

Multi-Propeller Interaction and Rotational Effects in Distributed Electric Propulsion Aircraft

Hyeon Su Song^{*}, Rho Shin Myong[†], Hakjin Lee[‡]

Gyeongsang National University, Jinju, Gyeongnam 52828, South Korea

Abstract

This study presents a high-fidelity numerical investigation of propeller–wing interaction (PWI) in a full-configuration distributed electric propulsion (DEP) aircraft using a lattice Boltzmann method coupled with a very large eddy simulation (LBM–VLES) framework. Unlike previous studies, which are limited to simplified configurations, the present work resolves the unsteady flow physics of twelve closely spaced propellers installed along the wingspan of the NASA X-57 Maxwell aircraft in full configuration. The LBM–VLES framework is validated using the NASA Workshop for Integrated Propeller Prediction (WIPP) benchmark and applied to a full-configuration DEP system. The results show that propeller slipstreams significantly modify wing aerodynamics by altering the local effective angle of attack and boundary layer behavior, increasing lift by approximately 40–45% while introducing a drag penalty. The analysis further reveals that propeller rotational direction governs inter-propeller wake interaction and aerodynamic performance. Co-rotating configurations produce more uniform spanwise loading and delayed stall, whereas counter-rotating configurations induce strong upwash interaction, leading to localized load concentration and increased drag at high angles of attack. These findings provide new physical insights into multi-propeller wake interaction and identify rotational direction as a key design parameter for DEP aircraft.

Keywords: Distributed electric propulsion, Propeller–wing interaction, X-57 Maxwell aircraft, Computational fluid dynamics, Lattice Boltzmann method

^{*}Graduate Student, School of Aerospace Engineering, AIAA Member

[†]Professor, School of Aerospace Engineering, Associate Fellow AIAA

[‡]Associate Professor, School of Aerospace Engineering, AIAA Member; (Corresponding author: hlee@gnu.ac.kr)

1. Introduction

Propeller-driven aircraft are regaining popularity owing to the growing demand for energy-efficient propulsion systems for low- to medium-speed flight segments, particularly within short-range and commuter-class markets. Compared to turbofan engines, propellers offer up to 10%–20% fuel efficiency at low Mach numbers [1], making them cost-effective propulsion systems for small commuter aircraft and an attractive option for next-generation sustainable aviation concepts. Their efficiency, combined with their simpler mechanical architecture and electric or hybrid-electric powertrains, has positioned propellers as key components in the development of advanced air mobility (AAM) and urban air mobility (UAM) platforms, which seek to alleviate traffic congestion and improve regional mobility by using electric or hybrid-electric vertical takeoff and landing (eVTOL) aircraft. Among the emerging propulsion concepts, distributed electric propulsion (DEP) systems have attracted particular interest due to their ability to integrate multiple propulsors along the wing or airframe, thereby enabling increased system redundancy, enhanced fault tolerance, and improved controllability through differential thrust [2]. In DEP configurations, the interaction between propeller slipstreams and lifting surfaces introduces additional aerodynamic functionality, such as lift augmentation, boundary-layer energization, and stall delay, which are critical for short takeoff and landing (STOL) operations and constrained urban environments with demanding takeoff and landing profiles [3]. Unlike conventional propulsion systems, where thrust generation is largely decoupled from wing aerodynamics, DEP architectures directly modify wing aerodynamic performance through propeller slipstream effects, influencing lift generation, flow separation, and stall behavior.

Despite the aerodynamic advantages offered by DEP systems, their implementation introduces significant aerodynamic complexity owing to strong interactions between the slipstreams of multiple propellers and surrounding structures, particularly the wing. These interactions, commonly referred to as propeller–wing interaction (PWI), substantially alter the local flow environment and aerodynamic performance of the aircraft [4]. Accelerated propeller slipstreams energize the boundary layer over the wing surface, enhancing lift production and delaying flow separation at high wing angles of attack [5]. Although the powered-lift effect reduces takeoff and landing distances for DEP-based platforms such as eVTOL and STOL aircraft, adverse effects also occur, including unsteady aerodynamic loading, increased induced drag, and complex wake interaction phenomena, posing challenges to airframe design and flight control [6]. Moreover, the interaction between multiple closely spaced propellers leads to highly three-dimensional and time-dependent flow structures [4,6]. Especially, the proximity of rotating propellers to lifting surfaces can disrupt the development of helical tip vortices, potentially

triggering early vortex breakdown and transition to a turbulent wake state [7]. Modification of wake structure and localized variations in effective angle of attack on wing surfaces can significantly influence stall behavior, load distribution, and overall aerodynamic efficiency [8]. In particular, the superposition of induced velocity fields from adjacent propellers can produce constructive or destructive interference patterns, resulting in strong spanwise variations in aerodynamic loading that cannot be predicted using single-propeller assumptions [6,9]. Therefore, accurate characterization of PWI in multi-propeller DEP configurations requires high-fidelity numerical approaches capable of resolving unsteady wake dynamics and their interaction with the airframe. This requirement becomes increasingly critical in configurations such as NASA's LEAPTech and X-57 Maxwell demonstrators [7], where multiple high-lift propellers are closely spaced along the wing, leading to strong coupling between individual slipstreams.

Numerous studies have investigated PWI phenomena through both experimental and computational approaches. Experimental investigations have provided fundamental insight into the aerodynamic impact of propeller slipstreams, including the influence of rotational direction and installation location. Johnson et al. [8] demonstrated that inboard-up propeller rotation enhances wing lift by modifying spanwise load distribution. Catalano et al. [10] and de Vries et al. [7] examined the impact of installation location, such as leading-edge or over-the-wing mounting, revealing intricate interactions between the propeller slipstream and wing boundary layer, particularly under high-lift conditions. Similarly, Stephenson et al. [11] showed that the presence of an upstream aerodynamic surface, such as a wing, directly affects propeller inflow conditions from wind tunnel experiments. These authors also reported that PWIs influence the aerodynamic performance of the propeller system and cause measurable variations in its acoustic signature. Recently, Zanotti et al. [6] performed detailed thrust, torque, and Particle Image Velocimetry (PIV) measurements to examine the influence of propeller spacing on wake interactions. On the computational side, various modeling approaches have been developed to analyze PWI. Lower-fidelity methods, such as actuator disk models [12], vortex-based approaches [13–15], and Euler simulations [16], enable efficient parametric studies and early-stage analysis but are limited in resolving viscous boundary-layer behavior and detailed turbulent wake evolution. Therefore, Computational Fluid Dynamics (CFD) simulations based on the Reynolds-averaged Navier–Stokes (RANS) equations have been widely employed to improve the prediction of propeller–wing and propeller–airframe interaction effects at a practical computational cost. Gomariz-Sancha et al. [17] investigated propeller–airframe interaction using both a steady actuator-disk model and an unsteady RANS simulation. Jayasundara et al. [18] performed aerodynamic and aeroacoustic analyses of a wingtip-mounted tractor propeller, coupling RANS simulation with a Ffowcs Williams–Hawkings

acoustic solver to examine the respective contributions of propeller and wing noise. Chandukrishna and Venkatesh [19] employed a coupled RANS–blade element momentum approach to investigate aerodynamically efficient wingtip-mounted propeller configurations, including the influence of tractor and pusher arrangements. However, these approaches often rely on simplified geometries, limited numbers of propellers, or modeling assumptions that restrict their ability to fully capture the complex, three-dimensional nature of multi-propeller DEP systems. Furthermore, most existing studies focus on either isolated propeller–wing configurations or a small number of propulsors, which limits their applicability to realistic DEP aircraft where multiple propellers operate in close proximity and interact strongly with one another. More recently, the lattice Boltzmann method (LBM) has been applied to aerodynamic problems involving rotorcraft and propeller flows, demonstrating inherent advantages in resolving unsteady vortex dynamics with reduced numerical dissipation [20]. However, existing LBM studies on DEP systems have largely focused on simplified configurations or limited propeller arrangements. Therefore, further investigation is needed to extend high-fidelity LBM simulations toward realistic multi-propeller DEP aircraft.

Despite growing research efforts, high-fidelity investigations of PWI in full-configuration DEP aircraft remain limited, particularly for configurations involving multiple closely spaced propellers and their associated wake interactions. In particular, the influence of propeller rotational direction on inter-propeller wake interaction, stall behavior, and spanwise load distribution has not been systematically examined in realistic DEP aircraft configurations such as the NASA X-57 Maxwell. This limitation is critical because rotational direction directly determines the direction of induced swirl and thus governs the interaction between adjacent slipstreams, which can either stabilize or destabilize the boundary layer depending on locations of propeller. The present study addresses these gaps by performing a high-fidelity numerical investigation of PWI in a full-configuration DEP aircraft using a Lattice Boltzmann Method coupled with a very large eddy simulation (LBM–VLES) framework. A key feature of this work is the use of a blade-resolved model of the full-configuration NASA X-57 Maxwell aircraft, incorporating twelve high-lift propellers along the wing leading edge. In addition, configurations with co- and counter-rotating propellers are simulated to quantify the effect of rotational direction on slipstream behavior and spanwise lift distribution. This study presents a comprehensive analysis over a wide range of wing angles of attack, from takeoff to near-stall conditions, incorporating detailed comparisons of surface pressure distributions, skin friction contours, wake development, velocity profiles, and integrated aerodynamic coefficients. Prior to analyzing the full-configuration DEP system, the LBM–VLES solver is validated against experimental data obtained using NASA’s Workshop for Integrated Propeller Prediction (WIPP) configuration [20]. This

benchmark case, which features a single propeller mounted at a wingtip, facilitates a detailed evaluation of the ability of the solver to predict PWI effects, including local pressure amplification, lift enhancement, and wake velocity profiles. Subsequently, the validated solver with a numerical setup is applied to the full-configuration X-57 Maxwell aircraft. The analysis focuses on three primary objectives: (1) evaluating the powered-lift effect and stall delay mechanisms induced by the propeller slipstream across a wide range of angles of attack; (2) investigating the aerodynamic effects of upwash and downwash from multiple closely-spaced high-lift propellers, with particular focus on their influence on suction peak and flow attachment over the wing; and (3) assessing the influence of rotational direction by comparing configurations with co- and counter-rotating propellers, highlighting the impact of inter-propeller wake interactions and swirl cancellation on wing load distribution. Through this analysis, the study aims to establish a direct link between inter-propeller wake interaction and macroscopic aerodynamic performance, providing a physics-based interpretation of DEP-induced aerodynamic behavior. The findings of the study contribute to a deeper understanding of the aerodynamic mechanisms governing DEP systems and offer valuable design guidance for future electric aircraft employing DEP systems.

2. Numerical Methods

2.1 Lattice Boltzmann Method

Aerodynamic simulations were performed using PowerFLOW (SIMULIA, Dassault Systèmes), a commercial solver based on the lattice Boltzmann method (LBM). LBM is a mesoscopic flow simulation approach derived from the Boltzmann transport equation, wherein macroscopic fluid behavior is recovered by evolving particle distribution functions on a discrete lattice rather than solving the Navier–Stokes equations directly [21,22]. This methodology offers three key advantages over conventional CFD approach based on finite-volume method. First, its explicit and time-resolved formulation is well suited to capturing transient vortex shedding and complex unsteady wake interactions [23]. Second, its inherent compatibility with Cartesian lattice structures enables efficient handling of complex geometries, such as full-configuration aircraft with multiple rotating propellers, without the need for body-fitted meshes [24]. Third, its reliance on localized algebraic operations rather than global matrix solutions yields high parallel scalability, enabling large-scale simulations with cell counts exceeding 200 million [25,26]. These features make the LBM framework particularly suitable for blade-resolved simulations of full-configuration DEP aircraft, in which multiple rotating propellers generate strongly coupled, unsteady slipstream–wing interactions over a wide spanwise extent.

The discrete Boltzmann equation governing particle streaming and collision is expressed as:

$$f_i(\mathbf{x} + \mathbf{e}_i \Delta t, t + \Delta t) - f_i(\mathbf{x}, t) = -\frac{1}{\tau} [f_i(\mathbf{x}, t) - f_i^{eq}(\mathbf{x}, t)] \quad (1)$$

where $f_i(\mathbf{x}, t)$ represents the distribution function in direction i , and $\mathbf{e}_i$ is the discrete velocity vector, and τ is the relaxation time. The collision term is modeled using the Bhatnagar–Gross–Krook (BGK) operator [27], and the equilibrium distribution f_i^{eq} follows a second-order Maxwell–Boltzmann expansion [28]. The three-dimensional, 19-velocity (D3Q19) lattice model is adopted. For the D3Q19 lattice model used in this study, the summation in the equations in Eqs. (2) and (3) is performed over all 19 discrete velocity directions, including the rest particle, i.e., ($i = 0, \dots, 18$). The macroscopic flow variables density ρ and velocity $\mathbf{u}$ are recovered via distribution function moments:

$$\rho(\mathbf{x}, t) = \sum_i f_i(\mathbf{x}, t) \quad (2)$$

$$\rho \mathbf{u}(\mathbf{x}, t) = \sum_i \mathbf{e}_i f_i(\mathbf{x}, t) \quad (3)$$

PowerFLOW has been extensively validated for high-fidelity aerodynamic and aeroacoustic applications involving rotating machinery, including propeller performance prediction [29], rotor blade–vortex interaction [30], and high-lift device [31,32]. Moreover, Qiao et al. [33] demonstrated that the LBM–VLES framework accurately resolves slipstream-induced lift augmentation mechanisms and PWI phenomena. These prior works establish the reliability of the present solver for analyzing complex PWI phenomena in multi-propeller DEP systems. The applicability of the numerical framework to the present problem is further assessed through validation against the NASA WIPP configuration in Section 3.

2.2 LBM–VLES simulation

The present study employs a very large eddy simulation (VLES) framework within the LBM solver to capture the unsteady turbulent flow structures generated by multiple rotating propellers and their interaction with the wing surface in the DEP configuration. The LBM–VLES approach adopted in PowerFLOW provides a distinct turbulence-modeling framework, in which macroscopic flow behavior is recovered from discrete particle distribution functions on a Cartesian lattice [34,35]. Unlike conventional hybrid RANS/LES or Detached Eddy Simulation (DES) approaches, which require explicit interface regions between modeled and resolved turbulence, the VLES framework dynamically adjusts the level of turbulence resolution according to the local grid density and flow unsteadiness, allowing a smooth transition between modeled and resolved turbulent structures without

user-defined switching criteria. Within this framework, turbulence is incorporated into the collision operator through a locally varying effective relaxation time (τ_{eff}) that accounts for subgrid-scale effects. Specifically, the solver employs a modified k - ε two-equation model based on the renormalization group (RNG) formulation [36] to compute τ_{eff} , which replaces the molecular relaxation time τ in the LBM equation:

$$\tau_{eff} = \tau + C_\mu \frac{k^2 / \varepsilon}{\sqrt{1 + \eta^2}} \quad (4)$$

where $C_\mu = 0.09$, k and ε denote the turbulent kinetic energy and its dissipation rate, respectively; and η is a local parameter dependent on the strain rate, vorticity, and helicity of the flow. This effective relaxation time determines the eddy viscosity within the LBM collision process and thereby controls the dissipation of turbulent energy. As a result, the LBM-VLES formulation retains the time history of turbulent fluctuations and captures the nonlinear evolution of coherent vortical structures [34,37]. This capability is particularly important for the present DEP problem, in which the flow field is dominated by strongly three-dimensional and unsteady interactions among propeller tip vortices, slipstream-induced boundary-layer reattachment, and inter-propeller wake interference.

2.3 Wall Modeling

Although the present framework enables efficient simulation of large-scale unsteady DEP flows, near-wall turbulence is not resolved down to the viscous sublayer; instead, it is modeled using a generalized wall-function approach. The near-wall velocity profile is reconstructed using a generalized wall function applied at the first wall-adjacent lattice cell, enabling the prediction of wall shear stress and boundary layer behavior without explicitly resolving the viscous sublayer. This treatment is particularly suitable for the present full-configuration DEP simulations, where the simultaneous resolution of multiple rotating propellers, wall-bounded flow, and extended wake structures would otherwise impose excessive computational cost. Within the LBM framework, solid surfaces are represented using the volumetric wall formulation implemented in PowerFLOW. In this formulation, the solid boundary is discretized using surface elements, which are embedded within the Cartesian voxel mesh [38]. The grid resolution employed in the present study is determined based on a grid convergence analysis, which is described in detail in Section 4.2. Briefly, five grid levels corresponding to 1.0%, 0.75%, 0.5%, 0.3%, and 0.25% of the propeller tip chord length were examined, and a minimum cell size corresponding to 0.5% of the tip chord was selected because the tip chord length serves as a representative length scale for rotor

simulation. In the present LBM–VLES framework, the selected grid resolution ensures that the first off-wall cells predominantly fall within the logarithmic region of the boundary layer.

The generalized law-of-the-wall model is applied at the first wall-adjacent cell. This formulation incorporates corrections for pressure-gradient effects and surface roughness, enabling the prediction of wall shear stress, flow separation, and reattachment under high-Reynolds-number conditions [39,40]. The relationship is expressed as follows:

$$u^+ = \frac{1}{\kappa} \ln \left(\frac{y^+}{A} \right) + B \quad (5)$$

$$A = 1 + f \left(\frac{dp}{dx} \right), \quad y^+ = \frac{u_\tau y}{\nu} \quad (6)$$

where u^+ denotes the normalized velocity relative to the friction velocity u_τ , y^+ refers to the non-dimensional wall distance based on the viscous length scale, A captures the effect of the local streamwise pressure gradient, and B provides the empirical offset required to match experimental measurements with $\kappa = 0.41$ and $B = 5.0$. The function smoothly bridges the viscous sublayer and the logarithmic region, enabling accurate shear stress estimation across different boundary layer regimes. In addition, the parameter A represents the pressure-gradient correction factor in the generalized law-of-the-wall model. In Eq. (6), dp/dx denotes the local streamwise pressure gradient evaluated along the wall-tangential direction, and $f(dp/dx)$ represents the empirical correction associated with the pressure-gradient effect on the near-wall velocity profile. For a zero-pressure-gradient boundary layer, this correction vanishes, and the parameter A approaches unity, reducing Eq. (5) to the standard logarithmic law of the wall. Under favorable or adverse pressure-gradient conditions, the parameter A modifies the effective wall coordinate, thereby adjusting the reconstructed near-wall velocity profile and the estimated wall shear stress. This formulation follows the generalized wall-function concept of Shih et al. [40], which accounts for pressure-gradient effects and extends the applicability of wall-function treatments to complex wall-bounded flows. The function smoothly bridges the viscous sublayer and the logarithmic region, enabling wall shear stress estimation across different boundary-layer regimes.

For the present DEP problem, this wall-model treatment is important because the aerodynamic behavior is strongly influenced by local flow attachment, separation onset, and slipstream-induced reattachment over the wing surface. In particular, the interaction between propeller slipstreams and the wing boundary layer introduces strong local variations in momentum and pressure gradients, making accurate prediction of wall shear stress and separation behavior essential for capturing the powered-lift effect and stall characteristics. The combined

volumetric wall formulation and generalized wall function provide a practical compromise between computational efficiency and near-wall fidelity for full-configuration simulations involving multiple rotating propellers and large wake regions [41,42]. Nevertheless, because the viscous sublayer is not explicitly resolved, the near-wall accuracy may be limited in regions with strong separation onset, transitional behavior, or locally complex wall-bounded vortex interactions. Therefore, the present study focuses on capturing large-scale aerodynamic phenomena, such as slipstream-induced lift augmentation, wake interaction, and separation behavior, which dominate the overall aerodynamic performance of DEP systems.

3. Numerical Validation

3.1 WIPP Model

In the present study, a two-stage validation approach is adopted to ensure the reliability of the numerical framework for analyzing PWI in DEP systems. First, the LBM-VLES solver is validated against the NASA WIPP benchmark, which represents a canonical single-propeller wingtip-mounted configuration and provides a well-established experimental database for assessing PWI prediction capability. Second, the applicability of the numerical approach to realistic DEP configurations involving multiple interacting propellers is further assessed using the full-configuration NASA X-57 Maxwell aircraft, where grid convergence and time-step sensitivity analyses are performed to ensure numerical consistency under multi-propeller operating conditions, as described in Section 4.

In aircraft employing DEP systems, PWIs play a critical role in determining overall aerodynamic performance. Mutual interference between the rotating propeller slipstream and wing surface induces complex and highly unsteady flow phenomena. In particular, the accelerated propeller slipstream substantially alters the pressure distribution and boundary layer behavior on the adjacent wing surface, influencing lift, drag, and stall characteristics. Accurate PWI predictions remain a considerable challenge during aerodynamic analyses of DEP systems. Nevertheless, capturing interactional phenomena is essential for reliable performance prediction and design optimization of DEP-integrated configurations. The predictive capability of the present numerical framework in capturing PWI effects was validated using NASA's WIPP configuration. The WIPP experimental campaign provides a rigorously documented, publicly accessible experimental database that was specifically designed for validating computational methods applied to wingtip-mounted propeller configurations [20]. The

numerical validation quantitatively compared the simulation results with experimental data for key aerodynamic metrics, including wing surface pressure coefficients (C_p) and wake velocity profiles.

The test model comprised a 10% scale C-130 propeller mounted on a finite wingtip with an aspect ratio of 6.7. The propeller featured a diameter of 16.2 inches and fixed pitch angle of 37.5° , which was representative of cruise operating conditions. The experiments were conducted in the Lockheed Martin Low-Speed Wind Tunnel (LSWT), where the model was instrumented to capture detailed aerodynamic characteristics of the flow field associated with propeller-wing interaction. In total, 96 static pressure taps were installed across six spanwise stations located 34, 44, 54, 57, 60.75, and 63 inches above the tunnel floor. Each station included 10 static pressure taps on the upper surface and 6 static pressure taps on the lower surface. In addition to surface pressure measurements, a wake rake system was employed to collect downstream flow-field data behind the propeller, nacelle, and wing. These data provide a high-fidelity reference for validating near-field and wake flow predictions. A detailed comparison of the calculated surface pressure coefficients along the spanwise stations is presented in Section 3.2, while the wake velocity profiles are quantitatively assessed in Section 3.3.

The computational simulations were conducted using the same model geometry employed in the WIPP experimental campaign, with the exception of the mounting structure used to support the model on the wind tunnel floor. Fig. 1 illustrates the overall surface and volume mesh for the isolated wing and full configuration considered during the validation. The mesh was generated using the Cartesian lattice-based meshing procedure implemented in PowerFLOW. Unlike conventional body-fitted finite-volume meshes, the present LBM-VLES framework discretizes the computational domain using Cartesian lattice cells, or voxels. The solid surfaces of the wing, nacelle, and propeller blades were represented by surface elements embedded within the Cartesian voxel grid, allowing curved and inclined geometries to be treated without generating body-fitted surface-conforming cells. The isolated wing configuration comprises the wing and nacelle, excluding the propeller, allowing an assessment of the baseline aerodynamic characteristics of the system without the influence of the rotating propeller. By contrast, the full configuration includes the wing, nacelle, and operating propeller, enabling a direct evaluation of PWI effects. The isolated wing configuration corresponds to the power-off test case, while the full configuration represents the power-on test case. The contribution of the propeller wake to changes in surface pressure and lift characteristics can be quantified by comparing these configurations. The computational domain was designed to replicate the test section of the LSWT environment. No-slip wall boundary conditions were imposed on all solid surfaces, including the propeller and wing. Near-wall flow was treated using the volumetric wall formulation and generalized wall-function model. Therefore, no body-fitted prism-layer mesh was generated near the solid

surfaces. The remaining outer boundaries were treated as the velocity inlet and pressure outlets to minimize nonphysical reflections. The domain was discretized using ~ 150 million cells, with a minimum resolution of 0.3 mm, corresponding to $\sim 0.3\%$ of the propeller tip chord. The near-field mesh was refined around the blade surface, and a high-resolution wake-capturing zone was applied in the downstream region to resolve the vortical structures generated from the propeller. Local mesh refinement was achieved using the variable-resolution (VR) approach available in PowerFLOW. In this approach, the Cartesian voxel grid is hierarchically refined by assigning nested refinement regions with different local cell sizes, allowing fine resolution to be concentrated only in flow regions where high spatial gradients are expected while maintaining a coarser resolution in the far field. In the present simulation, these nested VR regions were prescribed around the propeller blades, nacelle, wing surface, and downstream wake region to resolve the propeller wake and its deformation caused by PWI effect. A cylindrical rotating region enclosing the propeller was defined, and the sliding mesh approach was employed to account for its rotation.

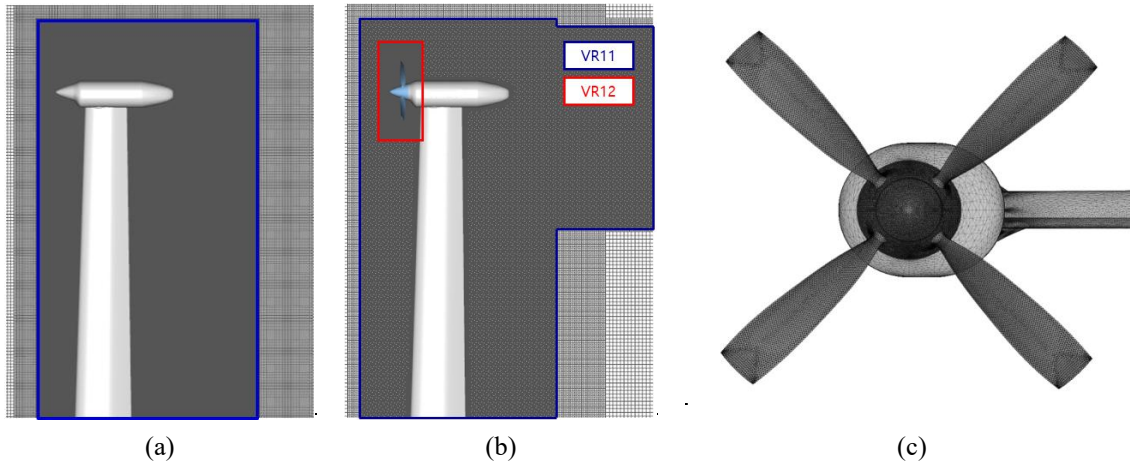


Fig. 1. Cartesian voxel mesh and variable-resolution layout for the WIPP validation: (a) cross-sectional view of the volume mesh around the nacelle, (b) nested variable-resolution regions around the propeller-wing configuration, and (c) detailed view of the surface mesh on the propeller blades.

The simulation conditions were matched to the experimental setup and are summarized in Table 1. The freestream Mach number was set to 0.08, and the propeller rotational speed was fixed at 5900 rpm, resulting in a tip Mach number of 0.437 and a chord-based Reynolds number of $\sim 5.3 \times 10^5$. The LBM-VLES simulations were conducted at four wing angles of attack, denoted by α , consistent with the WIPP test matrix to evaluate aerodynamic sensitivity to inflow incidence. These conditions were consistent with the test matrix employed in WIPP experiments, enabling a direct comparison of predicted values with measured surface pressure distributions and wake profiles. Each simulation was conducted for 15 propeller revolutions, corresponding to 0.15 s. Fig. 2

presents the time histories of the propeller thrust coefficient (C_T) and torque coefficient (C_Q) of propeller at $\alpha = 0^\circ$ over the final 10 propeller revolutions. Both coefficients exhibit periodic fluctuations associated with blade passage and wake interaction, while remaining bounded around nearly constant mean values. This behavior indicates that the propeller loading reached a statistically stationary state. Therefore, the final 10 revolutions were used to compute the time-averaged flow fields and aerodynamic coefficients.

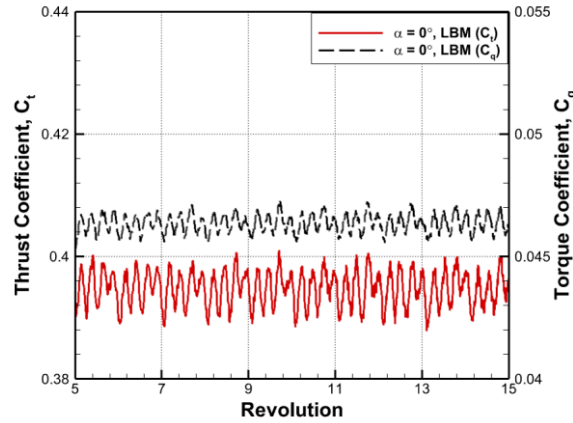


Fig. 2. Time histories of propeller loading for the WIPP validation case at $\alpha = 0^\circ$ over the final 10 propeller revolutions.

Table 1. Simulation conditions during the validation, consistent with WIPP experiments.

Parameter	Value
Freestream Mach number [M_∞]	0.08
Wing angle of attack [α]	$0^\circ, 5^\circ, 7^\circ, 15^\circ$
Pressure [Pa]	101,325
Total simulation time	15 revolutions (0.15 s)
Reynolds number (Re)	528,962
Wingtip Mach number (M_{tip})	0.437
Rotating speed (rpm)	5900

3.2 PWI Effects on Surface Pressure Coefficients

The LBM-based solver's ability to capture PWI effects on aerodynamic performance was evaluated by calculating the surface pressure coefficient distribution and integrated aerodynamic forces. Quantitative comparisons between the LBM-predicted and experimental data were performed at selected spanwise locations to assess the accuracy of the simulation in resolving the localized influence of the propeller slipstream on the wing. Surface pressure taps (S1–S3) were located along the spanwise direction from the wing root. S1 was located directly within the core of the propeller slipstream and was expected to exhibit the strongest PWI effects. S2 and

S3 were located progressively outward from the propeller disk and provided insights into the radial attenuation of PWI effects across the wing. The velocity measurement planes (P1–P4) were defined along the freestream direction from the propeller spinner, enabling the characterization of propeller wake development and slipstream decay in the wake region. Fig. 3 illustrates the locations of surface pressure taps and velocity measurement planes considered during the validation, while their spatial coordinates are provided in Table 2. Such a comparative validation enabled a comprehensive validation by enabling quantitative analysis of aerodynamic loading and 3D wake flow evolution, which are critical to understanding the nature of PWIs in DEP systems.

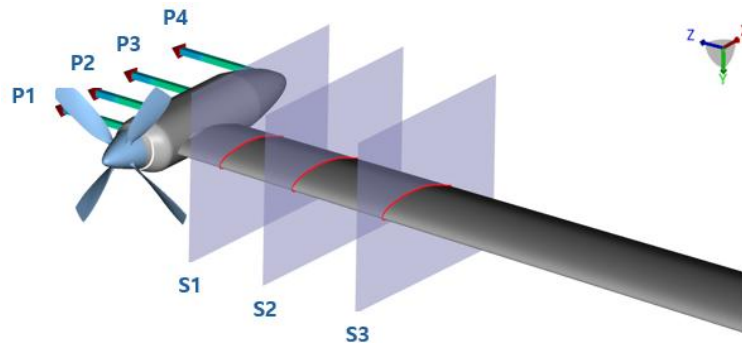


Fig. 3. Locations of surface pressure taps (S1–S3) and wake velocity measurement planes (P1–P4) in the validation.

Table 2. Spatial coordinates of pressure taps (S1–S3) and wake velocity planes (P1–P4)

Section	Distance from wing root (along +z direction) (in)	Section	Distance from spinner (along +x direction) (in)
S1	60.955	P1	1.5
S2	57.386	P2	5.0
S3	44.386	P3	13.0
		P4	21.0

Prior to the detailed comparison of wing pressure distributions, the aerodynamic performance of the propeller was evaluated for the isolated wing and full configuration at $\alpha = 0^\circ$. The predicted thrust coefficient (C_T) and torque coefficient (C_Q) values were compared with experimental data. The LBM–VLES simulation yielded $C_T = 0.397$ and $C_Q = 0.0465$, in close agreement with the measured values of $C_T = 0.391$ and $C_Q = 0.0485$, indicating that the LBM–VLES simulation accurately predicted propeller loading under representative operating conditions. Fig. 4 presents a detailed comparison of the LBM-predicted and experimentally obtained surface pressure coefficient distributions at S1, S2, and S3. The isolated wing (power-off case) and full-configuration (power-on

case) results are illustrated at four wing angles of attack. This comparative analysis enabled a spanwise-resolved assessment of PWI effects under varying operational conditions, offering insights into the induced local aerodynamic loading.

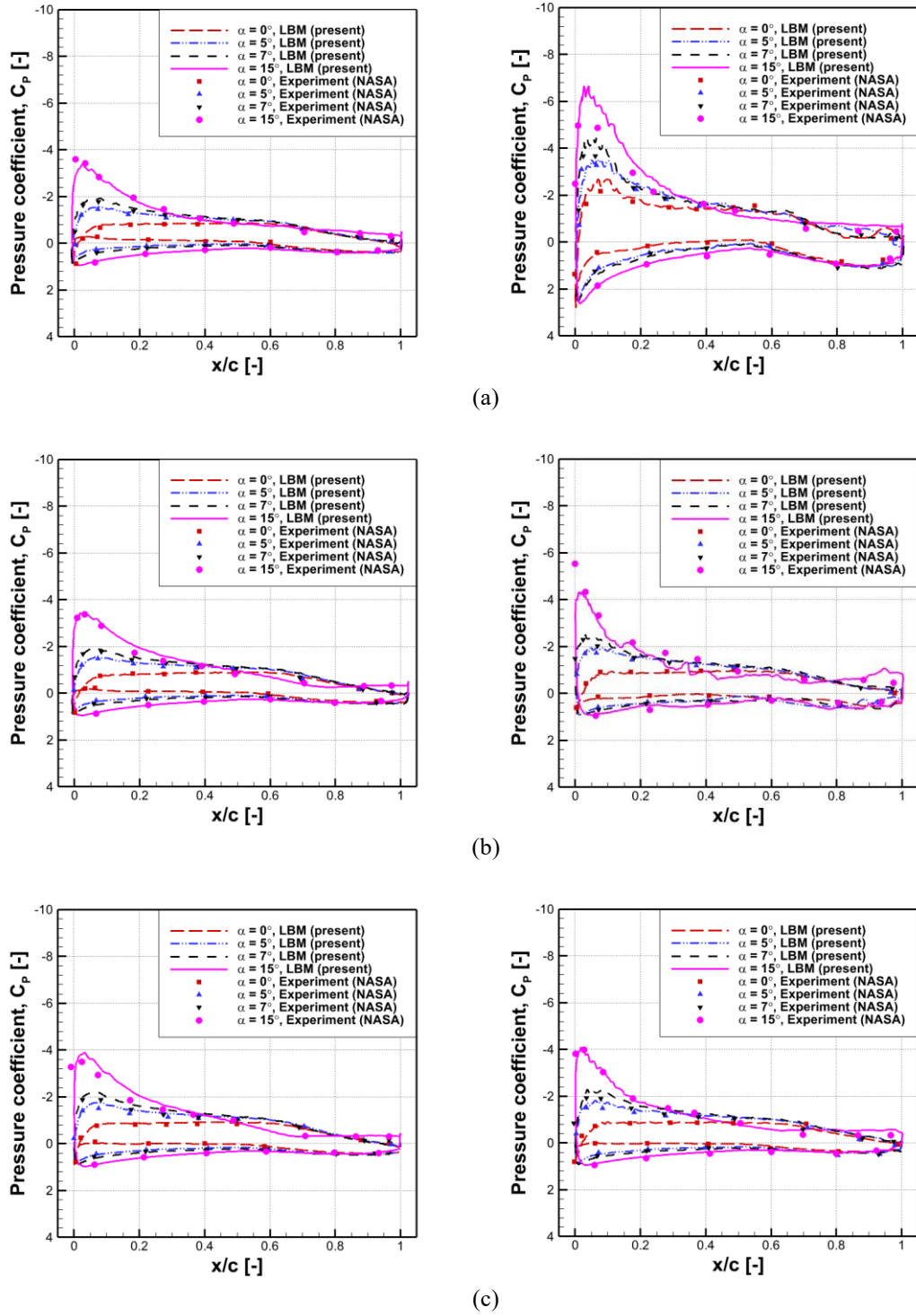
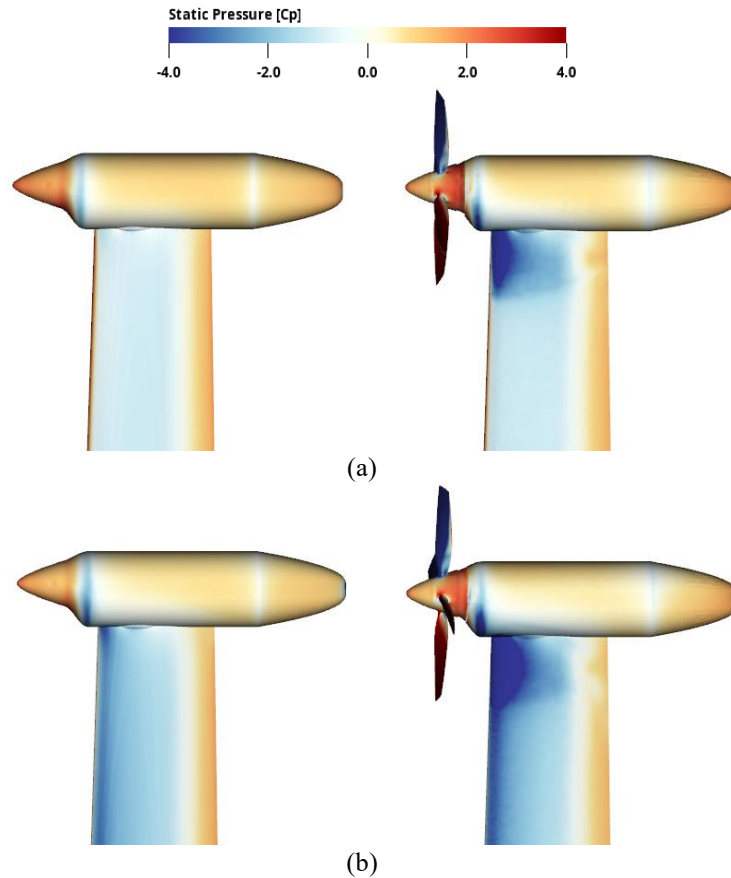


Fig. 4. Comparison of surface pressure coefficient (C_p) distributions of the isolated wing (left) and full configuration (right) at $\alpha = 0^\circ, 5^\circ, 7^\circ$, and 15° at spanwise stations: (a) S1, (b) S2, and (c) S3.

As shown in Fig. 4(a), the PWI influence was most pronounced at S1, which was located within the core of the propeller slipstream. Among all wing angles of attack, the difference in sectional pressure distribution between the isolated wing and full configuration was greatest at this location, highlighting the dominant role of propeller-induced flow near the wingtip. For the full configuration, the suction peak near the leading edge was substantially amplified for all wing angles of attack. At $\alpha = 0^\circ$, the magnitude of the negative pressure coefficient for the full configuration was nearly double that for the isolated wing, which was attributed to the local increase in dynamic pressure induced by accelerated axial flow in the propeller wake. The resulting lift enhancement was consistent with the mechanism of propulsive lift augmentation, as previously reported in experimental and numerical studies of DEP systems [20,33]. The LBM–VLES predictions accurately captured the shape and magnitude of the surface pressure profiles at S1, indicating that the numerical method was capable of resolving PWI-induced highly localized flow phenomena. The suction peak intensified with increasing wing angle of attack for both configurations; however, the full configuration consistently exhibited a higher magnitude than the isolated wing. At $\alpha = 15^\circ$, the simulation slightly overpredicted suction at the leading edge, potentially due to predictive limitations in stall onset. Nonetheless, the overall trends were well captured, and the influence of the propeller remained dominant at S1 for all wing angles of attack. As shown in Fig. 4(b), the influence of the propeller became less pronounced but remained detectable at S2, which was located near the outer boundary of the slipstream envelope. The full configuration exhibited a moderate increase in suction on the upper surface, particularly at lower wing angles of attack. However, the magnitude of these changes was reduced compared to S1. Although the influence of the propeller wake was weaker at S2 than at S1, it remained measurable and physically meaningful. Moreover, the LBM–VLES simulation results agreed closely with experimental data at low and moderate wing angles of attack (0° , 5° , and 7°), successfully capturing the pressure gradient and suction peak locations for both configurations. However, the simulation tended to slightly underpredict the magnitude of the suction peak for the full configuration compared to experimental data at the highest wing angles of attack (15°). This behavior was consistent with previously reported RANS- and DES-based simulations of the WIPP configuration [20], potentially due to modeling limitations in resolving transitional separation and stall characteristics in regions of intermediate slipstream exposure. It is also worth noting that, at the highest angle of attack, the discrepancy between the simulation and experiment exhibits opposite trends at S1 and S2. The LBM–VLES prediction slightly overestimates the suction peak at S1, whereas it underestimates the suction level at S2. Because S1 is located within the core of the propeller slipstream and S2 lies near the outer boundary of the slipstream envelope, this opposite error tendency suggests that the predicted wake distribution may be slightly shifted or more spatially

concentrated compared with the experiment. In other words, a small difference in the spanwise position or radial diffusion of the propeller wake can locally intensify the suction at S1 while reducing the slipstream influence at S2. This sensitivity becomes more pronounced at high angle of attack, where the propeller wake is further distorted by the wing-induced adverse pressure gradient and incipient separation. A similar tendency is also observed in the wake velocity comparison discussed in Section 3.3, where the LBM–VLES results show slight wake contraction and reduced peak axial velocity relative to the experimental data. As shown in Fig. 4(c), the surface pressure distributions for the isolated wing and full configuration were nearly indistinguishable at S3, which was located outside the influence of the propeller wake. For all wing angles of attack, the LBM solver successfully captured the baseline aerodynamic characteristics of the wing, including the sharp suction peak at the leading edge and smooth pressure recovery along the chord. These results confirmed that the aerodynamic effects of the propeller were highly localized and did not considerably influence outboard wing sections in the freestream-dominated region. Overall, the validation results demonstrated that the simulation framework accurately captured localized surface pressure amplification due to propeller-induced flow and the radial decay of slipstream influence along the wingspan. Notably, the agreement with experimental data under multiple operating conditions confirmed the ability of the LBM–VLES method to predict integrated PWI effects in DEP systems.



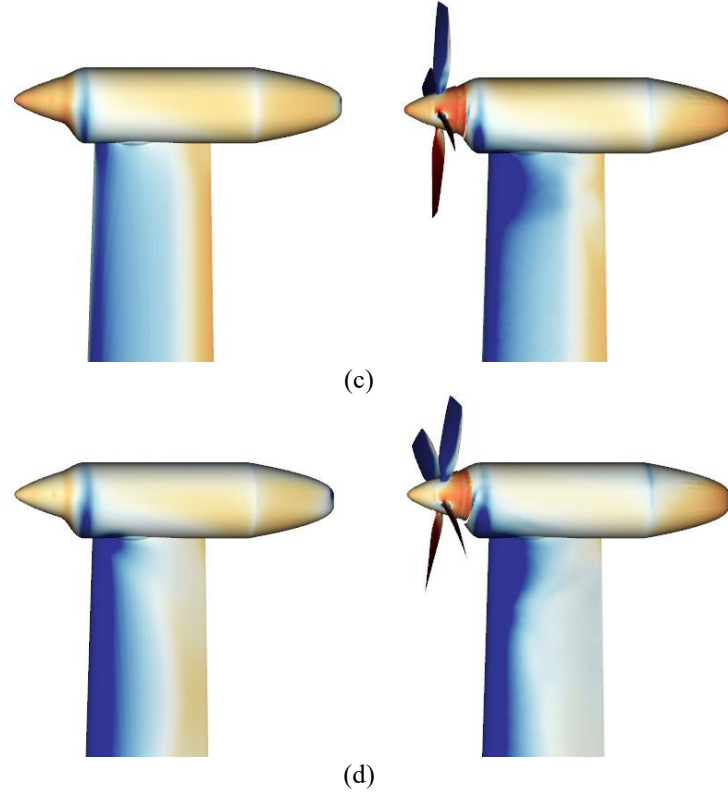


Fig. 5. Comparison of static pressure contours on the upper wing surface for the isolated wing (left) and full configuration (right) at: (a) $\alpha = 0^\circ$, (b) $\alpha = 5^\circ$, (c) $\alpha = 7^\circ$, and (d) $\alpha = 15^\circ$.

Fig. 5 provides a qualitative assessment of the pressure distribution over the upper wing surface by comparing the static pressure contours for both configurations at four wing angles of attack. These results further substantiated the effect of the propeller slipstream on wing aerodynamics as a function of α , providing critical insights into PWIs and the lift augmentation mechanisms observed in Fig. 4. As shown in Fig. 5(a), the isolated wing exhibited a relatively uniform pressure distribution along the wingspan at $\alpha = 0^\circ$, with a mild suction region near the leading edge due to the low α . By contrast, the full configuration exhibited a localized region of intensified suction immediately downstream of the propeller. This highly negative pressure zone, which coincided with the S1 section in the surface pressure distribution, resulted from the slipstream-induced increase in dynamic pressure and reflected the onset of propulsive lift augmentation even at $\alpha = 0^\circ$. This finding was consistent with prior numerical studies on DEP systems reporting strong flow acceleration in the wingtip vicinity due to the propeller wake [20,33]. The suction region broadened and deepened for both configurations at $\alpha = 5^\circ$ and 7° (Fig. 5(b) and (c), respectively). However, the full configuration exhibited substantially stronger pressure, particularly near the nacelle and outboard wing section, where the slipstream remained dominant [33]. Furthermore, the isolated wing maintained a uniform spanwise pressure distribution, whereas the full configuration revealed marked non-

uniformity, with evident pressure field asymmetry, leading to uneven local inflow angles and asymmetric aerodynamic loading on the wing surface. The pressure field reached maximum suction intensity at $\alpha = 15^\circ$, as shown in Fig. 5(d). Notably, the full configuration revealed a sharply defined low-pressure region that persisted near the nacelle and wingtip section. Despite the overall increase in suction peak at a high α , the differences in pressure contours between the isolated wing and full configuration remained spatially confined. The most pronounced discrepancies were observed near the nacelle and outboard wing section, where the propeller slipstream directly impinged upon the surface. By contrast, both configurations exhibited largely similar pressure distributions over the inboard regions farther from the propeller wake. This finding was consistent with the sectional pressure coefficient results presented in Fig. 4. Substantial differences between the isolated wing and full configuration were observed at S1 and S2 (locations within or near the slipstream core), while strong agreement between configurations was observed at S3 even at $\alpha = 15^\circ$, indicating that the aerodynamic influence of the propeller rapidly diminished with increasing distance from its rotating axis.

Table 3. Comparison of integrated lift (C_L), drag (C_D), and lift-to-drag ratio (L/D) coefficients between the isolated wing and full configuration.

α	Isolated wing			Full configuration		
	C_L	C_D	L/D	C_L	C_D	L/D
0°	0.738	0.032	23.06	0.798	0.034	23.47
5°	1.171	0.060	19.51	1.222	0.062	19.71
7°	1.318	0.086	15.32	1.332	0.098	13.59
15°	1.491	0.191	7.810	1.553	0.236	6.58

The integrated aerodynamic coefficients for both configurations at four wing angles of attack are listed in Table 3. This quantitative summary complements the aforementioned sectional pressure distributions and contour visualizations, providing insights into the effects of the slipstream on aerodynamic performance. The full configuration consistently exhibited higher lift coefficient (C_L) values compared to the isolated wing for all wing angles of attack. The largest increase in lift coefficient value was observed at $\alpha = 0^\circ$, with an 8% improvement from the isolated wing to the full configuration. Although the lift enhancement became less pronounced at higher wing angles of attack, it remained evident at $\alpha = 15^\circ$, with a 4% increase from 1.491 (isolated wing) to 1.553 (full configuration). This powered-lift effect was attributed to the propeller slipstream elevating local dynamic pressure on the upper wing surface, leading to increased suction, as confirmed by the surface pressure distributions (Fig.

4) and contours (Fig. 5). The results indicated that the simulation captured the integrated manifestation of localized lift augmentation mechanisms. However, the lift gain was accompanied by a drag penalty. The full configuration exhibited a consistently higher drag coefficient (C_D) value, with the difference reaching up to 23% at $\alpha = 15^\circ$. This drag penalty was primarily attributed to increased skin friction due to higher slipstream velocities and additional drag induced by the increased circulation associated with higher lift. The latter became particularly influential at high wing angles of attack, where wake expansion and spanwise pressure imbalance were intensified by the propeller slipstream. Consequently, the overall aerodynamic efficiency, represented by the L/D ratio, was reduced for the full configuration compared to the isolated wing. This finding highlights a fundamental design trade-off in DEP systems: although propeller-induced flow can locally enhance lift, the overall aerodynamic efficiency may be degraded due to additional drag components.

3.3 PWI Effects on Downstream Flow Field

To further understand the mechanisms underlying the previously discussed increase in suction peak and lift augmentation effect, the wake development behind the propeller was examined using iso-surfaces of vorticity magnitude. This analysis enabled a physical interpretation of the influence of wing installation on the 3D wake structure, which in turn affects the downstream velocity field near the nacelle and wingtip section. Figs. 6 and 7 provide a comparative assessment of wake development for the isolated propeller and full configuration, respectively. Fig. 6 presents the wake structure of the isolated propeller at $\alpha = 0^\circ$, which served as a baseline case for evaluating the impact of wing installation on propeller wake evolution, which is presented in Fig. 7. The influence of downstream wing–wake interactions on the coherence, deformation, and breakdown of the slipstream was analyzed by comparing the isolated propeller case in Fig. 6 with the full-configuration case in Fig. 7 at varying wing angles of attack. As shown in Fig. 6, the propeller generated a highly coherent and symmetric helical vortex system due to the absence of downstream obstructions, characterized by strong tip vortices that maintained their structure well downstream. A weaker hub vortex was visible along the rotating axis. This organized and stable structure reflected an idealized propulsion environment with minimal energy dissipation, which is often associated with high propulsive efficiency.

By contrast, Fig. 7 presents the wake structures for the full configuration, in which the propeller operated in conjunction with the wing. As shown in Fig. 7(a), even at $\alpha = 0^\circ$, the presence of the wing caused vortex splitting near the wingtip section, leading to evident wake structure asymmetry. The inboard portion of the helical vortex

system, corresponding to the section near the wingtip and nacelle junction, impinged directly upon the upper wing surface. Interaction-induced vortex splitting initiated the rapid loss of coherence in tip vortex filaments, disrupting the organized wake structure and promoting early onset of deformation and breakdown. The wake deformation became more pronounced with increasing α , as shown in Figs. 7(b)–7(d). At $\alpha = 5^\circ$ and 7° , interaction between the propeller tip vortex and wing surface led to destructive vortex interference, reducing coherence and promoting earlier vortex breakdown. At $\alpha = 15^\circ$, the wing began to generate a vortical structure from its upper surface due to the high angle of attack, and interaction between vortical systems was observed (Fig. 7(d)). Subsequently, the wake almost entirely lost its organized structure, exhibiting rapid dissipation, lateral distortion, and irregular breakdown patterns. The accelerated vortex breakdown and transition to a turbulent wake state at high wing angles of attack contributed to increased wake interaction, drag penalty, and L/D ratio degradation, as previously discussed (see Table 3). These findings aligned with previous numerical observations reported by Qiao et al. [33] and Zhou et al. [20], who described similar vortex interaction mechanisms in DEP systems.

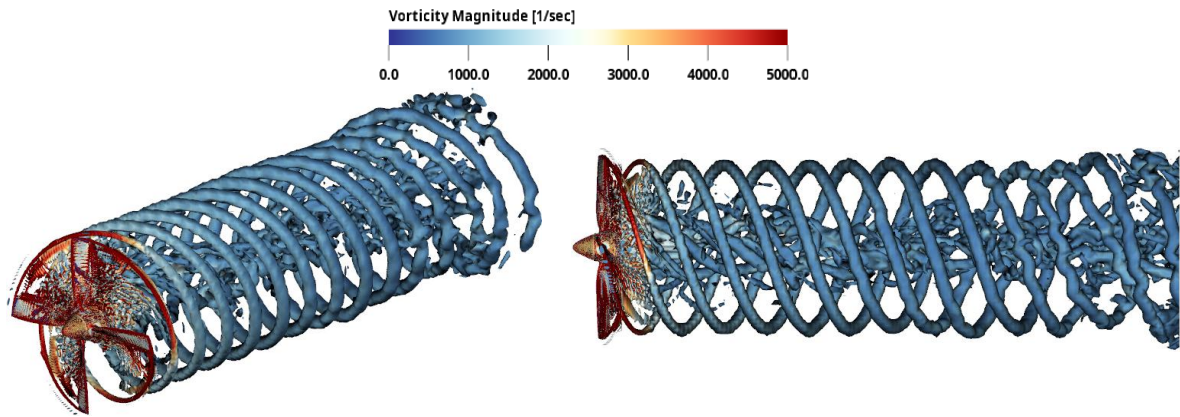
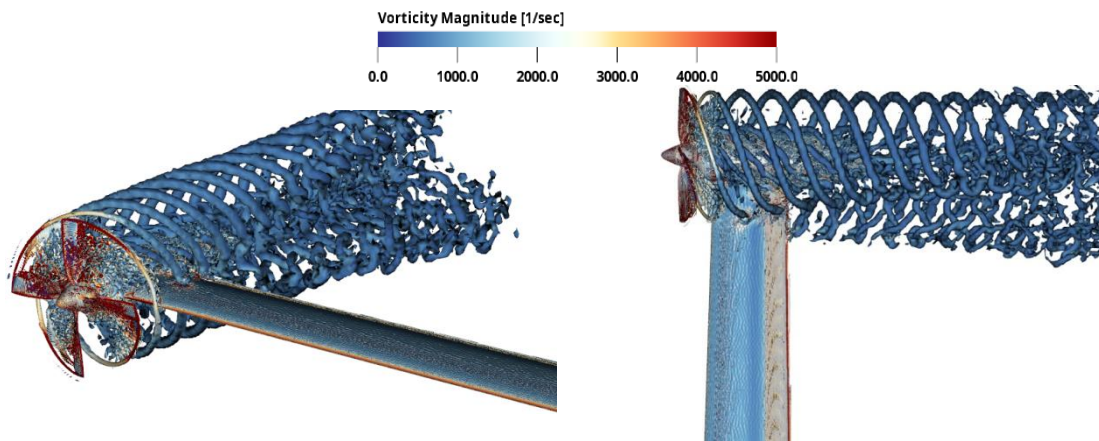


Fig. 6. Wake structure of the isolated propeller at $\alpha = 0^\circ$, visualized using vorticity iso-surfaces: side view (left) and top view (right).



(a)

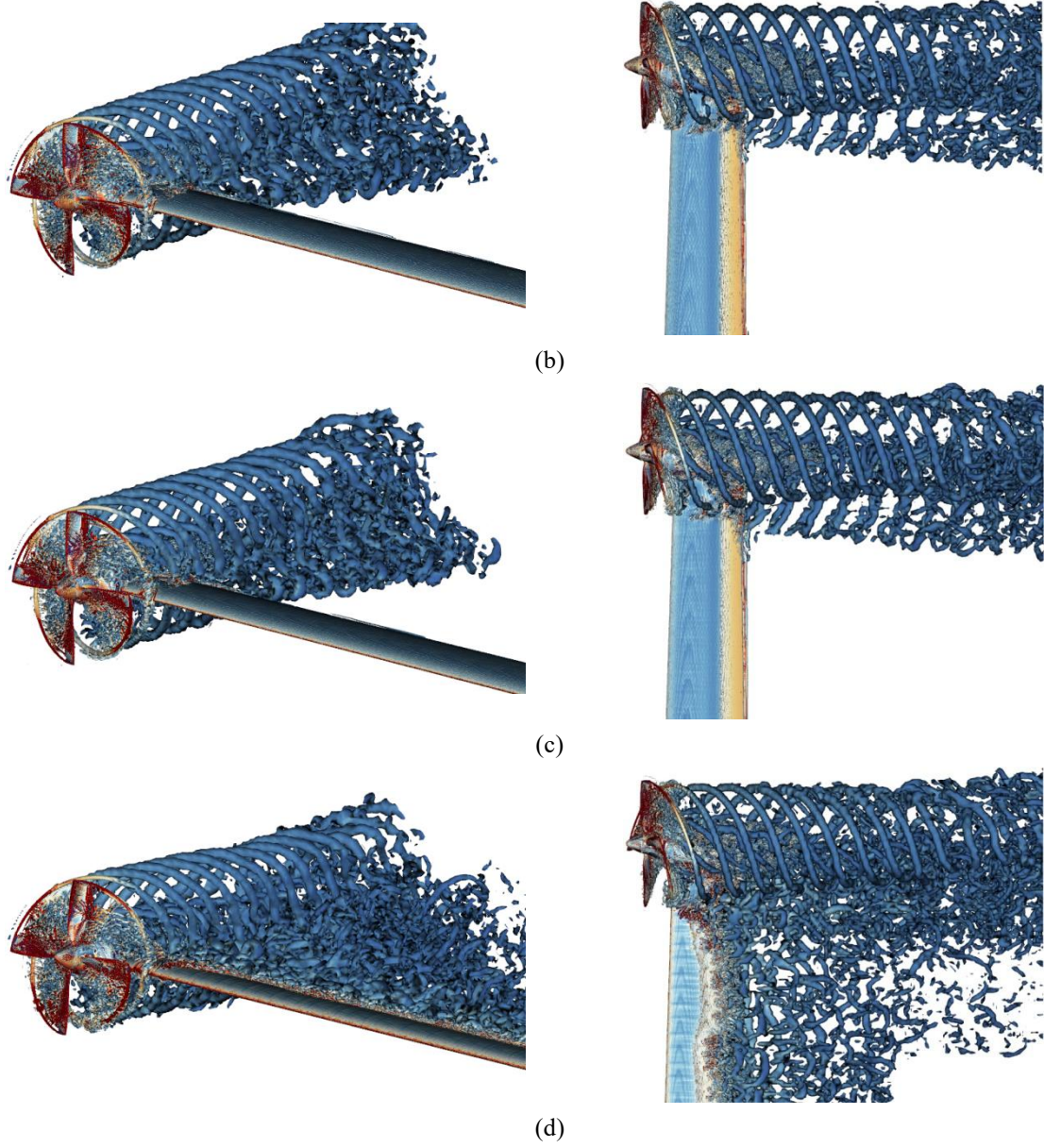


Fig. 7. Wake structures of the full configuration visualized using vorticity iso-surfaces at: (a) $\alpha = 0^\circ$, (b) $\alpha = 5^\circ$, (c) $\alpha = 7^\circ$, and (d) $\alpha = 15^\circ$: side view (left) and top view (right).

To complete the numerical framework validation, a quantitative assessment of the propeller wake was conducted by comparing the time-averaged axial velocity profiles at four downstream locations (P1–P4), as defined in Fig. 3. Fig. 8 presents a comparison of the LBM predictions, experimental data, and reference numerical results obtained using the SU2 solver [20]. The reference SU2 results were obtained using an enhanced delayed detached-eddy simulation (EDDES) model based on the Spalart–Allmaras (SA) turbulence model, referred to as EDDES-SA, whereas the present simulations employ an LBM–VLES framework. The main modeling difference is associated with the treatment of effective viscosity. In the LBM–VLES approach, the effective kinematic viscosity is governed by the local effective relaxation time defined in Eq. (4), and is expressed as

$$\nu_{eff}^{LBM} = c_s^2 \left(\tau_{eff} - \frac{1}{2} \right) \Delta t \quad (7)$$

where c_s and Δt denote the lattice speed of sound and time step, respectively. In contrast, the SA-based EDES formulation solves the Navier–Stokes equations with an effective viscosity expressed as

$$\nu_{eff}^{SA} = \nu + \nu_t \quad (8)$$

where ν is the molecular kinematic viscosity and ν_t is the modeled eddy viscosity obtained from the SA turbulence model. In EDES-SA, the modeled dissipation is further affected by the hybrid RANS/LES length scale and shielding functions, whereas in LBM–VLES the dissipation level is controlled through the local relaxation process. Therefore, wake diffusion in the two approaches is governed by different mechanisms: relaxation time based dissipation in LBM–VLES and eddy viscosity/length scale based dissipation in EDES-SA. These differences can influence wake contraction and peak axial velocity in the downstream wake profiles.

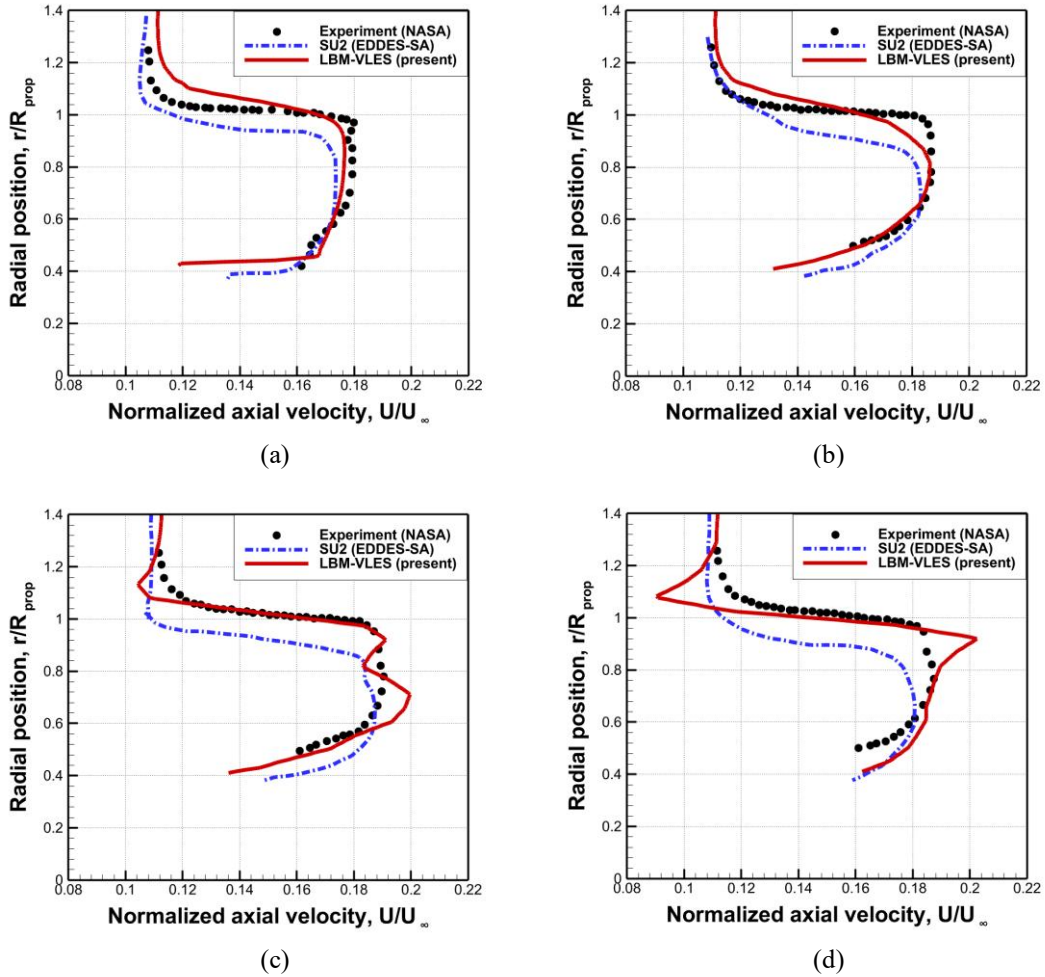


Fig. 8. Comparison of time-averaged axial velocity profiles at four downstream locations: (a) P1, (b) P2, (c) P3, and (d) P4.

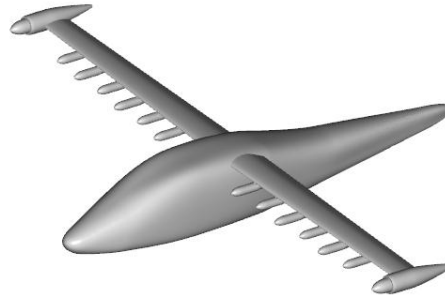
The present LBM–VLES simulation generally captured the shape of the wake velocity profiles at all locations. The predictions exhibited reasonable agreement with experimental data in terms of velocity deficit and wake width in the near-wake region, corresponding to P1 and P2 (Fig. 8(a) and (b), respectively). The characteristic features, including the central hub–induced velocity dip and outer slipstream contours, were well reproduced in the present simulation. However, several quantitative discrepancies were observed between the LBM–VLES predictions and experimental data. Notably, the peak axial velocities within the slipstream were underpredicted at all locations, which was likely due to limitations in resolving the high-gradient shear layers surrounding the propeller slipstream. Additionally, the velocity profiles exhibited a slight inward radial shift relative to experimental data, suggesting a more contracted wake in the simulation. This over-contraction may be attributed to differences in turbulence modeling or boundary layer development along the nacelle and wing surfaces. The discrepancies became more pronounced in the far-wake region, particularly at P3 and P4 (Fig. 8(c) and (d), respectively), where the simulation exhibited increased numerical dissipation, leading to the loss of fine-scale structures in the shear layer. At P3 and P4, the differences between the LBM–VLES and SU2 predictions can be attributed to the combined effects of turbulence closure, mesh topology, wake refinement, and numerical dissipation. For reference, the SU2 EDDES-SA simulations reported in Ref. [20] used unstructured tetrahedral meshes containing approximately 35.3–64.9 million cells with wall-resolved boundary-layer treatment. In contrast, the present LBM–VLES validation used a Cartesian voxel-based lattice with approximately 150 million cells, local wake refinement, and generalized wall-function treatment. The SU2 simulations used a different mesh topology and wall-treatment strategy from the present LBM–VLES calculation. In addition, their computational runtime was not reported; therefore, the comparison is limited to wake-profile characteristics relative to the experiment rather than solver-to-solver efficiency. The local behavior observed at P3 can be interpreted as a characteristic of the intermediate wake region, where the coherent helical tip-vortex system has partially diffused while the slipstream shear layer still retains relatively strong velocity gradients. In this region, the time-averaged axial velocity profile becomes sensitive to small differences in wake contraction, turbulent diffusion, and shear-layer broadening. Therefore, the local deviation observed in Fig. 8(c) is attributed to the combined effects of wake contraction and modeled turbulent diffusion during downstream wake development. This interpretation is consistent with the progressive underprediction of peak axial velocity and smoothing of the high-gradient shear-layer region from P2 to P4. Despite these differences, the LBM–VLES simulation captured the essential wake dynamics, including the spatial distribution of the velocity deficit, radial symmetry, and core profile features. The validation results demonstrated

that the proposed approach provided sufficient fidelity to resolve the primary aerodynamic characteristics of the propeller wake, thereby supporting its use in predicting PWI effects in aircraft employing DEP systems.

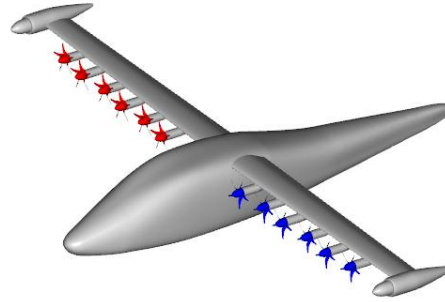
4. Numerical Setup

4.1 Maxwell X-57 Aircraft Model

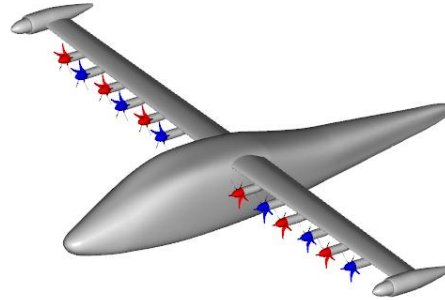
NASA's X-57 Maxwell aircraft was adopted as the simulation model in this study [43]. The X-57 Maxwell aircraft was developed as part of NASA's Scalable Convergent Electric Propulsion Technology Operations Research (SCEPTOR) project, aiming to explore high-efficiency, low-emission flight enabled by electric propulsion. The full-scale X-57 Maxwell aircraft includes 14 propellers, including 2 large cruise propellers located at the wingtips and 12 smaller high-lift propellers mounted along the leading edge of the wing. The 12 high-lift propellers on the leading edge of the wing, which are primarily responsible for enhancing low-speed lift through active flow control, were the primary focus of this study. The computational model omitted wingtip-mounted cruise propellers and fuselage details to explore the powered-lift effect and PWI mechanisms driven by the DEP system. Fig. 9 presents the simplified configuration and outlines the three operational scenarios investigated herein: (1) the baseline power-off configuration, in which the wing was equipped with nacelles but all propellers were excluded (Fig. 9(a)); (2) the power-on configuration with co-rotating propellers, where all six high-lift propellers rotated in the same direction on each wing, with the six propellers on the left wing rotating counterclockwise when viewed from the front, while those on the right wing rotated clockwise, generating a uniform slipstream over-the-wing surface (Fig. 9(b)); and (3) the power-on configuration with counter-rotating propellers, in which the rotational direction alternated between adjacent propellers (Fig. 9(c)). The latter configuration was analyzed to investigate the impact of swirl cancellation between neighboring propellers on lift augmentation and aerodynamic load distribution along the spanwise direction. Comparison of the three cases enabled a comprehensive understanding of DEP-induced aerodynamic phenomena. The first comparison between the power-off configuration and power-on configuration with co-rotating propellers aimed to quantify the powered-lift effect, which is a primary aerodynamic benefit of DEP systems during critical flight phases such as takeoff and landing. The second comparison between power-on configurations with co- and counter-rotating propellers investigated the impact of PWIs and rotational direction on aerodynamic efficiency and spanwise load distribution.



(a)



(b)



(c)

Fig. 9. Computational configuration of NASA’s X-57 Maxwell high-lift system: (a) power-off configuration, (b) power-on configuration with co-rotating propellers, and (c) power-on configuration with counter-rotating propellers. Red propellers rotating counterclockwise, blue propellers rotating clockwise.

4.2 Computational Domain for LBM–VLES Simulation

Each high-lift propeller had a diameter (D) of 0.576 m and consisted of five blades, driven by 10.5 kW electric motors, which was consistent with NASA’s technical specifications for the Mod IV configuration. The wingspan (S) was 9.642 m, with a platform area of 6.196 m², and the propellers were uniformly spaced at intervals of 0.86 m, corresponding to 1.48 D along the wingspan, which ensured distributed slipstream coverage. The computational domain, depicted in Fig. 10, consisted of a cubic volume with side lengths equal to 10 S to ensure minimal boundary interference effects. A Cartesian grid covering the entire simulation domain was employed in the LBM–

VLES framework, with lattice cells to ensure sufficient resolution of near-field and wake regions. The X-57 simulations adopted the same Cartesian voxel based mesh generation strategy described for the WIPP validation case, with the aircraft geometry embedded within the Cartesian lattice. In this case, fourteen nested variable-resolution regions were used to control the local grid spacing, with the finest refinement levels assigned near the propeller blades, nacelles, adjacent wing surfaces, and downstream slipstream paths, as shown in Fig. 11. This refinement layout was designed to resolve blade-tip vortices, slipstream shear layers, and slipstream–wing interaction over the wing, while maintaining coarser resolution away from the aircraft to reduce computational cost. Each of the twelve high-lift propellers was enclosed within an individual cylindrical rotating region, and the propeller motion was modeled using the sliding mesh treatment.

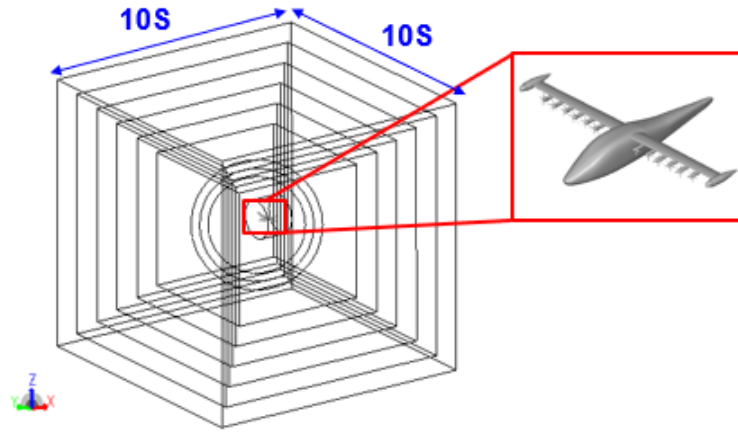


Fig. 10. Overall view of the computational domain for NASA's X-57 Maxwell aircraft employing a DEP system.

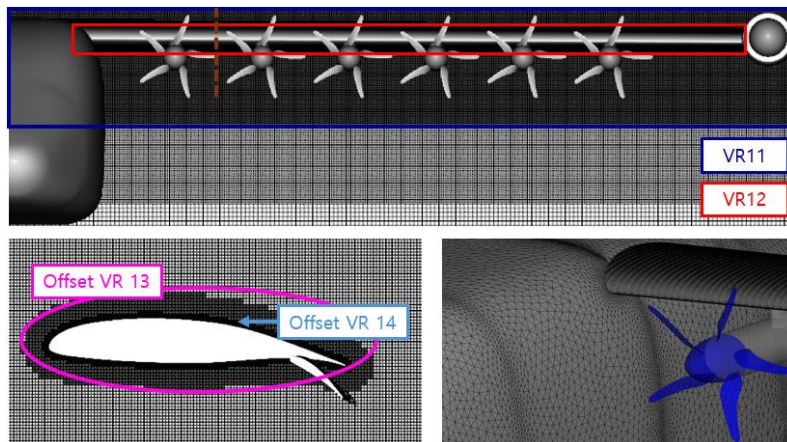


Fig. 11. Local Cartesian voxel mesh and variable-resolution distributions around the wing, nacelle, and high-lift propellers of the X-57 Maxwell configuration.

Table 4. Comparison of predicted lift coefficient and computational cost for different grid resolutions at wing angle of attack of 2° using 256 computational cores. The asterisk (*) denotes the selected grid resolution.

	Case 1 (Coarse)	Case 2	Case 3* (Medium)	Case 4	Case 5 (Fine)
Tip chord resolution [%]	1.00	0.75	0.50	0.30	0.25
Total cells	120 M	180 M	240 M	300 M	360 M
Computational time [h]	60	112	164	231	380
LBM-VLES prediction (C_L) [-]	2.62	2.67	2.72	2.74	2.75
NASA experiment (C_L) [-]	2.85	2.85	2.85	2.85	2.85
Error [%]	7.96	6.21	4.56	4.04	3.85

Prior to presenting the aerodynamic results, grid sensitivity study was conducted to assess the reliability of the numerical framework employed for the X-57 Maxwell simulations. The simulation was performed for the full configuration at wing angle of attack of 2° with the power-on co-rotating propeller setup, defined in Fig. 9(b), using 256 CPU cores of a high-performance computing cluster equipped with Intel® Xeon® Gold 6230R processors operating at 2.1 GHz. To examine the influence of spatial resolution, the total number of cells was varied by adjusting the minimum grid size at the propeller tip chord. Five grid resolutions were considered: 1.0%, 0.75%, 0.5%, 0.3%, and 0.25% of the tip chord, corresponding to Cases 1 through 5, respectively. The lift coefficient (C_L) was adopted as the primary metric for evaluating grid sensitivity and was compared with the NASA reference value of 2.85 at wing angle of attack of 2° . The total number of cells, computational times, and predicted lift coefficients for each case are summarized in Table 4. The predicted C_L increased with increasing grid resolution, while the difference between Cases 3 and 4 became relatively small. Cases 3 and 4 yielded errors of 4.56% and 4.04%, respectively, relative to the NASA reference [44]. Although Cases 4 and 5 provided slightly lower errors, the improvement in prediction accuracy was marginal compared with the substantial increase in computational cost. In particular, the computational times of Cases 4 and 5 were 41% and 132% greater than that of Case 3, respectively. Therefore, Case 3, corresponding to a minimum cell size of 0.5% of the propeller tip chord and approximately 240 million cells, was selected for all simulations as an appropriate compromise between accuracy and computational efficiency. Moreover, a near-surface resolution of 3 mm was maintained, corresponding to 0.5% of the propeller tip chord to resolve near-wall flow phenomena and accurately capture vortical structures.

4.3 Simulation Conditions

For the boundary conditions, a velocity inlet was imposed on the upstream boundary, while all other far-field surfaces were treated as pressure outlets. No-slip boundary conditions were enforced on aircraft surfaces to account for viscous effects. The rotational motion of the 12 propellers was simulated using the sliding mesh technique within defined cylindrical rotating zones. The freestream conditions listed in Table 5 were matched to those employed in NASA's computational campaigns for the Mod IV configuration, representing a takeoff scenario [43]. The freestream Mach number was set to 0.092, corresponding to a flight altitude of 2500 ft. The chord-based Reynolds number was 610,000, and the tip Mach number of the propellers was 0.401 at a rotational speed of 4548 rpm. LBM-VLES simulations were conducted at four wing angles of attack to examine aerodynamic behavior across a range of wing angles of attack. A flap deflection of 30° was imposed on the trailing edge of the wing to emulate a high-lift configuration typical of takeoff conditions. Each unsteady simulation was performed for 20 propeller revolutions to allow the development of unsteady flow structures. Fig. 12 shows the time histories of the thrust and torque coefficients of a representative high-lift propeller for the power-on co-rotating X-57 Maxwell case at $\alpha = 2^\circ$ over the final 5 propeller revolutions. Both coefficients remain bounded around nearly constant mean values with periodic fluctuations associated with blade passage and inter-propeller wake interaction. It indicates that the representative propeller loading reached a statistically stationary state. Therefore, the final 5 revolutions were used to compute the time-averaged flow fields and integrated aerodynamic coefficients.

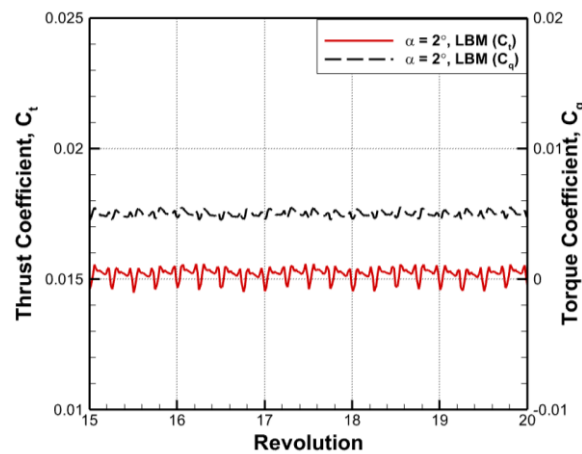


Fig. 12. Time histories of thrust and torque coefficients of a representative high-lift propeller loading for the power-on co-rotating X-57 Maxwell case at $\alpha = 2^\circ$ over the final 5 propeller revolutions.

Table 5. Operating conditions for X-57 Maxwell aircraft simulations.

Parameter	Value
Freestream Mach number [M_∞]	0.092
Wing angle of attack [α]	2°, 8°, 12°, 16°
Pressure [Pa]	92,499.6
Reynolds number (Re)	610,000
Mach number (M_{tip})	0.401
Rotating speed (rpm)	4548

Table 6. Comparison of predicted lift coefficient and computational cost for different temporal resolutions at $\alpha = 2^\circ$ using 256 computational cores and the selected grid. The asterisk (*) denotes the selected temporal resolution.

	Case 1	Case 2*	Case 3
Angular resolution [deg/step]	5	3	1
Computational time [h]	157	164	180
LBM-VLES prediction (C_L) [-]	2.680	2.720	2.725
NASA experiment (C_L) [-]	2.85	2.85	2.85
Error [%]	6.12	4.56	4.30

Using the selected grid system of Case 3 in Table 4, a temporal sensitivity study was performed to examine the influence of time-step resolution on the predicted aerodynamic performance. Three temporal resolutions were considered by varying the angular displacement of the rotating propeller per time-step to 5°, 3°, and 1°, corresponding to Cases 1, 2, and 3, respectively. The predicted lift coefficients and corresponding computational times are summarized in Table 6. Time-step of 3° and 1° produced nearly identical results, with $C_L=2.720$ and 2.725, respectively. Refining the temporal resolution from 5° to 3° per time-step noticeably improved the prediction accuracy, reducing the error relative to the NASA reference from 6.12% to 4.56%. However, further refinement from 3° to 1° per time-step led to only a marginal improvement of 0.26 percentage points while increasing the computational cost. Therefore, an angular resolution of 3° per time-step, corresponding to approximately 120 time-steps per propeller revolution, was adopted for all subsequent simulations [25,26]. Overall, the sensitivity studies indicate that the selected numerical setup, consisting of a 240-million-cell mesh and an angular resolution of 3° per time-step, provides sufficient accuracy to resolve the dominant aerodynamic characteristics of PWI while maintaining reasonable computational cost. The simulations were executed on 256 CPU cores of a high-performance computing cluster, which was equipped with Intel® Xeon® Gold 6230R processors operating at 2.1 GHz. The computational time required for each case was approximately 164 hours,

reflecting the considerable computational demand of resolving the unsteady propeller–wing interactions and wake dynamics of the full-configuration DEP aircraft.

5. Numerical Results

5.1 PWI Effects on Wing Aerodynamics in DEP Systems

The aerodynamic influence of distributed propeller slipstreams is investigated by comparing the power-off configuration and the power-on configuration with co-rotating propellers introduced in Fig. 9. The integrated aerodynamic coefficients are first presented in Table 7 to provide a quantitative overview of the PWI effects on lift, drag, and aerodynamic efficiency for the two configurations. Detailed flow-field analyses of surface pressure, skin friction, and near-wall velocity distributions are then used to elucidate the physical mechanisms responsible for these aerodynamic trends. As shown in Table 7, the lift coefficient increased with increasing α up to $\alpha = 12^\circ$ for both configurations, after which a notable reduction in lift was observed at $\alpha = 16^\circ$, indicating the onset of flow separation and partial stall. However, the magnitude of post-stall degradation differed markedly between configurations. The power-on configuration with co-rotating propellers consistently yielded a significantly higher lift coefficient than the power-off configuration by approximately 40%–45% for all wing angles of attack, demonstrating propeller slipstream-induced lift augmentation. This lift enhancement is attributed to improved boundary layer attachment and intensified suction pressure over the upper wing surface, which will be discussed from the surface pressure and skin friction analyses presented in Figs. 13–15. Nevertheless, the power-on configuration with co-rotating propellers incurred a substantial drag penalty despite the lift gain. The drag coefficient for the power-on configuration with co-rotating propellers was increased by 40%–60% compared to the power-off configuration, with the gap widening at higher wing angles of attack. The increased drag was primarily attributed to an increase in skin friction due to accelerated local flow velocities under the slipstream and augmented lift. Consequently, the L/D ratio deteriorated under power-on conditions, particularly at higher wing angles of attack. The power-on configuration with co-rotating propellers exhibited a reduction in aerodynamic efficiency by approximately 10%–20% relative to the power-off configuration, highlighting a trade-off between lift enhancement and overall efficiency in DEP systems.

Table 7. Comparison of lift (C_L), drag (C_D), and lift-to-drag (L/D) coefficients between power-off configuration and power-on configuration with co-rotating propellers.

α	Power-off			Power-on with co-rotating propellers		
	C_L	C_D	L/D	C_L	C_D	L/D
2°	1.911	0.172	11.11	2.776	0.325	8.53
8°	2.521	0.224	11.25	3.628	0.515	7.04
12°	2.614	0.285	9.17	4.018	0.734	5.46
16°	2.381	0.485	4.91	3.852	0.946	4.07

To elucidate the lift augmentation and drag increase summarized in Table 7, Fig. 13 presents the static pressure coefficient contours on the upper wing surface for both configurations at four wing angles of attack. These contours highlight the influence of distributed propeller slipstreams on local suction enhancement and spanwise pressure variations induced by co-rotating high-lift propellers. For the power-off configuration, the pressure field exhibited spanwise-uniform suction distribution, which intensified with increasing α due to the conventional lifting behavior of the wing. As expected, the magnitude of the suction peak near the leading edge increased at higher wing angles of attack, while the overall spanwise pressure variation remained relatively smooth. By contrast, the power-on configuration with co-rotating propellers exhibited a substantially altered pressure field. At all wing angles of attack, the surface pressure was characterized by discrete low-pressure regions directly downstream of each propeller. These localized suction regions were attributed to freestream acceleration by the propeller slipstream, resulting in increased dynamic pressure. The slipstream footprints were spatially confined and did not merge along the wingspan, even at high wing angles of attack. Moreover, evident asymmetry was observed in each suction footprint, with consistently more pronounced suction on the right side (starboard side) of each propeller disk when viewed from upstream. The suction asymmetry was attributed to clockwise propeller rotation: the descending blade (on the right) induced a higher local effective α , enhancing suction, while the ascending blade (on the left) reduced the effective α , as depicted in Fig. 14. This asymmetric loading was consistent with previous observations in DEP-induced flow fields and highlighted the 3D nature of PWIs. This behavior can be physically interpreted by considering the modification of the local effective angle of attack induced by the propeller slipstream. The accelerated axial velocity and associated swirl increase the local dynamic pressure and alter the inflow direction, leading to enhanced suction and delayed separation, which directly contributes to the lift augmentation observed in Table 7. At $\alpha = 2^\circ$, the slipstream footprints were visible but relatively shallow, and the pressure distribution outside these regions remained comparable to the baseline case.

At $\alpha = 8^\circ$, the suction intensity substantially increased, with stronger spanwise gradients developing along the wing. At $\alpha = 12^\circ$, the interactions became more prominent, and the pressure distribution exhibited increasing non-uniformity. Finally, at $\alpha = 16^\circ$, although suction remained strong near each slipstream, indications of early separation and pressure plateauing were evident near the trailing edge, particularly inboard. Overall, the results confirmed that the distributed high-lift propellers introduce considerable non-uniformities in the spanwise pressure field, locally enhancing lift through increased suction while inducing asymmetric and spatially varying loading. These flow characteristics, which have important implications for aerodynamic performance and structural design, served as the basis for further analysis of flow separation and integrated aerodynamic forces.

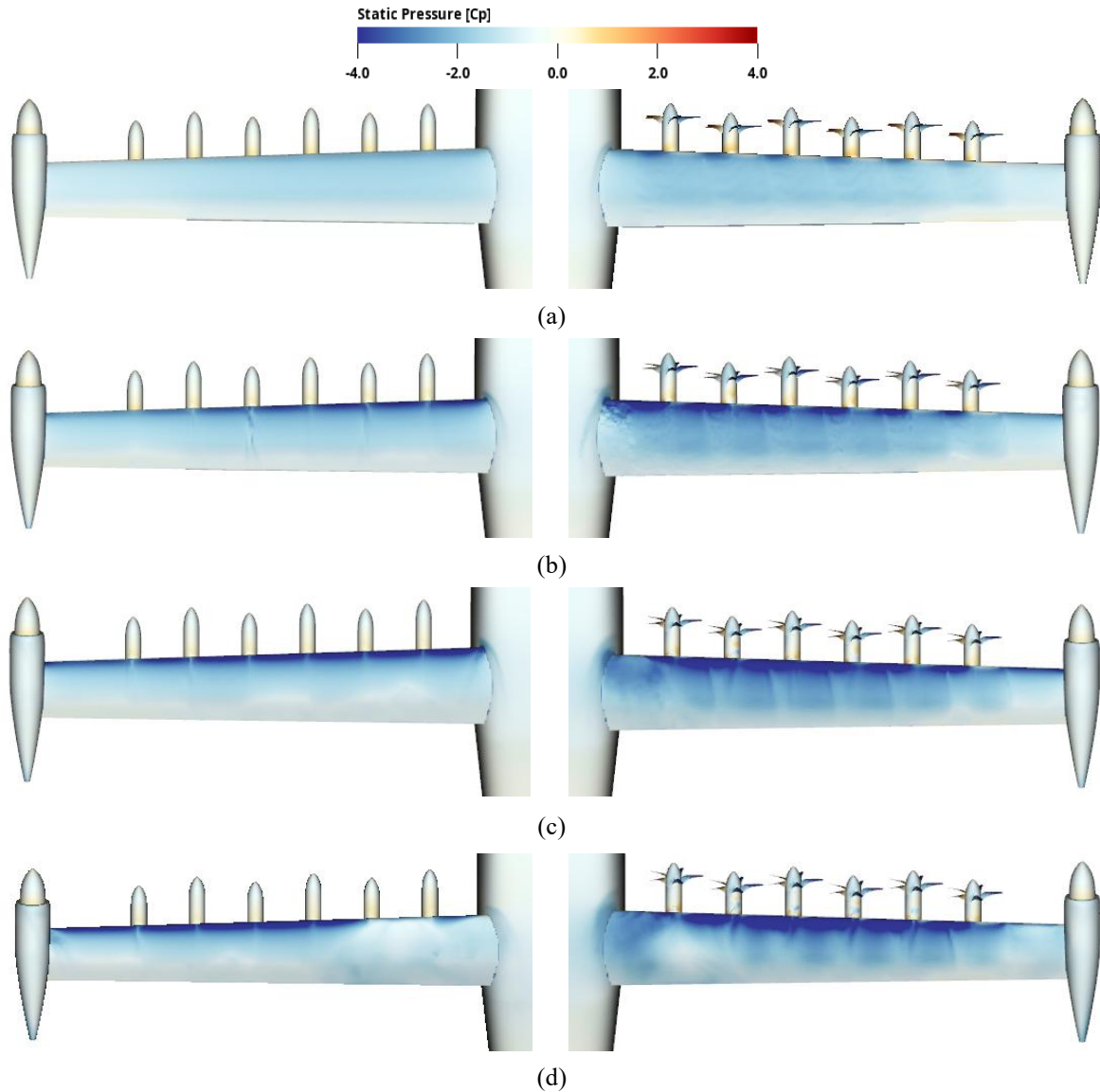


Fig. 13. Comparison of static pressure coefficient contours on the upper wing surface between the power-off configuration (left) and power-on configuration with co-rotating propellers (right) at: (a) $\alpha = 2^\circ$, (b) $\alpha = 8^\circ$, (c) $\alpha = 12^\circ$, and (d) $\alpha = 16^\circ$.

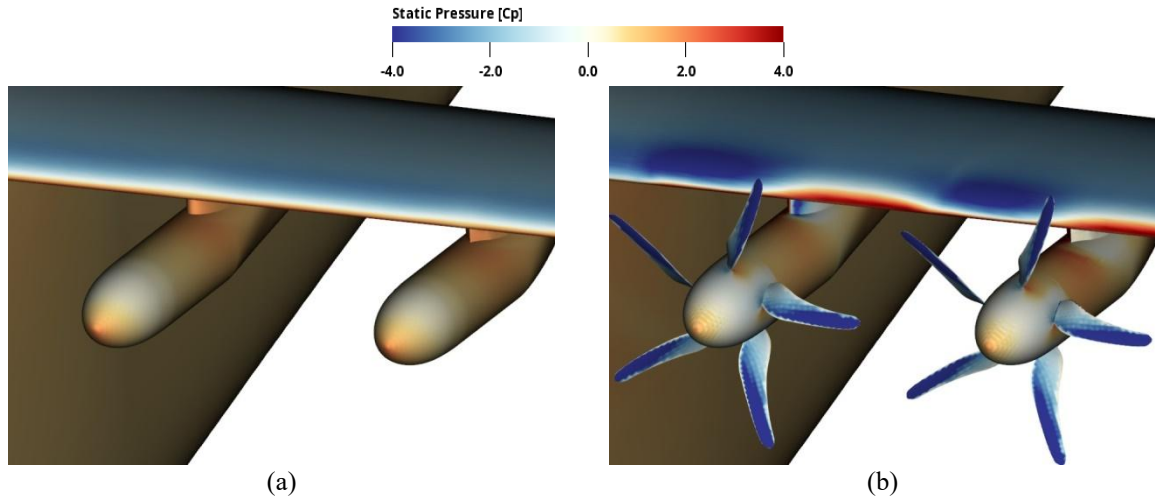


Fig. 14. Qualitative visualization of the pressure field modification due to propeller operation: (a) power-off configuration, and (b) power-on configuration with co-rotating propellers.

The drag increase and stall delay observed in Table 7 can be further attributed to changes in flow separation characteristics. Fig. 15 presents the skin friction coefficient contours over the upper wing surface for the power-off configuration and power-on configuration with co-rotating propellers at four wing angles of attack. High skin friction values indicated attached flow, while near-zero values corresponded to regions of separation. This analysis offers insights into boundary layer detachment and flow separation characteristics, which are directly linked to the drag penalty and stall behavior. For the power-off configuration, evident progression of trailing-edge separation was observed with increasing α . The separated region, characterized by low skin friction values, originated at the trailing edge at lower wing angles of attack and rapidly grew upstream and spanwise. The flow remained fully attached at $\alpha = 2^\circ$, as evidenced by uniformly high skin friction values. However, at $\alpha = 8^\circ$ and 12° , regions of near-zero skin friction emerged near the trailing edge, indicating flow separation onset. At $\alpha = 16^\circ$, a large, separated flow region extended from the mid-chord to the trailing edge, particularly over the inboard wing section. This progression reflected trailing-edge stall behavior and was consistent with prior findings for the X-57 Maxwell aircraft in a power-off configuration. The massive separation originated from the wing-fuselage junction and progressively extended outward with increasing α , indicating partial wing stall onset, which resulted in the substantial degradation of lift generation and control effectiveness. By contrast, the influence of the propeller slipstream in maintaining flow attachment was evident for the power-on configuration with co-rotating propellers, demonstrating the influence of the DEP system on flow control. Even at $\alpha = 16^\circ$, the flow remained largely attached in distinct regions directly downstream of each propeller, as evidenced by high skin friction values, indicating strong suppression of flow separation. This phenomenon was attributed to the continuous injection of

high-momentum flow via propeller slipstream, enabling the flow to overcome the strong adverse pressure gradient at high wing angles of attack. Although the stall-delaying influence of the DEP system was spatially non-uniform, demonstrating the greatest effects in regions aligned with slipstreams and less pronounced effects between adjacent propellers, controlling boundary layer detachment was the primary mechanism responsible for the lift augmentation effect, particularly at higher wing angles of attack.

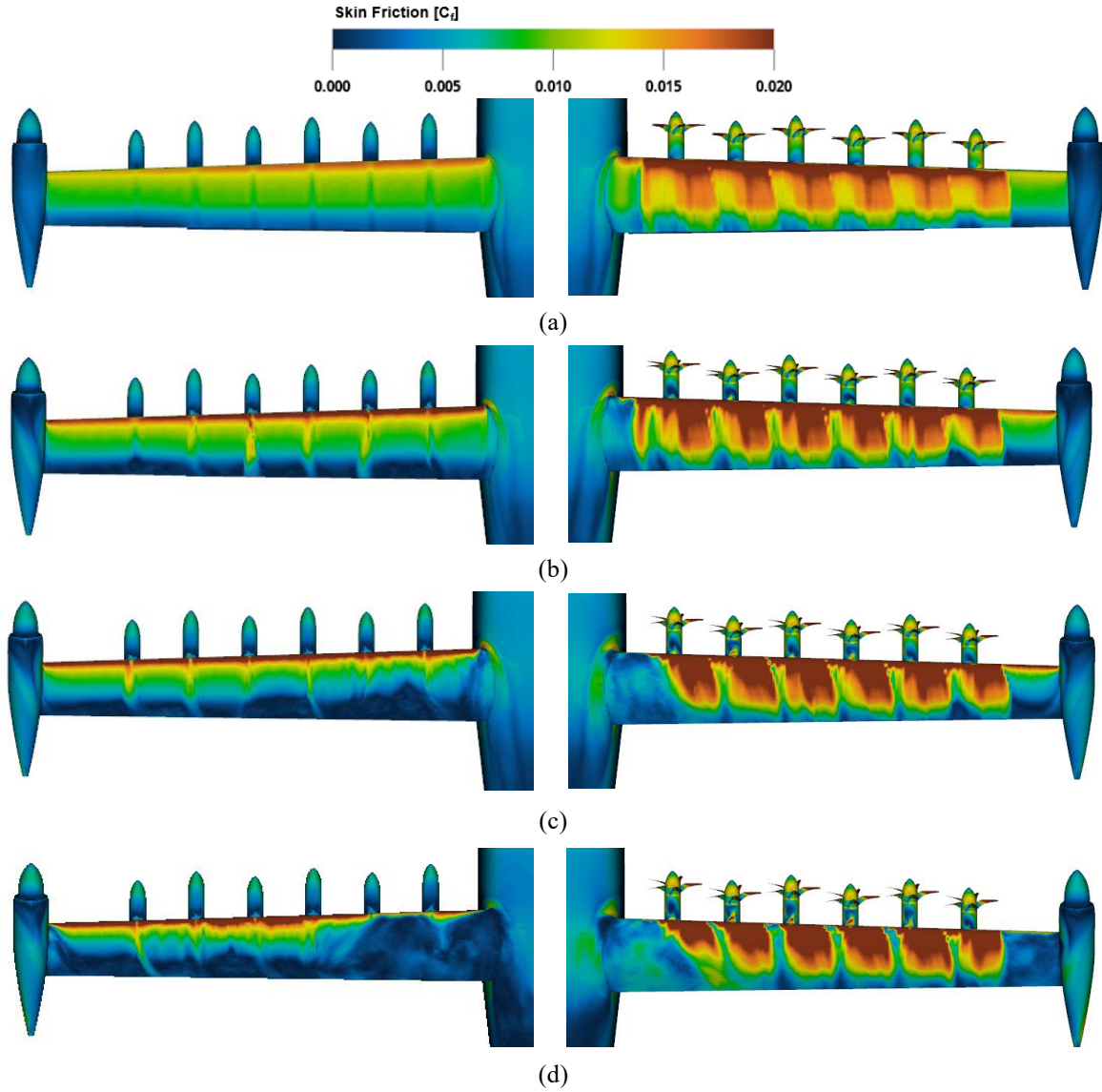


Fig. 15. Comparison of surface skin friction coefficient contours on the upper wing surface between the power-off configuration (left) and power-on configuration with co-rotating propellers (right) at: (a) $\alpha = 2^\circ$, (b) $\alpha = 8^\circ$, (c) $\alpha = 12^\circ$, and (d) $\alpha = 16^\circ$.

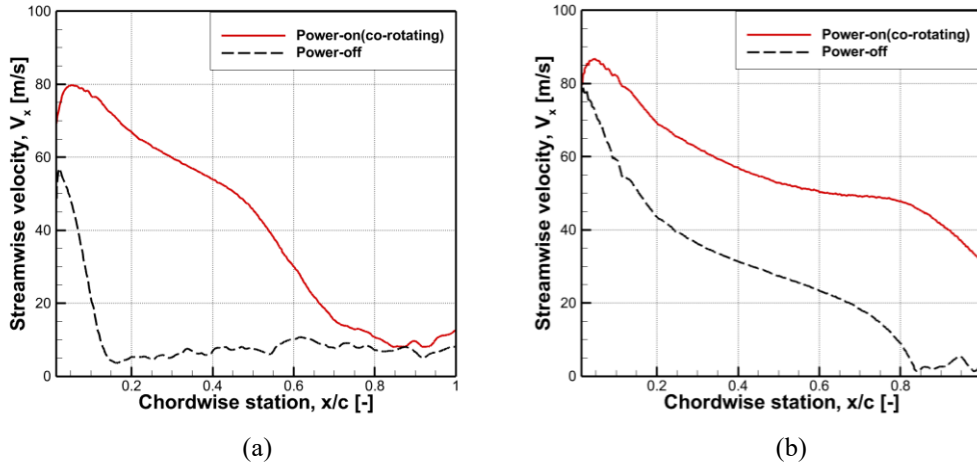


Fig. 16. Comparison of streamwise velocity along the chordwise direction over the upper wing surface at $\alpha = 16^\circ$: (a) $y/b = 0.2$ and (b) $y/b = 0.5$.

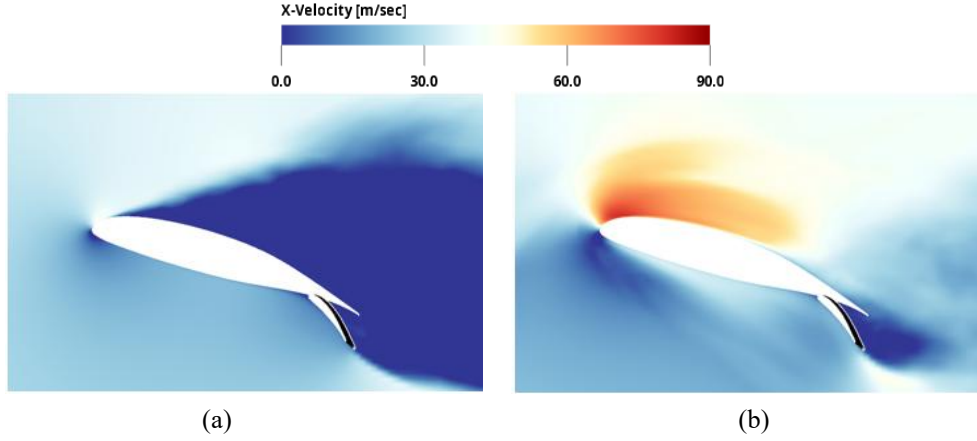


Fig. 17. Comparison of sectional velocity contours at $y/b = 0.2$ spanwise location and $\alpha = 16^\circ$: (a) power-off configuration and (b) power-on configuration with co-rotating propellers.

To further explain the DEP-induced stall delay mechanism, the streamwise velocity profiles over the upper wing surface were examined at $\alpha = 16^\circ$. Fig. 16 illustrates the chordwise distribution of x-velocity at inboard ($y/b = 0.2$) and mid-span ($y/b = 0.5$) sections, two representative spanwise locations, comparing the power-off configuration and power-on configuration with co-rotating propellers. Here, b denotes the semi-span of the wing, with y/b representing the normalized spanwise location from the wing root to tip. For the power-off configuration, the inboard section exhibited distinct, deep stall features. As shown in Fig. 16(a), the velocity profile was nearly flat and its magnitude remained close to zero across the entire chord, indicating extensive flow separation originating near the leading edge. This flow separation was visually confirmed by the sectional velocity contour in Fig. 17(a), demonstrating a large region of low-velocity flow covering the entire upper surface of the airfoil. The severe velocity deficit reflected a breakdown of the boundary layer and the inability to maintain forward

momentum under the strong adverse pressure gradient. At the mid-span section (Fig. 16(b)), the power-off configuration continued to exhibit pronounced trailing-edge separation, as evidenced by the steep velocity decay in the aft part of the chord. Although the leading-edge region retained some flow attachment, the velocity rapidly diminished downstream, consistent with the separated flow structure observed in the skin friction contours in Fig. 15. By contrast, the power-on configuration with co-rotating propellers displayed fully developed velocity profiles at both spanwise sections. The boundary layer remained attached along most chordwise locations, with minimal signs of flow separation due to the high-momentum flow introduced by the propeller wake, which enhanced boundary layer attachment despite the high α . This effect is visualized in Fig. 17(b), where the high-velocity slipstream remained attached to the upper wing surface, effectively suppressing flow separation. These findings confirmed that DEP systems play a pivotal role in mitigating leading- and trailing-edge separation, thereby delaying stall onset and sustaining aerodynamic performance under aggressive operating conditions.

5.2 Rotational Direction Effects on Wing Aerodynamics in DEP Systems

Unlike the previous analysis, which focused on lift augmentation and stall delay due to the propeller wake by comparing power-off and power-on configurations, the goal of this section is to explore the impact of relative rotational direction of adjacent propellers on surface pressure development, spanwise load distribution, and overall aerodynamic performance of the DEP system. The influence of inter-propeller rotational direction in DEP systems is examined by comparing the configurations with co- and counter-rotating propellers introduced in Fig. 9. The integrated aerodynamic coefficients are first summarized in Table 8, which compares the lift, drag, and L/D coefficients for the power-on configurations with co- and counter-rotating propellers over a range of wing angles of attack. The lift coefficient results indicated that the configuration with co-rotating propellers consistently produced ~5% higher lift, on average, than the configuration with counter-rotating propellers. This trend can be observed from the static surface pressure distributions shown in Fig. 18, where the configuration with co-rotating propellers generated a more continuous and spanwise-uniform low-pressure field, whereas the configuration with counter-rotating propellers exhibited highly localized and intermittent suction peaks between adjacent propellers. Both configurations exhibited a progressive rise in lift coefficient value with increasing α up to $\alpha = 12^\circ$, beyond which a decrease was observed at $\alpha = 16^\circ$, indicating stall onset. The reduction in lift was more substantial for the configuration with counter-rotating propellers than with co-rotating propellers, suggesting that the spanwise concentration of aerodynamic loading between adjacent propellers may lead to local flow separation and trigger

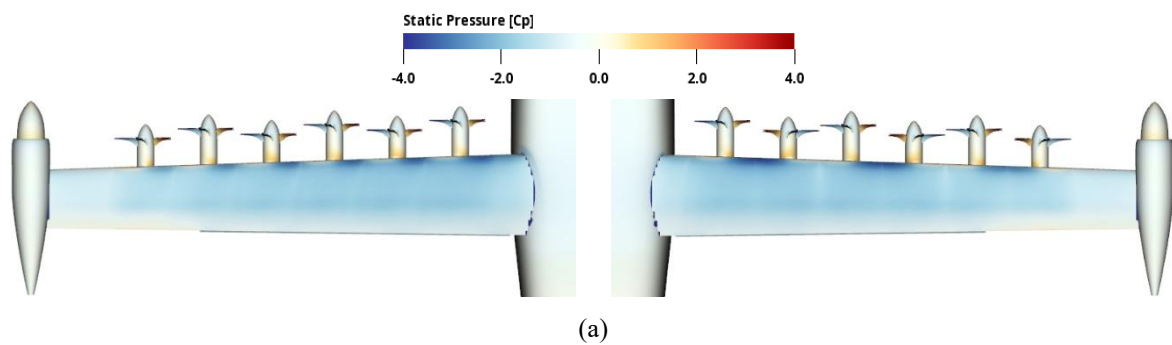
earlier stall at high wing angles of attack. A notable disparity in drag characteristics arose at higher wing angles of attack. At $\alpha = 16^\circ$, the configuration with counter-rotating propellers exhibited $\sim 23\%$ higher drag compared to that with co-rotating propellers. This increase was primarily associated with extensive flow separation and was further intensified by the strong localized upwash generated through adjacent slipstream interactions. Although this upwash contributed to lift augmentation at moderate wing angles of attack, it also elevated the local effective α between propellers, prematurely promoting flow separation and increasing pressure drag in those regions. Consequently, the configuration with co-rotating propellers achieved a consistently higher L/D ratio, particularly under the near-stall condition of $\alpha = 16^\circ$, where it exceeded its rotational counterpart by $>35\%$. These findings highlight a critical aerodynamic trade-off: while counter-rotating propellers can enhance localized lift via constructive slipstream interaction, they also incur performance penalties at high wing angles of attack. These results clearly demonstrate that the rotational direction fundamentally alters inter-propeller wake interaction patterns, thereby governing both spanwise load distribution and stall characteristics in multi-propeller DEP systems.

Table 8. Comparison of lift (C_L), drag (C_D), and lift-to-drag (L/D) coefficients between power-on configurations with co- and counter-rotating propellers.

α	Power-on with co-rotating propellers			Power-on with counter-rotating propellers		
	C_L	C_D	L/D	C_L	C_D	L/D
2°	2.776	0.325	8.53	2.493	0.319	7.81
8°	3.628	0.515	7.04	3.498	0.514	6.81
12°	4.018	0.734	5.46	3.964	0.701	5.56
16°	3.852	0.946	4.07	3.476	1.165	2.98

Fig. 18 presents the static pressure contours on the upper wing surface at four wing angles of attack for configurations with co- and counter-rotating propellers. The configuration with co-rotating propellers where all propellers on each wing rotated in the same direction, produced six distinct suction regions downstream of each propeller, characterized by pronounced asymmetry that reflected the directional swirl of the propeller wake. These asymmetric low-pressure footprints created a highly non-uniform spanwise pressure distribution, with alternating gradients that were strongly influenced by the rotational direction of the blade. By contrast, the configuration with counter-rotating propellers generated a markedly different pressure field. As observed in Fig. 18, adjacent slipstreams constructively interacted and tended to merge due to the alternating swirl directions of the wake structures, producing strong mutual upwash between propellers that led to more intense suction peaks between

propellers. This phenomenon became increasingly prominent at higher wing angles of attack. At $\alpha = 2^\circ$ and 8° , suction intensification was evident between propellers. By $\alpha = 12^\circ$ and especially at $\alpha = 16^\circ$, the configuration with counter-rotating propellers created large, merged suction regions that dominated the inboard wing section. This distinct pressure distribution was driven by the converging upwash from counter-rotating propeller pairs. For the configuration with counter-rotating propellers, the induced velocity fields merged to form a stronger localized jet-like upwash as the inner blades of adjacent propellers moved upward (i.e., on the sides facing each other) due to constructive interference. The strong upwash directly impinged upon the wing surface between propellers, substantially increasing the local effective angle of attack in inter-propeller regions. This intensified upwash drove the formation of high-magnitude suction peaks, leading to lift enhancement. However, this benefit was accompanied by a trade-off: while the configuration with counter-rotating propellers yielded a higher suction peak and potentially stronger local lift augmentation, it introduced discontinuous aerodynamic load distribution. This finding contrasted with the configuration with co-rotating propellers, where lift was distributed more evenly along the wingspan. The implications are critical for wing structure design given that highly concentrated aerodynamic loads can lead to increased fatigue and stress in confined areas. Therefore, the configuration with counter-rotating propellers may enhance aerodynamic performance but also impose additional demands on structural load management. This effect is further visualized in Fig. 19, which compares the wake structure and suction footprint between the two configurations. The configuration with co-rotating propellers yielded well-aligned helical wake structures with minimal mutual interference owing to the slipstreams from adjacent propellers rotating in the same direction (Fig. 19(a)). By contrast, the configuration with counter-rotating propellers exhibited strong upwash in the inter-propeller region due to the constructive interaction of adjacent slipstreams with alternating rotational directions between neighboring propellers (Fig. 19(b)). This interaction intensified the suction effect between propellers and generated more irregular wake structures with reduced coherence and increased unsteadiness, highlighting the aerodynamic consequences of the rotational scheme in DEP systems.



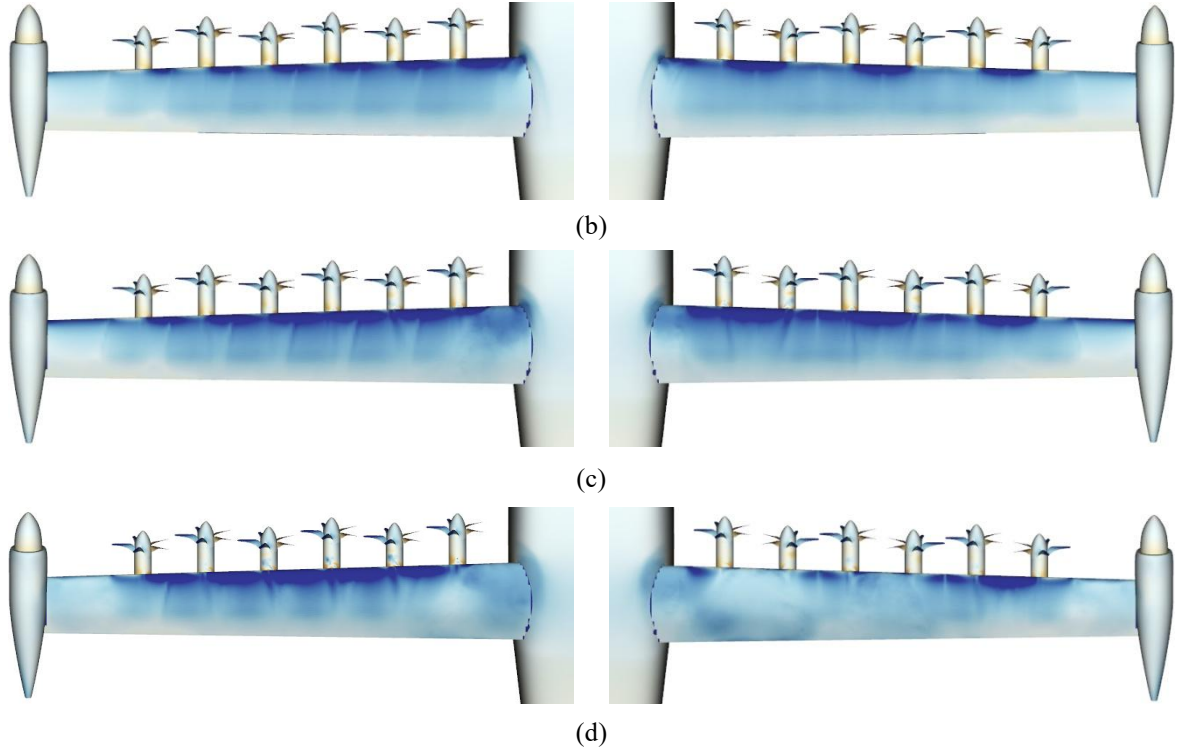


Fig. 18. Comparison of static pressure coefficient contours on the wing upper surface between configurations with co-rotating (left column) and counter-rotating (right column) propellers at: (a) $\alpha = 2^\circ$, (b) $\alpha = 8^\circ$, (c) $\alpha = 12^\circ$, and (d) $\alpha = 16^\circ$.

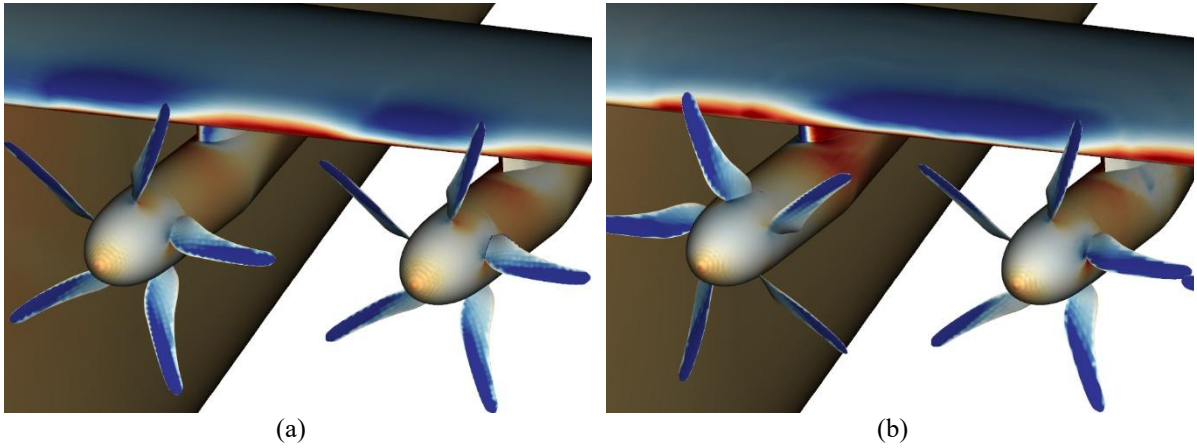
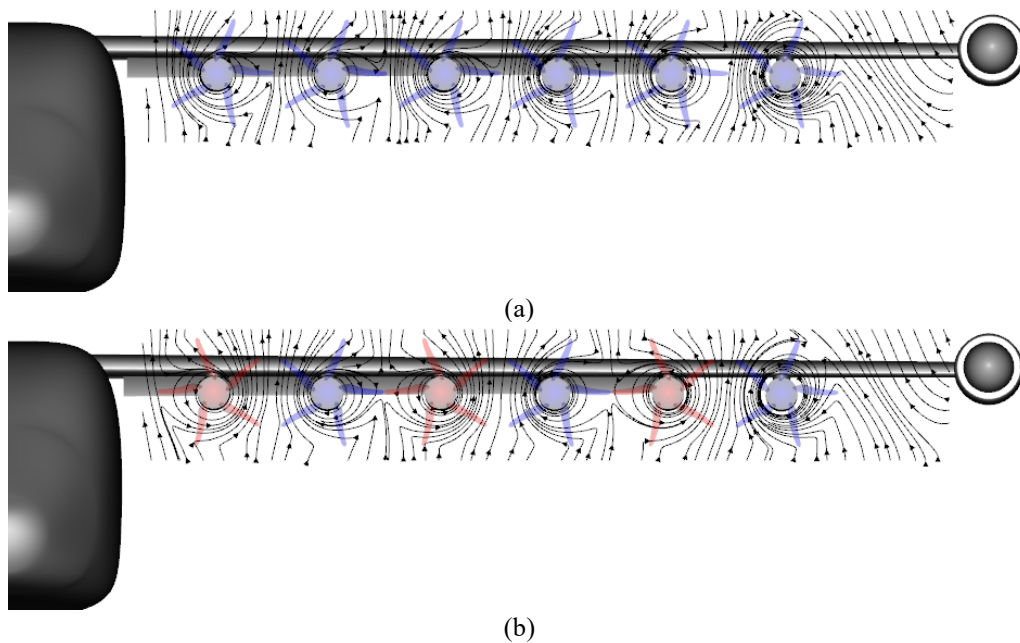


Fig. 19. Comparison of wake and suction effects between configurations with (a) co-rotating and (b) counter-rotating propellers, visualized by vortex structures and surface pressure fields.

The aerodynamic mechanisms underlying the spanwise pressure characteristics presented in Fig. 18 were further explored. Fig. 20 compares the spanwise pressure coefficient distribution at a chordwise location and the corresponding streamline patterns near the propellers for both rotational configurations at $\alpha = 2^\circ$, emphasizing the influence of propeller rotational patterns on near-field flow structures and load distribution. For the configuration with co-rotating propellers (Fig. 20(a)), the spanwise pressure profile revealed six distinct, periodic suction peaks,

each coinciding with the location of a propeller. The observed suction peaks implied that the suction enhancement was spatially confined, with each region of intensified pressure gradient limited to the area directly influenced by the individual propeller slipstream. Streamline visualization supported this observation, demonstrating an isolated vortex structure behind each propeller, with little to no interaction between adjacent wakes. By contrast, the configuration with counter-rotating propellers (Fig. 20(b)) generated fundamentally different aerodynamic features. The spanwise pressure profile exhibited three major suction peaks, located between adjacent propellers rather than directly behind them. This behavior was directly linked to the alternating swirl directions of the slipstreams, whereby the inboard side of each propeller pair generated upward flow that converged in the inter-propeller region, leading to constructive upwash interference. The streamline plot in Fig. 20 demonstrates the merging of these swirling flows, creating intense jets of upward momentum that impinged upon the wing surface, increasing the local effective α , and enhancing suction. This comparison highlights an important aerodynamic trade-off between the two rotational setups: the configuration with co-rotating propellers exhibited isolated suction peaks aligned with each propeller, while that with counter-rotating propellers generated merged slipstreams and amplified suction regions between adjacent propellers.



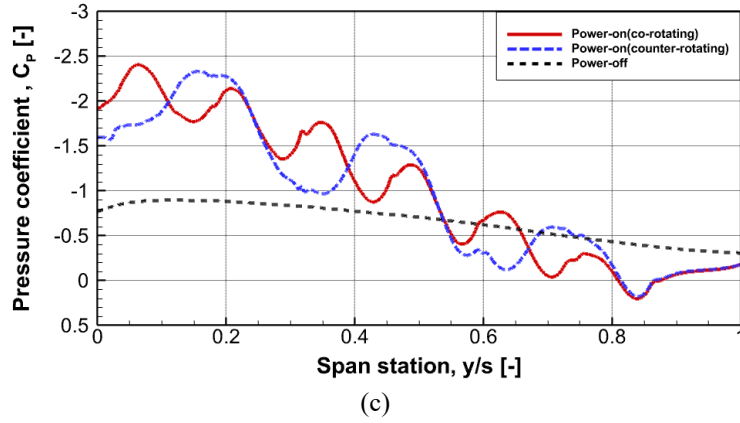


Fig. 20. Flow streamlines around propellers at $\alpha = 2^\circ$ for configurations with (a) co-rotating and (b) counter-rotating propellers; (c) spanwise distribution of pressure coefficient

6. Conclusion

This study presents a high-fidelity numerical investigation of the aerodynamic characteristics of DEP systems, focusing on complex flow interactions between the propeller slipstream and wing surface. To explore PWI effects on aerodynamic performance, a comprehensive analysis was conducted over a wide range of wing angles of attack, from takeoff to near-stall conditions, with multiple rotational configurations. A high-fidelity LBM–VLES approach was employed to resolve the unsteady, 3D flow structure characteristics of PWI phenomena. The numerical framework was first validated against experimental data obtained using NASA’s WIPP configuration, demonstrating its ability to capture PWI-induced surface pressure amplification, lift augmentation, and wake distortion. Subsequently, the validated solver was applied to the full-scale X-57 Maxwell aircraft, which features 12 high-lift propellers distributed along the leading edge of the wing. Unlike previous studies limited to simplified or single-propeller configurations, the present work resolves the coupled aerodynamic effects of multiple interacting propeller slipstreams, enabling direct assessment of inter-propeller wake interaction and its influence on wing aerodynamic performance. The key findings of the study are summarized as follows:

1. Propeller slipstreams substantially enhanced lift by improving the suction effect on the upper wing surface and delaying flow separation, resulting in up to 45% greater lift for the full configuration (power-on case) compared to the isolated wing (power-off case). This powered-lift effect was especially pronounced in regions directly influenced by propeller wake where the dynamic pressure substantially increased.
2. The full configuration exhibited stall delay behavior, maintaining flow attachment even under highly adverse pressure gradients. However, the lift gain was accompanied by a drag penalty of up to 60%, primarily due to increased friction and induced drag, revealing a performance trade-off during DEP system optimization.

3. Rotational direction was found to critically impact wing aerodynamic performance. The configuration with co-rotating propellers yielded more favorable spanwise pressure distributions and delayed flow separation, while that with counter-rotating propellers introduced destructive interference between adjacent slipstreams at high wing angles of attack, promoting premature stall and resulting in a 23% increase in drag.

The results demonstrate that aerodynamic performance is governed not only by the slipstream effect of individual propellers but also by inter-propeller wake interaction, which strongly affects spanwise load distribution, flow separation, and stall characteristics. From a design perspective, this finding suggests that DEP propellers should not be treated as independent thrust-producing devices; instead, propeller placement, thrust level, flap setting, and rotational direction should be considered together to balance lift augmentation, drag penalty, load uniformity, and stall margin. In particular, the comparison between co- and counter-rotating configurations indicates that rotational direction is a critical design parameter for controlling wake-interaction intensity and avoiding excessive localized upwash under high-lift conditions.

While the findings of the study offer a comprehensive assessment of PWIs across a range of static wing angles of attack, future work will incorporate off-design and maneuvering conditions, including yaw, crosswind, and gust environments. These factors are known to substantially alter slipstreams and propeller–airframe interactions, necessitating their inclusion in evaluations of DEP performance during flight operations. In addition, the acoustic implications of PWIs were not addressed in this study. Consequently, future work will aim to incorporate aeroacoustic modeling frameworks, such as acoustic analogies based on the Ffowcs Williams–Hawkings equation, to investigate PWI effects on noise generation in DEP systems, thereby contributing to the development of quiet and efficient electric aviation platforms. Because the aeroacoustic simulations of full-configuration DEP aircraft are expected to require substantially higher computational cost, simplified configurations consisting of two neighboring propellers with spanwise periodic boundary conditions will also be considered as an efficient approach for examining local inter-propeller wake interaction and its acoustic characteristics.

Declaration of competing interests

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

Acknowledgements

This work was supported by a National Research Foundation of Korea (NRF) grant funded by the Ministry of Science and ICT (Project Numbers: RS-2024-00397400, RS-2025-25423496), and the Glocal University 30 Project Fund of Gyeongsang National University in 2026.

References

- [1] Guynn, M. D., Berton, J. J., Haller, W. J., Hendricks, E. S., and Tong, M. T., “Performance and Environmental Assessment of an Advanced Aircraft with Open Rotor Propulsion,” NASA/TM-2012-217772, National Aeronautics and Space Administration, Oct. 2012.
- [2] Borer, N. K., Patterson, M. D., Viken, J. K., Moore, M. D., Bevirt, J., Stoll, A. M., and Gibson, A. R., “Design and Performance of the NASA SCEPTOR Distributed Electric Propulsion Flight Demonstrator,” 2016.
<https://doi.org/10.2514/6.2016-3920>
- [3] Deere, K. A., Viken, J. K., Viken, S., Carter, M. B., Wiese, M., and Farr, N., “Computational Analysis of a Wing Designed for the X-57 Distributed Electric Propulsion Aircraft,” 2017.
<https://doi.org/10.2514/6.2017-3923>
- [4] Veldhuis, L. L. M., “Review of Propeller-Wing Aerodynamic Interference,” 24th Congress of the International Council of the Aeronautical Sciences, ICAS 2004-6.3.1, Yokohama, Japan, 2004.
- [5] Sinnige, T., van Arnhem, N., Stokkermans, T. C. A., Eitelberg, G., and Veldhuis, L. L. M., “Wingtip-Mounted Propellers: Aerodynamic Analysis of Interaction Effects and Comparison with Conventional Layout,” *Journal of Aircraft*, Vol. 56, No. 1, 2019, pp. 295–312.
<https://doi.org/10.2514/1.C034978>
- [6] Zanotti, A., Velo, A., Pepe, C., Savino, A., Grassi, D., and Riccobene, L., “Aerodynamic Interaction between Tandem Propellers in EVTOL Transition Flight Configurations,” *Aerospace Science and Technology*, Vol. 147, 2024, p. 109017.
<https://doi.org/10.1016/j.ast.2024.109017>
- [7] de Vries, R., van Arnhem, N., Avallone, F., Ragni, D., Vos, R., Eitelberg, G., and Veldhuis, L. L. M., “Experimental Investigation of Over-the-Wing Propeller–Boundary-Layer Interaction,” *AIAA Journal*, Vol. 59, No. 6, 2021, pp. 2169–2182.

- <https://doi.org/10.2514/1.J059770>
- [8] Johnson, R. T., Witkowski, D. P., and Sullivan, J. P., “Experimental Results of a Propeller/Wing Interaction Study,” *SAE Transactions*, 1991, pp. 121–130.
- <https://doi.org/10.4271/910998>
- [9] Deere, K. A., Viken, S., Carter, M. B., Viken, J. K., Cox, D. E., Wiese, M. R., and Farr, N. L., “Computational Component Build-up for the X-57 Maxwell Distributed Electric Propulsion Aircraft,” 2018.
- <https://doi.org/10.2514/6.2018-1275>
- [10] Catalano, F. M., “On the Effects of an Installed Propeller Slipstream on Wing Aerodynamic Characteristics,” *Acta Polytechnica*, Vol. 44, No. 3, 2004.
- <https://doi.org/10.14311/562>
- [11] Stephenson, J. H., Schatzman, N. L., Cheung, B. K., Zawodny, N. S., Sargent, D. C., and Sim, B. W.-C., “Aeroacoustic Measurements from the Aerodynamic and Acoustic Rotorprop Test (AART) in the National Full-Scale Aerodynamics Complex (NFAC) 40-by 80-Foot Wind Tunnel,” 2021.
- [12] Stokkermans, T. C. A., Van Arnhem, N., Sinnige, T., and Veldhuis, L. L. M., “Validation and Comparison of RANS Propeller Modeling Methods for Tip-Mounted Applications,” *AIAA Journal*, Vol. 57, No. 2, 2019, pp. 566–580.
- <https://doi.org/10.2514/1.J057398>
- [13] Tugnoli, M., Montagnani, D., Syal, M., Droandi, G., and Zanotti, A., “Mid-Fidelity Approach to Aerodynamic Simulations of Unconventional VTOL Aircraft Configurations,” *Aerospace Science and Technology*, Vol. 115, 2021, p. 106804.
- <https://doi.org/10.1016/j.ast.2021.106804>
- [14] Zanotti, A., Savino, A., Palazzi, M., Tugnoli, M., and Muscarello, V., “Assessment of a Mid-Fidelity Numerical Approach for the Investigation of Tiltrotor Aerodynamics,” *Applied Sciences*, Vol. 11, No. 8, 2021, p. 3385.
- <https://doi.org/10.3390/app11083385>
- [15] Sengupta, B., Lee, Y., Araghizadeh, M. S., Myong, R. S., and Lee, H., “Comparative Analysis of Direct Method and Fast Multipole Method for Multirotor Wake Dynamics,” *International Journal of Aeronautical and Space Sciences*, Vol. 25, No. 3, 2024, pp. 789–808.
- <https://doi.org/10.1007/s42405-023-00699-w>

- [16] Janus, J. M., Chatterjee, A., and Cave, C., “Computational Analysis of a Wingtip-Mounted Pusher Turboprop,” *Journal of Aircraft*, Vol. 33, No. 2, 1996, pp. 441–444.
<https://doi.org/10.2514/3.46959>
- [17] Gomariz-Sancha, A., Maina, M., and Peace, A. J., “Analysis of Propeller-Airframe Interaction Effects through a Combined Numerical Simulation and Wind-Tunnel Testing Approach,” 53rd AIAA Aerospace Sciences Meeting, Kissimmee, FL, 2015.
<https://doi.org/10.2514/6.2015-1026>
- [18] Jayasundara, D., Jung, Y. S., and Baeder, J., “Aeroacoustic and Aerodynamic Investigation of a Wingtip-Mounted Tractor Propeller,” *Journal of the American Helicopter Society*, Vol. 68, No. 1, 2023, pp. 1–16.
<https://doi.org/10.4050/JAHS.68.012011>
- [19] Chandukrishna, Y., and Venkatesh, T. N., “Numerical Investigation of Aerodynamically Efficient Wing Tip-Mounted Propeller Configuration Using Coupled RANS–BEM Approach,” *Aircraft Engineering and Aerospace Technology*, Vol. 95, No. 6, 2023, pp. 995–1001.
<https://doi.org/10.1108/AEAT-09-2022-0254>
- [20] Zhou, B. Y., Morelli, M., Gauger, N. R., and Guardone, A., “Simulation and Sensitivity Analysis of a Wing-Tip Mounted Propeller Configuration from the Workshop for Integrated Propeller Prediction (WIPP),” AIAA AVIATION 2020 Forum, Virtual Event, 2020.
<https://doi.org/10.2514/6.2020-2683>
- [21] Chen, S., and Doolen, G. D., “Lattice Boltzmann Method for Fluid Flows,” *Annual Review of Fluid Mechanics*, Vol. 30, No. 1, 1998, pp. 329–364.
<https://doi.org/10.1146/annurev.fluid.30.1.329>
- [22] Succi, S., “The Lattice Boltzmann Equation: For Fluid Dynamics and Beyond,” Oxford University Press, 2001.
<https://doi.org/10.1093/oso/9780198503989.001.0001>
- [23] König, B., and Fares, E., “Exa Powerflow Simulations for the Sixth AIAA Drag Prediction Workshop,” *Journal of Aircraft*, Vol. 55, No. 4, 2018, pp. 1482–1490.
<https://doi.org/10.2514/1.C034480>
- [24] Kim, S. J., Hwang, Y.-H., Myong, R. S., and Lee, H., “Interactional Aerodynamics and Acoustics of a Rotor with an Airframe in Hover,” *Physics of Fluids*, Vol. 36, No. 1, 2024.
<https://doi.org/10.1063/5.0185036>

- [25] Van der Velden, W. C. P., Romani, G., and Casalino, D., “Validation and Insight of a Full-Scale S-76 Helicopter Rotor Using the Lattice-Boltzmann Method,” *Aerospace Science and Technology*, Vol. 118, 2021, p. 107007.
<https://doi.org/10.1016/j.ast.2021.107007>
- [26] Lyu, C., Liu, P., Hu, T., Geng, X., Sun, T., and Akkermans, R. A. D., “A Sliding Mesh Approach to the Lattice Boltzmann Method Based on Non-Equilibrium Extrapolation and Its Application in Rotor Flow Simulation,” *Aerospace Science and Technology*, Vol. 128, 2022, p. 107755.
<https://doi.org/10.1016/j.ast.2022.107755>
- [27] Bhatnagar, P. L., Gross, E. P., and Krook, M., “A Model for Collision Processes in Gases. I. Small Amplitude Processes in Charged and Neutral One-Component Systems,” *Physical Review*, Vol. 94, No. 3, 1954, pp. 511–525.
<https://doi.org/10.1103/PhysRev.94.511>
- [28] Chen, H., Teixeira, C., and Molvig, K., “Realization of Fluid Boundary Conditions via Discrete Boltzmann Dynamics,” *International Journal of Modern Physics C*, Vol. 9, No. 8, 1998, pp. 1281–1292.
<https://doi.org/10.1142/S0129183198001151>
- [29] Grande, E., Shubham, S., Avallone, F., Ragni, D., and Casalino, D., “Computational Aeroacoustic Study of Co-Rotating Rotors in Hover,” *Aerospace Science and Technology*, Vol. 153, 2024, p. 109381.
<https://doi.org/10.1016/j.ast.2024.109381>
- [30] Romani, G., and Casalino, D., “Rotorcraft Blade-Vortex Interaction Noise Prediction Using the Lattice-Boltzmann Method,” *Aerospace Science and Technology*, Vol. 88, 2019, pp. 147–157.
<https://doi.org/10.1016/j.ast.2019.03.029>
- [31] Konig, B., Duda, B. M., and Laskowski, G. M., “Lattice Boltzmann Simulations for the 4th AIAA High-Lift Prediction Workshop Using PowerFLOW,” AIAA AVIATION 2022 Forum, Chicago, IL & Virtual, 2022.
<https://doi.org/10.2514/6.2022-3433>
- [32] Singh, D., Konig, B., Fares, E., Murayama, M., Ito, Y., Yokokawa, Y., and Yamamoto, K., “Lattice-Boltzmann Simulations of the JAXA JSM High-Lift Configuration in a Wind Tunnel,” AIAA Scitech 2019 Forum, San Diego, CA, 2019.
<https://doi.org/10.2514/6.2019-1333>

- [33] Qiao, G., Zhang, T., and Barakos, G. N., “Numerical Simulation of Distributed Propulsion Systems Using CFD,” *Aerospace Science and Technology*, Vol. 147, 2024, p. 109011.
<https://doi.org/10.1016/j.ast.2024.109011>
- [34] Teixeira, C. M., “Incorporating Turbulence Models into the Lattice-Boltzmann Method,” *International Journal of Modern Physics C*, Vol. 9, No. 08, 1998, pp. 1159–1175.
<https://doi.org/10.1142/S0129183198001060>
- [35] Teruna, C., Ragni, D., Avallone, F., and Casalino, D., “A Rod-Linear Cascade Model for Emulating Rotor-Stator Interaction Noise in Turbofans: A Numerical Study,” *Aerospace Science and Technology*, Vol. 90, 2019, pp. 275–288.
<https://doi.org/10.1016/j.ast.2019.04.047>
- [36] Yakhot, V., and Orszag, S. A., “Renormalization Group Analysis of Turbulence. I. Basic Theory,” *Journal of Scientific Computing*, Vol. 1, No. 1, 1986, pp. 3–51.
<https://doi.org/10.1007/BF01061452>
- [37] Chen, H., Orszag, S. A., Staroselsky, I., and Succi, S., “Expanded Analogy between Boltzmann Kinetic Theory of Fluids and Turbulence,” *Journal of Fluid Mechanics*, Vol. 519, 2004, pp. 301–314.
<https://doi.org/10.1017/S0022112004001211>
- [38] Zhang, X., Gomez-Paz, J., Chen, X., McDonough, J. M., Islam, M. M., Andreopoulos, Y., Zhu, L., and Yu, H., “Volumetric Lattice Boltzmann Method for Wall Stresses of Image-Based Pulsatile Flows,” *Scientific Reports*, Vol. 12, No. 1, 2022, p. 1697.
<https://doi.org/10.1038/s41598-022-05269-w>
- [39] Launder, B. E., and Spalding, D. B., “The Numerical Computation of Turbulent Flows,” *Numerical Prediction of Flow, Heat Transfer, Turbulence and Combustion*, Elsevier, 1983, pp. 96–116.
<https://doi.org/10.1016/B978-0-08-030937-8.50016-7>
- [40] Shih, T.-H., Povinelli, L. A., Liu, N.-S., Potapczuk, M. G., and Lumley, J. L., “A Generalized Wall Function,” NASA/TM-1999-209398, ICOMP-99-08, National Aeronautics and Space Administration, Glenn Research Center, July 1999.
- [41] Malaspinas, O., and Sagaut, P., “Wall Model for Large-Eddy Simulation Based on the Lattice Boltzmann Method,” *Journal of Computational Physics*, Vol. 275, 2014, pp. 25–40.
<https://doi.org/10.1016/j.jcp.2014.06.020>

- [42] Asmuth, H., Janßen, C. F., Olivares-Espinosa, H., and Ivanell, S., “Wall-Modeled Lattice Boltzmann Large-Eddy Simulation of Neutral Atmospheric Boundary Layers,” *Physics of Fluids*, Vol. 33, No. 10, 2021.
<https://doi.org/10.1063/5.0065701>
- [43] Duensing, J. C., Yoo, S., Maldonado, D., Housman, J. A., Jensen, J. C., and Kiris, C. C., “Establishing Best Practices for X-57 Maxwell CFD Database Generation,” AIAA Scitech 2019 Forum, San Diego, CA, 2019.
<https://doi.org/10.2514/6.2019-0274>
- [44] Yoo, S., Duensing, J. C., Deere, K. A., Viken, J. K., and Frederick, M., “Computational Analysis on the Effects of High-Lift Propellers and Wing-Tip Cruise Propellers on X-57,” *AIAA AVIATION 2023 Forum*, San Diego, CA, 2023.
<https://doi.org/10.2514/6.2023-3382>