

Transonic Cruise Slotted Wing Design for Commercial Transport Aircraft using CDISC

Brett R. Hiller¹, Richard L. Campbell², and Michelle N. Banchy³

NASA Langley Research Center, Hampton, VA, 23681

A knowledge-based aerodynamic design method, CDISC, has been extended to enable the computational design of transonic cruise slotted wings for commercial transport aircraft. The cruise slotted wing is a multielement wing concept with a forward main element and an aft flap element, separated to form an intermediate slot. This slot favorably redirects airflow from the main element lower surface toward the low-momentum, upper-surface boundary layer of the flap. Relative to the supercritical wing, the cruise slotted wing enables greater aft loading that helps to reduce shock strength and transonic pressure drag. The cruise slotted wing may be considered a passive drag reduction technology with potential fuel-burn savings and increased vehicle range for next-generation aircraft. The current paper seeks to quantify the drag-saving benefits of cruise slotted wing technology in application to single-aisle commercial transport aircraft. A series of multielement design constraints were developed within CDISC for the design of a partial-span, cruise slotted wing for a Mach-0.8 variant of the Common Research Model. To mitigate the skin-friction drag penalty associated with cruise slotted wings, the design features a flap with natural laminar flow over approximately 75% of the flap surface area. Cruise drag estimates from the NASA USM3D-ME flow solver between a supercritical wing and the partial-span, cruise slotted wing design were within one drag count. Near-cruise, off-design analyses showed limited laminar-flow sensitivity to angle of attack and a more gradual drag rise compared to a supercritical wing. Based on this observed benefit, future work is motivated to design a cruise slotted wing with laminar flow on both wing elements to achieve a significant reduction in cruise drag while delaying drag divergence.

Nomenclature

Acronyms

ALE	=	Align Leading Edge
ALMA	=	Aft Laminar Multielement Airfoil
BLSTA3D	=	Boundary Layer code for Stability Analysis 3D
CATNLF	=	Crossflow Attenuated Natural Laminar Flow
CDISC	=	Constrained Direct Iterative Surface Curvature
CF	=	Crossflow
CRM	=	Common Research Model
CRM-M8	=	Mach-0.8 variant of the Common Research Model
CSW	=	Cruise Slotted Wing
LASTRAC	=	Langley Stability and Transition Analysis Code
NF	=	N-Factor
NLF	=	Natural Laminar Flow
SNLF	=	Slotted, Natural Laminar Flow
TS	=	Tollmien-Schlichting
USM3D-ME	=	Unstructured Mesh 3D, Navier-Stokes Mixed-element flow solver

¹Research Aerospace Engineer, NASA Langley Research Center, Configuration Aerodynamics Branch, AIAA Member.

²Senior Research Engineer, NASA Langley Research Center, Configuration Aerodynamics Branch, AIAA Associate Fellow.

³Research Aerospace Engineer, NASA Langley Research Center, Configuration Aerodynamics Branch, AIAA Member.

Symbols

b_{ref}	=	Configuration reference span, inch
c	=	Local component chord, inch
c_d	=	Sectional drag coefficient, nondimensional
C_D	=	Configuration drag coefficient, nondimensional
$c_{d,f}$	=	Sectional skin-friction drag coefficient, nondimensional
$c_{d,p}$	=	Sectional pressure drag coefficient, nondimensional
$C_{D,p}$	=	Configuration pressure drag coefficient, nondimensional
$C_{D,v}$	=	Configuration viscous drag coefficient, nondimensional
c_F	=	Flap element chord, inch
$C_{f,x}$	=	X-component of skin-friction drag coefficient, nondimensional
C_L	=	Configuration lift coefficient, nondimensional
c_l	=	Sectional lift coefficient, nondimensional
C_m	=	Configuration pitching moment coefficient, nondimensional
c_M	=	Main element chord, inch
c_m	=	Sectional pitching moment coefficient, nondimensional
C_p	=	Pressure coefficient, nondimensional
c_{ref}	=	Configuration reference chord, inch
g_e	=	Slot exit gap, nondimensionalized by total local chord
g_i	=	Slot inlet gap, nondimensionalized by total local chord
M_∞	=	Freestream Mach number, nondimensional
ML/D	=	Aerodynamic efficiency, nondimensional
M_{shk}	=	Local normal-shock Mach number, nondimensional
o_F	=	Flap overhang, nondimensionalized by total local chord
$(r/c)_{\text{LE}}$	=	Leading-edge radius, nondimensionalized by local component chord
Re_{cref}	=	Reynolds number based on reference chord, nondimensional
S_{base}	=	Nominal surface cell size, inch
S_{curv}	=	Maximum surface cell-facet curvature angle
S_{min}	=	Minimum surface cell size, inch
S_{ref}	=	Reference area, square inch
$(t/c)_{\text{max}}$	=	Maximum airfoil thickness-to-chord ratio, nondimensional
$(t/c)_{\text{TE,F}}$	=	Trailing-edge thickness of the flap element, nondimensionalized by total local chord
$(t/c)_{\text{TE,M}}$	=	Trailing-edge thickness of the main element, nondimensionalized by total local chord
x/c	=	x-location, nondimensionalized by total local chord
x/c_{shk}	=	Terminating shock location, nondimensionalized by total local chord
x_1	=	Location of end of initial flow acceleration, nondimensionalized by local component chord
x_2	=	Location of start of pressure recovery region, nondimensionalized by local component chord
z/c	=	z-location, nondimensionalized by total local chord
α	=	Angle of attack, degree
δ	=	Deflection, degree
η	=	Semispan location, nondimensional
γ	=	Slot inlet-to-exit gap ratio (slot ratio), nondimensional
λ	=	Midchord wing sweep, degree
ϕ	=	Twist, degree

I. Introduction

THE aeronautics industry remains in continuous pursuit of advanced technologies and capabilities that offer revolutionary improvements in efficiency, safety, and sustainability for next-generation aircraft systems. Toward addressing this objective, the NASA Aeronautics Research Mission Directorate (ARMD) released its 2023 Strategic Implementation Plan that identified six strategic research thrusts: (1) Safe, Efficient Growth in Global Operations, (2) Innovation in Commercial High-Speed Aircraft, (3) Ultra-Efficient Subsonic Transport, (4) Safe, Quiet, and Affordable Vertical Lift Air Vehicles, (5) In-Time System-Wide Safety Assurance, and (6) Assured Autonomy for Aviation Transformation [1]. In particular, the Ultra-Efficient Subsonic Transport strategic thrust targets improvements in aircraft efficiency, coupled with reductions in noise and emissions, through innovative airframe and propulsion technologies. In support of this strategic thrust, the Advanced Air Transport Technology (AATT) Project within the ARMD Advanced Air Vehicles Program (AAVP) seeks to develop such technologies in application to fixed-wing, subsonic transport aircraft. Current research within the AATT Project has focused on the maturation of technologies, such as boundary layer ingestion propulsion, active flow control technology, Crossflow Attenuated Natural Laminar Flow (CATNLF) aircraft components, the Transonic Truss-Braced Wing (TTBW), and the Cruise Slotted Wing (CSW) [2].

The cruise slotted wing is a multielement wing concept with a forward main element and an aft flap element, separated to form a slot that redirects airflow from the main element lower surface toward the low-momentum, upper-surface boundary layer of the flap. As shown in Fig.1, cruise slotted airfoils enable greater aft loading without flow separation compared to supercritical airfoils. Supercritical airfoils [Fig. 1(a)] are designed to avoid shock-induced flow separation over their upper surface by limiting the pressure recovery gradient imposed on the boundary layer when returning the flow toward freestream conditions at the trailing edge. This is typically achieved by designing the upper surface to have a terminating shock located at approximately 60% to 70% chord, followed by a gradual reduction in surface slope toward the trailing edge to promote a subcritical pressure recovery free of separation. In contrast, the cruise slotted airfoil [Fig. 1(b)] only requires the flow along the main element upper surface to undergo a partial pressure recovery. Inclusion of the slot allows for accelerated airflow along the main element lower surface to merge with flow from the main element upper surface to traverse behind the trailing edge as an unconfined wake. If the wake is designed to avoid interference with the upper-surface boundary layer of the flap, the flap is capable of tolerating an abrupt pressure recovery over approximately the last 5% chord without separation. Relative to the supercritical airfoil, the cruise slotted airfoil enables greater aft loading which helps to reduce shock strength and transonic pressure drag. The cruise slotted wing may be considered a passive drag reduction technology, leading to fuel savings and increased vehicle range for next-generation aircraft.

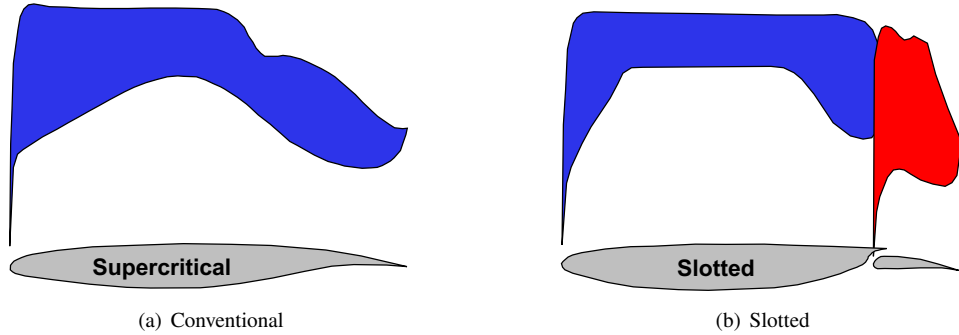


Figure 1. Notional pressure distributions for conventional and slotted airfoils (adapted from [3]).

In addition to drag reduction, the CSW concept may be leveraged to provide other design trade benefits. These benefits include increases in Mach number, lift coefficient, wing thickness and/or a decrease in wing sweep while holding drag constant. These competing design trades are analytically described using the 3D Korn equation for transonic wing performance, as shown in Eq. 1. The 3D Korn equation relates an airfoil technology factor (κ_A) to Mach number (M_∞), lift coefficient (C_L), wing thickness (t/c), and wing sweep (λ) [4]. The airfoil technology factor is an empirical constant determined for each unique class of airfoils, e.g., $\kappa_A = 0.95$ for NASA supercritical airfoils and $\kappa_A = 0.87$ for NACA 64A-series airfoils.

$$\kappa_A = M_\infty \cos \lambda + \frac{C_L}{10 \cos^2 \lambda} + \frac{(t/c)}{\cos \lambda} \quad (1)$$

An improvement in aerodynamic efficiency can be considered an increase in airfoil technology factor. This increment translates to an increase in Mach number, lift coefficient, wing thickness and/or a decrease in wing sweep. From an aerodynamics perspective, an increase in Mach number, lift coefficient, or wing thickness will increase the average velocity over the airfoil upper surface with a subsequent increase in shock strength. A similar result is expected when decreasing wing sweep due to an increase in streamwise velocity. Use of a cruise slotted airfoil permits greater aft loading to mitigate increases in shock strength and pressure drag. This design feature enables improvements in Mach number, lift coefficient, wing thickness, and/or wing sweep that translate to unique vehicle-level benefits. Increasing Mach number would allow reduced travel time, while increasing lift coefficient would allow increases in aircraft weight capacity that may be used for additional cargo or fuel for extended-range missions. Alternatively, increasing wing thickness would allow for a lighter wing structure and greater fuel volume. Similarly, reducing wing sweep not only leads to a lighter wing structure, but also increases the potential for Natural Laminar Flow (NLF) by reducing the growth of Crossflow (CF) boundary layer instabilities that can lead to transition [5]. Integration of CSW technology into next-generation commercial transports would allow for one or a combination of vehicle-level benefits to be attained.

Since the original development of the cruise slotted airfoil in 1965 [6], the concept has yet to be integrated into production aircraft due to historical concerns regarding the modeling accuracy of complex viscous-flow interactions, increased manufacturing complexity, and system integration uncertainty [3]. However, prior research by Boeing and the NASA Langley Research Center in the early 2000s experimentally confirmed the technology's benefit of increased aerodynamic efficiency, (ML/D), for both partial- and full-span CSW variants of a representative twin-aisle transonic transport by 1% due to a 0.03 increase in drag divergence Mach number, as shown in Fig. 2 [7, 8]. The demonstrated benefit of CSW technology has led to continued industry interest with multiple active research programs focused on increasing the Technology Readiness Level (TRL) for potential integration to next-generation aircraft [9–15].

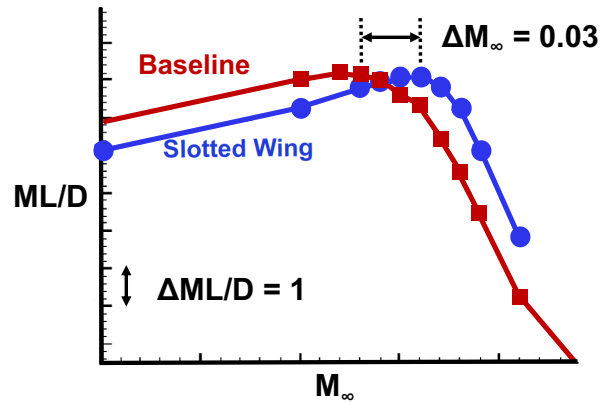


Figure 2. Aerodynamic efficiency (ML/D) comparison between baseline and slotted wings designed and tested by NASA/Boeing (adapted from [3])

One noteworthy development is the NASA/University of Tennessee at Knoxville University Leadership Initiative (ULI) investigation of the Slotted, Natural Laminar Flow (SNLF) airfoil concept as a drag reduction technology [13]. SNLF airfoils [10–13] are cruise slotted airfoils with main element upper and lower surfaces shaped to achieve full-chord favorable pressure gradients, which are known to promote NLF by suppressing the growth of Tollmien-Schlichting (TS) instabilities [5]. This design feature is possible due to the ability of the cruise slotted airfoil to tolerate increased aft loading, while permitting the main element to undergo a partial pressure recovery at its trailing edge.

To quantify the drag benefit of SNLF airfoils, the ULI program designed a SNLF airfoil, referred to as the S207, for assessment on a Boeing TTBW configuration [14]. The S207 airfoil was designed for cruise at $M_\infty = 0.7$ and $Re_c = 13.1$ million using MSES, a coupled Euler/integral-boundary-layer solver [16]. Computational results indicate that significant extents of NLF could be achieved over both main element surfaces because of its design for favorable pressure gradients over a range of sectional lift coefficients. Full-chord NLF is expected over the flap element lower surface due to its relatively low chord Reynolds number and mild pressure gradient. The flap element upper surface is assumed to be turbulent due to the impact of Görtler vortices that form over the concave main element lower surface [17, 18]. These vortices cause the lower-surface boundary layer of the main element to become turbulent, creating a slot blockage that produces an adverse pressure gradient over the flap upper surface responsible for transition due to TS [14]. Using the S207 section characteristics, a conceptual aircraft design assessment was performed by Boeing Research & Technology

to quantify the performance benefits SNLF technology in application to a TTBW configuration with a cruise Mach number of 0.727 [19]. The study found that the SNLF airfoil improvement in aerodynamic efficiency resulted in an additional 5% decrease in block fuel per seat over the original wing design for the Boeing TTBW. For validation of the SNLF concept, an experiment was conducted in the NASA Ames 11-ft Unitary Plan Wind Tunnel using a semispan wing based on the S207 SNLF airfoil [15]. The test confirmed extensive regions of laminar flow and established the feasibility of SNLF airfoils and CSW technology at TTBW flight conditions.

The present research, under the NASA AATT Project, seeks to quantify the potential vehicle-performance benefits of CSW technology in application to next-generation single-aisle transonic commercial transports with cruise speeds at Mach 0.8. For cruise Mach numbers greater than 0.75, the combination of Mach number and lift coefficient requirements becomes too prohibitive for effective SNLF airfoil design with extensive regions of NLF [14]. Furthermore, an NLF design strategy, such as the CATNLF method [20, 21], that suppresses the growth of CF instabilities is required for leading-edge wing sweeps and flight Reynolds numbers typical of transonic transport aircraft. Cruise slotted airfoil concepts are sought to meet these more demanding design requirements while achieving improvements in cruise drag and near-cruise drag divergence behavior. This motivated the need for an aerodynamic design tool that would help quantify the potential drag savings and other wing design benefits enabled by CSW technology.

To address this need, a knowledge-based aerodynamic design method, Constrained Direct Iterative Surface Curvature (CDISC) [22], has been extended for CSW design through the formulation of several multielement geometric and aerodynamic design constraints. Previous research documented the initial development of these constraints and the design of a cruise slotted airfoil at conditions representative of a Mach-0.8 variant of the Common Research Model (CRM-M8) [9]. A key design finding was that the skin-friction penalty associated with cruise slotted airfoils could primarily be attributed to the formation of a new boundary layer on the flap element. One solution to mitigate this skin-friction penalty is the Aft Laminar Multielement Airfoil (ALMA) concept shown in Fig. 3.

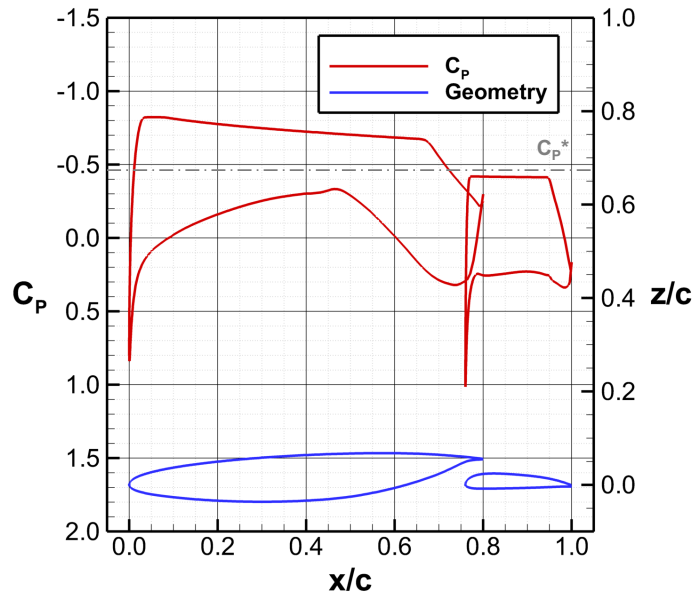


Figure 3. Example CDISC cruise slotted airfoil design using the ALMA concept.

The ALMA concept is a cruise slotted airfoil with a turbulent main element and an NLF flap. Reynolds-averaged Navier-Stokes (RANS) simulations predict a 2.7% reduction in sectional cruise drag and improved near-cruise drag rise behavior when compared to a supercritical airfoil. ALMA effectively offsets the increased skin-friction drag penalty of multielement airfoils, while retaining a pressure drag benefit. The concept also serves as an incremental step toward the integration of smaller NLF components, with reduced manufacturability and maintainability requirements, on next-generation aircraft. Also, because NLF design requirements are not imposed on the main element, conventional high-lift, leading-edge devices may be used without transition concerns due to steps/gaps/etc. These advantages motivate continued research focused on quantifying the drag savings of the ALMA concept when extended to CSW design.

The current paper demonstrates the use of CDISC for the design of a conventional supercritical wing and a partial-span, CSW for the CRM-M8 configuration. The CSW design leverages the ALMA concept and features an NLF flap element that supports significant extents of laminar flow over its upper and lower surfaces. CDISC was coupled to the NASA Unstructured Mesh 3D, Navier-Stokes Mixed-element (USM3D-ME) flow solver to quantify the cruise drag differences between the designed configurations. Design results are compared from three configuration analyses, including: (1) the conventional supercritical wing, (2) the partial-span CSW with an NLF flap, and (3) the partial-span CSW analyzed fully turbulent as an off-design consideration for loss of NLF. Additionally, near-cruise off-design performance is assessed using drag polar and drag rise analyses.

II. Computational Tools

A flow chart of the design and analysis process used in this work is illustrated in Fig. 4. This process consists of a suite of computational tools including a flow solver, grid manipulation tools, a design module, and transition prediction software. The primary design and analysis modules are shown in red, whereas auxiliary output information is shown in blue. Given an initial geometry, the design loop begins with generating a baseline flow solution at the design condition of interest. The extraction module obtains the surface geometry, pressure distribution, and skin-friction distribution data at select spanwise airfoil design stations, aligned in the freestream direction. The design module is used to shape and twist each airfoil to better match a user-defined target pressure distribution. The grid movement code is then used to blend these airfoil designs along the wingspan to create a new wing surface and to deform the grid from the initial geometry to the new CDISC design shape. This new computational grid is run in the flow solver for the updated design solution. This design process is repeated until the design pressures are deemed sufficiently converged to the target pressures. An external transition prediction module reads in the extracted airfoil geometry and CFD pressure distribution, then uses this information to predict the chordwise extents of natural laminar flow achieved for each design surface. This transition information is passed to the flow solver for forced laminarization simulations to predict the aerodynamic performance of NLF design components.

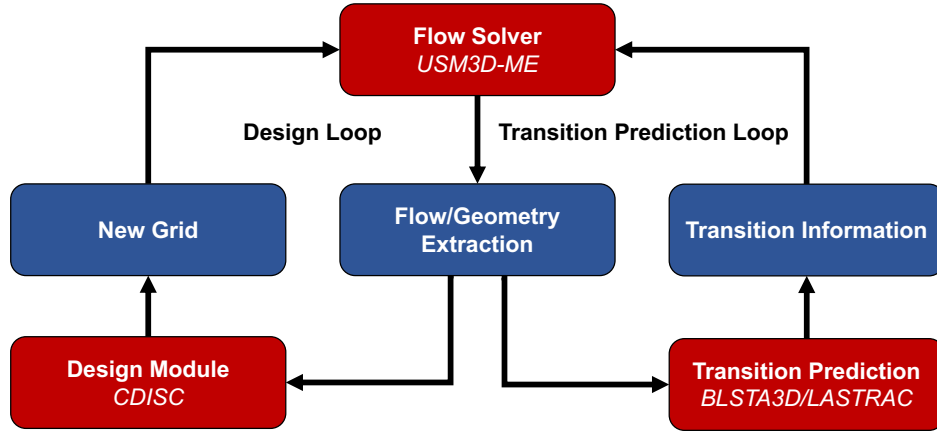


Figure 4. Flow chart of the CDISC design and analysis process.

A. Flow Solver

The NASA USM3D-ME flow solver is a mixed-element, cell-centered, Reynolds-averaged Navier-Stokes solver [23] and was used to generate fully turbulent and forced laminarization solutions. Solutions were computed on two mixed-element grids generated for the baseline conventional and cruise slotted wing models of the CRM-M8 configuration, as will be discussed in Section V. Inviscid fluxes were computed with Roe's flux-differencing scheme with no flux limiting. The Spalart-Allmaras (SA) one-equation turbulence model, with Rotation/Curvature (RC) and Quadratic Constitutive Relation (QCR) 2000 corrections, was used to model regions of turbulent flow. Forced laminarization simulations were used to model laminar flow ahead of predicted transition fronts to properly model the reduced skin-friction drag and boundary layer thickness of laminar flow regions.

B. Design Module

The design module selected for this study is CDISC, a knowledge-based design method compatible with USM3D-ME, among many other flow solvers. CDISC uses flow-geometry sensitivity derivatives, predetermined from empirical and analytical studies, to estimate the geometry changes needed to minimize the difference between current and target pressure distributions. This computationally efficient design approach eliminates the need to numerically calculate sensitivity derivatives and allows the design to converge with the flow solution. The CDISC method has had extensive use for both turbulent and NLF computational designs at transonic and supersonic conditions [9, 21, 24–27]. CDISC flow constraints are available to enforce a variety of engineering design variables, including: sectional force and moment coefficients, spanwise load distributions, and shock location/strength. Additionally, geometry constraints are available to address structural and manufacturing requirements, including: thickness, curvature, volume, and leading-edge radius. New geometry and aerodynamic constraints for multielement airfoil design are detailed in Section III.

C. Transition Prediction

For laminar flow predictions, transition front information was generated using BLSTA3D (Boundary Layer code for Stability Analysis 3D) [28] and LASTERAC (Langley Stability and Transition Analysis Code) [29]. BLSTA3D provides the required boundary layer temperature and velocity profiles based on a geometry and its associated pressure coefficient distribution. These boundary layer profiles are exported to LASTERAC for stability analyses, which calculates the growth of both Tollmien-Schlichting (TS) and crossflow (CF) modal instabilities. In the present research, stability calculations are based on the Linear Stability Theory (LST) e^N method with compressibility effects but not curvature influence. The critical amplification factor or N-factor chosen for this research was nine, which is typically assumed for a transonic flight environment [5, 30]. For the current work, transition predictions were generated for the flap element target pressure distribution to confirm the suppression of TS and CF modal instabilities. These transition predictions were used to define a target transition front that was linearly interpolated along the flap element based on the chordwise transition locations from each airfoil design station. During the design process, USM3D-ME forced laminarization simulations were performed using the target transition fronts on the upper and lower surfaces of the flap element and a fully turbulent main element. Transition predictions are used to conduct laminar simulations at cruise and near-cruise conditions to estimate transition front sensitivity to angle of attack and Mach number.

III. CDISC Multielement Design Constraints

A. Geometry Constraints

Figure 5 illustrates the CDISC geometry constraints developed for multielement airfoil design. The slotted airfoil is defined by a main element and flap that are positioned to form an intermediate slot between the main element lower surface and flap upper surface. In addition to traditional airfoil geometry constraints, such as thickness, camber, or leading-edge radius, four additional CDISC geometry constraints have been defined for multielement airfoil design to control the flap position, flap orientation, and slot shape. These geometry constraints include flap overhang (o_F), flap deflection (δ_F), slot exit gap (g_e), and slot inlet gap (g_i), as shown in Fig. 5. The definitions of these constraints are detailed below with additional insights provided regarding best practices for cruise slotted airfoil design. A spanwise leading-edge blending constraint developed for partial-span, CSW configurations is also reviewed.

1. Flap Overhang

Flap overhang (o_F) is defined as the horizontal distance between the flap leading edge and the main element trailing edge, normalized by total chord. The overhang parameter defines the slot length and flap chord. In CDISC, the chordwise location of the main element trailing edge is fixed, and the chord length of the flap is scaled to match a user-prescribed value for overhang. The purpose of the slot is to accelerate the flow from the low-velocity region along the main element lower surface toward the upper-surface boundary layer of the flap. This feature allows for the main element to undergo a partial pressure recovery while increasing the velocities over the upper surface of the flap for increased aft loading. In terms of design trade-offs, flap loading is maximized when the flow is accelerated as quickly as possible, which necessitates minimization of slot length. The implication of a shorter slot length is greater slot curvature, but also a shorter flap chord. A shorter flap chord decreases the maximum potential loading of the flap, counteracting the benefits of a shorter slot length. Constraints on overhang are likely to be a practical consideration with values chosen to meet manufacturability and/or aerostructural limitations. In the absence of multidisciplinary

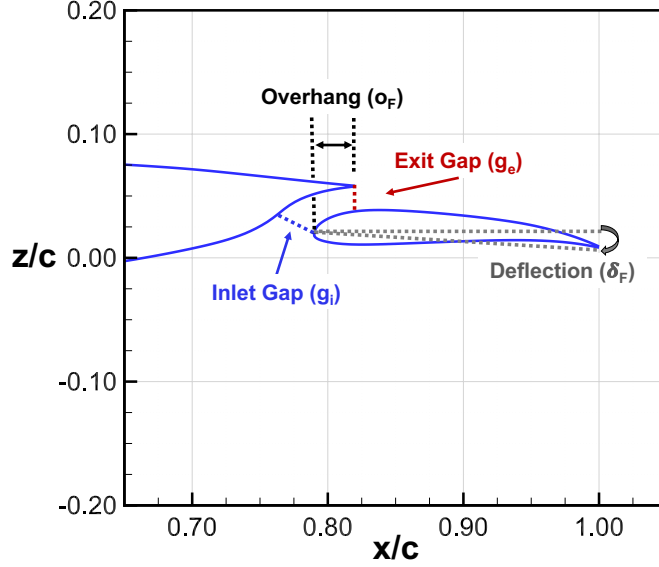


Figure 5. CDISC multielement geometry constraints.

constraints, overhang is typically held constant during the design process and set to a value near the target chordwise location of the initial flow acceleration on the upper surface of the flap. This is done to minimize the pressure difference between the main element trailing edge and the flap upper surface target, providing more uniform flow through the slot.

2. Flap Deflection

Flap deflection (δ_F) is defined as the flap incidence angle, measured in degrees, relative to the zero-angle-of-attack reference line. Deflection has a direct impact on controlling both the leading-edge acceleration and total lift generated by the flap component. In the CDISC design process, an aerodynamic constraint is used to define a target pressure distribution for the flap to control the initial acceleration, a midchord pressure gradient, and pressure recovery location over the upper surface for a given lift requirement, as detailed in Section III.B. The CDISC algorithm changes the deflection or twist of the flap element, along with shape changes, iteratively to minimize the differences between the analysis pressures and the target pressure distribution. During design, user-prescribed deflection values are not set, and the geometric variable is driven by the aerodynamic constraint to meet target pressure distributions. For off-design analysis at low-speed, high-lift conditions, CDISC users may define a desired flap deflection angle to assess stall performance.

3. Slot Exit Gap

The slot exit gap (g_e) is defined as the vertical distance between the main element trailing edge and the flap upper surface, normalized by total chord. At the main-element trailing edge, the upper and lower surface boundary layers combine to form a wake that traverses above the upper surface of the flap. The wake generated contributes to an aft-chord pressure recovery that is resistant to flow reversal due to turbulent mixing. Richard Whitcomb's prior research on slotted airfoils led to the observation that the wake was able to traverse above the flap distinctly from the upper-surface boundary layer for slot exit gap values approximately twice that of the main element lower-surface boundary layer thickness at the trailing edge [6]. Previous USM3D-ME simulations for a CDISC cruise slotted airfoil design study supported this empirical guideline by showing that the main element wake traveled distinctly from the flap boundary layer without interference for slot exit gap values greater than 1.8 times the estimated boundary layer thickness [9]. As a best practice, the slot exit gap is recommended to be equal to two times the flat-plate estimate for turbulent boundary layer thickness using the main element chord. For CSW design, nondimensional slot exit gap can be held constant across the span based on the design station with the largest interference-free gap requirement. The limiting gap is typically calculated at the most outboard design station because of increases in required nondimensional slot exit gap with decreases in local chord Reynolds number.

4. Slot Inlet Gap

The slot inlet gap (g_i) is defined as the shortest distance between the leading edge of the flap and the maximum surface slope point on the main element lower surface, normalized by total chord. In CDISC, users implicitly define the slot inlet gap value by setting the required slot exit gap and an inlet-to-exit gap ratio ($\gamma = g_i/g_e$). This inlet-to-exit gap ratio, or slot ratio, is used as a more convenient parameterization of the slot inlet gap (multiples of exit gap). For a given flap shape/deflection/overhang, CDISC first positions the flap to satisfy the user-specified values for slot exit gap. Then, the slot ratio is used to set the slot inlet gap by moving the maximum surface slope point on the main element lower surface accordingly. The main element lower surface is blended into the existing geometry ahead of this point. Aft of this point, the main element lower surface is reshaped to satisfy a linear area distribution between the slot inlet and exit gap in reference to the upper surface design shape of the flap. An increase in slot ratio effectively reshapes the main element lower surface to have greater aft camber. This increase in aft camber increases the aft loading of the main element by increasing the "pressure bucket" area. This increased aft loading over the main element permits lower velocities over the upper surface and lower wave drag for a given sectional lift (c_l) requirement. A slot ratio upper limit is reached once the increase in camber creates too significant of a pressure gradient through the slot while accelerating the flow from the low-velocity "pressure bucket" to the high-velocity upper surface of the flap. This limiting pressure gradient causes flow separation along the main element lower surface. This separation creates an unintended drag penalty and could lead to unsteady pressure waves over the flap upper surface that cause early transition. For CSW design, slot inlet gap is manually increased to maximize main element aft loading while ensuring sectional skin-friction drag values remain positive through the slot to avoid flow reversal.

5. Align Leading Edge

During the development of the partial-span, CSW design, a constraint was needed to help blend the leading-edge geometry at the wing planform break where the wing transitions from a conventional supercritical airfoil to a cruise slotted airfoil. The increased aft loading of the cruise slotted airfoil generally orients the main element to a lower twist angle relative to a conventional airfoil. This twist discrepancy can lead to abrupt changes in the leading-edge geometry near the airfoil transition. The Align Leading Edge (ALE) constraint can be used at the most inboard cruise slotted airfoil design station near the planform break to blend the nose geometry between the adjacent conventional airfoil design station and the next outboard cruise slotted airfoil station.

B. Aerodynamics Constraints

Figure 6 provides an example CDISC target pressure distribution for a baseline transonic cruise slotted airfoil. This target pressure distribution for multielement airfoil design was developed using two aerodynamic, or flow, constraints in CDISC. The first aerodynamic constraint defines the target pressure characteristics over the main element and flap upper surfaces. The second aerodynamic constraint is used to redistribute the sectional loading between the two elements while matching total sectional lift. The use of these aerodynamic constraints is detailed below with additional insights provided regarding best practices for cruise slotted airfoil design.

1. Target Pressure Generation

The target pressure generation constraint is used to design the upper surface pressure distribution based on three parameters, including the chordwise location to end the initial flow acceleration (x_1), the midchord pressure gradient ($\Delta C_p/\Delta(x/c)$), and the chordwise location to start the pressure recovery region (x_2). For turbulent design, the x_1 location can be set using a CDISC design best practice of seven times the non-dimensional leading-edge radius. For NLF design, x_1 is often driven by requirements to dampen CF instability growth. An empirical CDISC relation based on leading-edge radius, local sweep, and user-specified values for critical N-factor and flight Reynolds number may be used to estimate the x_1 value needed to suppress CF instabilities. The midchord pressure gradient can be calculated internally within CDISC using a best practice for multipoint transonic design. For NLF design, a favorable midchord pressure gradient is typically required to suppress TS instabilities. In laminar designs, the midchord pressure gradient required for TS suppression can be calculated using an empirical CDISC relation based on user-specified values for N-factor and flight Reynolds number. For transonic airfoils, the x_2 location is typically set as the target shock location. For subsonic airfoils, x_2 may be chosen as the start of the subcritical pressure recovery region. Once these parameters have been defined, a target pressure distribution is generated that simultaneously satisfies sectional lift and pitching moment requirements.

For CSW design, a target pressure architecture typically used for supercritical airfoils was used for the main element design, where the x_2 terminating shock location was placed as far aft as possible to limit the Mach number ahead of the shock in hopes of reducing wave drag. Due to the relatively low chord Reynolds number and leading-edge sweep of the flap element, CF instabilities are easily suppressed below the critical N-factor with x_1 locations consistent with turbulent airfoil best practices. Furthermore, TS instabilities are suppressed using mild adverse pressure gradients without the need for favorable midchord pressure gradients often used for NLF airfoils. Laminar flow is maximized over the flap element by setting the x_2 pressure recovery to begin at 80% flap chord with an abrupt return to freestream conditions over the last 5% of the total airfoil chord.

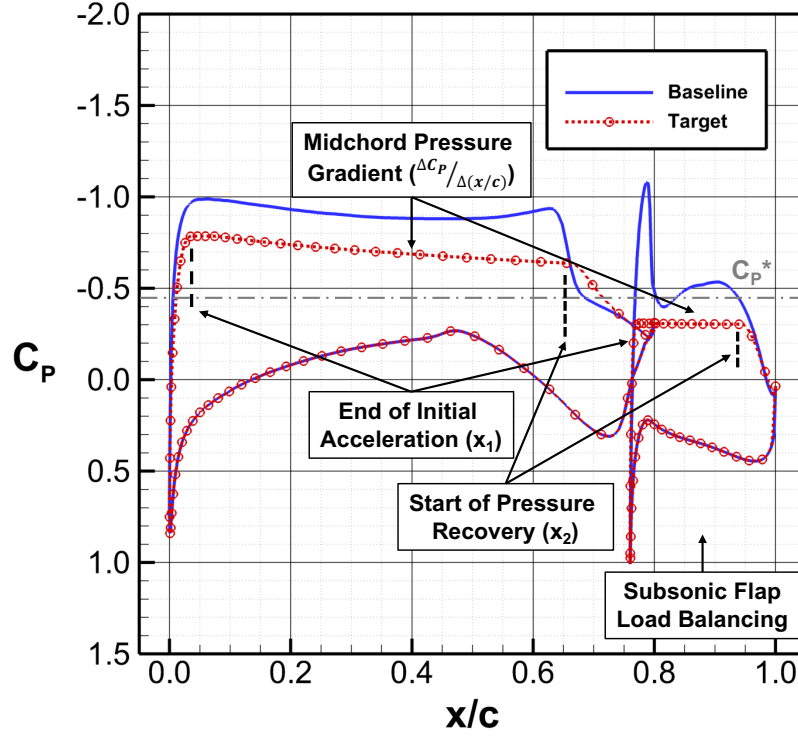


Figure 6. Example transonic cruise slotted airfoil target pressure distribution in CDISC.

2. Multielement Load Balancing

A multielement load balancing constraint was added to alter the proportion of sectional lift contributions from the main element and flap. Users may specify a flap loading ratio, which is the ratio of the sectional lift of the flap to the sectional lift of the main element. For cruise slotted airfoil design, the loading ratio should be increased to maximize aft loading without introducing shocks that could cause increased wave drag or strong adverse pressure gradients that lead to early transition. While not used in the present research, the load balancing constraint can also be used to satisfy hinge loading requirements for the flap in consideration of aerostructural constraints.

IV. Geometry

A. Conventional Wing Model

The NASA Common Research Model (CRM) is a generic configuration representative of widebody commercial transports, with a cruise Mach number of 0.85 [31]. To assess the benefits of CSW technology in application to single-aisle commercial transport, a Mach-0.8 variant of the CRM (CRM-M8) was developed by decreasing the wing sweep and geometric scale from the original CRM. Simple sweep theory was used to reduce the CRM original wing quarter-chord sweep from 35 to 25 degrees. A geometry scaling factor of 0.6 and a weight scaling factor of 0.3 were determined to be representative of the single-aisle transport class. A 40,000 ft cruise altitude was assumed, resulting in a scaled cruise lift coefficient of 0.543 for the CRM-M8. Table 1 summarizes the differences in geometry and flight conditions between the CRM and the CRM-M8 configurations. The resulting CRM-M8 cruise condition is $M_\infty = 0.8$, $Re_{cref} = 21.1$ million, and $C_L = 0.543$. The CRM-M8 moment reference center was scaled to the coordinates $(x, y, z) = (737.8, 0.0, 26.2)$ inches for the model with the fuselage nose centered at the grid origin.

Table 1. Geometry and flight conditions for the CRM and CRM-M8 configurations.

Configuration	c_{ref} (in)	b_{ref} (in)	S_{ref} (in ²)	λ	M_∞	Re_{cref}	C_L
CRM	275.8	1,156.75	297,360	35°	0.85	43.2×10^6	0.500
CRM-M8	165.5	694.05	107,050	25°	0.80	21.1×10^6	0.543

Figure 7 illustrates the resulting CRM-M8 configuration used as the baseline for the fully turbulent conventional wing design. The CRM-M8 configuration includes only the wing and fuselage components. Nacelles, pylons, and tail components were removed to reduce the required grid size for the wing design studies. The spanwise distributions of nondimensional maximum thickness and leading-edge radius remain unchanged from the original CRM configuration. The target spanwise lift distribution for the conventional wing design was held fixed to the baseline spanwise lift distribution at the lift-matched cruise condition. The CRM-M8 configuration is an unofficial variant of the CRM and serves only as a research vehicle with aerodynamics representative of a modern transonic single-aisle transport aircraft.

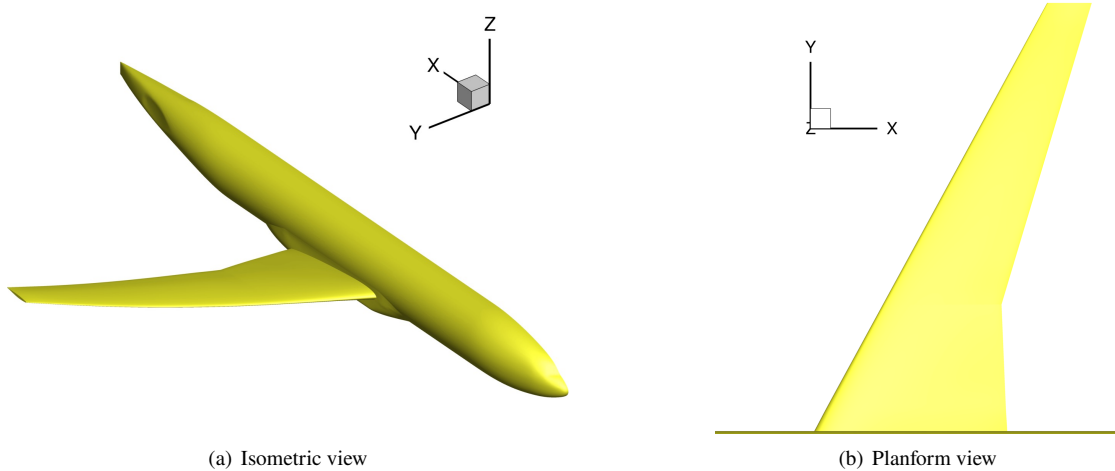


Figure 7. CRM-M8 conventional wing model isometric and planform views.

B. Partial-span, Cruise Slotted Wing Model

A partial-span, slotted wing variant of the CRM-M8 configuration was created using an in-house MAKESLOT code, which creates a slotted wing geometry from a conventional wing using the slotted airfoil design variables previously shown in Fig. 5. The conventional wing design developed for the CRM-M8 configuration served as the initial geometry to MAKESLOT. Table 2 lists the geometric constraints used to create the outboard CSW section. The main element trailing edge was held fixed at a nondimensional total chord location of $x/c = 0.8$ across the span of the slotted wing region. The overhang was set equal to 4% total local chord. The flap deflection was arbitrarily set to two degrees for an initial flap loading with changes expected during the design process to meet the target flap loading. The nondimensional slot exit gap was set to $0.025c$ using the CDISC best practice for interference-free flow between the main element wake and flap upper-surface boundary layer at the most limiting outboard station. The slot ratio was set to an initial value of 1.3, based on insights from previous 2D simulations [9], with the expectation that changes may be needed in the 3D wing design to reduce potential separation on the main element lower surface. The trailing-edge thickness (nondimensionalized by local chord) for the flap element matched the values for the conventional CRM-M8 wing. The main element trailing-edge thickness was decreased to a value of 0.001 to limit a potential base drag penalty.

Table 2. Baseline slotted wing design variables.

δ_F	c_F	δ_F	g_c	γ	$(t/c)_{TE,M}$	$(t/c)_{TE,F}$
0.04	0.22	2°	0.025	1.3	0.001	0.003

Figure 8 shows the baseline partial-span, slotted wing model for the CRM-M8 configuration, developed using MAKESLOT. The fuselage and inboard wing components are yellow and represent the components consistent with the conventional wing CRM-M8 design. For the outboard CSW section, the red component is the main element, and the blue component is the flap. The CSW section begins at the planform break with the exception of a two-inch (0.3% semispan) gap added between the inboard conventional wing and outboard flap element to simplify grid generation. The spanwise distribution of dimensional thickness from the conventional wing was matched for the main element. Due to the relative differences in chord, the main element had nondimensional maximum thickness values approximately 30% greater compared to the conventional wing. The flap thickness was set to a constant 13.5% across the span in the absence of known structural constraints.

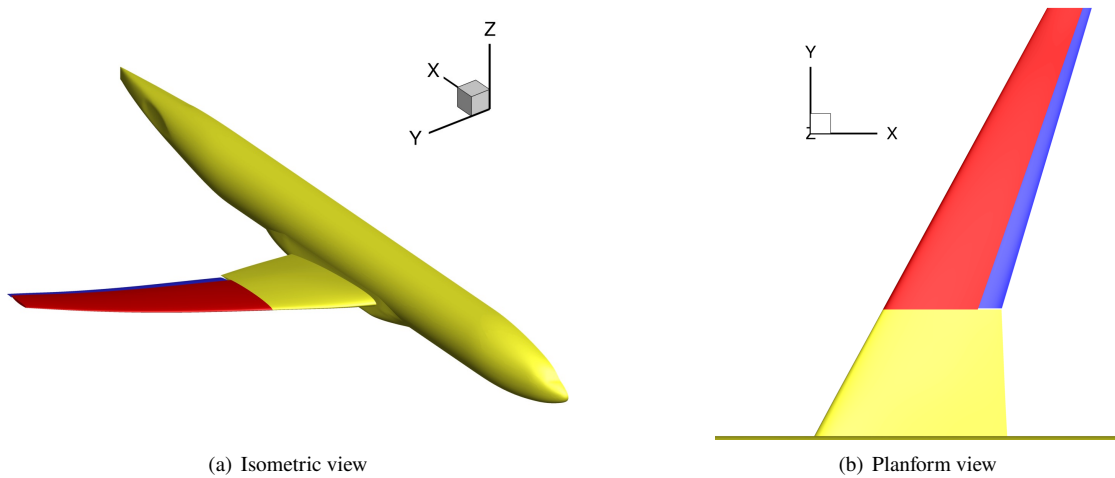


Figure 8. CRM-M8 partial-span, slotted wing model isometric and planform views.

V. Computational Grids

Mixed-element computational grids were generated for the baseline conventional and partial-span, CSW geometries detailed in Section IV. The grids were generated using HeldenMesh™ v4.12, a grid generation software developed by Helden Aerospace Corporation for efficiently creating three-dimensional, mixed-element unstructured meshes of complex configurations [32]. HeldenMesh™ utilizes automated advancing-layers and advancing-front unstructured grid generation methods. A simple, text-based input file allows the user to control the surface grid characteristics for each unique geometry component, the viscous boundary layer growth properties, and additional volume sourcing for improved flow feature resolution, e.g., wakes, shocks, etc.

The grid characteristics for the conventional and partial-span, CSW meshes are summarized in Table 3. The surface was resolved using prescribed values for the nominal cell size (S_{base}), minimum cell size (S_{min}), and maximum cell-facet curvature angle (S_{curv}) of each vehicle component. In HeldenMesh™, the surface grid is initialized using the nominal cell size, then performs a curvature-based cell refinement using the maximum surface curvature angle, where a minimum cell size is used to limit grid size. The nominal cell size was set to 0.75% of component-specific reference lengths. A global minimum cell size was set to 0.015% of the vehicle reference chord, and the meshes were refined using a S_{curv} value of two degrees. A maximum surface curvature angle of two degrees generally provides adequate grid resolution near the highly-curved leading-edges of turbulent design components. The volume grid was generated using the CRM-M8 flight Reynolds number with an average cell-centered y^+ equal to 1 and a minimum of 30 viscous cell layers. For the CSW grid, no volume sourcing was added to resolve the slot region due to the complexity in creating a volume source that accommodates spanwise variations in geometry and twist. Furthermore, the use of CDISC best practices values for slot exit gap in the development of the baseline CSW model provided sufficient area through the slot to avoid the intersection of growing boundary layer cells between the two elements. The final grid sizes, in terms of cell count, were 30.5 and 70.8 million for the conventional and slotted wing grids, respectively. An example of the surface grid resolution for the conventional and partial-span, slotted wings is provided in Fig. 9.

Table 3. Characteristics of the mixed-element HeldenMesh™ grids for the conventional and partial-span, CSW models.

Grid	S_{base}/L_{ref} [%]	S_{min}/c_{ref} [%]	S_{curv} [deg]	Nodes [million]	Cells [million]
Conventional Wing	0.75	0.015	2	12.8	30.5
Slotted Wing	0.75	0.015	2	29.2	70.8

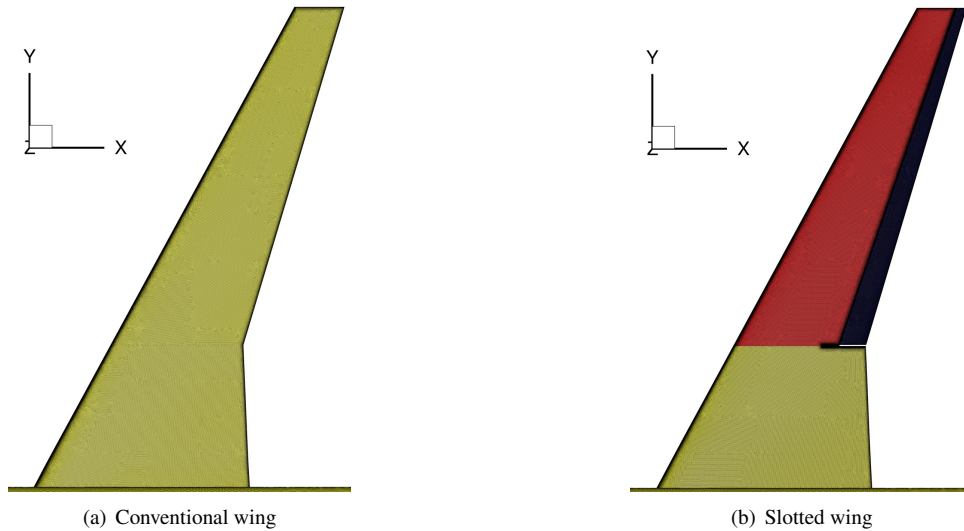


Figure 9. Surface grid for the CRM-M8 conventional and slotted wings.

VI. Design Approach

This section summarizes the CDISC design approach used for the CRM-M8 conventional and partial-span, CSW designs. The wings were designed to meet the CRM-M8 cruise condition of $M_\infty = 0.8$, $Re_{cref} = 21.1$ million, and $C_L = 0.543$, as calculated in Section IV. The design strategy consisted of maintaining identical inboard wing constraints between the two designs, so aerodynamic performance differences could be isolated to the outboard wing design. A common series of spanwise wing design stations were setup between the two models to limit design inconsistencies. The CDISC constraints detailed in Section III were applied to generate a conventional supercritical wing design and a partial-span, CSW design with NLF on the flap element. Details about the CDISC design setup and constraints used for each wing design are provided below.

A. Design Stations

For the CDISC designs, 13 airfoil design stations were created along the wing semispan, as shown in Fig. 10. The six stations denoted by dashed, black lines consist of two inboard and four outboard spanwise locations referenced in the remainder of the paper. The general parameters for the six referenced design stations are shown in Table 4. The inboard stations are referenced to confirm consistency in the geometry and pressure distributions between the two wing designs. The outboard stations are referenced to explore spanwise differences in the sectional aerodynamic characteristics and geometry between the conventional and cruise slotted wings. The six design stations have nondimensional semispan locations (η) ranging from 0.11 to 0.95 with a variation in local chord Reynolds number (Re_c) from 35.5 to 9.3 million.

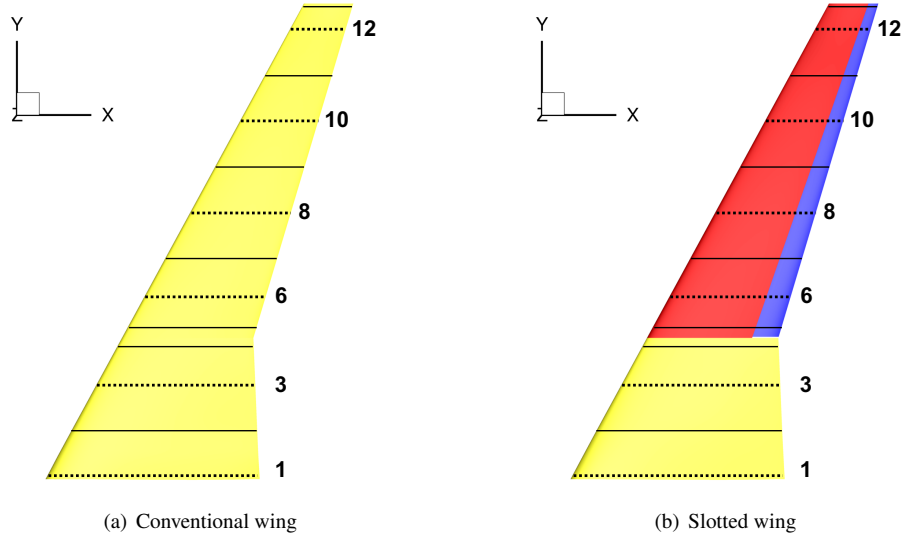


Figure 10. Planform view of the CRM-M8 conventional and slotted wing models with 13 CDISC design stations along the semispan.

Table 4. General parameters for the six referenced design stations.

Station	Location	η	Chord (ft)	Re_c (million)
1	Inboard	0.11	23.2	35.5
3	Inboard	0.28	17.3	26.5
6	Outboard	0.45	13.2	20.2
8	Outboard	0.61	11.0	16.8
10	Outboard	0.78	8.5	13.0
12	Outboard	0.95	6.1	9.3

B. Conventional Wing Design

To begin the design process, USM3D-ME simulations were performed for the baseline wing to get an initial solution at the lift-matched cruise condition. The baseline pressure distributions and geometry (blue-line) are shown for reference at station 3 on the inboard wing and at station 8 on the outboard wing in Fig. 11, in addition to the CDISC target pressure distributions (red-circle) and design results (red-line).

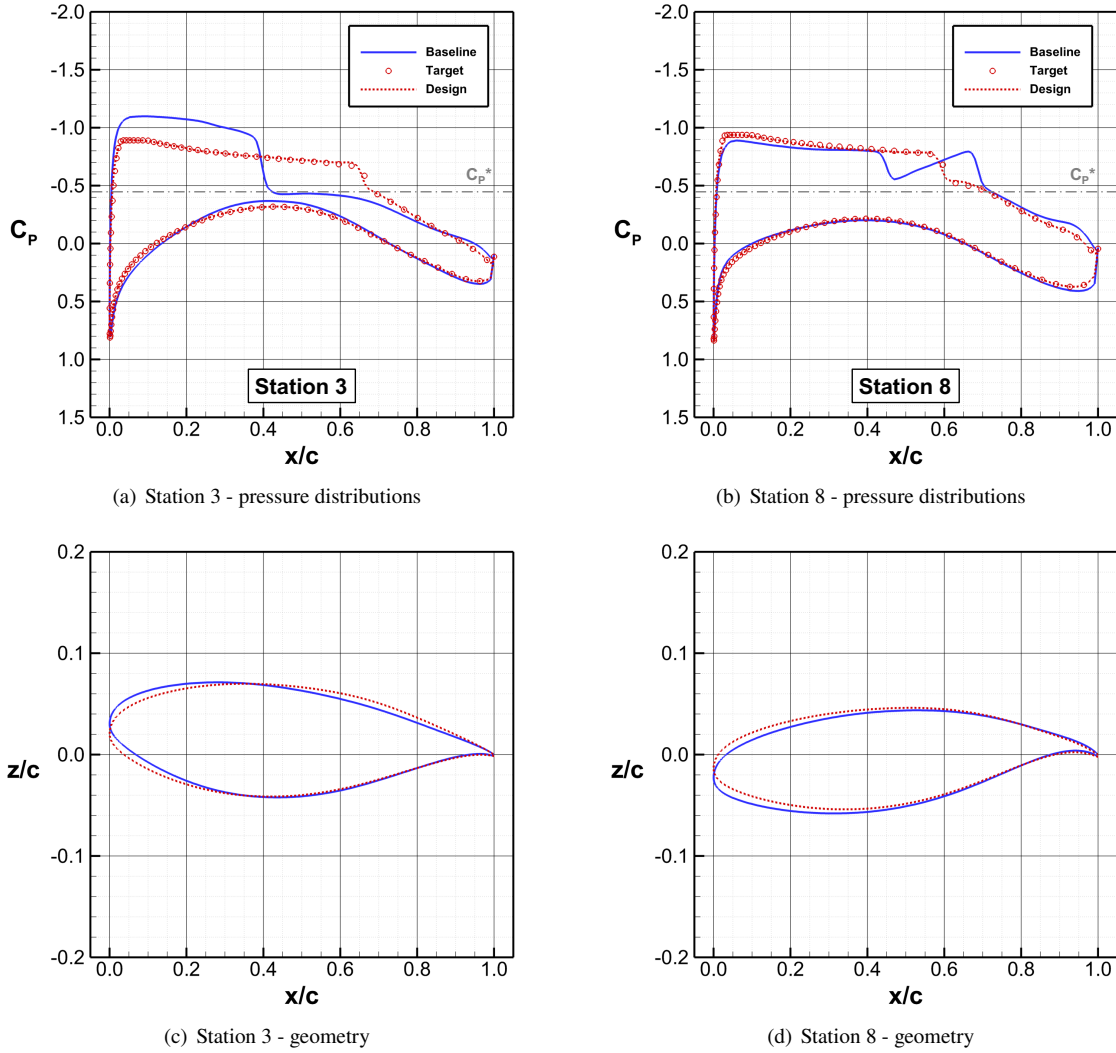


Figure 11. CRM-M8 conventional wing pressure distributions and geometry at stations 3 and 8 for the baseline and design.

The primary aerodynamic constraint used was the target pressure generation constraint described in Section III. The initial flow acceleration and midchord pressure gradient were set using CDISC best practices for multipoint design. The target shock location was set using a CDISC best practice related to the local normal-shock Mach number. At select stations, the target shock location was moved aft to reduce wave drag if the analysis pressures suggested additional design freedom for a stable, weaker shock. The target sectional lift coefficient was held fixed to the baseline value. The target sectional pitching moment coefficient (c_m) was set to $-0.2c_l$, as a CDISC best practice for turbulent airfoils. The target pressure distribution is generated to meet the design pressures on the upper surface, while satisfying sectional lift and pitching moment requirements. The lower surface pressure distribution is unconstrained and is allowed to undergo shape changes to simultaneously satisfy the aerodynamics and geometry constraints. The leading-edge radius and airfoil thickness were held fixed to baseline values. In Fig. 11, the design pressures show sufficient convergence to the target pressures after 60 design cycles. Minor changes in airfoil twist and shape were required to meet the target pressures.

C. Slotted Wing Design

The CDISC design process began with performing USM3D-ME simulations to get an initial flow solution for the baseline CSW. The baseline solution was generated at the cruise angle of attack determined for the conventional wing design. This was done to match configuration angle of attack between the conventional and CSW designs and limit differences in fuselage drag. The baseline pressure distributions and geometry for the CSW are shown for reference at station 3 on the inboard wing and at station 8 on the outboard wing in Fig. 12, in addition to the CDISC target pressure distributions and design results.

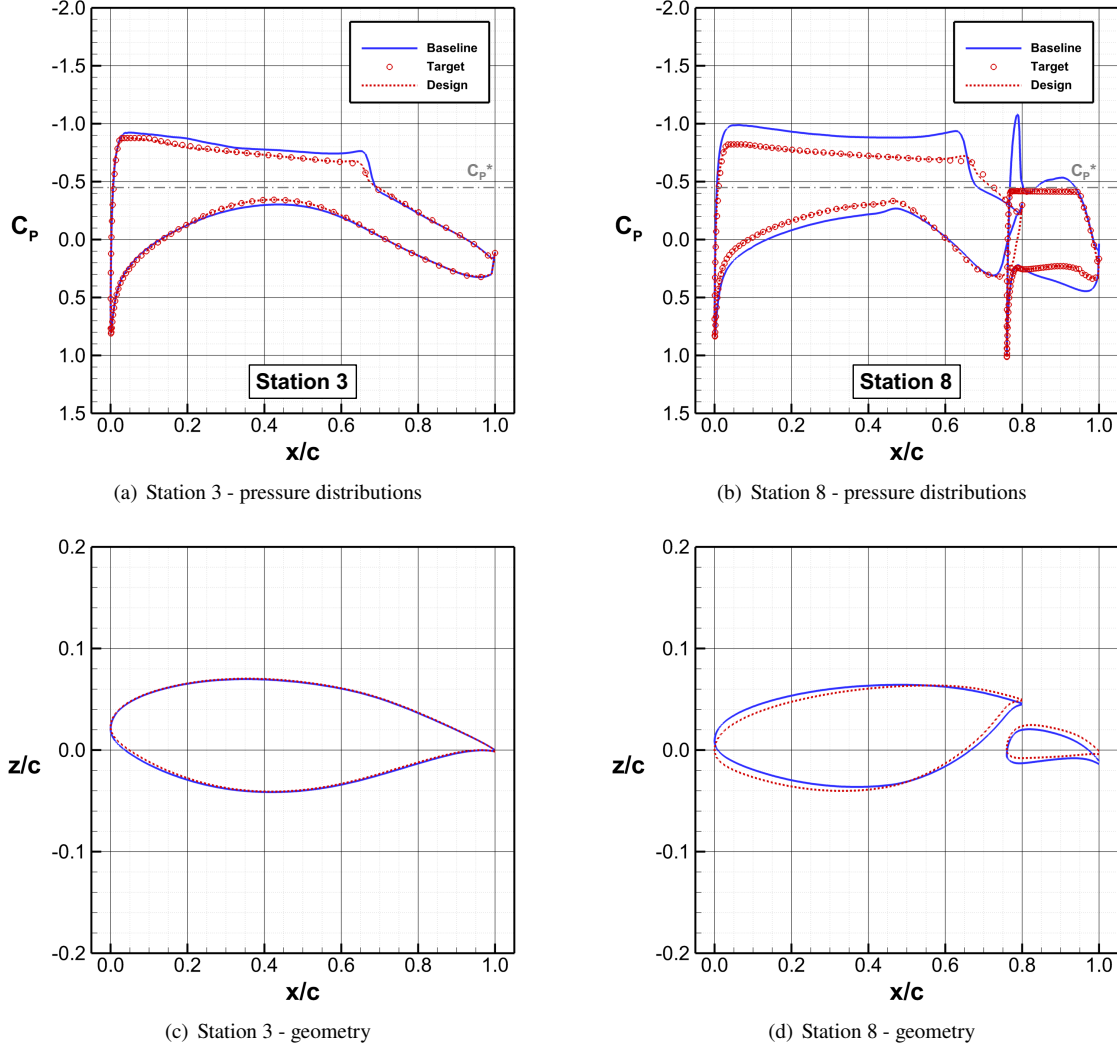


Figure 12. CRM-M8 slotted wing pressure distributions and geometry at stations 3 and 8 for the baseline and design.

The target spanwise lift distribution for the CSW design was matched to the conventional wing design to limit changes in induced drag. The design constraints for the inboard conventional wing design were replicated in the CSW design to isolate drag changes to the outboard wing design. The target pressure distribution for the outboard main element design was created using the target pressure generation constraint, detailed in Section III. Similar to the conventional wing, CDISC best practices were used to control the upper surface initial acceleration and midchord pressure gradient. The benefits of the slot acceleration on the main element pressure recovery allows for the terminating shock location to be moved further aft relative to conventional airfoils. The terminating shock locations on the outboard wing were extended from 60% chord for the conventional wing to 70% chord for the CSW in an attempt to minimize wave and pressure drag. Leading-edge radius and thickness were held constant during the design process.

A second target pressure generation constraint was used to design the flap element for NLF. Due to the relatively low chord Reynolds number of the flap element, concerns for CF growth are limited and TS instabilities can be suppressed with mild adverse pressure gradients. Without concerns for CF growth, a more gradual initial flow acceleration could be tolerated in order to preserve leading-edge radius for low-speed, high-lift considerations. The initial flow acceleration was set to 5% flap chord at each design station. A near-zero midchord pressure gradient was used to suppress TS instabilities while maximizing flap loading. The target NLF region over the upper surface ends at the start of the pressure recovery region, which was set to the baseline pressure recovery location of 80% flap chord. For NLF on the lower surface, a similar pressure distribution was desired with a gradual flow acceleration followed by a near-zero midchord pressure gradient up to the freestream pressure recovery. A curvature constraint was used for the lower surface design to indirectly achieve the desired pressure distribution characteristics while satisfying geometry constraints. This curvature constraint was applied over the midchord region from 15% to 80% flap chord. The baseline slotted wing design variables, including overhang, exit gap, and slot ratio, were held fixed during the design process. Flap deflection was iteratively changed by CDISC during the design to best match upper surface target pressures.

The final aerodynamic constraint used was multielement load balancing, as detailed in Section III. The loading ratio was set to a value of one to evenly balance the sectional lift contributions between the main element and flap toward the total sectional lift requirement. Once the sectional lift coefficient requirements for each element have been defined, the target pressure generation constraint can create target pressure distributions for the main element and flap that satisfy the desired flow characteristics over the upper surfaces of each element while meeting the total sectional lift requirement. The total sectional pitching moment coefficient can be implicitly controlled using the multielement loading ratio. For the current design, a loading ratio of one maximized the aft loading on the flap while keeping the upper surface subsonic. Transonic flow over the flap is undesirable due to the risk of introducing shocks in the slot region, which could lead to adverse pressure gradients on the upper surface of the flap that cause early transition due to TS instability growth.

In Fig. 12, the design pressures show sufficient convergence to the target pressures after 80 design cycles. Small changes in airfoil twist and shape were required to meet the target pressures. In particular, station 3 from the inboard wing section showed minor differences in the pressures and geometry between the baseline and design. This is expected because the conventional wing design was used by the MAKESLOT code to develop the baseline CSW geometry. Additionally, identical inboard wing constraints were used between the two designs to limit differences in drag from the inboard wing. The outboard slotted wing design station shows a main element with a terminating shock location at approximately 70% total chord, followed by a gradual pressure recovery toward the target pressures on the upper surface of the flap. The design pressures for the upper surface of the flap meet the target pressures required to dampen CF and TS instabilities at flight Reynolds numbers. The design pressures for the lower surface of the flap feature a rapid initial acceleration followed by a slightly favorable midchord gradient up to the freestream pressure recovery region. This result confirms the success of using a curvature constraint to implicitly achieve the target pressure characteristics for NLF over the lower surface of the flap.

The CSW design was completed in two phases. First, fully turbulent USM3D-ME simulations were used for the first 60 design cycles to more rapidly design the CSW for general agreement with the target pressure distributions. Once the turbulent design phase was complete, forced laminarization simulations were used to model the estimated extents of NLF on the flap upper and lower surface. Twenty additional design cycles were performed using forced laminarization simulations with estimated transition locations of 70% and 80% flap chord for the upper and lower flap surfaces, respectively. This second design phase is necessary for redesigning the flap shape to meet the target pressure distribution while accounting for the decambering effect of the laminar boundary layers.

Once design was completed, the upper and lower surfaces (US/LS) of the flap element were analyzed using the transition prediction software to confirm the targeted NLF extents. The CF and TS N-factor (NF) growth plots for stations 6 and 12 are shown in Fig. 13. These two stations represent the most inboard and outboard design stations of the flap element and are representative of the spanwise characteristics. Across the span, CF is easily suppressed on both the upper and lower surfaces of the flap with CF N-Factor reaching a peak value of 3 across the span. This may be attributed to the relatively low chord Reynolds number, less than five million, and leading-edge sweep, 20°, of the flap element. TS growth is also suppressed up to the pressure recovery region over the last 20% of the flap chord. A peak TS NF value of six was reached across the span of the flap. Assuming a critical N-factor (NF^*) of nine, the flap element can achieve nearly full-chord extents of NLF. Given the observed suppression of CF and TS instabilities, the flap is expected to support NLF for critical N-factors as low as six at the cruise condition of interest. Figure 14 illustrates the predicted transition fronts on the upper and lower surfaces of the flap element for the final CSW design. NLF was achieved over 71% of the flap upper surface and 77% of the flap lower surface.

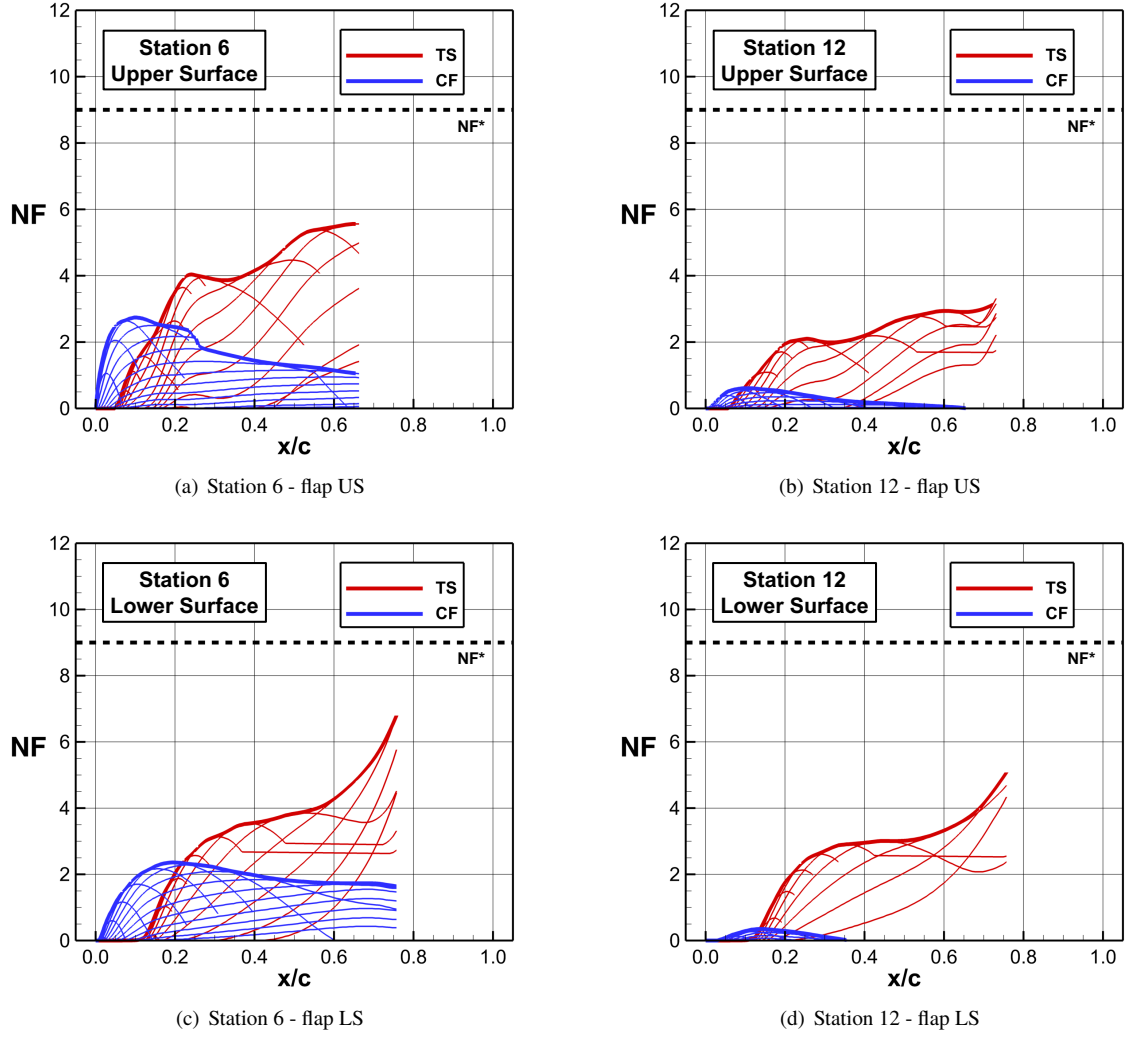


Figure 13. TS/CF instability growth for the flap element upper and lower surfaces at design stations 6 and 12 for the CRM-M8 slotted wing design.

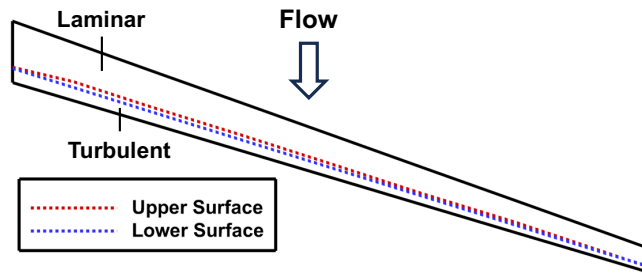


Figure 14. Transition fronts on upper and lower surface of flap element for the CRM-M8 slotted wing design.

VII. Design Results

This section will present the cruise and off-design performance comparisons between the conventional and partial-span, slotted wing designs. The slotted wing design will include both laminar and fully turbulent (loss of laminar flow) results for comparisons to the conventional wing design. Comparisons of the pressure distributions, skin-friction distributions, and airfoil geometry are provided at the reference design stations to highlight semispan differences between the wing designs. Spanwise aerodynamic and geometric constraints are summarized. A drag coefficient decomposition is provided to compare the total, pressure, and viscous drag contributions from each airframe component. Near-cruise, off-design analyses, including drag polar and drag rise simulations, were used to assess CSW performance and NLF sensitivity to perturbations in angle of attack or Mach number.

A. Cruise Performance

Figure 15 shows USM3D-ME predictions for pressure coefficient along the wing for the CRM-M8 conventional and slotted wing designs at the cruise condition of $M_\infty = 0.8$, $Re_{\text{cref}} = 21.1$ million, and $C_L = 0.543$. The dark red regions are consistent with regions of supersonic flow, where the critical pressure coefficient is approximately -0.44. The two configurations show consistent pressure contours along the inboard wing (stations 1-4), suggesting that the design strategy was effective in isolating drag differences due to the outboard wing. Over the outboard wing region (stations 5-13), the conventional wing shows smooth pressure isobars with the effective aerodynamic sweep closely matching the geometric sweep. The terminating shock on the upper surface is located at approximately 60% total chord. For the CSW design, it was possible to extend the terminating shock location aft to 70% chord. Within the pressure contours, this effect is observed on the main element with an expanded, but weakened region of supersonic flow. Additionally, the flap element shows the expected increase in flow velocities compared to the conventional wing over the same chordwise region due to the increase in aft loading enabled by the CSW.

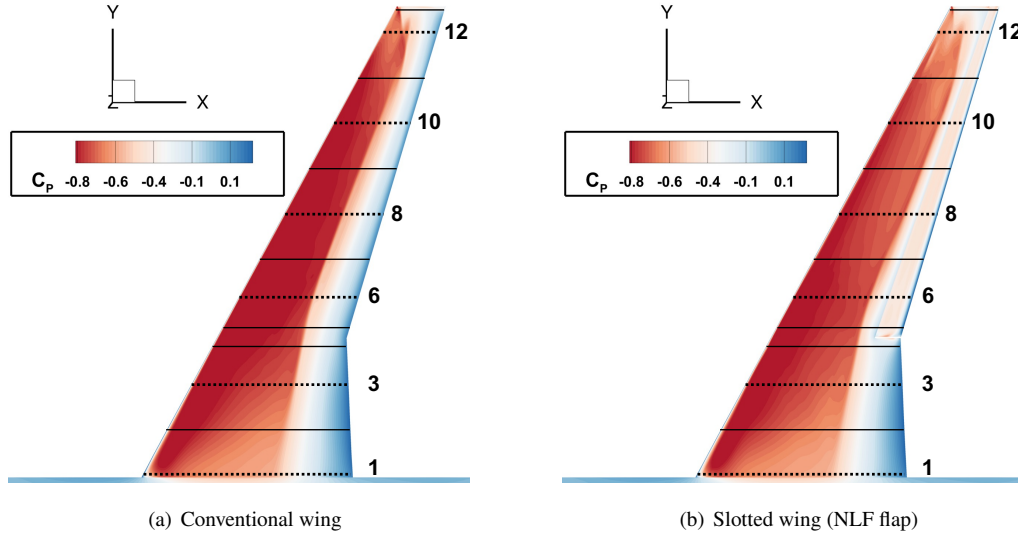


Figure 15. Pressure coefficient contour data for the CRM-M8 conventional and slotted wing designs.

An example of the flow characteristics over the outboard wing section is provided in Fig. 16, showing Mach contour data at design station 8 for the conventional and slotted wing sections. The supersonic "bubble" above the upper surface of the main element shows an expected shift in the aft chord direction relative to the conventional wing airfoil with a decrease in the off-body, vertical extent of supersonic flow. The pressure contours in the vicinity of the cruise slotted airfoil flap confirm the desired increase in aft loading compared to the conventional airfoil. The flow immediately above the upper surface of the flap remains subcritical, and no supersonic flow is observed through the slot. Additionally, the CDISC best practice for slot exit gap permits the main element wake to traverse above the upper surface of the flap without impinging on the boundary layer. This design features allows for NLF to be achieved over flap upper surface without introducing secondary shocks that could lead to early transition or degraded flap performance.

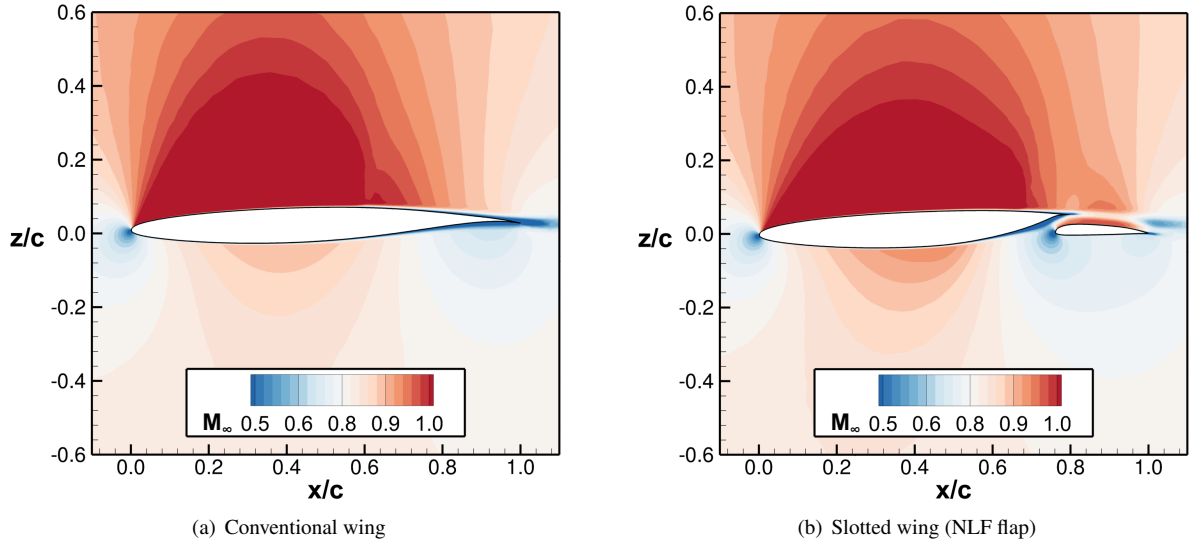


Figure 16. Mach contour data at station 8 for the CRM-M8 conventional and slotted wing designs.

Figure 17 compares USM3D-ME predictions for the x-component of skin-friction coefficient along the upper surface between the conventional and slotted wing designs. This skin-friction data are useful for identifying regions at risk of flow separation (value approaches zero due to flow reversal) or for confirming laminar flow regions (blue). The skin-friction data shows consistent behavior over the inboard wing section between the two wing designs. The main element of the CSW shows an aft shift in the skin-friction front over the upper surface due to a weaker and more aft shock compared to the conventional wing. The flap element shows reduced skin-friction values over the upper surface compared to the conventional wing over the same chordwise region. These reduced skin-friction values confirm that the intended extents of NLF on the upper and lower (not shown) surfaces of the flap were correctly simulated using USM3D-ME forced laminarization simulations.

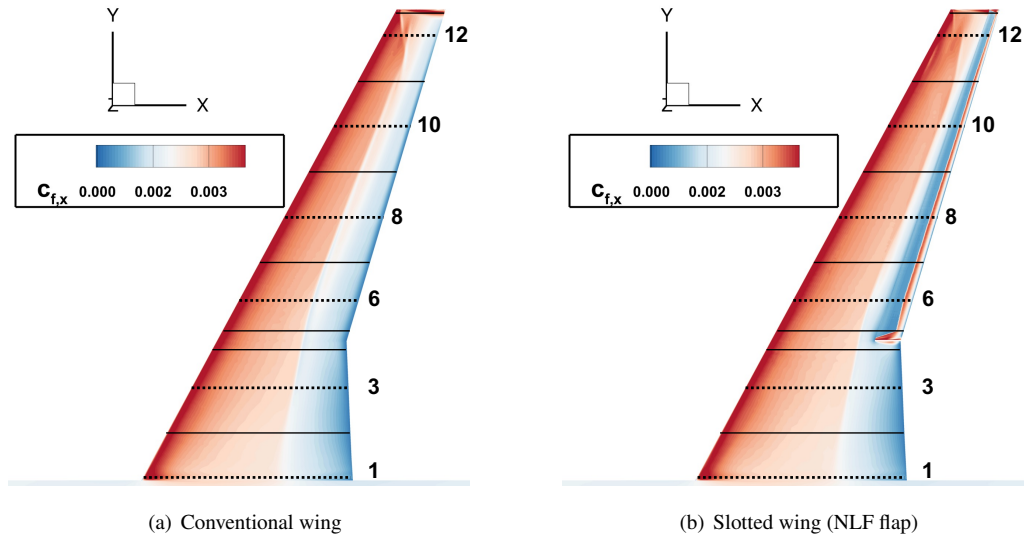


Figure 17. Skin-friction coefficient contour data for the CRM-M8 conventional and slotted wing designs.

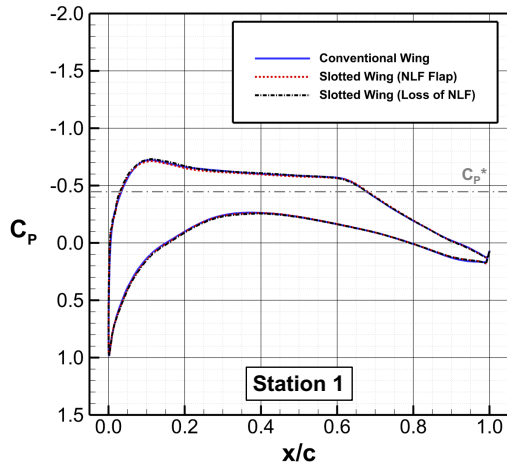
The pressure distributions, skin-friction distributions, and geometry for the conventional and partial-span, CSW designs are provided in Figs. 18-20 to highlight the key design feature differences between the two wings. Simulations for the CSW design includes forced laminarization predictions to model the aerodynamic performance benefits of a laminar CSW flap, in addition to fully turbulent simulations to assess performance in the case of a loss in NLF over the flap element upper and lower surfaces. The conventional wing design results are fully turbulent and are representative of a modern supercritical wing design.

Figure 18 shows the noted data at stations 1 and 3 along the inboard wing. The pressure distributions and geometry for the conventional and partial-span, CSW designs show favorable agreement as intended due to the use of identical design constraints at the inboard design stations. This was done to isolate cruise drag differences to the differences in outboard wing design. The skin-friction distributions show good agreement between the three analyses with limited inboard wing effects due to having NLF on only the outboard flap element.

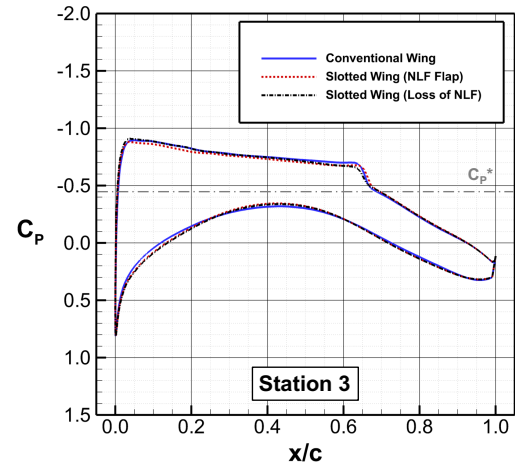
Figure 19 compares the same data at stations 6 and 8, which are positioned near the semispan midpoint on the outboard wing, as shown in Fig. 17. The ALE constraint, detailed in Section III, was used near station 6 to blend the leading-edge geometry near the planform break to account for abrupt differences in required twist between the two wing sections. As a result, the analysis pressures show that the slotted wing was capable of positioning the shock only 2.5% further aft than the conventional wing with a minor decrease in average surface velocities over the upper surface of the main element. Design station 8 is located further outboard and provides a good representation of the pressure differences between conventional and slotted wing designs in the absence of wingtip effects. At station 8, the slotted wing is able to generate enough aft loading to weaken the shock with a terminating shock location approximately 10% further aft than the conventional wing. The skin-friction distributions at stations 6 and 8 show the relative differences between the conventional and slotted wing as well as the impact of loss of NLF on the flap element. Relative to the conventional wing, the slotted wing with loss of NLF shows increased skin-friction drag over the flap region. This can be attributed to the relatively low chord Reynolds number of the flap and the formation of a new turbulent boundary layer. Using the ALMA concept, the flap shows a targeted reduction in skin-friction drag relative to conventional wing values. Comparisons of the conventional and slotted wing geometry at station 6 show limited changes in airfoil twist due to application of the ALE constraint. At station 8, the cruise slotted airfoil has increased total twist relative to the conventional airfoil due to flap deflection. The flap is deflected to meet the target sectional loading, which permits decreased loading over the main element and a decrease in main element incidence relative to the conventional wing.

Figure 20 shows similar design trends at outboard wing design stations 10 and 12 with the exception of wingtip effects at the most outboard design station. Toward the wingtip, the shock along the upper surface of the main element tends to unsweep, resulting in a forward shift in the terminating shock location. In general, the sectional pressure distributions along the wing confirmed that the inboard wing performance was matched between the two wing designs and that the cruise slotted airfoil was successfully designed to generate increased aft loading and a perceived reduction in shock strength. Additionally, natural laminar flow is achieved over a significant extent of the flap surface area over the entire outboard wing section.

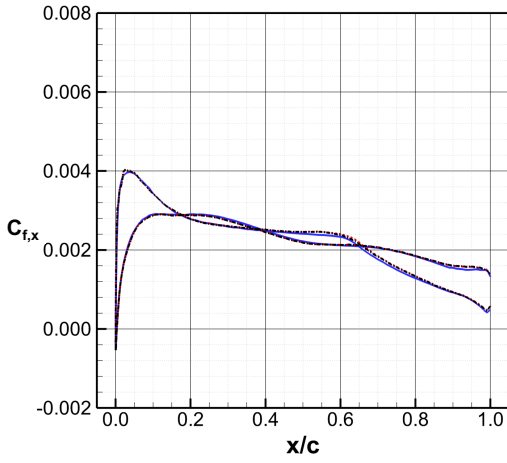
The spanwise characteristics of the conventional and partial-span, CSW designs for the CRM-M8 configuration are shown in Figure 21. The sectional lift distribution was held relatively constant to limit changes in induced drag or root bending moment between the two wing designs. The sectional pitching moment shows an expected decrease over the outboard wing section when using a cruise slotted airfoil due to the capability for increased aft loading. Increased aft loading permits a terminating shock location that is approximately 10% further aft over the outboard wing region, which provides approximately a 0.04 reduction in normal-shock Mach number. This mild reduction in shock strength over the outboard wing suggests that the opportunity for CSW pressure drag savings is lower at flight conditions representative of single-aisle, transonic commercial transport aircraft, due to the relatively weak shock strength that can be achieved using conventional supercritical airfoils. The leading-edge radius of the primary wing component was unconstrained with minor differences observed along the semispan between the two designs. As previously noted, the total twist is greater for the CSW over the outboard wing region due to required increases in flap deflection for the prescribed multielement loading ratio. Figure 22 illustrates the spanwise thickness distributions for the conventional and slotted wing components. Over the inboard wing, nondimensional airfoil thickness is matched between the two wing designs. For the outboard wing, the flap was held fixed to 13.5%, whereas the main element nondimensional thickness was scaled from the conventional wing using local chord in an attempt to match dimensional thickness. As discussed in Section I, wing thickness is an alternative CSW design trade that has a direct impact on baseline cruise drag for a given lift requirement. For this reason, the dimensional thickness distribution was matched between the conventional wing and the main element of the CSW. In doing so, the drag reduction benefits of CSW technology could be assessed for a fixed wing planform at fixed cruise conditions.



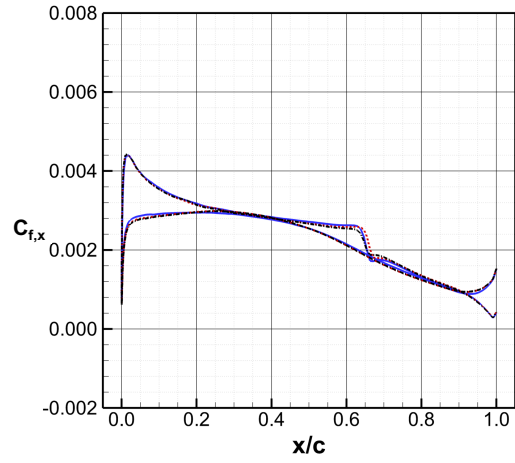
(a) Station 1 - pressure distributions



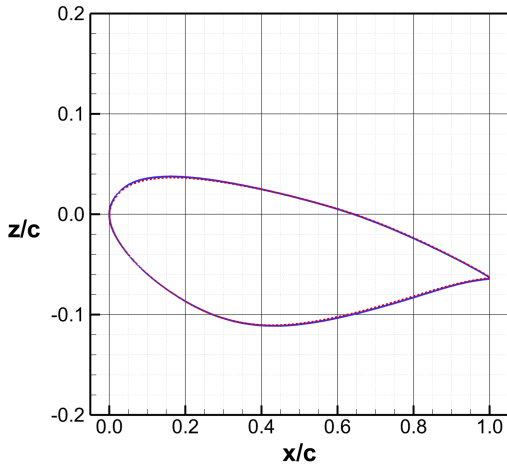
(b) Station 3 - pressure distributions



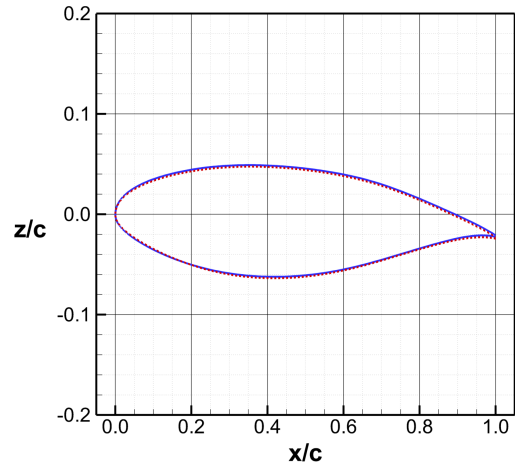
(c) Station 1 - skin-friction distributions



(d) Station 3 - skin-friction distributions

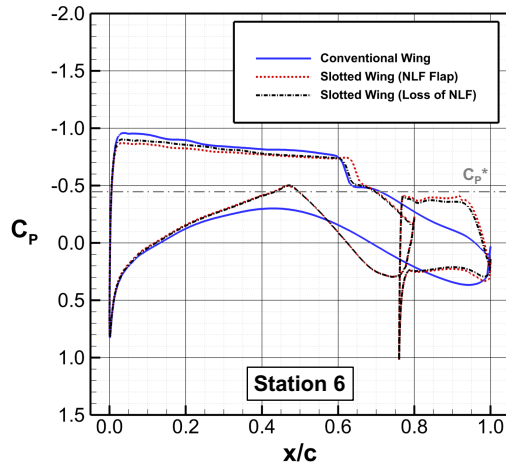


(e) Station 1 - geometry

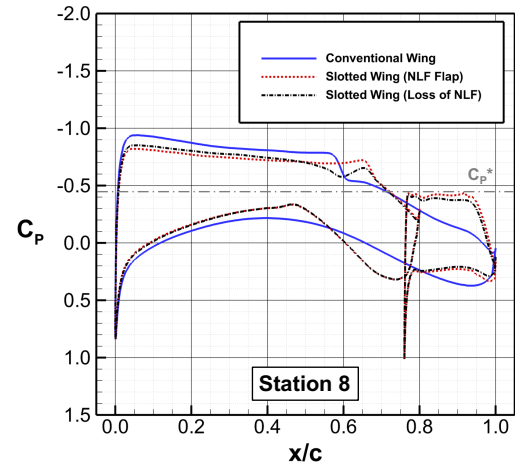


(f) Station 3 - geometry

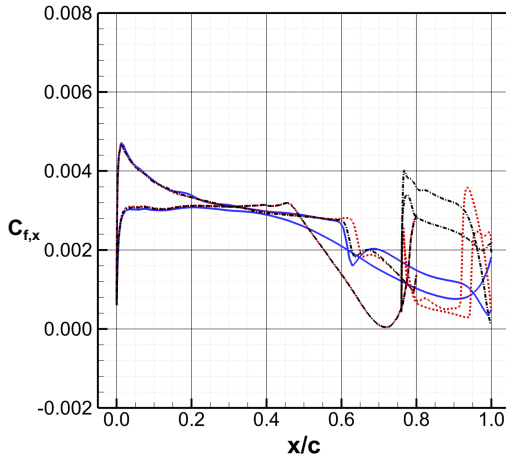
Figure 18. Pressure distributions, skin-friction distributions, and geometry at design stations 1 and 3 for the CRM-M8 conventional and slotted wing designs.



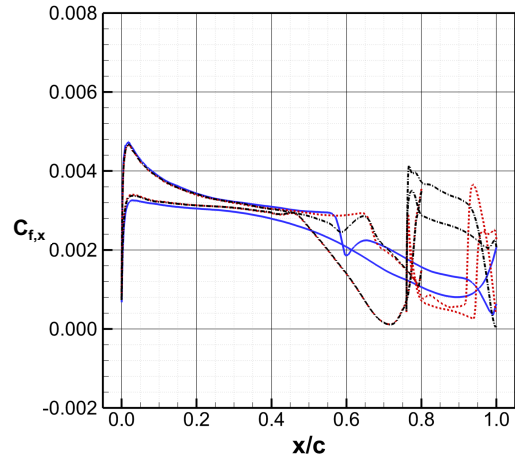
(a) Station 6 - pressure distributions



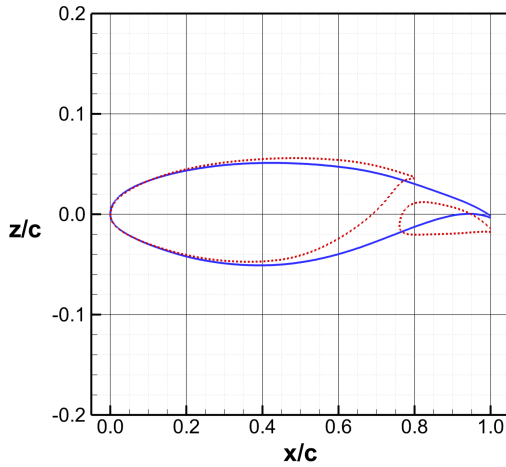
(b) Station 8 - pressure distributions



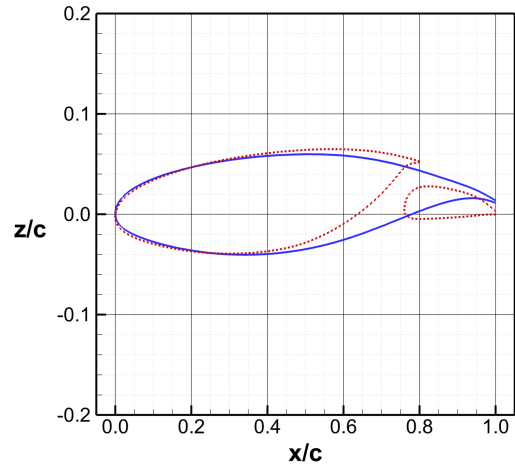
(c) Station 6 - skin-friction distributions



(d) Station 8 - skin-friction distributions

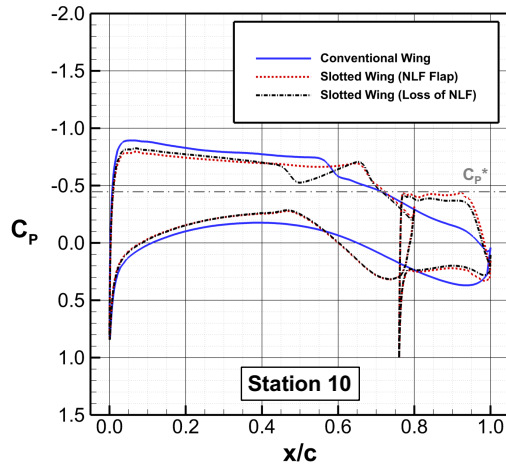


(e) Station 6 - geometry

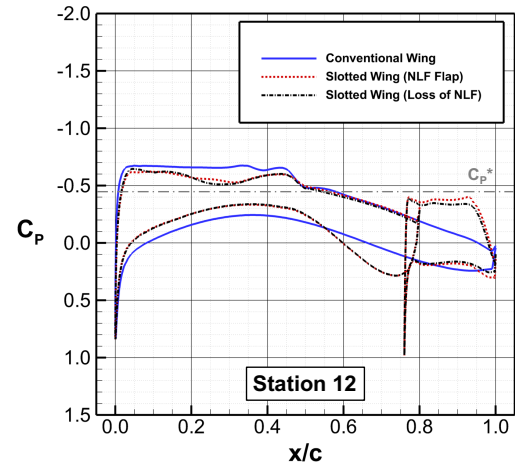


(f) Station 8 - geometry

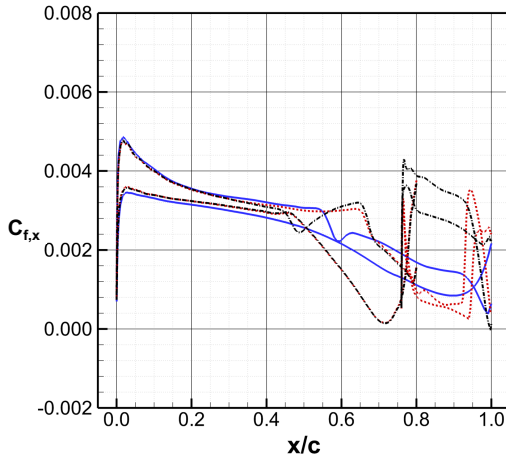
Figure 19. Pressure distributions, skin-friction distributions, and geometry at design stations 6 and 8 for the CRM-M8 conventional and slotted wing designs.



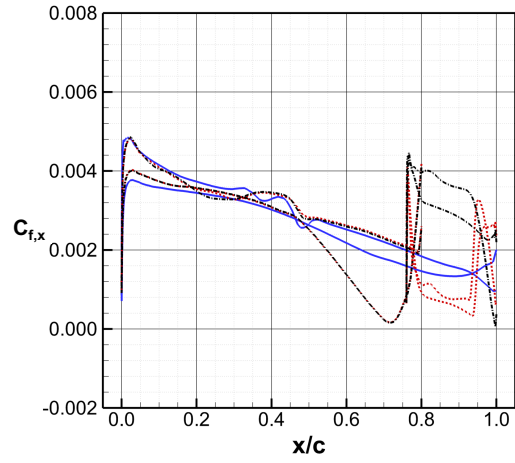
(a) Station 10 - pressure distributions



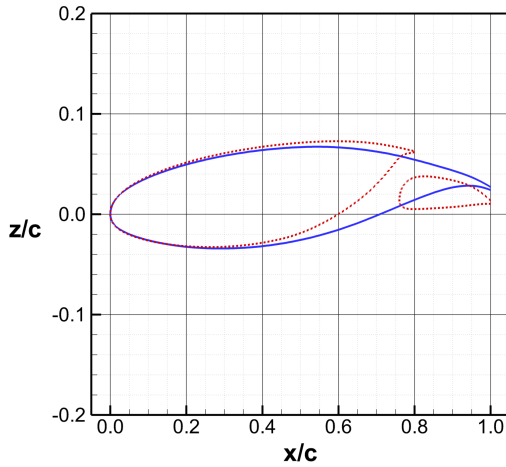
(b) Station 12 - pressure distributions



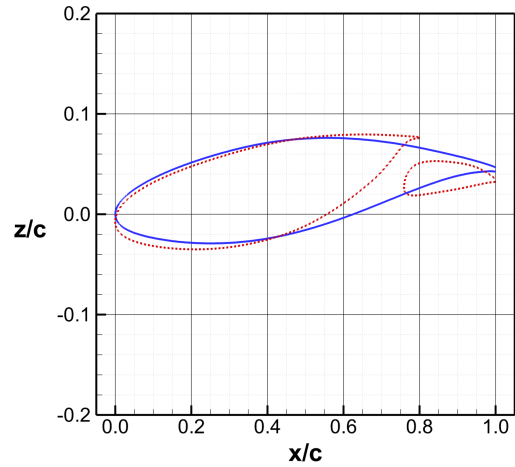
(c) Station 10 - skin-friction distributions



(d) Station 12 - skin-friction distributions

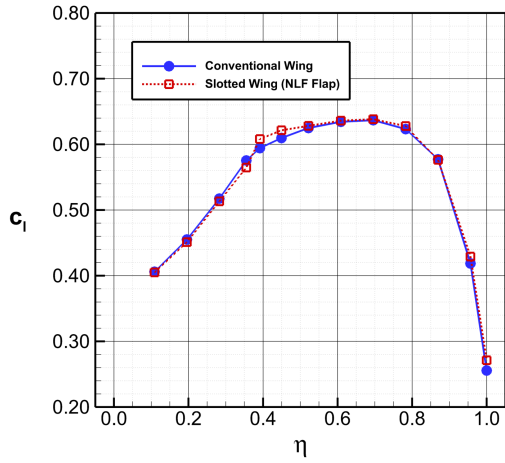


(e) Station 10 - geometry

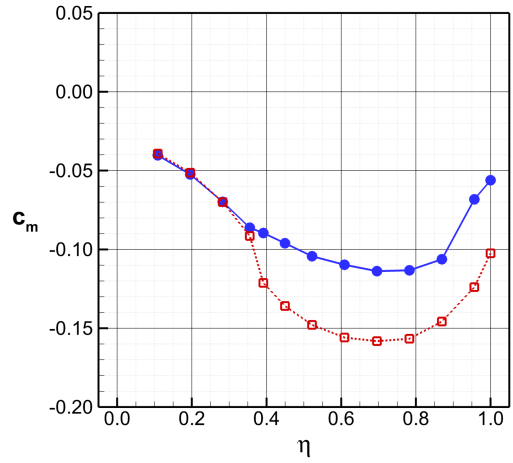


(f) Station 12 - geometry

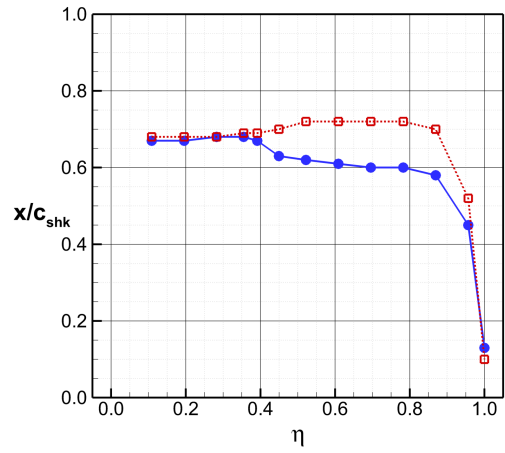
Figure 20. Pressure distributions, skin-friction distributions, and geometry at design stations 10 and 12 for the CRM-M8 conventional and slotted wing designs.



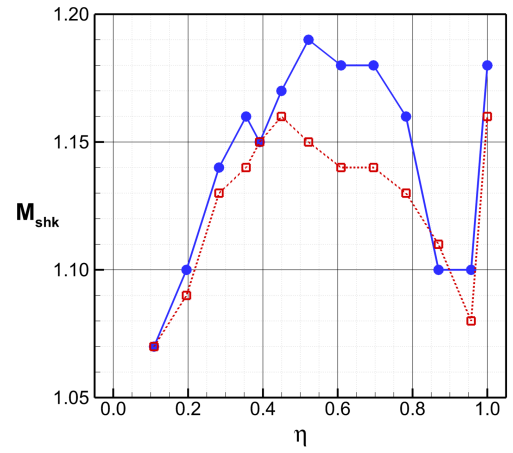
(a) Sectional lift coefficient



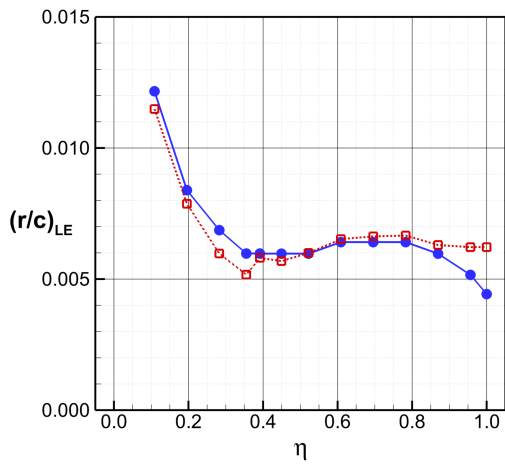
(b) Sectional pitching moment coefficient



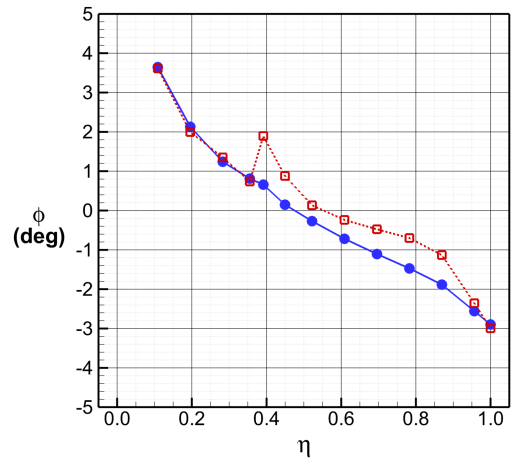
(c) Shock location



(d) Shock Mach number



(e) Leading-edge radius



(f) Twist

Figure 21. Spanwise distributions of sectional lift coefficient, sectional pitching moment coefficient, shock location, shock Mach number, leading-edge radius, and twist for the CRM-M8 conventional and slotted wing designs.

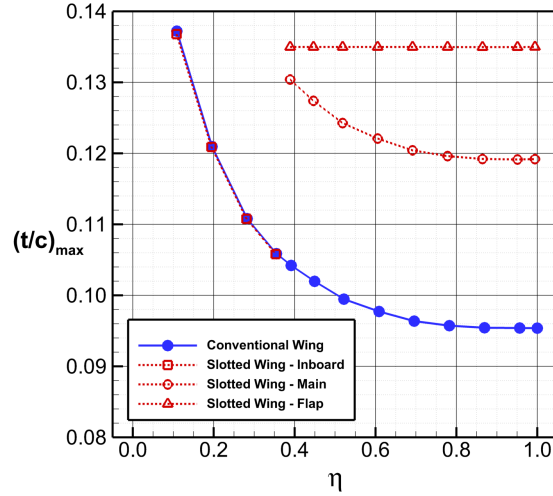


Figure 22. Spanwise thickness distributions for the CRM-M8 conventional and slotted wing designs.

The force and moment data from the USM3D-ME flow solver were used to quantify observed performance differences between the conventional and partial-span, CSW designs at the CRM-M8 cruise condition of $M_\infty = 0.8$, $Re_{\text{cref}} = 21.1$ million, and $C_L = 0.543$. Table 5 compares the aerodynamic performance between the conventional wing, the CSW with the NLF flap, and the CSW at turbulent (loss of NLF) conditions. The configuration angle of attack between the conventional wing and slotted wing (NLF flap) differed by 0.1 deg. This small difference was achieved by matching the target spanwise load distribution between the two designs, creating a common inboard wing section, and initializing the CSW design at the design angle of attack from the conventional wing design. Angle of attack was limited to minimize differences in fuselage and inboard wing contributions to total drag between the two designs, effectively isolating cruise drag changes to the outboard wing design.

Table 5. Total force and moment coefficient data for the CRM-M8 conventional and slotted wing designs at the cruise condition of $M_\infty = 0.8$, Alt. = 40,000 ft, $Re_{\text{cref}} = 21.1$ million, and $C_L = 0.543$.

Model	α (deg)	C_L	C_D	$C_{D,v}$	$C_{D,p}$	C_m	L/D
Conventional Wing	2.1	0.543	0.02382	0.00941	0.01441	0.017	22.8
Slotted Wing (NLF Flap)	2.2	0.543	0.02393	0.00946	0.01447	0.002	22.7
Slotted Wing (Loss of NLF)	2.3	0.543	0.02445	0.00980	0.01466	0.015	22.2

Lift-matching simulations were conducted for each model to match the total cruise lift coefficient, $C_L = 0.543$, within a tolerance of approximately $\pm 0.1\%$. The total drag coefficient, C_D , for the conventional wing was approximately 238 drag counts ($\Delta C_D = 0.0001$). Despite the benefits of NLF, the CSW design with an NLF flap had a one-count penalty in total drag coefficient relative to the conventional wing. For turbulent flow conditions, the CSW total drag coefficient penalty increases from one to seven counts. The viscous drag ($C_{D,v}$) and pressure drag ($C_{D,p}$) contributions to C_D are also provided. The one-count total drag penalty for the CSW design with an NLF flap can be attributed to minor increases in viscous and pressure drag. In the current design, the main element shock was not weakened enough to achieve decreases in pressure drag. At off-design conditions, the loss of NLF leads to a three-count viscous drag penalty, in addition to a two-count pressure drag penalty due to boundary layer effects on the main element shock (see Fig. 19). The C_m for the conventional wing design was 0.017 in the absence of a horizontal tail component to trim the model. The increased aft loading of the slotted wing design (with an NLF flap) led to a 0.015 decrease (nose-down) in C_m . At turbulent flow conditions, C_m for the CSW increases (nose-up) due to turbulent boundary layer effects on main element shock position and flap loading. The lift-to-drag ratio, L/D, was just under 23 for the conventional wing and CSW with an NLF flap. At cruise conditions representative of single-aisle transonic transports, the partial-span, CSW design was not successful in reducing pressure drag levels or increasing aerodynamic efficiency relative to a conventional supercritical wing. For aircraft traveling at cruise speeds greater than Mach 0.8, the potential pressure-drag savings of CSW technology may be increased due to stronger shocks.

A cruise drag decomposition is provided in Fig. 23 to assess the individual drag contributions of the fuselage, inboard wing, and outboard wing between the conventional and partial-span CSW design. These component drag contributions are separated into total, viscous, and pressure drag coefficients. The total drag contributions show that the general design strategy was successful in matching the fuselage and inboard wing drag between the two designs in order to isolate total drag differences to the outboard wing design. Another key observation is that the inboard wing contribution to total drag was approximately 50% compared to 10% for the outboard wing for all configurations. While viscous drag contributions are fairly similar between the inboard and outboard wing, the pressure drag contributions are significantly different. The inboard wing produces a pressure drag penalty of over 100 drag counts, whereas the outboard wing has a negligible contribution. To better understand the differences in pressure drag contributions between the inboard and outboard wing sections, the pressure and skin-friction distributions at each design station were integrated to show how pressure and viscous drag change with wing semispan.

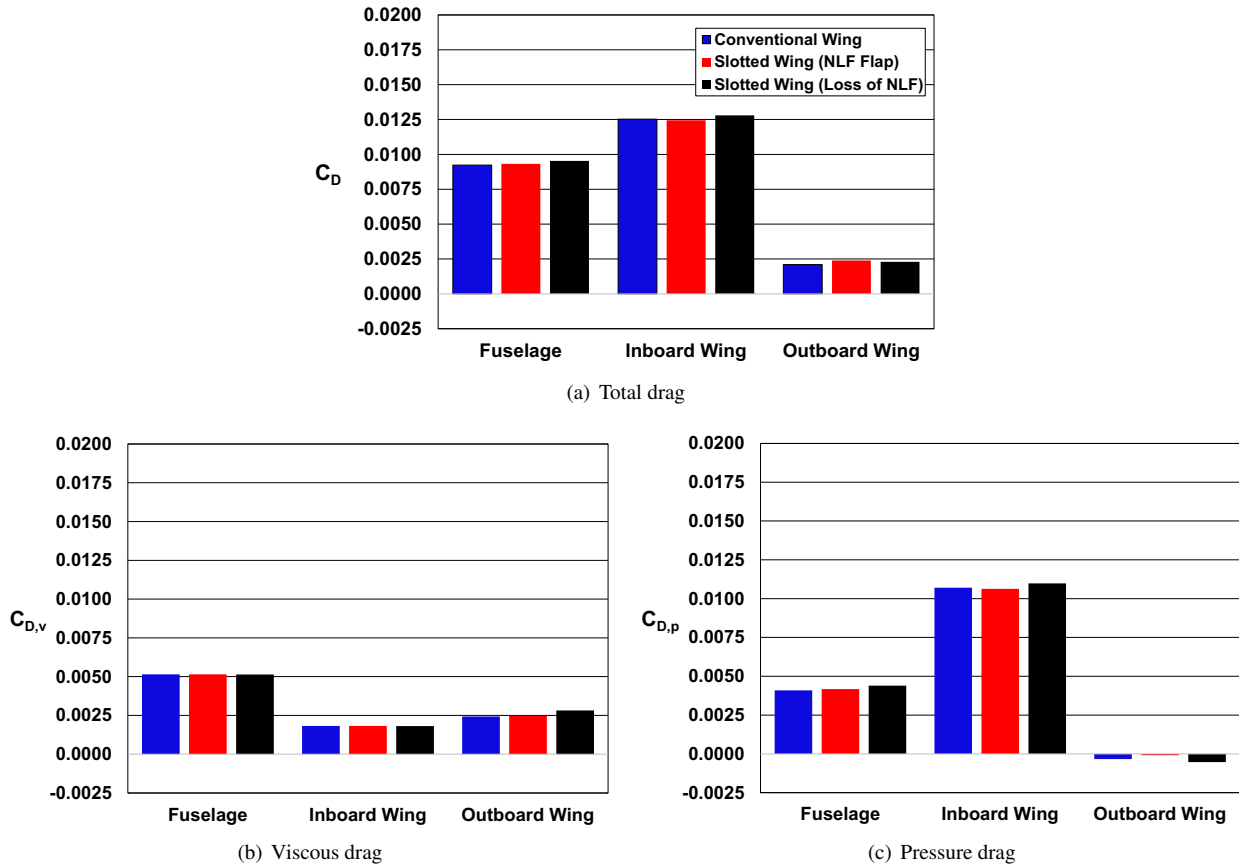


Figure 23. Component contributions to total, viscous, and pressure drag at cruise for the CRM-M8 conventional and slotted wing designs.

Figure 24 shows the spanwise distributions of sectional pressure and skin-friction drag for the CRM-M8 conventional and slotted wing designs. For fully turbulent flow conditions, the CSW skin-friction levels are greater over the outboard wing section compared to the conventional wing. As previously shown in Figs. 18-20, this increased skin-friction can be attributed to the formation of a new turbulent boundary layer with increased skin-friction relative to the conventional wing over the same chordwise extent. By designing the slotted wing with an NLF flap using the ALMA concept [9], this multielement skin-friction penalty can be offset to drag levels comparable to the conventional wing. The spanwise distribution in pressure drag shows a linear decrease in pressure drag from root to tip. The pressure drag increases substantially near the root, supporting the previously noted differences in integrated pressure drag contributions between the inboard and outboard wing. The relative differences in pressure drag along the outboard wing region between the designs are negligible in comparison to the total spanwise trend. Insights regarding the spanwise difference in sectional pressure drag can be made using spanwise comparisons of the airfoil geometry and pressures.

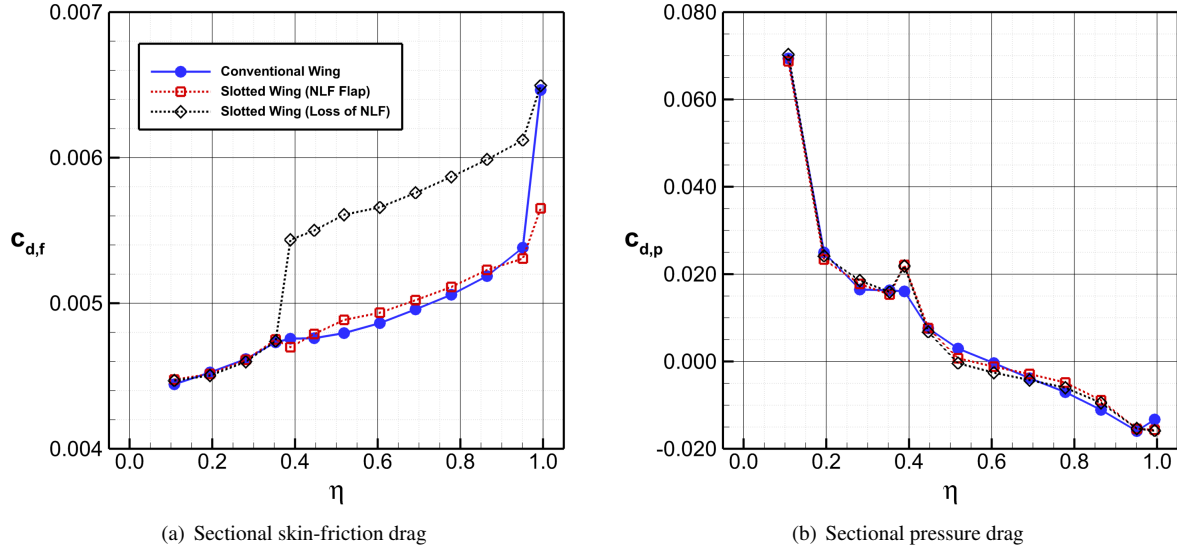


Figure 24. Spanwise distributions of sectional skin-friction and pressure drag coefficients for the CRM-M8 conventional and slotted wing designs.

Figure 25 provides an example of the spanwise variation in geometry and design pressures for the CRM-M8 conventional wing design. The airfoil twist is noticeably decreased from station 1 near the root toward station 12 on the outboard wing. In particular, rapid changes in twist are observed between stations 1 and 3 due to a nonlinear spanwise twist distribution, see Fig. 21. As twist increases, the suction surface becomes more oriented in the drag direction, such that pressure drag due to lift increases. In addition to twist, the airfoil thickness grows inboard to structurally support the wing root bending moment and to accommodate fuel volume. The leading-edge radius also increases with thickness toward the wing root. A relatively large leading-edge radius inboard is generally preferred for favorable low-speed, high-lift performance. The unique combination of thickness, leading-edge radius, and twist at the root provides a more gradual flow acceleration near the leading edge. As a result, the initial flow acceleration terminates near the root airfoil crest, and a greater portion of the loading is oriented in the drag direction. The spanwise variations in pressure drag due to multidisciplinary geometric constraints exceed the magnitude of pressure drag savings offered by the CSW technology at cruise conditions representative of single-aisle, transonic transport aircraft.

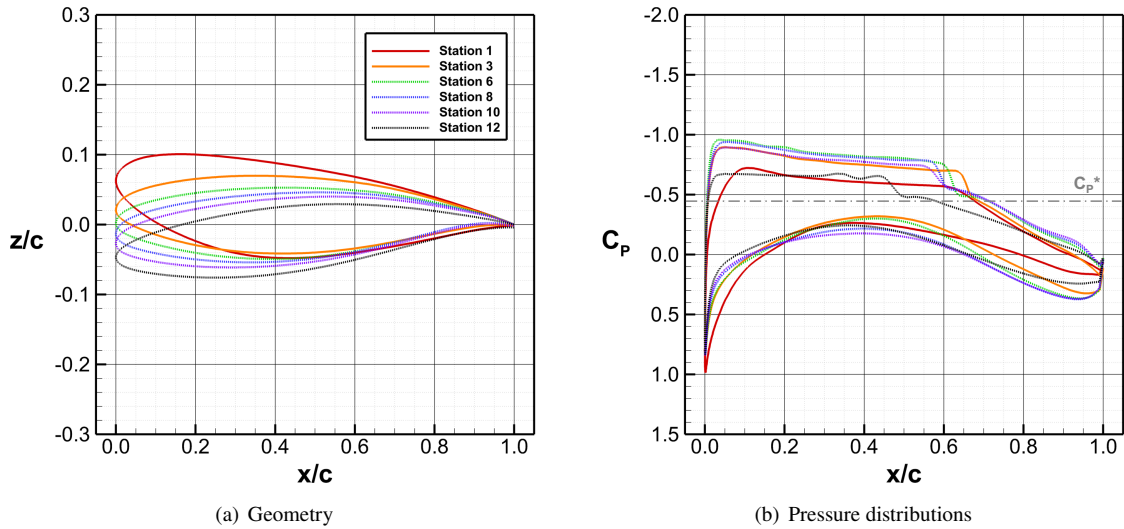


Figure 25. Spanwise geometry and pressure distribution variation for the CRM-M8 conventional wing.

B. Off-Design Performance

It is important to assess the near-cruise, off-design performance of the CSW to quantify cruise drag and NLF sensitivity to perturbations in flight conditions. Drag polar and drag rise analyses were performed for the conventional wing, the CSW with an NLF flap, and the CSW at turbulent flow conditions where loss of NLF is encountered. A loss in laminar flow could be expected at off-design flight conditions that cause pressure changes that lead to early transition due to CF/TS, or due to contaminants, e.g., frost, insects, etc., that cause bypass transition. Aerodynamic performance differences are assessed using pressure data. Transition predictions were used to estimate the sensitivity of NLF transition fronts on the flap due to changes in lift coefficient and Mach number.

1. Drag Polar

A drag polar was generated at the cruise Mach number of 0.8 for the conventional and slotted wing designs as illustrated in Fig. 26. An angle-of-attack range from 0 to 3.5 degrees with 0.5 deg increments was used to sample a C_L range of 0.2 to 0.7. Figure labels highlight the $\pm 10\%$ $C_{L,cruise}$ range, which corresponds to the conditions most likely encountered during cruise. Another relevant off-design condition of interest is buffet, which is often evaluated at 1.3 times the cruise lift coefficient. Across the $\pm 10\%$ $C_{L,cruise}$ range, the CSW with an NLF flap and conventional wing showed comparable drag levels. For turbulent conditions, the CSW incurs a six-count cruise drag penalty through the primary lift coefficient range due to loss of NLF on the flap. As lift coefficient increases toward the buffet criteria, the CSW design shows a more gradual increase in drag relative to the conventional wing.

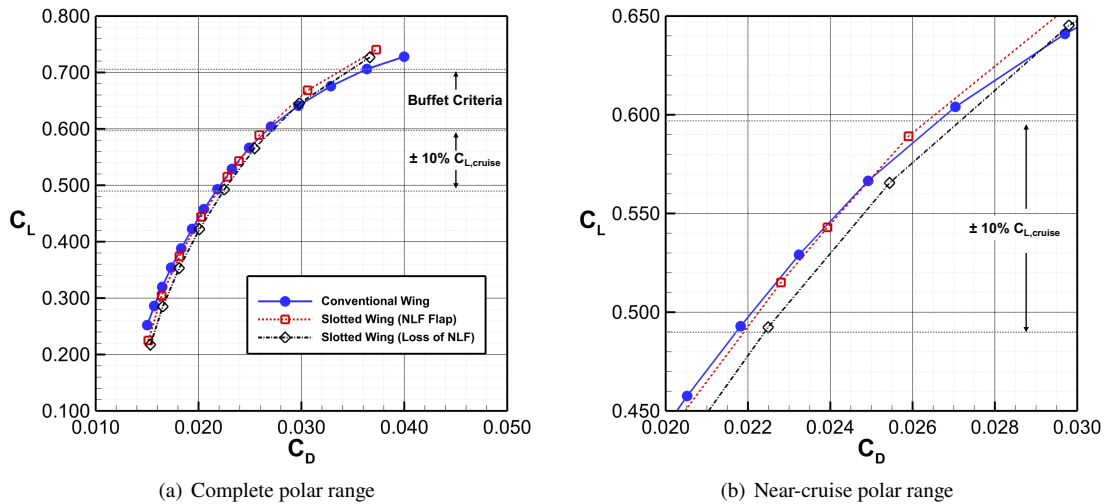
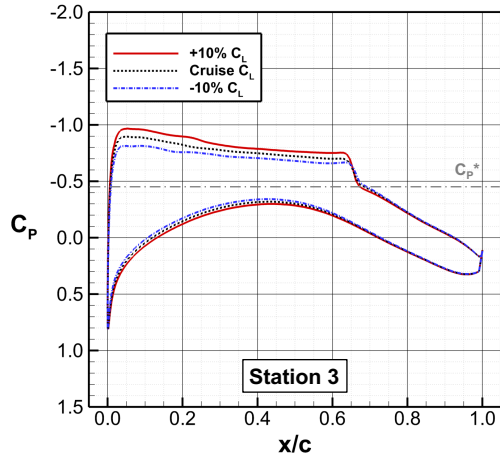


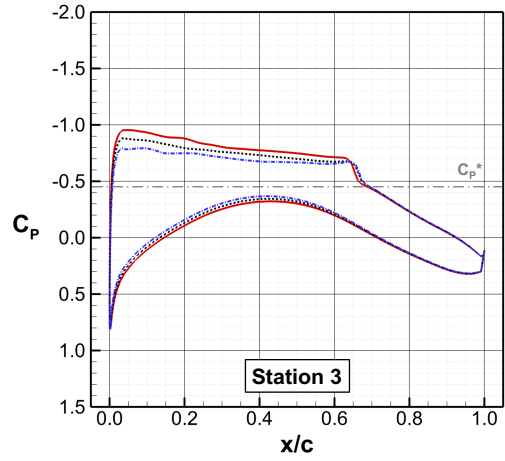
Figure 26. Near-cruise drag polar comparing the CRM-M8 conventional and slotted wing designs over the complete and near-cruise ranges.

Figure 27 shows the pressure distributions for the conventional and slotted wing designs at cruise and $\pm 10\%$ $C_{L,cruise}$. At station 3 on the inboard wing, both designs show the expected increase in forward loading due to an increase in angle of attack with lift. At station 6 on the outboard wing, similar pressure distribution changes are observed for the conventional wing and main element. In contrast, the pressure distributions for the flap remain insensitive to changes in angle of attack and lift coefficient. Further outboard at station 10, the conventional wing and main element show an increase in shock strength, more so for the conventional wing, along with a forward translation of the shock location due to wingtip stall effects. Despite these effects, the flap loading does not change with angle of attack.

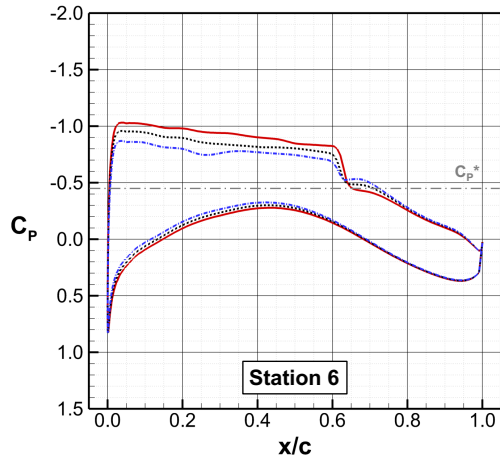
Transition predictions were used to quantify the sensitivity of the NLF transition fronts to perturbations in lift coefficient, or angle of attack. Figure 28 shows the transition fronts predicted for the upper and lower surfaces of the flap at $C_{L,cruise}$ and $\pm 10\%$ $C_{L,cruise}$. Because the flap pressure distributions showed negligible qualitative differences with changes in lift coefficient, the laminar flow area remains relatively constant. At cruise, approximately 71% and 77% of the upper and lower surface area, respectively, achieves natural laminar flow. Over the $\pm 10\%$ $C_{L,cruise}$ range, these NLF surface area extents are reduced by less than 3%, confirming the ability of the flap to sustain NLF with limited sensitivity to near-cruise perturbations in angle of attack.



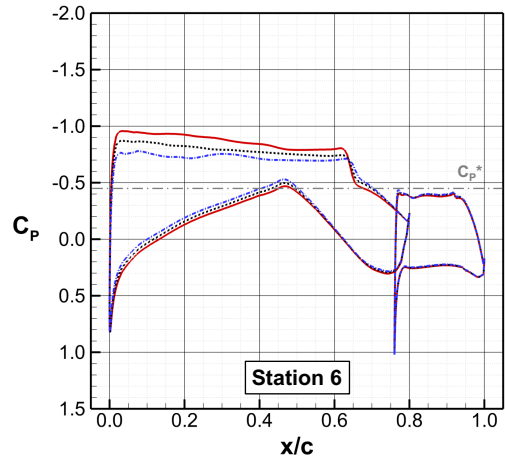
(a) Conventional wing - station 3



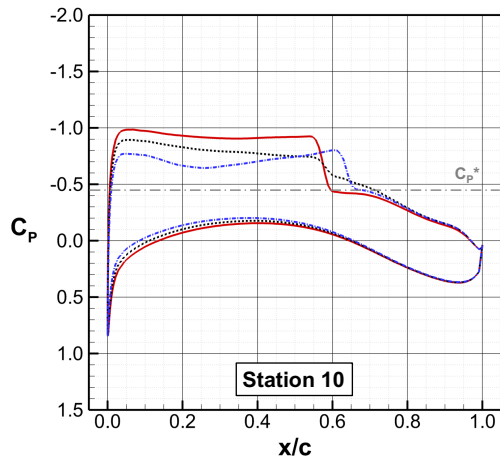
(b) Slotted wing (NLF flap) - station 3



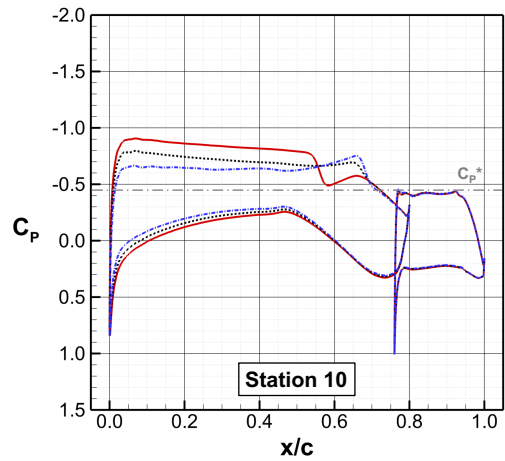
(c) Conventional wing - station 6



(d) Slotted wing (NLF flap) - station 6



(e) Conventional wing - station 10



(f) Slotted wing (NLF flap) - station 10

Figure 27. Near-cruise pressure distribution sensitivity to lift coefficient for the CRM-M8 conventional and slotted wing designs at stations 3, 6, and 10.

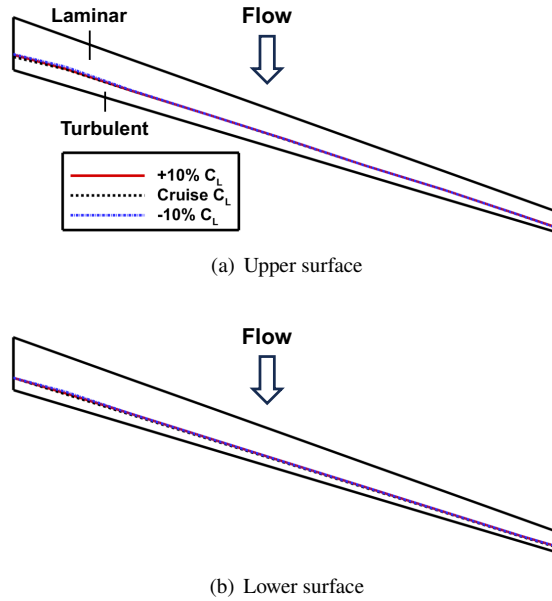


Figure 28. Transition front sensitivity to lift coefficient on the flap upper and lower surfaces.

2. Drag Rise

Drag rise simulations were conducted for a Mach number range of 0.75 to 0.85 with a Mach number increment of 0.01. At each Mach number, the angle of attack was iteratively changed to converge to the cruise lift coefficient. Figure 29 shows the variation in configuration drag with Mach number for the conventional and slotted wing designs. The conventional wing and slotted wing with an NLF flap show comparable drag levels over the low-drag region prior to reaching the cruise Mach number at 0.8. For further increases in cruise speed, the conventional wing shows rapid drag divergence up to Mach 0.85. In contrast, the CSW provides a more gradual increase in drag rise behavior. At turbulent conditions, the CSW incurs a six-count skin-friction drag penalty (loss of NLF on the flap) that is maintained below the cruise Mach number. As cruise speed is increased, the differences between the laminar and fully turbulent flap solutions diminish, suggesting that NLF extents are sensitive to changes in Mach number. Figure 30 shows similar performance conclusions in terms of aerodynamic efficiency, ML/D . The conventional wing and CSW designs show comparable efficiency up to the cruise Mach number. As Mach number increases, the slotted wing achieves higher aerodynamic efficiency due to the slot impact on pressure drag due to separation.

Figure 31 shows the pressure distributions for the conventional and slotted wing designs at Mach numbers of 0.80, 0.82, and 0.84 from the drag rise analysis. At station 3 on the inboard wing, both designs show an expected increase in shock strength with a more aft position. At stations 6 and 10 on the outboard wing, the conventional wing shows similar qualitative changes in the pressure distribution with Mach number. For conventional wings, drag divergence occurs due to a rapid increase in pressure drag due to shockwave-boundary-layer interactions that lead to separation, as evidenced by trailing edge C_p values at or below zero. The effect of Mach number on the CSW are shown at stations 6 and 10. As Mach number is increased, the main element upper surface shock grows in strength until the shock reaches the main element trailing edge. Once this happens, supersonic flow is formed through the slot, leading to increased wave drag and adverse pressure gradients over the flap upper surface that can lead to early transition due to TS.

Transition prediction software was used to quantify the sensitivity of the NLF transition fronts to perturbation in Mach number. Figure 32 shows the transition fronts predicted for the upper and lower surfaces of the flap at Mach number of 0.8, 0.82, and 0.84. At the cruise Mach number of 0.8, approximately 71% and 77% of the upper and lower surface area, respectively, achieves natural laminar flow. As Mach number increases to 0.82, the transition locations on the upper surface of the flap shift forward over the inboard region due to the formation of shocks in the slot that lead to early transition. Further increases in speed up to Mach 0.84 lead to a progressive forward shift in the transition front over a greater spanwise extent. The NLF extent on the upper surface of the flap reduces from 71% at the Mach 0.8 cruise condition to 64% and 32% at Mach 0.82 and 0.84, respectively. For the flap lower surface, NLF extents do not change because of limited pressure distribution sensitivity to Mach number.

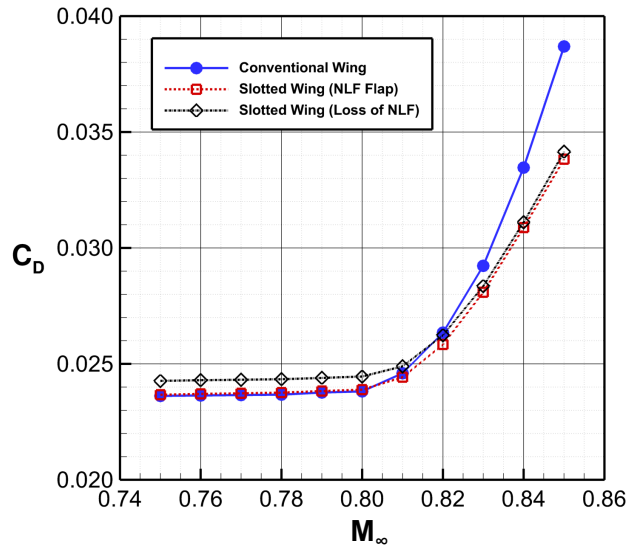


Figure 29. Near-cruise drag rise comparing the conventional and slotted wing designs.

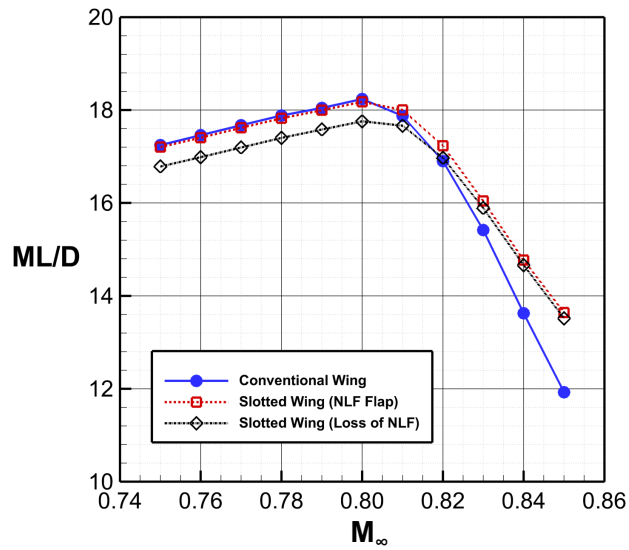
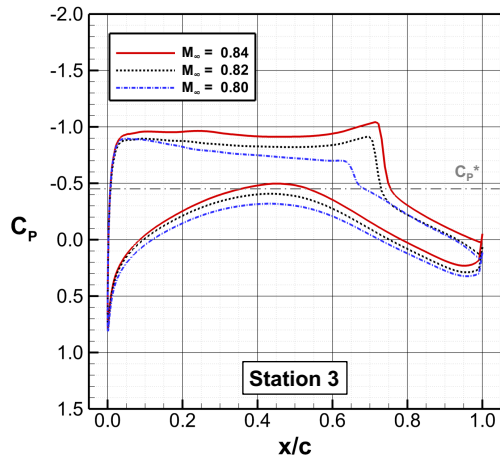
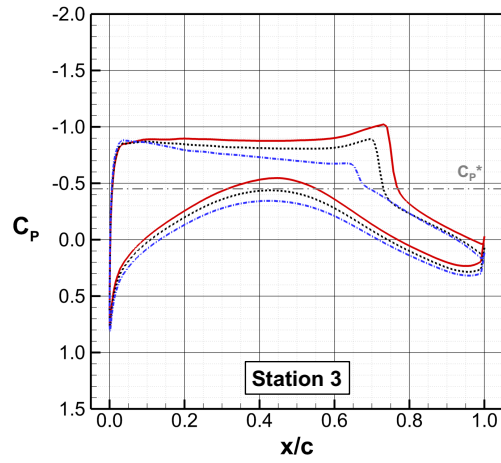


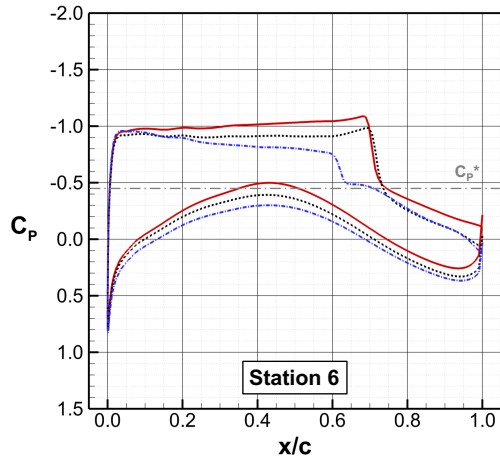
Figure 30. Aerodynamic efficiency curve comparing the conventional and slotted wing designs.



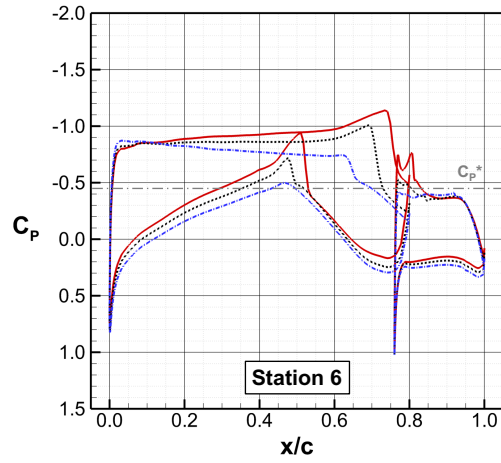
(a) Conventional wing - station 3



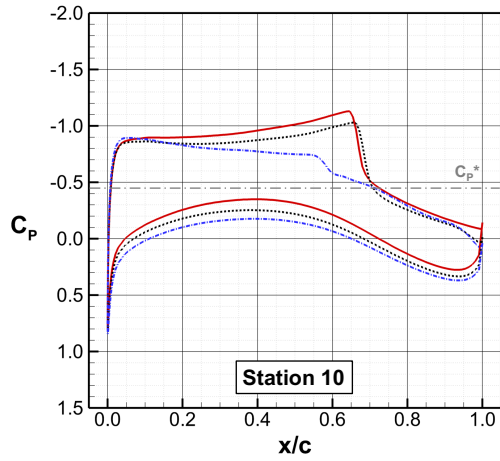
(b) Slotted wing (NLF Flap) - station 3



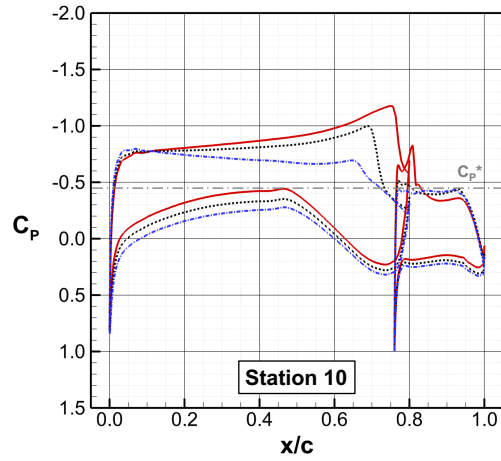
(c) Conventional wing - station 6



(d) Slotted wing (NLF Flap) - station 6



(e) Conventional wing - station 10



(f) Slotted wing (NLF Flap) - station 10

Figure 31. Near-cruise pressure distribution sensitivity to Mach number for the CRM-M8 conventional and slotted wing designs.

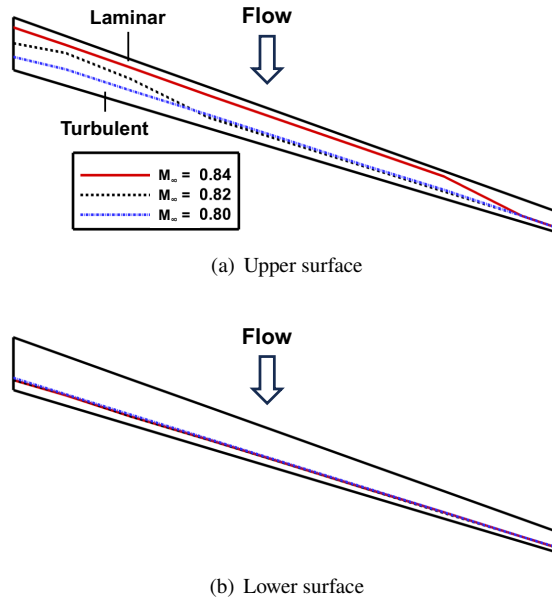


Figure 32. Transition front sensitivity to Mach number on the flap upper and lower surfaces.

VIII. Concluding Remarks

CSW technology has the potential to significantly improve the aerodynamic performance of next-generation commercial aircraft. The present research sought to quantify the drag-saving benefits of the concept in application to single-aisle transonic transport configurations. Toward this objective, the CDISC aerodynamic design code has been extended to enable the computational design of transonic cruise slotted wings. A series of multielement aerodynamics and geometry constraints were created to enable the simultaneous design of the main element and flap. CDISC was used to create two conventional wing and partial-span, CSW designs for a Mach-0.8 variant of the Common Research Model.

The CDISC CSW design used the Aft Laminar Multielement Airfoil concept to reduce the skin-friction penalty historically associated with cruise slotted wings by shaping the upper and lower surfaces of the flap to support NLF. Due to the low chord Reynolds number and leading-edge sweep of the flap, concerns for crossflow instabilities were limited and TS instabilities could be suppressed with mild adverse pressure gradients. At cruise, approximately 71% and 77% of the flap upper and lower surface area, respectively, achieves laminar flow. Transition predictions for CF and TS growth showed a conservative margin between peak CF/TS N-factor values and the critical N-factor. This design feature translates to a greater tolerance for adverse pressure gradients at off-design conditions and sustained NLF without early transition due to TS. The CSW design was successful in increasing the aft loading, as evidenced by a 45% decrease in sectional pitching moment coefficient and by permitting a terminating shock location 10% further aft relative to the conventional wing design. Despite these improvements, cruise drag predictions showed comparable performance between the conventional and partial-span, CSW designs. At flight conditions representative of single-aisle transonic transport aircraft, the CSW technology has less potential for reduced pressure drag due to the relatively low shock strength achieved with supercritical wings. Near-cruise drag polar and drag rise analyses showed limited NLF sensitivity to angle of attack and improved drag rise characteristics relative to a conventional wing.

Based on this demonstrated benefit, future work is planned to quantify the performance benefits of a CRM-M8 CSW design with full-chord natural laminar flow using the ALMA and CATNLF concepts. The CATNLF design strategy may be used to shape the main element for the suppression of CF instabilities at leading-edge sweeps and flight Reynolds numbers representative of single-aisle transonic transports. The design of a CSW using the ALMA and CATNLF concepts would combine the drag-saving benefits of NLF with the improved drag rise behavior offered by CSW technology. To further increase the Technology Readiness Level of CSW technology, a cruise slotted airfoil wind tunnel model is currently being designed for testing at the NASA Langley 0.3-meter Transonic Cryogenic Tunnel. This risk-reduction test will be used to assess correlation between computational and experimental pressure data for cruise slotted airfoils and to experimentally verify that full chord NLF can be achieved on the main element and on the flap upper surface in the presence of the main element wake.

Acknowledgments

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