator disk theory, the corresponding slipstream exit velocities are

$$V_{\text{exit,TO}} = 45.8 \text{ m/s}, \quad V_{\text{exit,cruise}} = 87.6 \text{ m/s}.$$

## 20.2 Take-off Performance

The take-off velocity of the aircraft is

$$V_{\infty,\text{TO}} = 20 \text{ m/s}.$$

The parametric study identified the optimal take-off configuration at:

$$\alpha_{\text{TO}} = 45^\circ, \quad \phi_{\text{TO}} = 17.5^\circ,$$

which yields a maximum lift coefficient

$$C_L = 6.911$$

The corresponding lift force per half-wing is

$$L_{\text{half}} = 78.025 \text{ N}, \quad L_{\text{total}} = 156.05 \text{ N}.$$

This lift capability corresponds to

$$\frac{L_{\text{total}}}{g} \approx 15.9 \text{ kg},$$

which exceeds the minimum required Lift of  $15 \text{ kg}$  at  $20 \text{ m/s}$ .

Thus, the blown-wing concept successfully satisfies the take-off lift requirement.

The aerodynamic efficiency at this operating point is

$$\left( \frac{L}{D} \right)_{\text{TO}} = 5.759,$$

which is greater than the minimum required value of 5. Therefore, both the lift and efficiency criteria for take-off are satisfied.

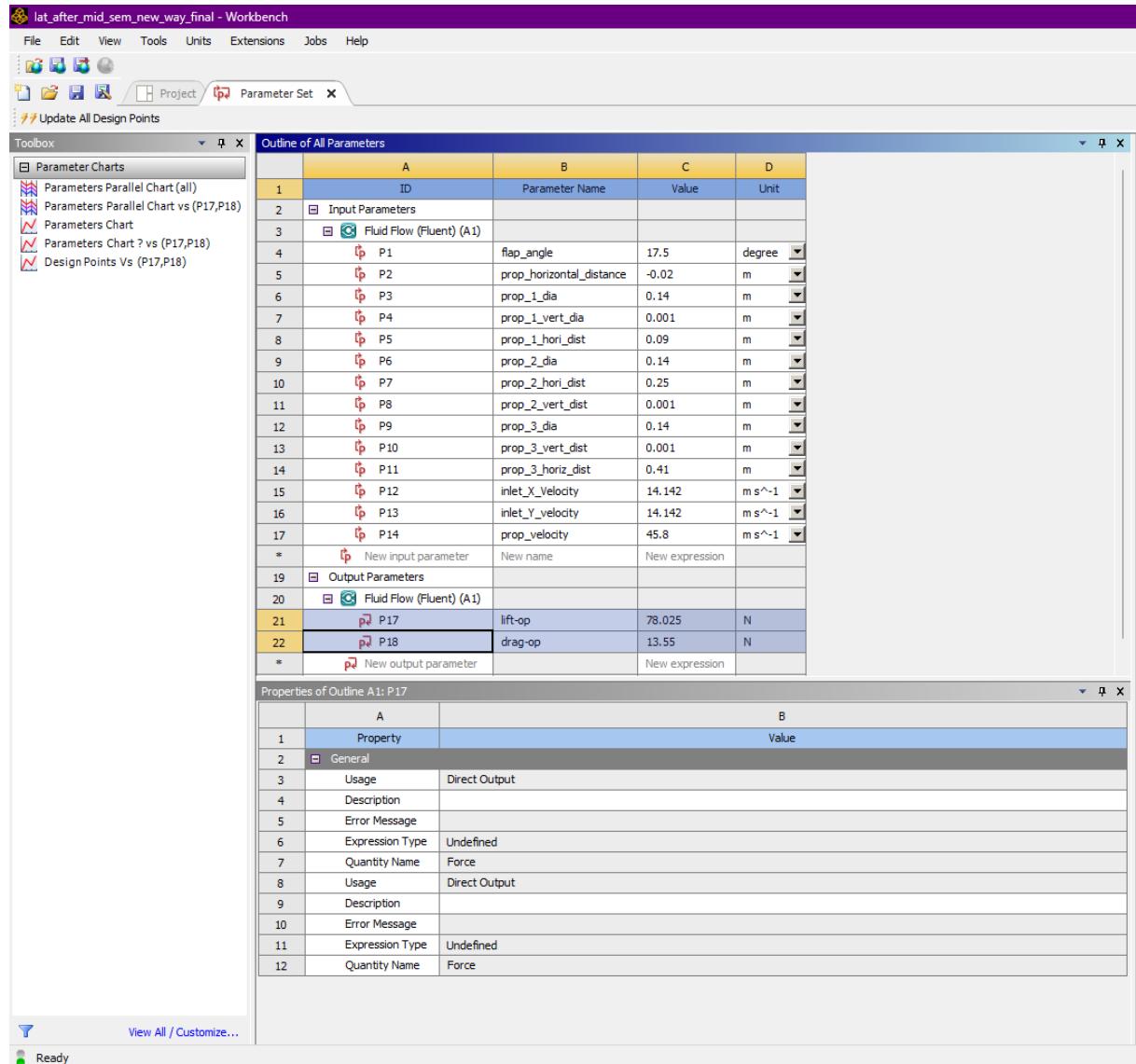


Figure 16: Lift and Drag Force at takeoff

### 20.3 Cruise Performance

Cruise conditions were evaluated at an altitude of 1000 m and a freestream velocity of

$$V_{\infty, \text{cruise}} = 80 \text{ m/s.}$$

For the clean-wing configuration ( $\phi = 0^\circ$ ,  $\alpha = 0^\circ$ ), the aerodynamic efficiency obtained was

$$\left( \frac{L}{D} \right)_{\text{cruise}} = 7.92$$

## 21 Flowfield Characterisation of the Final Blown-Wing Configuration

To validate the aerodynamic mechanisms responsible for the enhanced powered lift, flowfield visualisations were analysed for the final take-off configuration: angle of attack  $\alpha = 45^\circ$ , flap deflection  $\phi = 17.5^\circ$ , freestream velocity  $V_\infty = 20$  m/s, and propeller-induced jet velocity  $V_j \approx 45.8$  m/s. The following subsections summarise the major flow features governing the observed high lift.

### 21.1 Jet–Wing Interaction and Coandă Attachment

Figure 17 presents the three-dimensional slipstream structure generated during takeoff. The high-momentum wake is deflected downward and remains attached to the highly cambered flap due to the Coandă effect. This sustained attachment prevents premature flow separation, enabling the wing–flap system to operate stably at  $\alpha = 45^\circ$ , far beyond the natural stall limit of the unblown S1223 airfoil.

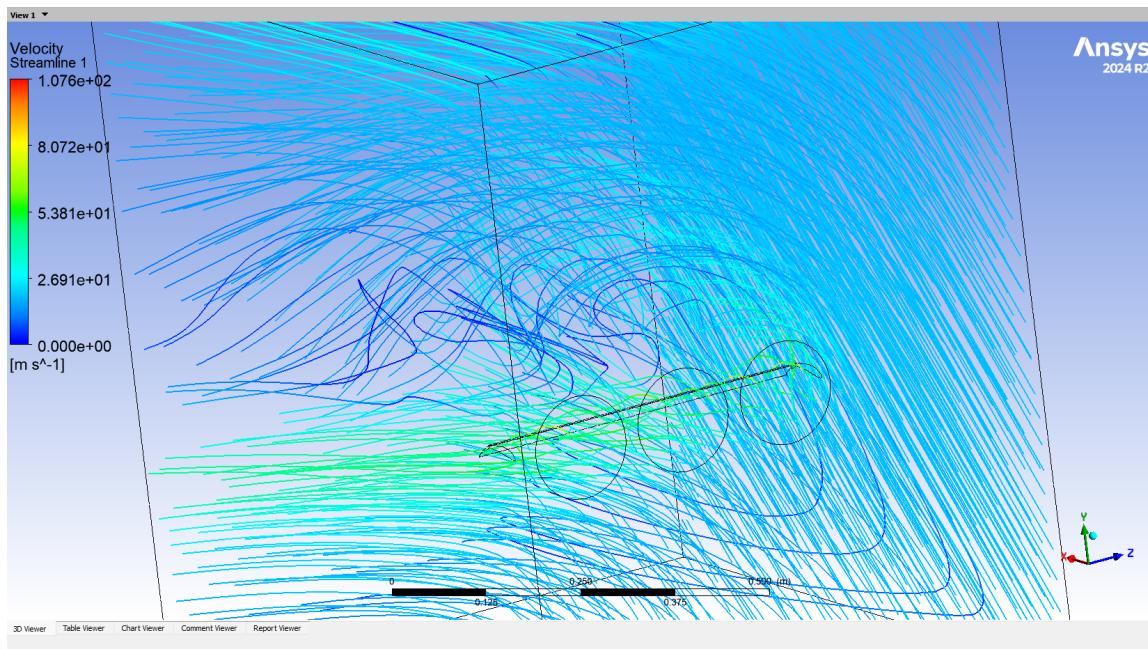


Figure 17: Three-dimensional streamlines showing slipstream attachment, jet curvature, and wake deflection for a half wing section.

A side-view of the same flowfield (Fig. 18) illustrates how the propeller slipstream expands, reattaches to the flap, and bends downward, producing a vertically deflected momentum jet that contributes directly to powered lift.

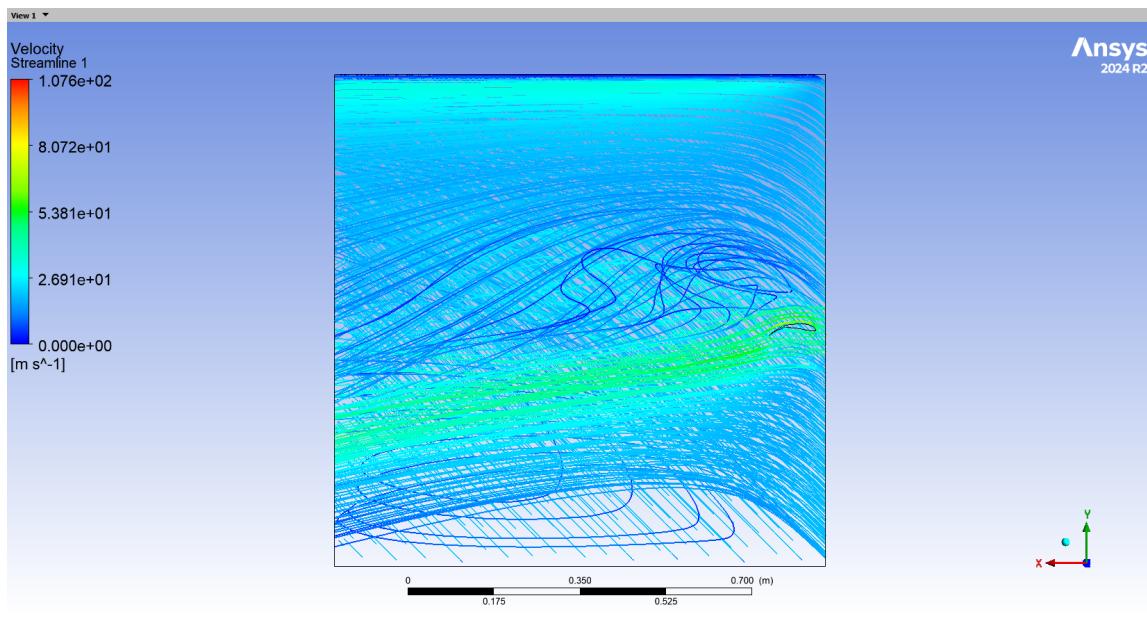


Figure 18: Side view of 3D streamlines showing slipstream adherence to the flap and downward jet deflection.

## 21.2 Circulation Augmentation and Surface Flow Acceleration

Mid-plane ( $z=0.24\text{m}$ ) and off-plane ( $z=0.33\text{m}$ ) streamline slices (Figs. 19 and 20) show pronounced acceleration over the upper surface. This velocity amplification is caused by both:

- the increased effective camber due to flap deflection, and
- the added momentum imparted by the propeller jets.

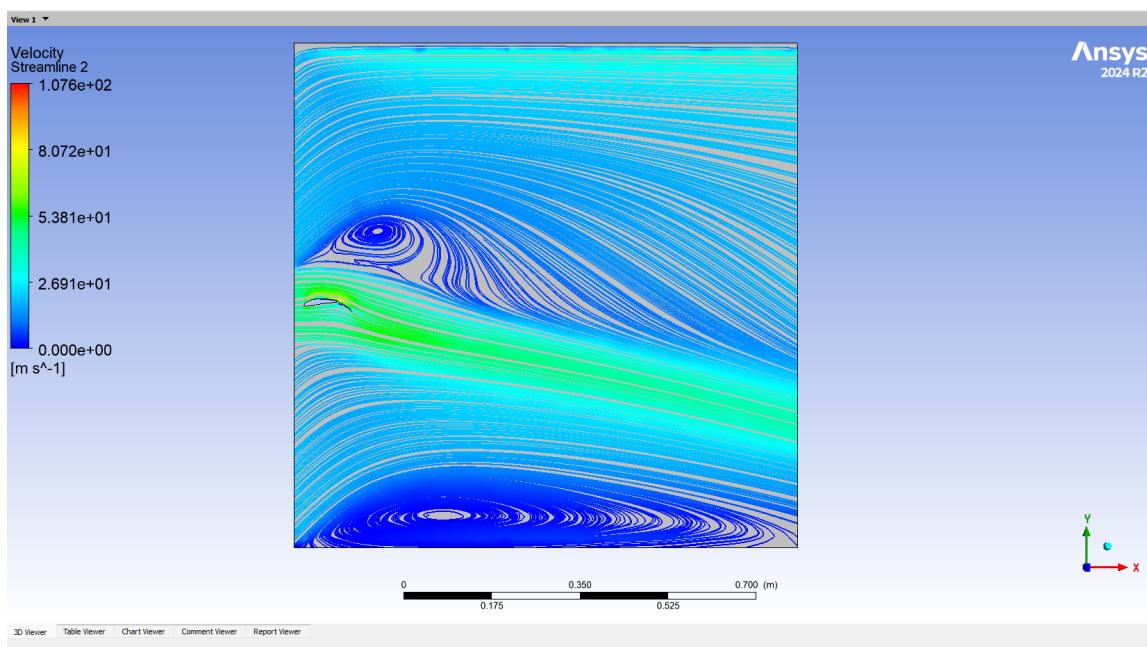


Figure 19: Mid-plane streamlines showing accelerated flow over the wing.

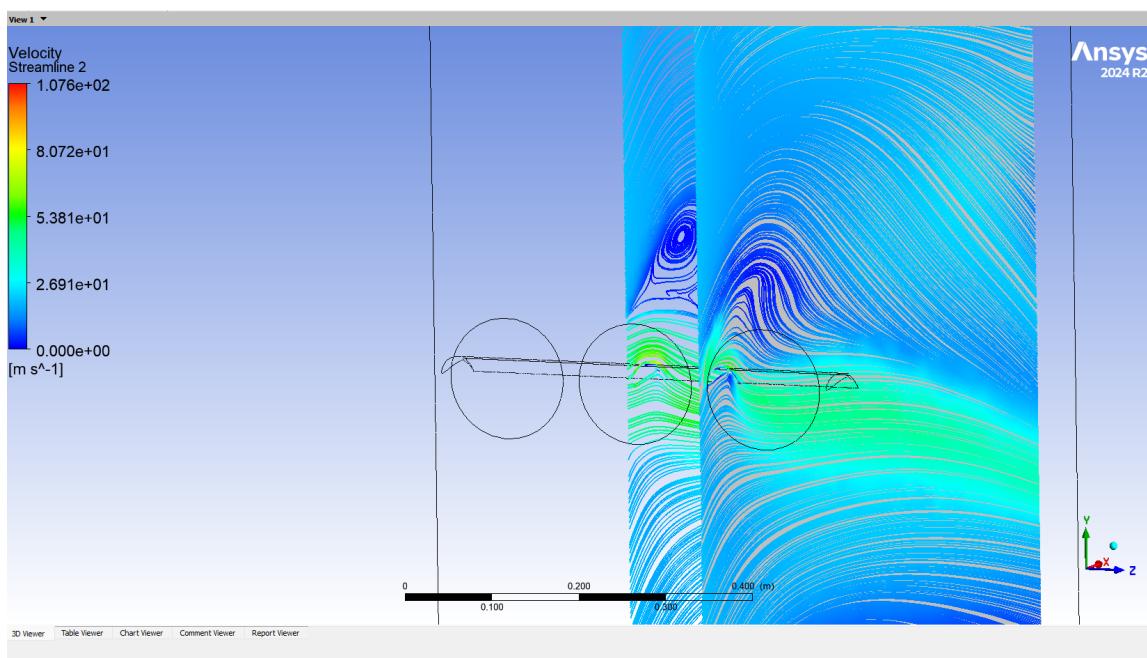


Figure 20: Streamlines at the two plane sections.

The amplified velocity field generates a large suction region on the upper surface, as shown by the pressure contours in Figs. 21 and 22. These low-pressure zones directly correlate with the measured lift coefficient of  $C_L = 6.911$ .

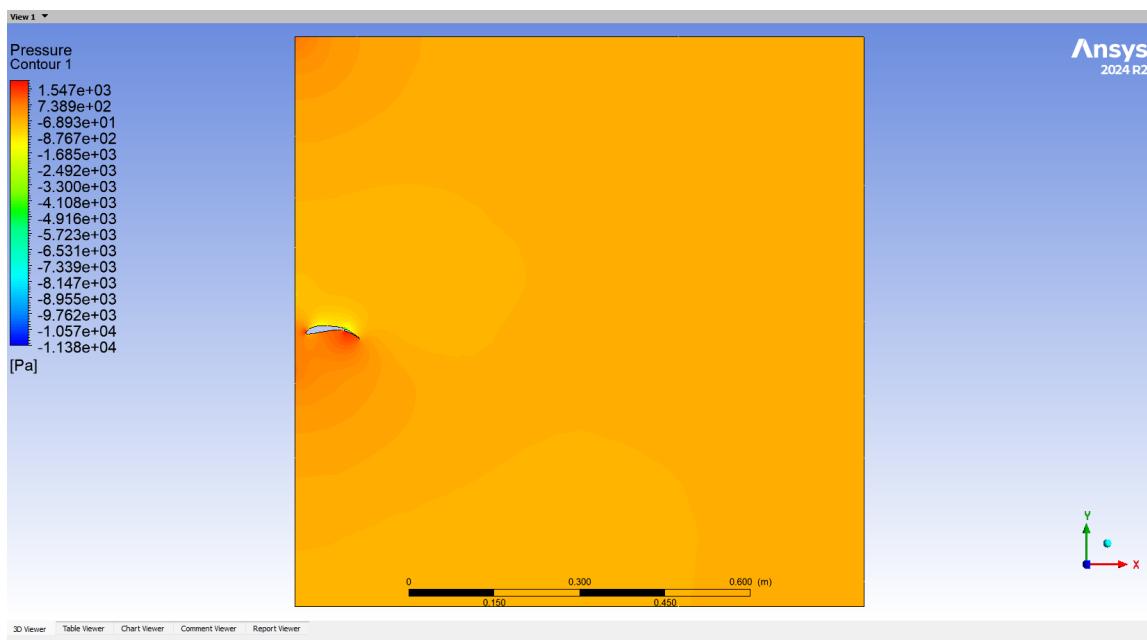


Figure 21: Pressure field at the mid-plane showing strong suction across the blown upper surface.

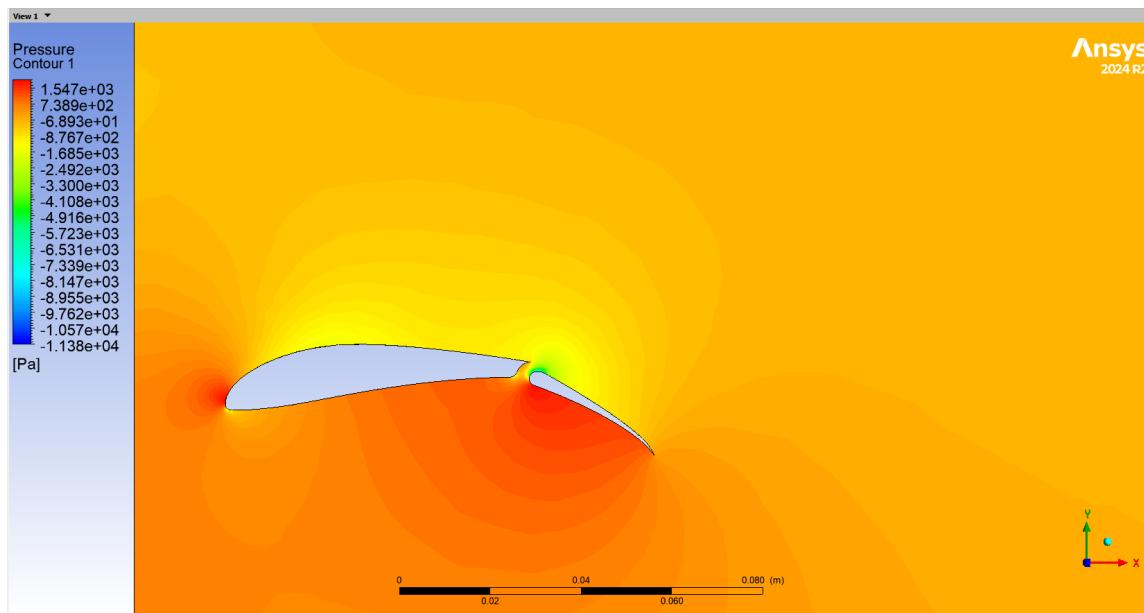


Figure 22: Close-up pressure contour showing intense low-pressure region at the flap leading edge and large pressure gradient on the upper and lower surface of the airfoil.

### 21.3 Momentum Wake Development

Figure 23 demonstrates that the wake downstream of the wing is highly collimated and strongly deflected downward. This confirms that the flap behaves as an effective momentum deflector, converting propulsive energy into aerodynamic lift.

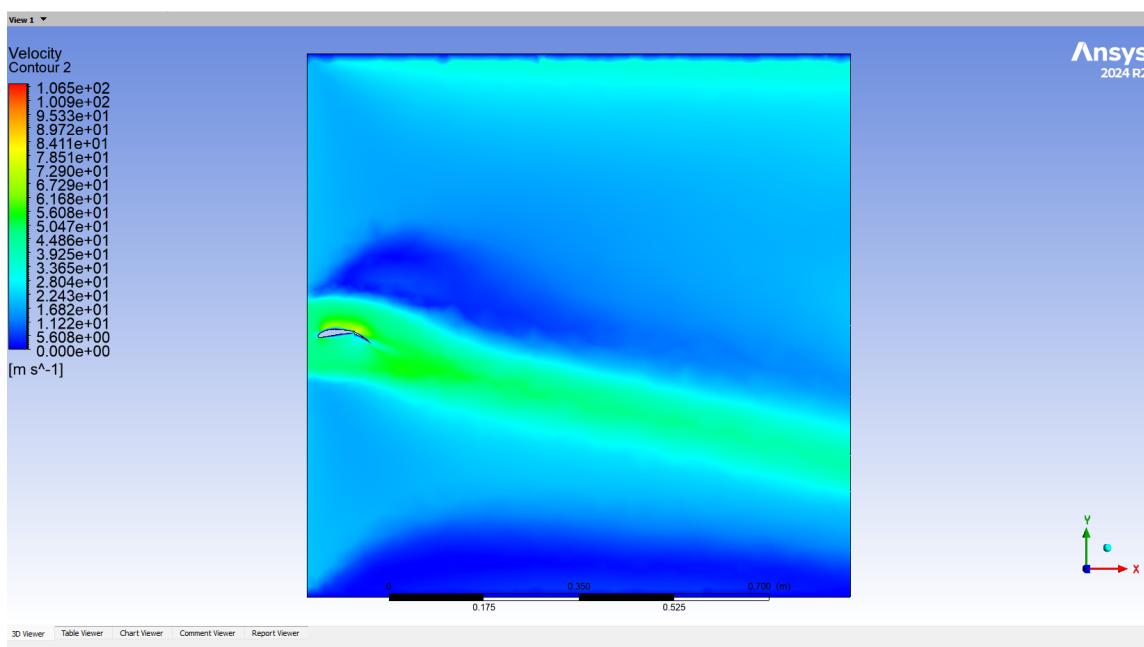


Figure 23: Downstream velocity field showing coherent, downward-deflected jet indicating powered-lift generation.

## 21.4 Flow Physics of the Slotted Flap

A key contributor to the sustained attached flow is the slotted flap system. As illustrated in Fig. 24, the slot allows a high-speed jet of air to pass through the gap between the main element and flap. This jet:

- re-energizes the boundary layer on the flap suction surface,
- delays or fully suppresses separation,
- enhances suction peaks, and
- boosts total circulation around the airfoil.

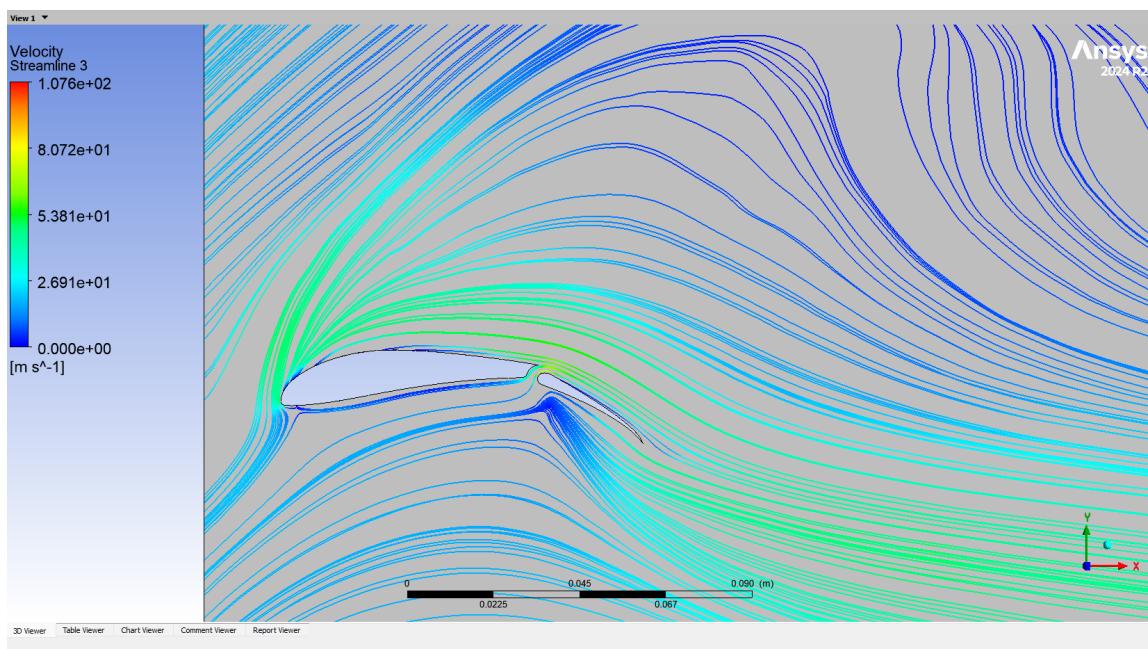


Figure 24: Streamlines passing through the slot and re-energizing the flap boundary layer.

The zoomed-in velocity and pressure contours (Figs. 25 and 26) clearly show the boundary-layer re-attachment downstream of the slot and the intensified suction peak.

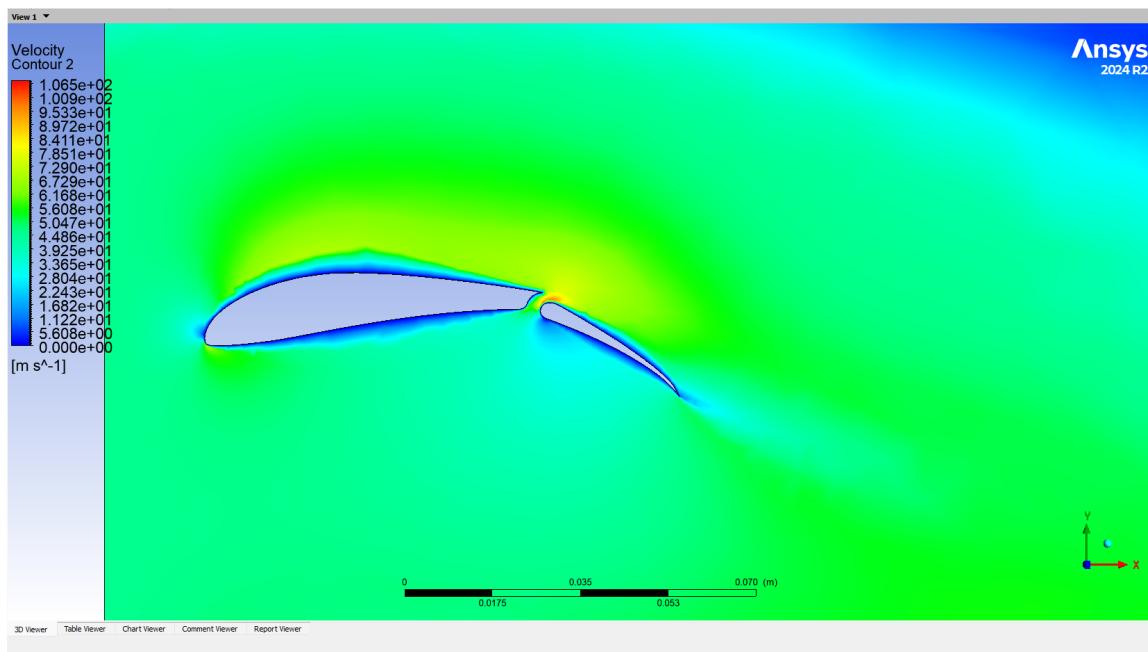


Figure 25: Zoomed-in velocity field at the slot showing high-speed jet injection and flap reattachment.

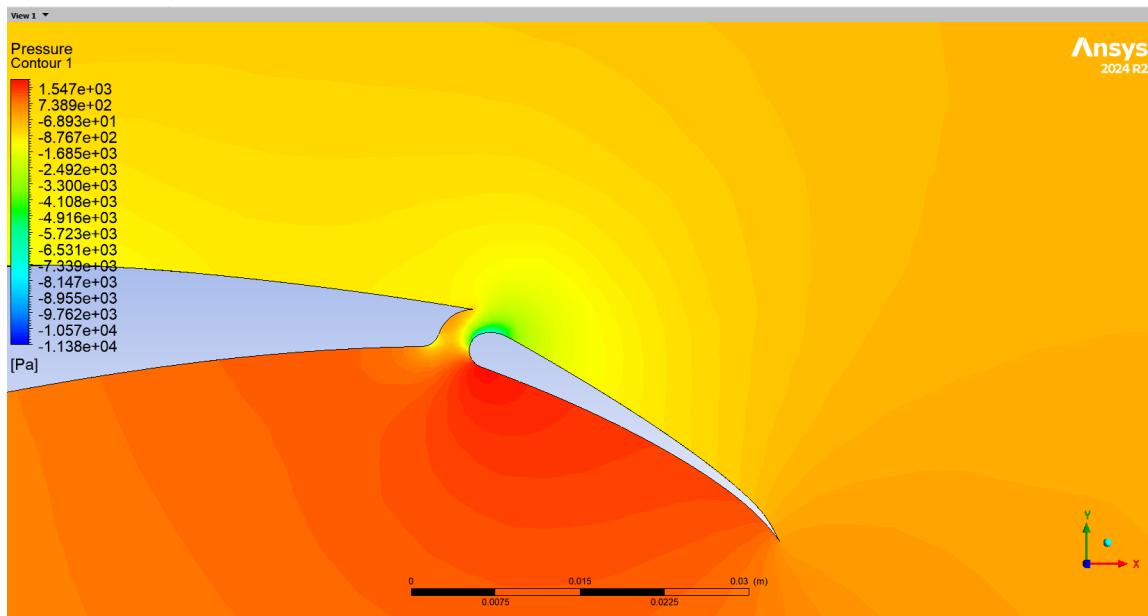


Figure 26: Zoomed-in pressure field showing the strong suction peak created by slot-induced reattachment.

## 21.5 Summary of Aerodynamic Mechanisms

The combined flow visualisations confirm:

- Complete suppression of stall at  $\alpha = 45^\circ$ .

- Strong velocity augmentation and suction over the blown flap.
- Efficient momentum deflection producing high powered lift.
- Effective boundary-layer re-energisation by the slotted flap.

These phenomena explain the high measured lift coefficient and demonstrate that the propulsion–airframe integration enables the aircraft to exceed the take-off requirement of lifting 20 kg at 20 m/s.

## 22 Overall Assessment

The work presented in this report establishes a comprehensive aerodynamic framework for a Short Take-Off and Landing (STOL) aircraft utilizing Externally Blown Flaps (EBF) driven by Distributed Electric Propulsion (DEP). The final configuration successfully integrates a high-aspect-ratio ( $AR = 10$ ) rectangular wing employing the Selig S1223 airfoil, selected for its superior low-Reynolds-number performance and responsiveness to slipstream energization. By strategically distributing six electric propellers along the leading edge, the design exploits the Coandă effect to reattach the boundary layer over a large, single-slotted flap deflected at  $17.5^\circ$ , thereby generating “super-circulation” far exceeding the capabilities of passive aerodynamic surfaces.

The design process followed a rigorous hierarchical approach, beginning with the development of a custom inverse-design tool based on Richard E. Kuhn’s semi-empirical method. This analytical phase screened over 36,000 geometric combinations to deterministically size the wing chord at 0.096 m, ensuring the baseline geometry was physically grounded before high-fidelity analysis. Subsequent validation was performed using RANS simulations in ANSYS Fluent, utilizing an actuator disk momentum source model validated against literature to accurately capture the physics of stall driven by jet detachment rather than classical separation.

AoA ( $^\circ$ )	Flap ( $^\circ$ )	$C_{L,CFD}$	$C_{L,theory}$
45	17.5	6.91	8.48

Table 8: Comparison of CFD and Theoretical Lift Coefficients for AoA–Flap Cases

The difference between the theoretical lift coefficient ( $C_{L,theory} = 8.48$ ) and the simulated value ( $C_{L,CFD} = 6.91$ ) reflects the different roles and assumptions of the two methods rather than an error in either approach. The modified Kuhn formulation represents an idealised, semi-empirical (upper-bound) estimate: it assumes efficient jet-to-flap momentum transfer, spanwise-uniform slipstream, and near-perfect flap turning and circulation augmentation. These assumptions intentionally neglect many three-dimensional and viscous loss mechanisms in order to produce a simple, conservative sizing guideline and a useful target for design.

By contrast, the RANS CFD solutions resolve the actual three-dimensional flow field around the finite wing and flap, including tip vortices, non-uniform slipstream footprint, propeller hub effects, viscous boundary layers, and local partial separation in the high-energy region. These physical mechanisms reduce the effective momentum available for lift and lower the realised circulation on the wing, producing the lower but physically realistic  $C_{L,CFD}$ . In practice this relationship is useful: the theoretical model sets an attainable design target and informs parametric exploration, while CFD provides the realistic performance baseline and identifies loss mechanisms that must be mitigated in detailed design and testing.

Performance evaluation confirms that the configuration meets and exceeds the primary mission objectives for short-field operations. At the critical take-off velocity of 20 m/s, the blown-wing system generates a maximum lift coefficient of approximately 6.91, resulting in a total lift force of 156.05 N. This performance provides a 15.9 kg lifting capacity, satisfying the 15 kg minimum payload requirement with a robust safety margin. Furthermore, the system achieves a take-off lift-to-drag ratio of 5.76, surpassing the minimum efficiency target of 5.0 and validating the effectiveness of the chosen propeller-flap interaction.

A definitive finding of the parametric studies was the extreme sensitivity of lift generation to the vertical placement of the propulsion units. Computational analysis revealed that positioning the propeller axis 0.001 m below the wing centerline was essential for ensuring full slipstream attachment to the airfoil surface, directly governing the magnitude of the powered-lift benefit. While the cruise performance at 80 m/s yielded a lift-to-drag ratio of 7.92—falling short of the idealized target for unblown flight—this represents an acceptable engineering compromise inherent to powered-lift designs, where high-camber airfoils optimized for extreme STOL capabilities naturally incur higher profile drag penalties during high-speed flight.

Complementing the aerodynamic success, preliminary structural sizing utilizing a Carbon-Epoxy composite box-spar architecture confirmed the feasibility of the design, yielding a low structural mass between 0.35 and 0.45 kg per half-wing while maintaining safety factors against bending stress and tip deflection. In conclusion, the project has delivered a fully validated, mechanically feasible aerodynamic configuration that eliminates the need for complex tilt-rotor mechanisms while reliably achieving VTOL-like lift coefficients. The integrated EBF-DEP architecture stands as a robust solution for air mobility applications requiring aggressive short take-off capabilities within constrained operating environments.

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