

Article

A CFD-Based Comparative Analysis of X-Wing Drone Performance with Varying Dihedral Angles

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Abstract

The aerodynamic performance of unmanned aerial vehicles (UAVs) with non-conventional geometries is a growing area of interest, particularly for improving stability and maneuverability. This study investigates the influence of the dihedral angle on the aerodynamic behavior and overall performance of drones configured in an X-wing layout. Four configurations with dihedral angles of 0°, 15°, 30°, and 45° were analyzed to assess how varying the wing inclination affects flight characteristics. Computational fluid dynamics (CFD) simulations were conducted to evaluate the aerodynamic forces and moments acting on each configuration under controlled conditions. Following the aerodynamic analysis, a performance assessment was carried out to determine the implications of each dihedral angle on parameters such as range, endurance, rate of climb, angle of climb or turn rate. The results indicate that increasing the dihedral angle can enhance maneuverability but may lead to trade-offs in aerodynamic efficiency, particularly at higher angles. The 15° and 30° configurations demonstrated a favorable balance between maneuverability and performance. These findings provide insight into the design optimization of X-wing UAVs and highlight the potential of dihedral angle tuning as a means to tailor drone behavior for specific operational needs.

Keywords: X-wing architecture; aerodynamics; performance; loitering munition; dihedral effect; skid-to-turn

1. Introduction

In recent years, the operational relevance of unmanned aerial vehicles (UAVs) has surged, driven by advancements in lightweight materials, battery technologies, and autonomous navigation systems. Among the many UAV categories, loitering munitions, also referred to as “kamikaze drones”, have emerged as a transformative element in modern warfare. These systems, exemplified by platforms such as the Lancet (Russia), Hero 20–120 series (Israel), and HX-2 (Germany), are designed to patrol an area for extended periods and strike targets upon identification. Their growing prominence in asymmetric and conventional conflicts highlights the urgent need to optimize design parameters for improved endurance, precision, and controllability [1].

A comparative study presented in [2] analyzes various loitering munition configurations, including conventional fixed-wing, canard, delta wing, cruciform (X-wing), tandem wing, and rotorcraft designs. The findings indicate that the cruciform or X-wing configuration is particularly well-suited for low-mass drones (under 40 kg) with relatively short



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endurance (less than 100 min). The analysis, which focuses on commercially available systems, further reveals that X-wing loitering munitions typically exhibit a wing loading of approximately 500 N/m^2 , an aspect ratio near 7, and a fuselage-to-wingspan ratio close to 1.

As presented in [3], the analyzed drone is a medium-range loitering munition (LM) equipped with an electric propulsion system, typically operating at a cruise speed of about 30 m/s and at altitudes below 1500 m . It is intended for both direct support of military units and engagement of enemy targets in depth of the defense.

A defining characteristic of many modern loitering munitions is the adoption of an X-wing configuration, in which the wings are arranged in a cross pattern with a dihedral angle relative to the lateral axis. This geometry has gained attention due to its compact folding capability for launch from tubular or rail systems, enhanced maneuverability, and symmetry, which can improve flight control and stability during loitering and attack phases. Furthermore, variations in dihedral angle within this X-wing layout can significantly affect aerodynamic behavior, potentially offering a passive means of improving roll and yaw stability without increasing the load on the flight control system. This is especially relevant for loitering munitions, which often rely on autonomous or semi-autonomous navigation under variable conditions, including wind gusts, complex terrain, and time-sensitive targeting.

While the benefits of dihedral angles in fixed-wing aircraft are well established, primarily in terms of enhancing lateral stability, there is limited literature on their role in X-wing UAVs, particularly in loitering munitions. The X-wing configuration is known to offer several key advantages, particularly in terms of lateral stability, which benefits from the increased vertical projected area of the wings. Its compact geometry allows for efficient folding mechanisms, made possible by the relatively small aspect ratio and the similar lengths of the fuselage and wings. Additionally, the reduced aspect ratio results in lower root bending loads, contributing to a lighter overall structure. However, these benefits come at the cost of aerodynamic efficiency. Compared to conventional wing configurations, the X-wing layout typically exhibits lower aerodynamic performance due to its reduced aspect ratio and the presence of additional wings, which collectively increase induced drag. As a result, the overall performance of X-wing drones tends to be lower than that of conventional wing designs. Despite these limitations, the X-wing configuration remains relevant for specific applications, especially where compactness and maneuverability are prioritized. In this context, the present study conducts a comparative aerodynamic and performance analysis of four X-wing UAV configurations, featuring dihedral angles of 0° , 15° , 30° , and 45° , in order to assess the impact of dihedral angle on flight characteristics.

This study extends the research conducted in [4–6], which investigated the flight characteristics of loitering munition drones. Using CFD simulations, we evaluate each configuration's force and moment characteristics. We then assess the overall performance implications in terms of flight efficiency and control authority. The results offer new insights into how dihedral angle tuning can be leveraged to enhance the flight characteristics of loitering munitions and other compact UAVs, contributing to more efficient and robust aerial platforms for both military and civilian applications.

The main research gap addressed by this study lies in the evident lack of scientific literature focused on X-wing drone configurations, particularly those designed for loitering munition applications. Despite their demonstrated effectiveness and growing presence on modern battlefields, there are no comprehensive aerodynamic or performance studies that analyze this configuration's advantages or limitations. Existing UAV research primarily focuses on conventional fixed-wing or tandem-wing designs, leaving a significant gap in understanding the aerodynamic behavior and design trade-offs of X-wing platforms. Furthermore, critical aspects such as the skid-to-turn (STT) and dive maneuvers, which

are essential for the precision control and terminal phase of loitering munitions, are rarely detailed or only briefly discussed in the current literature. This paper fills that gap by presenting a consistent and systematic study of X-wing UAVs, combining CFD-based aerodynamic analysis with flight performance evaluations to deliver an accurate and quantitative comparison among various dihedral-angle configurations. By addressing these unstudied dynamics and linking aerodynamic design to real operational needs, this research provides new insight into the practical and theoretical understanding of X-wing drones and their application in precision-guided, loitering munition missions.

2. Mathematical Modeling

2.1. Model Design

To conduct this study, four X-wing drone models with varying dihedral angles are designed using OpenVSP 3.45.1. In order to ensure a consistent and reliable comparison, all models share identical fuselage, empennage, and wing geometries, with the only difference being the dihedral angle of the wings, as illustrated in Figure 1.

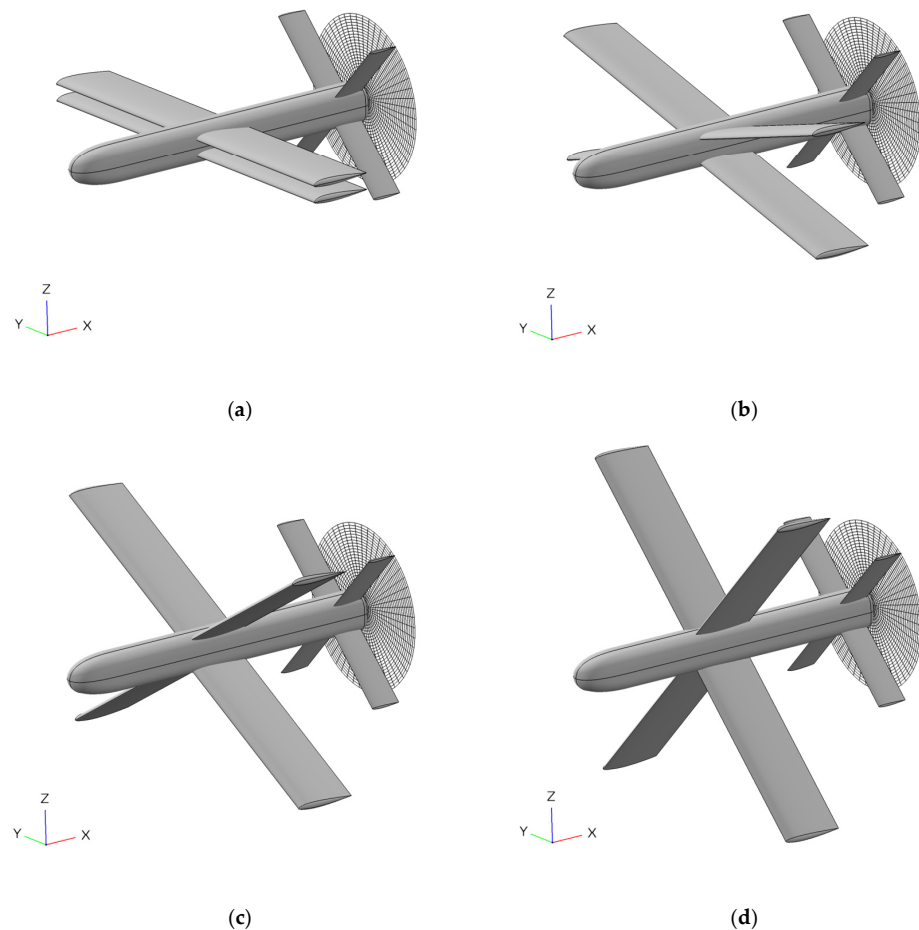


Figure 1. Geometries of analyzed vehicles: (a) with 0° dihedral angle; (b) with 15° dihedral angle; (c) with 30° dihedral angle; and (d) with 45° dihedral angle.

The drone geometry features a fuselage with a constant cross section, a rounded nose, and a blunt rear end. The fuselage is 1.65 m in length and has a rectangular cross section with rounded corners, measuring $0.18 \text{ m} \times 0.18 \text{ m}$, and a corner radius of 0.06 m. A pusher-type propeller is mounted at the rear of the fuselage, but it is excluded from the aerodynamic analysis for the simplicity.

The empennage consists of four vertical fins arranged at a 45° dihedral angle, equally spaced at 90° intervals around the tail section. Each fin has a span of 0.5 m and a constant chord of 0.15 m , employing the symmetric NACA 0010 airfoil. The longitudinal position of the empennage, measured from the nose, is set at 1.45 m .

The wing configuration includes four identical wings symmetrically positioned relative to the vertical plane and mounted at varying dihedral angles, depending on the case. Each wing has a span of 1 m and a constant chord of 0.3 m , using the cambered NACA 23012 airfoil. The wings are mounted at an x-station of 0.65 m from the nose of the fuselage.

The main geometrical characteristics of the model are presented in Figure 2, in frontal view, and in Figure 3, in side view.

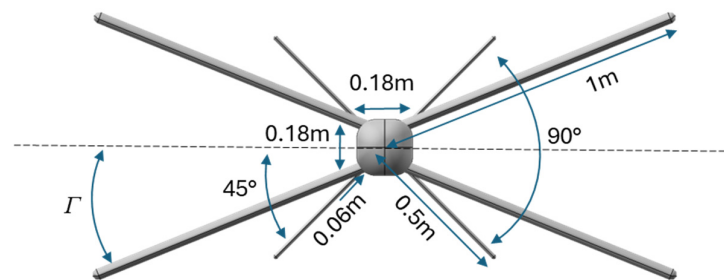


Figure 2. Frontal view of the model with dimensions.

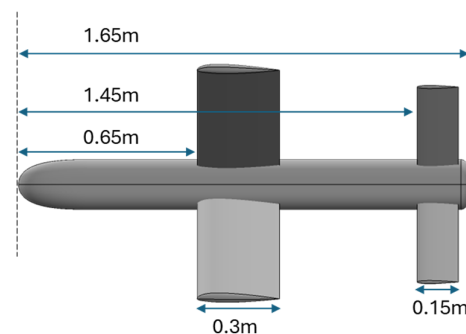


Figure 3. Side view of the model with dimensions.

The models are equipped with an X-wing empennage functioning as monoblock ruddervators, in which the fins rotate entirely around their focal axis. The ailerons are positioned on the wings between 0.5 m and 0.9 m along the span, with a chordwise width of 35% of the wing chord.

Given that the total weight of the model is 30 kg and based on its geometric parameters such as a wingspan of 2 m , a wing area of 1.2 m^2 , and a fuselage length of 1.65 m the resulting wing aspect ratio ranges from 6.66 at a dihedral angle of 0° to 4.71 at a dihedral angle of 45° . The corresponding wing loading is 245 N/m^2 , while the fuselage length-to-wing span ratio is 0.825 . Although these values are somewhat lower than the average values reported in [2], they remain within a reasonable and acceptable range for loitering munition UAVs.

Considering the drone's total mass of 30 kg , it can be powered by either a thermal engine or an electric motor. For simplification of this study, it is assumed that the drones are equipped with an electric motor, which allows the total mass to remain constant during flight. To assess the performance of the drone, an estimation of the battery mass is necessary.

Based on the commercial drone datasheet provided in [2,3], for a drone with a total mass of 30 kg , the payload capacity is assumed to be approximately 10 kg .

The airframe is designed as a monocoque structure, constructed from lightweight materials such as plastics or composites. Assuming a total wetted surface area (S_{wet}) of

3.75 m², a skin thickness (t_s) of 1.5 mm, and an average material density (ρ_m)—such as carbon fiber reinforced polymer (CFRP)—of 1600 kg/m³ [7], the resulting structural mass (m_s) is estimated to be 9 kg, as calculated using relation (1):

$$m_s = \rho_m \cdot t_s \cdot S_{wet} = 1600 \cdot 1.5 \cdot 10^{-3} \cdot 3.75 = 9 \text{ kg}, \quad (1)$$

The mass of the electronic systems and propulsion unit comprises several key components: the flight control system (~0.4 kg), onboard camera (~1.2 kg), data link and communication module (~0.6 kg), power management and distribution unit (~0.6 kg), mission computer (~0.5 kg), detonation electronics (~0.3 kg), wiring harnesses and printed circuit boards (~0.6 kg), and the electric motor with propeller (~0.8 kg). The total estimated mass of these components is approximately 5 kg.

Therefore, the battery mass is estimated by subtracting the payload, structural components, and the combined mass of the electronic systems and motor from the drone's total mass, resulting in an available battery mass of approximately 6 kg. Furthermore, assuming an average lithium-ion battery energy density of 220 Wh/kg [8], the total available energy is estimated to be approximately 1320 Wh.

After accounting for approximately 320 Wh consumed by the electronic systems (excluding the motor), the remaining 1000 Wh is allocated to the propulsion unit and will be used in the subsequent performance analysis. To prioritize the aerodynamic effects of the dihedral angle on the performance of the considered vehicles, discharge-rate effects were not included and a constant specific energy model is assumed in the present analysis, even though battery capacity and efficiency are known to depend on discharge rate due to internal resistance, voltage sag, and electrochemical limitations as presented in [9].

Figure 4 presents a general mass distribution of a 30 kg loitering munition drone with a X-wing configuration.

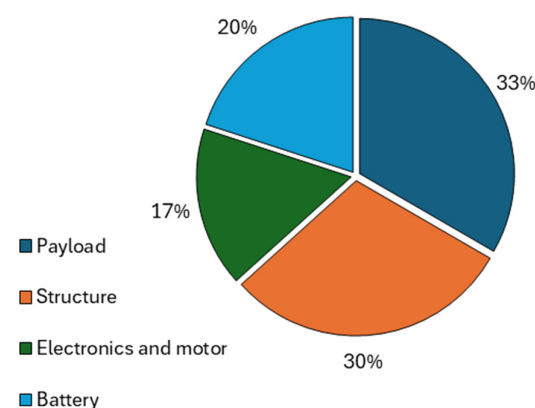


Figure 4. Mass distribution.

2.2. Aerodynamic Modeling

2.2.1. Computational Domain and Grid Generation

The computational domain was constructed as a spherical region with a radius of 400 m. For the longitudinal analysis (variation with angle of attack), only a semi-spherical domain was considered, whereas for the lateral analysis (variation with sideslip angle), the full spherical domain was considered. A mesh convergence study was then performed for the longitudinal case at $\alpha = 0^\circ$, in which the axial force, normal force, and pitch moment coefficient were monitored.

The study focused on surface mesh refinement, with the medium-sized grid selected as the baseline. The coarse and fine grids were generated by halving and doubling, respectively, the surface mesh resolution for each geometric component. The surface mesh

was primarily composed of quadrilateral elements across all regions. For the wing, the maximum element size was set to 10 mm, with localized refinement at the wingtip down to 4 mm. The empennage was meshed with a maximum element size of 6 mm, refined to 3 mm at the tip, while the fuselage was assigned a maximum element size of 15 mm. A consistent minimum element size of 0.5 mm was applied throughout the geometry. Additionally, anisotropic meshing was applied along the leading and trailing edges, ensuring an appropriate balance between numerical accuracy and computational cost as can be seen in Figure 5.

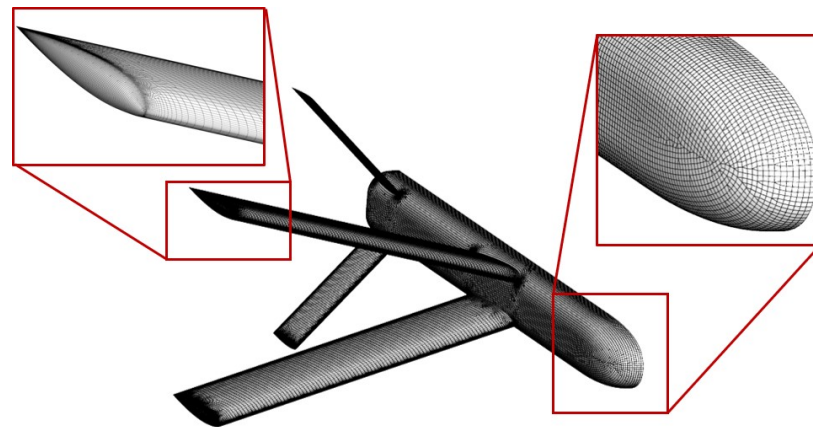


Figure 5. Surface grid size representation.

For the volume mesh, the first cell height was set to 0.022 mm corresponding to a $y^+ = 1$ with a growth rate of 1.1, providing high boundary-layer resolution. For the exterior domain hexahedral-dominant option was chosen, with four buffer layers to ensure a smooth transition between the boundary-layer mesh and the outer volume grid.

A grid convergence study was performed using three systematically refined meshes (3.2 M, 6.5 M, and 14 M cells). The Grid Convergence Index (GCI) was evaluated following Roache's method [10]. The resulting GCI values are 7.8×10^{-4} for C_{L0} and 8.2×10^{-3} for C_{D0} , indicating excellent mesh convergence for both lift and drag. Based on these results, the medium mesh was selected for all subsequent simulations.

Figure 6 presents the variation in the lift and drag coefficients at $\text{AoA} = 0^\circ$ for the $\Gamma = 30^\circ$ configuration as a function of grid resolution, demonstrating that the medium grid provides sufficiently accurate results while maintaining an efficient use of computational resources.

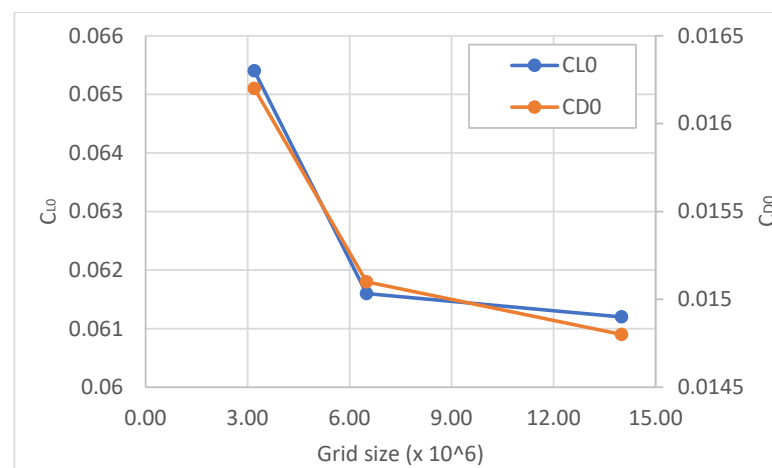


Figure 6. Variation in lift and drag coefficient at zero angle of attack with grid size.

2.2.2. Flow Model

Numerical simulations were performed using the ANSYS Fluent CFD 2021 R2 solver by solving the Reynolds-Averaged Navier–Stokes (RANS) equations coupled with the four-equation Transition Shear Stress Transport (SST) turbulence model due to low Reynold number ($Re = 561,921.5$) which indicate a transitional flow. As presented in [11,12], the Transition SST turbulence model is recommended for transitional flows, as it allows for more accurate prediction of flow separation.

To prevent the unphysical buildup of turbulent kinetic energy in stagnation regions, production limiters were applied to the turbulent kinetic energy transport equations. Additionally, the flow is assumed incompressible, given the low Mach numbers (~ 0.1) analyzed in this study.

The RANS system of equation is presented in (2) and (3):

$$\frac{\partial \bar{u}_i}{\partial x_i} = 0 \quad (2)$$

$$\frac{\partial(\rho \bar{u}_i)}{\partial t} + \frac{\partial}{\partial x_j}(\rho \bar{u}_j \bar{u}_i) = -\frac{\partial \bar{p}}{\partial x_i} + \mu \frac{\partial^2 \bar{u}_i}{\partial x_j^2} - \frac{\partial \tau'_{ij}}{\partial x_j} \quad (3)$$

where ρ represents the density, $\bar{u}_i$ the time-averaged velocity components, p the pressure, μ the dynamic viscosity, and τ'_{ij} the Reynolds stress tensor. These equations are coupled with the Transition SST [13,14] four equation turbulence model as presented in (4)–(7):

$$\frac{\partial}{\partial t}(\rho k) + \frac{\partial}{\partial x_i}(\rho k u_i) = \frac{\partial}{\partial x_j} \left(\Gamma_k \frac{\partial k}{\partial x_j} \right) + G_k^* - Y_k^* + S_k \quad (4)$$

$$\frac{\partial}{\partial t}(\rho \omega) + \frac{\partial}{\partial x_i}(\rho \omega u_i) = \frac{\partial}{\partial x_j} \left(\Gamma_\omega \frac{\partial \omega}{\partial x_j} \right) + G_\omega - Y_\omega + D_\omega + S_\omega \quad (5)$$

$$\frac{\partial(\rho \gamma)}{\partial t} + \frac{\partial(\rho U_i \gamma)}{\partial x_j} = P_{\gamma_1} - E_{\gamma_1} + P_{\gamma_2} - E_{\gamma_2} + \frac{\partial}{\partial x_j} \left[\left(\mu + \frac{\mu_t}{\sigma_\gamma} \right) \frac{\partial \gamma}{\partial x_j} \right] \quad (6)$$

$$\frac{\partial(\rho \widetilde{Re_{\theta_t}})}{\partial t} + \frac{\partial(\rho U_j \widetilde{Re_{\theta_t}})}{\partial x_j} = P_{\theta_t} + \frac{\partial}{\partial x_j} \left[\sigma_{\theta_t} (\mu + \mu_t) \frac{\partial \widetilde{Re_{\theta_t}}}{\partial x_j} \right] \quad (7)$$

The first equation presents the transport equation for the turbulent kinetic energy k , where Γ_k represents the effective diffusivity of k , G_k^* denotes the modified term corresponding to the generation of turbulent kinetic energy due to mean velocity gradients, Y_k^* denotes the modified term corresponding to the dissipation of k , and S_k represents the source term. The second equation presents the transport equation for the specific dissipation rate ω , where Γ_ω represents the effective diffusivity of ω , G_ω denotes the generation of ω , Y_ω denotes the dissipation of ω , D_ω represents the cross-diffusion term, and S_ω represents the source term. The third equation represents the transport equation for the intermittency γ , where the transition source terms are defined by P_{γ_1} and E_{γ_1} , the relaminarization source terms are defined by P_{γ_2} and E_{γ_2} , and σ_γ is a model constant. The fourth equation represents the transport equation for the transition momentum thickness Reynolds number $\widetilde{Re_{\theta_t}}$, where the source term is defined by P_{θ_t} and σ_{θ_t} is a model constant.

A velocity inlet boundary condition was selected, where both the magnitude of the flow speed and the direction components were adjusted according to the case. The inlet turbulence properties were prescribed with a turbulence intensity of 1% and a turbulent viscosity ratio of 1, following established best practices as reported in [12]. Simulations were conducted for all four configurations, considering both longitudinal and lateral flow

conditions. A symmetry boundary condition was additionally applied in the longitudinal cases to reduce the computational effort.

The coupled pressure–velocity scheme was employed due to its robust single-phase implementation for steady state flows. This scheme simultaneously solves the momentum and pressure-based equations through an implicit discretization of the pressure gradient terms in the momentum equation and the face mass fluxes. For spatial discretization, the least-squares cell-based method was adopted, as it is well-suited for irregular, skewed, distorted unstructured meshes. It provides superior accuracy compared to the standard cell-based gradient method while remaining less computationally expensive than the node-based gradient method. Flow variables such as turbulent kinetic energy, specific dissipation rate, intermittency, and momentum-thickness Reynolds number were discretized using second-order upwind schemes.

Convergence of the flow solution was assessed through monitoring residual histories as well as the evolution of axial and lateral aerodynamic forces and moments. Convergence was considered achieved when all monitored quantities either reached a plateau or exhibited quasi-steady oscillatory behavior. For instance, the mean values over the final 500 iterations stabilized at approximately 10^{-3} for the normal force and 10^{-4} for the axial force.

2.3. Performance Modeling

2.3.1. Modeling of Propulsion System

The propulsion system modeling involves selecting an appropriate electric motor and propeller to equip the analyzed drone configurations.

Assuming an average propeller efficiency of 0.7 in cruise conditions [7] and an average BLDC motor efficiency of 0.9 [15], the overall propulsion system efficiency is estimated to be approximately 0.63. The motor is therefore sized based on the required power while accounting for this combined efficiency ($\eta = 0.63$), a cruise speed of 30 m/s, and a flight altitude of 1000 m. As presented in [16], applying relation (8) under these flight conditions yields the following result:

$$\eta P = TV, \quad (8)$$

$$P = \frac{TV}{\eta}, \quad (9)$$

assuming steady level flight, where the required thrust (T) equals the aerodynamic drag of the model, relation (9) becomes

$$P = \frac{\frac{1}{2}\rho V^3 S C_D}{\eta}, \quad (10)$$

for an estimated drag coefficient in cruise (C_D) of 0.03, the required motor power in cruise condition (P) is calculated using relation (10) to be approximately 856 W. However, to ensure sufficient power margin for maneuvers (climb, acceleration), this value is multiplied by a factor of four, resulting in a maximum continuous motor power (P_{max}) of approximately 3500 W. The motor sizing algorithm described above provides a robust and reliable method for estimating the power requirements of an aerospace vehicle. The maximum continuous power estimated above is in good agreement with the power-to-weight ratios reported for general aviation aircraft in [7].

The two fundamental geometric parameters of a propeller are its diameter, which determines the volume of air displaced, and its pitch, which defines the distance the

propeller would theoretically travel forward in one rotation. According to [17], the diameter (D_p) and pitch (p) of a propeller are estimated using the following methods:

$$D_p = \sqrt{\frac{a^2}{\pi^2 n^2} (M_{tip}^2 - M^2)}, \quad (11)$$

$$p = \frac{V}{n}, \quad (12)$$

where a is the speed of sound, n is the angular velocity, M_{tip} is the Mach number at the tip of blade and M is the Mach number of flight.

Using relations (11) and (12), with an angular velocity of 100 rps and a blade tip Mach number of 0.5, the calculated propeller diameter is 0.53 m, and the pitch is 0.3 m. These dimensions correspond to approximately a 22" × 12" propeller. To ensure an accurate analysis, the propeller characteristics were selected from the APC website [18], choosing a propeller with these specifications—namely, the APC 22 × 12E model. The characteristics of this propeller are presented in Figure 7.

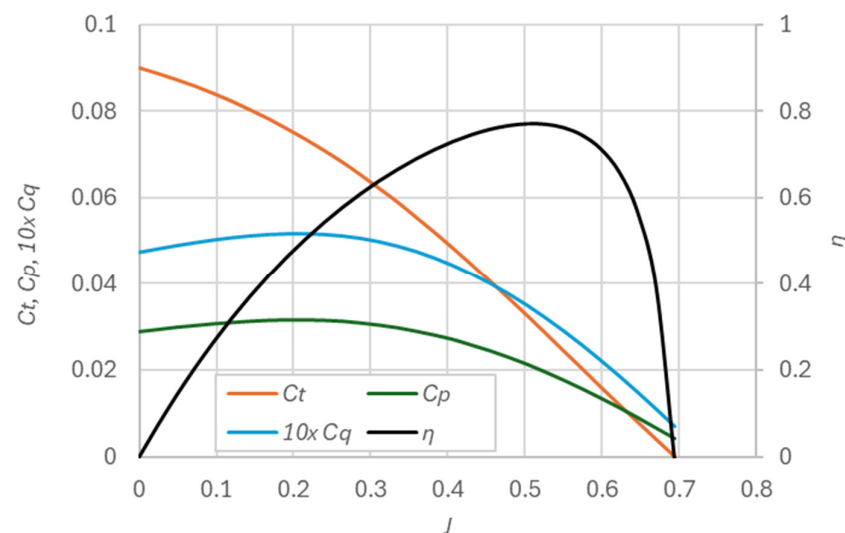


Figure 7. Characteristics of APC 22 × 12E propeller.

Figure 7 presents the variation in main characteristics: C_t (thrust coefficient), C_p (power coefficient), C_q (torque coefficient) and η (propeller efficiency), with advance ratio (J). According to [17], these parameters are defined as follows:

$$C_t = \frac{T}{\rho n^2 D_p^4}, \quad (13)$$

$$C_p = \frac{P}{\rho n^3 D_p^5}, \quad (14)$$

$$C_q = \frac{Q}{\rho n^2 D_p^5}, \quad (15)$$

$$J = \frac{V}{n D_p}, \quad (16)$$

$$\eta = J \frac{C_t}{C_p}, \quad (17)$$

The propeller characteristics available on [18] are obtained analyzing the propeller geometry with a specialized computational method that is based on Vortex theory.

2.3.2. Take-Off and Climb

Stall Speed

Stall speed is the minimum steady flight speed at which the aircraft can generate enough lift to balance its weight. Below this speed, the airflow over the wings becomes insufficient to maintain lift, leading to a stall—where the wing loses its ability to sustain controlled flight [16].

$$V_{stall} = \sqrt{\frac{2W}{\rho S C_{L_{max}}}}, \quad (18)$$

where W represents the weight of the vehicle, ρ represents the air density, S represents the wing surface, and $C_{L_{max}}$ represents the maximum lift coefficient.

Launch Length

The minimum required rail launch length is the distance necessary to accelerate the UAV from rest to a velocity equal to or greater than the safe launch speed, typically 1.2 to 1.3 times the stall speed under the available acceleration provided by the launch system.

$$L_{launch} = \frac{V_{launch}^2}{2a} = \frac{(1.3V_{stall})^2}{2 \frac{F_{launch}}{m}}, \quad (19)$$

where F_{launch} represents the launch force of the rail launch, and m represents the mass of the vehicle.

Rate of Climb

The rate of climb is defined as the vertical component of a drone's velocity during an ascent. It quantifies how quickly an aircraft gains altitude and is a key indicator of its climbing performance. The rate of climb depends on the available excess power and is influenced by factors such as aircraft weight, altitude, airspeed, and atmospheric conditions [16].

$$V_V = V_{\infty} \sin \gamma = \frac{P_{AV} - P_{REQ}}{W}, \quad (20)$$

where γ represents path angle, P_{AV} represents the available power, and P_{REQ} represents the required power.

The best rate of climb corresponds to the maximum vertical velocity, $V_{V_{opt}}$. An important related performance parameter is the best rate-of-climb airspeed, V_Y , which is the airspeed at which this maximum rate of climb is achieved.

Angle of Climb

The angle of climb is defined as the angle between the horizontal ground and the flight path of an aircraft during a steady ascent. It represents the steepness of the climb and is determined by the ratio between the vertical and horizontal components of the drone's velocity. This angle depends primarily on the available excess thrust over drag and is a key parameter in assessing the vehicle's ability to clear obstacles shortly after takeoff or during ascent [16].

$$\gamma = \sin^{-1} \left(\frac{P_{AV} - P_{REQ}}{V_{\infty} W} \right), \quad (21)$$

The best angle of climb is defined by the maximum climb angle, γ_{opt} , that the vehicle can achieve. A key related performance parameter is the best angle-of-climb airspeed, V_X which is the airspeed at which this maximum climb angle occurs.

2.3.3. Cruise

Range

Range is the maximum distance an aircraft can travel in level flight under specified conditions before depleting its available energy or fuel. Considering that the studied vehicle is an electric powered drone, the relation for range estimation, as presented in [16], is

$$R = \frac{\eta_{tot} E_{batt}}{W} \cdot \frac{C_L}{C_D}, \quad (22)$$

where η_{tot} is the total efficiency of propulsion system, and E_{batt} represents the total available energy of battery.

The range can be estimated for each airspeed, allowing the identification of the maximum range and the corresponding best-range airspeed, both of which are key performance parameters.

Endurance

Endurance is the maximum amount of time an aircraft can remain in sustained flight under specified conditions before exhausting its available energy or fuel. Considering that the studied vehicle is an electric powered drone, the relation for endurance estimation, as presented in [16], is

$$E = \frac{\eta_{tot} E_{batt}}{V_{\infty} W} \cdot \frac{C_L}{C_D}, \quad (23)$$

Endurance can be evaluated at different airspeeds, making it possible to determine the maximum endurance and the corresponding best-endurance airspeed—both of which are important performance metrics.

Max Speed

Maximum speed of an aircraft is defined as the highest steady-level flight speed at which the power available from the propulsion system equals the power required to overcome aerodynamic drag. Beyond this point, the aircraft cannot accelerate in level flight because there is no excess power to compensate for the increased drag.

$$V_{max} = \sqrt[3]{\frac{P_{AV}}{\frac{1}{2} \rho S C_D}}, \quad (24)$$

where P_{AV} and C_D are the available power, respectively, the drag coefficient at V_{max} airspeed. Indeed, this expression defines V_{max} implicitly, and its solution can be obtained either through an iterative numerical method or by graphically identifying the point where the power available curve (P_{AV} vs. V) intersects the power required curve (P_{REQ} vs. V).

Turn rate

Turn rate performance is a key measure of an aircraft's maneuverability, representing its ability to change heading over time. It is commonly quantified by the turn rate, turn radius, and time to complete a turn over at a specified angle. The dynamics of turning are governed by the balance between centripetal force and lift, and depend on the load factor, which is the ratio of lift to weight.

In bank-to-turn (BTT) maneuver, turning is achieved by rolling the aircraft to a bank angle and using the horizontal component of lift to generate centripetal force. This is the most common and efficient method for conventional fixed-wing UAVs and aircraft.

Considering the general equations of motion for cruise, as presented in [16]:

$$\begin{aligned} VD &= P_{AV} \\ L \sin \phi - \frac{W}{g} \frac{V^2}{R_{turn}} &= 0, \\ L \cos \phi - W &= 0 \end{aligned} \quad (25)$$

the following expressions are derived:

$$\begin{aligned} n &= \frac{1}{\cos \phi} \\ R_{turn} &= \frac{V^2}{ng \sin \phi} \\ t_\psi &= \frac{R_{turn}}{V} \psi \frac{\pi}{180} \\ \dot{\psi} &= \frac{V}{R_{turn}} \end{aligned} \quad (26)$$

where ϕ is the bank angle, n is the load factor, ψ is the turn angle and g is the earth gravity constant.

In contrast, skid-to-turn (STT) is used primarily in missiles or symmetric configurations like cruciform or X-wing drones, where the vehicle yaws using control surfaces or thrust vectoring without rolling. STT allows for faster directional changes without altering orientation but may induce higher drag and energy loss.

Considering the general equations of motion for cruise:

$$\begin{aligned} \frac{1}{2} \rho V^2 S (C_{Y_\beta} \beta + C_{Y_{\delta_r}} \delta_r + C_{Y_{\delta_a}} \delta_a) - \frac{W}{g} \frac{V^2}{R_{turn}} &= 0 \\ \frac{1}{2} \rho V^2 S b (C_{n_\beta} \beta + C_{n_{\delta_r}} \delta_r + C_{n_{\delta_a}} \delta_a) &= 0 \\ \frac{1}{2} \rho V^2 S b (C_{l_\beta} \beta + C_{l_{\delta_r}} \delta_r + C_{l_{\delta_a}} \delta_a) &= 0 \end{aligned} \quad (27)$$

results that:

$$\begin{aligned} \delta_r &= \beta \frac{C_{n_{\delta_a}} C_{l_\beta} - C_{l_{\delta_a}} C_{n_\beta}}{C_{n_{\delta_a}} C_{l_{\delta_r}} - C_{l_{\delta_a}} C_{n_{\delta_r}}} = \beta \gamma_{\delta_r} \\ \delta_a &= \beta \frac{C_{n_{\delta_r}} C_{l_\beta} - C_{l_{\delta_r}} C_{n_\beta}}{C_{n_{\delta_r}} C_{l_{\delta_a}} - C_{l_{\delta_r}} C_{n_{\delta_a}}} = \beta \gamma_{\delta_a} \\ \beta &= \frac{2m}{R_{turn} \rho S (C_{Y_\beta} + \gamma_{\delta_r} C_{Y_{\delta_r}} + \gamma_{\delta_a} C_{Y_{\delta_a}})} \end{aligned} \quad (28)$$

and can be obtained:

$$\begin{aligned} n &= \frac{V^2}{g R_{turn} \frac{2m}{\rho S (C_{Y_\beta} + \gamma_{\delta_r} C_{Y_{\delta_r}} + \gamma_{\delta_a} C_{Y_{\delta_a}})}} \\ R_{turn} &= \frac{V^2}{g \frac{2m}{\rho S (C_{Y_\beta} + \gamma_{\delta_r} C_{Y_{\delta_r}} + \gamma_{\delta_a} C_{Y_{\delta_a}})}} \\ t_\psi &= \frac{R_{turn}}{V} \psi \frac{\pi}{180} \\ \dot{\psi} &= \frac{V}{R_{turn}} \end{aligned} \quad (29)$$

where C_{Y_β} , C_{n_β} , C_{l_β} , $C_{Y_{\delta_r}}$, $C_{n_{\delta_r}}$, $C_{l_{\delta_r}}$, $C_{Y_{\delta_a}}$, $C_{n_{\delta_a}}$, $C_{l_{\delta_a}}$ are the aerodynamic derivatives of side force coefficient (Y), yaw moment coefficient (n) and roll moment coefficient (l) with side-slip angle (β), rudder deflection (δ_r) and aileron deflection (δ_a).

Evaluating turn performance under both BTT and STT modes is crucial for assessing the agility and responsiveness of loitering munitions or tactical UAVs, particularly in scenarios requiring quick target acquisition or evasive maneuvers.

2.3.4. Descent

Dive Speed

In the context of loitering munitions, the dive speed is defined as the velocity attained at ground impact when the vehicle descends from a given altitude along a specified flight path. Unlike the structural design dive speed used in conventional aeronautics, this definition refers to the operational impact condition, where the final velocity results from the balance between gravitational acceleration, aerodynamic drag, and flight path geometry. The dive speed is therefore a function of the initial altitude, the chosen dive angle, and the aerodynamic properties of the vehicle. It represents a critical performance parameter, as it directly influences the kinetic energy available at impact and the terminal effectiveness of the munition. To evaluate this performance, the equations of motion for symmetric, unpowered flight are considered, as illustrated in Figure 8.

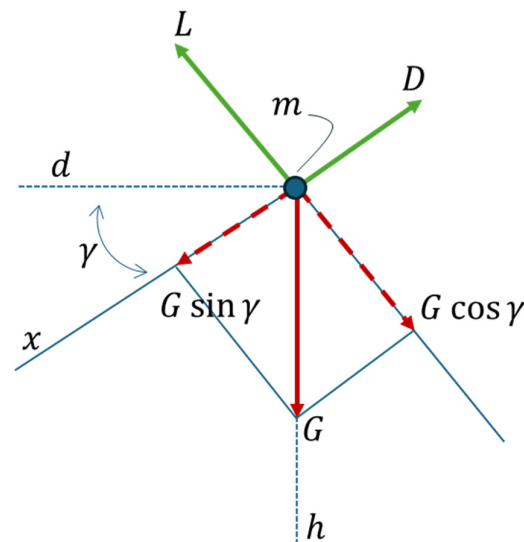


Figure 8. Forces acting on the vehicle in dive.

Based on Figure 8, the motion can be described in (30) and (31):

$$\begin{aligned} x &= \frac{H_0 - h}{\sin \gamma} \\ \dot{x} &= V \end{aligned} \quad (30)$$

$$\begin{aligned} \frac{1}{2} \rho \dot{x}^2 S C_L &= mg \cos \gamma \\ m \ddot{x} &= mg \sin \gamma - \frac{1}{2} \rho \dot{x}^2 S C_D \end{aligned} \quad (31)$$

where h represents the altitude of the vehicle, H_0 represents the initial altitude, γ represents the flight path angle, and x represents the distance along Ox while its first- and second-time derivatives represent the velocity and acceleration along Ox axis.

The dynamic equation is numerically integrated using a Runge–Kutta method [19], which provides a robust and accurate approach for solving ordinary differential equations (ODEs). In this study, a fourth-order Runge–Kutta scheme is employed due to its balance between computational efficiency and solution accuracy, allowing for reliable estimation of the vehicle's trajectory, dive speed, and time to impact.

When the vehicle reaches an altitude of 0 m, both the time to impact and the corresponding dive speed can be determined. These parameters are crucial for assessing how quickly a target can be intercepted from a given altitude.

3. Results

3.1. Aerodynamic Results

3.1.1. Force and Moments Coefficients Variations

The aerodynamic analysis comprises an extensive set of simulations for each configuration, structured into three parts: variation in aerodynamic coefficients with angle of incidence, involving at least 15 simulations for each configuration, depending on the stall angle; variation in aerodynamic coefficients with sideslip angle, performed through 6 simulations; and variation in aerodynamic coefficients with control surface deflections, also carried out through 6 simulations.

The simulations are conducted under constant conditions, with an airspeed of 30 m/s at an altitude of 1000 m using standard atmosphere parameters. This operating regime is representative of medium-range loitering munitions, as presented in [3].

The analysis of the variation in aerodynamic coefficients with angle of incidence was carried out considering only half of the domain, due to the symmetry with respect

to the xOz plane, in order to evaluate the variation in lift coefficient, drag coefficient and pitch moment coefficient with angle of attack. Simulations were conducted from -7° up to the post-stall angle, which varies depending on the configuration. Specifically, the $\Gamma = 0^\circ$ configuration includes 27 simulations for angles of attack between -7° and -17.5° ; the $\Gamma = 15^\circ$ configuration includes 15 simulations between -7° and -14° ; the $\Gamma = 30^\circ$ configuration includes 17 simulations between -7° and -15° ; and the $\Gamma = 45^\circ$ configuration includes 22 simulations between -7° and -17.5° .

The analysis of the variation in aerodynamic coefficients with sideslip angle was performed over the full domain, considering sideslip angles between 0° and 10° , in order to evaluate the slopes of the side force coefficient, roll moment coefficient, and yaw moment coefficient with respect to sideslip angle. In addition, the analysis of the variation in aerodynamic coefficients with control surface deflection was carried out for deflections of 5° and 10° for each control surface, allowing the determination of the corresponding aerodynamic derivatives.

The aerodynamic coefficients provide the basis for estimating model performance, allowing a direct comparison of how dihedral angle variations influence the most relevant performances of X-wing UAVs.

Figure 9 presents the variation in aerodynamic coefficients for each configuration, as follows: (a) lift coefficient (C_L) vs. angle of attack (AoA), (b) drag coefficient (C_D) vs. AoA, (c) pitching moment coefficient (C_m) vs. AoA, (d) side force coefficient (C_Y) vs. sideslip angle (SSA), (e) rolling moment coefficient (C_l) vs. SSA, (f) yawing moment coefficient (C_n) vs. SSA, (g) C_L vs. C_D , and (h) lift-to-drag ratio (C_L/C_D) vs. AoA.

Figure 9a illustrates that the dihedral angle has a strong influence on the lift coefficient, affecting both the lift curve slope and the stall angle. At zero incidence, the lift coefficients are nearly identical. The lift slope for $\Gamma = 30^\circ$ is higher than for $\Gamma = 0^\circ$, but lower than for $\Gamma = 15^\circ$, while $\Gamma = 45^\circ$ shows the smallest slope overall. In terms of stall, the configuration $\Gamma = 45^\circ$ reaches the highest stall angle but with the lowest maximum lift coefficient, whereas $\Gamma = 15^\circ$ achieves the highest maximum lift coefficient at the lowest stall angle. Overall, the lift slope tends to decrease with increasing dihedral angle, while the stall angle rises, resulting in lower maximum lift coefficients. The exception is the $\Gamma = 0^\circ$ case, where strong interference between the upper and lower wings reduces the lift slope while increasing the stall angle.

Figure 9b shows that the dihedral angle consistently affects the drag coefficient. At AoA = 0° , the drag coefficient for $\Gamma = 0^\circ$ is higher than for the other configurations, which exhibit similar values. This offset is attributed to strong interference between the upper and lower wings. Regarding the shape of the curves, the $\Gamma = 0^\circ$ and $\Gamma = 45^\circ$ configurations display similar trends with a nearly constant offset, while the $\Gamma = 15^\circ$ and $\Gamma = 30^\circ$ curves have a narrower aspect, indicating higher induced drag associated with the increased lift slope observed in Figure 9a.

Figure 9c shows that the dihedral angle has a minimal effect on the pitching moment coefficient, with no significant differences observed between configurations. The curve slopes and increments are quite similar, except near stall angles, where slight variations in behavior can be noticed.

Figure 9d also illustrates that the dihedral angle has a significant impact on the side force coefficient. The variation in side force coefficient with sideslip angle is linear within the analyzed range. Furthermore, the slopes differ noticeably between configurations, as expected, since the projected side area of the vehicle increases with dihedral angle, resulting in higher side forces for the same sideslip angle.

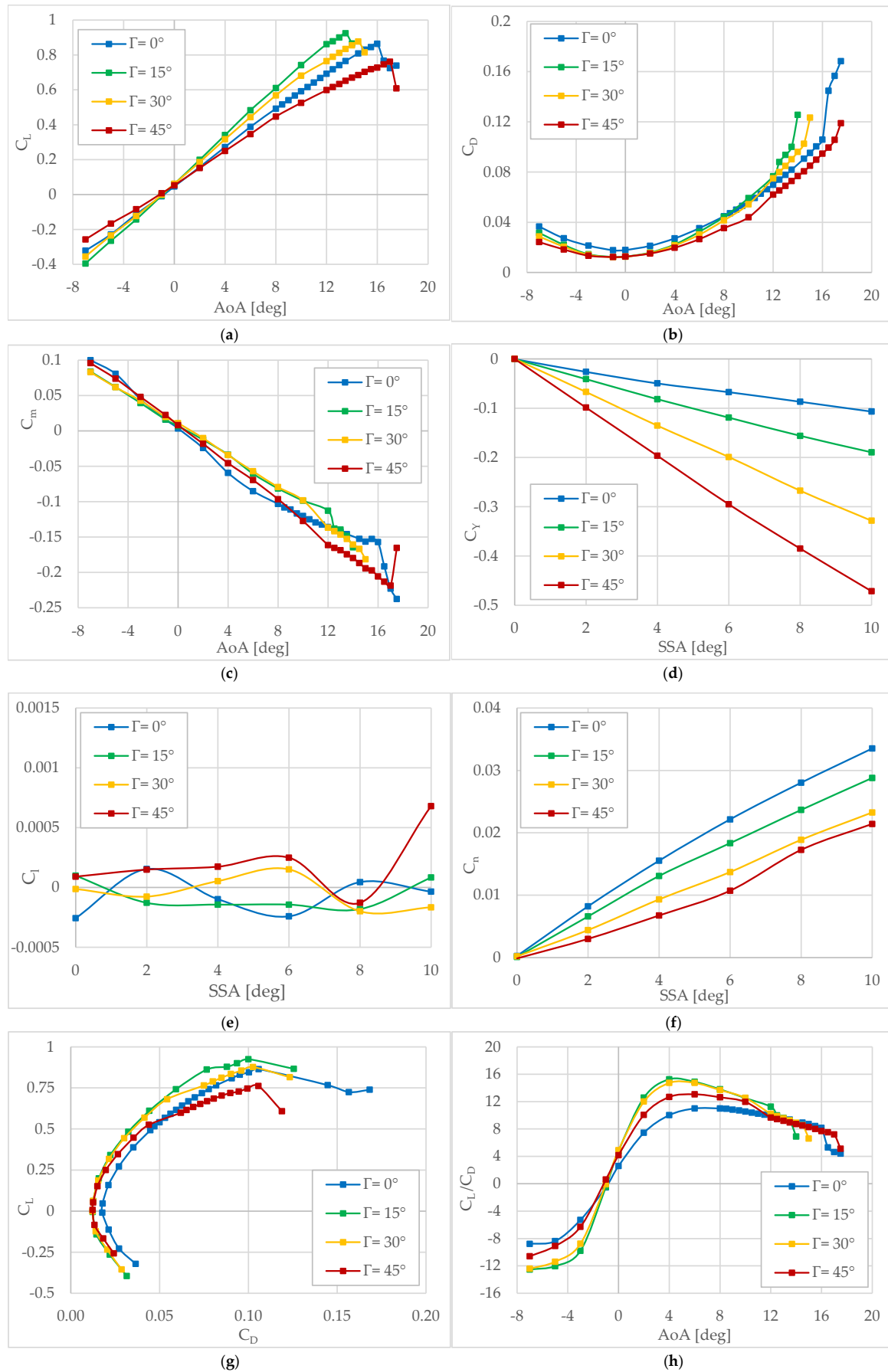


Figure 9. Variation in aerodynamic coefficients: (a) C_L vs. AoA, (b) C_D vs. AoA, (c) C_m vs. AoA, (d) C_Y vs. SSA, (e) C_l vs. SSA, (f) C_n vs. SSA, (g) C_L vs. C_D , and (h) C_L/C_D vs. AoA.

Figure 9e shows that the dihedral angle has no noticeable influence on the roll moment coefficient, which is expected given the symmetry of the wing positions relative to the XoY plane. The roll moment coefficient remains essentially constant across the sideslip angle range for all configurations.

Figure 9f shows that the dihedral angle has a significant effect on the yaw moment coefficient. The variation with sideslip angle remains linear and positive, while increasing the dihedral angle reduces the slope of this variation.

Figure 9g shows the drag polar for each configuration. It is observed that the polars become narrower as the dihedral angle increases, except for the $\Gamma = 0^\circ$ case, which presents a shape similar to that of $\Gamma = 15^\circ$, but shifted by a constant offset along the drag axis.

Figure 9h illustrates the variation in the lift-to-drag ratio with angle of attack. The dihedral angle has a pronounced influence, with the highest lift-to-drag ratio achieved at $\Gamma = 15^\circ$ and the lowest at $\Gamma = 0^\circ$. The $\Gamma = 30^\circ$ configuration yields value close to those of $\Gamma = 15^\circ$, while $\Gamma = 45^\circ$ provides lower values than $\Gamma = 15^\circ$ but still higher than $\Gamma = 0^\circ$. For all configurations, the best lift-to-drag ratio occurs at an angle of attack of approximately 5° .

Table 1 summarizes the aerodynamic results, providing a comparison between the analyzed configurations. The aerodynamic coefficients shown above were calculated through regression analysis of the CFD results, as described in [16].

Table 1. Aerodynamic coefficients of the analyzed drones.

	$\Gamma = 0^\circ$	$\Gamma = 15^\circ$	$\Gamma = 30^\circ$	$\Gamma = 45^\circ$
C_{L_0}	0.0464	0.0605	0.0616	0.0527
C_{L_α} [rad ⁻¹]	2.7957	3.6778	3.2635	2.3945
$C_{L_{stall}}$	0.8642	0.9243	0.8772	0.7613
α_0 [deg]	−1	−1	−1	−1
α_{stall} [deg]	16	13.5	14.5	17
$C_{D_{min}}$	0.0177	0.0124	0.0122	0.0123
$C_{L_{min}}$	−0.0095	−0.0058	−0.0011	0.0074
k	0.1335	0.0952	0.1115	0.1530
C_{m_α} [rad ⁻¹]	−0.6872	−0.6447	−0.6790	−0.7421
C_{m_0}	0.0035	0.0069	0.0110	0.0081
C_{Y_β} [rad ⁻¹]	−0.5990	−1.0884	−1.8878	−2.7127
C_{l_β} [rad ⁻¹]	0.0005	−0.0002	−0.0009	0.0018
C_{n_β} [rad ⁻¹]	0.1902	0.1636	0.1335	0.1265
$C_L/C_{D_{max}}$	10.9990	15.2611	14.7293	13.0670
$C_{Y_{\delta_r}}$ [rad ⁻¹]	0.6021	0.5965	0.6195	0.6429
$C_{l_{\delta_r}}$ [rad ⁻¹]	0.0005	0.0019	0.0002	−0.0005
$C_{n_{\delta_r}}$ [rad ⁻¹]	−0.2234	−0.2192	−0.2182	−0.2187
$C_{Y_{\delta_a}}$ [rad ⁻¹]	−0.0009	0.0080	0.0164	−0.0150
$C_{l_{\delta_a}}$ [rad ⁻¹]	−0.1962	−0.2708	−0.2882	−0.2985
$C_{n_{\delta_a}}$ [rad ⁻¹]	0.0009	0.0014	0.0014	0.0019
$C_{L_{\delta_e}}$ [rad ⁻¹]	0.6495	0.6509	0.6486	0.6307
$C_{m_{\delta_e}}$ [rad ⁻¹]	−1.4816	−1.4534	−1.4483	−1.4473

The coefficients listed in Table 1 represent the aerodynamic derivatives of lift, C_L , pitch moment, C_m , side force, C_Y , roll moment, C_l , and yaw moment, C_n with respect to incidence, α , sideslip angle, β , and control surface deflections (elevator, δ_e , rudder, δ_r , and aileron, δ_a). Specifically, C_{L_0} and C_{m_0} denote the lift and pitch moment intercepts, C_{L_α} is the lift curve slope, and $C_{L_{stall}}$ is the lift coefficient at stall. The parameters α_0 and α_{stall} represent the zero-lift angle of attack and stall angle, respectively. $C_{D_{min}}$ is the minimum

drag coefficient, with the corresponding lift coefficient $C_{L_{min}}$, while k is the drag polar factor. Finally, $C_L/C_{D_{max}}$ indicates the maximum lift-to-drag ratio.

Table 1 presents that the following coefficients are slightly constant along the dihedral angles analyzed: $C_{L_0} \sim 0.05$, $\alpha_0 \sim -1^\circ$, $C_{L_{min}} \sim 0.005$, $C_{m_0} \sim 0.007$, $C_{m_\alpha} \sim -0.7 \text{ rad}^{-1}$, $C_{l_\beta} \sim 0 \text{ rad}^{-1}$, $C_{Y_{\delta_r}} \sim 0.6 \text{ rad}^{-1}$, $C_{l_{\delta_r}} \sim 0 \text{ rad}^{-1}$, $C_{n_{\delta_r}} \sim -0.22 \text{ rad}^{-1}$, $C_{n_{\delta_a}} \sim 0 \text{ rad}^{-1}$, $C_{L_{\delta_e}} \sim 0.64 \text{ rad}^{-1}$, $C_{m_{\delta_e}} \sim -1.45 \text{ rad}^{-1}$.

The other coefficients, such as C_{L_α} , $C_{L_{stall}}$, α_{stall} , k , C_{Y_β} , C_{n_β} , $C_L/C_{D_{max}}$, $C_{Y_{\delta_a}}$, and $C_{l_{\delta_a}}$, present a nonlinear dependence on the dihedral angle, primarily due to upper wing–lower wing interference at 0° dihedral. In general, the coefficients either start at their lowest values at 0° , increase up to 15° , and then decrease as the dihedral angle reaches 45° ; or conversely, they begin at their highest values at 0° , decrease up to 15° , and then increase again toward 45° .

3.1.2. Distribution of Pressure Coefficient

Figure 10 presents the distribution of pressure coefficient on the surface of the configurations at $\text{AoA} = 0^\circ$. This analysis aims to highlight the aerodynamic interference occurring between the wings at small dihedral angles, as well as the interaction between the wings and the empennage.

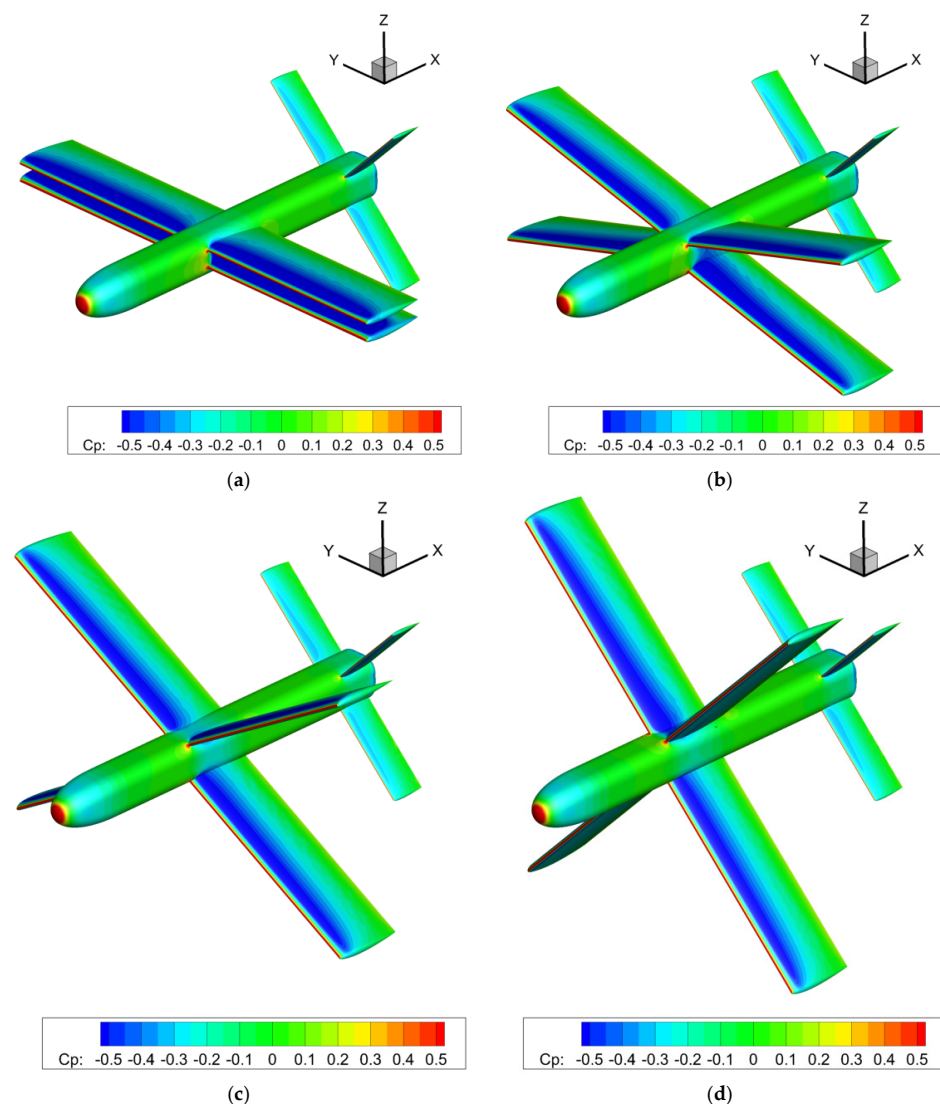


Figure 10. Distribution of pressure coefficient on the model surface: (a) $\Gamma = 0^\circ$, (b) $\Gamma = 15^\circ$, (c) $\Gamma = 30^\circ$, and (d) $\Gamma = 45^\circ$.

The pressure coefficient distributions across the four configurations at $\text{AoA} = 0^\circ$ reveal that, for the zero-dihedral case, an accelerated flow region develops on the upper surface of the lower wing as a consequence of the channel effect formed between the upper and lower wings. However, this flow acceleration reduces the lift contribution of the upper wing, since it diminishes the pressure difference between its upper and lower surfaces. Although less pronounced, this effect is also observed in the 15° dihedral configuration. In contrast, the phenomenon is absent in the other two configurations, where the interaction between the lower and upper wings is minimal. Additionally, at the root of the lower wing, the acceleration region is still present, exhibiting an inverse relationship with the dihedral angle, thus the acceleration decreases as the channel between the wings increases. Furthermore, the dihedral angle appears to exert little to no influence on the empennages. The pressure distribution remains nearly symmetric between the upper and lower surfaces, primarily as a result of the local incidence induced by the wing's wake. Nevertheless, the effect of the dihedral angle becomes evident near the empennage tip, where the magnitude of the flow acceleration decreases with increasing dihedral angle. This trend indicates that, as the dihedral angle grows, the wing's wake progressively interacts with the empennage, thereby altering the local flow characteristics.

3.2. Performance Results

3.2.1. Take-Off and Climb Phases

Table 2 presents the stall speeds at three altitudes (0 km, 0.5 km, and 1 km) for each configuration, along with the required catapult launch length under take-off conditions.

Table 2. Stall Speed and Launch Length.

		$\Gamma = 0^\circ$	$\Gamma = 15^\circ$	$\Gamma = 30^\circ$	$\Gamma = 45^\circ$
V_{Stall} [m/s]	$H = 0$ km	21.51	20.80	21.35	22.92
	$H = 0.5$ km	22.04	21.31	21.87	23.48
	$H = 1$ km	22.59	21.84	22.42	24.06
L_{Launch} [m/s]		3.91	3.65	3.85	4.43

The stall speed is lowest for the $\Gamma = 15^\circ$ configuration, followed by $\Gamma = 30^\circ$, $\Gamma = 0^\circ$ and $\Gamma = 45^\circ$, consistent with the maximum lift coefficients shown in Figure 9a. Accordingly, at 0 km altitude, the required catapult launch length is also minimum for $\Gamma = 15^\circ$, with the same order of increase across the other configurations ($\Gamma = 30^\circ$, $\Gamma = 0^\circ$, and $\Gamma = 45^\circ$).

To estimate the rate of climb (ROC), angle of climb (AOC), range, and endurance, the difference between available power (P_{av}) and required power (P_{req}) as a function of airspeed is needed. Figure 11 shows the available and required power for each configuration, along with the stall speed at 500 m altitude.

The available power (P_{av}) is defined as the maximum continuous motor power (3500 W) multiplied by the combined motor–propeller efficiency shown in Figure 7, which varies with airspeed.

The intersections between the available power curve (black) and the required power curves (colored) define the flight speed limits. The right-side intersections indicate the minimum speeds, ranging from 9 to 13 m/s, while the left-side intersections correspond to the maximum speeds, which fall within a narrower range of 37 to 38 m/s. Since the stall speeds range between 22 and 24 m/s, flight regimes to the left of these speeds are not achievable, as they would require lift coefficients exceeding the maximum value.

Figures 12 and 13 illustrate the variations in rate of climb (ROC) and angle of climb (AOC) with airspeed for each configuration, highlighting the points of best performance (black dots) and the stall speeds (dashed vertical lines). The best ROC is achieved by the

$\Gamma = 15^\circ$ configuration, followed by $\Gamma = 30^\circ$, $\Gamma = 45^\circ$, and $\Gamma = 0^\circ$, with the corresponding velocities ranging between 27 and 28 m/s, providing a safe margin above stall speed. In contrast, the best AOC is obtained for $\Gamma = 15^\circ$, followed by $\Gamma = 30^\circ$, $\Gamma = 0^\circ$, and $\Gamma = 45^\circ$. However, the associated velocities lie very close to, or even below, the stall speeds, representing unsafe flight regimes due to their proximity to stall.

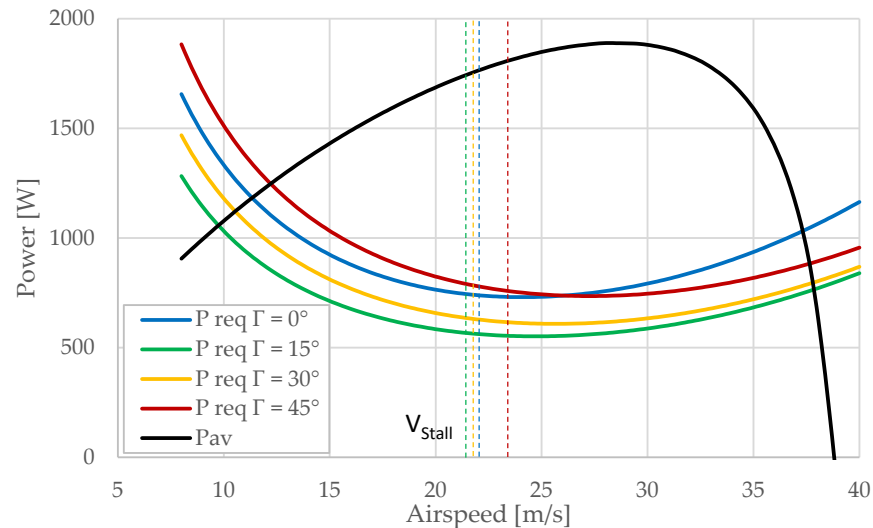


Figure 11. Variation in required and available power with airspeed.

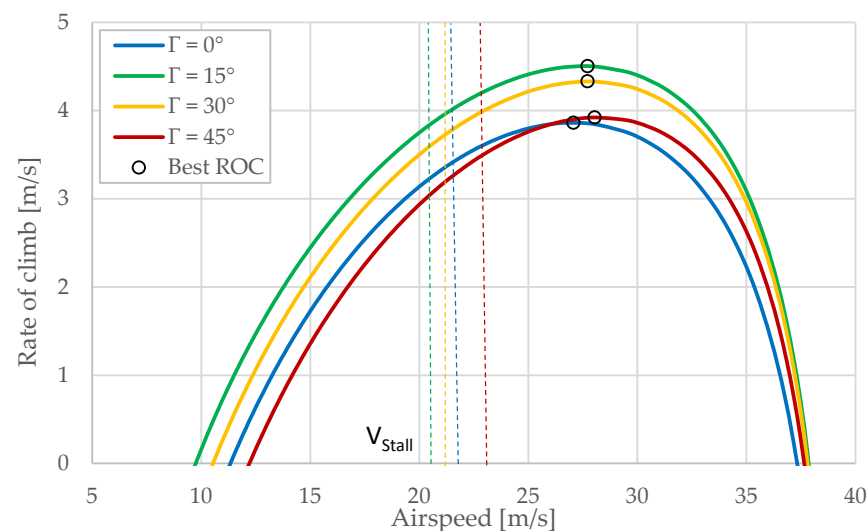


Figure 12. Rate of climb variation with airspeed.

Table 3 summarizes the best ROC and AOC values along with their associated velocities. The best ROC ranges between 3.5 and 4.5 m/s, while the best AOC lies between 9° and 11° . A significant difference is observed between the configurations with $\Gamma = 15^\circ$, $\Gamma = 30^\circ$, and $\Gamma = 0^\circ$ while the difference between $\Gamma = 0^\circ$ and $\Gamma = 45^\circ$ remains very small.

Table 3. Best ROC and AOC.

	$\Gamma = 0^\circ$	$\Gamma = 15^\circ$	$\Gamma = 30^\circ$	$\Gamma = 45^\circ$
Best ROC [m/s]	3.86	4.50	4.33	3.92
$V_{\text{Best ROC}}$ [m/s]	27.07	27.72	27.72	28.04
Best AOC [deg]	9.12	10.81	10.13	8.78
$V_{\text{Best AOC}}$ [m/s]	21.58	20.28	21.25	22.87

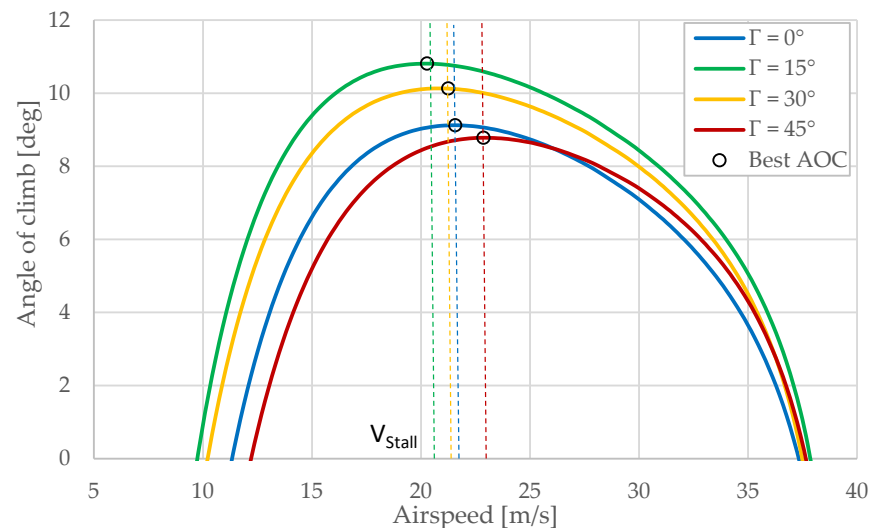


Figure 13. Angle of climb variation with airspeed.

3.2.2. Cruise Phase

Figures 14 and 15 present the variation in endurance and range of each configuration with airspeed for cruise phase ($H = 1$ km). The black dots indicate the best performance of each configuration, while the vertical dashed lines mark the stall limit at cruise altitude, allowing evaluation of the best performance margins.

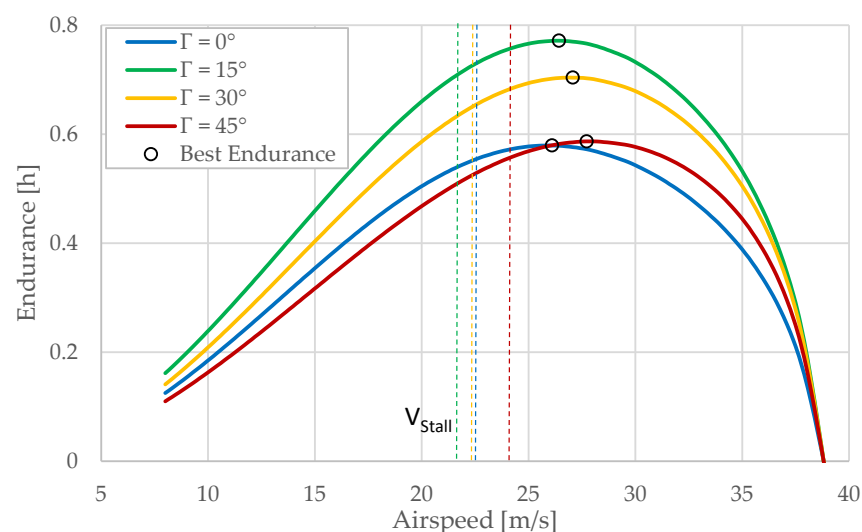


Figure 14. Endurance variation with airspeed.

Figures 14 and 15 present that the $\Gamma = 15^\circ$ configuration achieves the best performance in terms of both endurance and range, followed at a relevant distance by $\Gamma = 30^\circ$ and $\Gamma = 45^\circ$ configurations. The $\Gamma = 0^\circ$ configuration performs similarly to $\Gamma = 45^\circ$ for both cases but represents the lowest-performing option. The optimal velocities for maximum endurance range between 26 and 28 m/s, while for maximum range they lie between 30 and 32 m/s, as summarized in Table 4. Additionally, the best endurance values vary from 0.58 to 0.77 h, whereas the corresponding maximum ranges are between 58 and 79 km.

Moreover, Table 4 reports the maximum speeds, which are similar across all configurations at approximately 37.5 m/s.

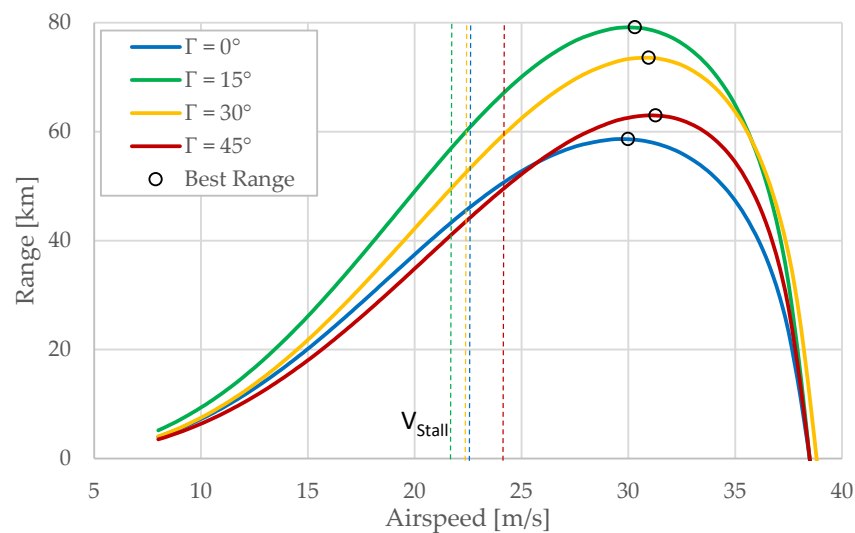


Figure 15. Range variation with airspeed.

Table 4. Best Endurance, Best Range and Maximum Speed.

	$\Gamma = 0^\circ$	$\Gamma = 15^\circ$	$\Gamma = 30^\circ$	$\Gamma = 45^\circ$
Best Endurance [h]	0.58	0.77	0.70	0.59
$V_{\text{Best Endurance}}$ [m/s]	26.10	26.42	27.07	27.72
Best Range [km]	58.63	79.12	73.55	62.97
$V_{\text{Best Range}}$ [m/s]	29.98	30.30	30.95	31.27
V_{Max} [m/s]	37.38	37.88	37.82	37.65

Figure 16 presents the variation in elevator deflection (δ_e) required with turn radius (R_{turn}) on primary axis, while on the second axis is represented the variation in load factor (n) with turn radius for bank-to-turn maneuver. In this case, the normal load factor ranges from 1, where the turn radius tends to infinity (i.e., straight flight), up to 2, which represents the structural limit. This case is analyzed at the cruise regime (i.e., $H = 1$ km, $V = 30$ m/s).

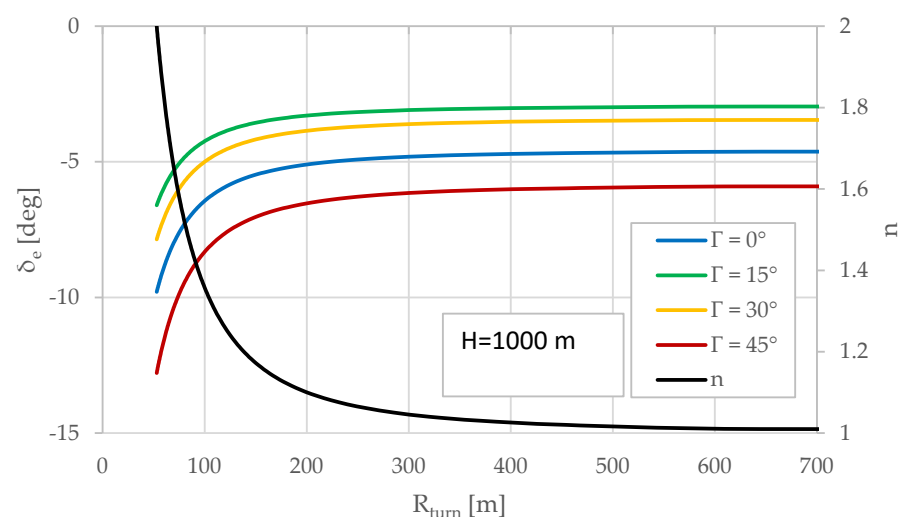


Figure 16. Variation in elevator deflection with turn radius in bank-to-turn maneuver.

The bank-to-turn case presents that the $\Gamma = 15^\circ$ configuration requires smaller elevator deflection, than the $\Gamma = 30^\circ$, $\Gamma = 0^\circ$ or $\Gamma = 45^\circ$ configurations for the same maneuver. Additionally, for tight turns (i.e., with a radius below 200 m) the required elevator deflection

decreases significantly, whereas for turn radii above 200 m, the deflection angle changes very little.

Figure 17 presents the variation in rudder deflection (δ_r) required with turn radius (R_{turn}) on primary axis, while on the second axis is represented the variation in load factor (n) with turn radius for skid-to-turn maneuver. In this case, the side load factor ranges from 0, where the turn radius tends to infinity (i.e., straight flight), up to 1.5, which represents the structural limit. This case is analyzed at the cruise regime (i.e., $H = 1$ km, $V = 30$ m/s).

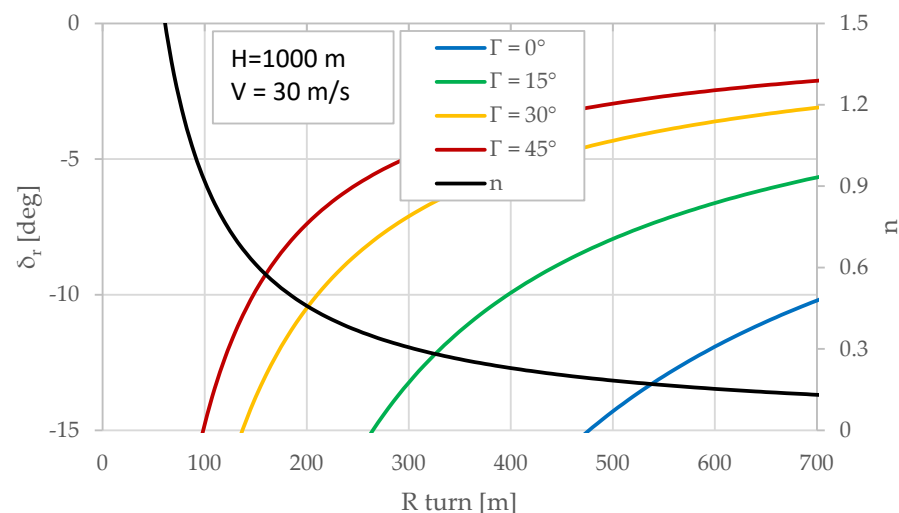


Figure 17. Variation in rudder deflection with turn radius in skid-to-turn maneuver.

In skid-to-turn case, the $\Gamma = 45^\circ$ configuration requires smaller rudder deflection, than the $\Gamma = 30^\circ$, $\Gamma = 15^\circ$ or $\Gamma = 0^\circ$ configurations for the same maneuver. The dihedral angle has a strong influence on skid-to-turn performance: when limiting rudder deflection to $\pm 15^\circ$ due to stall angle constrains, as presented in [20], the minimum achievable turn radius is obtained by $\Gamma = 45^\circ$ (i.e., ~ 100 m at $n \approx 0.9$), followed by $\Gamma = 30^\circ$ (i.e., ~ 140 m at $n \approx 0.65$), $\Gamma = 15^\circ$ (i.e., ~ 260 m at $n \approx 0.35$) and $\Gamma = 0^\circ$ (i.e., ~ 480 m at $n \approx 0.19$).

Figures 16 and 17 present a significant difference between bank-to-turn and skid-to-turn maneuvers in terms of load factor and control surface deflection. For a representative turn radius of 200 m, all configurations can execute the maneuver in bank-to-turn mode, requiring different levels of elevator deflection. In contrast, under skid-to-turn, only the $\Gamma = 45^\circ$ and $\Gamma = 30^\circ$ configurations can complete the maneuver within the $\pm 15^\circ$ rudder deflection limit, while the $\Gamma = 15^\circ$ and $\Gamma = 0^\circ$ configurations would require larger deflections. Furthermore, for the same maneuver ($R_{\text{turn}} = 200$ m), the load factor differs significantly: $n = 1.1$ for bank-to-turn and $n = 0.46$ for skid-to-turn, the latter implying reduced structural loads on the vehicle.

In the bank-to-turn maneuver, the vehicle rolls to tilt the lift vector and generate the lateral force required for turning. This increases the load factor above unity, imposes higher structural loads, and raises stall risk at low speeds. Moreover, because the drone must first spend time rolling to the commanded bank angle before initiating the turn, the overall response is slower, which reduces maneuvering efficiency in fast reaction scenarios. In the skid-to-turn maneuver, the turn is achieved by yawing with the ruddervators, generating side force while the wings remain nearly level. This approach keeps the normal load factor close to one, leading to lighter structural requirements and improved low-speed safety, while also providing an almost instantaneous turning response since no prior banking is needed. Owing to the X-wing's dihedral empennage, which enhances lateral force

generation, the skid-to-turn method is generally more effective for such configurations, although it may incur higher drag compared to the bank-to-turn method at higher speeds.

3.2.3. Descend Phase

Figures 18 and 19 present the variations in dive speed and dive time as functions of path angle for each configuration. The path angle is considered between 20° and 80° , which represents a relevant range for such vehicles. By integrating the dynamic equations of motion, it is found that the impact speed during the dive ranges from 60 to 100 m/s, while the time to descend from cruise altitude (1000 m) to the target at ground level varies between 13 and 20 s.

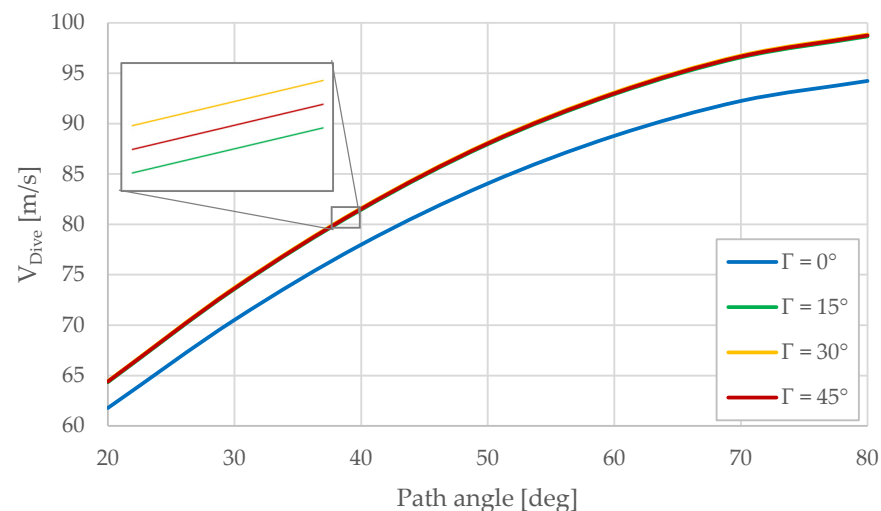


Figure 18. Variation in dive speed with path angle.

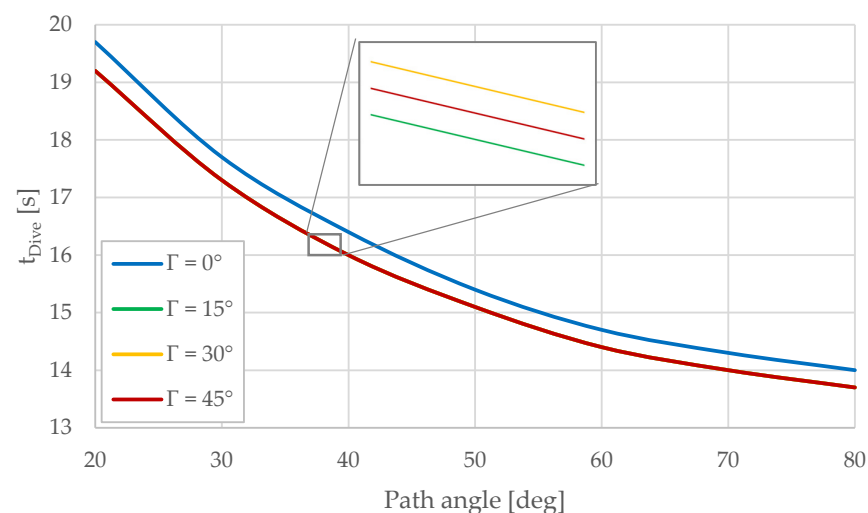


Figure 19. Variation in dive time to path angle.

Both the dive speed and dive time present asymptotic behavior with respect to path angle: dive speed increases asymptotically, while dive time decreases asymptotically. The $\Gamma = 15^\circ$, $\Gamma = 30^\circ$ or $\Gamma = 45^\circ$ configurations show very similar trends, whereas the $\Gamma = 0^\circ$ configuration is offset due to its higher minimum drag coefficient, resulting in greater drag, lower dive speed, and longer dive time. For these analyses, the drag coefficient during the dive is assumed to be close to the minimum drag, as the vehicles are flying at small angles of attack.

The dive speeds obtained in this analysis are theoretical, as they are at least twice the cruise speeds, suggesting that the structure may experience failure due to aeroelastic phenomena such as divergence or flutter. To safely achieve these velocities, the drone's structure must be sufficiently robust to withstand the aerodynamic loads encountered during the dive. Even though such speeds occur only once, during the terminal phase of flight, just before impact, the UAV must be capable of maintaining structural integrity throughout the dive to ensure mission success and impact accuracy.

4. Discussion

The results obtained in this study indicate that each configuration presents specific advantages, allowing the formulation of an optimization problem based on a selected objective function. The aerodynamic analysis shows that the configuration with $\Gamma = 0^\circ$ has the highest drag coefficient due to wing interference. However, to enable a consistent comparison, a biplane-type wing layout was chosen instead of a high-aspect ratio wing, which would provide lower drag and higher lift characteristics. Nevertheless, such a configuration would also result in greater wing loading, requiring a stronger—and therefore heavier—structure, making it unsuitable for direct comparison with the other configurations.

The performance results indicate that the configurations with $\Gamma = 15^\circ$ and $\Gamma = 45^\circ$ offer the best performance, each excelling under different criteria. However, the $\Gamma = 30^\circ$ configuration, while not the best in any single category, achieves values very close to the top in all metrics, making it the best overall configuration. This setup combines favorable characteristics of $\Gamma = 15^\circ$ —such as high range, endurance, stall, maximum, and dive speeds—with the good turning performance of $\Gamma = 45^\circ$ configuration. Conversely, the $\Gamma = 0^\circ$ configuration shows the weakest overall performance. Implementing a variable-dihedral wing system to alternate between low angles for cruise efficiency and high angles for maneuverability would add excessive complexity and mass, offering limited practical benefit.

5. Conclusions

The present study investigated the aerodynamic and flight performance characteristics of an X-wing unmanned aerial vehicle (UAV) with different dihedral angles ($\Gamma = 0^\circ, 15^\circ, 30^\circ$, and 45°). The objective was to evaluate the influence of wing dihedral on aerodynamic efficiency, and maneuverability, providing a basis for selecting an optimal configuration for loitering-munition UAV applications. The analysis was conducted using RANS simulations to determine aerodynamic coefficients and dynamic performance parameters, complemented by flight mechanics evaluations to estimate performances.

From the aerodynamic results, it was observed that the dihedral angle strongly affects both lift and drag behavior due to interference effects between the wings and the empennages. The $\Gamma = 0^\circ$ configuration presents the largest drag coefficient as a result of strong aerodynamic interference at zero dihedral, while configurations with higher dihedral angles reduced this effect significantly. The maximum lift coefficient was found for $\Gamma = 15^\circ$, leading to the lowest stall speed among all configurations. Increasing the dihedral angle beyond 15° resulted in a slight reduction in lift performance but also a smoother lateral stability behavior.

Performance analysis revealed that the $\Gamma = 15^\circ$ configuration achieved the best rate of climb (ROC) and angle of climb (AOC), with associated speeds providing a comfortable safety margin above the stall limit. In contrast, the $\Gamma = 30^\circ$ and $\Gamma = 45^\circ$ configurations showed slightly lower climb rates but remained within a comparable performance envelope. The endurance and range results indicated that $\Gamma = 15^\circ$ again delivered the best overall efficiency, with a flight duration of 0.77 h and range of 79 km, depending on altitude and

power conditions. The $\Gamma = 0^\circ$ configuration consistently showed the weakest results across all performance metrics due to its higher drag and less favorable aerodynamic efficiency.

In maneuvering analysis, both bank-to-turn (BTT) and skid-to-turn (STT) cases were studied to evaluate controllability. The BTT maneuver required larger elevator deflections and induced higher load factors, leading to structurally demanding flight conditions. In contrast, the STT maneuver demonstrated smoother control response and lower load factors, which are advantageous for precision and stability during low-altitude or terminal operations. The results showed that $\Gamma = 45^\circ$ and $\Gamma = 30^\circ$ configurations required the smallest rudder deflections for equivalent turn radii, confirming the beneficial influence of higher dihedral angles on yaw control. Furthermore, skid-to-turn maneuvers were found to be more suitable for X-wing UAVs, as they avoid the time delay associated with rolling to a new bank angle and allow faster directional corrections.

Finally, when all performance and maneuverability aspects are considered together, the $\Gamma = 30^\circ$ configuration emerges as the most balanced and efficient design. While $\Gamma = 15^\circ$ excels in endurance and climb performance, and $\Gamma = 45^\circ$ in maneuverability, the intermediate $\Gamma = 30^\circ$ configuration provides a near-optimal compromise between aerodynamic efficiency and control effectiveness. Therefore, for operational applications requiring both efficient cruise and agile maneuvering, a fixed dihedral angle of around 30° represents the best overall design choice for an X-wing UAV.

Despite providing a comprehensive analysis of X-wing loitering munition UAVs, the present study is limited to CFD-based aerodynamic and performance simulations without experimental validation. Only fixed dihedral angles were considered, and structural, aeroelastic, and propulsion system effects were not explicitly modeled. Future research could include wind-tunnel or flight testing to validate the computational predictions, investigation of structural and aeroelastic behavior, and exploration of variable-dihedral designs to further optimize maneuverability and cruise efficiency. These directions could provide a foundation for the practical design and deployment of X-wing UAVs and guide future developments in loitering munition platforms.

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Abbreviations

The following abbreviations are used in this manuscript:

UAV	Unmanned Aerial Vehicle
CFD	Computational Fluid Dynamics
RANS	Reynolds-Averaged Navier–Stokes.
SST	Shear Stress Transport
ODE	Ordinary Differential Equation
AoA	Angle of attack

SSA	Side slip angle
AOC	Angle of climb
ROC	Range of climb
BTT	Bank-to-turn
STT	Skid-to-turn

References

1. Bode, I.; Watts, T. *Loitering Munitions and Unpredictability*; Center of War Studies: Odense, Denmark, 2023.
2. Voskuil, M. Performance analysis and design of loitering munitions: A comprehensive technical survey of recent developments. *Def. Technol.* **2022**, *18*, 325–343. [[CrossRef](#)]
3. Zhugan, O.; Degtyarev, M. Version of loitering munitions classification based on the state-of-the-art and trends analysis. *Space Energy Power Propuls.* **2024**, *30*, 31–39. [[CrossRef](#)]
4. Dennis, M.; Rao, A. Performance Optimization and Guidance of a Low-Altitude Skid-to-Turn Vehicle. Part I: Performance Optimization. In Proceedings of the AIAA SciTech Forum, San Diego, CA, USA, 7–11 January 2019.
5. Dennis, M.; Rao, A. Performance Optimization and Guidance of a Low-Altitude Skid-to-Turn Vehicle. Part II: Optimal Guidance and Control. In Proceedings of the AIAA SciTech Forum, San Diego, CA, USA, 7–11 January 2019.
6. Zhang, Z.; Li, J.; Yang, Y.; Yang, C.; Mao, R. Research on Speed Scheme for Precise Attack of Miniature Loitering Munition. *Math. Probl. Eng.* **2020**, *2020*, 4963738. [[CrossRef](#)]
7. Raymer, D.P. *Aircraft Design: A Conceptual Approach*; American Institute of Aeronautics and Astronautics: Reston, VA, USA, 2018.
8. Mekdour, K.; Reddy, A.K.M.R.; Dawkins, J.I.G.; Selva, T.M.G.; Zaghib, K. Comparative Analysis of Cell Design: Form Factor and Electrode Architectures in Advanced Lithium-Ion Batteries. *Batteries* **2025**, *11*, 450. [[CrossRef](#)]
9. Liu, S.; Chen, J.; Zhang, C.; Jin, L.; Yang, Q. Experimental study on lithium-ion cell characteristics at different discharge rates. *J. Energy Storage* **2022**, *45*, 103418. [[CrossRef](#)]
10. Roache, P. Perspective: A Method for Uniform Reporting of Grid Refinement Studies. *J. Fluids Eng.* **1994**, *116*, 405–413. [[CrossRef](#)]
11. Sandip, G.; Xiang, N.; Yue, W. Assessment of Transitional RANS Models and Implementation of Transitional IDDES Method for Boundary Layer Transition and Separated Flows in OpenFOAM-V2312. *Fluids* **2025**, *10*, 230. [[CrossRef](#)]
12. Menter, F.; Sechner, R.; Matyushenko, A. Best Practice: RANS Turbulence Modeling in Ansys CFD. Ansys. Available online: <https://www.ansys.com/resource-center/technical-paper/best-practice-rans-turbulence-modeling-in-ansys-cfd> (accessed on 7 December 2025).
13. Menter, F.; Langtry, R.; Likki, S.; Suzen, Y.; Huang, P.; Volker, S. A Correlation-Based Transition Model Using Local Variables: Part I-Model Formulation. *J. Turbomach.* **2004**, *5*, 413–422.
14. Langtry, R.; Menter, F.; Likki, S.; Suzen, Y.; Huang, P.; Volker, S. A Correlation-Based Transition Model Using Local Variables—Part II: Test Cases and Industrial Applications. *J. Turbomach.* **2004**, *128*, 423–434. [[CrossRef](#)]
15. Candida, Y.; Jenish, T. Performance analysis of BLDC Motors and Its Various Control Strategies. *Int. J. Sci. Res. Eng. Manag.* **2023**, *7*.
16. Gudmundsson, S. *General Aviation Aircraft Design*; Butterworth-Heinemann: Oxford, UK, 2022.
17. Lan, C.-T.E.; Roskam, J. *Airplane Aerodynamics and Performances*; DARcorporation: Lawrence, KS, USA, 2008.
18. Advanced Precision Composites. Available online: <https://www.apcprop.com/technical-information/performance-data/?v=f5b15f58caba> (accessed on 7 November 2025).
19. Shampine, L.; Reichelt, M. The MATLAB ODE Suite. *SIAM J. Sci. Comput.* **1997**, *18*, 1–22. [[CrossRef](#)]
20. Abbott, I.H.; Doenhoff, A.E.V. *Theory of Wing Sections*; DOVER Publications: New York, NY, USA, 1958.

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