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Aerodynamic Performance on a Wing Using Passive and Pneumatically Active Engine Nacelle Strakes

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Abstract

Aerodynamic Performance on a Wing Using Passive and Pneumatically Active Engine
Nacelle Strakes

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The aerodynamic interactions at the nacelle-wing junction create complex flow structures that promote localized flow separation, increase aerodynamic drag, and reduce aircraft efficiency. To mitigate these aerodynamic penalties, this thesis experimentally evaluated passive and active pneumatic nacelle-mounted strakes installed at the nacelle-wing junction of a semi-span wing model representative of a Boeing 737-800 wing planform. Low-speed wind tunnel experiments were conducted over a wing angle of attack (α) range from -5° to 20° under representative cruise, takeoff, and landing conditions with flap deflection angles (δ_f) of 0° , 20° , and 45° . Aerodynamic performance was evaluated using measurements of lift coefficient (C_L), drag coefficient (C_D), and lift-to-drag ratio (L/D). The results demonstrated that the effectiveness of both passive and active strakes depended strongly on flap deflection and flow control configuration. Passive strakes reduced C_D , increasing L/D by up to 36.5% during cruise and 22.8% during landing, but generally degraded performance during takeoff because drag increases outweighed modest lift increases. Active strakes achieved the greatest improvement during cruise, increasing L/D by 42.9%, but reduced aerodynamic efficiency during takeoff and landing for the same reason. These findings demonstrate that effective

nacelle-wing flow control depends on matching the flow control strategy to the surrounding aerodynamic environment.

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NOMENCLATURE

AERL	=	Aeronautical Engineering Research Laboratory
AFC	=	active flow control
A_j	=	jet exit area
b	=	wing span
c	=	wing chord length
$\bar{c}$	=	mean wing aerodynamic chord
$c(y)$	=	wing chord as a function of spanwise position
c_r	=	wing root chord
c_t	=	wing tip chord
CAD	=	Computer Aided Drafting
C_D	=	drag coefficient
$C_{D,\min}$	=	minimum drag coefficient
CFD	=	Computational Fluid Dynamics
$CI_{95,x}$	=	95% confidence interval for variable x
C_L	=	lift coefficient
$C_{L,\max}$	=	maximum lift coefficient
C_μ	=	blowing coefficient
CRM	=	Common Research Model
DAQ	=	data acquisition
D_m	=	measured drag force component
DNS	=	Direct Numerical Simulation
dS	=	differential wing planform area
D_w	=	wind-axis drag force
H1	=	forward pneumatic injection location
H2	=	middle pneumatic injection location
H3	=	aft pneumatic injection location
i_{strake}	=	strake incidence angle
L	=	characteristic length
L/D	=	lift-to-drag ratio

$(L/D)_{\max}$	=	maximum lift-to-drag ratio
L_m	=	measured lift force component
L_w	=	wind-axis lift force
M_∞	=	freestream Mach number
$\dot{m}_j$	=	jet mass flow rate
n	=	number of samples
NACA	=	National Advisory Committee for Aeronautics
PB	=	pulsed blowing
PFC	=	passive flow control
PIV	=	particle image velocimetry
PLA	=	polylactic acid
q_∞	=	freestream dynamic pressure
QTY	=	quantity
Re	=	Reynolds number
$Re_{\bar{c}}$	=	Reynolds number based on mean aerodynamic wing chord
s_x	=	sample standard deviation of variable x
SB	=	steady blowing
SE_x	=	standard error of the mean for variable x
SJs	=	synthetic jets
S_{ref}	=	wing reference area
t	=	time
$t_{0.975, n-1}$	=	two-sided Student's t critical value with $n - 1$ degrees of freedom
U_∞	=	freestream velocity
V_j	=	jet exit velocity
VG	=	vortex generator
x	=	generic measured variable
$\bar{x}$	=	sample mean of variable x
x_i	=	i th observation of variable x
y	=	spanwise coordinate
ZNMF	=	zero-net-mass-flux
α	=	wing angle of attack
α_{stall}	=	wing Stall angle
$\Delta(L/D)_{\max}$	=	percent change in maximum lift-to-drag ratio relative to the baseline
δ_f	=	flap deflection angle

η	=	normalized spanwise coordinate
λ	=	wing taper ratio
μ	=	dynamic viscosity
ν	=	kinematic viscosity
ρ	=	air density
ρ_∞	=	freestream air density
ρ_j	=	jet air density

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Chapter 1

INTRODUCTION

1.1 Motivation

Modern commercial aircraft design continues to rely heavily upon the need for increased aerodynamic efficiency, reduced fuel consumption and compliance with ever increasing regulations related to environmental and economic requirements. Improvements in L/D translate into substantial savings in fuel costs and operational expenses over the life cycle of an airplane. Consequently, researchers continue to focus on developing and refining flow control devices capable of mitigating flow separation, enhancing lift, and reducing drag, particularly in those areas where complex three-dimensional flow patterns prevail.

One area of considerable interest involves the nacelle-wing junction of turbofan-powered commercial aircraft as shown in Figure 1.1a. The installation of an engine nacelle and pylon in close proximity to a wing creates strong aerodynamic interference effects, consisting of local accelerations, adverse pressure gradients, and three-dimensional flow separation as shown in Figure 1.1b. These effects are magnified as the α increases. Additionally, the effects become even greater during high-lift configurations such as during takeoff and landing, when wing flaps and slats are extended. Flow separation in the nacelle-wing junction can adversely affect lift performance, create additional drag, and lower aerodynamic efficiency of the entire aircraft.

To alleviate some of the issues identified above, nacelle-mounted strakes have become popular passive flow control (PFC) devices. Typically, a nacelle strake consists of a small, fixed aerodynamic surface attached to the nacelle, producing a coherent streamwise vortex as shown in Figure 1.2. This vortex can draw high velocity freestream fluid towards the wing upper surface boundary layer, thus delaying flow separation and improving its aerodynamic performance. Although they contain very little geometrical complexity, nacelle strakes can

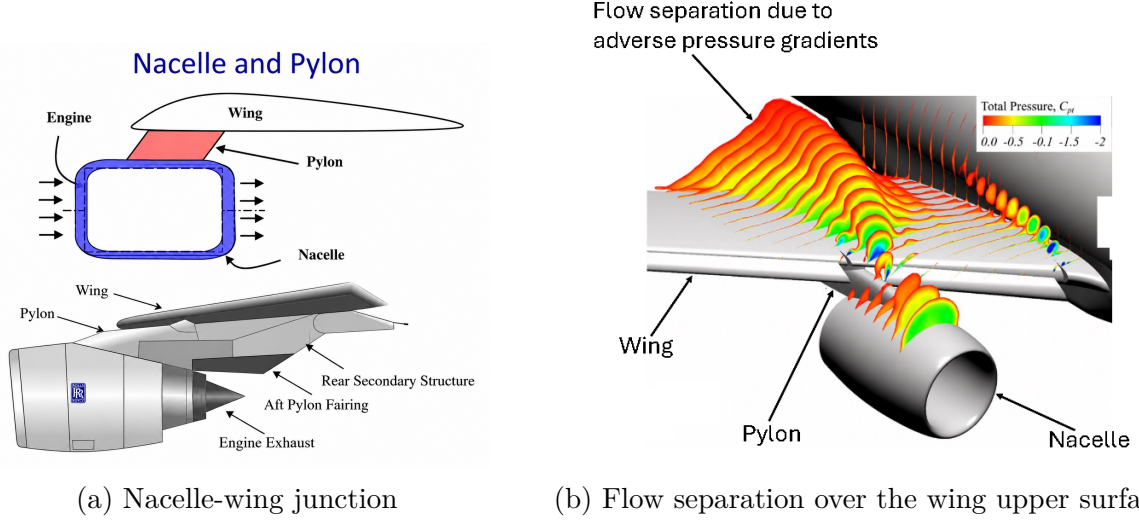


Figure 1.1: (a) Representative nacelle-wing junction and (b) the region over the wing upper surface where localized flow separation may occur due to adverse pressure gradients caused by aerodynamic interference of the nacelle and pylon [1].

modify the local flow field and have been demonstrated to improve stall characteristics and lift performance in various flight-representative regimes.

Nacelle strakes have traditionally been implemented effectively on large commercial aircraft; however, the effect that nacelle strakes impose on the local flow field is highly dependent on several geometrical parameters such as the i_{strake} and placement. Additionally, prior works have focused either on full-scale flight testing, computational studies or limited experimental configurations. Therefore, further controlled experimental investigations are needed to determine and document how changes in strake geometries impact nacelle strake aerodynamics across numerous flight-representative regimes. In particular, the influence that nacelle-mounted strakes have on wing flaps has yet to be thoroughly documented in published literature.

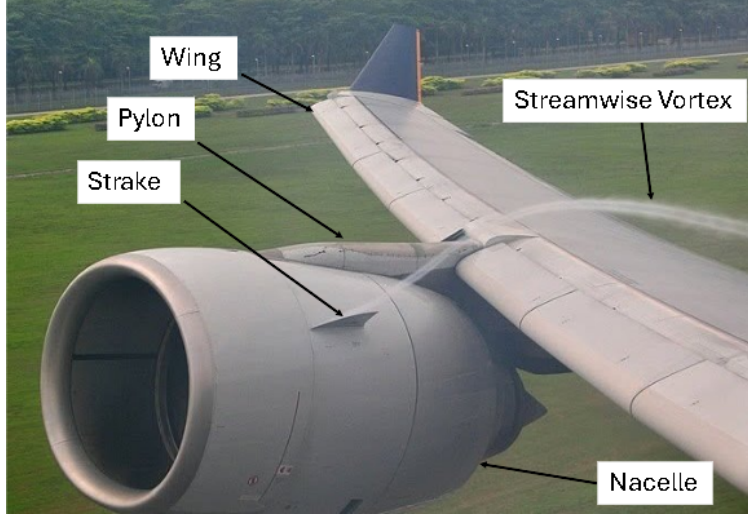


Figure 1.2: Nacelle strake producing streamwise vortex over the wing upper surface [2].

Therefore, the purpose of this thesis is to experimentally investigate the aerodynamic characteristics of a semi-span wing model featuring a nacelle-mounted strake under a variety of flight-representative regimes. Specifically, emphasis will be given to documenting how variations in i_{strake} affect C_L , C_D and L/D for several δ_f configurations. To accomplish this task, wind tunnel tests were performed on a semi-span wing model designed from publicly available commercial aircraft geometry. Aerodynamic forces were measured across a range of α . Each i_{strake} configuration was compared against a no-strake baseline configuration to evaluate both the advantages and disadvantages of strake implementation.

The first portion of this work focuses upon passive strake configurations and provides a parametric data set covering a range of i_{strake} configurations and δ_f configurations. By systematically changing both of these parameters, this study aims to illustrate and define the underlying aerodynamic phenomena influencing passive strake effectiveness and to determine those conditions in which strakes may be effective or ineffective at enhancing overall aerodynamic performance. It is anticipated that these findings will help to understand nacelle-wing flow interactions better and provide experimental evidence that could guide future designs and optimizations of nacelle strakes.

Beyond providing a comprehensive analysis of passive strake configurations, this the-

sis also evaluates active strake configurations using pneumatic air injection. Active strake configurations incorporate controlled pneumatic air injections into discrete locations along the surface of the strake. Pneumatic air injection enables modification of the local vortex strength and development of the boundary layer near the nacelle-wing junction. A parametric study has been developed to examine three individual pneumatic air injection locations while varying i_{strake} and δ_f . The comparison between passive and active strake configurations will allow this research effort to assess the viability of targeting additional momentum addition as an enhancement technique for improving separation control, lift generation, and L/D under cruise, takeoff, and landing flight regimes.

By investigating both passive and active strake configurations, this thesis evaluates two complementary approaches to improving nacelle-wing aerodynamic interaction. Data collected within this thesis provide new insights into the relative robustness of passive strakes and the potential benefits and performance limitations of active strakes.

The remainder of this thesis is structured as follows: Chapter 2 reviews relevant literature related to boundary layer separation, vortex-based flow control devices, active flow control (AFC) techniques, and nacelle-mounted strake applications. Chapter 3 details the design and fabrication of the experimental model, apparatus, and the wind tunnel test setup. Chapter 4 discusses the experimental methodology, data acquisition procedures, and data reduction techniques. Chapter 5 documents and analyzes the aerodynamic results, including C_L , C_D , and L/D for all configurations examined. Chapter 6 summarizes key findings and conclusions based on data collected within this thesis. Finally, Chapter 7 provides suggestions and recommendations for future research involving possible extensions to AFC strategies.

Chapter 2

LITERATURE REVIEW

2.1 High-Lift Aerodynamics and Flow Separation

High-lift systems employing flaps and slats are essential for modern commercial aircraft because they improve lift during takeoff and landing while maintaining acceptable stall characteristics [3, 4, 5]. The use of flaps and slats increases wing camber and can also increase circulation around the wing. However, as the wing camber is increased, so too are the adverse pressure gradients across the wing upper surface. This increased adverse pressure gradient can make the boundary layer more susceptible to flow separation along the wing's upper surface.

Flow separation, also referred to as boundary layer separation, as shown in Figure 2.1, occurs whenever low-momentum fluid adjacent to the wing upper surface cannot counteract the applied adverse pressure gradient. This causes flow detachment from the wing upper surface and creates a region of flow instability that increases drag and reduces lift. For high-lift systems, the separated flow develops very rapidly into a strongly three-dimensional flow structure due to the interaction between flap-generated vortices, nacelle wakes, and turbulent shear layers. As a result, understanding this three-dimensional separation behavior remains one of the most significant aerodynamic challenges in high-lift system design.

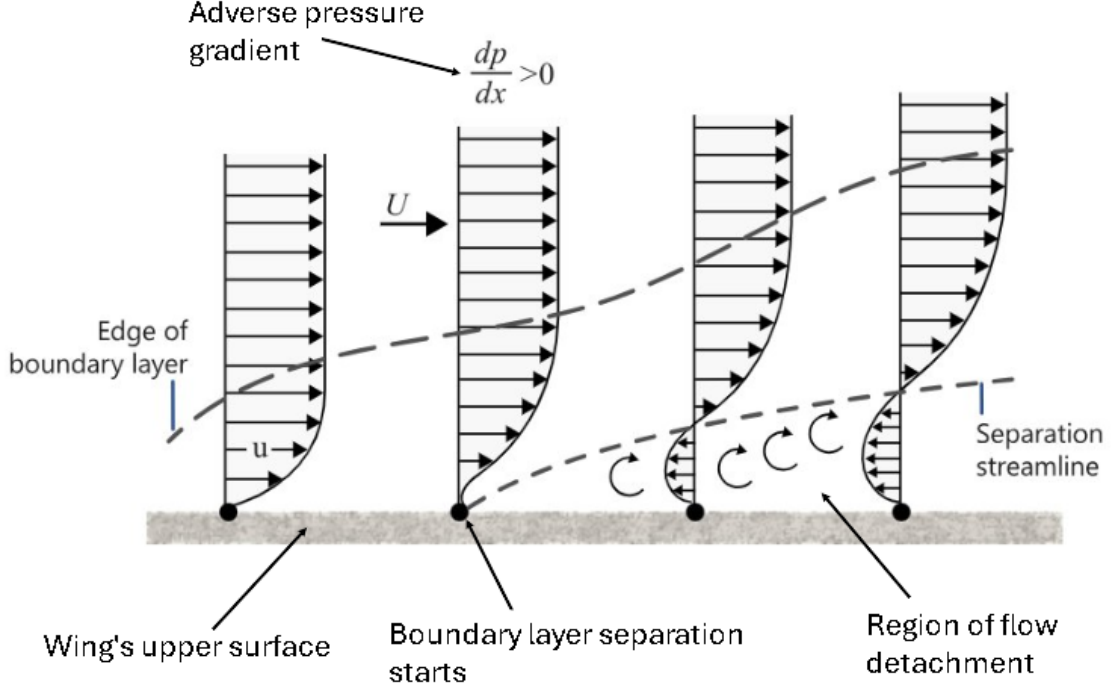


Figure 2.1: Representation of flow separation along the wing upper surface due to adverse pressure gradients developing downstream [3].

Bradshaw and Wong's hot wire measurements revealed that separated turbulent shear layers remain far from equilibrium even after reattachment, exhibiting higher turbulence levels than those observed under equilibrium conditions [6]. Importantly, they demonstrated that both the magnitude of turbulent transport and the coherence of the large-scale turbulent eddies within the separated shear layer govern post-reattachment behavior. Building upon this, Pope utilized data collected through direct numerical simulation (DNS) to demonstrate that Reynolds stresses and turbulent momentum transfer play a central role in determining a boundary layer's resistance to separation. Specifically, sufficient momentum transfer to near-wall low-momentum fluid can significantly enhance its ability to withstand adverse pressure gradients [7].

Wynanski and Greenblatt subsequently found that large-scale vortex structures generated within flow control devices can influence separated flow behavior by increasing rates of momentum transfer to the near-wall fluid [8]. This finding provided the aerodynamic rationale for developing PFC and AFC strategies that target manipulation of large-scale vortex

structures. Due to the localized adverse pressure gradients and separation promoted by the nacelle-wing interactions, researchers focused on next generation aircraft have increasingly employed these strategies as a means of enhancing high-lift performance and overall aerodynamic efficiency [9, 10, 11]. Of particular concern is the aerodynamic interference between the nacelle-wing junction, which introduces uniquely severe localized effects on boundary layer behavior.

2.2 Nacelle-Wing Junction Interference

Flow interference associated with the aircraft engine nacelle and pylon as part of the nacelle-wing junction affects wing surface pressure distributions and the resulting three-dimensional flow structures [12, 13, 14]. These effects are particularly pronounced within high-lift low-moderate α ranges, during which the boundary layer is already subjected to significant adverse pressure gradients and thus more vulnerable to interference-induced separation.

Initial research by Hieser and Whitcomb showed that nacelle-wing junction interference produces elevated pressure magnitudes near the junction and promotes earlier flow separation compared to unaffected regions of the wing [13]. Their work also established that nacelle geometry influences local pressure distributions, boundary layer development, and the effective sweep angle of the wing airfoil. Building on this, Kulfan revealed that both the nacelle mounting location and pylon design have a significant influence on total system aerodynamics, such that modest geometric variations in either component reduce interference drag and alter localized flow behavior [12]. Patterson further demonstrated that careful nacelle and pylon shaping can reduce interference drag, and crucially, that nacelle-wing junction interference does not inherently imply a performance penalty — the outcome depends on how local pressure gradients and secondary flow structures interact with the adjacent boundary layer [14].

More recent studies utilizing the NASA Common Research Model (CRM) have confirmed

that nacelle-wing junction interference is responsible for developing a downstream wake of localized separation regions as shown in Figure 2.2; these localized separation regions contribute to lift loss and drag increases [15, 1, 9]. Collectively, these findings demonstrate that vortices arising from the nacelle-wing junction interference play a major role in determining commercial aircraft aerodynamic performance, providing strong justification for the development of nacelle-mounted flow control devices. Understanding the three-dimensional flow structures that drive this interference — including horseshoe vortices, secondary flows, and streamwise vortex interactions — is therefore essential.

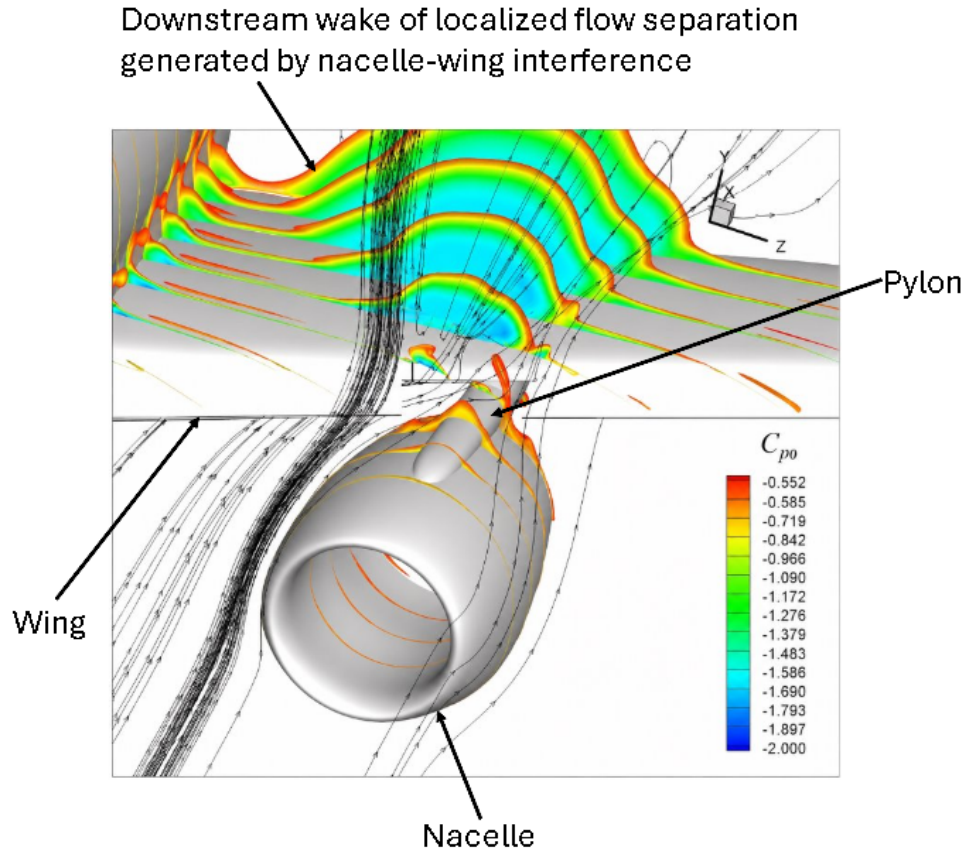


Figure 2.2: Representation of a wake of localized flow separation regions largely developing downstream over the wing upper surface due to nacelle-wing junction interference [16].

2.3 Junction Flow Physics and Vortex Dynamics

In the vicinity of the nacelle-wing junction, the primary flow structures that dominate the flow field include horseshoe vortices, secondary flow structures, and areas of turbulent recirculation. Together, these features create a highly three-dimensional flow environment. Horseshoe vortices form where a turbulent boundary layer meets a nacelle or pylon and experiences a large adverse pressure gradient at the junction, resulting in local separation. An example of a horseshoe vortex developing around the nacelle-wing junction is shown in Figure 2.3.

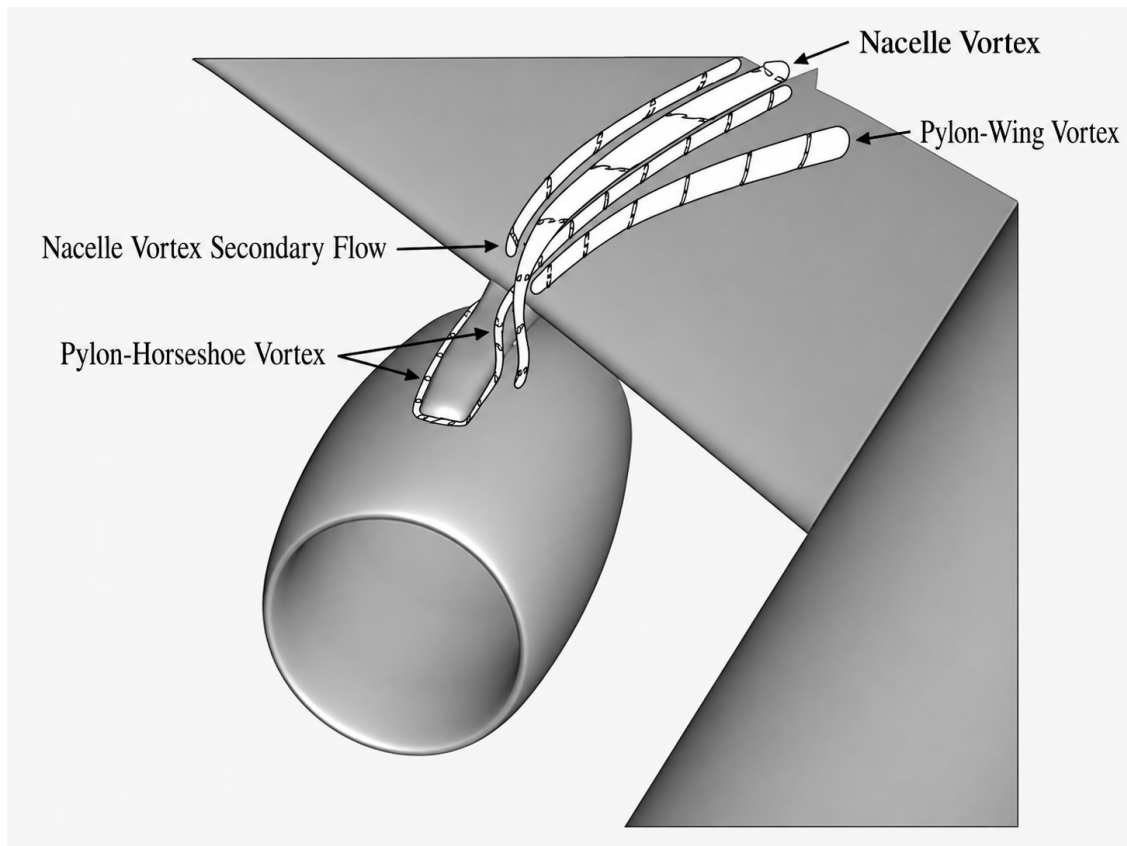


Figure 2.3: Conceptual illustration of the interacting vortical structures that may develop at the nacelle-wing junction and over the wing upper surface. Vortical structures include a pylon horseshoe vortex, nacelle-vortex secondary flow, nacelle vortex, and pylon-wing vortex [17].

Investigations by Simpson demonstrated that horseshoe vortices generated at the nacelle-

wing junction significantly affect downstream flow separation behavior over the wing upper surface through the substantial unsteady effects they impose on local pressure recovery and boundary layer development along the adjacent wall [18]. Building on this, Devenport and Simpson showed that nacelle-wing junction flows exhibit considerably greater turbulence energy than other flow types, along with higher energy content in coherent structures associated with vortex stretching and vortex-wall interactions — all of which combine to increase aerodynamic drag through enhanced boundary layer separation [18, 19].

Of particular concern in this flow environment are streamwise vortices, whose direction of travel, size, and velocity determine whether flow separation is suppressed or promoted by redistributing momentum within the boundary layer. Beneficial vortices draw high-momentum fluid toward the surface, while unfavorable vortex interactions can lift low-momentum fluid away from the wall and worsen separation. Vortex-wall interaction has therefore become one of the most prominent factors governing aerodynamic performance at nacelle-wing junctions. These findings collectively provide the physical basis for flow control devices — specifically vortex generators (VGs) and nacelle-mounted strakes — designed to manipulate streamwise vortices and enhance near-wall momentum transfer.

2.4 Passive Flow Control: Vortex Generators and Passive Nacelle Strakes

VGs are among the most commonly used PFC devices that reduce flow separation by creating streamwise vortices that transport high-momentum fluid toward the wall, thereby increasing near-wall boundary layer energy and improving resistance to adverse pressure gradients [20, 21]. A conceptual representation of the effect of an arbitrary wing VG is shown in Figure 2.4 for reference. Lin demonstrated that low-profile VGs — those mostly embedded within the boundary layer — provide effective flow separation delay without introducing a large drag penalty compared to traditional vane-type VGs, which incur parasitic drag losses due to their larger size [20]. Hoerner noted that the aerodynamic advantage of any VG can never exceed the drag penalty introduced by the device itself, emphasizing the importance

of careful geometric optimization in VG design [21].

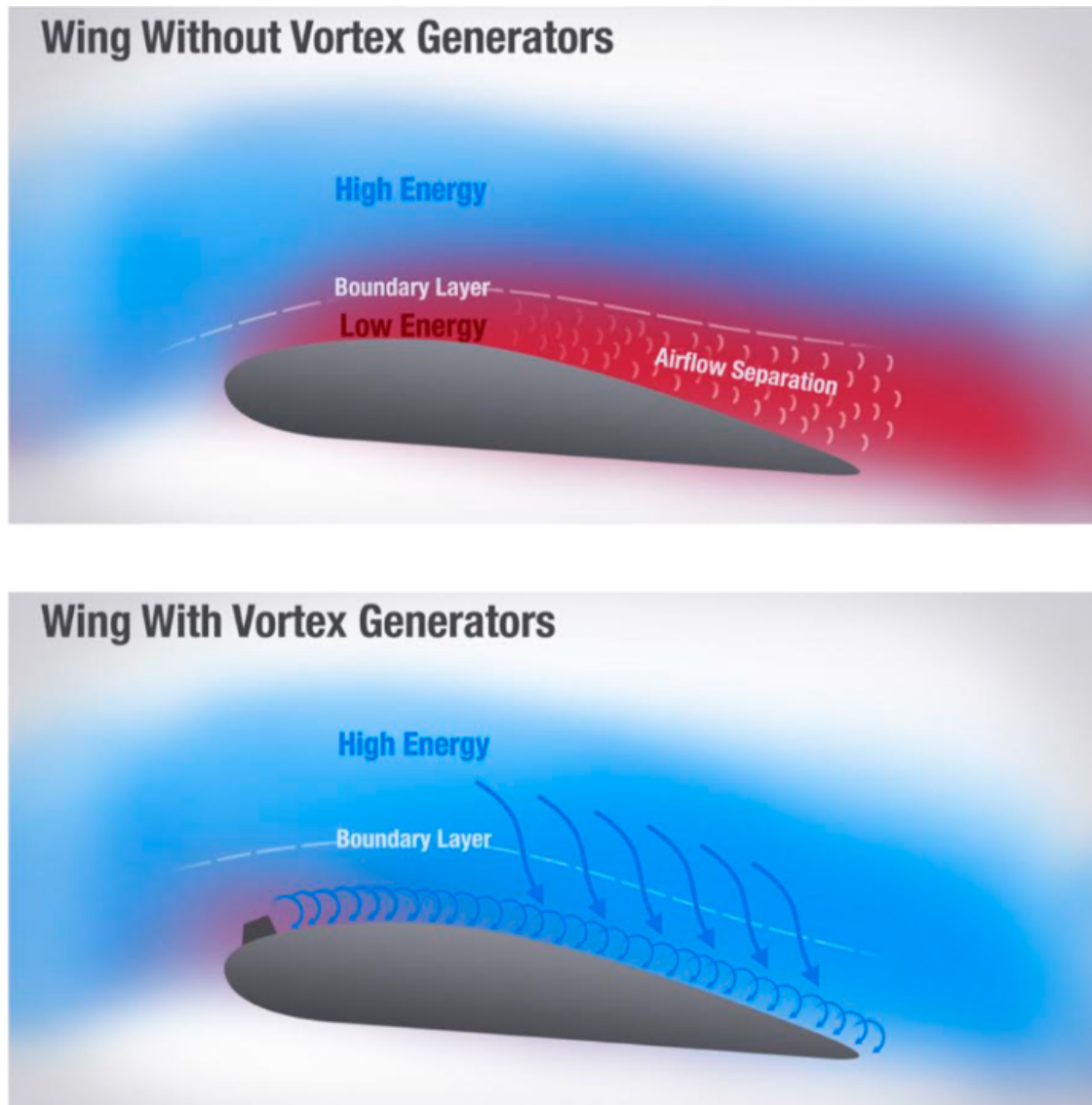


Figure 2.4: Conceptual comparison of airflow over an airfoil without and with VGs. The VGs promote the transfer of high-momentum fluid into the near-wall boundary layer, helping to delay or suppress flow separation [22].

Polhamus showed that the generation of coherent vortical structures produces vortex-induced suction that alters local pressure distributions and contributes to lift enhancement [23]. Although originally developed for delta wing applications, this vortex lift effect is directly applicable to passive nacelle strakes — a specialized form of VG designed to mitigate aerodynamic interference between the nacelle-wing junction. Thus, these studies indicate

that passive nacelle strakes produce streamwise vortices that can alter local pressure recovery and enhance boundary layer attachment along the wing upper surface.

In addition, Zhang illustrated that passive nacelle strakes delay stall and extend the usable α range by producing stabilizing streamwise vortices above the wing upper surface, enhancing near-wall momentum transfer and reducing the boundary layer’s susceptibility to nacelle-induced separation [16]. Koklu similarly demonstrated that passive nacelle strakes energize the boundary layer downstream of the nacelle wake, with the resulting vortex structures suppressing separation zones and improving pre-stall aerodynamic performance [1]. Soudakov further confirmed that passive nacelle strakes induce significant changes in flow behavior under high-lift conditions [24].

Although passive nacelle strakes have demonstrated considerable benefits in stall prevention and lift maintenance, all strake designs inherently involve a trade-off between high-lift separation control and cruise aerodynamic efficiency. The vortex strength required to prevent separation at high α ranges inevitably induces excess drag under cruise conditions where the flow remains attached. This limitation provides strong motivation for developing AFC methods capable of dynamically adjusting vortex strength to match varying aerodynamic conditions across the full flight envelope — specifically through jet blowing and pneumatic air injection techniques.

2.5 Active Flow Control: Jet Blowing and Pneumatic Air Injection Techniques

AFC methods dynamically alter fluid behavior in response to variable aerodynamic conditions through the use of energy inputs. Unlike PFC devices such as vortex generators and passive nacelle strakes, AFC systems can be activated or deactivated to suit a given flight condition, minimizing the drag penalties associated with fixed passive strakes during cruise [25, 26].

Fluidic AFC methods modify the momentum distribution within the boundary layer

by adding or removing momentum via air jets. Of these methods, steady blowing (SB), pulsed blowing (PB), and synthetic jets (SJs) have garnered the greatest research interest for application to commercial aircraft high-lift performance [25, 26, 8, 10, 27]. Due to the localized nature of adverse pressure gradient effects and the complex three-dimensional flow phenomena produced by nacelle-wing junction interference, passive control measures are inherently limited in effectiveness. Fluidic AFC systems are particularly well-suited to this environment, offering the flexibility to adjust momentum injection rates to match the severity and location of flow separation across varying flight conditions.

introduces a continuous stream of air injected tangentially into the boundary layer immediately upstream of a potential flow separation region. Zraggen demonstrated that SB produces reductions in flow separation and increases in lift, particularly in environments dominated by strong adverse pressure gradients [28]. However, the large mass flow rates required to develop sufficient momentum input for effective control render SB generally impractical for full-scale implementation.

PB overcomes this limitation by generating short-duration pulses of momentum into the boundary layer, creating streamwise vorticity and turbulent structures that promote increased near-wall mixing and momentum transfer [29]. Seifert and Zraggen found that PB requires substantially less mass flow than SB to achieve similar or superior levels of separation control, primarily because PB exploits naturally occurring flow instabilities to produce enhanced turbulent mixing at lower energy cost [25, 28]. typically requires a blowing coefficient (C_μ) in the range of $0.01 < C_\mu \leq 0.10$ to achieve adequate separation control, whereas PB can accomplish comparable or superior control authority at low ranges of $0.001 < C_\mu \leq 0.01$ — a factor of ten reduction in mass flow requirement [28]. This substantial efficiency advantage makes PB particularly attractive for full-scale aircraft applications where available bleed air is severely limited.

SJs are zero-net-mass-flux (ZNMF) devices that inject and remove air from the boundary layer in equal measure, eliminating net mass addition and removing the need for a pressurized

air source. The resulting oscillatory velocity fields generate coherent vortical structures that promote increased mixing and momentum transfer across the boundary layer, analogous to other fluidic AFC methods. Glezer and Amitay found that SJs can dramatically alter the aerodynamic characteristics of lifting surfaces without net mass addition, making them particularly attractive for applications where mass flow is severely constrained [26, 27].

A conceptual comparison sketch of the three principal fluidic AFC methods discussed in this section is presented in Figure 2.5. Although each approach seeks to energize the boundary layer and delay flow separation, they differ substantially in their momentum delivery mechanisms, mass flow requirements, and implementation complexity.

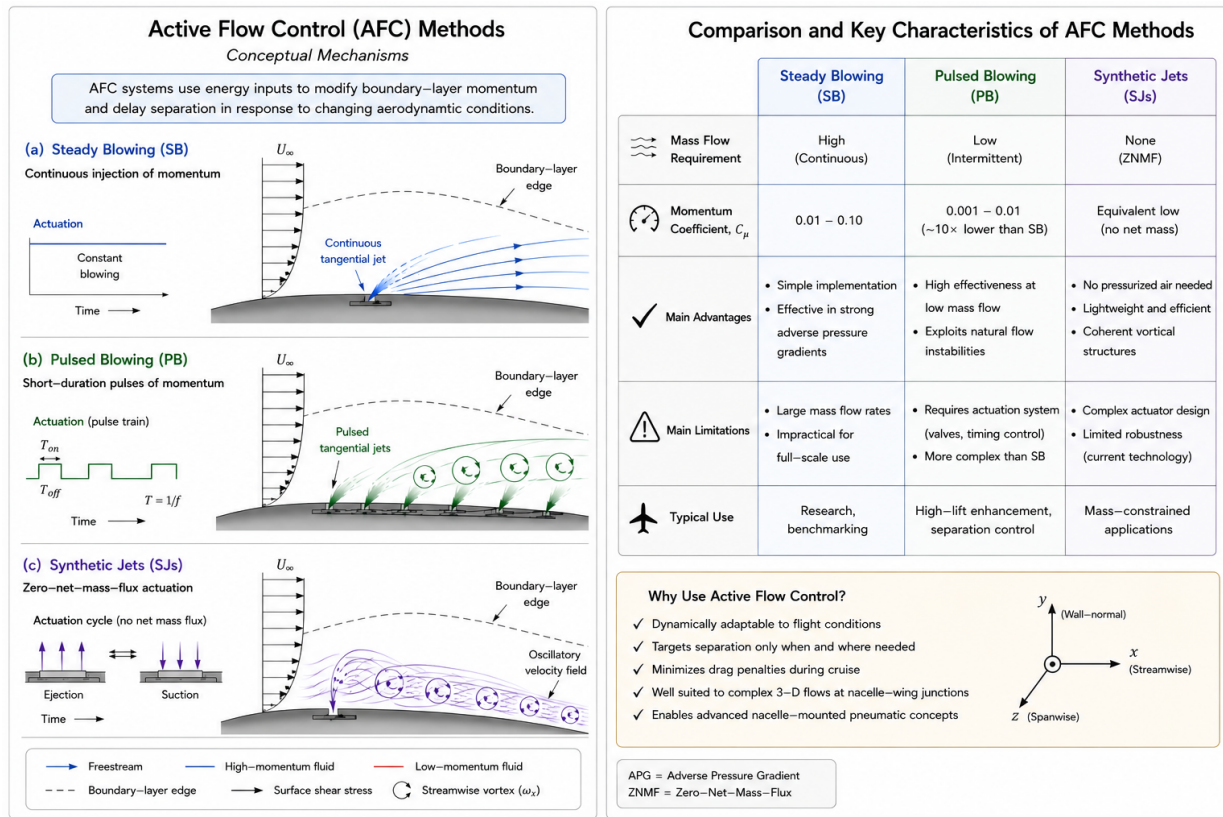


Figure 2.5: Comparison sketch of the principal fluidic AFC techniques used for boundary layer separation control. SB continuously injects momentum into the boundary layer, PB introduces intermittent momentum to exploit naturally occurring flow instabilities, and SJs generate coherent vortical structures through ZNMF actuation. The accompanying comparison summarizes the relative mass flow requirements, C_μ ranges, advantages, limitations, and representative applications of each method.

Although AFC methods show considerable promise for enhancing aircraft high-lift performance, conventional AFC systems continue to suffer from their dependence on pressurized air supplies — typically engine bleed air or auxiliary compressors — both of which introduce weight and efficiency penalties at the system level [9, 11, 30]. Consequently, current research efforts are increasingly focused on developing pneumatic air injection-based AFC concepts that require minimal mass flow and can be readily integrated into nacelle-mounted strakes.

2.6 Active Strake and Pneumatic Injection Concepts

Building on the pneumatic air injection-based concepts, recent research has extended these principles beyond simple boundary layer injection toward actively manipulating vortex formation itself. This represents a shift away from traditional VGs, which produce only a fixed level of vortex generation. Unlike traditional VGs, which are fixed in their physical location along the airflow path, active systems can modify the local flow field by adjusting the strength or location of momentum injection. This adaptability allows active strakes to respond more effectively to changes in flight condition, α , and δ_f .

Zraggen demonstrated that injecting air into the flow through active strake configurations significantly enhances lift performance and delays the onset of stall, primarily by altering the trajectories of developing streamwise vortices within the boundary layer [28]. Zraggen further determined that near-axial jets aligned parallel to the freestream velocity produced the greatest aerodynamic benefit, as the additional jet momentum reinforced the natural development of the vortex structure, whereas misaligned jets disrupted this process [28].

Building on Zraggen’s work, Weber showed that pneumatic air injection can dynamically alter the position of streamwise vortices within turbulent boundary layer flows [31]. Since streamwise vortex trajectory governs the degree of near-wall momentum transfer and, by extension, separation resistance, a system capable of modifying vortex trajectory enables proactive control of vortex-wall interaction across varying aerodynamic conditions. Con-

nelly similarly demonstrated that plasma-based actuators can alter vortex trajectory and coherence, offering another mechanism by which aerodynamic performance can be managed through targeted manipulation of coherent vortex structures rather than relying solely on large-scale momentum addition [32].

Spitzley et al. extended this research by developing a smart pneumatic nacelle-mounted strake integrated into a representative semi-span wing model of a commercial transport aircraft [33]. As part of the University of Washington undergraduate Capstone project, the experimental wing model, active pneumatic nacelle-mounted strake, and baseline nacelle geometries corresponding to passive i_{strake} configurations of 5° , 10° , and 15° were designed and fabricated. Their investigation evaluated both passive and active nacelle-mounted strakes by examining multiple pneumatic blowing pressures together with single-, dual-, and triple-hole pneumatic injection configurations to investigate the aerodynamic response of actively controlled nacelle-generated vortices. Their work demonstrated the feasibility of integrating pneumatic air injection directly into nacelle-mounted strakes and established the experimental platform upon which the present investigation was conducted [33].

Figure 2.6 illustrates a conceptual sketch showing the operation of an active pneumatic nacelle-mounted strake. Similar to other active flow control devices, pneumatic air injection augments the streamwise vortex generated by the strake, promoting the transfer of high-momentum fluid toward the wing upper-surface boundary layer and delaying localized flow separation near the nacelle-wing junction.

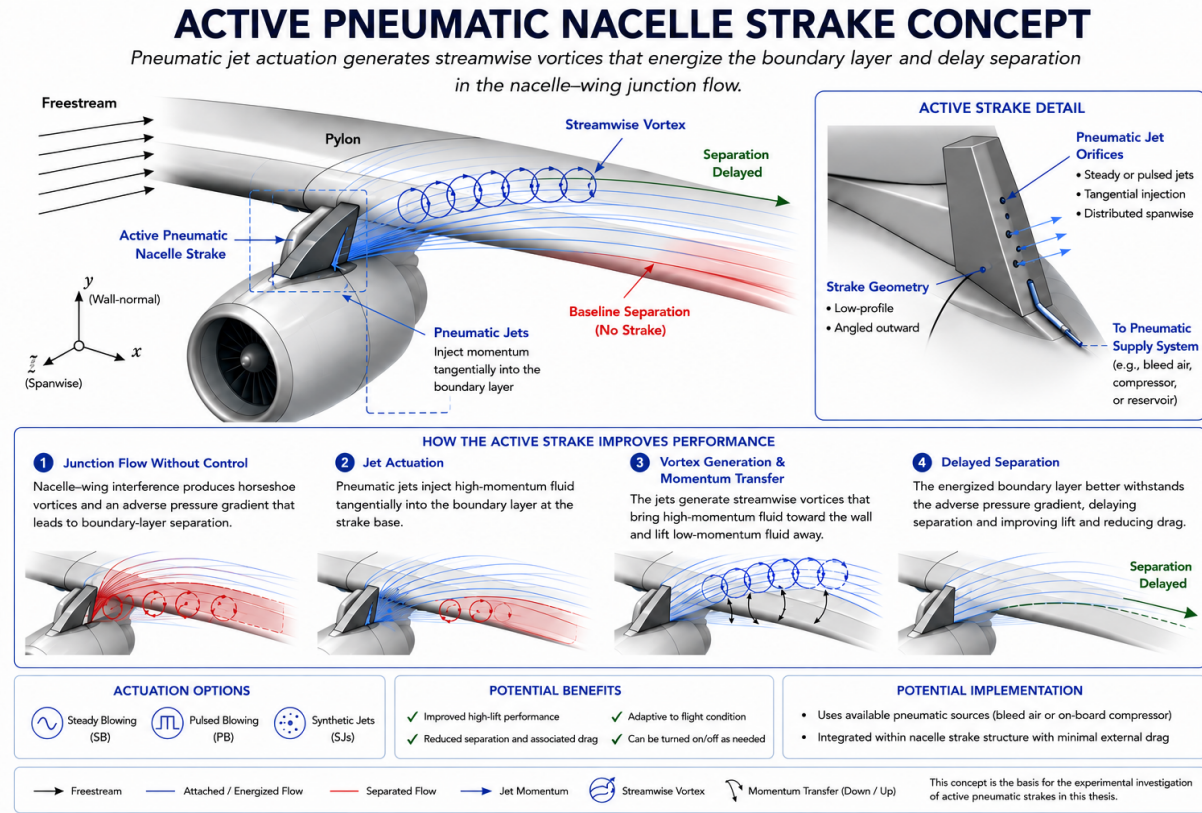


Figure 2.6: Conceptual sketch of an arbitrary active pneumatic nacelle strake. Pneumatic air injection near the strake generates and augments streamwise vortices that transport high-momentum fluid toward the wing upper surface, energizes the boundary layer, and delays flow separation in the nacelle–wing junction region.

Although these investigations demonstrated the potential of active pneumatic nacelle-mounted strakes, additional experimental research is needed to understand how passive and active strake performance varies under representative aircraft operating conditions. In particular, the influence of flight-representative cruise, takeoff, and landing conditions and more expansive passive i_{strake} configurations has not been thoroughly investigated. These remaining knowledge gaps motivate the present investigation and are addressed in the following section.

2.7 *Research Gap*

The areas discussed above clearly show that the current state of knowledge regarding streamwise vortex aerodynamics, nacelle-mounted strakes, and AFC all share common shortfalls that together provide the basis for this thesis.

Many researchers have shown that streamwise vortices enhance boundary layer adherence and inhibit flow separation by transferring momentum-rich fluid toward the wing surface, using mechanisms including tangential jet flows and rotating cylinders [20, 8]. This concept extends directly into practical application through nacelle-mounted strakes, which provide a method to eliminate or significantly minimize nacelle-induced flow separation at high α ranges, improving overall commercial aircraft aerodynamic performance [16, 24]. Together, these results demonstrate that vortex-based flow separation control is a viable alternative to traditional high-lift devices for commercial aircraft.

While vortex-based separation control offers a means of mitigating flow separation, an inherent problem remains: such systems necessarily trade high-lift performance for cruise aerodynamic efficiency due to fundamental design limitations [34]. For example, a strake or VG sufficient to prevent flow separation during takeoff and landing will introduce increased drag once the flow becomes attached at cruise speed — a compromise that passive strakes, by virtue of their fixed geometry, cannot escape.

A second shortfall in the literature concerns the representation of AFC systems. While most AFC studies have relied on two-dimensional model geometries, [26, 27] actual nacelle-wing junction flows are inherently three-dimensional - characterized by horseshoe vortices, secondary flow structures, and periodic separation regions that depend heavily on junction geometry, Reynolds number (Re), and α . Pneumatic air injection research in particular has remained largely confined to simple geometric models, [28, 31] and few experiments have examined actively controlled nacelle strakes in configurations representative of commercial aircraft. As a result, the realistic three-dimensional behavior of AFC systems at nacelle-wing

junctions remains inadequately described, and more detailed experimental data are required before definitive conclusions can be drawn about their effectiveness in this environment.

Building upon the experimental platform developed by Spitzley et al., the present investigation addresses several remaining knowledge gaps by expanding both the passive and active nacelle-mounted strake investigations. Four additional passive i_{strake} configurations were developed to expand the passive nacelle geometry beyond the original study. In addition, interchangeable flap assemblies representing representative cruise, takeoff, and landing configurations were developed to investigate the influence of flap deployment on passive and active nacelle-mounted strake performance. The active pneumatic testing methodology was also modified to isolate the aerodynamic influence of individual pneumatic injection locations.

This thesis addresses that gap by experimentally comparing passive and active nacelle-mounted strakes in a configuration representative of nacelles of commercial aircraft. Special emphasis is given to determining how changes in i_{strake} , δ_f , and pneumatic air injection influence the resulting aerodynamic performance — specifically C_D and C_L measurements and computed L/D values under low-speed wind tunnel testing conditions.

Chapter 3

DESIGN

3.1 *Background*

The objective of this thesis was to determine how a passive and active nacelle-mounted strake influences the aerodynamic characteristics of a representative commercial aircraft wing model as functions of i_{strake} , δ_f , and pneumatic air injection under subsonic wind tunnel conditions. The wing model includes a wing-pylon-nacelle system and particular attention was given to evaluating the influence of each variable on C_L , C_D , and L/D . L/D values were selected as the primary measure of aerodynamic efficiency because they simultaneously account for changes in both lift and drag, thereby providing a comprehensive assessment of the effectiveness of the PFC and AFC strategies investigated in this research.

The experiments were conducted in the University of Washington Aeronautical Engineering Research Laboratory (AERL) 3 ft $\times$ 3 ft low-speed wind tunnel at a Reynolds number based on the mean aerodynamic wing chord of $Re_{\bar{c}} \approx 1.93 \times 10^5$, where $(\bar{c})$ is the aerodynamic wing chord as determined in Appendix B. The freestream Mach number (M_∞) remained below 0.30. Consequently, compressibility effects were considered negligible, allowing the flow to be treated as incompressible throughout the investigation. The resulting aerodynamic coefficients therefore represent time-averaged, quasi-steady measurements that are well suited for comparing the relative aerodynamic performance of the various passive and active strake configurations evaluated in this thesis.

The dominant aerodynamic phenomena governing the wing model include adverse pressure gradients, three-dimensional boundary layer development, streamwise vortex formation within the nacelle-wing junction, and flow separation over the wing upper surface. These aerodynamic effects become increasingly significant as α increases and are further intensified when flaps are deployed during takeoff and landing to increase wing lift. Collectively,

these complex flow phenomena govern the aerodynamic performance of the wing model and provide the physical basis for the PFC and AFC concepts investigated throughout this thesis.

The installation of passive nacelle-mounted strakes modifies the local flow field by generating coherent streamwise vortices that transport high-momentum freestream fluid toward the wing upper surface. As a result, the boundary layer receives additional momentum, increasing its resistance to adverse pressure gradients and delaying the onset of flow separation. For the active strake configurations, pneumatic air injection provides additional momentum to the developing vortex, allowing its strength and resulting aerodynamic influence to be modified through controlled blowing. Consequently, the AFC system provides an additional means of manipulating the local flow field beyond that achievable through passive vortex generation alone.

Since the primary objective of this thesis is to evaluate the relative aerodynamic performance of numerous passive and active strake configurations rather than to directly solve the governing flow equations, the theoretical treatment presented in the following sections is limited to the physical relationships required to interpret the experimental measurements. These governing relationships provide the theoretical framework used to explain the aerodynamic mechanisms responsible for the measured aerodynamic forces and efficiency presented throughout the remainder of this thesis.

3.2 Modeling and Governing Equations

The aerodynamic flow about the wing model is governed by three-dimensional incompressible Navier-Stokes equations, which express mass and momentum conservation for a Newtonian fluid [3]. Because the experiments were conducted in a low-speed wind tunnel with $M_\infty \ll 0.30$, compressibility effects were considered negligible, allowing the flow throughout the test section to be treated as incompressible.

The aerodynamic behavior of the wing model is evaluated experimentally through direct measurements of the integrated aerodynamic forces acting on the model. Consequently, the

aerodynamic response is characterized using several non-dimensional parameters that facilitate comparison between the various passive and active strake configurations investigated throughout this thesis.

The primary similarity parameter governing the present study is the Reynolds number (Re) as shown in Equation 3.1,

$$Re = \frac{\rho_{\infty} U_{\infty} L}{\mu} = \frac{U_{\infty} L}{\nu} \quad (3.1)$$

where U_{∞} is the freestream velocity, ρ_{∞} is the freestream density, L is the characteristic length, and ν is the kinematic viscosity. In the present study, L is taken as the mean aerodynamic wing chord $\bar{c}$ of the wing, consistent with standard aerodynamic practice for finite wings. This leads to Equation 3.2,

$$Re_{\bar{c}} = \frac{U_{\infty} \bar{c}}{\nu} \quad (3.2)$$

U_{∞} is directly obtained from wind tunnel freestream dynamic pressure (q_{∞}) measurements as shown in Equation 3.3 as,

$$q_{\infty} = \frac{1}{2} \rho_{\infty} U_{\infty}^2 \quad U_{\infty} = \sqrt{\frac{2q_{\infty}}{\rho_{\infty}}} \quad (3.3)$$

For the nominal operating condition of $q_{\infty} \approx 2$ psf, the resulting $Re_{\bar{c}}$ value from Equation 3.2 based on $\bar{c}$ was computed from Appendix B as,

$$Re_{\bar{c}} \approx 1.93 \times 10^5. \quad (3.4)$$

Maintaining this laminar $Re_{\bar{c}}$ value throughout the experimental program ensured that all passive and active strake configurations were evaluated under essentially identical aerodynamic conditions, thereby permitting meaningful comparisons between the various configurations.

Although the present investigation was conducted at $Re_{\bar{c}} \approx 1.93 \times 10^5$, a representative full-scale Boeing 737-800 operating under cruise conditions experiences $Re \approx 2.34 \times 10^7$, which is approximately 123 times larger than that of the present wind tunnel model. This difference is primarily a consequence of the reduced geometric scale of the experimental model and the lower U_{∞} used during testing.

Despite this Re disparity, the primary objective of the present investigation is to compare the relative aerodynamic performance of passive and active nacelle-mounted strake configurations under identical experimental conditions rather than reproduce the absolute aerodynamic performance of a full-scale aircraft. Since every configuration was evaluated at the same $Re_{\bar{c}}$, the measured differences in C_L , C_D , and L/D values provide a consistent basis for assessing the relative aerodynamic effectiveness of the investigated passive and active strake concepts. Although Re influences boundary layer development, transition, and flow separation, the aerodynamic trends identified throughout this investigation provide useful insight into the relative behavior of the investigated configurations while establishing a foundation for future studies conducted at higher Re .

The aerodynamic forces acting on the model were measured using the six-component wind tunnel balance and subsequently non-dimensionalized in terms of C_L and C_D in Equation 3.5 as,

$$C_L = \frac{L_w}{q_{\infty} S_{ref}}, \quad C_D = \frac{D_w}{q_{\infty} S_{ref}} \quad (3.5)$$

Where L_w is the wind-axis lift force, D_w is the wind-axis drag force, and S_{ref} is the wing reference area.

Since the objective of this thesis is to evaluate the overall aerodynamic efficiency of the various passive and active strake configurations, the primary performance metric used to measure this efficiency are the L/D values computed in Equation 3.6 as,

$$\frac{L_w}{D_w} = \frac{C_L}{C_D}. \quad (3.6)$$

Unlike the individual C_L and C_D values, the L/D values simultaneously account for aerodynamic improvements in lift while also considering the associated drag penalties. Consequently, they serve as the principal metric used throughout Chapter 5 to compare the relative aerodynamic performance of the PFC and AFC strategies investigated in this thesis.

For active strake configurations using pneumatic air injection, the strength of the pneumatic injection may be characterized by the blowing coefficient as shown in Equation 3.7 as,

$$C_\mu = \frac{\dot{m}_j V_j}{\frac{1}{2} \rho_\infty U_\infty^2 S_{ref}} = \frac{2 \rho_j A_j V_j^2}{\rho_\infty U_\infty^2 S_{ref}} \quad (3.7)$$

where $\dot{m}_j$ is the jet mass flow rate through a strake exit hole, ρ_j is the jet air density through a strake exit hole, A_j is the jet exit area through a strake exit hole, and V_j is the jet exit velocity of air blowing through a strake exit hole.

The governing equations and non-dimensional parameters presented in this section establish the theoretical framework used to interpret the experimental results discussed throughout the remainder of this thesis. Rather than predicting the flow analytically, these relationships provide the physical basis for explaining the aerodynamic mechanisms responsible for the measured aerodynamic forces and efficiency of the passive and active nacelle-mounted strakes.

3.3 *Experimental Setup*

Experimental testing was conducted in the University of Washington AERL 3 ft $\times$ 3 ft low-speed wind tunnel. The facility consists of an open-return wind tunnel with a test section measuring 3 ft $\times$ 3 ft $\times$ 8 ft and is capable of generating U_∞ up to 135 mph. The wind tunnel is routinely utilized for instructional and research activities within the Department of Aeronautics and Astronautics. A summary of the primary wind tunnel parameters is

provided in Table 3.1, while a photograph of the facility is shown in Figure 3.1.

Table 3.1: University of Washington AERL 3 ft $\times$ 3 ft Low-Speed Wind Tunnel Specifications

Parameter	Value
Tunnel Type	Open Return
Test Section	3 ft $\times$ 3 ft $\times$ 8 ft
Maximum U_∞	135 mph
Contraction Ratio	9:1
Facility	UW AERL



Figure 3.1: University of Washington AERL 3 ft $\times$ 3 ft low-speed wind tunnel.

3.3.1 Model Mounting System

A circular 17 in diameter T6 6061 aluminum splitter plate with an embedded 2 in diameter ATI half-body force balance located in the center of the splitter plate was installed in the

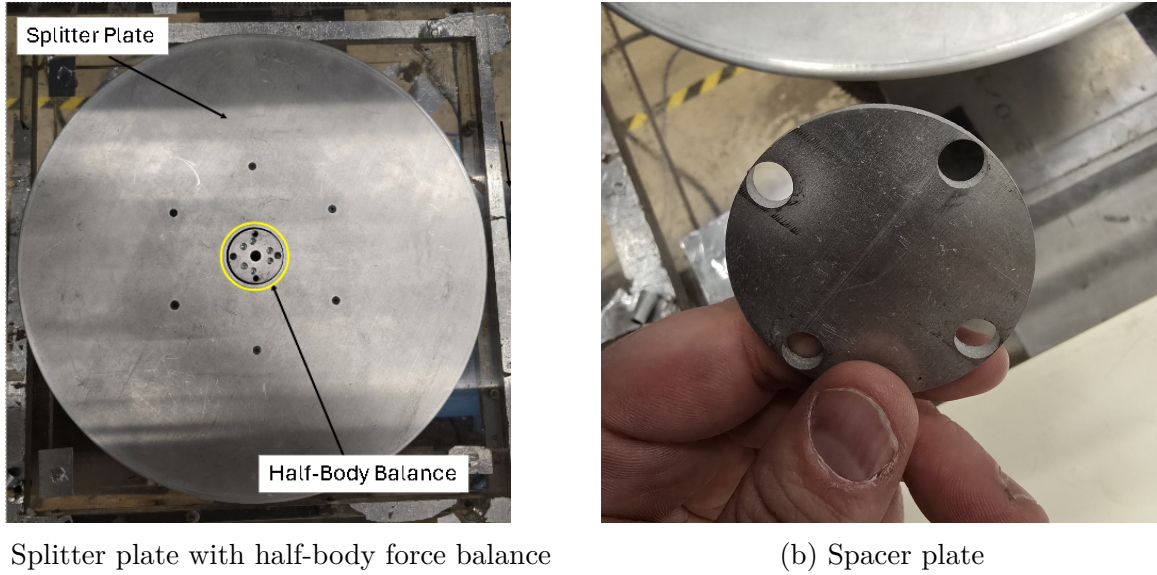


Figure 3.2: (a) Splitter plate integrated with the half-body force balance mounted in the wind tunnel test section and (b) spacer plate used between the half-body force balance and wing.

wind tunnel test section floor as shown in Figure 3.2a. The splitter plate with the half-body force balance was elevated approximately 3.25 in above the test section floor and provided an aerodynamic symmetry plane while shielding the mounting hardware from the freestream flow. The splitter plate served as the rotatable surface where the wing model was mounted during testing and the half-body force balance served to measure aerodynamic loads. See Chapter 4 for a schematic of the splitter plate and half body balance as well as to reference the force and moment limits of the half-body force balance. A custom-machined 2 in diameter T6 6061 aluminum spacer plate shown in Figure 3.2b was installed between the half-body force balance within the splitter plate, and the wing model to maintain adequate separation between these components during testing. Maintaining this separation prevented unintended contact between the wing model and the splitter plate, and ensured that all aerodynamic loads were transmitted exclusively through the half-body force balance, eliminating parasitic load paths that could adversely affect measurement accuracy.

The wing model was mounted vertically in the test section on the splitter plate and half-body force balance in a semi-span configuration using a custom machined T6 6061 aluminum

mount plate that was designed and fabricated by Spitzley et al. and shown in Figure 3.3 [33]. The mount plate served as the primary structural interface to disperse aerodynamic loads through the wing and maintain proper alignment of the wing model within the test section. Internal pockets were machined into the mount plate to reduce weight while preserving structural stiffness.



Figure 3.3: Custom-machined aluminum mount plate used to interface the wing model with the half-body force balance [33].

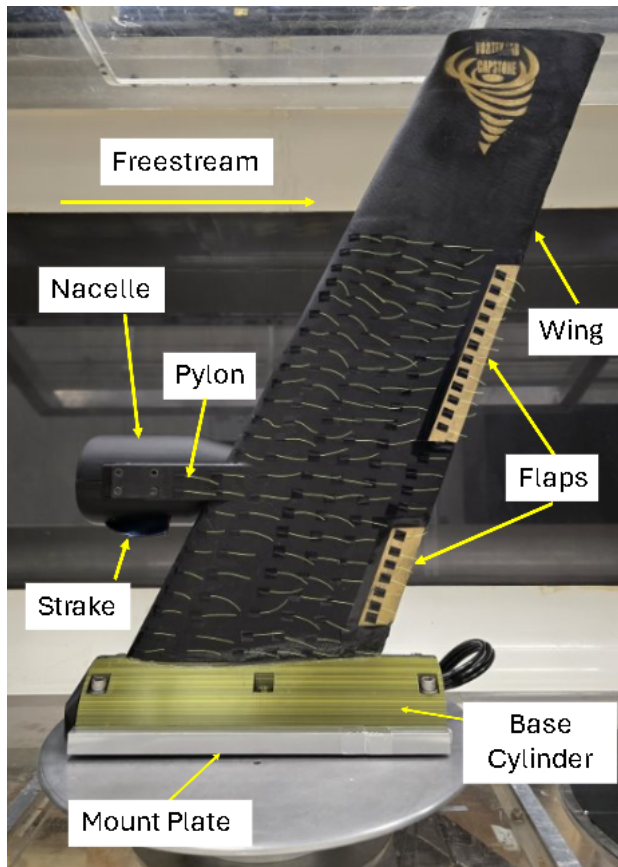
The wing model α was adjusted using an external manual indexing mechanism connected to the half-body force balance. The indexing mechanism incorporated 1° graduations to facilitate repeatable α positioning. Additional details regarding the α test parameters and testing procedure are presented in Chapter 4.

3.3.2 Wing, Pylon, and Nacelle Assemblies

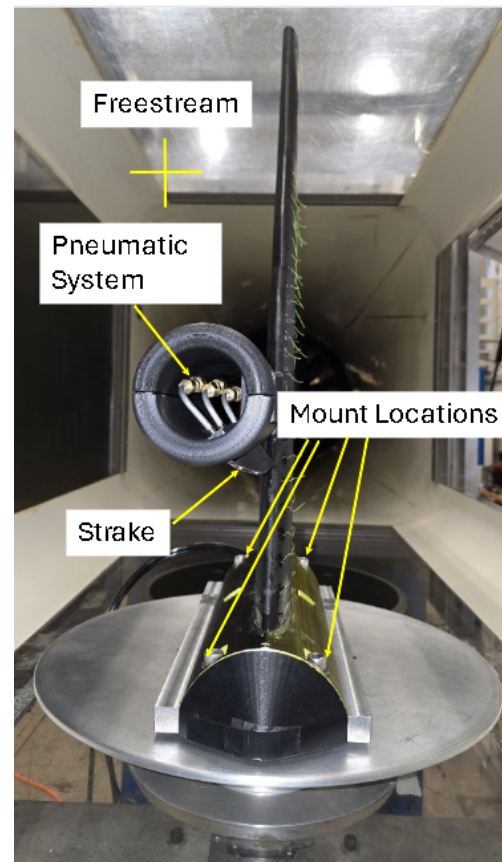
A three-dimensional semi-span wing model representative of a Boeing 737-800 wing planform, originally designed and fabricated by Spitzley et al. was utilized throughout the present investigation, as shown in Figures 3.4a and 3.4b [33]. The model utilized a NACA 2412 airfoil section and consisted of five individual wing segments, a pylon, and a cylindrical base structure fabricated using polylactic acid (PLA) additive manufacturing techniques.

The wing segments were assembled using structural epoxy adhesive and reinforced using two 1045 steel round stock spars. The forward spar was positioned near the quarter-chord location, while the aft spar was positioned near the three-quarter-chord location. These spars extended through the semi-span of the wing model and increased overall structural rigidity. 1045 steel was selected due to its availability, ease of machining, and ability to provide adequate stiffness for the anticipated aerodynamic loading conditions.

To minimize aeroelastic deformation during testing, the fused wing segments were wrapped with two 0.03 in thick plies of 120 4H satin weave fiberglass using West Systems 2000 epoxy resin. The fiberglass reinforcement significantly increased structural stiffness while providing a smooth aerodynamic surface finish. The final wing geometry possessed a semi-span of 32.28 in, a wing root chord (c_r) of 10.66 in, a wing tip chord (c_t) of 6.90 in, and S_{ref} of 258.85 in². The leading-edge and trailing-edge sweep angles were 63.49° and 69.80° respectively. The wing model's geometric parameters are summarized in Table 3.2 for reference.



(a) Side view of wing



(b) Front view of wing

Figure 3.4: (a) Side view of wing model mounted in the wind tunnel test section; each component of wing is labeled for clarity. (b) Front view of the wing model showing the mounting hardware locations and the active strake pneumatic system housed within the nacelle [33].

Table 3.2: Semi-Span Wing Model Geometry

Parameter	Value
Airfoil	NACA 2412
Semi-Span	32.28 in
Root Chord (c_r)	10.66 in
Tip Chord (c_t)	6.90 in
Wing Reference Area (S_{ref})	258.85 in ²
Leading Edge Sweep	63.49°
Trailing Edge Sweep	69.80°

The original experimental platform developed by Spitzley et al. consisted of baseline nacelle geometries corresponding to passive i_{strake} configurations of 5°, 10°, and 15° [33]. Building upon this platform, four additional nacelle geometries corresponding to passive i_{strake} configurations of 3°, 8°, 13°, and 18° were designed and fabricated as part of the present investigation. Collectively, seven interchangeable length engine nacelles in total were fabricated using PLA, as shown in Figure 3.5a. The nacelles incorporated mounting provisions corresponding to passive i_{strake} configurations of 3°, 5°, 8°, 10°, 13°, 15°, and 18° relative to the freestream direction. The baseline no-strake configuration was achieved by covering both the exterior and interior strake mounting surfaces with black vinyl tape, as depicted in Figure 3.5b. The interchangeable nacelle design enabled rapid replacement while maintaining repeatable passive i_{strake} positioning throughout the experimental program.

Each nacelle had a length of 7.75 in, an outer diameter of 4.00 in, an inner diameter of 2.50 in, and an integrated strake slot oriented for a specific i_{strake} configuration. The inner diameter dimension was chosen to provide enough spatial clearance for mounting the passive-active strake inside the engine nacelle. CAD models showcasing the dimensions of the nacelle size are shown below in Figures 3.6 and 3.7.

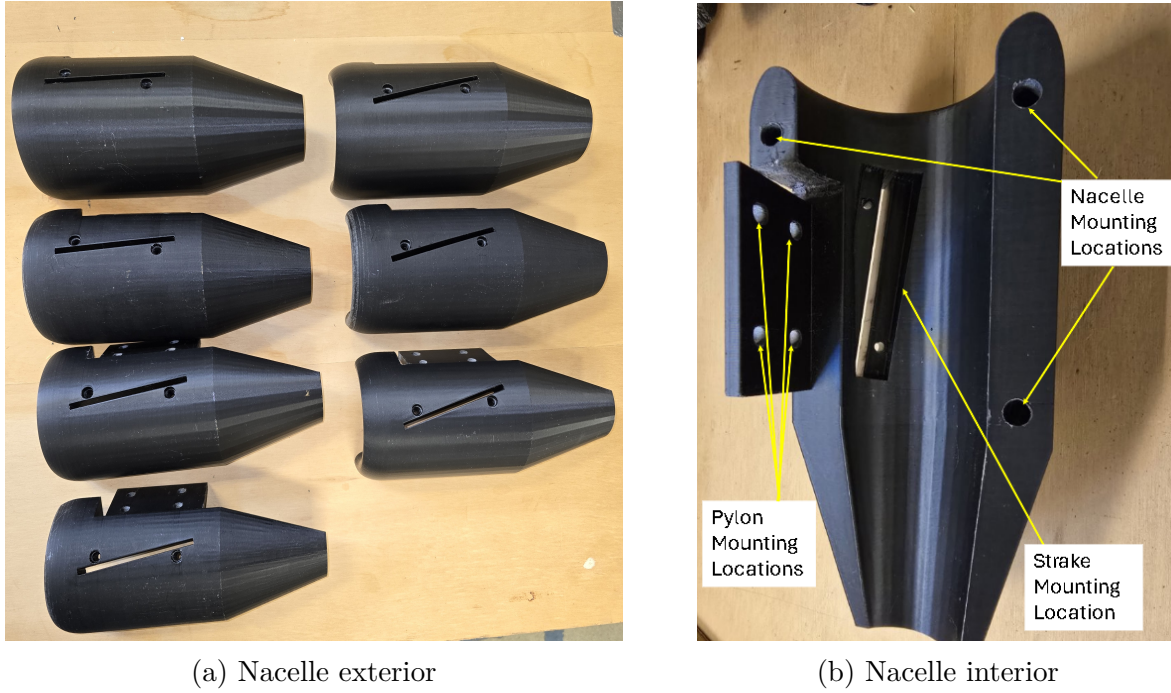


Figure 3.5: (a) Nacelle exterior surfaces for the different i_{strake} configurations and (b) the nacelle interior surface showing various mounting locations.

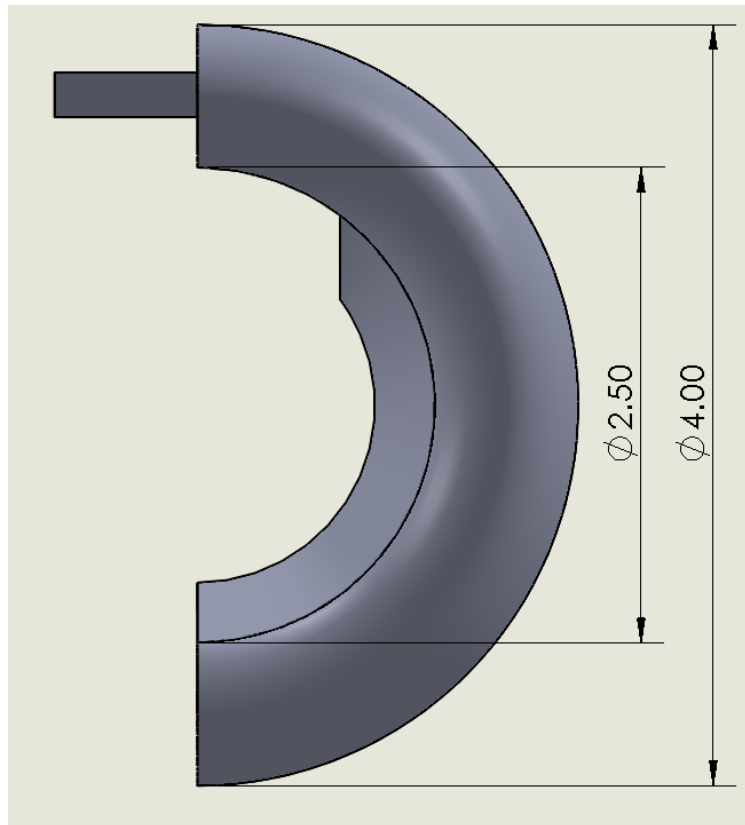


Figure 3.6: CAD model of the front of the nacelle showing the 4.00 in outer diameter and the 2.50 in inner diameter [33].

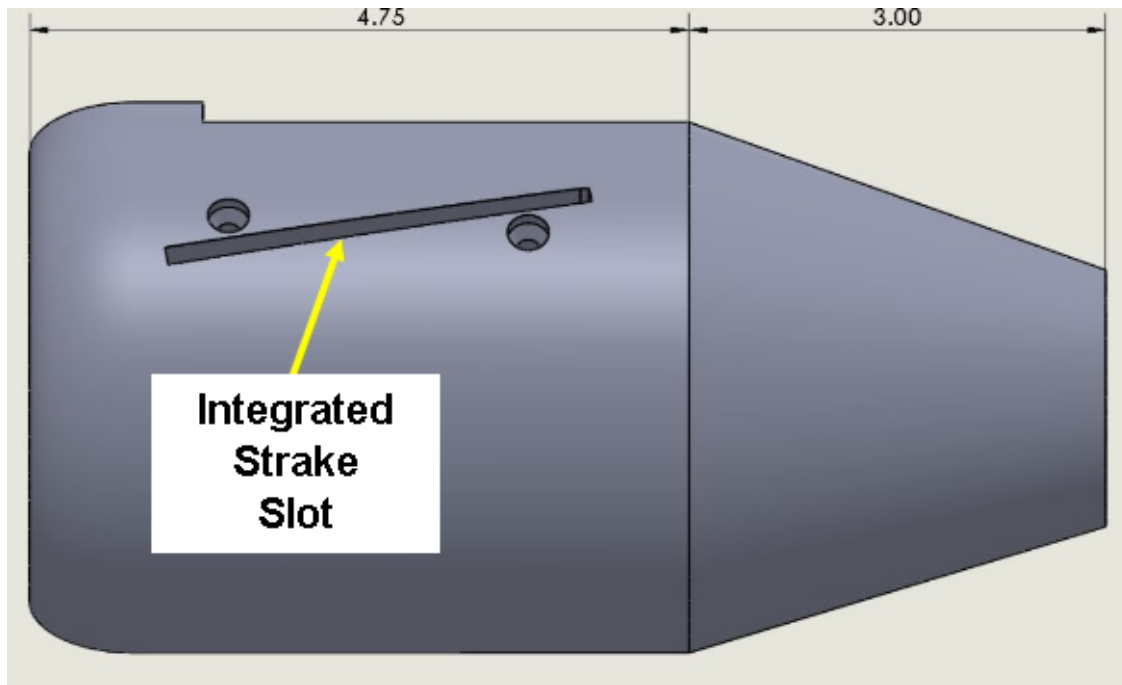


Figure 3.7: CAD model of the side of the nacelle showing the 7.75 in length and the integrated strake slot to mount the passive-active strake [33].

A CAD model showing the wing model built with the aforementioned components by Spitzley et al. is shown Figure 3.8 for reference [33].

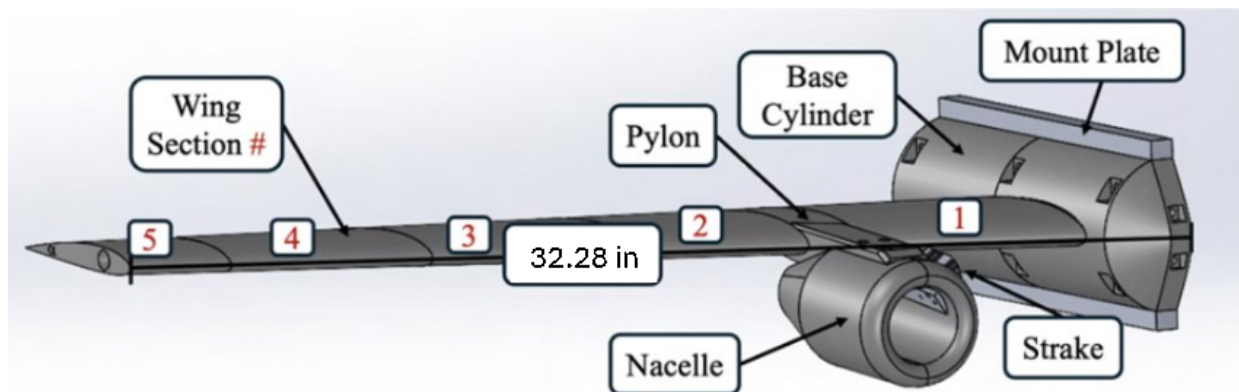


Figure 3.8: CAD model of the wing model used for testing showing the wing sections and components that were built along with the semi-span dimension [33].

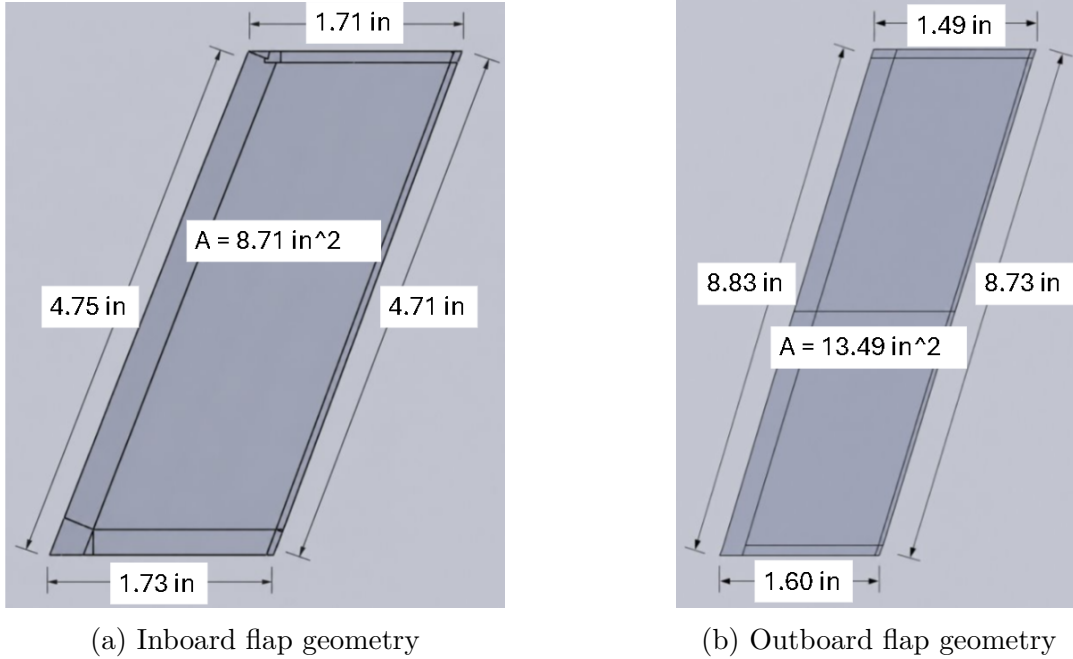


Figure 3.9: CAD geometries and principal dimensions of the (a) inboard and (b) outboard flaps. The inboard and outboard flap planform areas were 8.71 in^2 and 13.49 in^2 , respectively.

3.3.3 Flap Assembly Sets

The original experimental platform developed by Spitzley et al. did not include interchangeable flap assemblies. To enable representative cruise, takeoff, and landing flight configurations, three sets of interchangeable flap assemblies were designed and fabricated as part of the present investigation. The flap assemblies were fabricated from PLA and reinforced using two 0.03 in thick plies of Fibreglast Style 120 E-Glass fiberglass. Each flap assembly set consisted of both an inboard flap and an outboard flap representative of a commercial aircraft high-lift system. δ_f configurations of 0° , 20° , and 45° were selected to represent cruise, takeoff, and landing flight configurations, respectively.

The CAD models of the inboard and outboard flaps are presented in Figure 3.9. Principal dimensions are provided to document the flap geometries used throughout the experimental investigation. The inboard flap possessed a planform area of 8.71 in^2 , while the outboard flap possessed a planform area of 13.49 in^2 , resulting in a combined flap planform area of 22.20 in^2 . The same flap planform geometries were used for all three δ_f configurations.

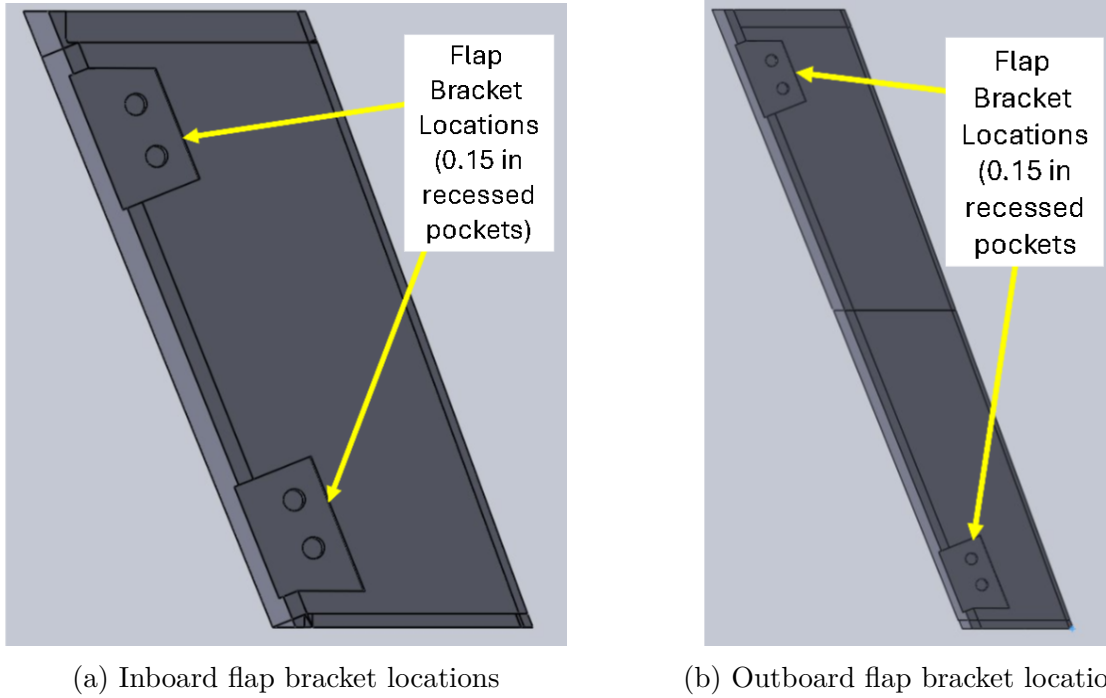


Figure 3.10: 0.15 in recessed pockets used as the bracket mounting locations into the (a) inboard and (b) outboard flaps.

Figure 3.10 presents the CAD models that illustrate the 0.15 in recessed pockets on the forward underside of the flaps that were used as the bracket mounting locations. Two aluminum mounting brackets were used for each flap to maintain the prescribed δ_f configuration while providing sufficient structural rigidity during aerodynamic loading. The bracket locations were selected to provide repeatable installation and minimize deformation throughout the experimental program.

CAD models of the inboard and outboard flap assemblies integrated with the wing are shown in Figure 3.11 for the $\delta_f = 20^\circ$ configuration. The underside views shown in Figures 3.11a and 3.11b illustrate the bracket arrangement used to secure the inboard and outboard flaps to the wing structure respectively. The section views shown in Figures 3.11c and 3.11d illustrate the relative positioning of the wing, flap, and mounting brackets. The brackets maintained the prescribed δ_f configuration while providing a rigid and repeatable connection between the flap and wing assemblies. Although the $\delta_f = 20^\circ$ configuration is shown in Figure 3.11 as a representative example, the same mounting approach was also implemented for the

$\delta_f = 0^\circ$ and 45° configurations used during testing.

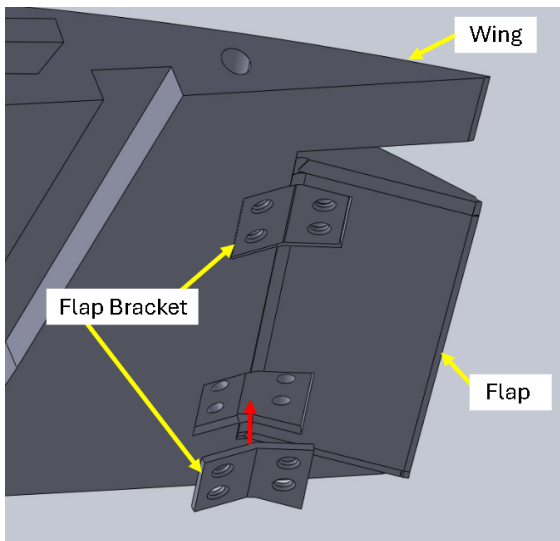
The completed flap assembly sets together with the custom aluminum mounting brackets are shown in Figure 3.12a. Custom flap brackets measuring approximately 1 in $\times$ 1 in $\times$ 0.125 in were machined from T6 6061 aluminum bar stock and maintained the prescribed δ_f configuration while minimizing structural deformation under aerodynamic loading. Heat-set threaded inserts were installed within both the wing and flap assemblies to provide durable threaded attachment points and facilitate repeated flap installation and removal throughout testing.

The 0.15 in recessed pockets shown in Figure 3.10 were incorporated into both the wing and flaps so that the aluminum brackets remained flush with the surrounding aerodynamic surfaces. This minimized geometric discontinuities near the flap interfaces and improved aerodynamic consistency between flap installations. Black vinyl tape was applied along the wing-flap interfaces to seal gaps and minimize unintended flow leakage and local flow disturbances during testing.

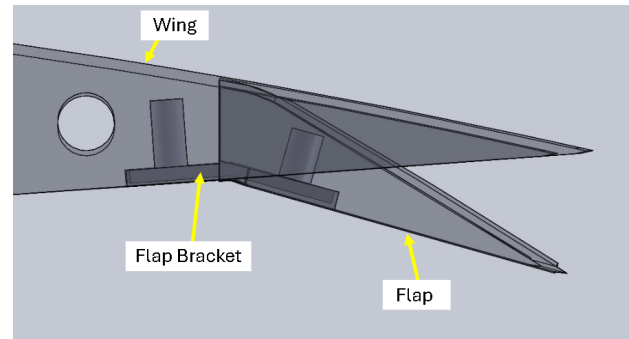
3.3.4 Passive and Active Strake Design

A hybrid passive-active nacelle-mounted strake originally designed and fabricated by Spitzley et al. was machined from T6 6061 aluminum, as shown in profile views in Figures 3.13a and 3.13b [33]. The strake was designed such that the same hardware could be utilized for both PFC and AFC investigations and could be swapped out for use in each nacelle easily. During passive testing, the strake functioned solely as a vortex-generating aerodynamic surface.

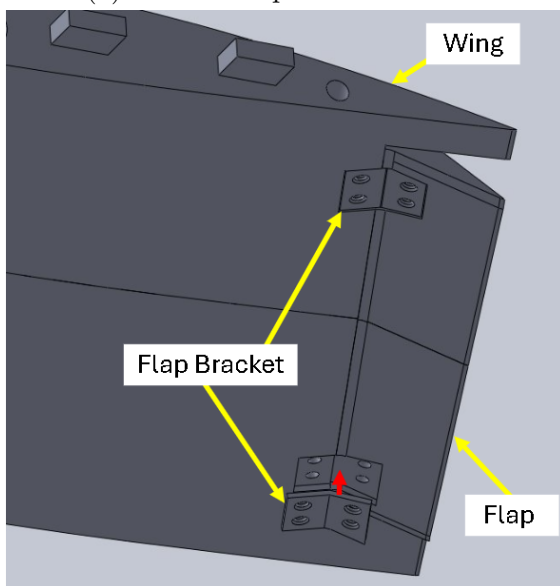
For active strake testing, three independent 0.079 in diameter holes representing the forward pneumatic injection location (H1), middle pneumatic injection location (H2), and the aft pneumatic injection location (H3) were integrated into the base of the strake as shown in Figure 3.14 [33]. Each injection location supplied air through a dedicated internal pneumatic passage incorporated directly within the aluminum strake structure before exiting



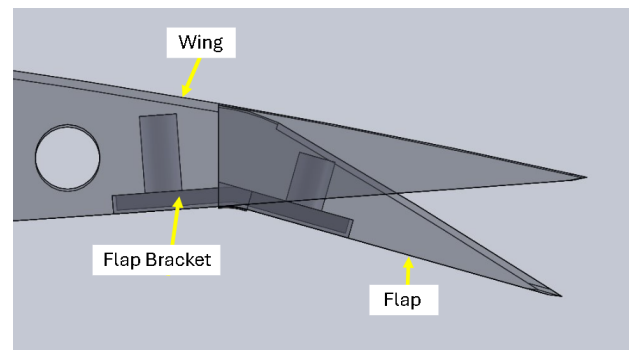
(a) Inboard flap underside view



(b) Inboard flap section view



(c) Outboard flap underside view



(d) Outboard flap section view

Figure 3.11: CAD models of the flap-to-wing integration at $\delta_f = 20^\circ$ showing the (a) inboard flap underside view, (b) inboard flap section view, (c) outboard flap underside view, and (d) outboard flap section view. The views illustrate the mounting brackets used to secure the flaps to the wing at the prescribed δ_f configuration.

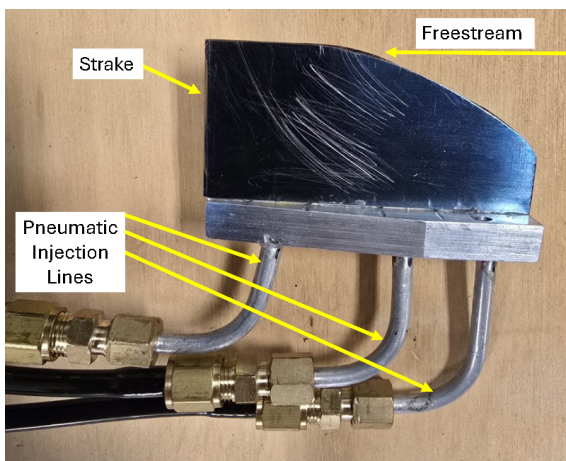


(a) Flap assembly sets

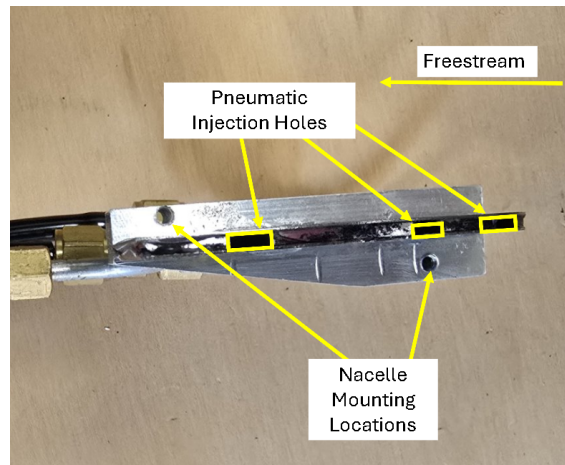


(b) Custom aluminum flap brackets

Figure 3.12: (a) Inboard and outboard flap assembly sets representing cruise, takeoff, and landing configurations and (b) aluminum brackets fabricated to mount the flaps onto the wing.



(a) Passive-active strake side profile



(b) Passive-active strake top profile

Figure 3.13: (a) Side profile of the passive-active strake showing the pneumatic injection lines feeding into the base of the strake and (b) top profile of the passive-active strake showing the nacelle mounting locations along with the pneumatic injection holes [33].

through 0.0256 in^2 rectangular holes as shown in Figure 3.15 [33]. These injection locations were selected to investigate the influence of momentum addition location on the resulting aerodynamic response.

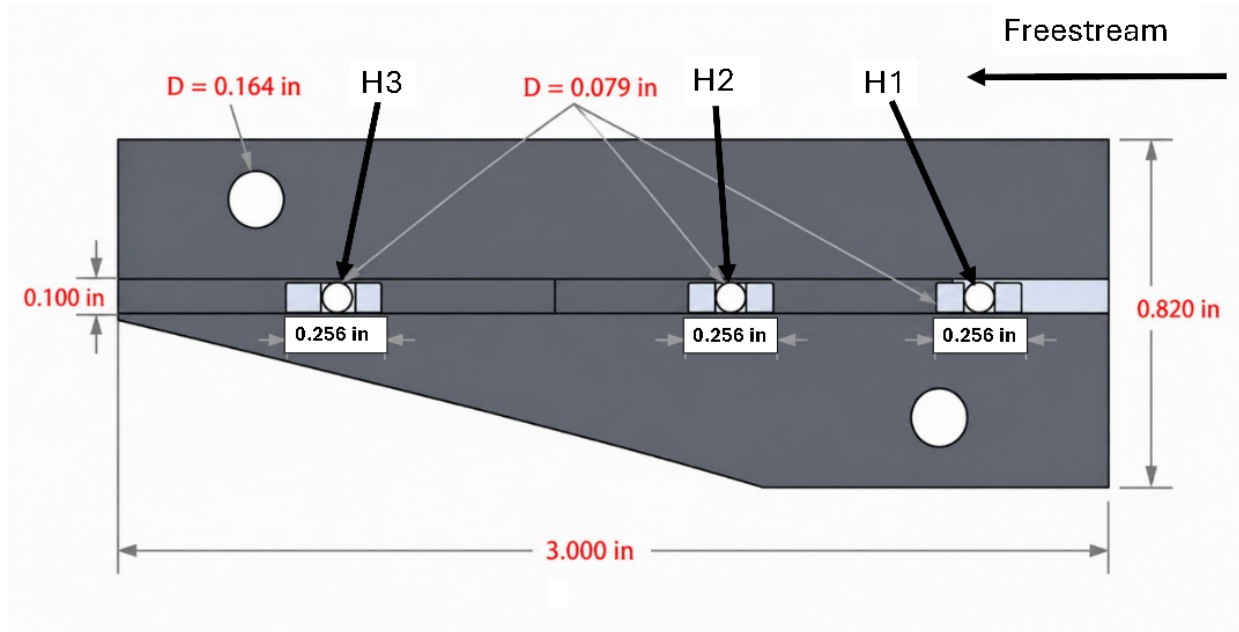


Figure 3.14: Strake base showing the three 0.0079 in diameter entry injection holes along with the 0.0256 in^2 rectangular holes. H1 represents the forward pneumatic injection location, H2 represents the middle pneumatic injection location, and H3 represents the aft pneumatic injection location [33].

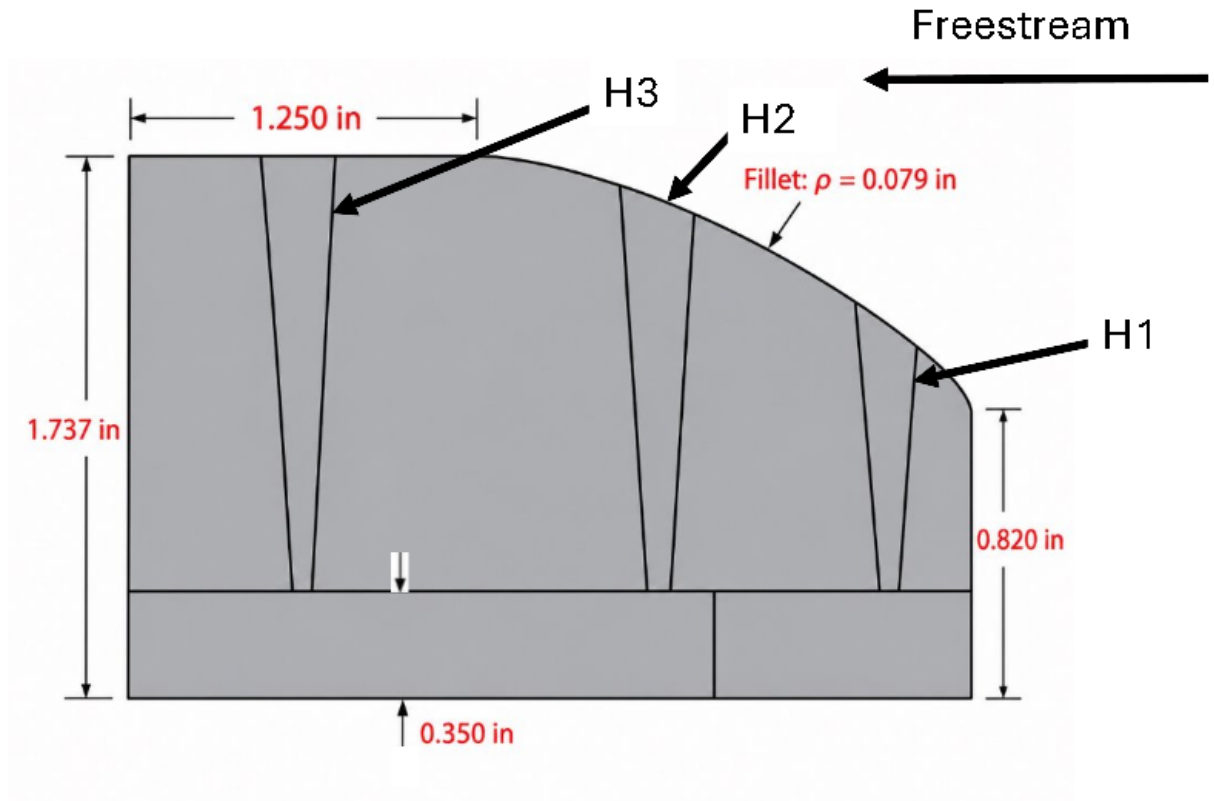


Figure 3.15: Detail view showing the air passages through the strake. [33].

3.3.5 Pneumatic System

The pneumatic system utilized throughout this investigation was originally designed and fabricated by Spitzley et al. [33]. The pneumatic system consisted of a custom pneumatic manifold designed to provide regulated compressed air to the active strake. The complete pneumatic system is shown in Figure 3.16. Laboratory compressed air entered the system through a flexible pneumatic supply line and passed through a SMC AR30-N02G1-NZ-B pressure regulator. Downstream of the regulator, an Ashcroft analog pressure gauge was used to continuously monitor system pressure during testing.

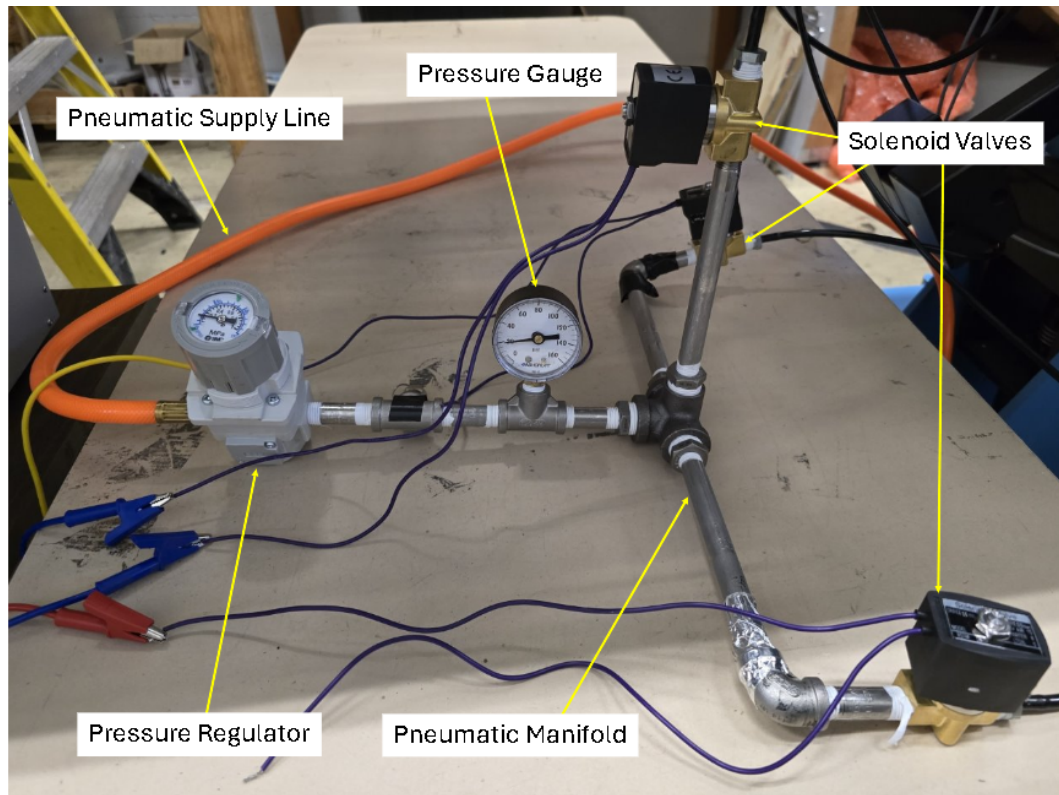


Figure 3.16: Pneumatic system used to control compressed air through the active strake [33].

Following regulation, airflow entered a steel distribution manifold supplying three independent pneumatic circuits. Each circuit incorporated an electrically actuated normally closed 12 VDC Atlantic Valves RSSM-3 pneumatic solenoid valve that controlled airflow to a single injection location within the active strake. Three solenoid valves were utilized, one for each injection location (H1, H2, and H3), thereby enabling each injection location to be independently activated.

Electrical power for pneumatic actuation was supplied by a Korad KD3303D regulated DC power supply operating at approximately 13 VDC, as shown in Figure 3.17. Independent electrical circuits were used to energize each solenoid valve, thereby providing individual control of each injection location.

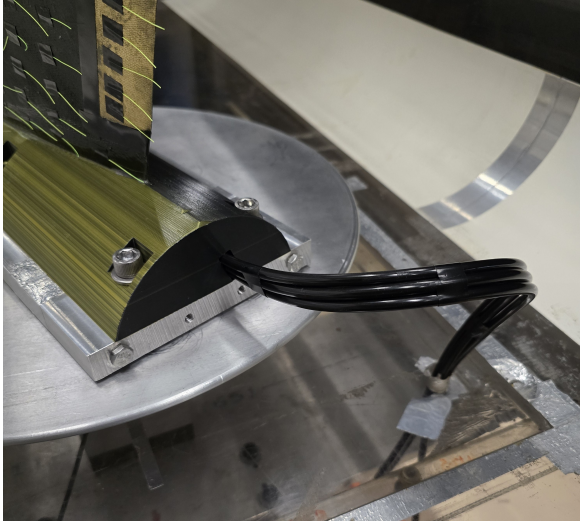


Figure 3.17: Power supply used to actuate the pneumatic solenoid valves.

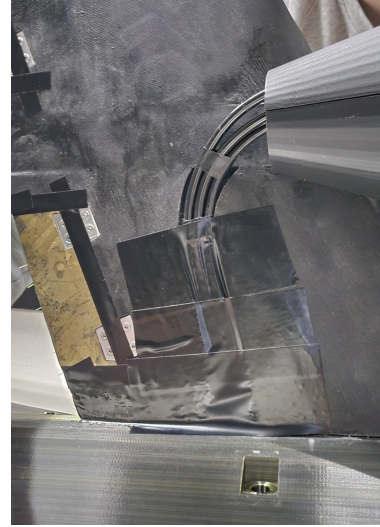
Compressed air exiting the solenoid valves was routed through flexible pneumatic injection lines around the perimeter of the splitter plate before entering the base cylinder as shown in Figure 3.18a. Internal channels were milled into the base cylinder and wing root directed the tubing beneath the wing and toward the nacelle assembly, as shown in Figure 3.18b. The tubing subsequently entered the nacelle and connected directly to the internal pneumatic passages integrated within the active strake.

Black vinyl tape was used to secure the pneumatic injection lines within the milled channels and cover recessed passages. This arrangement prevented line movement during testing while minimizing aerodynamic interference associated with exposed pneumatic hardware. This strategy also helped protect the pneumatic lines from damage while maintaining compact external geometry.

All active strake experiments were conducted using a nominal supply pressure of 20 psi. During a given test condition, only one injection location was activated at a time to isolate



(a) Routing lines through wing model



(b) Routing lines to active strake in nacelle

Figure 3.18: (a) View showing the pneumatic injection lines entering through the wind tunnel test section floor and routing through the base cylinder and (b) view showing the pneumatic injection lines being routed into the internal channels on the underside of the wing and feeding into the strake mounted inside the nacelle.

the aerodynamic influence of the selected blowing location. A summary of the pneumatic system and active strake parameters is provided in Table 3.3.

Table 3.3: Pneumatic System and Active Strake Specifications

Parameter	Value
Supply Pressure	20 psi
Pressure Regulator Model	SMC AR30-N02G1-NZ-B
Power Supply Model	Korad KD3303D
Operating Voltage	~13 VDC
Solenoid Valve Model	Atlantic Valves RSSM-3 (3 QTY)
Injection Locations	H1, H2, H3
Strake Material	T6 6061 Aluminum

3.3.6 Flow Visualization and Instrumentation

Qualitative flow visualization was performed using approximately 200 surface tufts fabricated from high-visibility sewing thread. The tuft arrangement used throughout testing is shown in Figure 3.4a and 3.12a. The tufts were distributed across the wing upper surface and each set of flap assemblies to provide visual indication of local flow direction, vortex behavior, and flow separation characteristics. Photographs and video recordings obtained during testing were used to supplement aerodynamic force measurements and identify regions of attached and separated flow.

Aerodynamic forces were measured using the facility half-body force balance and recorded through a National Instruments data acquisition (DAQ) system operating within LabVIEW. q_∞ measurements obtained from the wind tunnel instrumentation were used to establish the desired test conditions and corresponding U_∞ values. Force measurements acquired from the half-body force balance were subsequently transformed into lift and drag components during post-processing.

Testing was conducted for passive and active strake configurations over a range of α and δ_f configurations representative of cruise, takeoff, and landing flight conditions. Additional details regarding testing procedures, data acquisition, aerodynamic coefficient calculations, and uncertainty analysis are presented in Chapter 4.

Chapter 4

TESTING

4.1 Chapter Overview

In this chapter, the experimental procedures used to assess the aerodynamic performance of the no-strake baseline, passive strake, and active strake configurations are described. The experimental equipment previously described in Chapter 3 was tested in the University of Washington AERL 3 ft $\times$ 3 ft low-speed wind tunnel. The aerodynamic force measurements obtained during testing were acquired using the wind tunnel DAQ system and were subsequently analyzed to determine C_L , C_D , and L/D values for each test configuration.

4.2 Test Conditions

Each experiment was conducted at a nominal $q_\infty = 2$ psf. In addition, q_∞ was continuously monitored during testing using the wind tunnel instrumentation and LabVIEW interface, as shown in Figure 4.1. To ensure good repeatability of results among different test runs, the wind tunnel q_∞ was gradually increased until reaching a range of 2 ± 0.1 psf before starting each test run. The wind tunnel q_∞ typically required approximately 120 s on average to stabilize within the desired 2 ± 0.1 psf range before each test began.

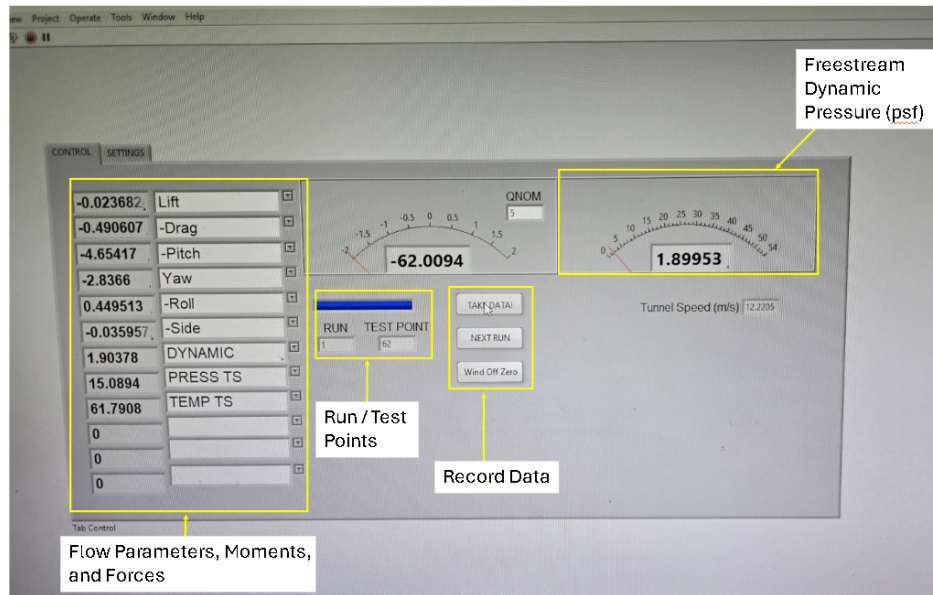


Figure 4.1: LabVIEW interface used for recording wind tunnel data.

The selected nominal q_∞ of 2 psf represented a compromise between maximizing the measurable aerodynamic loads and remaining within the operation limits of the half-body force balance as depicted in Figure 4.2. Additionally, a modestly high q_∞ was needed to produce measurable aerodynamic forces and moments on the wing model. Finally, the selected test condition was sufficient to capture the stall onset and post-stall behavior across the entire α testing range.

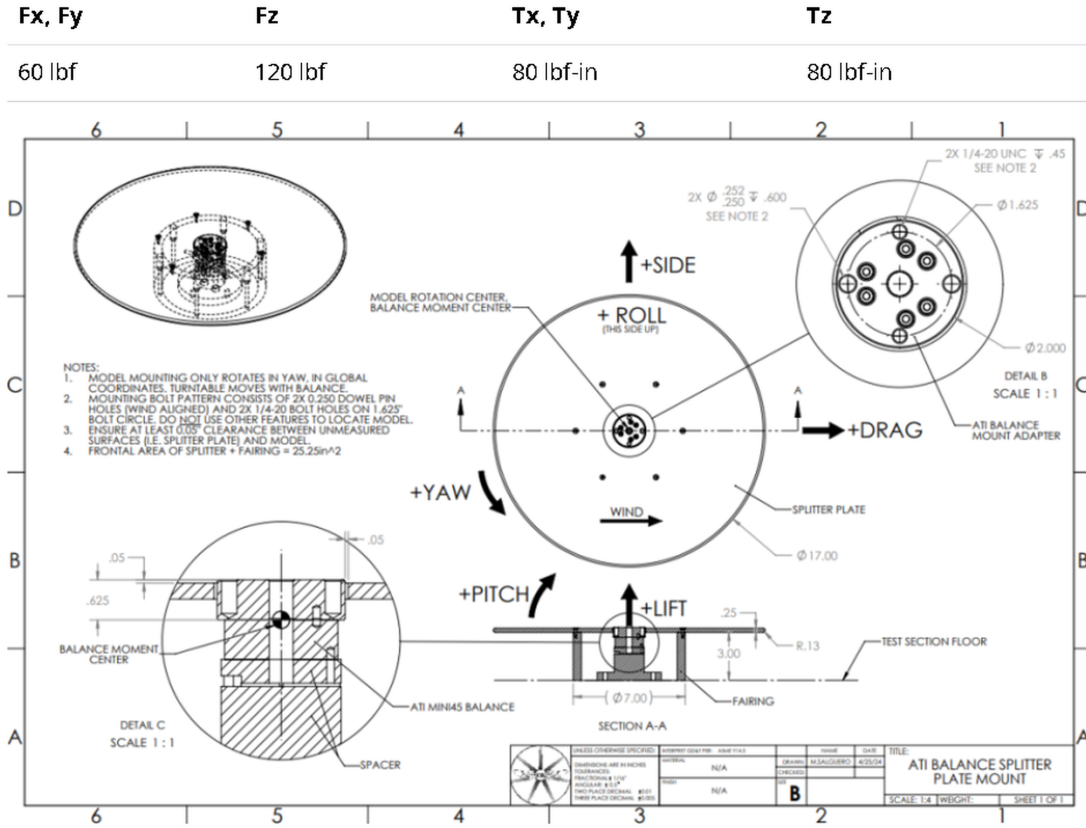


Figure 4.2: Schematic of the splitter plate and half-body force balance assembly that show the maximum force and moment limits at the top of the schematic. Here F_x , F_y , and F_z represent the maximum force limits and T_x , T_y , and T_z represent the maximum moment limits [35].

4.3 Passive Strake Test Parameters

The passive strake study involved evaluating the no-strake baseline configuration together with seven different i_{strake} configurations. The i_{strake} configurations evaluated were 3° , 5° , 8° , 10° , 13° , 15° , and 18° , and were specified relative to the incoming freestream direction at zero degrees α . Each strake configuration was evaluated at three δ_f configurations of 0° , 20° , and 45° , representing representative cruise, takeoff, and landing flight conditions, respectively.

The passive strake investigation therefore consisted of 24 unique aerodynamic configurations, corresponding to eight strake configurations (including the baseline) evaluated at

three δ_f configurations. Each configuration was subsequently tested over an α range of $-5^\circ \leq \alpha \leq 20^\circ$ using 1° increments, resulting in 26 α test conditions per configuration and a total of 624 individual passive wind tunnel measurements. A summary of the passive test parameters is provided in Table 4.1.

Table 4.1: Passive Strake Test Parameters

Parameter	Test Conditions
δ_f Configurations	$0^\circ, 20^\circ, 45^\circ$
i_{strake} Configurations	$3^\circ, 5^\circ, 8^\circ, 10^\circ, 13^\circ, 15^\circ, 18^\circ$
Additional Flow Control Configurations	Baseline (no-strake)
Unique Passive Configurations	24
α Test Conditions per Configuration	26
Total Passive Wind Tunnel Measurements	624
α Range	$-5^\circ \leq \alpha \leq 20^\circ$
α Increment	1°
Nominal q_∞	2 psf
Data-Acquisition Duration	10 s per α

To minimize unwanted flow disturbances prior to commencing each test run, exposed fastener holes and gap areas between the wing flaps and the fuselage were sealed using black vinyl tape whenever possible. Sealing these gaps and holes eliminated potential leakage paths, thereby reducing unintended surface disturbances that could have influenced the flow around the wing during testing.

4.4 *Angle of Attack Control and Data Collection*

Although δ_f configurations of 0° , 20° , and 45° were utilized to represent cruise, take-off, and landing configurations respectively, each δ_f configuration was evaluated over the complete experimental α range of $-5^\circ \leq \alpha \leq 20^\circ$. This broader testing range was intentionally selected to characterize passive and active nacelle-mounted strake performance under pre-stall, stall, and post-stall conditions and to provide a consistent basis for comparing aerodynamic performance across all flap configurations. Accordingly, the terms cruise, takeoff, and landing used throughout this thesis refer to the representative δ_f configuration being

investigated rather than the complete operational α envelope of a commercial transport aircraft.

For each combination of passive strake configuration and flap setting, the wing model was tested at all discrete α ranging from $-5^\circ \leq \alpha \leq 20^\circ$ in increments of 1° as depicted in Table 4.1. The α increment was controlled manually using an external lever linked to a turntable that rotated the splitter plate and half-body force balance assembly within the wind tunnel test section. The turntable included α tick marks that each represented 1° increments, and was used to position the wing model at each specified α as shown in Figure 4.3.

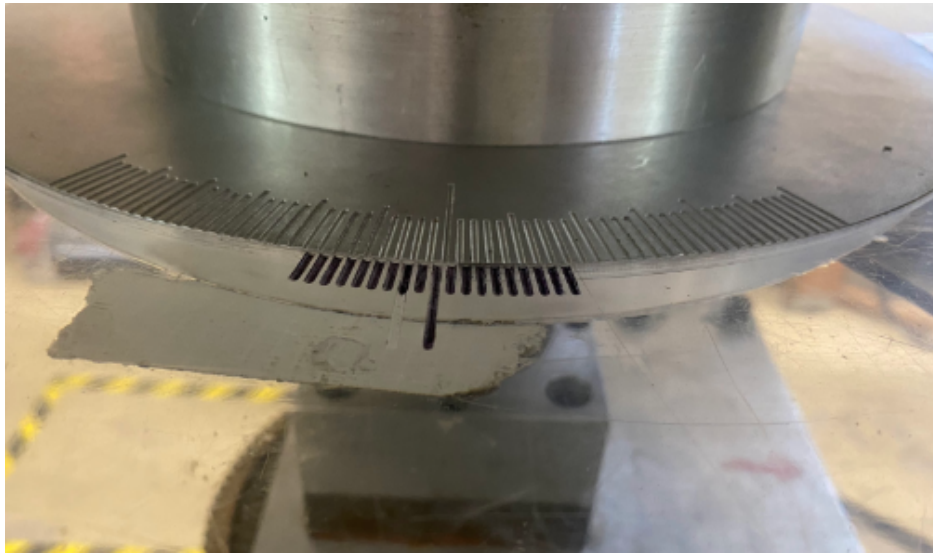


Figure 4.3: Turntable α tick marks.

After positioning the wing model at specified α , a 5 s wait time was implemented before commencing data collection. This permitted some time so that the wind tunnel flow conditions could stabilize due to the positioning the wing model at the new α . Upon completion of the wait time, 10 s of force data was acquired from the DAQ through the LabVIEW interface. The LabVIEW output was set up to capture one force measurement averaged over 1000 data points for each 1 s of data. Therefore, for each α , a total of ten separate 1 s averaged force measurements were captured that were subsequently analyzed.

4.5 Active Strake Test Parameters

Similar to the passive strake investigation, active strake testing was performed using the seven i_{strake} configurations together with the three δ_f configurations described previously. In addition, the active strake investigation independently evaluated three pneumatic injection locations corresponding to H1, H2, and H3. Unlike the previous investigation by Spitzley et al., which evaluated multiple pneumatic blowing pressures together with single-, dual-, and triple-hole injection configurations, the present investigation maintained a constant pneumatic supply pressure of 20 psi while independently evaluating each pneumatic injection location. During each test condition, only one H1, H2, or H3 pneumatic injection location was activated at a time to isolate the aerodynamic influence of blowing location.

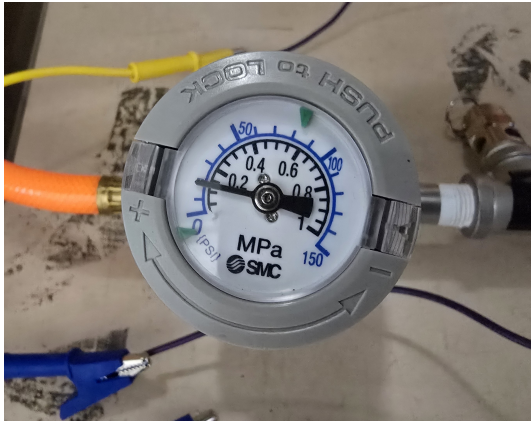
Unlike the passive strake investigation, which employed uniform 1° increments in α , active strake testing utilized a non-uniform α schedule to reduce overall testing time while preserving the principal aerodynamic trends associated with stall onset and post-stall behavior. The active strake α schedule consisted of -5° , -3° , -1° , 0° , 2° , 4° , 6° , 8° , 10° , 12° , 13° , 14° , 15° , 16° , 17° , and 20° .

The active strake investigation therefore consisted of 63 unique aerodynamic configurations, corresponding to seven i_{strake} configurations evaluated at three δ_f configurations and three pneumatic injection locations. Each configuration was subsequently tested at 16 α conditions, resulting in a total of 1008 individual active wind tunnel measurements. A summary of the active strake test parameters is provided in Table 4.2.

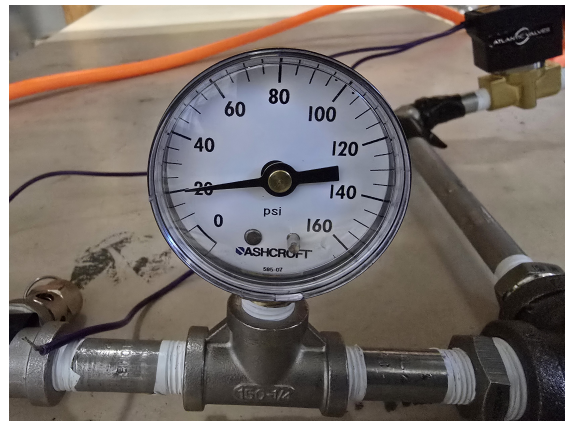
Prior to performing active strake testing, the pneumatic supply pressure was set to 20 psi using the pressure regulator as depicted in Figure 4.4a. Next, the pressure reading from the pressure gauge was checked to confirm that a constant 20 psi pressure existed throughout the pneumatic system, as shown in Figure 4.4b. Once the prescribed supply pressure was verified, the power supply was energized and electrical power was supplied to only one of the three pneumatic solenoid valves corresponding to the selected H1, H2, or H3 injection

Table 4.2: Active Strake Test Parameters

Parameter	Test Conditions
δ_f Configurations	$0^\circ, 20^\circ, 45^\circ$
i_{strake} Configurations	$3^\circ, 5^\circ, 8^\circ, 10^\circ, 13^\circ, 15^\circ, 18^\circ$
Pneumatic Injection Locations	H1, H2, H3
Unique Active Configurations	63
α Test Conditions per Configuration	16
Total Active Wind Tunnel Measurements	1008
α Schedule	$-5^\circ \leq \alpha \leq 20^\circ$ (non-uniform)
Nominal q_∞	2 psf
Pneumatic Supply Pressure	20 psi
Data-Acquisition Duration	10 s per α



(a) Pressure regulator set to 20 psi



(b) Pressure gauge reading 20 psi

Figure 4.4: (a) Pressure regulator adjusted to provide a 20 psi pneumatic supply pressure and (b) pressure gauge confirming the regulated pressure within the pneumatic system prior to testing.

location for the test condition being evaluated.

When switching between H1, H2, and H3 injection locations, a 10 s waiting period was implemented after deactivating one solenoid valve and activating another. This waiting period provided sufficient time for pressure fluctuations within the pneumatic system to dissipate and stabilize to the prescribed 20 psi supply pressure before acquiring aerodynamic data. Pneumatic system stabilization was verified through visual observation of the pressure gauge shown in Figure 4.4b.

4.6 Tare Procedure

Tare measurements were made to provide corrections for half-body force balance offset errors due to model weight effects, mounting loads, and non-aerodynamic loadings introduced by experimental hardware. Tare measurements were made while the wind tunnel fan was turned off, the pneumatic system was pressurized to the H2 injection location only, and the model was positioned at each corresponding α within the active strake test parameters. Tare measurements were gathered using similar LabVIEW and DAQ collection techniques as those utilized during wind-on testing.

During active strake testing, the pneumatic system was pressurized to only one of the H1, H2, and H3 injection locations at a time for each α sweep. Although tare measurements were acquired using the H2 injection location only, this approach was considered representative of all three injection locations since the non-aerodynamic loads introduced by the pneumatic injection line tension, internal pressure loading, and jet reaction forces were consistent across H1, H2, and H3 at the nominal 20 psi supply pressure. Differences in tare loads between injection locations were therefore considered negligible relative to the measured aerodynamic forces.

The tare correction for forces related to the pneumatic injection line tension, internal pressure loading, and jet operation were incorporated into the tare correction regardless of whether wind-on aerodynamic loading was present or absent. Therefore, after completing testing at each α sweep and active blowing condition, the corresponding tare measurement taken at that α was subtracted from each wind-on measurement taken at that α and active blowing condition.

4.7 Coordinate Transformation and Tare Correction

The aerodynamic forces measured by the half-body force balance were transformed into wind-axis lift and drag components prior to determining the aerodynamic coefficients. A

distinguishing feature of the present experiments was that as the wing model was rotated to each α , the balance axes also rotated. Consequently, when the measured force components were determined, they did not correspond directly to the freestream-aligned lift and drag components at the non-zero α .

To correct for this effect, the measured force components were transformed using an appropriate rotation matrix according to Equation 4.1:

$$\begin{bmatrix} D_w \\ L_w \end{bmatrix} = \begin{bmatrix} \cos \alpha & -\sin \alpha \\ \sin \alpha & \cos \alpha \end{bmatrix} \begin{bmatrix} D_m \\ L_m \end{bmatrix} \quad (4.1)$$

where D_m and L_m are the respective sign-corrected drag and lift force components from the half-body force balance. This transformation ensured that all reported L_w and D_w values were referenced to the freestream coordinate system rather than to the rotated balance axes.

The tare measurements were also transformed using the same rotation matrix as the corresponding wind-on measurements. These transformed tare measurements were subtracted from the corresponding transformed wind-on values at the same α . The corrected values were averaged and non-dimensionalized using the q_∞ and S_{ref} to determine C_L and C_D values.

Once C_L and C_D values were determined, the average L/D values and the corresponding 95% confidence intervals were calculated. The entire data reduction process is outlined in Figure 4.5

4.8 *Experimental Uncertainty*

Experimental uncertainty was quantified based upon the random variability associated with repeated measurements obtained at each α . The output from LabVIEW captured a total of ten separate 1 s averaged force measurements for each α , where each 1 s average was derived from 1000 individual samples recorded through the DAQ. The mean of a particular variable, x , was determined by averaging all values of x as follows in Equation 4.2 [36]:

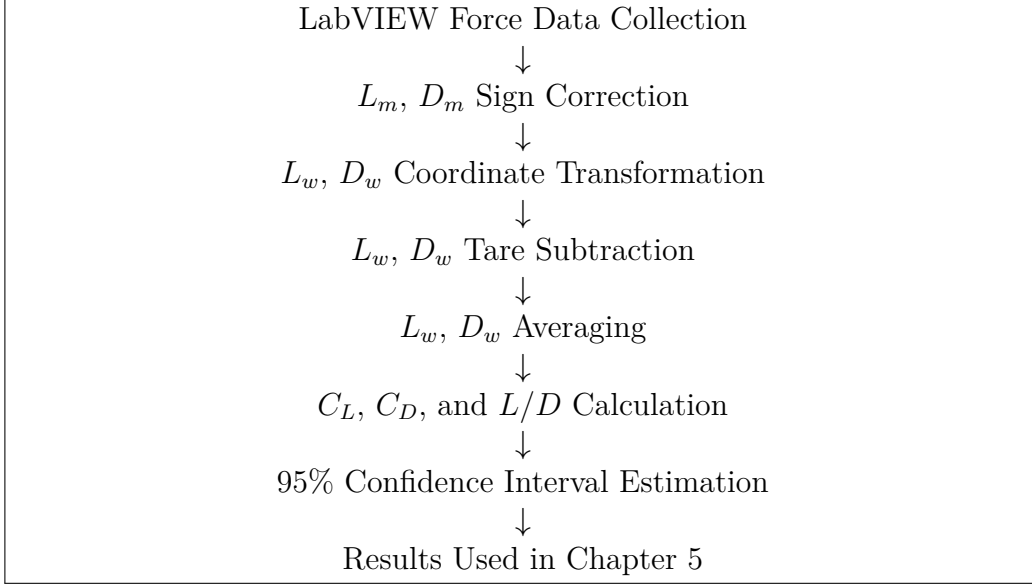


Figure 4.5: Data reduction flow chart.

$$\bar{x} = \frac{1}{n} \sum_{i=1}^n x_i, \quad (4.2)$$

where $n = 10$ for each α . The sample standard deviation for x was determined using Equation 4.3 [36]:

$$s_x = \sqrt{\frac{1}{n-1} \sum_{i=1}^n (x_i - \bar{x})^2}, \quad (4.3)$$

The standard error of the mean for x was then found as follows in Equation 4.4 [36]:

$$SE_x = \frac{s_x}{\sqrt{n}}. \quad (4.4)$$

Finally, the 95% confidence interval about x was determined by multiplying the standard error of the mean by a two-sided Students t-value at 95% confidence with $n - 1$ degrees of freedom as shown in Equation 4.5 [36]:

$$CI_{95,x} = t_{0.975,n-1} SE_x. \quad (4.5)$$

Since ten separate 1 s averaged force measurements were captured for each α condition, $n = 10$ and $n - 1 = 9$. The 95% confidence intervals were used to evaluate the repeatability of the measured aerodynamic quantities and to assess whether differences between configurations were large relative to the experimental scatter.

In addition, the 95% confidence intervals calculated from Equation 4.5 are presented as error bars on all aerodynamic performance plots in Chapter 5 to provide a graphical representation of the experimental uncertainty associated with the measured aerodynamic coefficients. The confidence intervals are used to aid interpretation of the experimental results by indicating the uncertainty associated with each measurement. Throughout Chapter 5, the degree of overlap between confidence intervals is used to assess whether the observed differences in aerodynamic coefficients between configurations are distinguishable within the experimental uncertainty.

Chapter 5

RESULTS AND DISCUSSION

5.1 Chapter Overview

The purpose of this chapter is to present and evaluate the aerodynamic performance of the passive and active strake configurations investigated experimentally through wind tunnel testing. The δ_f configurations investigated were representative of cruise (0°), takeoff (20°), and landing (45°) flight conditions. Aerodynamic performance was evaluated using measured C_L , C_D , and L/D results, with each configuration compared against the baseline no-strake configuration. A primary objective of this investigation was to assess how variations in i_{strake} , pneumatic injection location, and δ_f influenced the measured aerodynamic performance, and to determine whether the observed changes resulted primarily from lift augmentation, drag reduction, or a combination of both.

Throughout this chapter, the terms cruise, takeoff, and landing refer to the representative $\delta_f = 0^\circ$, 20° , and 45° configurations, while each configuration was evaluated over the complete experimental α range as discussed in Chapter 4.

The flow field surrounding a wing with an engine nacelle and pylon installed in close proximity is highly three-dimensional and is characterized by strong pressure gradients, localized flow separation, and aerodynamic interference effects. Passive nacelle-mounted strakes are designed to generate streamwise vortices that transport high-momentum freestream fluid toward the lower-energy boundary layer adjacent to the wing upper surface. As a result, the boundary layer becomes more resistant to adverse pressure gradients, thereby reducing the likelihood of flow separation and the associated pressure drag. Active pneumatic strakes inject momentum locally into the flow field in an effort to modify the aerodynamic characteristics surrounding the nacelle-wing junction. However, because the present investigation did not directly measure the detailed flow structures responsible for the measured aero-

dynamic performance, the observed aerodynamic trends are interpreted using established aerodynamic theory together with previously published experimental investigations.

Throughout this chapter, the aerodynamic performance of each configuration is evaluated using experimentally determined aerodynamic coefficients together with their associated 95% confidence intervals calculated using the Student's t-distribution from Equation 4.5. These confidence intervals provide a graphical representation of the experimental uncertainty associated with the measured aerodynamic coefficients and are used throughout the discussion to determine whether differences between configurations are distinguishable within the uncertainty of the experiment. Consequently, comparisons between configurations are based not only on the measured aerodynamic coefficients, but also on the degree of overlap between their associated confidence intervals.

For the purposes of this investigation, the wing stall angle (α_{stall}) was identified from the first significant breakdown in the C_L curve and occurred at $\alpha_{\text{stall}} = 15^\circ$, $\alpha_{\text{stall}} = 14^\circ$, and $\alpha_{\text{stall}} = 13^\circ$ for δ_f configurations of 0° , 20° , and 45° , respectively. Accordingly, the maximum lift coefficient ($C_{L,\text{max}}$) was identified and associated with the stall onset. Although several configurations exhibited small secondary increases in lift following the initial stall event, these post-stall increases were not interpreted as delayed stall and were therefore not used in determining $C_{L,\text{max}}$. This criterion was applied consistently throughout the chapter when comparing the lift characteristics of the passive and active strake configurations.

In addition to measuring C_L , C_D , and L/D results, drag polars are presented where appropriate to provide additional insight into the measured aerodynamic behavior. By comparing C_D values at equivalent C_L values, drag polars provide a means of determining whether observed improvements in aerodynamic performance result from genuine reductions in drag rather than simply changes in lift production. Consequently, the drag polar analyses provide additional evidence supporting the aerodynamic interpretations presented throughout this chapter.

This chapter is organized according to δ_f configurations and aerodynamic performance

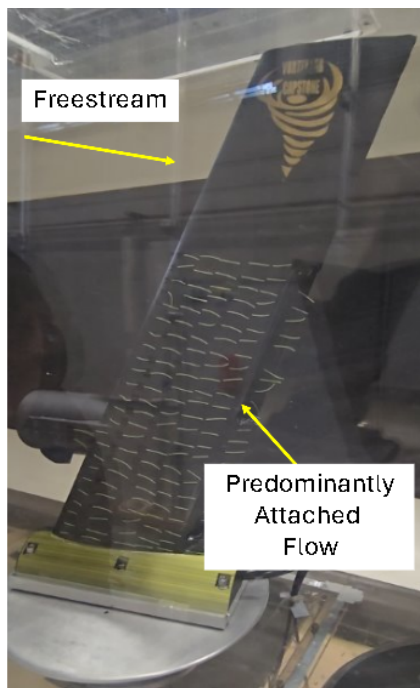
metrics. Sections 5.2 through 5.4 present the passive strake configuration results for δ_f configurations of 0° , 20° , and 45° , respectively. Within each flap investigation, the measured C_L , C_D , and L/D results are evaluated to identify the passive strake configuration that provides the greatest overall aerodynamic benefit relative to the baseline configuration. Sections 5.5 through 5.7 present the corresponding active strake configuration results, where the highest-performing configurations are compared with both the baseline configuration and the optimal passive strake configuration identified for each flap setting. Additional passive and active strake comparisons with the baseline configuration are listed in Appendix C for reference. Summary tables are provided at the conclusion of each flap investigation to summarize the principal aerodynamic performance metrics and facilitate direct comparison among the investigated configurations.

5.1.1 Qualitative Flow Visualization

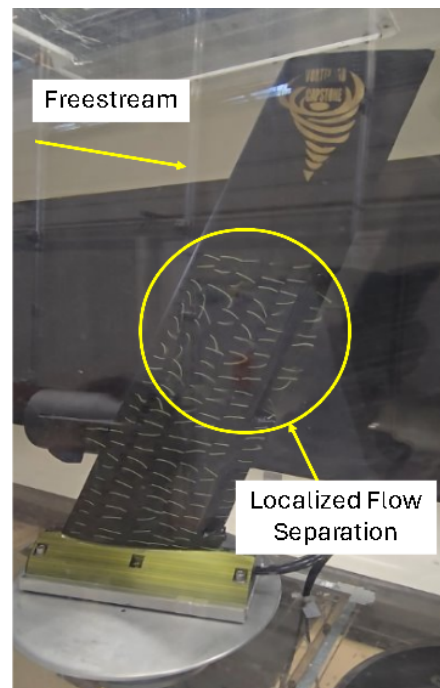
During wind tunnel testing, qualitative flow visualization was performed using surface-mounted tufts to aid interpretation of the measured aerodynamic behavior. Although the primary conclusions of this investigation are based on experimentally measured aerodynamic coefficients, the tuft observations provided visual evidence of boundary layer attachment and localized flow separation over the wing surface.

Representative tuft flow visualization images are shown in Figure 5.1. Under attached-flow conditions, the tufts remained predominantly aligned with the local freestream direction, indicating that the boundary layer remained attached over the majority of the wing surface. As α increased, localized flow separation became evident through regions of disorganized tuft orientation and increased tuft oscillation. Throughout the experimental program, this separation pattern was consistently observed to initiate near the outboard portion of the wing before progressively propagating inboard as α increased.

Although the present investigation did not include quantitative flow field measurements such as particle image velocimetry (PIV) or surface pressure measurements, the qualitative



(a) Predominantly attached flow



(b) Localized flow separation

Figure 5.1: Representative tuft flow visualization during wind tunnel testing. (a) Shows predominantly attached flow with the tufts aligned with the local freestream direction and (b) shows localized flow separation characterized by disorganized tuft orientation.

flow visualization was consistent with the measured aerodynamic trends presented throughout this chapter. Configurations exhibiting improved aerodynamic performance generally maintained attached flow over a greater portion of the wing surface, whereas configurations producing higher drag exhibited larger regions of localized flow separation. These observations provide additional qualitative support for the aerodynamic interpretations presented in the following sections.

5.1.2 Influence of Flap Deflection on Baseline Aerodynamic Performance

Before evaluating the aerodynamic performance of the passive and active nacelle-mounted strake configurations, it is useful to first examine the influence of the δ_f configurations with each of the passive baseline cases. Figures 5.2 and 5.3 compare the measured baseline C_L and C_D curves for the three $\delta_f = 0^\circ$, 20° , and 45° configurations. These baseline comparisons isolate the aerodynamic effects of flap deployment and establish the distinct flow environments in which the passive and active nacelle-mounted strake configurations were subsequently evaluated.

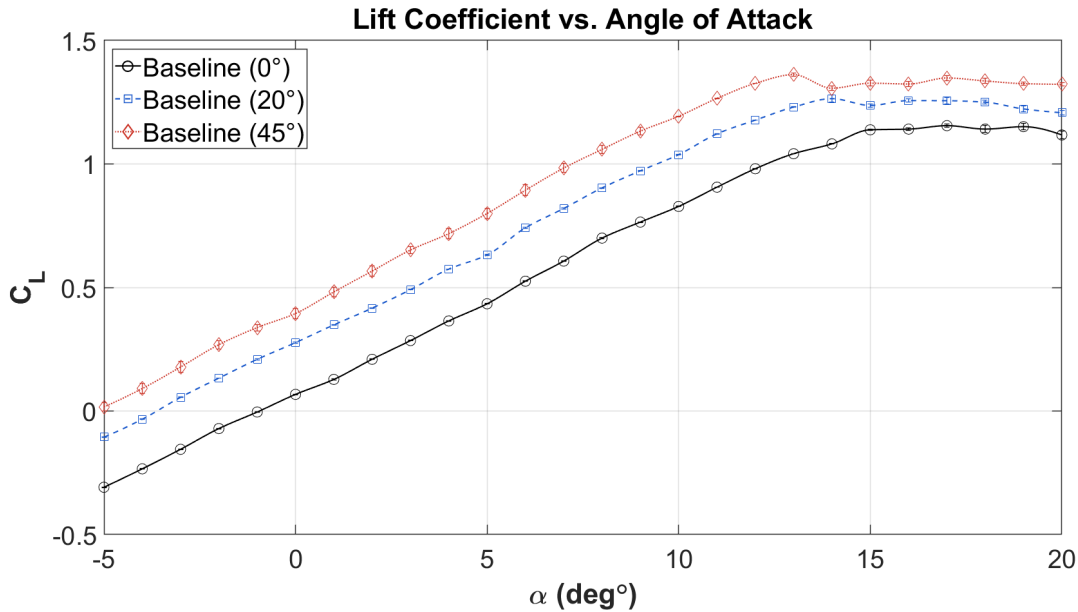


Figure 5.2: Wing C_L vs. α of all passive strake baseline configurations for $\delta_f = 0^\circ$, 20° , and 45° .

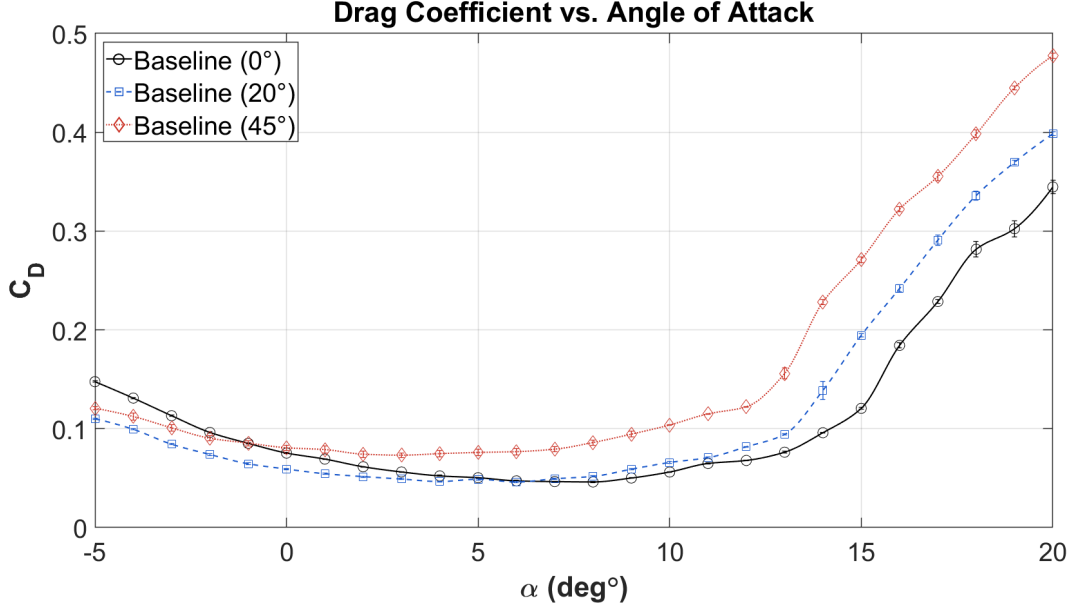


Figure 5.3: Wing C_D vs. α of all passive strake baseline configurations for $\delta_f = 0^\circ$, 20° , and 45° .

The comparison of the baseline C_L values versus α for each δ_f configuration is shown in Figure 5.2. The results show that increasing δ_f progressively increases C_L throughout most of the investigated α range by suggestively increasing the effective camber of the wing. The influence of flap deployment became increasingly pronounced with increasing α , resulting in greater separation between the C_L curves as the wing approached stall. The baseline α_{stall} decreased from $\alpha_{\text{stall}} = 15^\circ$ for the $\delta_f = 0^\circ$ configuration to 14° and 13° for the 20° and 45° δ_f configurations, respectively. Correspondingly, the baseline $C_{L,\text{max}}$ increased from $C_{L,\text{max}} \approx 1.138$ for the $\delta_f = 0^\circ$ configuration to approximately 1.264 and 1.362 for the 20° and 45° δ_f configurations, respectively. These results illustrate the expected aerodynamic tradeoff whereby increasing δ_f improves lift generation while reducing α_{stall} .

The corresponding comparison of the baseline C_D values versus α for each δ_f configuration is shown in Figure 5.3. Increasing δ_f generally produced larger C_L values throughout the investigated α range. The baseline minimum drag coefficients ($C_{D,\text{min}}$) were $C_{D,\text{min}} \approx 0.0461$, 0.0460, and 0.0732 for the 0° , 20° , and 45° δ_f configurations, respectively. Although $C_{D,\text{min}}$ for the 20° δ_f configuration was similar to that of the 0° δ_f configuration, drag increased more

rapidly with increasing α because of the deployed flap geometry. The 45° δ_f configuration consistently produced the highest C_L values throughout most of the operating envelope, illustrating the substantial aerodynamic penalty associated with the landing configuration.

Collectively, the baseline C_L and C_D comparisons demonstrate that flap deployment fundamentally altered the aerodynamic characteristics of the wing. Increasing δ_f improved lift generation but also increased aerodynamic drag and shifted stall to a lower α_{stall} . Consequently, the cruise ($\delta_f = 0^\circ$), takeoff ($\delta_f = 20^\circ$), and landing ($\delta_f = 45^\circ$) configurations established distinct aerodynamic environments in which the passive and active nacelle-mounted strake configurations were evaluated. Consequently, any aerodynamic improvements observed in the following sections can be interpreted relative to the baseline aerodynamic characteristics established for each δ_f configuration.

5.2 Passive Strake Results: Cruise (0° Flaps)

5.2.1 Lift Coefficient Behavior

Figure 5.4 shows how C_L varies with α for the different passive strake configurations and the baseline no-strake configuration for $\delta_f = 0^\circ$. In general, the passive strake configurations exhibited lift characteristics that were almost identical to those of the baseline throughout most of the operating envelope. This suggests that the addition of passive strakes had only a limited effect on the overall lift-generating ability of the wing model during cruise flight.

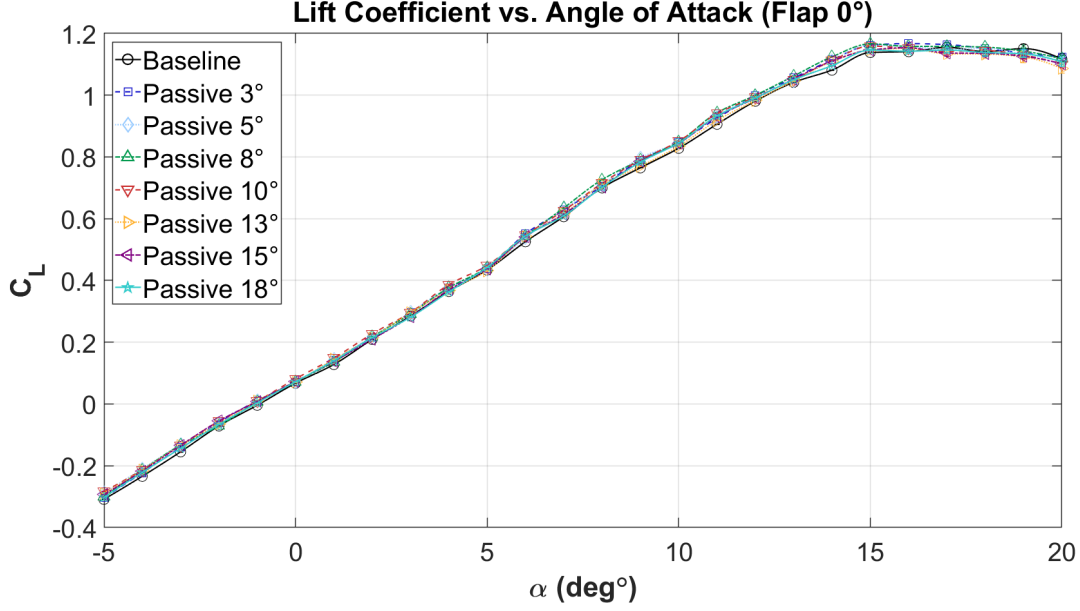


Figure 5.4: Wing C_L vs. α for all passive strake configurations with the baseline at $\delta_f = 0^\circ$.

In the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), the C_L curves for all configurations show very little deviation from one another. At $\alpha < 2.5^\circ$, there is virtually no measurable difference in C_L due to the passive strakes. Comparison of the measured values with the associated 95% confidence intervals indicates that these differences are not distinguishable within the experimental uncertainty. Therefore, it can be concluded that the passive strakes made virtually no contribution to the lift generated while the flow around the wing remained attached.

Within the moderate- α regime ($0^\circ < \alpha \leq 8^\circ$), all passive strake configurations exhibit nearly colinear increases in C_L with similar slopes. The variations between configurations become slightly more pronounced as the α continues to increase; however, the overall aerodynamic response remains quite consistent across the various passive strake geometries. These observations suggest that the local changes to the flow pattern near the nacelle-wing junction caused by the presence of the passive strakes do not significantly alter the global pressure distribution responsible for lift generation.

In the high- α regime ($8^\circ < \alpha \leq 17^\circ$), some of the passive strake configurations produced modest increases in C_L above that of the baseline at certain operating points, primarily as

α approached $\alpha_{\text{stall}} = 15^\circ$. The 8° passive strake produced the greatest $C_{L,\text{max}}$, reaching $C_{L,\text{max}} \approx 1.166$, representing an increase of approximately 2.5% relative to the baseline configuration of $C_{L,\text{max}} \approx 1.138$. The 3° and 5° passive strake configurations each produced $C_{L,\text{max}} \approx 1.162$, while the 10° passive strake produced $C_{L,\text{max}} \approx 1.159$. The remaining passive strake configurations exhibited only modest changes in $C_{L,\text{max}}$ relative to the baseline. Although several configurations exhibited small secondary increases in lift at α values greater than α_{stall} , these occurred after the first significant C_L curve breakdown and therefore were not used in determining $C_{L,\text{max}}$. Consequently, the passive strakes did not measurably delay stall, which remained at $\alpha_{\text{stall}} = 15^\circ$ for all passive configurations within the 1° angular resolution of the experiment.

Overall, the incorporation of the passive strake configurations produced only modest improvements in C_L under cruise conditions. While small increases in C_L were observed at selected operating conditions, the overall aerodynamic benefit remained limited.

5.2.2 Drag Coefficient Behavior

The C_D versus α results for the passive strake configurations with the baseline no-strake configuration at $\delta_f = 0^\circ$ are illustrated in Figure 5.5. These results show substantial reductions in drag throughout much of the low- α and moderate- α operating envelope resulting from the addition of the passive strakes and indicate that the primary aerodynamic benefit of incorporating passive strakes under cruise conditions is associated with drag reduction rather than lift enhancement.

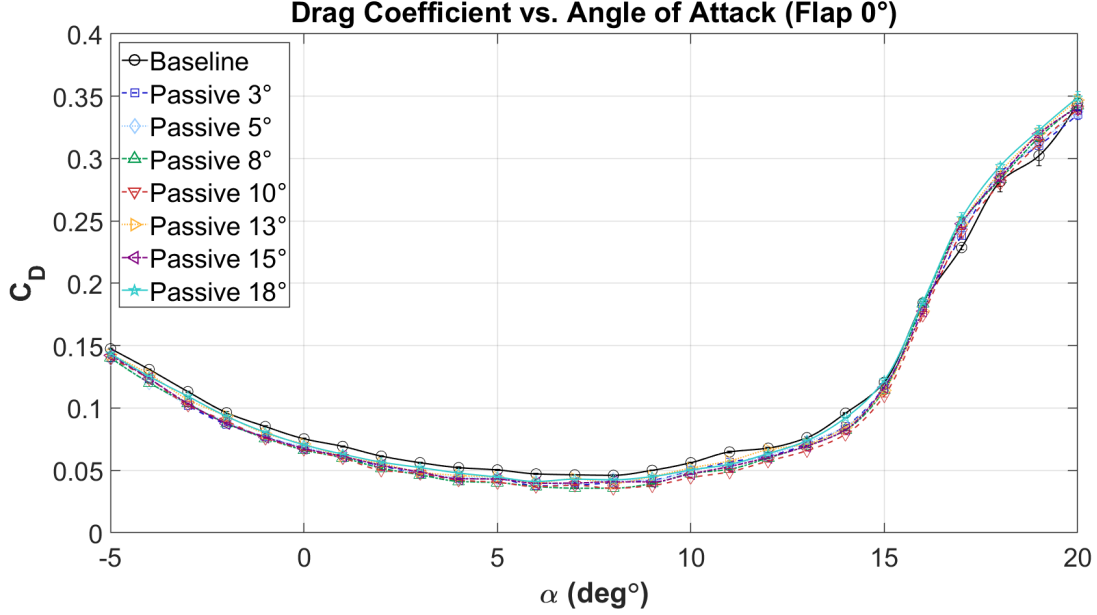


Figure 5.5: Wing C_D vs. α for all passive strake configurations with the baseline at $\delta_f = 0^\circ$

Within the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), the C_D results varied very little among the different configurations. Most differences in drag coefficient fell within the associated 95% confidence intervals, indicating that the variations between configurations are not distinguishable within the experimental uncertainty. Consequently, there appears to be little, if any, measurable difference in drag between the passive strake configurations and the baseline throughout this operating range.

However, within the moderate- α regime ($0^\circ < \alpha \leq 8^\circ$), a clear trend in drag performance develops among the passive strake configurations. The 10° passive strake generally exhibited the lowest C_D values throughout much of this operating range. The baseline configuration achieved $C_{D,\min} \approx 0.0461$ at $\alpha = 8^\circ$, whereas the 10° passive strake reduced to $C_{D,\min} = 0.0357$ at the same α , corresponding to an approximately 22.7% reduction in C_D . Since the C_L values for both configurations remained nearly identical, these reductions represent genuine improvements in aerodynamic efficiency rather than reductions resulting from decreased lift generation.

Additional evidence supporting this conclusion is provided by the drag polar presented

in Figure 5.6. At nearly every C_L value, the 10° passive strake maintained lower C_D values than the baseline configuration. Consequently, the measured reductions in drag cannot be attributed solely to differences in lift generation. Instead, the measured aerodynamic behavior suggests that the passive strake produced favorable modifications to the aerodynamic characteristics of the nacelle-wing junction. Although the present investigation did not directly measure the underlying flow physics responsible for these changes, the observed trends are consistent with the expected behavior from PFC devices.

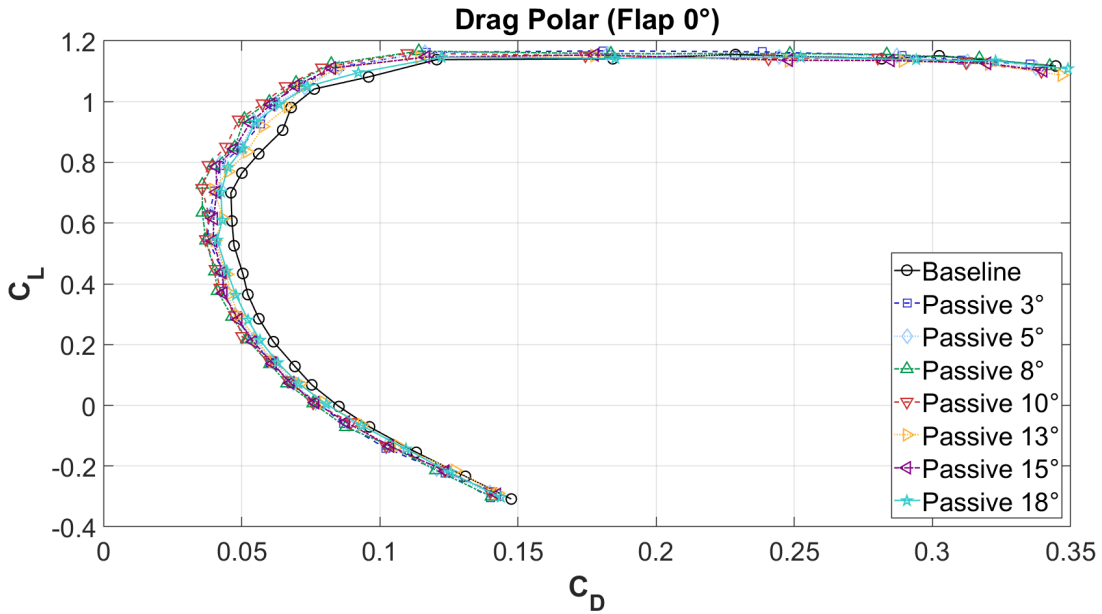


Figure 5.6: Wing Drag Polar for all passive strake configurations with the baseline at $\delta_f = 0^\circ$.

The measured drag reductions are also in good agreement with the published literature reviewed in Chapter 2. Previous investigations by Lin, Greenblatt and Wagnanski, and Warsop reported that passive strakes can reduce aerodynamic drag by energizing the boundary layer and increasing its resistance to adverse pressure gradients [20, 8, 29]. While the present investigation did not directly observe the underlying flow structures, the substantial drag reductions measured for the 10° passive strake are consistent with the aerodynamic trends reported in these earlier experimental investigations.

In the high- α regime ($\alpha > 8^\circ$), C_D increased for all passive strake configurations as the

wing model approached $\alpha_{\text{stall}} = 15^\circ$ and flow separation became increasingly significant. Although several passive strake configurations continued to exhibit lower C_D values than the baseline throughout portions of this operating range, the magnitude of the improvement gradually diminished as drag increased rapidly while approaching $\alpha_{\text{stall}} = 15^\circ$.

Therefore, based on these measurements, the passive strake configurations were considerably more effective at reducing drag than increasing lift under cruise conditions. Among the strakes investigated, the 10° passive strake consistently produced the greatest aerodynamic benefit by providing the largest and most repeatable reductions in drag throughout much of the cruise operating envelope, particularly within the moderate- α regime.

5.2.3 Lift-to-Drag Ratio Behavior

Figure 5.7 shows how L/D varies with α for each passive strake configuration at $\delta_f = 0^\circ$. Since L/D reflects both lift generation and drag production, it provides the most comprehensive measure of aerodynamic performance and therefore serves as the primary metric for evaluating the effectiveness of the passive strakes under cruise conditions.

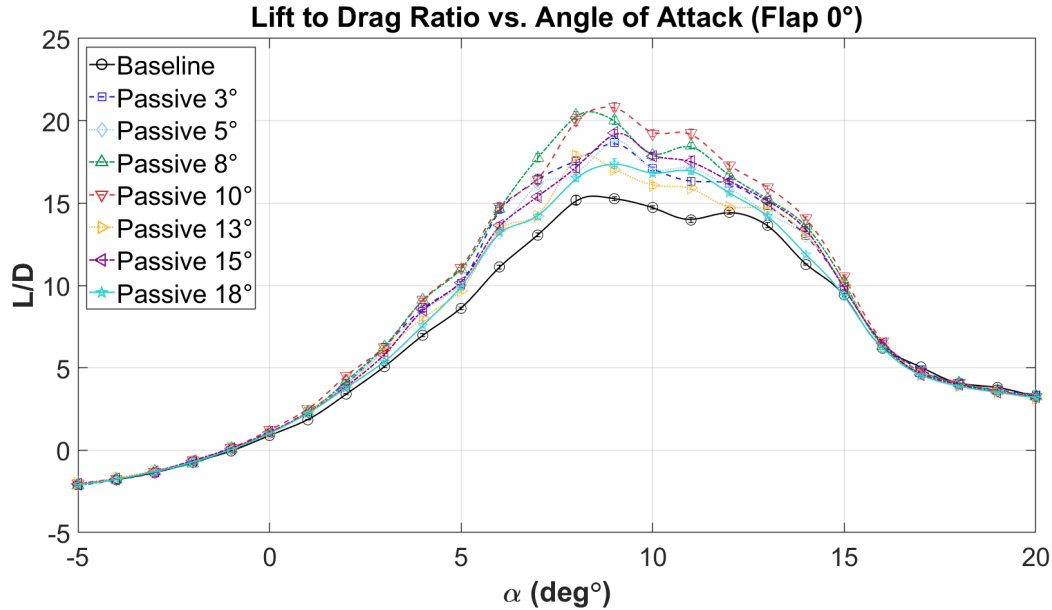


Figure 5.7: Wing L/D vs. α for all passive strake configurations with the baseline at $\delta_f = 0^\circ$.

During the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), the aerodynamic efficiencies of all the passive strake configurations exhibited similar behavior. As indicated by the C_L and C_D results presented in Sections 5.2.1 and 5.2.2, only slight variations were observed between the different passive strake geometries. This suggests that the passive strakes produced very little measurable improvement in aerodynamic efficiency while the flow remained attached.

Upon entering the moderate- α regime ($0^\circ < \alpha \leq 9^\circ$), obvious differences emerged among the passive strake configurations. The 10° passive strake generally exhibited the highest L/D values throughout most of this operating range. The baseline configuration produced a maximum lift-to-drag ($(L/D)_{\max}$) ratio of $(L/D)_{\max} \approx 15.27$ at $\alpha = 9^\circ$, whereas the 10° passive strake increased to $(L/D)_{\max} \approx 20.85$ at the same α , corresponding to an improvement in aerodynamic efficiency of approximately 36.5%.

When considered together, the C_L , C_D , and drag polar results indicate that the majority of the measured improvements in aerodynamic efficiency resulted from reductions in drag rather than increases in lift. Throughout the operating range where the largest increases in L/D were observed, the C_L values for all configurations varied only slightly; however, the 10° passive strake consistently exhibited lower C_D values than the baseline configuration. Furthermore, as illustrated by the drag polar presented in Figure 5.6, the 10° passive strake maintained lower C_D values than the baseline at equivalent C_L values. Many of these measured improvements also exceeded the associated 95% confidence intervals throughout much of the moderate- α regime, indicating that the improvements are distinguishable from the experimental uncertainty. Collectively, these results demonstrate that the observed improvements in aerodynamic efficiency were achieved primarily through drag reduction rather than lift augmentation.

Beyond the high- α regime ($\alpha > 9^\circ$), L/D decreased progressively for all passive strake configurations as aerodynamic drag increased approaching stall at $\alpha_{\text{stall}} = 15^\circ$ due to the onset of large-scale flow separation. Although several passive strake configurations continued to exhibit slightly greater aerodynamic efficiencies than the baseline throughout portions of

this operating range, the magnitude of the improvement progressively diminished as stall was approached.

Overall, the cruise condition results demonstrate that the passive strake configurations have the potential to provide substantial improvements in aerodynamic efficiency when the appropriate strake geometry is selected for the operating condition. Among all passive strakes investigated, the 10° passive strake produced the greatest overall aerodynamic benefit by increasing $(L/D)_{\max}$ by approximately 36.5% while producing virtually no change in lift generation. The close agreement between the C_L , C_D , and drag polar results indicates that these improvements were achieved primarily through reductions in aerodynamic drag rather than increases in lift. A summary of the principal aerodynamic performance metrics for the passive strake cruise investigation is presented in Table 5.1.

Table 5.1: Summary of Passive Strake Cruise Aerodynamic Performance

Config.	$C_{L,\max}$	$\alpha@C_{L,\max}$	$C_{D,\min}$	$\alpha@C_{D,\min}$	$(L/D)_{\max}$	$\alpha@(L/D)_{\max}$	$\Delta(L/D)_{\max}$
Baseline	1.138	15°	0.0461	8°	15.27	9°	No Chg.
Passive 3°	1.162	15°	0.0377	6°	18.69	9°	+22.4%
Passive 5°	1.162	15°	0.0389	7°	18.91	9°	+23.8%
Passive 8°	1.166	15°	0.0357	8°	20.33	8°	+33.1%
Passive 10°	1.159	15°	0.0357	8°	20.85	9°	+36.5%
Passive 13°	1.146	15°	0.0398	8°	17.88	8°	+17.1%
Passive 15°	1.146	15°	0.0397	6°	19.28	9°	+26.2%
Passive 18°	1.146	15°	0.0411	6°	17.40	9°	+13.9%

5.3 Passive Strake Results: Takeoff (20° Flaps)

5.3.1 Lift Coefficient Behavior

The comparison of the baseline no-strake and passive strake configurations is provided in Figure 5.8, which shows how C_L varies with α when $\delta_f = 20^\circ$. The data indicate that deployment of the 20° flap resulted in greater lift generation for all configurations compared with the cruise investigation presented in Section 5.2.1 due to the increased wing camber. While slightly greater differences among the passive strake geometries were observed throughout portions of the operating envelope, the passive strakes continued to exhibit only modest influences on the overall lift-producing capability of the wing.

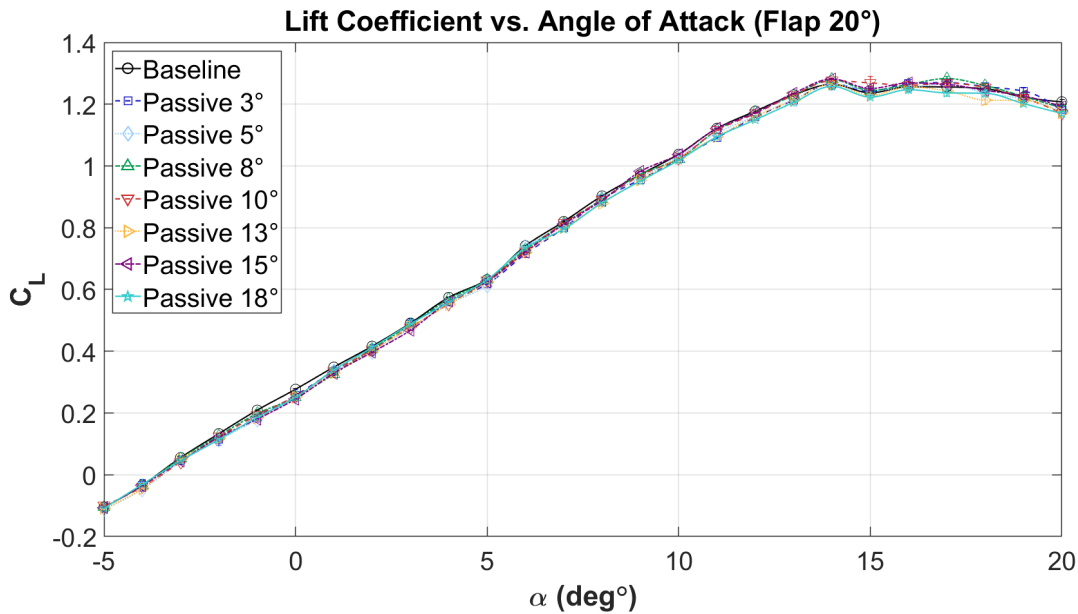


Figure 5.8: Wing C_L vs. α for all passive strake configurations with the baseline at $\delta_f = 20^\circ$.

Within the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), the C_L curves for the different passive strake configurations remained closely grouped. In addition, although minor differences in C_L were observed between the passive strake configurations, the measured differences generally fell within the associated 95% confidence intervals. Therefore, based on these results, the passive strakes had little measurable influence on lift production throughout this operating regime.

During the moderate- α regime ($0^\circ < \alpha \leq 10^\circ$), C_L increased almost colinearly for all configurations. Although the differences between the various passive strake geometries became progressively larger with increasing α , the overall C_L trends remained very similar. These observations suggest that the passive strakes produced localized modifications to the flow near the nacelle-wing junction, but had only minimal influence on the global pressure distribution responsible for lift generation.

Beyond the high- α regime ($\alpha > 10^\circ$), several passive strake configurations produced modest increases in C_L relative to the baseline configuration, especially as α approached $\alpha_{\text{stall}} = 14^\circ$. The 15° passive strake produced the greatest $C_{L,\text{max}}$, reaching $C_{L,\text{max}} \approx 1.282$, representing an increase of approximately 1.4% relative to the baseline configuration of $C_{L,\text{max}} \approx 1.264$. The 8° passive strake produced a nearly identical $C_{L,\text{max}} \approx 1.280$, while the remaining passive strake configurations produced only modest differences relative to the baseline. These results indicate that the passive strakes produced only minor improvements in maximum lift during the takeoff investigation.

Although several passive strake configurations exhibited small secondary increases in lift at α values greater than $\alpha_{\text{stall}} = 14^\circ$, these occurred after the first significant C_L curve break-down and therefore were not used in determining $C_{L,\text{max}}$. Consequently, the passive strakes did not measurably delay stall, which remained at $\alpha_{\text{stall}} = 14^\circ$ for all passive configurations within the 1° angular resolution of the experiment.

Overall, the passive strake configurations produced only modest improvements in lift performance at $\delta_f = 20^\circ$. Although several configurations generated slightly greater C_L values than the baseline, the magnitude of these improvements remained relatively small.

5.3.2 Drag Coefficient Behavior

Figure 5.9 demonstrates how C_D varies with α for the passive strake configurations together with the baseline no-strake configuration at $\delta_f = 20^\circ$. Unlike the cruise investigation conducted in Section 5.2.2, the passive strakes generally did not produce a measurable re-

duction in drag compared with the baseline configuration. Most passive configurations had C_D values equal to or greater than those of the baseline throughout a large portion of the operating envelope. This indicates that the beneficial drag reductions observed during cruise conditions were not maintained when the flap was deflected to the takeoff position.

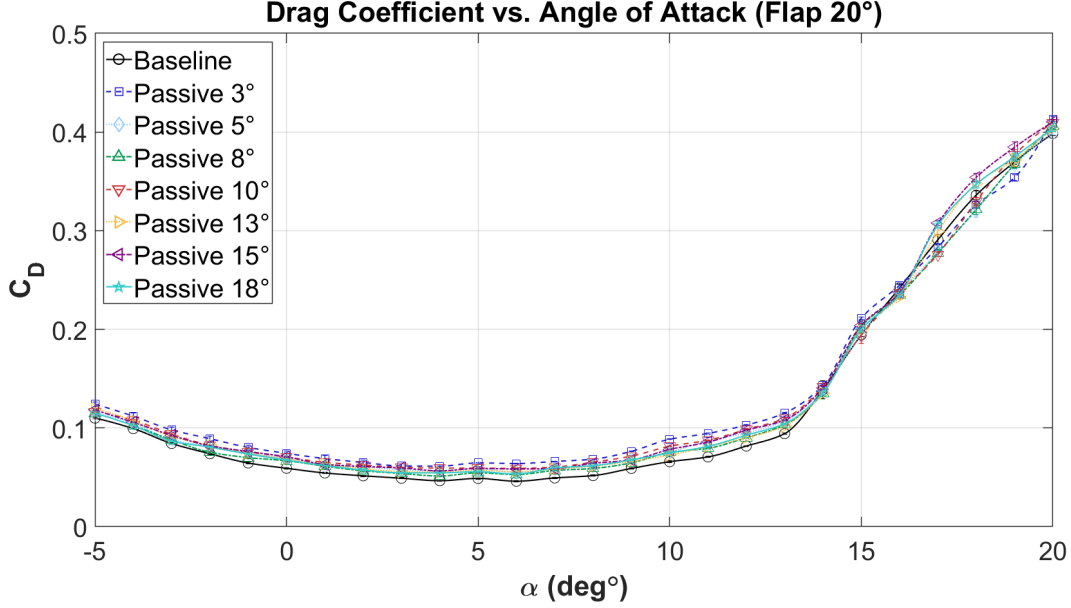


Figure 5.9: Wing C_D vs. α for all passive strake configurations with the baseline at $\delta_f = 20^\circ$.

In the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), all passive strake configurations exhibited very similar C_D trends. The measured C_D values among the various configurations generally fell within their associated 95% confidence intervals. Therefore, there was very little distinguishable effect of the passive strakes on aerodynamic drag while the flow remained attached throughout this operating regime.

The C_D trends of the passive strake configurations changed noticeably as α increased into the moderate- α regime ($0^\circ < \alpha \leq 10^\circ$). In contrast to the cruise investigation, where several passive strake configurations produced measurable reductions in drag, the baseline configuration generally retained the lowest C_D throughout much of this operating range. Among the strakes, the 8° passive strake produced $C_{D,\min} \approx 0.0513$ at $\alpha = 4^\circ$. However, this value remained greater than that of the baseline configuration, which achieved a decrease

of $C_{D,\min} = 0.0460$ at $\alpha = 6^\circ$, which corresponds to approximately a 10.3% increase in C_D from the baseline.

More information can be obtained by examining the drag polar presented in Figure 5.10. At equivalent C_L values, the majority of the passive strake configurations exhibited C_D values that were equal to or greater than those of the baseline throughout much of the operating envelope. Consequently, the measured increases in drag cannot be attributed solely to differences in lift generation. Although the present investigation did not directly measure the underlying flow structures associated with each passive strake geometry, the passive strakes did not produce the same level of drag reduction observed during the cruise investigation.

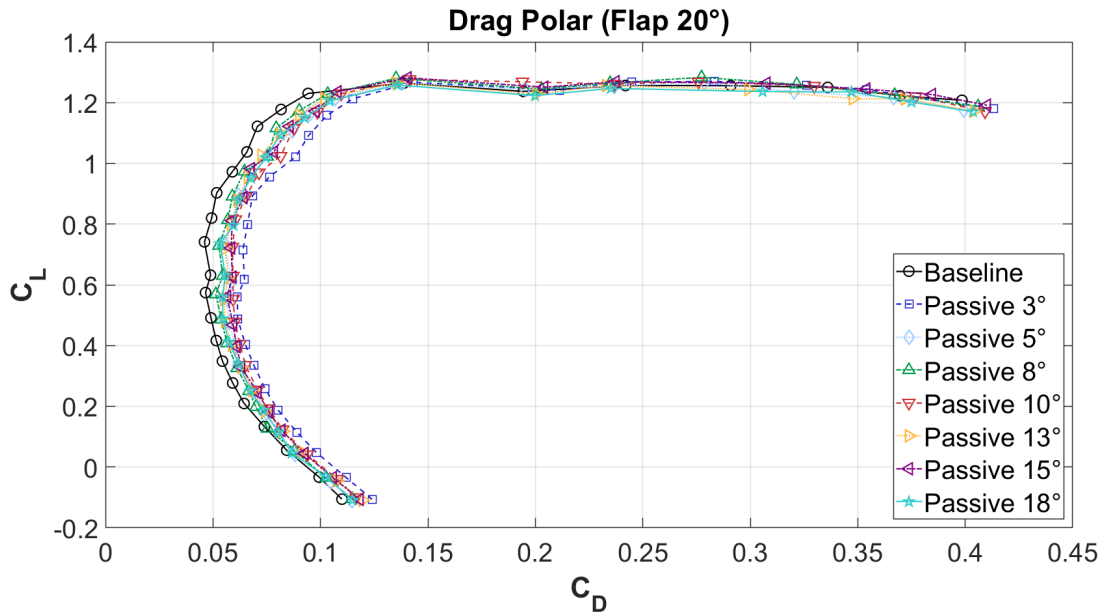


Figure 5.10: Wing Drag Polar for all passive strake configurations with the baseline at $\delta_f = 20^\circ$.

Unlike the cruise investigation, these results are not entirely consistent with the aerodynamic trends reported in the literature for passive strakes operating under attached flow conditions. Previous studies by Lin, Greenblatt and Wagnanski, and Warsop demonstrated that passive strakes can reduce aerodynamic drag by energizing the boundary layer and increasing its resistance to adverse pressure gradients [20, 8, 29]. The absence of similar drag

reductions under takeoff conditions suggests that the increased δ_f substantially altered the local flow field surrounding the nacelle-wing junction, thereby reducing the effectiveness of the passive strakes.

Beyond the high- α regime ($\alpha > 10^\circ$) and as the wing model approached $\alpha_{\text{stall}} = 14^\circ$, the C_D values increased rapidly for all configurations. Although additional variability among the passive strake geometries became evident throughout this operating range, none of the passive strake configurations demonstrated consistently lower C_D values than the baseline.

Therefore, the C_D results indicate that the passive strake configurations did not produce measurable improvements in drag performance under the takeoff conditions investigated. In contrast to the cruise investigation, where the 10° passive strake consistently reduced C_D relative to the baseline, none of the passive configurations evaluated under takeoff conditions produced a measurable reduction in C_D compared with the baseline configuration.

5.3.3 Lift-to-Drag Ratio Behavior

Figure 5.11 shows the variation of L/D with α for the passive strake configurations tested at $\delta_f = 20^\circ$. Since L/D provides the most comprehensive measure of aerodynamic performance because it reflects both lift generation and drag production. Consequently, it serves as the primary metric for evaluating the overall aerodynamic effectiveness of the passive strake configurations under takeoff conditions.

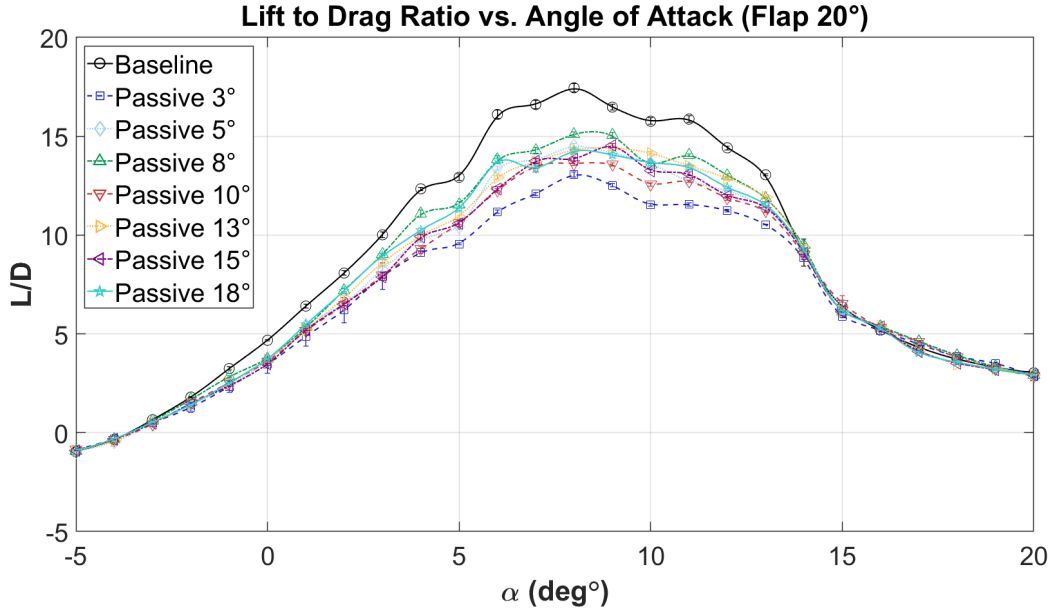


Figure 5.11: Wing L/D vs. α for all passive strake configurations with the baseline at $\delta_f = 20^\circ$.

At the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), the aerodynamic efficiencies of all configurations were very similar. As indicated by the C_L and C_D results presented in Sections 5.3.1 and 5.3.2, the differences in aerodynamic performance among the passive strake configurations were minimal, indicating that the passive strakes had little measurable influence on aerodynamic efficiency while the flow remained attached.

However, as α increased into the moderate α regime ($0^\circ < \alpha \leq 10^\circ$), clear variations in aerodynamic performance developed among the different passive strake configurations. Unlike the cruise investigation, the baseline configuration generally maintained the highest L/D values throughout much of this operating range. The baseline configuration reached $(L/D)_{\max} \approx 17.46$ at $\alpha = 8^\circ$. In comparison, the best-performing passive strake configuration (the 8° passive strake) achieved $(L/D)_{\max} \approx 15.11$ at the same α , representing an approximate 13.5% reduction in L/D relative to the baseline.

When considered together, the C_L , C_D , and drag polar results indicate that the reduction in L/D was primarily attributable to increased drag rather than reduced lift generation. Although several passive strake configurations produced modest increases in $C_{L,\max}$ at higher

α , these improvements were insufficient to offset the additional drag penalties discussed in Section 5.3.2. Furthermore, the drag polars presented in Figure 5.7 indicate that most passive strakes exhibited greater C_D values than the baseline at equivalent C_L values. Many of these differences exceeded their associated 95% confidence intervals throughout much of the moderate- α regime, indicating that the observed reductions in aerodynamic efficiency are distinguishable from the experimental uncertainty.

Within the high- α regime ($\alpha > 10^\circ$), L/D decreased progressively for all configurations as aerodynamic drag increased approaching stall at $\alpha_{\text{stall}} = 14^\circ$. Although some differences between configurations became more apparent throughout this operating range, none of the passive strake configurations consistently produced greater aerodynamic efficiency than the baseline.

Overall, the passive strake configurations did not produce improved aerodynamic efficiency at $\delta_f = 20^\circ$. Although several passive strakes exhibited modest increases in lift generation, those improvements were more than offset by increases in drag. Consequently, the baseline configuration remained the most aerodynamically efficient configuration under the takeoff conditions investigated. A summary of the principal aerodynamic performance metrics for the passive takeoff investigation is presented in Table 5.2.

Table 5.2: Summary of Passive Strake Takeoff Aerodynamic Performance

Config.	$C_{L,\max}$	$\alpha@C_{L,\max}$	$C_{D,\min}$	$\alpha@C_{D,\min}$	$(L/D)_{\max}$	$\alpha@(L/D)_{\max}$	$\Delta(L/D)_{\max}$
Baseline	1.264	14°	0.0460	6°	17.46	8°	No Chg.
Passive 3°	1.267	14°	0.0613	4°	13.06	8°	-25.2%
Passive 5°	1.266	14°	0.0551	6°	14.50	8°	-17.0%
Passive 8°	1.280	14°	0.0513	4°	15.11	8°	-13.5%
Passive 10°	1.276	14°	0.0591	6°	13.62	8°	-22.0%
Passive 13°	1.266	14°	0.0552	4°	14.41	9°	-17.5%
Passive 15°	1.282	14°	0.0566	4°	14.54	9°	-16.7%
Passive 18°	1.258	14°	0.0534	6°	14.26	8°	-18.3%

5.4 *Passive Strake Results: Landing (45° Flaps)*

5.4.1 Lift Coefficient Behavior

The comparison of the C_L as a function of α for the passive strake configurations, together with the baseline no-strake configuration at $\delta_f = 45^\circ$, is illustrated in Figure 5.12. At $\delta_f = 45^\circ$, relative to both the cruise and takeoff investigations, all configurations demonstrated significantly greater lift due to the increased wing camber and aerodynamic loading. Although slightly greater variations in C_L were observed among the passive strake configurations across portions of the operating envelope, the passive strakes continued to produce only modest improvements in the overall lift-generating capability of the wing.

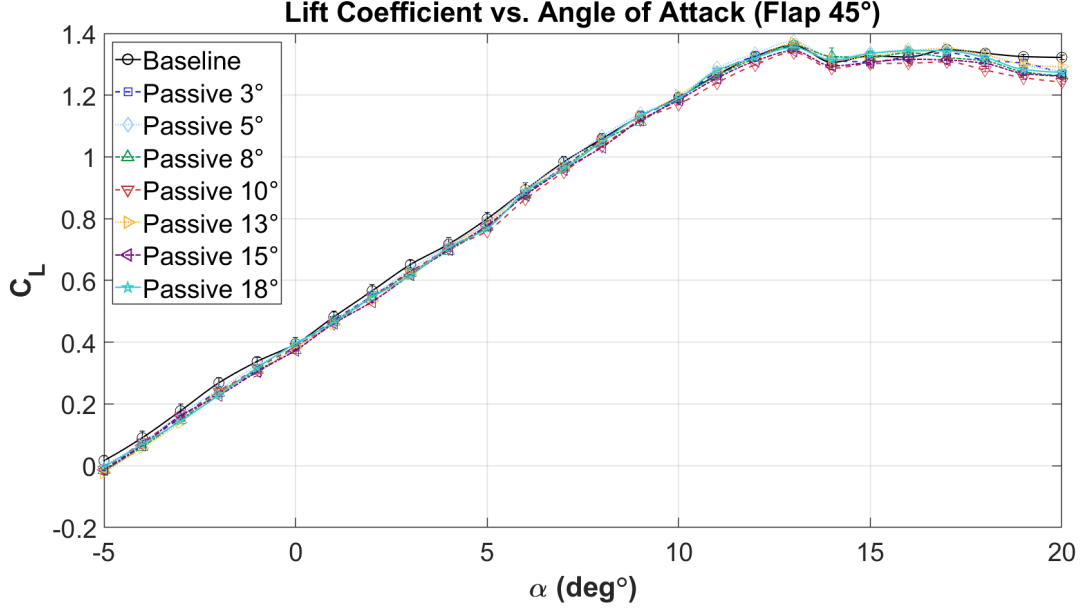


Figure 5.12: Wing C_L vs. α for all passive strike configurations with the baseline at $\delta_f = 45^\circ$.

In the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), the C_L curves for all configurations remained closely grouped, with only minor differences observed among the passive strike geometries. For all configurations examined, the measured differences in C_L were typically smaller than the associated 95% confidence intervals. This suggests that the passive strikes had little measurable influence on lift generation while the flow remained attached.

Within the moderate- α regime ($0^\circ < \alpha \leq 10^\circ$), C_L continued to increase for all configurations while exhibiting only modest variation among the passive strike geometries. Although several passive configurations produced slightly greater C_L values than the baseline throughout portions of this operating range, the overall C_L trends remained remarkably similar for all configurations. These observations suggest that the passive strikes produced localized modifications to the flow near the nacelle-wing junction while having only a minimal influence on the global pressure distribution responsible for lift generation.

Within the high- α regime ($\alpha > 10^\circ$), the passive strike configurations produced their largest measured improvements in C_L relative to the baseline, more specifically, as α approached $\alpha_{\text{stall}} = 13^\circ$. The 5° passive strike produced the greatest $C_{L,\text{max}}$, reaching $C_{L,\text{max}} \approx 1.375$. Compared with the baseline configuration, which produced $C_{L,\text{max}} \approx 1.362$ cor-

responding to an increase of approximately 1.0%. The greatest point-wise increase in C_L occurred at $\alpha = 11^\circ$, where the improvement approached approximately 1.7%. Although several of these improvements exceeded the associated 95% confidence intervals, their overall magnitude remained relatively small. Furthermore, none of the passive strake configurations produced a measurable stall delay. Stall remained at $\alpha_{\text{stall}} = 13^\circ$ for all configurations, indicating that the passive strakes had little influence on the stall characteristics of the wing model under landing conditions.

Overall, the passive strake configurations produced only modest improvements in lift performance at $\delta_f = 45^\circ$. Although several configurations generated slightly greater C_L values than the baseline, the magnitude of these improvements remained relatively small.

5.4.2 Drag Coefficient Behavior

C_D as a function of α for the passive strake configurations together with the baseline no-strake configuration at $\delta_f = 45^\circ$ is illustrated in Figure 5.13. Unlike the takeoff investigation presented in Section 5.3.2, the passive strake configurations produced measurable reductions in C_D relative to the baseline throughout significant portions of the operating envelope. These results indicate that the aerodynamic benefits observed during the cruise investigation were largely restored under landing conditions, with drag reduction once again becoming the primary mechanism responsible for improving aerodynamic performance.

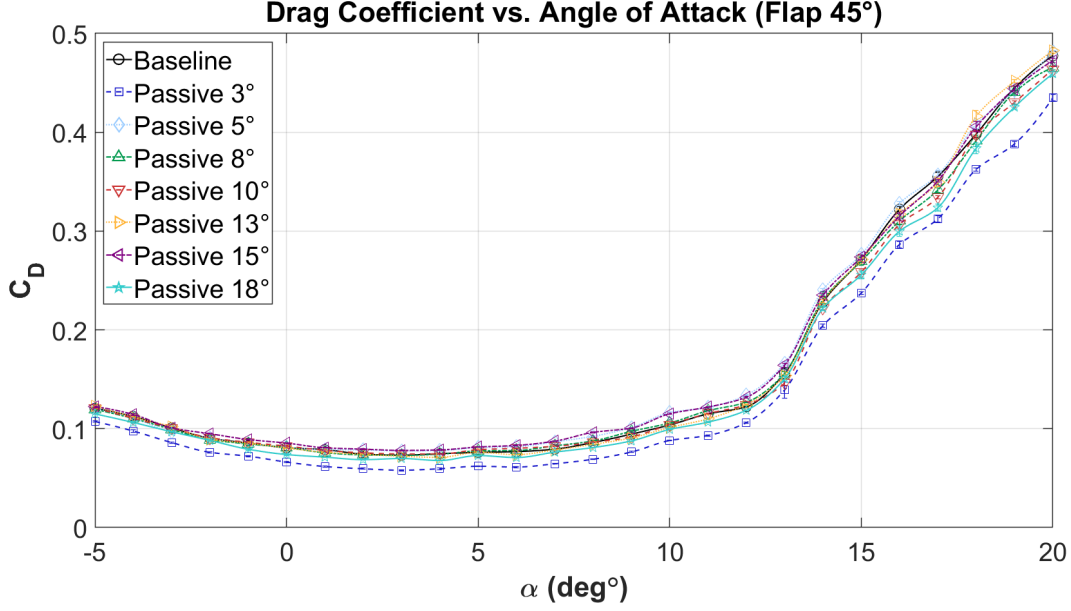


Figure 5.13: Wing C_D vs. α for all passive strike configurations with the baseline at $\delta_f = 45^\circ$.

Within the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), noticeable differences in C_D were observed among the 3° passive strike and baseline configurations. In particular the C_D values for the 3° passive strike were consistently reduced from the baseline. The reduced C_D values resided outside of the 95% confidence intervals, indicating that the 3° passive strike had a measurable effect on aerodynamic drag throughout this operating regime.

As α increased into the moderate- α regime ($0^\circ < \alpha \leq 8^\circ$), clear and measurable differences in C_D developed among the passive strike configurations. The 3° passive strike consistently exhibited the lowest C_L values throughout much of this operating range and produced the greatest overall aerodynamic benefit of all passive strikes investigated at $\delta_f = 45^\circ$. The baseline configuration achieved $C_{D,\min} \approx 0.0732$ at $\alpha = 3^\circ$, whereas the 3° passive strike reduced to $C_{D,\min} \approx 0.0580$ at the same α , corresponding to a reduction of approximately 20.8%. Since the associated C_L values differed by only approximately 1%, these reductions represent genuine improvements in aerodynamic performance rather than decreases resulting from reduced lift generation.

Further insight is provided by the drag polar presented in Figure 5.14. At equivalent C_L values, the 3° passive strike consistently exhibited lower C_D values than the baseline

throughout much of the operating envelope. Consequently, the measured drag reductions cannot be attributed solely to differences in lift generation. Rather, the measured aerodynamic behavior suggests that the passive strake produced localized modifications to the flow near the nacelle-wing junction, resulting in lower aerodynamic drag. Although the present investigation did not directly measure the underlying flow structures responsible for these improvements, the observed aerodynamic trends are consistent with previous investigations of passive strakes devices operating under favorable local flow conditions [20, 8, 29].

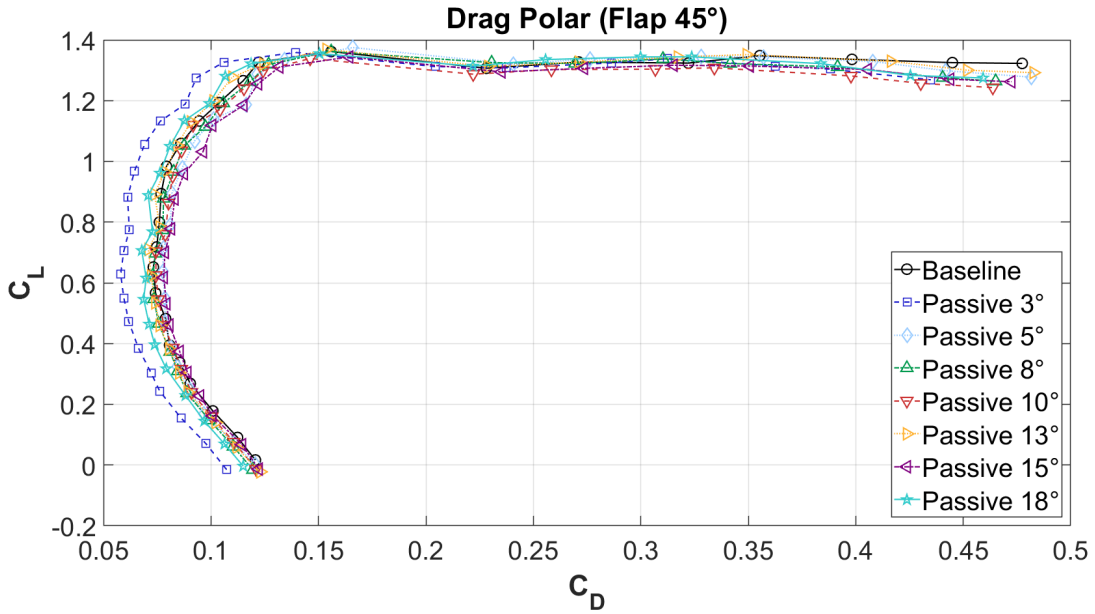


Figure 5.14: Wing Drag Polar for all passive strake configurations with the baseline at $\delta_f = 45^\circ$.

Within the high- α regime ($\alpha > 8^\circ$), C_D increased for all configurations as the wing model approached $\alpha_{\text{stall}} = 13^\circ$ and large-scale flow separation became increasingly prevalent. Nevertheless, the 3° passive strake generally maintained lower C_D values than both the remaining passive strakes and the baseline throughout much of this operating range. This observation demonstrates that the aerodynamic benefit associated with the 3° passive strake extended well beyond the operating conditions corresponding to minimum drag.

Overall, the landing investigation demonstrates that the aerodynamic benefits associated with the passive strake configurations were restored under the landing configuration. Unlike

the cruise investigation, where the 10° passive strake produced the greatest reduction in drag, the landing investigation identified the 3° passive strake as the most effective configuration. These findings demonstrate that the aerodynamic effectiveness of passive strakes is strongly dependent upon the operating condition and local flow environment.

5.4.3 Lift-to-Drag Ratio Behavior

Figure 5.15 shows the variation of L/D with α for the passive strake configurations tested at $\delta_f = 45^\circ$. Since L/D provides the most comprehensive measure of aerodynamic performance because it reflects both lift generation and drag production. Consequently, it serves as the primary metric for evaluating the overall aerodynamic effectiveness of the passive strake configurations under landing conditions.

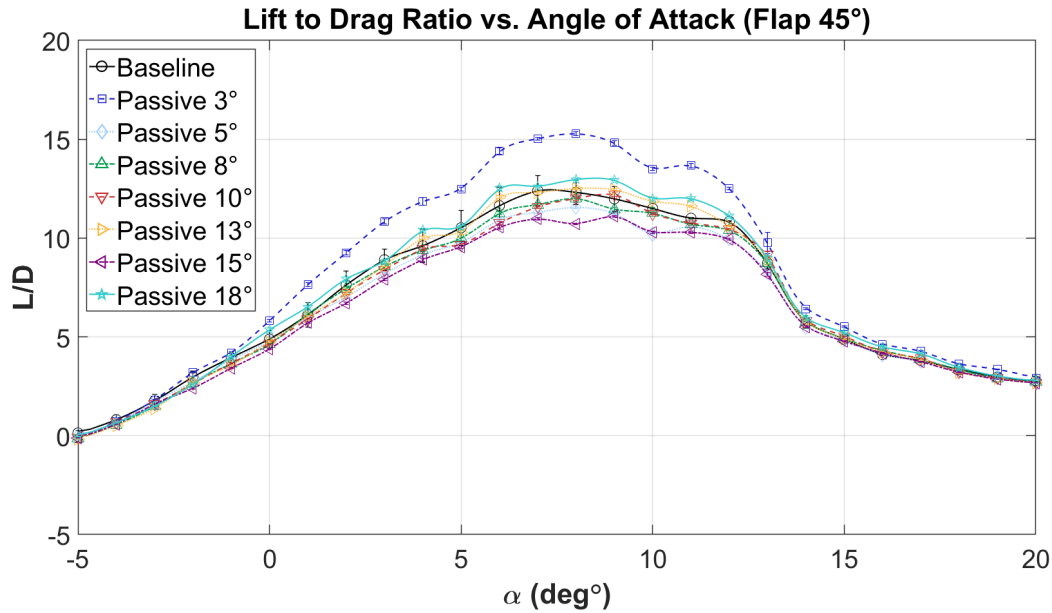


Figure 5.15: Wing L/D vs. α for all passive strake configurations with the baseline at $\delta_f = 45^\circ$.

Within the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), all configurations exhibited very similar aerodynamic efficiencies. This behavior is consistent with the C_L and C_D results presented in Sections 5.4.1 and 5.4.2, which showed only minor differences among the passive strake geometries while the flow remained attached. Consequently, the passive strakes produced

little measurable improvement in aerodynamic efficiency throughout this operating regime.

Moving into the moderate- α regime ($0^\circ < \alpha \leq 10^\circ$), clear differences in aerodynamic performance developed among the passive strake configurations. The 3° passive strake consistently produced the highest L/D values throughout much of this operating range, reflecting the substantial C_D reductions discussed in Section 5.4.2. The baseline configuration achieved $(L/D)_{\max} \approx 12.45$ at $\alpha = 7^\circ$. In comparison, the 3° passive strake increased to $(L/D)_{\max} \approx 15.28$ at $\alpha = 3^\circ$, corresponding to an improvement of approximately 22.8%. The greatest improvements in aerodynamic efficiency occurred throughout the moderate- α regime, corresponding directly to the operating conditions where the largest reductions in C_D were observed.

When considered together, the C_L , C_D , and drag polar results demonstrate that the improvements in L/D resulted primarily from reductions in drag rather than increases in lift generation. Throughout the operating range where the largest increases in L/D occurred, the C_L values differed by less than approximately 2% between the passive configurations and the baseline. In contrast, the 3° passive strake reduced $C_{D,\min}$ by approximately 20.8% while consistently maintaining lower C_D values than the baseline at equivalent C_L values. These observations demonstrate that the passive strake improved the aerodynamic performance through reductions in aerodynamic drag rather than significant increases in lift generation.

Within the high- α regime ($\alpha > 10^\circ$), L/D decreased for all configurations as aerodynamic drag increased and the wing model approached $\alpha_{\text{stall}} = 13^\circ$. Nevertheless, the 3° passive strake generally maintained greater aerodynamic efficiency than both the baseline and the remaining passive configurations throughout much of this operating range, demonstrating that its aerodynamic benefits extended beyond the operating conditions corresponding to the $(L/D)_{\max}$.

Overall, the landing investigation demonstrates that passive strake configurations can provide meaningful improvements in aerodynamic efficiency when their geometry is appropriately matched to the operating condition. Unlike the cruise investigation, where the 10°

passive strake produced the greatest improvement in aerodynamic performance, the landing investigation identified the 3° passive strake as the optimal passive configuration. The close agreement among C_L , C_D , and drag polar results demonstrates that these improvements were achieved primarily through reductions in aerodynamic drag rather than substantial increases in lift generation. A summary of the principal aerodynamic performance metrics for the passive landing investigation is presented in Table 5.3.

Table 5.3: Summary of Passive Strake Landing Aerodynamic Performance

Config.	$C_{L,\max}$	$\alpha@C_{L,\max}$	$C_{D,\min}$	$\alpha@C_{D,\min}$	$(L/D)_{\max}$	$\alpha@(L/D)_{\max}$	$\Delta(L/D)_{\max}$
Baseline	1.362	13°	0.0732	3°	12.45	7°	No Chg.
Passive 3°	1.359	13°	0.0580	3°	15.28	8°	+22.8%
Passive 5°	1.375	13°	0.0776	3°	11.56	8°	-7.2%
Passive 8°	1.362	13°	0.0730	3°	12.01	8°	-3.5%
Passive 10°	1.339	13°	0.0743	4°	12.21	9°	-1.9%
Passive 13°	1.369	13°	0.0710	4°	12.51	8°	+0.5%
Passive 15°	1.344	13°	0.0780	3°	11.12	9°	-10.7%
Passive 18°	1.355	13°	0.0678	4°	12.97	8°	+4.2%

5.5 Active Strake Results: Cruise (0° Flaps)

5.5.1 Lift Coefficient Behavior

Figure 5.16 illustrates the variation of C_L with α for the selected 18° active pneumatic strake at the three pneumatic injection locations (H1, H2, and H3), together with the optimal 10° passive strake and the baseline no-strake configuration at $\delta_f = 0^\circ$. The 18° active strake was selected for detailed evaluation because it produced the greatest overall aerodynamic performance among the active strake configurations investigated under cruise conditions. This section evaluates how the location of pneumatic injection influenced lift production

relative to both the optimal passive configuration and the baseline.

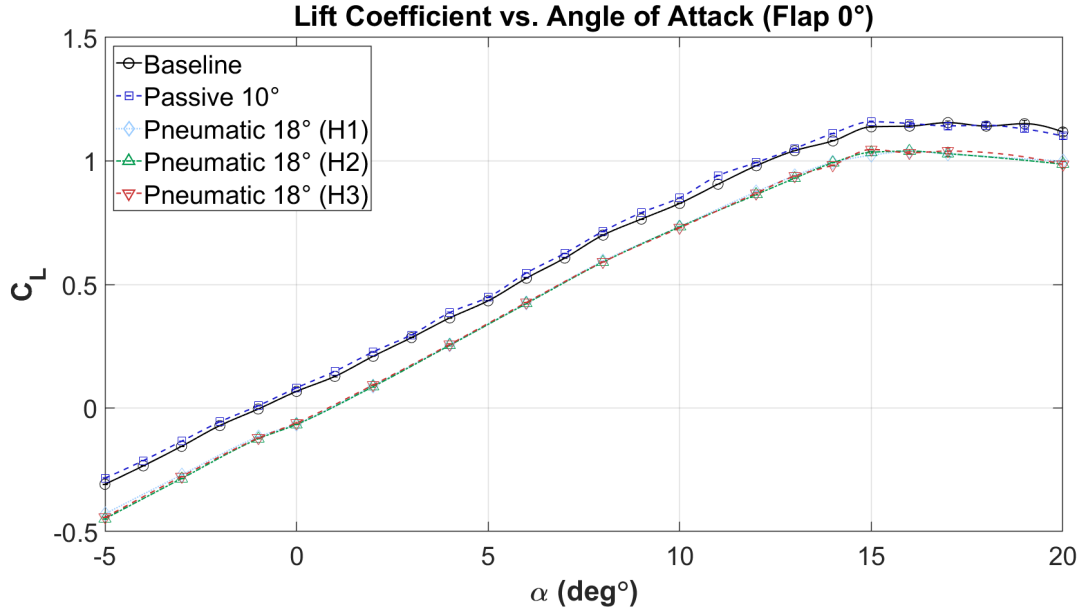


Figure 5.16: Wing C_L vs. α for the optimal passive and active strake configurations with the baseline at $\delta_f = 0^\circ$.

Within the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), all three 18° active strake configurations produced lower C_L values than both the baseline and the 10° passive strake. Measurable differences were also observed among the three blowing locations, with H1 generally producing the lowest C_L values, while H2 and H3 produced slightly higher C_L values throughout much of this operating range. These observations indicate that pneumatic blowing reduced lift generation even while the flow remained attached, with the magnitude of the reduction depending upon the location of pneumatic injection.

Throughout the moderate- α regime ($0^\circ < \alpha \leq 10^\circ$), the 10° passive strake consistently produced the highest C_L values and generally remained close to those of the baseline configuration. All three actively blown configurations continued to produce lower C_L values throughout most of this operating range. Among the active blowing locations, H3 generally produced the highest C_L values, followed closely by H2, whereas H1 consistently produced the lowest C_L values. Although pneumatic injection influenced the magnitude of lift reduction, none of the actively blown configurations exceeded the C_L values of either the baseline

or the 10° passive strake.

Within the high- α regime ($\alpha > 10^\circ$), the differences between the 18° active and the 10° passive strake configurations became steadily apparent, especially as α approached $\alpha_{\text{stall}} = 15^\circ$. The baseline configuration produced $C_{L,\text{max}} \approx 1.138$, while the 10° passive strake increased slightly to $C_{L,\text{max}} \approx 1.159$. In contrast, the H1, H2, and H3 blowing locations produced $C_{L,\text{max}}$ values of approximately 1.039, 1.040, and 1.047, respectively. Among the actively blown configurations, H3 produced the greatest $C_{L,\text{max}}$, whereas H1 produced the lowest. Nevertheless, even the H3 blowing location remained approximately 9.7% lower than the optimal passive strake. These results indicate that pneumatic blowing reduced maximum lift generation under cruise conditions.

Although small differences in lift were observed among the three blowing locations, pneumatic blowing produced little measurable influence on stall. The stall remained at $\alpha_{\text{stall}} = 15^\circ$ for the baseline, passive strake, and active strake configurations within the 1° angular resolution of the experiment, indicating that the active strake altered the magnitude of lift generation but did not appreciably delay stall.

Overall, the 18° active pneumatic strake configuration did not improve lift performance under cruise conditions. Instead, pneumatic blowing reduced the measured C_L values relative to both the baseline and the 10° passive strake configuration. Among the three blowing locations investigated for the 18° active strake, H3 produced the highest $C_{L,\text{max}}$; however, neither H3 nor the remaining blowing locations achieved lift performance equal to that of either the baseline or the 10° passive strake. These results demonstrate that any aerodynamic benefit provided by active blowing under cruise conditions was achieved primarily through reductions in drag rather than increases in lift generation.

5.5.2 Drag Coefficient Behavior

Figure 5.17 illustrates the variation of C_D with α for the selected 18° active pneumatic strake at the three pneumatic injection locations (H1, H2, and H3), together with the optimal

10° passive strake and the baseline no-strake configuration at $\delta_f = 0^\circ$. Unlike the C_L characteristics discussed in Section 5.5.1, all three pneumatic injection locations produced substantially lower C_D values than both the baseline and the 10° passive strake throughout much of the cruise operating envelope. Furthermore, the differences observed among H1, H2, and H3 demonstrate that the location of pneumatic injection significantly influenced aerodynamic performance. These data indicate that the primary aerodynamic advantage of the 18° active strake under cruise conditions resulted from reductions in drag rather than increases in lift generation.

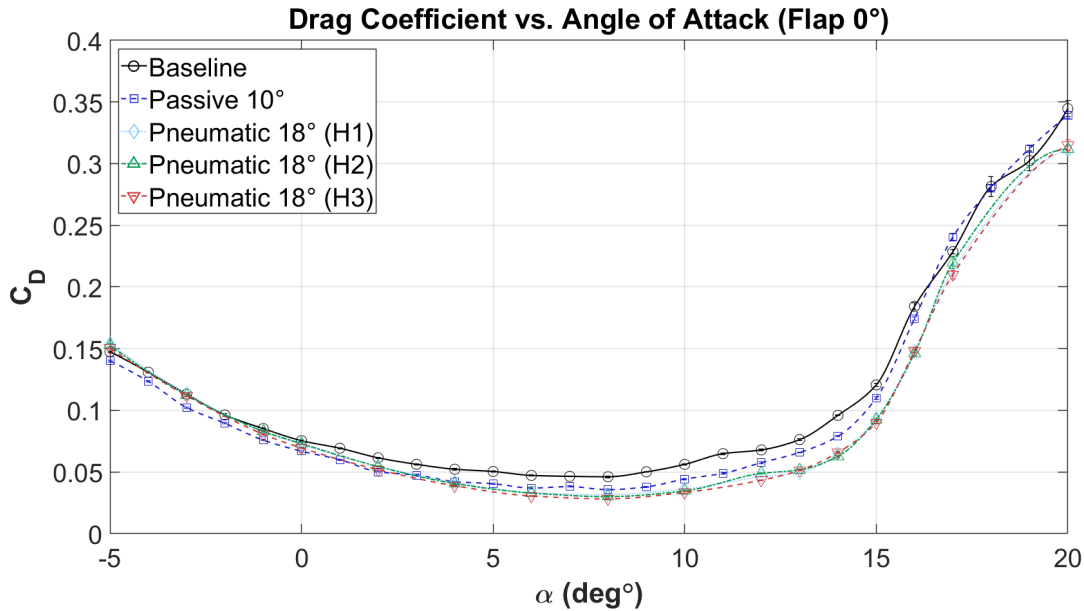


Figure 5.17: Wing C_D vs. α for the optimal passive and active strake configurations with the baseline at $\delta_f = 0^\circ$.

Within the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), the 10° passive strake and the 18° active strake both exhibited relatively low C_D values relative to the baseline configuration. Although the actively blown configurations generally produced slightly lower C_D values than both the baseline and the 10° passive strake, some of these differences remained within the associated 95% confidence intervals. Consequently, pneumatic blowing produced only modest improvements in drag performance while the flow remained attached.

Moving into the moderate- α regime ($0^\circ < \alpha \leq 10^\circ$), the aerodynamic benefits of pneu-

matic blowing became increasingly apparent. Throughout much of this operating range, all three active injection locations produced lower C_D values than both the baseline and the 10° passive strake. Among the three blowing locations, H3 produced the greatest drag reduction, achieving $C_{D,\min} \approx 0.0282$ at $\alpha = 8^\circ$. Compared to the baseline configuration which increased to $C_{D,\min} \approx 0.0461$ at the same α , representing approximately a 38.8% reduction in drag. Relative to the 10° passive strake, which achieved $C_{D,\min} \approx 0.0357$ at $\alpha = 8^\circ$, the H3 configuration achieved an additional 21.0% reduction in drag. These results clearly demonstrate that pneumatic blowing substantially reduced aerodynamic drag under cruise conditions.

Further insight is obtained by examining the drag polar shown in Figure 5.18. At equivalent C_L values, all three active strake configurations generally maintained lower C_D values than both the baseline and 10° passive strake throughout much of the tested operating envelope. Therefore, the observed reductions in drag cannot be attributed solely to differences in lift generation. Instead, the drag polar results indicate that pneumatic blowing modified the local flow field near the nacelle-wing junction in a manner that reduced aerodynamic losses. Although the present investigation did not directly measure the underlying flow structures responsible for these improvements, the measured drag reductions are consistent with those reported by Greenblatt and Wygnanski and Seifert, who demonstrated that momentum injection can beneficially modify local boundary layer behavior and reduce aerodynamic losses under appropriate operating conditions [8, 25].

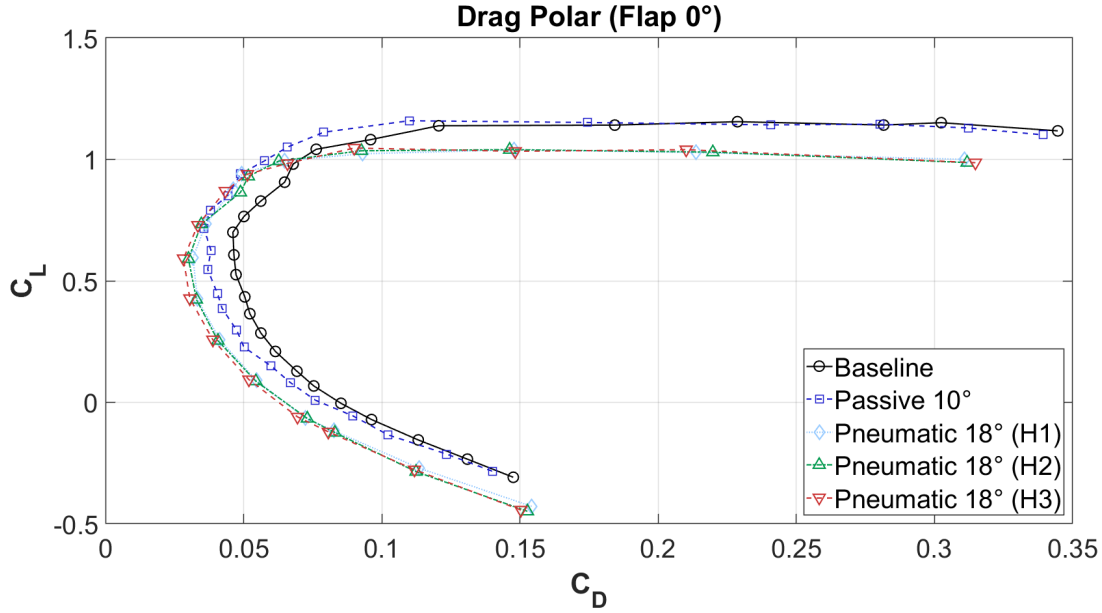


Figure 5.18: Wing Drag Polar for the optimal passive and active strake configurations with the baseline at $\delta_f = 0^\circ$.

Within the high- α regime ($\alpha > 10^\circ$), C_D values increased rapidly for all configurations as the wing model approached $\alpha_{\text{stall}} = 15^\circ$. Nevertheless, the 18° active strake generally continued to maintain lower C_D values than both the baseline and the 10° passive strake throughout much of this operating range. Although the differences among H1, H2, and H3 gradually diminished as stall was approached, H3 generally remained the lowest drag configuration, indicating that the aerodynamic benefits of pneumatic blowing extended beyond the operating condition corresponding to minimum drag.

Overall, the active pneumatic strake investigation demonstrated that pneumatic blowing substantially improved drag performance under cruise conditions. Among the three injection locations investigated, H3 produced the greatest drag reduction, while H2 and H1 provided progressively smaller drag reductions. These findings demonstrate that the effectiveness of the 18° active strake is strongly dependent upon the location of pneumatic injection and that, under cruise conditions, the principal aerodynamic benefit of pneumatic blowing was achieved through reductions in drag rather than improvements in lift generation.

5.5.3 Lift-to-Drag Ratio Behavior

Figure 5.19 illustrates the variation of L/D with α for the selected 18° active pneumatic strake at the three pneumatic injection locations (H1, H2, and H3), together with the optimal 10° passive strake and the baseline no-strake configuration at $\delta_f = 0^\circ$. Because L/D incorporates both C_L and C_D , it provides the most comprehensive measure of aerodynamic performance. Consequently, the L/D results are used to determine whether the reductions in C_L discussed in Section 5.5.1 were sufficient to offset the reductions in C_D presented in Section 5.5.2.

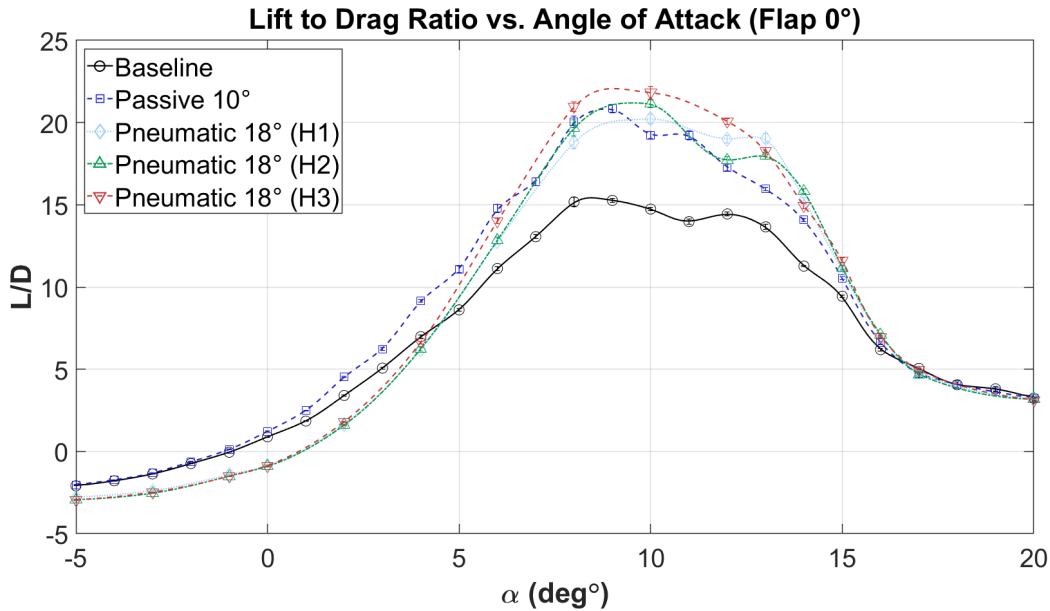


Figure 5.19: Wing L/D vs. α for the optimal passive and active strake configurations with the baseline at $\delta_f = 0^\circ$.

Within the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), the actively blown configurations generally produced lower L/D values than both the baseline and the 10° passive strake. The reductions in aerodynamic efficiency observed throughout this operating range primarily resulted from the reductions in C_L discussed in Section 5.5.1 because the reductions in C_D presented in Section 5.5.2 were not yet sufficiently large to offset those losses in lift. Furthermore, the measured differences exceeded the associated 95% confidence intervals throughout much of

this operating range, indicating that the reductions in aerodynamic efficiency were statistically distinguishable from the experimental uncertainty.

As α increased into the moderate- α regime ($0^\circ < \alpha \leq 10^\circ$), the aerodynamic benefits of pneumatic flow control became increasingly apparent. Throughout much of this operating range, all three pneumatic injection locations produced substantially greater L/D values than the baseline configuration. Among the actively blown configurations, H3 consistently exhibited the highest aerodynamic efficiency, followed closely by H2, while H1 produced the lowest L/D values. The H3 configuration achieved a maximum aerodynamic efficiency of $(L/D)_{\max} \approx 21.83$ at $\alpha = 10^\circ$, representing an improvement of approximately 42.9% relative to the baseline configuration at $(L/D)_{\max} \approx 15.27$ at $\alpha = 9^\circ$, and approximately a 4.7% improvement relative to the optimal 10° passive strake at $(L/D)_{\max} \approx 20.85$ at $\alpha = 9^\circ$. These findings demonstrate that the substantial reductions in C_D produced by pneumatic blowing more than compensated for the relatively modest reductions in C_L observed under cruise conditions.

The analyses of C_L , C_D , and the drag polar collectively explain the measured improvements in aerodynamic efficiency. Although pneumatic blowing reduced C_L relative to both the baseline and the 10° passive strake, the substantially larger reductions in C_D more than compensated for those losses. Furthermore, the drag polar demonstrated that the actively blown configurations maintained lower C_D values than both the baseline and the 10° passive strake while producing equivalent C_L values throughout much of the operating envelope. Therefore, the measured improvements in aerodynamic efficiency resulted primarily from reductions in C_D rather than enhancements in C_L .

Within the high- α regime ($\alpha > 10^\circ$), L/D decreased rapidly for all configurations as C_D increased approaching $\alpha_{\text{stall}} = 15^\circ$. Nevertheless, the actively blown configurations generally maintained greater aerodynamic efficiencies than the baseline throughout much of this operating range. Although the differences among H1, H2, and H3 gradually diminished as stall was approached, H3 generally remained the highest-performing injection location, indi-

cating that the aerodynamic benefits of pneumatic blowing extended beyond the operating condition corresponding to maximum aerodynamic efficiency.

Overall, the cruise investigation demonstrated that the 18° active strake produced the greatest overall aerodynamic performance among the active strake configurations investigated. Although pneumatic blowing reduced C_L relative to both the baseline and the 10° passive strake, the substantially larger reductions in C_D resulted in greater L/D values throughout much of the cruise operating envelope. Among the three pneumatic injection locations investigated, H3 consistently produced the highest aerodynamic efficiency, increasing $(L/D)_{\max}$ by approximately 42.9% relative to the baseline and approximately 4.7% relative to the 10° passive strake. These findings demonstrate that the aerodynamic benefits of AFC under cruise conditions were achieved primarily through reductions in C_D , with the H3 injection location providing the greatest overall aerodynamic performance. A summary of the principal aerodynamic performance metrics of the 10° passive strake and 18° active strake for the cruise investigation is presented in Table 5.4.

Table 5.4: Summary of Optimal Passive-Active Strake Cruise Aerodynamic Performance

Config.	$C_{L,\max}$	$\alpha@C_{L,\max}$	$C_{D,\min}$	$\alpha@C_{D,\min}$	$(L/D)_{\max}$	$\alpha@(L/D)_{\max}$	$\Delta(L/D)_{\max}$
Baseline	1.138	15°	0.0461	8°	15.27	9°	No Chg.
10° Passive	1.159	15°	0.0357	8°	20.85	9°	+36.5%
18° HL1	1.039	15°	0.0317	8°	20.24	10°	+32.5%
18° HL2	1.040	15°	0.0300	8°	21.25	10°	+38.5%
18° HL3	1.047	15°	0.0282	8°	21.83	10°	+42.9%

5.6 Active Strake Results: Takeoff (20° Flaps)

5.6.1 Lift Coefficient Behavior

Figure 5.20 illustrates the variation of C_L with α for the selected 15° active pneumatic strake at the three pneumatic injection locations (H1, H2, and H3), together with the optimal

8° passive strake and the baseline no-strake configuration at $\delta_f = 20^\circ$. The 15° active strake was selected for detailed evaluation because it produced the greatest overall aerodynamic performance among the active strake configurations investigated under takeoff conditions. This section examines how pneumatic injection influences lift generation relative to both the optimal passive configuration and the baseline.

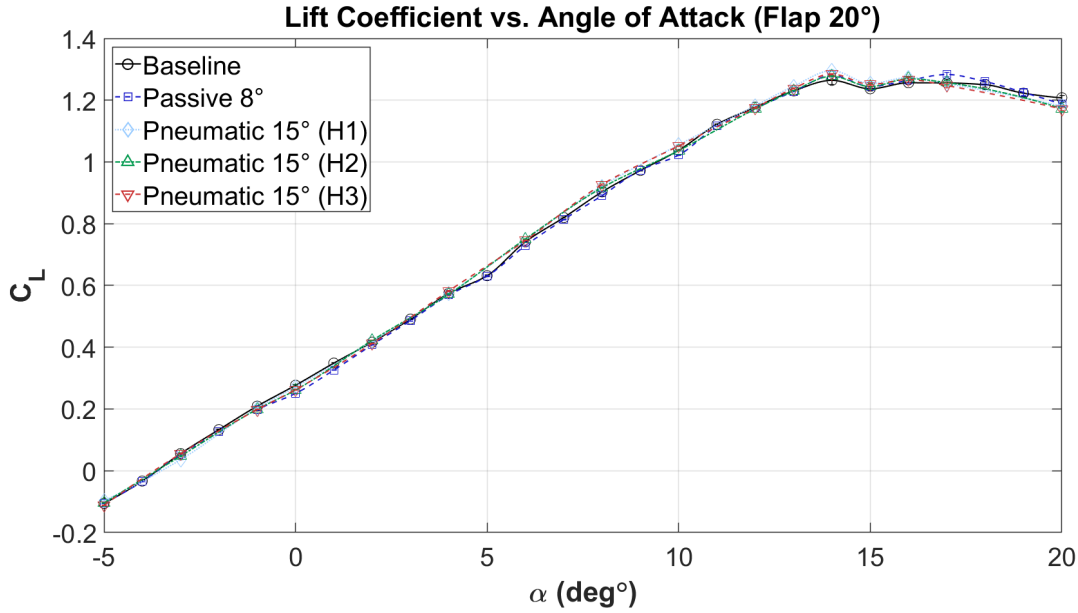


Figure 5.20: Wing C_L vs. α for the optimal passive and active strake configurations with the baseline at $\delta_f = 20^\circ$.

Within the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), all three active pneumatic injection locations produced C_L values that were very similar to those of both the baseline and the 8° passive strake. Although slight differences were observed among H1, H2, and H3, they generally remained within the associated 95% confidence intervals. Consequently, pneumatic blowing produced little, if any, measurable influence on lift generation while the flow remained largely attached throughout this operating regime.

As α increased into the moderate- α regime ($0^\circ < \alpha \leq 10^\circ$), the 15° active strake exhibited slightly higher C_L values than both the baseline and the 8° passive strake over portions of the operating envelope. Among the active strake configurations, H1 generally produced the highest C_L values, followed closely by H3 and H2. Although these improvements were

relatively modest, they demonstrate that pneumatic injection can produce measurable increases in lift under takeoff conditions. Unlike the cruise investigation, where pneumatic blowing consistently reduced C_L throughout much of the operating envelope, active blowing under takeoff conditions produced modest improvements in lift generation.

Within the high- α regime ($\alpha > 10^\circ$), the aerodynamic differences between the baseline, passive strake, and active strake configurations became more apparent, primarily as the wing model approached $\alpha_{\text{stall}} = 14^\circ$. The baseline configuration achieved $C_{L,\text{max}} \approx 1.264$, while the 8° passive strake increased to $C_{L,\text{max}} \approx 1.282$. The H1, H2, and H3 active strake configurations achieved $C_{L,\text{max}} \approx 1.296$, 1.278 , and 1.285 , respectively. Among the active strake configurations, H1 produced the highest $C_{L,\text{max}}$ values, exceeding both the baseline and the 8° passive strake by approximately 2.5% and 1.1%, respectively. Unlike the cruise investigation, pneumatic blowing therefore produced a modest increase in lift-generating capability during takeoff.

The stall occurred at $\alpha_{\text{stall}} = 14^\circ$ for all active strake configurations, indicating that pneumatic injection had little measurable influence on stall delay. Instead, the active strake primarily altered the magnitude of lift generation while producing negligible change in the initial α_{stall} .

Overall, the results demonstrate that active pneumatic blowing produced a fundamentally different aerodynamic response under takeoff conditions than under cruise conditions. Whereas pneumatic blowing reduced both C_L and $C_{L,\text{max}}$ throughout much of the cruise investigation, it produced modest increases in lift during takeoff, with H1 providing the greatest lift enhancement among the three pneumatic injection locations. Nevertheless, the improvements in $C_{L,\text{max}}$ remained relatively small.

5.6.2 Drag Coefficient Behavior

Figure 5.21 illustrates the variation of C_D with α for the selected 15° active pneumatic strake at the three pneumatic injection locations (H1, H2, and H3), together with the optimal

8° passive strake and the baseline no-strake configuration at $\delta_f = 20^\circ$. Unlike the cruise investigation, the actively blown configurations generally produced larger C_D values than both the baseline and 8° passive strake throughout much of the operating envelope. Although the magnitude of the drag increase varied somewhat with pneumatic injection location, all three active strake configurations exhibited increased aerodynamic drag. Consequently, the primary objective of reducing aerodynamic drag through pneumatic blowing was not achieved under takeoff conditions.

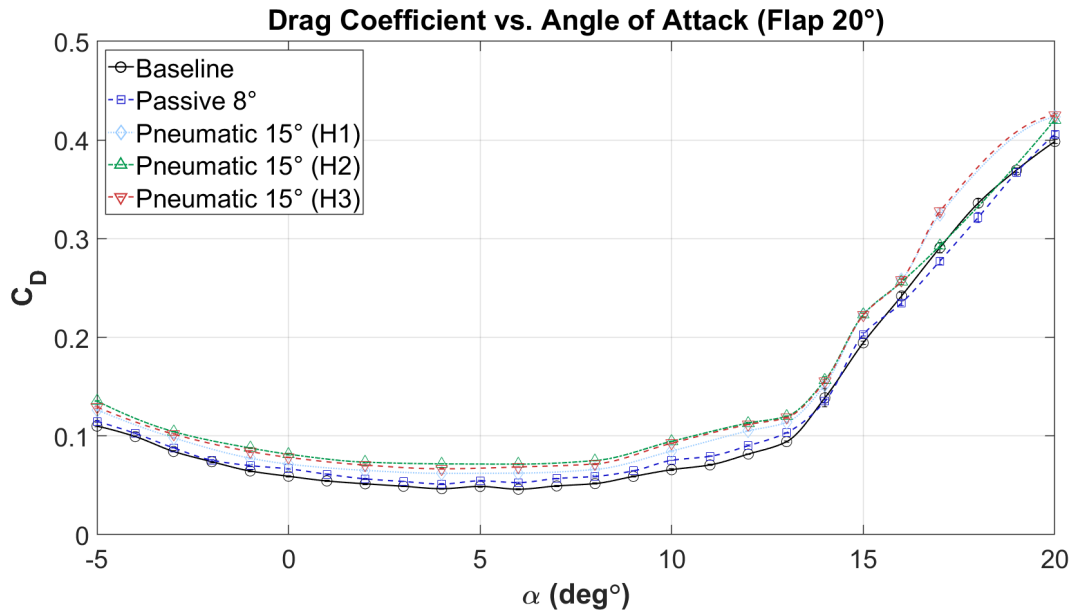


Figure 5.21: Wing C_D vs. α for the optimal passive and active strake configurations with the baseline at $\delta_f = 20^\circ$.

Within the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), all configurations exhibited relatively low C_D values characteristic of predominantly attached flow. Although the differences among the configurations remained relatively modest, the 15° active strake generally produced slightly larger C_D values than both the baseline and 8° passive strake throughout much of this operating range. Among the active strake configurations, H1 generally produced lower C_D values than H2 and H3. Because the flow remained largely attached throughout this operating range, pneumatic blowing provided little, if any, measurable reduction in aerodynamic drag.

Within the moderate- α regime ($0^\circ < \alpha \leq 10^\circ$), the aerodynamic differences became

more pronounced. Unlike the cruise investigation, the baseline configuration consistently maintained the lowest C_D values throughout much of this operating range, followed closely by the 8° passive configuration. Quantitatively, the baseline configuration achieved $C_{D,\min} \approx 0.0460$ at $\alpha = 6^\circ$, while the 8° passive configuration increased to $C_{D,\min} \approx 0.0566$ at $\alpha = 4^\circ$. The H1, H2, and H3 active strake configurations achieved 0.0621 at $\alpha = 4^\circ$, 0.0715 at $\alpha = 6^\circ$, and 0.0667 at $\alpha = 4^\circ$, respectively. Relative to the baseline configuration, these values represent increases in minimum drag of approximately 34.9%, 55.4%, and 45.0%, respectively. Among the active strake configurations, H1 consistently produced the lowest C_D values, while H2 and H3 generated progressively larger C_D values. None of the 15° active strake configurations produced C_D values lower than either the baseline or the 8° passive strake. These results demonstrate that pneumatic blowing introduced an aerodynamic drag penalty under takeoff conditions rather than providing the substantial drag reductions observed during the cruise investigation.

Additional insight into this behavior is obtained by examining the drag polar shown in Figure 5.22. At comparable values of C_L , the 15° active strake generally maintained larger C_D values than both the baseline and 8° passive configurations throughout much of the tested operating envelope. Consequently, the measured increases in drag cannot be attributed solely to differences in lift generation. Instead, the drag polar results indicate that pneumatic injection altered the local flow field surrounding the nacelle-wing junction in a manner that increased aerodynamic losses under takeoff conditions. Although previous investigations by Greenblatt and Wygnanski, Seifert, and Radespiel demonstrated that momentum injection can reduce aerodynamic losses under favorable operating conditions, the present investigation indicates that those benefits were not realized at $\delta_f = 20^\circ$ [8, 25, 10]. These findings suggest that the effectiveness of AFC depends strongly upon the local flow environment and operating condition.

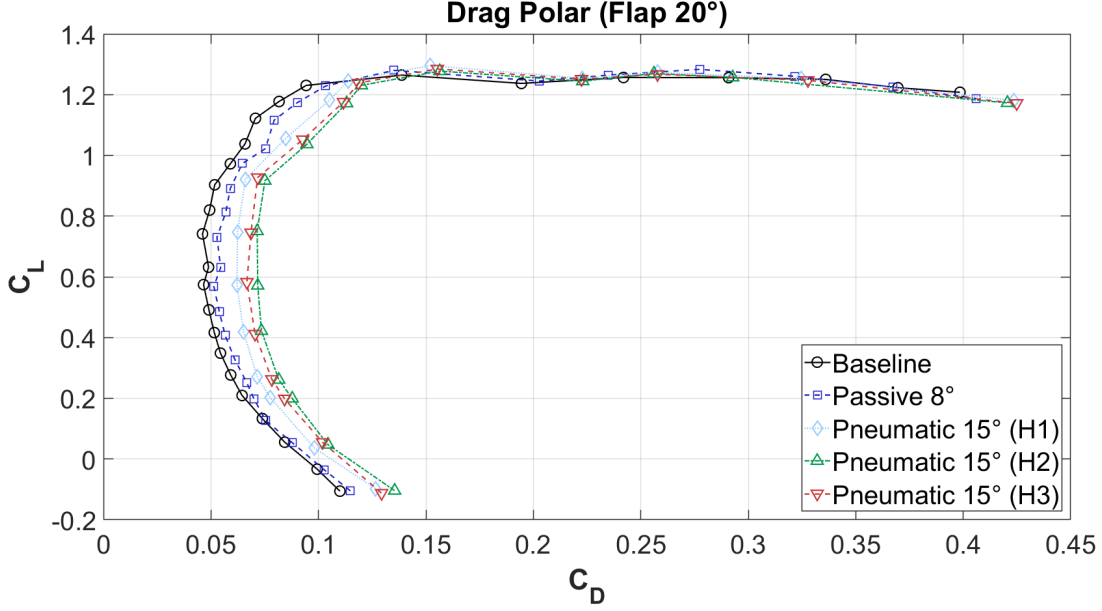


Figure 5.22: Wing Drag Polar for the optimal passive and active strake configurations with baseline at $\delta_f = 20^\circ$.

Within the high- α regime ($\alpha > 10^\circ$), C_D increased rapidly for all configurations. Although the differences among the active injection locations gradually diminished with increasing α , none of the 15° active strake configurations consistently produced lower C_D than either the baseline or the 8° passive strake configuration throughout much of this operating range. H1 generally remained the lowest drag active strake configuration, while H2 and H3 continued to generate the largest drag penalties among the actively blown cases.

Overall, the 15° active pneumatic strake investigation demonstrated that pneumatic blowing did not improve drag performance under takeoff conditions. All three active injection locations generally produced larger C_D values than both the baseline and 8° passive strake throughout much of the operating envelope. Among the active strake configurations, H1 consistently produced the lowest C_D values, while H2 and H3 generated progressively larger C_D values. These findings demonstrate that the aerodynamic effectiveness of AFC depends strongly upon the operating condition. Unlike the cruise investigation, pneumatic blowing was unable to reduce aerodynamic drag during takeoff and instead introduced a measurable drag penalty throughout much of the operating envelope.

5.6.3 Lift-to-Drag Ratio Behavior

The variation of L/D with α for the selected 15° active pneumatic strake at the three pneumatic injection locations (H1, H2, and H3), together with the optimal 8° passive strake and the baseline no-strake configuration at $\delta_f = 20^\circ$, is illustrated in Figure 5.23. Since L/D incorporates both lift generation and drag production, it provides the most comprehensive measure of aerodynamic performance. Consequently, the L/D results are used to determine whether the modest increases in C_L presented in Section 5.6.1 were sufficient to offset the increases in C_D discussed in Section 5.6.2.

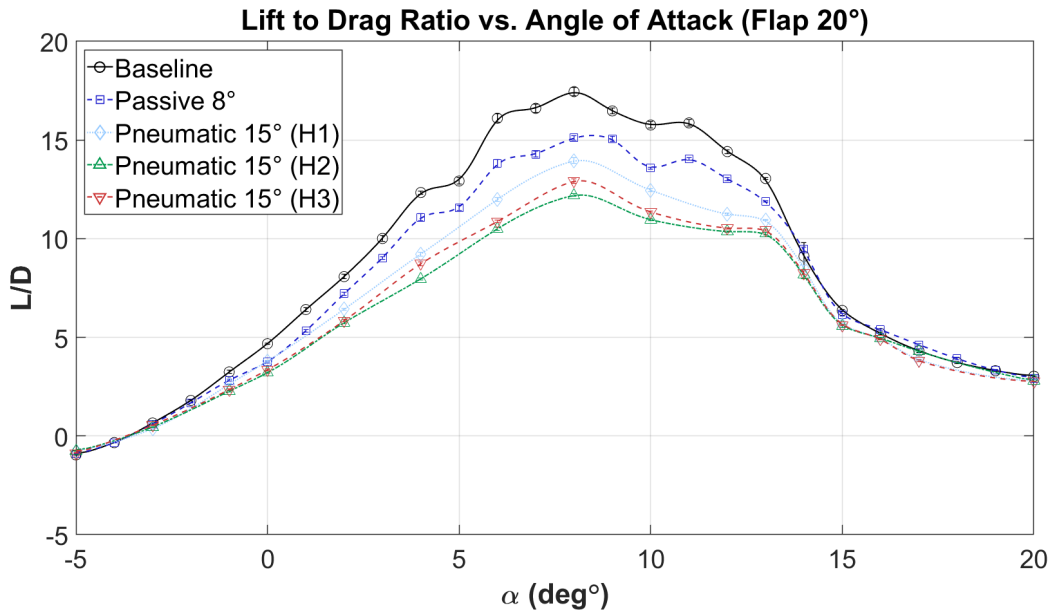


Figure 5.23: Wing L/D vs. α for the optimal passive and active strake configurations with the baseline at $\delta_f = 20^\circ$.

Within the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), the aerodynamic efficiencies of all configurations were relatively similar. However, the 15° active strake generally produced lower L/D values than both the baseline and 8° passive strake throughout much of this operating range. These reductions in aerodynamic efficiency resulted primarily from the modest drag penalties introduced by pneumatic blowing while the flow remained largely attached. Among the active strake configurations, H1 generally produced the highest L/D , followed by H3 and

H2.

As α increased into the moderate- α regime ($0^\circ < \alpha \leq 10^\circ$), the differences in aerodynamic efficiency became considerably more pronounced. Unlike the cruise investigation, the baseline configuration consistently produced the highest L/D values throughout much of this operating range. Specifically, the baseline configuration achieved $(L/D)_{\max} \approx 17.46$ at $\alpha = 8^\circ$, whereas the 8° passive strake decreased to $(L/D)_{\max} \approx 14.54$ at $\alpha = 9^\circ$, representing a reduction in aerodynamic efficiency of approximately 16.7% relative to the baseline. Among the actively blown configurations, H1 achieved $(L/D)_{\max} \approx 13.96$ at $\alpha = 8^\circ$, while H3 and H2 achieved $(L/D)_{\max} \approx 12.93$ and 12.21 , respectively at the same α . Relative to the baseline configuration, these values represent reductions in maximum aerodynamic efficiency of approximately 20.0%, 25.9%, and 30.1%, respectively. These data clearly demonstrate that the modest increases in C_L produced by pneumatic blowing were insufficient to overcome the substantially larger increases in C_D observed under takeoff conditions.

Collectively, the C_L , C_D , and drag polar results explain the measured reductions in aerodynamic efficiency. Although pneumatic blowing produced modest increases in C_L relative to both the baseline and the 8° passive strake, these improvements were more than offset by the substantially larger increases in C_D . Furthermore, as discussed in Section 5.6.2, Figure 5.22 demonstrates that the 15° active strake generally maintained larger C_D values than both the baseline and 8° passive strake while producing comparable values of C_L . Therefore, the reductions in aerodynamic efficiency resulted primarily from increased aerodynamic drag rather than insufficient lift generation.

Within the high- α regime ($\alpha > 10^\circ$), L/D decreased rapidly for all configurations as aerodynamic drag increased when approaching $\alpha_{\text{stall}} = 14^\circ$. Although differences among the active injection locations gradually diminished with increasing α , the baseline configuration generally maintained the highest aerodynamic efficiency throughout much of this operating range. Among the active strake configurations, H1 continued to produce the highest L/D , while H2 generally produced the lowest aerodynamic efficiency.

Overall, the 15° active pneumatic strake investigation demonstrated that pneumatic blowing did not improve overall aerodynamic performance under takeoff conditions. Although pneumatic blowing produced modest increases in C_L , the accompanying increases in C_D resulted in lower L/D values throughout much of the operating envelope. Among the three pneumatic injection locations investigated, H1 consistently produced the greatest aerodynamic efficiency; however, its $(L/D)_{\max}$ remained approximately 20.0% below that of the baseline configuration. These findings demonstrate that the aerodynamic penalties associated with increased drag outweighed the modest gains in lift, indicating that the active strake did not provide an aerodynamic benefit under takeoff conditions. A summary of the principal aerodynamic performance metrics of the 8° passive strake and 15° active strake for the takeoff investigation is presented in Table 5.5.

Table 5.5: Summary of Optimal Passive-Active Strake Takeoff Aerodynamic Performance

Config.	$C_{L,\max}$	$\alpha@C_{L,\max}$	$C_{D,\min}$	$\alpha@C_{D,\min}$	$(L/D)_{\max}$	$\alpha@(L/D)_{\max}$	$\Delta(L/D)_{\max}$
Baseline	1.264	14°	0.0460	6°	17.46	8°	No Chg.
8° Passive	1.280	14°	0.0566	4°	14.54	9°	-16.7%
15° HL1	1.296	14°	0.0621	4°	13.96	8°	-20.0%
15° HL2	1.278	14°	0.0715	6°	12.21	8°	-30.1%
15° HL3	1.285	14°	0.0667	4°	12.93	8°	-25.9%

5.7 Active Strake Results: Landing (45° Flaps)

5.7.1 Lift Coefficient Behavior

Figure 5.24 illustrates the variation of C_L with α for the selected 5° active pneumatic strake at the three pneumatic injection locations (H1, H2, and H3), together with the optimal 3° passive strake and the baseline no-strake configuration at $\delta_f = 45^\circ$. The 5° active pneumatic strake was selected for detailed evaluation because it provided the greatest overall aerodynamic performance among the active strake configurations investigated under landing

conditions. This section examines the influence of pneumatic injection on lift generation relative to both the 3° passive strake and the baseline.

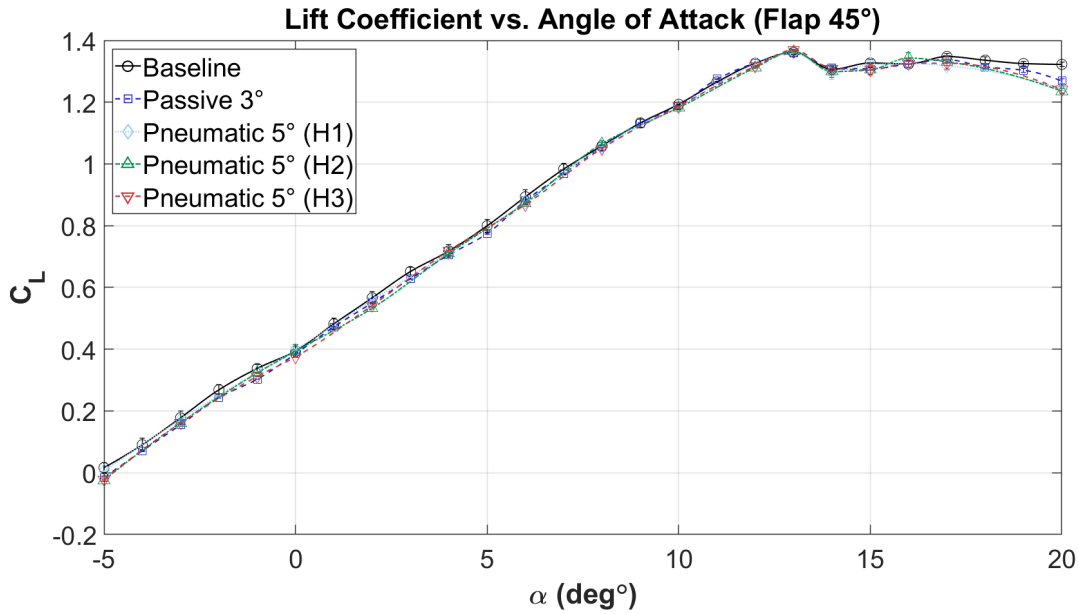


Figure 5.24: Wing C_L vs. α for the optimal passive and active strake configurations with the baseline at $\delta_f = 45^\circ$.

Within the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), all configurations exhibited very similar values of C_L . Although small differences were observed among the three active injection locations, these differences generally remained within their associated 95% confidence intervals. Consequently, pneumatic blowing produced little measurable influence on lift generation while the flow remained largely attached throughout this operating range. Among the active strake configurations, H2 generally produced the highest C_L values, while H3 generally produced the lowest.

Moving into the moderate- α regime ($0^\circ < \alpha \leq 10^\circ$), the lift characteristics of all configurations remained remarkably similar. Although modest differences in C_L became apparent among the active injection locations, none of these differences consistently produced C_L values greater than either the baseline or the 3° passive configuration. Among the active strake configurations, H2 generally produced slightly higher C_L values than H1 and H3 throughout portions of this operating range; however, these differences remained relatively small, indi-

cating that pneumatic blowing had only a modest influence on lift generation under landing conditions.

Within the high- α regime ($\alpha > 10^\circ$), the aerodynamic differences became slightly more apparent, primarily as the wing model approached $\alpha_{\text{stall}} = 13^\circ$. The baseline configuration produced $C_{L,\text{max}} \approx 1.362$, while the optimal 3° passive strake produced $C_{L,\text{max}} \approx 1.359$. The H1, H2, and H3 active strake configurations produced $C_{L,\text{max}}$ values of approximately 1.361, 1.365, and 1.369, respectively. Among the active strake configurations, H3 produced the greatest $C_{L,\text{max}}$, exceeding both the baseline and the optimal passive strake by approximately 0.5% and 0.7%, respectively. Although these increases were measurable, their overall magnitude remained relatively small.

The stall occurred at $\alpha_{\text{stall}} = 13^\circ$ for all active strake configurations, indicating that pneumatic blowing had little measurable influence on stall delay. Instead, the active strake primarily altered the magnitude of lift generation while producing negligible change in the initial α_{stall} .

Overall, the results demonstrate that pneumatic blowing produced only modest changes in C_L under landing conditions. Unlike the cruise investigation, where active blowing generally reduced lift, and the takeoff investigation, where modest increases in lift were observed, the landing investigation showed that pneumatic injection produced only relatively small changes in lift generation. Among the three pneumatic injection locations investigated, H3 produced the largest C_L , followed closely by H2 and H1; however, the differences between the active strake configurations and the baseline remained relatively small. These findings suggest that any improvements in overall aerodynamic performance under landing conditions are more likely to arise from changes in C_D than from substantial increases in lift generation.

5.7.2 Drag Coefficient Behavior

Figure 5.25 illustrates the variation of C_D with α for the selected 5° active pneumatic strake at the three pneumatic injection locations (H1, H2, and H3), together with the op-

timal 3° passive strake and the baseline no-strake configuration at $\delta_f = 45^\circ$. Unlike the cruise investigation, the 5° active strake generally produced larger values of C_D than both the baseline and 3° passive strake throughout much of the operating envelope. Although the magnitude of the drag increase varied somewhat with pneumatic injection location, all three active strake configurations introduced an aerodynamic drag penalty under landing conditions. Consequently, pneumatic blowing did not improve drag performance at $\delta_f = 45^\circ$.

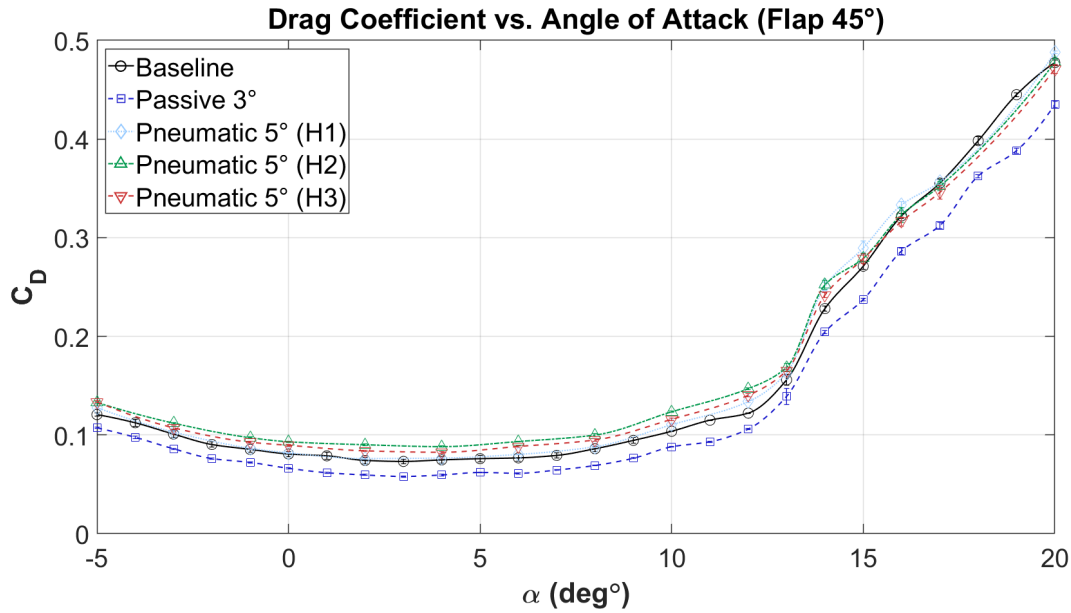


Figure 5.25: Wing C_D vs. α for the optimal passive and active strake configurations with the baseline at $\delta_f = 45^\circ$.

Within the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), all configurations exhibited relatively low values of C_D characteristic of predominantly attached flow. Although only modest differences existed among the configurations, the 5° active strake generally produced slightly larger values of C_D than both the baseline and 3° passive strake throughout much of this operating range. Among the active strake configurations, H1 generally maintained the lowest C_D values, followed closely by H3, while H2 consistently produced the largest C_D values. Because the flow remained largely attached throughout this operating range, pneumatic blowing provided little measurable aerodynamic benefit.

As α increased into the moderate- α regime ($0^\circ < \alpha \leq 10^\circ$), the aerodynamic differences

became considerably more pronounced. The 3° passive strake consistently maintained the lowest values of C_D throughout much of this operating range, followed by the baseline configuration. Specifically, the passive 3° strake achieved $C_{D,\min} \approx 0.0580$ at $\alpha = 3^\circ$, representing a reduction of approximately 20.8% relative to the baseline configuration, which achieved $C_{D,\min} \approx 0.0732$ at $\alpha = 3^\circ$. The H1, H2, and H3 active strake configurations achieved $C_{D,\min} \approx 0.0761$ at $\alpha = 2^\circ$, 0.0882 at $\alpha = 4^\circ$, and 0.0825 at $\alpha = 4^\circ$, respectively. Relative to the baseline configuration, these values correspond to increases in minimum drag of approximately 4.0%, 20.5%, and 12.7%, respectively. Among the active strake configurations, H1 consistently produced the lowest values of C_D , while H2 generated the largest values of C_D . These results clearly demonstrate that pneumatic blowing did not recover the drag reduction benefits achieved by the 3° passive strake under landing conditions.

Additional insight into these results is obtained through examination of the drag polar shown in Figure 5.26. At comparable values of C_L , the 5° active strake generally produced larger values of C_D than both the baseline and 3° passive strake throughout much of the tested operating envelope. Consequently, the measured drag increases cannot be attributed solely to differences in lift generation. Rather, the drag polar results indicate that pneumatic injection altered the local flow field surrounding the nacelle-wing junction in a manner that increased aerodynamic losses under landing conditions. Previous investigations by Greenblatt and Wagnanski, Seifert, and Radespiel demonstrated that momentum injection can reduce aerodynamic losses under favorable operating conditions [8, 25, 10]. However, those benefits were not realized at $\delta_f = 45^\circ$. These findings further demonstrate that the effectiveness of AFC depends strongly upon both the local flow environment and the operating condition.

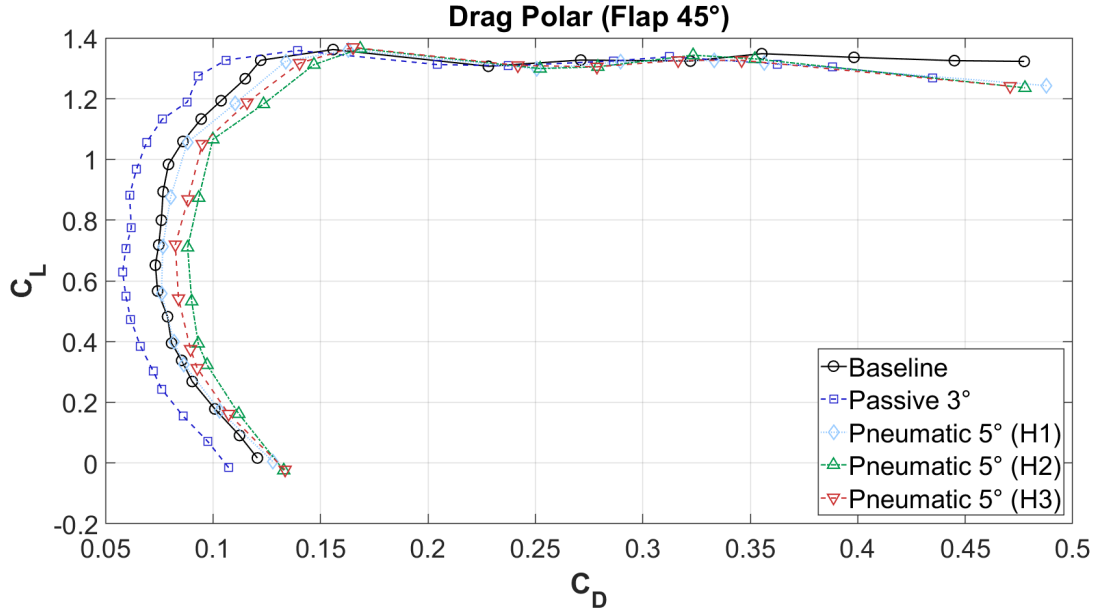


Figure 5.26: Wing Drag Polar for the optimal passive and active strake configurations with the baseline at $\delta_f = 45^\circ$.

Within the high- α regime ($\alpha > 10^\circ$), C_D increased rapidly for all configurations as the wing model approached $\alpha_{\text{stall}} = 13^\circ$. Although the differences among the active injection locations gradually diminished with increasing α , none of the 5° active strake configurations produced lower values of C_D than either the baseline or 3° passive configuration throughout much of this operating range. H1 generally remained the lowest drag active strake configuration, while H2 consistently generated the largest drag penalty.

Overall, the active pneumatic strake investigation demonstrated that pneumatic blowing did not improve drag performance under landing conditions. All three active injection locations generally produced larger values of C_D than both the baseline and 3° passive strake throughout much of the operating envelope. Among the active strake configurations, H1 consistently produced the lowest C_D values, while H2 and H3 generated progressively larger C_D values. These findings demonstrate that, unlike the cruise investigation, pneumatic blowing was unable to reduce aerodynamic drag under landing conditions and instead diminished the aerodynamic benefits previously achieved by the 3° passive strake.

5.7.3 Lift-to-Drag Ratio Behavior

Figure 5.27 illustrates the variation of L/D with α for the selected 5° active pneumatic strake at the three pneumatic injection locations (H1, H2, and H3), together with the optimal 3° passive strake and the baseline no-strake configuration at $\delta_f = 45^\circ$. Because L/D incorporates both lift generation and drag production, it provides the most comprehensive measure of aerodynamic performance. Consequently, the L/D results are used to determine whether the modest changes in C_L discussed in Section 5.7.1 were sufficient to offset the changes in C_D presented in Section 5.7.2.

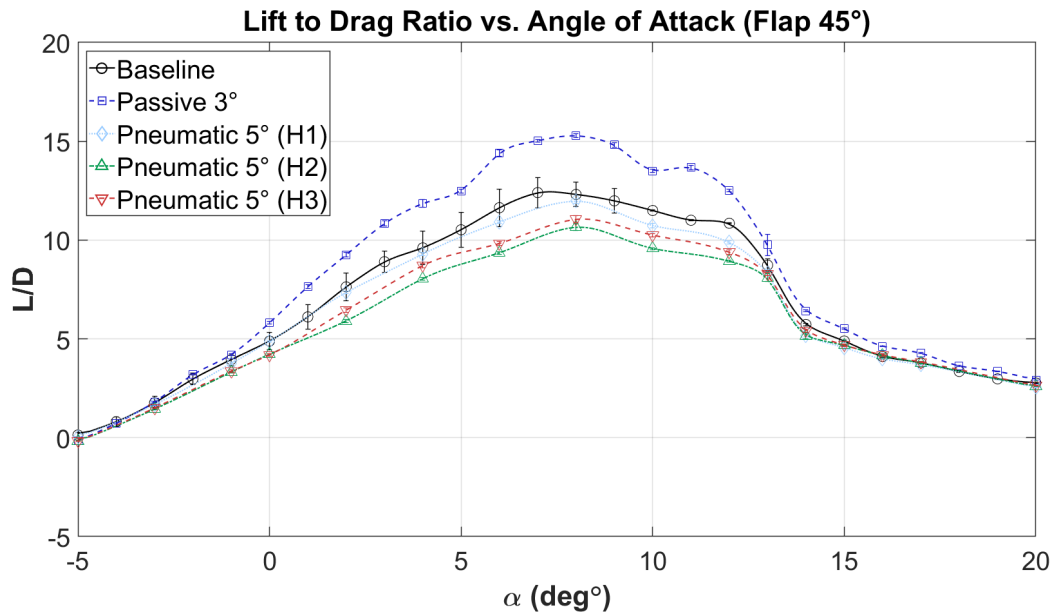


Figure 5.27: Wing L/D vs. α for the optimal passive and active strake configurations with the baseline at $\delta_f = 45^\circ$.

Within the low- α regime ($-5^\circ \leq \alpha \leq 0^\circ$), the aerodynamic efficiencies of all configurations were relatively similar. Although the 5° active strake generally produced slightly lower values of L/D than the baseline configuration, the differences remained relatively small throughout much of this operating range. These reductions in aerodynamic efficiency resulted primarily from the increase in C_D discussed in Section 5.7.2 because only minor differences in C_L were observed in Section 5.7.1. Among the active strake configurations, H1

generally produced the highest aerodynamic efficiency, followed closely by H3 and then H2.

Proceeding into the moderate- α regime ($0^\circ < \alpha \leq 10^\circ$), the differences in aerodynamic efficiency became considerably more pronounced. Throughout much of this operating range, the 3° passive strake consistently produced the highest values of L/D . Specifically, the 3° passive strake achieved $(L/D)_{\max} \approx 15.28$ at $\alpha = 8^\circ$, representing an improvement of approximately 22.8% relative to the baseline configuration, which achieved $(L/D)_{\max} \approx 12.45$ at $\alpha = 7^\circ$. H1, H2, and H3 achieved $(L/D)_{\max} \approx 11.98$, 10.67, and 11.06 all at $\alpha = 8^\circ$ respectively. Relative to the baseline configuration, these values represent reductions in maximum aerodynamic efficiency of approximately 3.8%, 11.2%, and 14.3%, respectively. These results indicate that the relatively small changes in C_L produced by pneumatic blowing were insufficient to overcome the substantially larger increases in C_D observed under landing conditions.

Collectively, the C_L , C_D , and drag polar results explain the measured changes in aerodynamic efficiency. Although pneumatic blowing produced only modest changes in C_L , Section 5.7.2 demonstrated that all three active strake configurations generally maintained larger values of C_D than both the baseline and 3° passive strake throughout much of the operating envelope. Furthermore the 5° active strake generally produced larger values of C_D while generating comparable values of C_L . Consequently, the reductions in aerodynamic efficiency resulted predominantly from increased aerodynamic drag rather than inadequate lift generation.

Within the high- α regime ($\alpha > 10^\circ$), L/D decreased rapidly for all configurations as aerodynamic drag increased when approaching $\alpha_{\text{stall}} = 13^\circ$. Although the differences among the active injection locations gradually diminished with increasing α , the 3° passive strake continued to maintain the highest aerodynamic efficiency throughout much of this operating range. Among the active strake configurations, H1 consistently produced the highest values of L/D , while H2 generally produced the lowest values of L/D .

Overall, the 5° active pneumatic strake investigation demonstrated that pneumatic blow-

ing did not improve aerodynamic performance under landing conditions. Although only modest changes in C_L were observed, the accompanying increases in C_D resulted in lower values of L/D throughout much of the operating envelope. The 3° passive strake remained the highest-performing configuration, while H1 consistently produced the greatest aerodynamic efficiency among the active strake configurations. Nevertheless, the maximum L/D achieved by H1 remained approximately 21.6% below that of the 3° passive strake and approximately 3.8% below that of the baseline configuration. These findings demonstrate that the aerodynamic penalties associated with increased drag outweighed the relatively small changes in lift, preventing the active strake from improving aerodynamic performance under landing conditions. A summary of the principal aerodynamic performance metrics of the 3° passive strake and 5° active strake for the landing investigation is presented in Table 5.6.

Table 5.6: Summary of Optimal Passive-Active Strake Landing Aerodynamic Performance

Config.	$C_{L,\max}$	$\alpha@C_{L,\max}$	$C_{D,\min}$	$\alpha@C_{D,\min}$	$(L/D)_{\max}$	$\alpha@(L/D)_{\max}$	$\Delta(L/D)_{\max}$
Baseline	1.362	13°	0.0732	3°	12.45	7°	No Chg.
3° Passive	1.359	13°	0.0580	3°	15.28	8°	+22.8%
5° H1	1.361	13°	0.0761	2°	11.98	8°	-3.8%
5° H2	1.365	13°	0.0882	4°	10.67	8°	-14.3%
5° H3	1.369	13°	0.0825	4°	11.06	8°	-11.2%

Chapter 6

CONCLUSIONS

The primary objective of this investigation was to experimentally evaluate the aerodynamic performance of passive and active nacelle-mounted strakes operating on a representative commercial aircraft wing model. Particular emphasis was placed on understanding how i_{strake} , δ_f , and pneumatic injection influence lift generation, drag characteristics, and overall aerodynamic efficiency. Wind tunnel testing was conducted using δ_f configurations representative of cruise, takeoff, and landing flight conditions. The conclusions drawn from the results presented in Chapter 5 are summarized below.

6.1 Passive Strake Investigation

This investigation examined how geometrically similar passive strakes installed at the nacelle-wing junction influenced aerodynamic performance under cruise, takeoff, and landing conditions. The results demonstrated that relatively simple geometric modifications to the nacelle-wing junction can produce measurable improvements in aerodynamic performance; however, the magnitude of those improvements depends strongly upon the operating condition. The primary aerodynamic benefit provided by the passive strakes resulted from reductions in C_D rather than substantial increases in C_L . Consequently, changes in aerodynamic efficiency were governed principally by differences in C_D , while changes in C_L remained comparatively small.

Under cruise conditions ($\delta_f = 0^\circ$), passive strakes produced their greatest aerodynamic benefit. Only relatively small variations in C_L were observed among the passive configurations; however, several passive strakes substantially reduced C_D throughout portions of the operating envelope. The optimum 10° passive strake produced the greatest overall improvement, increasing the $(L/D)_{\text{max}}$ by approximately 36.6% relative to the baseline configuration

while producing only a modest increase in C_L . These results indicate that the streamwise vortices generated by the passive strake modified the nacelle-wing junction flow field, delaying localized flow separation and reducing aerodynamic losses without significantly altering the lift-producing capability of the wing.

The aerodynamic response differed considerably under takeoff conditions ($\delta_f = 20^\circ$). Although passive strakes continued to produce only minor changes in C_L , the reductions in C_D observed during cruise were no longer present. Instead, nearly all passive configurations produced larger values of C_D than the baseline configuration, resulting in lower aerodynamic efficiencies throughout much of the operating envelope. Furthermore, none of the passive configurations exceeded the aerodynamic performance of the baseline configuration. These findings indicate that deployment of the flaps fundamentally altered the interaction between the nacelle-generated vortex system and the wing model boundary layer, thereby substantially reducing the effectiveness of passive vortex control.

Landing conditions ($\delta_f = 45^\circ$) produced yet another distinct aerodynamic response. Similar to the previous flap settings, passive strakes caused only minor variations in C_L ; however, substantial reductions in C_D were again observed. Specifically, the optimum 3° passive strake produced approximately a 20.8% reduction in $C_{D,\min}$ relative to the baseline configuration and increased $(L/D)_{\max}$ by approximately 22.8%. These improvements demonstrate that relatively low incidence passive strakes provide greater aerodynamic benefit under the highly separated flow conditions associated with landing than the larger incidence strakes that proved optimal during cruise. Consequently, the optimum passive strake geometry was shown to depend strongly upon δ_f and the associated flow environment.

Passive strakes had little measurable influence on either $C_{L,\max}$ or α_{stall} . The α values corresponding to $C_{L,\max}$ for each δ_f configuration remained unchanged among all passive and active strake configurations, suggesting that passive vortex generation primarily modified the local flow near the nacelle-wing junction without significantly altering the large-scale separation mechanisms governing stall. Instead, the principal aerodynamic benefit of passive

strakes resulted from reducing aerodynamic drag over portions of the operating envelope where localized junction flow separation contributed significantly to total aircraft drag.

Overall, this investigation demonstrated that nacelle-mounted passive strakes provide a simple and effective means of improving aerodynamic performance. However, the aerodynamic effectiveness of a passive strake depends strongly upon both its geometry and the surrounding flow environment. No single passive strake configuration provided the greatest aerodynamic benefit across the complete flight envelope investigated. Consequently, PFC devices should be designed specifically for the operating conditions in which they are intended to operate.

6.2 *Active Strake Investigation*

In contrast to the passive strake investigation, the active pneumatic strake investigation demonstrated that the aerodynamic effectiveness of pneumatic blowing depended strongly upon both the operating condition and the pneumatic injection location. While the passive strakes generally produced predictable aerodynamic responses, active blowing generated distinctly different behavior under cruise, takeoff, and landing conditions. These results indicate that AFC is considerably more sensitive to the local flow environment than passive vortex generation and that its effectiveness depends upon matching the injected momentum to the surrounding aerodynamic conditions.

Under cruise conditions ($\delta_f = 0^\circ$), the 18° active strake produced its greatest aerodynamic benefit. Although all three active injection locations generally reduced C_L relative to both the baseline and the optimal 10° passive strake, they also produced substantially larger reductions in C_D . The decrease in aerodynamic drag more than compensated for the modest reduction in lift, resulting in larger values of L/D throughout much of the operating envelope. Among the active strake configurations investigated, H3 consistently produced the greatest aerodynamic efficiency, increasing $(L/D)_{\max}$ by approximately 42.9% relative to the baseline configuration and by approximately 4.7% relative to the optimal passive configu-

ration. These findings demonstrate that, under cruise conditions, pneumatic blowing was capable of providing aerodynamic improvements beyond those achieved through PFC.

The aerodynamic response changed substantially under takeoff conditions ($\delta_f = 20^\circ$). Although pneumatic blowing by the 15° active strake produced modest increases in C_L , particularly for the H1 injection location, these improvements were accompanied by significantly larger increases in C_D . Consequently, all actively blown configurations produced lower values of L/D than the baseline configuration throughout much of the operating envelope. Among the active strake configurations, H1 consistently produced the greatest aerodynamic efficiency; however, even the 15° active strake remained inferior to the baseline because the drag penalties outweighed the modest improvements in lift. These results demonstrate that pneumatic blowing was unable to improve aerodynamic performance under takeoff conditions.

The overall aerodynamic trends observed during takeoff conditions continued under landing conditions ($\delta_f = 45^\circ$). Pneumatic blowing by the 5° active strake produced only modest changes in C_L , while all three active strake configurations generally exhibited larger values of C_D than both the baseline and the optimal 3° passive strake. Consequently, the aerodynamic efficiencies of the actively blown configurations remained below those of both comparison cases throughout much of the operating envelope. Among the active strake configurations, H1 consistently produced the greatest aerodynamic efficiency; however, the optimal 3° passive strake remained the highest-performing configuration under landing conditions. These findings further demonstrate that the effectiveness of AFC diminished as the flow environment became increasingly dominated by flap deployment and large-scale flow separation.

A significant finding of this investigation is that no single pneumatic injection location provided the greatest aerodynamic benefit throughout the entire flight envelope. Under cruise conditions, H3 consistently produced the greatest aerodynamic improvement, whereas H1 proved superior during both takeoff and landing. These results indicate that relatively

small changes in the location of momentum injection can significantly alter the local flow field surrounding the nacelle-wing junction. Consequently, successful implementation of AFC depends not only upon providing sufficient momentum to modify the local flow field but also upon carefully selecting the injection location relative to the dominant aerodynamic flow structures.

Overall, this investigation demonstrated that pneumatic blowing is capable of producing substantial aerodynamic improvements under appropriate operating conditions but does not provide universal benefits throughout the flight envelope. Unlike the passive strake investigation, where the aerodynamic response remained comparatively predictable, the effectiveness of active blowing depended strongly upon δ_f , local flow structure, and pneumatic injection location. These findings indicate that successful implementation of AFC requires careful integration of the blowing strategy with the surrounding aerodynamic environment rather than the application of a single configuration for all operating conditions.

6.3 Aerodynamic Mechanisms

In order to better understand the aerodynamic behavior observed throughout this investigation, both the passive and active strakes were evaluated based on their influence on the complex flow field surrounding the nacelle-wing junction. Although the aerodynamic responses of both PFC and AFC devices varied with δ_f , the experimental results consistently demonstrated that changes in aerodynamic performance were governed primarily by changes in C_D rather than substantial changes in C_L . Consequently, the principal mechanism responsible for improvements in aerodynamic efficiency was drag reduction rather than lift enhancement.

Passive strakes generated streamwise vortices that modified the local boundary layer developing around the nacelle-wing junction. Depending upon the operating condition, these vortices introduced higher-momentum fluid into the near-wall region of the boundary layer, thereby increasing its resistance to adverse pressure gradients. As a result, localized flow

separation near the nacelle-wing junction was delayed, reducing C_D while producing only relatively small changes in C_L . Furthermore, because the passive strakes primarily modified the local flow structure rather than the overall wing loading, neither $C_{L,\max}$ nor the onset of aerodynamic stall changed appreciably for any of the passive configurations investigated.

Unlike passive vortex generation, active pneumatic blowing introduced additional momentum directly into the local flow field. Consequently, the aerodynamic response became considerably more dependent upon the interaction between the injected jet and the surrounding flow structure. Under cruise conditions, pneumatic blowing substantially reduced C_D while producing only modest reductions in C_L . These results indicate that momentum injection beneficially modified the flow at the nacelle-wing junction by reducing localized aerodynamic losses to a greater extent than could be achieved using passive vortex generation alone. However, as the δ_f increased, the local flow environment changed considerably, reducing the effectiveness of momentum injection. Under both takeoff and landing conditions, the increases in C_D produced by pneumatic blowing outweighed the relatively small changes observed in C_L , resulting in lower values of L/D throughout much of the operating envelope.

One of the most significant findings of this investigation was the strong dependence of aerodynamic performance on pneumatic injection location. The observation that H3 produced the greatest aerodynamic improvement under cruise conditions, whereas H1 consistently outperformed the other injection locations during both takeoff and landing, demonstrates that relatively small changes in momentum injection location can substantially alter the resulting aerodynamic response. These findings suggest that the interaction between the injected jet and the naturally occurring flow structures at the nacelle-wing junction governs the overall effectiveness of AFC. Consequently, successful implementation of AFC depends not only upon the amount of momentum added to the flow but also upon careful selection of the injection location relative to the dominant aerodynamic flow structures.

The experimental results also demonstrated that flap deployment fundamentally altered

the aerodynamic environment surrounding the nacelle-wing junction. As δ_f increased, the optimum passive strake geometry shifted from 10° during cruise to 3° during landing, while the effectiveness of active pneumatic blowing deteriorated substantially. These observations indicate that changes in pressure gradients, boundary layer development, and three-dimensional flow separation strongly influenced the ability of both passive vortex generation and active momentum injection to modify the surrounding flow field. Consequently, the aerodynamic mechanisms governing flow control cannot be considered independent of aircraft configuration but instead must be evaluated within the context of the intended operating condition.

Overall, the experimental evidence demonstrates that successful implementation of either passive or active nacelle-mounted strakes depends upon achieving a favorable interaction with the local aerodynamic environment. Passive vortex generation provided robust drag reductions under favorable operating conditions while producing little influence on lift or stall behavior. In contrast, active pneumatic blowing demonstrated greater peak aerodynamic capability during cruise but substantially greater sensitivity to operating condition and injection location. These findings suggest that future flow control concepts should focus not only on increasing momentum addition but also on optimizing the interaction between the flow control device and the naturally occurring flow structures present at the nacelle-wing junction.

6.4 Engineering Implications

Building upon the experimental platform originally developed by Spitzley et al., the present investigation expanded both the passive and active nacelle-mounted strake investigations by incorporating additional passive strake geometries, interchangeable flap assemblies representing representative cruise, takeoff, and landing configurations, and a simplified active testing methodology that independently evaluated each pneumatic injection location. These additions enabled the aerodynamic performance of nacelle-mounted strakes to be evaluated over a broader range of aircraft operating conditions than previously investigated.

The results of this investigation demonstrate that nacelle-mounted strakes have the potential to produce meaningful improvements in aerodynamic performance when their design is properly matched to the surrounding flow environment. However, the experimental results also demonstrate that no single passive or active strake configuration provided the greatest aerodynamic benefit throughout the complete flight envelope. Consequently, the successful implementation of nacelle-mounted strakes requires careful consideration of the operating condition for which they are intended rather than the application of a single universal design.

The passive strake investigation demonstrated that relatively simple geometric modifications can produce substantial reductions in aerodynamic drag while requiring no additional energy input or mechanical complexity. Because passive strakes are lightweight, inexpensive, and inherently reliable, they represent an attractive means of improving aerodynamic efficiency when the operating condition is well defined. However, the optimum passive strake geometry varied considerably with δ_f , indicating that even PFC devices should be designed specifically for the aerodynamic environment in which they are expected to operate.

The active pneumatic strake investigation demonstrated both the advantages and limitations of AFC. Under cruise conditions, pneumatic blowing produced aerodynamic efficiencies that exceeded those of the best passive configuration, illustrating the potential for AFC to outperform passive devices when the injected momentum interacts favorably with the local flow field. Conversely, under both takeoff and landing conditions, pneumatic blowing generally increased aerodynamic drag and reduced overall aerodynamic efficiency. These findings indicate that AFC systems are considerably more sensitive to operating condition than PFC systems and therefore require substantially greater optimization before they can be implemented successfully across a broad flight envelope.

One particularly important finding of this investigation is that the effectiveness of AFC depended not only upon the amount of momentum injected into the flow but also upon the location of momentum injection. The observation that different pneumatic injection locations

produced the greatest aerodynamic benefit under different flight conditions demonstrates that relatively small geometric changes can significantly alter the resulting aerodynamic response. This finding suggests that future AFC systems may benefit from adaptive or reconfigurable blowing strategies capable of modifying momentum addition in response to changing flight conditions rather than relying upon a single fixed injection configuration.

Collectively, the passive and active investigations demonstrate that aerodynamic performance at the nacelle-wing junction is governed by a complex interaction between local flow structures, pressure gradients, and momentum exchange. Consequently, future flow control concepts should focus not only on increasing momentum addition or vortex strength but also on optimizing the interaction between the flow control device and the naturally occurring flow structures present at the nacelle-wing junction. Such an approach offers the greatest potential for improving aerodynamic efficiency while minimizing adverse drag penalties across a wider range of operating conditions.

Overall, this investigation demonstrates that both passive and active nacelle-mounted strakes have practical applications for improving aircraft aerodynamic performance. Passive strakes offer a simple, robust, and energy-efficient method of reducing aerodynamic drag under favorable operating conditions, whereas active pneumatic strakes provide greater peak aerodynamic performance but requires considerably greater optimization to achieve consistent performance. These findings provide practical guidance for the future design of nacelle-mounted strakes and demonstrate that successful aerodynamic optimization requires tailoring the flow control strategy to the specific operating environment rather than attempting to employ a single configuration throughout the entire flight envelope.

6.5 Limitations and Final Remarks

Although this investigation provides valuable insight into the aerodynamic behavior of passive and active nacelle-mounted strakes, several limitations should be considered when interpreting the experimental results.

First, all experiments were conducted at a single U_∞ and Re . Consequently, the aerodynamic characteristics reported herein represent only one operating condition and may not be directly applicable to aircraft operating over a broader range of flight speeds or Re . Because both passive vortex generation and active momentum injection are influenced by the surrounding flow environment, additional testing over a wider range of Re and M_∞ would provide a more comprehensive understanding of their aerodynamic behavior.

Second, the active strake investigation was performed using only a single pneumatic supply pressure of 20 psi. Unlike the previous investigation by Spitzley et al., which examined multiple pneumatic blowing pressures and combinations of active injection locations, the present investigation intentionally employed a constant supply pressure while independently evaluating each pneumatic injection location. Although this simplified testing methodology enabled the aerodynamic influence of individual blowing locations to be isolated, it represents only one momentum input condition. Previous research has shown that the effectiveness of active strakes depends strongly upon C_μ , suggesting that different blowing pressures or momentum values may produce substantially different aerodynamic responses. Consequently, the present investigation cannot identify the optimum blowing intensity for each operating condition.

Third, the active pneumatic strake investigation examined only three discrete pneumatic injection locations (H1, H2, and H3), with only one location activated during each test condition. Although these locations demonstrated that injection placement significantly influences aerodynamic performance, they represent only a small portion of the possible design space. Additional injection locations, different jet orientations, multiple simultaneous injection locations, or alternative blowing strategies may produce improved aerodynamic performance.

Another limitation of this investigation is that the aerodynamic mechanisms responsible for the measured changes in performance were inferred from aerodynamic force measurements rather than being observed directly. While the measured variations in C_L , C_D , and L/D

provide strong evidence of changes occurring within the nacelle-wing flow field, and the qualitative tuft flow visualization presented in Section 5.8 provided additional supporting evidence of localized flow separation, no quantitative flow field measurements such as PIV, surface pressure measurements, or hot wire anemometry measurements were performed. Consequently, the proposed aerodynamic mechanisms should be interpreted as physically supported explanations based upon the experimental data rather than direct observations of the flow field.

Finally, the present investigation focused exclusively on steady state aerodynamic performance using a semi-span wind tunnel wing model. Dynamic effects, unsteady flow phenomena, aeroelastic deformation, and full-scale aircraft installation effects were beyond the scope of this study. While these factors may influence the aerodynamic behavior of PFC and AFC devices in practical aircraft applications, the present investigation provides a controlled experimental foundation upon which future investigations may build.

Despite these limitations, the experimental program successfully identified the primary aerodynamic trends associated with both passive and active nacelle-mounted strakes under representative cruise, takeoff, and landing conditions. The consistency of the measured aerodynamic behavior across the three δ_f configurations provides confidence in the principal conclusions of this investigation while also identifying several opportunities for continued research and refinement of nacelle-mounted strake concepts.

Overall, this investigation has demonstrated that both passive and active nacelle-mounted strakes are capable of modifying the aerodynamic characteristics of the nacelle-wing junction; however, their effectiveness depends strongly upon the surrounding flow environment. Passive strakes provided robust aerodynamic improvements primarily through reductions in C_D , whereas active strakes demonstrated greater peak aerodynamic capability but considerably greater sensitivity to operating condition and pneumatic injection location. These findings contribute to a better understanding of nacelle-mounted strakes and provide guidance for the future development of passive and active strake concepts for commercial aircraft.

Chapter 7

FUTURE WORK

The results of the present investigation demonstrated that both passive and active nacelle-mounted strakes are capable of modifying the aerodynamic interaction at the nacelle-wing junction and improving aerodynamic performance under selected operating conditions. While the experimental program identified several meaningful aerodynamic trends, numerous opportunities remain for expanding upon the findings of this investigation. The following recommendations are provided for future research.

7.1 Blowing Coefficient Optimization

One of the most significant findings of the active pneumatic strake investigation was the strong sensitivity of aerodynamic performance to momentum injection. Although several actively blown configurations produced substantial improvements in L/D during cruise conditions, many configurations also generated increased aerodynamic drag and reduced aerodynamic efficiency under takeoff and landing conditions.

The results of this investigation suggest that the 20 psi blowing pressure employed during testing may have produced C_μ values that were larger than optimal for the relatively low q_∞ utilized in the wind tunnel. Future investigations should therefore evaluate a broader range of blowing pressures and C_μ values to determine the optimum actuation level for each operating condition. Testing should include lower blowing pressures than those investigated herein to determine whether reduced jet momentum can produce favorable vortex behavior while minimizing excessive mixing losses and aerodynamic drag.

Such investigations would improve understanding of the relationship between C_μ and aerodynamic performance while identifying optimum blowing conditions for cruise, takeoff, and landing operations.

7.2 *Strake Geometry and Injection Optimization*

The present investigation demonstrated that both passive strake geometry and pneumatic injection location strongly influenced aerodynamic performance. The optimum passive strake varied from 10° during cruise to 3° during landing, while H3 produced the greatest AFC benefits during cruise whereas H1 consistently performed best during takeoff and landing.

Future investigations should examine a broader design space, including:

- Additional passive i_{strake} Configurations,
- Different strake heights,
- Alternative strake lengths,
- Modified leading edge geometries,
- Additional pneumatic injection locations,
- Different hole diameters,
- Different jet orientations,
- Multiple simultaneously activated injection locations, and
- Variations in overall strake placement relative to the nacelle-wing junction.

Such investigations would provide a more complete understanding of how vortex strength, vortex trajectory, and momentum injection interact with the naturally occurring flow structures surrounding the nacelle-wing junction.

7.3 *Experimental Flow Measurements and CFD Validation*

The aerodynamic mechanisms proposed throughout this investigation were inferred from aerodynamic force measurements. Future investigations should directly measure the flow field surrounding the nacelle-wing junction through techniques such as:

- Particle Image Velocimetry (PIV),
- Oil flow visualization,
- Smoke visualization,
- Pressure-sensitive paint,
- Surface pressure measurements, and
- Hot-wire anemometry.

In addition, validated Computational Fluid Dynamics (CFD) simulations should be developed using the experimental data obtained during this investigation. Once validated, CFD could be used to visualize vortex trajectories, quantify boundary layer development, identify separation locations, and evaluate additional passive and active strake configurations at significantly lower cost than experimental testing alone.

The combination of experimental flow field measurements and CFD would substantially improve understanding of the physical mechanisms responsible for the aerodynamic behavior observed in the present study.

7.4 Flight Condition and Scaling Effects

The experiments conducted during this investigation were performed at $Re_{\bar{c}} \approx 1.93 \times 10^5$ and q_{∞} of 2 psf. Although these conditions provided a consistent basis for comparison among configurations, they represent only one portion of the operational flight envelope.

Future investigations should evaluate passive and active strake performance over broader ranges of Re , q_{∞} , U_{∞} , M_{∞} , and various other δ_f configurations. Such studies would determine the scalability of the present results while improving understanding of how operating condition influences vortex behavior and AFC effectiveness.

7.5 Adaptive Active Flow Control

Perhaps the most important conclusion of this investigation is that no single passive or active strake configuration provided optimum aerodynamic performance throughout the entire flight envelope. Configurations that performed exceptionally well during cruise often performed poorly during takeoff and landing.

This observation suggests that future AFC systems should employ adaptive control strategies capable of modifying blowing conditions in response to changing flight conditions. Future research should investigate:

- Variable blowing pressure,
- Independently controlled injection locations,
- Pulsed Blowing (PB),
- Closed-loop control systems,
- Sensor-based flow control algorithms, and
- Real-time optimization of momentum injection.

Such systems may allow active pneumatic strakes to dynamically optimize aerodynamic performance throughout the flight envelope rather than relying upon a single fixed blowing configuration.

7.6 Aircraft Integration

While the present investigation demonstrated the aerodynamic feasibility of passive and active nacelle-mounted strakes using a semi-span wing model, additional work is required before such technologies can be implemented on full-scale commercial aircraft.

Future investigations should therefore examine:

- Aircraft performance benefits,
- Fuel burn reductions,
- Bleed-air requirements,
- System weight,
- Maintenance considerations,
- Reliability,
- Certification requirements, and
- Life-cycle operating costs.

Ultimately, these investigations would determine whether the aerodynamic improvements demonstrated in the present investigation justify implementation on future commercial aircraft.

Overall, the present investigation demonstrates that both passive and active nacelle-mounted strakes possess significant potential for improving aerodynamic performance through favorable modification of the nacelle-wing flow field. Continued experimental, computational, and full-scale investigations will further improve understanding of the aerodynamic mechanisms governing vortex-induced flow control and facilitate the development of more efficient nacelle-mounted strakes for future commercial aircraft.

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APPENDIX A

MATLAB CODE

A.1 Data Processing

The following MATLAB program was developed specifically for this investigation. This program was used to process the raw wind tunnel measurements, apply tare corrections, calculate the C_L , C_D , and L/D values, perform uncertainty and statistical analyses, identify optimum configurations, and generate the figures and summary tables presented throughout Chapters 4 and 5. The source code is provided to document the data reduction methodology and to facilitate reproducibility of the experimental results.

```

1 clear; clc; close all;
2
3 %% =====
4 % USER SETTINGS
5 % =====
6 rawDataFolder = 'raw_data'; % change to 'raw data' only if
   that is your
7 %real folder
8 metadataFile = 'metadata.csv';
9 outputFile = 'processed_results_rotated.csv';
10
11 % Reference area
12 S_ref_in2 = 258.8505;
13 S_ref_ft2 = S_ref_in2 / 144;
14
15 %% =====
16 % LOAD METADATA USING TEXTSCAN (MATLAB 2017b SAFE)
17 %
18 % Expected metadata columns:
19 % filename,test_type,flap_deg,strake_deg,blowing,aoa_list,
   rows_per_aoa,tare
20 %_filename,tare_rows_per_aoa
21 % =====
22 fid = fopen(metadataFile,'r');
23 if fid == -1
24     error('Could not open metadata file: %s', metadataFile);
25 end
26
27 C = textscan(fid, '%s%s%f%f%f%s%f%s%f', ...
28     'Delimiter', ',', ...

```

```

29     'HeaderLines', 1);
30
31 fclose(fid);
32
33 meta_filename      = C{1};
34 meta_test_type     = C{2};
35 meta_flap_deg      = C{3};
36 meta_strake_deg    = C{4};
37 meta_blowing       = C{5};
38 meta_aoa_list      = C{6};
39 meta_rows_per_aoa  = C{7};
40 meta_tare_filename = C{8};
41 meta_tare_rows_per_aoa = C{9};
42
43 nFiles = length(meta_filename);
44 fprintf('Loaded metadata for %d files.\n', nFiles);
45
46 results = table();
47
48 %% =====
49 % PROCESS EACH FILE
50 % =====
51 for f = 1:nFiles
52
53     filename = strtrim(meta_filename{f});
54     filename = strrep(filename, ' ', '');
55     filename = strrep(filename, sprintf('\r'), '');
56     filename = strrep(filename, sprintf('\n'), '');
57
58     filePath = fullfile(rawDataFolder, filename);
59
60     fprintf('\nProcessing %s\n', filename);
61
62     if exist(filePath, 'file') ~= 2
63         warning('Missing file: %s', filePath);
64         continue;
65     end
66
67     %% READ RAW DATA FILE
68     T = readtable(filePath);
69
70     if height(T) < 2
71         warning('File %s has fewer than 2 rows. Skipping.',
72             filename);
72         continue;

```



```

73     end
74
75     % Remove first exported tare/zero row
76     T = T(2:end,:);
77
78     % These names assume MATLAB 2017b renamed:
79     % DYNAMIC PRESSURE -> DYNAMICPRESSURE
80     % -Drag             -> x_Drag
81     % -Side             -> x_Side
82     q_all               = T.DYNAMICPRESSURE;
83     Drag_meas_all      = -T.x_Drag;
84     Lift_meas_all      = -T.x_Side;
85
86     %% READ METADATA FOR THIS FILE
87     aoa_text = strtrim(meta_aoa_list{f});
88     aoa_text = strrep(boa_text, ' ', '');
89     aoa_text = strrep(boa_text, sprintf('\r'), '');
90     aoa_text = strrep(boa_text, sprintf('\n'), '');
91     aoa_values = str2num(boa_text); %#ok<ST2NM>
92
93     rows_per_aoa = meta_rows_per_aoa(f);
94
95     if isempty(boa_values)
96         warning('AoA list could not be parsed for file %s.
97             Skipping.', ...
98             filename);
99         continue;
100     end
101
102     if isempty(rows_per_aoa) || rows_per_aoa <= 0
103         warning('Invalid rows_per_aoa for file %s. Skipping.',
104             filename);
105         continue;
106     end
107
108     %% OPTIONAL: LOAD AoA-DEPENDENT TARE FOR BLOWING CASES
109     use_tare_subtraction = false;
110     tare_rows_per_aoa = 0;
111     tare_filename = '';
112
113     if meta_blowing(f) == 1
114         tare_filename = strtrim(meta_tare_filename{f});
115         tare_filename = strrep(tare_filename, ' ', '');
116         tare_filename = strrep(tare_filename, sprintf('\r'), ''
117             );

```

```

115     tare_filename = strrep(tare_filename, sprintf('\n'), ''
116         );
117     tare_rows_per_aoa = meta_tare_rows_per_aoa(f);
118
119     if isempty(tare_filename)
120         warning('Blowing case %s has no tare_filename
121             listed. Proceeding without tare subtraction.',
122                 filename);
123
124     elseif isempty(tare_rows_per_aoa) || tare_rows_per_aoa
125         <= 0
126         warning('Blowing case %s has invalid
127             tare_rows_per_aoa. Proceeding without tare
128             subtraction.', filename);
129
130     else
131         tarePath = fullfile(rawDataFolder, tare_filename);
132
133         if exist(tarePath, 'file') ~= 2
134             warning('Tare file not found for %s: %s.
135                 Proceeding without tare subtraction.',
136                     filename, tarePath);
137
138         else
139             T_tare = readtable(tarePath);
140
141             if height(T_tare) < 2
142                 warning('Tare file %s is too short.
143                     Proceeding without tare subtraction.',
144                         tare_filename);
145             else
146                 T_tare = T_tare(2:end,:);
147
148                 q_tare_all = T_tare.DYNAMICPRESSURE;
149                 Drag_tare_all = -T_tare.x_Drag;
150                 Lift_tare_all = -T_tare.x_Side;
151
152                 use_tare_subtraction = true;
153             end
154         end
155     end
156 end
157
158 %% CHECK NUMBER OF AVAILABLE BLOCKS

```

```

150     n_expected_rows = length(aoa_values) * rows_per_aoa;
151     n_available_rows = height(T);
152
153     if n_available_rows < rows_per_aoa
154         warning('Not enough rows in %s to form even one AoA
155             block. Skipping.', filename);
156         continue;
157     end
158
159     if n_available_rows < n_expected_rows
160         warning('%s has fewer rows (%d) than expected (%d).
161             Truncating AoA list.', ...
162             filename, n_available_rows, n_expected_rows);
163         n_blocks = floor(n_available_rows / rows_per_aoa);
164         aoa_values = aoa_values(1:n_blocks);
165     else
166         n_blocks = length(aoa_values);
167     end
168
169     %% CHECK TARE BLOCKS IF USING TARE
170     if use_tare_subtraction
171         n_tare_blocks = floor(length(q_tare_all) /
172             tare_rows_per_aoa);
173
174         if n_tare_blocks < n_blocks
175             warning('Tare file %s has fewer AoA blocks than
176                 data file %s. Truncating to common block count.'
177                 , ...
178                 tare_filename, filename);
179             n_blocks = n_tare_blocks;
180             aoa_values = aoa_values(1:n_blocks);
181         end
182     end
183
184     %% LOOP THROUGH EACH AoA BLOCK
185     for i = 1:n_blocks
186
187         idx_start = (i-1)*rows_per_aoa + 1;
188         idx_end   = i*rows_per_aoa;
189
190         q_block      = q_all(idx_start:idx_end);
191         Drag_meas_block = Drag_meas_all(idx_start:idx_end);
192         Lift_meas_block = Lift_meas_all(idx_start:idx_end);
193
194         %% APPLY AoA-DEPENDENT TARE SUBTRACTION BEFORE ROTATION

```

```

190     if use_tare_subtraction
191         tare_idx_start = (i-1)*tare_rows_per_aoa + 1;
192         tare_idx_end   = i*tare_rows_per_aoa;
193
194         Drag_tare_block = Drag_tare_all(tare_idx_start:
195                                         tare_idx_end);
196         Lift_tare_block = Lift_tare_all(tare_idx_start:
197                                         tare_idx_end);
198
199         Drag_tare_mean = mean(Drag_tare_block(~isnan(
200                                 Drag_tare_block)));
201         Lift_tare_mean = mean(Lift_tare_block(~isnan(
202                                 Lift_tare_block)));
203
204         Drag_meas_block = Drag_meas_block - Drag_tare_mean;
205         Lift_meas_block = Lift_meas_block - Lift_tare_mean;
206     end
207
208     %% ROTATION ANGLE = AoA FOR THAT BLOCK
209     % Keep this EXACTLY as in your working non-blowing
210     % script
211     aoa_deg = aoa_values(i);
212     theta_deg = aoa_deg;
213
214     R = [cosd(theta_deg), -sind(theta_deg);
215          sind(theta_deg),  cosd(theta_deg)];
216
217     F_meas = [Drag_meas_block.'; Lift_meas_block.'];
218     F_rot  = R * F_meas;
219
220     Drag_rot_block = F_rot(1,:).';
221     Lift_rot_block = F_rot(2,:).';
222
223     %% BASIC STATISTICS
224     q_valid = q_block(~isnan(q_block));
225     Drag_valid = Drag_rot_block(~isnan(Drag_rot_block));
226     Lift_valid = Lift_rot_block(~isnan(Lift_rot_block));
227
228     q_mean = mean(q_valid);
229     q_std  = std(q_valid);
230
231     Drag_mean = mean(Drag_valid);
232     Drag_std  = std(Drag_valid);
233
234     % Keep this EXACTLY as in your working non-blowing

```

```

    script
230 Lift_mean = -(mean(Lift_valid));
231 Lift_std  = std(Lift_valid);
232
233 %% COMPUTE COEFFICIENTS
234 % Keep this EXACTLY as in your working non-blowing
    script
235 CL_series = -(Lift_rot_block ./ (q_block * S_ref_ft2));
236 CD_series = Drag_rot_block ./ (q_block * S_ref_ft2);
237
238 LD_series = nan(size(CL_series));
239 validLD = abs(CD_series) > 1e-12;
240 LD_series(validLD) = CL_series(validLD) ./ CD_series(
    validLD);
241
242 CL_valid = CL_series(~isnan(CL_series) & ~isinf(
    CL_series));
243 CD_valid = CD_series(~isnan(CD_series) & ~isinf(
    CD_series));
244 LD_valid = LD_series(~isnan(LD_series) & ~isinf(
    LD_series));
245
246 if isempty(CL_valid) || isempty(CD_valid)
247     warning('Invalid coefficient data in %s at AoA %.1f
248             . Skipping block.', filename, aoa_deg);
249     continue;
250 end
251
252 CL_mean = mean(CL_valid);
253 CL_std  = std(CL_valid);
254
255 CD_mean = mean(CD_valid);
256 CD_std  = std(CD_valid);
257
258 if isempty(LD_valid)
259     LD_mean = NaN;
260     LD_std  = NaN;
261     LD_ci95 = NaN;
262     nLD = 0;
263 else
264     LD_mean = mean(LD_valid);
265     LD_std  = std(LD_valid);
266     nLD = length(LD_valid);
267     LD_ci95 = 1.96 * LD_std / sqrt(nLD);
268 end

```

```

268
269     nCL = length(CL_valid);
270     nCD = length(CD_valid);
271
272     CL_ci95 = 1.96 * CL_std / sqrt(nCL);
273     CD_ci95 = 1.96 * CD_std / sqrt(nCD);
274
275     fprintf('AoA = %5.1f deg | q = %6.3f psf | Drag = %8.4f
276             lbf | Lift = %8.4f lbf | CL = %7.4f | CD = %7.4f\n'
277             , ...
278             aoa_deg, q_mean, Drag_mean, Lift_mean, CL_mean,
279             CD_mean);
280
281 %% STORE RESULTS
282 newRow = table( ...
283     {filename}, ...
284     {meta_test_type{f}}, ...
285     meta_flap_deg(f), ...
286     meta_strake_deg(f), ...
287     meta_blowing(f), ...
288     aoa_deg, ...
289     rows_per_aoa, ...
290     theta_deg, ...
291     q_mean, q_std, ...
292     Drag_mean, Drag_std, ...
293     Lift_mean, Lift_std, ...
294     CL_mean, CL_std, CL_ci95, ...
295     CD_mean, CD_std, CD_ci95, ...
296     LD_mean, LD_std, LD_ci95, ...
297     'VariableNames', { ...
298         'filename', ...
299         'test_type', ...
300         'flap_deg', ...
301         'strake_deg', ...
302         'blowing', ...
303         'aoa_deg', ...
304         'rows_per_aoa', ...
305         'theta_deg', ...
306         'q_mean_psf', 'q_std_psf', ...
307         'Drag_mean_lbf', 'Drag_std_lbf', ...
308         'Lift_mean_lbf', 'Lift_std_lbf', ...
309         'CL_mean', 'CL_std', 'CL_ci95', ...
310         'CD_mean', 'CD_std', 'CD_ci95', ...
311         'LD_mean', 'LD_std', 'LD_ci95'} ...
312     );

```

```
310
311         results = [results; newRow]; %#ok<AGROW>
312     end
313 end
314
315 %% SORT AND SAVE
316 if ~isempty(results)
317     results = sortrows(results, {'flap_deg','strake_deg','
        blowing','aoa_deg'});
318 end
319
320 writetable(results, outputFile);
321
322 fprintf('\nDone. Saved results to %s\n', outputFile);
```

APPENDIX B

Reference Calculations

B.1 Mean Aerodynamic Wing Chord Calculation

$\bar{c}$ of a wing is defined as the area-weighted average chord length of a finite wing [3] given by

$$\bar{c} = \frac{\int c^2 dS}{\int c dS} \quad (1)$$

For a straight, trapezoidal wing with linear chord variation along the span of the wing, the differential area is

$$dS = c(y) dy \quad (2)$$

Substituting Equation 2 into Equation 1 and integrating from the wing root to the wing tip, $\bar{c}$ becomes

$$\bar{c} = \frac{\int_0^{b/2} c^2(y) dy}{\int_0^{b/2} c(y) dy} \quad (3)$$

The chord distribution for a trapezoidal wing with a taper ratio is defined as

$$c(y) = c_r \left[1 - (1 - \lambda) \frac{2y}{b} \right] \quad (4)$$

where c_r is the wing root chord and $\lambda = c_t/c_r$ is the taper ratio of the wing.

By introducing the normalized spanwise coordinate $\eta = 2y/b$ and substituting into Equation 4, then substituting Equation 4 into Equation 3, $\bar{c}$ becomes

$$\bar{c} = \frac{\int_0^1 c_r^2 [1 - (1 - \lambda)\eta]^2 d\eta}{\int_0^1 c_r [1 - (1 - \lambda)\eta] d\eta} \quad (5)$$

Evaluating the integrals in Equation 5 yields

$$\int_0^1 [1 - (1 - \lambda)\eta]^2 d\eta = \frac{1 + \lambda + \lambda^2}{3} \quad (6)$$

$$\int_0^1 [1 - (1 - \lambda)\eta] d\eta = \frac{1 + \lambda}{2} \quad (7)$$

Substituting Equations 6 and 7 into Equation 5 reduces $\bar{c}$ to

$$\bar{c} = \frac{2}{3} c_r \frac{1 + \lambda + \lambda^2}{1 + \lambda} \quad (8)$$

Which is the standard closed-form expression for $\bar{c}$ of a trapezoidal wing with λ . Equation 8 was used to calculate $Re_{\bar{c}}$ for the wing model.

APPENDIX C

Reference Figures

C.1 Additional Passive and Active Strake Comparison Figures

This appendix contains a collection of aerodynamic performance plots for the optimum passive and active nacelle-mounted strake configurations identified during this investigation. For each δ_f configuration (cruise, takeoff, and landing), the appendix presents direct comparisons of the baseline configuration, the optimum passive strake, and the associated active pneumatic strake at the same i_{strake} . The contents include C_L , C_D , and L/D figures for reference.

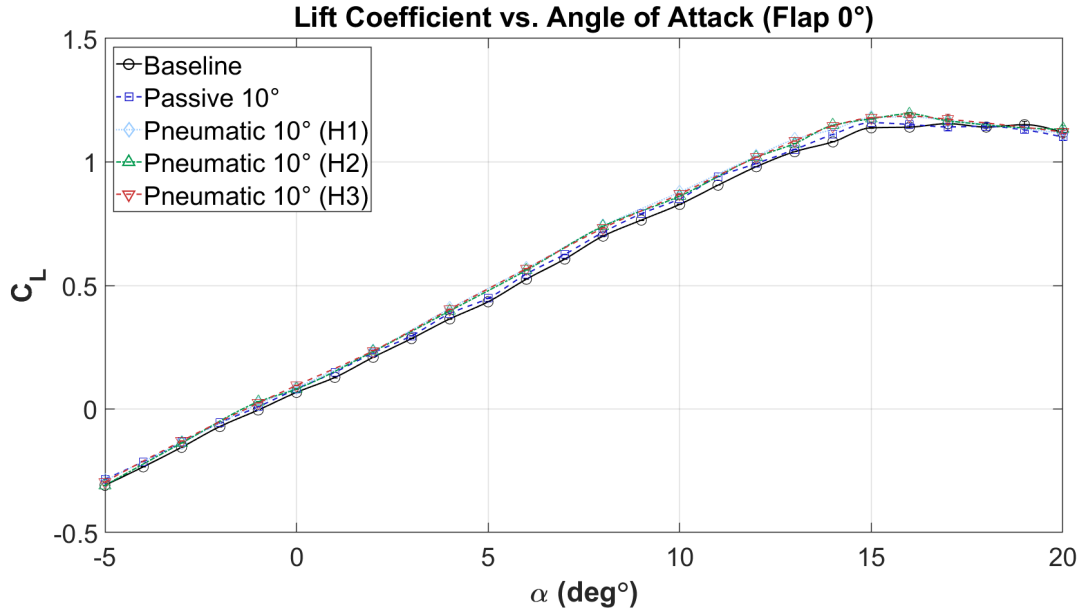


Figure 1: Wing C_L vs. α for the optimal passive strake and active strake at the same i_{strake} with the baseline at $\delta_f = 0^\circ$.

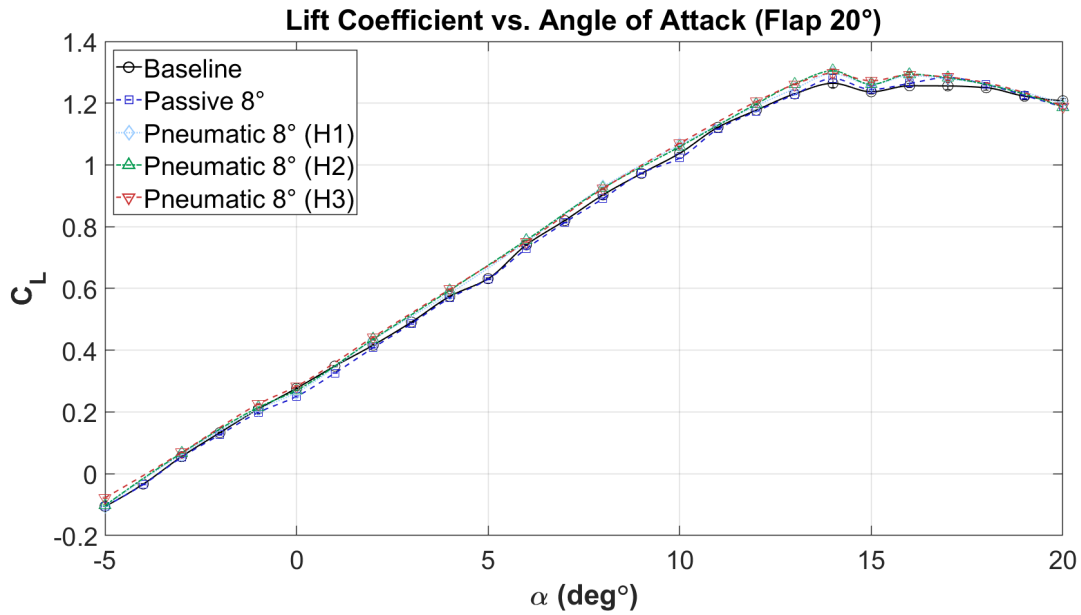


Figure 2: Wing C_L vs. α for the optimal passive stroke and active stroke at the same i_{stroke} with the baseline at $\delta_f = 20^\circ$.

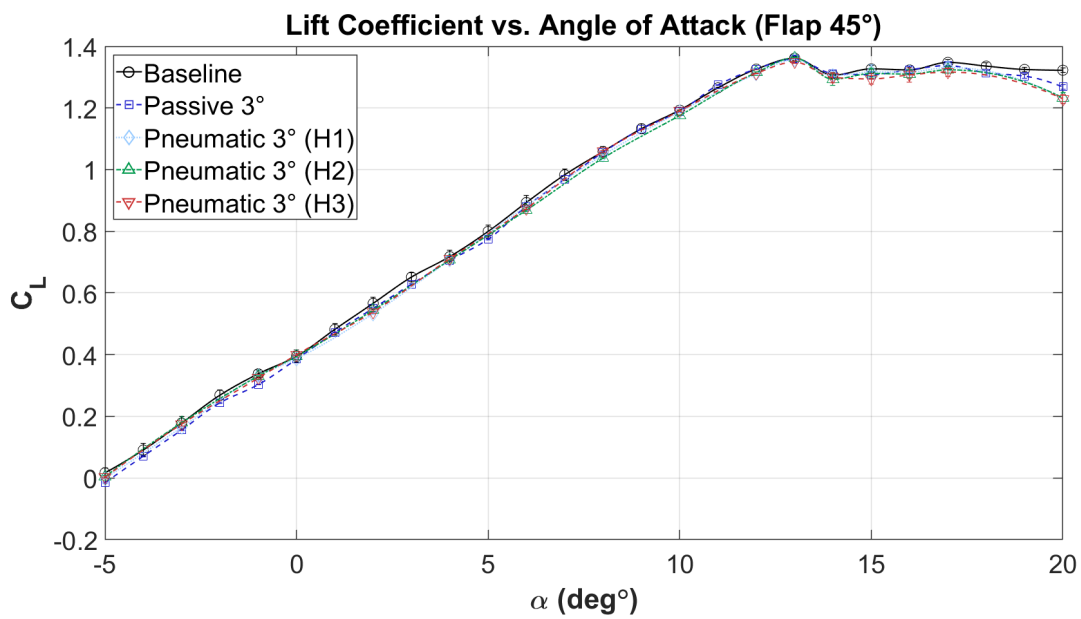


Figure 3: Wing C_L vs. α for the optimal passive stroke and active stroke at the same i_{stroke} with the baseline at $\delta_f = 45^\circ$.

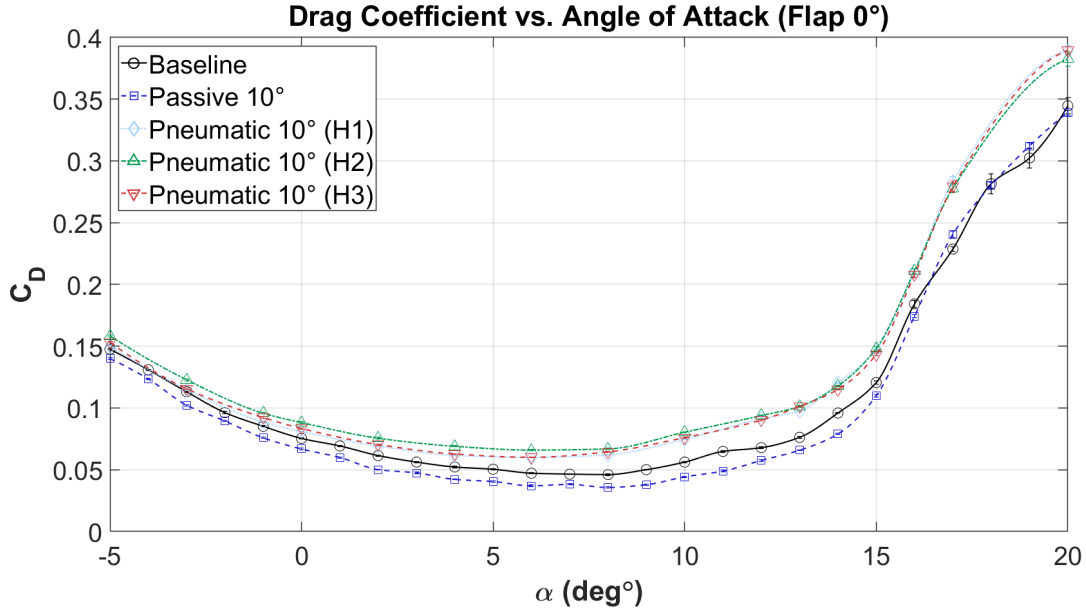


Figure 4: Wing C_D vs. α for the optimal passive stroke and active stroke at the same i_{stroke} with the baseline at $\delta_f = 0^\circ$.

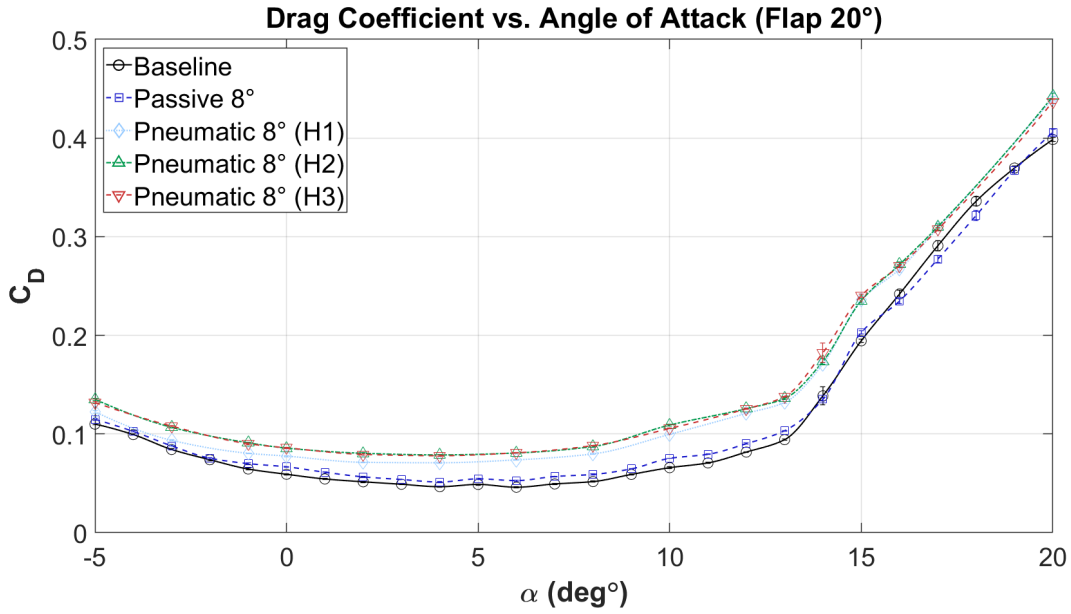


Figure 5: Wing C_D vs. α for the optimal passive stroke and active stroke at the same i_{stroke} with the baseline at $\delta_f = 20^\circ$.

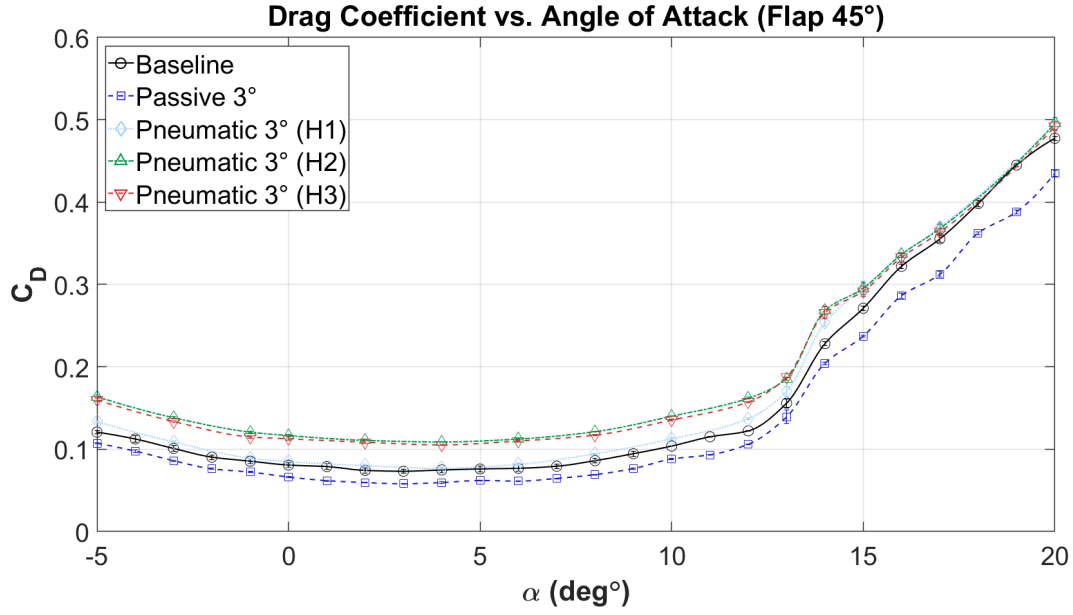


Figure 6: Wing C_D vs. α for the optimal passive stroke and active stroke at the same i_{stroke} with the baseline at $\delta_f = 45^\circ$.

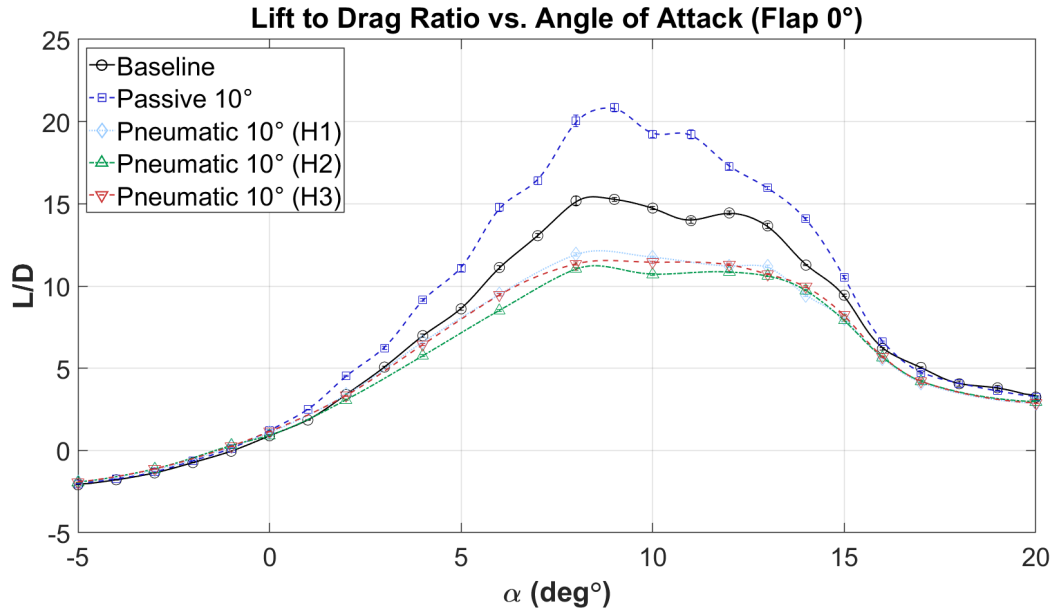


Figure 7: Wing L/D vs. α for the optimal passive stroke and active stroke at the same i_{stroke} with the baseline at $\delta_f = 0^\circ$.

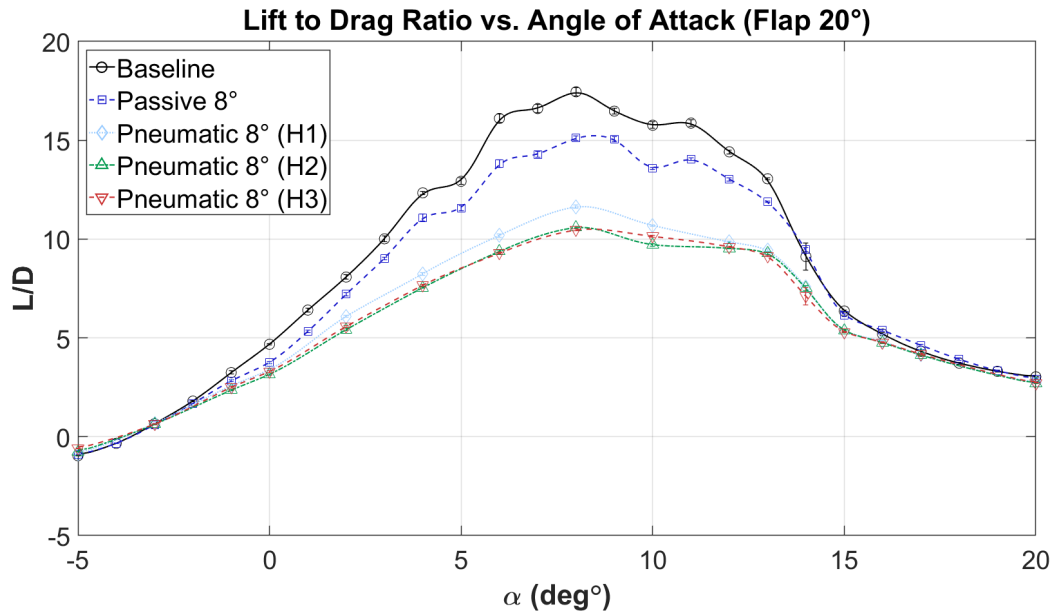


Figure 8: Wing L/D vs. α for the optimal passive stroke and active stroke at the same i_{stroke} with the baseline at $\delta_f = 20^\circ$.

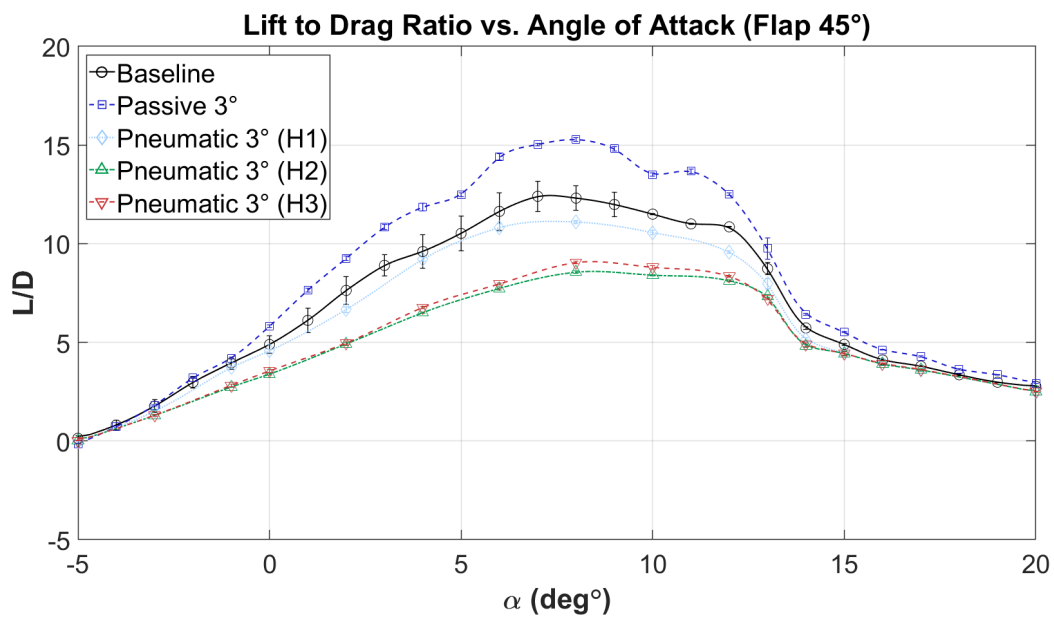


Figure 9: Wing L/D vs. α for the optimal passive stroke and active stroke at the same i_{stroke} with the baseline at $\delta_f = 45^\circ$.