

Prediction of Laminar-to-Turbulent Transition for the XV-15 Hovering Rotor Flows Using a Crossflow Model

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The Amplification Factor Transport and Medida-Baeder’s local correlation-based transition models are extended to support crossflow-induced transition in NASA’s OVERFLOW CFD code. The algebraic crossflow correlations use helicity as an indicator of local crossflow intensity within the boundary layer. The implementation of these models is validated against standard test cases of crossflow-dominated transition: NLF(2)-0415 45° swept airfoil and TU Braunschweig sickle wing. The full-scale XV-15 rotor is simulated in low- and high-thrust conditions to evaluate the impact of crossflow transition in the prediction of rotor performance. A comparison is established between the fully-turbulent, baseline transition, and crossflow-extended transition models. Even though the transition models agreed more closely with experimental measurements than the fully-turbulent models, the addition of crossflow-induced transition resulted in minimal differences.

Nomenclature

Acronyms

Acronym	Description
AFT	Amplification Factor Transport
FM	Figure of merit
MB	Medida-Baeder
SA	Spalart-Allmaras
SST	Shear Stress Transport

Greek symbols

Symbol	Description
α	Angle of attack
γ	Intermittency
$\tilde{\gamma}$	Modified intermittency
Λ	Geometric sweep
$\tilde{\nu}$	Modified kinematic eddy viscosity
ν_t	Kinematic eddy viscosity
σ	Rotor solidity
θ_0	Rotor collective pitch angle

Roman symbols

Symbol	Description
c	Chord length
c_f	Skin friction coefficient
C_Q	Torque coefficient
C_T	Thrust coefficient
d	Wall distance
H	Helicity
h	Surface roughness
M	Mach number
$\tilde{n}$	Transported envelope amplification factor
R	Rotor radius
r	Radial coordinate
Re	Reynolds number
$\overline{Re}_{\theta_t}$	Transported transition onset momentum thickness Reynolds number
Tu	Turbulence intensity
x	Chordwise coordinate
y^+	Non-dimensional viscous wall spacing

I. Introduction

TRANSITION modeling is essential for accurate computational fluid dynamics (CFD) simulations in cases that involve external flow over solid surfaces such as rotor blades and aircraft wings. The prediction of heat transfer and aerodynamic forces is significantly impacted by the flow characteristics related to transition from laminar to turbulent

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flow. Specifically, turbulent boundary layers exhibit higher energy dissipation and enhanced mixing, which increases momentum and heat exchange near the surface. Traditionally, CFD approaches rely on the assumption of fully turbulent flow and frequently overestimate drag and heat transfer on surfaces with partially laminar boundary layers.

The rotor blade boundary layer presents unique challenges compared to a fixed wing due to large spanwise variations in Reynolds and Mach numbers, as well as in twist distribution. The presence of strong crossflow and three-dimensional effects further complicate the flow environment [1]. The influence of laminar-to-turbulent transition in rotorcraft simulations has only been partially investigated, which can be attributed to two main challenges: (1) difficulties in developing robust, CFD-compatible models, and (2) lack of experimental data to validate and calibrate these models. However, ongoing research has led to significant progress in recent years. Efficient and robust transition models need to be locally formulated to leverage massive parallelization and domain decomposition capabilities of modern CFD codes. Examples of models that satisfy this requirement are the $\gamma\text{-}\overline{Re}_{\theta_t}$ models [2, 3], which are implemented as extensions of well-established turbulence models such as Menter’s Shear Stress Transport (SST) model and the Spalart-Allmaras (SA) model; or the Amplification Factor Transport (AFT) model [4], which is also built as an extension of the SA turbulence model. Additionally, a growing body of experiments of various rotor systems [1, 5–7] have generated useful data for comparison.

While many researchers have reported reasonable agreement between experimental observations and CFD computations with transition models, they have also noted several limitations. For example, Jain [8] compared fully-turbulent and free-transition simulations of the pressure-sensitive-paint (PSP) rotor using the SST- $\gamma\text{-}\overline{Re}_{\theta_t}$ model. Although the first-order effects of transition were successfully captured, the study acknowledged discrepancies in the figure of merit under low thrust conditions, presumably due to an overprediction of transition delay. Vieira et al. [9] simulated the PSP rotor using the AFT model, showing promising agreement for performance and transition onset predictions. However, not accounting for bypass transition and crossflow instabilities led to erroneous estimations near the tip. Sheng [10] utilized the SST- and SA- $\gamma\text{-}\overline{Re}_{\theta_t}$ models to analyze several rotors and demonstrated the prediction capabilities of transition models —leading-edge laminar separation bubbles, separation-driven transition, and root/tip flow separations. Richez [11] compared the results of the SST- $\gamma\text{-}\overline{Re}_{\theta_t}$ model with a non-local semi-empirical transition criteria model for the PSP rotor, documenting best practices on the application of these models. Garcia Perez et al. [12] compared fully-turbulent and free-transition simulations of the XV-15 rotor using the SST- and SA- $\gamma\text{-}\overline{Re}_{\theta_t}$ models. Incorporating transition correlations improved the prediction of the figure of merit and skin friction coefficient.

Even though these models are able to predict natural, separation-induced, and bypass transition, they fail to capture the effects of crossflow instabilities. The presence of crossflow is a common phenomenon in three-dimensional boundary layers of rotating blades. Crossflow instabilities result from a combination of sweep and pressure gradients that create a crossflow component of the velocity within the boundary layer [13]. If neglected, it could lead to delayed transition and underpredicted drag. Many early transition models failed to account for crossflow instabilities, leading to less accurate predictions of three-dimensional flows. Several modifications have been developed to incorporate crossflow models into the transition frameworks, including (1) local helicity-based methods, such as those proposed by Müller and Herbst [14] or Langtry et al. [15]; and (2) C1-criterion based models, such as the one proposed by Choi and Kwon [16]. Numerous studies have investigated the use of these models for rotorcraft applications, with a particular focus on the PSP rotor. For example, Carnes and Coder [17] coupled the crossflow model of Langtry et al. with the AFT model, showing mild improvements in performance prediction when compared to experimental observations. However, the authors noted that the helicity of the vortices near the tip tended to amplify crossflow instabilities, resulting in a more upstream transition prediction. Lee et al. [18] used the SA- $\gamma\text{-}\overline{Re}_{\theta_t}$ model coupled with Müller and Herbst’s crossflow model. The inclusion of crossflow transition resulted in minimal differences in performance prediction. Hwang and Kwon [19] utilized the SST- $\gamma\text{-}\overline{Re}_{\theta_t}$ model and the crossflow model by Choi and Kwon, concluding that the crossflow model improves the prediction of transition onset as well as the agreement between computational and experimental performance metrics. Despite the recent studies, crossflow transition is still an ongoing area of research.

Recently, NASA’s research on rotorcraft has increasingly focused on supporting the emerging advanced air mobility industry, where rotor-based vertical take-off and landing vehicles play a central role. NASA’s OVERFLOW CFD code [20] has been widely used to analyze the aerodynamics of conceptual air taxi vehicles [21–25]. Currently, OVERFLOW only includes crossflow predictive capabilities for the SST- $\gamma\text{-}\overline{Re}_{\theta_t}$ model. The integration of additional crossflow models to simulate laminar-to-turbulent transition could potentially improve the prediction of aerodynamic forces and performance of new vehicle designs.

The goal of this work is to expand OVERFLOW’s predictive capabilities and contribute to the transition modeling efforts within the rotorcraft community. To that end, this paper focuses on extending the SA- $\gamma\text{-}\overline{Re}_{\theta_t}$ and SA-AFT frameworks to predict transition due to crossflow instabilities. In particular, Coder’s SA-AFT model is coupled with

the crossflow model of Langtry et al., and Medida-Baeder’s SA- γ - $\overline{Re}_\theta$ transition model is coupled with the crossflow model of Müller and Herbst. Two validation test cases are simulated to assess the accuracy of the augmented models: (1) the NLF(2)-0415 45° swept airfoil and (2) the TU Braunschweig sickle wing. Finally, simulations of the XV-15 rotor in hovering conditions serve to analyze the impact of crossflow transition in the prediction of the figure of merit and skin friction coefficient.

II. Numerical Approach

A. Overset Grid Generation

The Chimera Grid Tools (CGT) [26] software package is used for the overset grid generation of the different cases. The use of overset grids simplifies the grid generation for complex geometries. The basic idea consists of breaking down the components into simpler shapes and generating individual grids for each one. The near-body (NB) grids are attached to the bodies, whereas a collection of stagnant Cartesian off-body (OB) grids fills the rest of the solution domain. Overset grids offer flexibility and control over grid generation and quality, since the NB grids can have different resolutions, topologies, and boundary conditions. This approach is especially suited to multi-body configurations with relative motion. A detailed description of the grids generated for each geometry is given in subsequent sections.

B. CFD Flow Solver

OVERFLOW solves the three-dimensional, time-dependent Reynolds-averaged Navier-Stokes (RANS) equations. It offers several inviscid flux algorithms, implicit numerical schemes, and turbulence models. The strong conservation law form of the governing equations can be expressed in generalized coordinates as follows:

$$\frac{\partial \hat{\mathbf{q}}}{\partial t} + \frac{\partial (\hat{\mathbf{F}} - \hat{\mathbf{F}}_v)}{\partial \xi} + \frac{\partial (\hat{\mathbf{G}} - \hat{\mathbf{G}}_v)}{\partial \eta} + \frac{\partial (\hat{\mathbf{H}} - \hat{\mathbf{H}}_v)}{\partial \zeta} = \mathbf{0}, \quad (1)$$

where ξ is the longitudinal coordinate, η is the circumferential coordinate, and ζ is the nearly-normal coordinate. In equation (1), $\hat{\mathbf{q}} = \mathbf{q}/J$, where $\mathbf{q} = [\rho, \rho u, \rho v, \rho w, e]^\top$ the vector of conserved variables, and J the transformation Jacobian between generalized coordinates (ξ, η, ζ) and Cartesian coordinates (x, y, z) . $\hat{\mathbf{F}}$, $\hat{\mathbf{G}}$, and $\hat{\mathbf{H}}$, as well as $\hat{\mathbf{F}}_v$, $\hat{\mathbf{G}}_v$, and $\hat{\mathbf{H}}_v$ are related to the inviscid and viscous fluxes, respectively, by the same coordinate transformation.

For the validation cases —NLF(2)-0415 45° swept airfoil and TU Braunschweig sickle wing—, the simulations are iteratively refined until a steady-state solution is reached. The inviscid flux vectors use a 3rd-order accurate whereas the viscous fluxes use a 2nd-order discretization. The successive symmetric over relaxation unfactored scheme [27] is used to solve equation (1).

For the XV-15 rotor case, the simulation is advanced in time using the 2nd-order accurate backward Euler method, where dual time-stepping is used to march in dual-time $\Delta\tau$ to steady-state. The physical time step Δt corresponds to 0.25° of rotor rotation, together with up to 25 dual-time sub-iterations to achieve 2.5 orders of magnitude drop in sub-iteration residual. The inviscid flux vectors at the cell interfaces use 6th-order central differences and 5th-order artificial dissipation to damp out high-frequency errors, resulting in a 5th-order accurate discretization. The viscous terms are discretized with 2nd-order accuracy. The diagonalized form of the diagonally-dominant alternating direction implicit factored scheme [28], with a sub-iteration procedure, is used to solve equation (1).

C. Turbulence Modeling

OVERFLOW includes algebraic, one-equation, and two-equation turbulence models, with the choice of hybrid Reynolds-averaged Navier-Stokes/large eddy simulation (RANS/LES) frameworks and transition correlations for one- and two-equation models.

The one-equation Spalart-Allmaras (SA) turbulence model [29] continues to be one of the preferred methods to provide closure to the RANS equations, since its accuracy is similar to a two-equation model at a reduced computational cost. The baseline SA turbulence model reads:

$$\frac{\partial \tilde{\nu}}{\partial t} + \mathbf{u} \cdot \nabla \tilde{\nu} = c_{b1} \tilde{\nu} (1 - f_{t2}) - \left(c_{w1} f_w - \frac{c_{b1}}{k^2} f_{t2} \right) \left(\frac{\tilde{\nu}}{d} \right)^2 + \frac{1}{\sigma} [\nabla \cdot ((\nu + \tilde{\nu}) \nabla \tilde{\nu}) + c_{b2} \nabla \tilde{\nu} \cdot \nabla \tilde{\nu}], \quad (2)$$

where the transported variable $\tilde{\nu}$ relates to the kinematic eddy viscosity ν_t . This work uses the SA-neg version [30] to address situations of under-resolved grids and unphysical transient states when $\tilde{\nu} < 0$.

OVERFLOW currently includes two transition models formulated around the SA turbulence model: (1) the Amplification Factor Transport (AFT) model and (2) the Medida-Baeder (MB) $\gamma\text{-}\overline{Re}_\theta$ model, which currently lack crossflow transition capabilities. This work is concerned with the implementation and assessment of algebraic crossflow correlations for these models. In particular, the SA-AFT turbulence model is coupled with the crossflow model of Langtry et al. [15] and the SA-MB turbulence model is coupled with the crossflow model of Müller and Herbst [14]. A description of these models is provided in the following sections.

1. Amplification Transport Factor Transition Model

The AFT model [4] is based on the approximate envelope method, using the amplification factor as a measure of the growth of boundary layer instabilities. It introduces a transport equation for the envelope amplification factor $\tilde{n}$, which evolves according to local flow conditions and estimated boundary layer properties; and is coupled with the SA turbulence model through a modified intermittency variable $\tilde{\gamma}$, which controls the eddy viscosity production.

The governing transport equation that describes the evolution of $\tilde{n}$ is:

$$\frac{\partial \rho \tilde{n}}{\partial t} + \nabla \cdot (\rho \mathbf{u} \tilde{n}) = \underbrace{\rho \Omega F_{\text{crit}} F_{\text{growth}} \frac{d\tilde{n}}{dRe_\theta}}_{\text{Production}} + \underbrace{\nabla \cdot [\sigma_n (\mu + \mu_t) \nabla \tilde{n}]}_{\text{Diffusion}}. \quad (3)$$

The production term is based on an estimate of the integral boundary layer shape factor H_{12} and the momentum thickness θ . The shape factor is calculated as:

$$H_{12} = \min [\max (0.26H_L + 2.4, 2.2), 20], \quad (4)$$

where H_L is a local boundary layer shape factor given by:

$$H_L = \frac{d^2}{\nu} [\nabla \cdot (\rho \mathbf{u} \cdot \nabla d) \cdot \nabla d]. \quad (5)$$

The function F_{crit} introduces the dependency with the critical Reynolds number, as the boundary layer must be sufficiently thick to support the growth of instabilities:

$$F_{\text{crit}} = \begin{cases} 0 & \text{if } Re_V < Re_{V,0}, \\ 1 & \text{if } Re_V \geq Re_{V,0}, \end{cases}$$

$$Re_{V,0} = k_V Re_{\theta,0},$$

$$k_V = \frac{1}{0.4036H_{12}^2 - 2.5394H_{12} + 4.3273}, \quad (6)$$

$$Re_V = \frac{\rho S d^2}{\mu + \mu_t},$$

$$\log_{10} Re_{\theta,0} = 0.7 \tanh \left(\frac{14}{H_{12} - 1} - 9.24 \right) + \frac{2.492}{(H_{12} - 1)^{0.43}} + 0.62.$$

In the above equations, ρ is the density, $\mathbf{u}$ is the velocity vector, Ω is the vorticity magnitude, μ is the molecular viscosity, μ_t is the eddy viscosity, ν is the kinematic viscosity, d is the wall distance, Re_V is the vorticity Reynolds number based on μ and μ_t , Re_θ is the momentum thickness Reynolds number, k_V is a proportionality factor between Re_V and Re_θ based on Falkner-Skan boundary layer profiles, S is the strain rate magnitude, and the subscript 0 denotes the condition at which instabilities are first possible. The function F_{growth} accounts for the growth rate of the boundary layer itself:

$$F_{\text{growth}} = D(H_{12}) \frac{1 + m(H_{12})}{2} l(H_{12}),$$

$$D(H_{12}) = \frac{2.4H_{12}}{H_{12} - 1},$$

$$l(H_{12}) = \frac{6.54H_{12} - 14.07}{H_{12}^2}, \quad (7)$$

$$m(H_{12}) = \frac{1}{l(H_{12})} \left[0.058 \frac{(H_{12} - 4)^2}{H_{12} - 1} - 0.068 \right],$$

where $D(H_{12})$, $l(H_{12})$, and $m(H_{12})$ are auxiliary functions. Finally, the non-dimensional growth rate of the amplification factor is given by:

$$\frac{d\tilde{n}}{dRe_\theta} = 0.028(H_{12} - 1) - 0.0345 \exp \left[- \left(\frac{3.87}{H_{12} - 1} 2.52 \right)^2 \right]. \quad (8)$$

The governing transport equation that describes the evolution of $\tilde{\gamma}$ is:

$$\frac{\partial \rho \tilde{\gamma}}{\partial t} + \nabla(\rho \mathbf{u} \tilde{\gamma}) = \underbrace{c_1 \rho S F_{\text{onset}} (1 - \exp \tilde{\gamma})}_{\text{Production}} - \underbrace{c_2 \rho \Omega F_{\text{turb}} (c_3 \exp \tilde{\gamma} - 1)}_{\text{Destruction}} + \underbrace{\nabla \cdot \left[\left(\mu + \frac{\mu_t}{\sigma_y} \right) \nabla \tilde{\gamma} \right]}_{\text{Diffusion}}, \quad (9)$$

where the modified intermittency $\tilde{\gamma}$ relates to the actual intermittency γ through a logarithmic dependency:

$$\tilde{\gamma} = \log \gamma. \quad (10)$$

This transformation ensures that γ will always be positive.

The function F_{onset} in the production term is designed to be zero upstream of transition onset and switch to unity in a turbulent boundary layer:

$$\begin{aligned} F_{\text{onset}} &= \max(F_{\text{onset}_2} - F_{\text{onset}_3}, 0), \\ F_{\text{onset}_1} &= \frac{\tilde{n}}{N_{\text{crit}}}, \\ F_{\text{onset}_2} &= \min(F_{\text{onset}_1}, 2), \\ F_{\text{onset}_3} &= \max \left[1 - \left(\frac{R_T}{3.5} \right)^3, 0 \right], \end{aligned} \quad (11)$$

The F_{onset_1} function is responsible for triggering transition once the envelope amplification factor $\tilde{n}$ surpasses a critical value N_{crit} in the sense of linear stability theory. This value of N_{crit} is calculated as:

$$N_{\text{crit}} = -8.43 - 2.4 \log \frac{\tau}{100}, \quad (12)$$

where N_{crit} depends on the freestream turbulence intensity Tu via the parameter τ :

$$\tau = 2.5 \tanh \frac{Tu(\%) }{2.5}. \quad (13)$$

The parameter τ is a turbulence intensity limiter that prevents negative values of N_{crit} if large values of Tu are specified. Several min/max limiter operators ensure that the functions remain within specified boundaries. In particular, F_{onset_2} limits excessive production and F_{onset_3} detects the presence of turbulence in the boundary layer to help maintain the levels of intermittency. Here, R_T is the characteristic turbulence Reynolds number:

$$R_T = \frac{\mu}{\mu_t}. \quad (14)$$

The function F_{turb} in the destruction term prevents the destruction of intermittency in the boundary layer where R_T is large, but enables relaminarization of boundary layers where R_T is small:

$$F_{\text{turb}} = \exp \left[- \left(\frac{R_T}{2} \right)^4 \right]. \quad (15)$$

The relevant constants and boundary conditions for equations (3) and (9) are:

$$\sigma_n = 1, \quad c_1 = 100, \quad c_2 = 0.06, \quad c_3 = 50, \quad \sigma_y = 1, \quad (16)$$

$$\tilde{n}_\infty = 0, \quad \tilde{\gamma}_\infty = 0, \quad \left. \frac{\partial \tilde{n}}{\partial y} \right|_{\text{wall}} = 0, \quad \left. \frac{\partial \tilde{\gamma}}{\partial y} \right|_{\text{wall}} = 0. \quad (17)$$

To provide an interface with the crossflow model of Langtry et al. [15], the function F_{onset_1} is updated with an additional criterion to trigger crossflow-induced transition [17]:

$$F_{\text{onset}_1} = \max \left(\frac{\tilde{n}}{N_{\text{crit}}}, \frac{Re_v}{2.193 Re_{\text{SCF}}} \right), \quad (18)$$

where Re_v is the vorticity Reynolds number and Re_{SCF} is the stationary crossflow Reynolds number:

$$Re_v = \frac{\rho d^2 S}{\mu}, \quad (19)$$

$$Re_{\text{SCF}} = \frac{\theta_t \rho (U/0.82)}{\mu} = -35.088 \log \frac{h}{\theta_t} + 319.51 + f(+\Delta H_{\text{crossflow}}) - f(-\Delta H_{\text{crossflow}}).$$

The empirical correlation for Re_{SCF} establishes a logarithmic dependency between the stationary crossflow Reynolds number Re_{SCF} and the surface roughness of the body h . This equation is calibrated with the NLF(2)-0415 airfoil experiments, with the functions $f(+\Delta H_{\text{crossflow}})$ and $f(-\Delta H_{\text{crossflow}})$ serving as the upward and downward shift terms to account for crossflow strengths different than that of the airfoil. The rest of the equations of the model are:

$$H_{\text{crossflow}} = \frac{d|\mathbf{u} \cdot \boldsymbol{\Omega}|}{U},$$

$$\Delta H_{\text{crossflow}} = H_{\text{crossflow}} \left[1 + \min \left(\frac{\mu_t}{\mu}, 0.4 \right) \right],$$

$$+\Delta H_{\text{crossflow}} = \max [(0.1066 - \Delta H_{\text{crossflow}}), 0], \quad (20)$$

$$f(+\Delta H_{\text{crossflow}}) = 6200(+\Delta H_{\text{crossflow}}) + 50000(\Delta H_{\text{crossflow}})^2$$

$$-\Delta H_{\text{crossflow}} = \max [-(0.1066 - \Delta H_{\text{crossflow}}), 0]$$

$$f(-\Delta H_{\text{crossflow}}) = 75 \tanh \frac{-\Delta H_{\text{crossflow}}}{0.0125},$$

where U is the velocity magnitude, θ_t is the momentum thickness at transition onset, and $H_{\text{crossflow}}$ is the non-dimensional crossflow strength based on the helicity parameter (streamwise vorticity).

Finally, the AFT model is coupled with the SA turbulence model by modifying the f_{t2} parameter in equation (2):

$$f_{t2} = c_{t3}(1 - \exp \tilde{\gamma}). \quad (21)$$

The reader should refer to Coder [4], Carnes and Coder [17], and Langtry et al. [15] for a more comprehensive description of the model.

2. Medida-Baeder Transition Model

The MB model [31] is a reformulation of the Langtry and Menter [2] local correlation-based transition model, adapted to be compatible with the SA turbulence model. It solves two additional transport equations for the intermittency γ , a variable that represents the fraction of time the flow is turbulent and controls the eddy viscosity production; and the transition onset momentum thickness Reynolds number $\overline{Re}_{\theta_t}$, which helps determine when and where transition should occur.

The governing transport equation that describes the evolution of γ is:

$$\frac{\partial(\rho\gamma)}{\partial t} + \nabla(\rho\mathbf{u}\gamma) = \underbrace{P_\gamma}_{\text{Production}} - \underbrace{D_\gamma}_{\text{Destruction}} + \underbrace{\nabla \cdot \left[\left(\mu + \frac{\mu_t}{\sigma_f} \right) \nabla \gamma \right]}_{\text{Diffusion}}. \quad (22)$$

The production and destruction terms are given by:

$$P_\gamma = \rho F_{\text{onset}} G_{\text{onset}} \max \left[\frac{\Omega}{F_{\text{length}}}, \frac{1}{F_{\text{length}, \min}} \right], \quad (23)$$

$$P_\gamma = (1 - \gamma)P_\gamma \quad \text{if} \quad \gamma > 1,$$

$$D_\gamma = \rho \Omega \gamma (1 - G_{\text{onset}}). \quad (24)$$

The function F_{onset} is designed to be zero in a laminar boundary layer and rapidly switch to unity in a turbulent boundary layer:

$$\begin{aligned} F_{\text{onset}} &= \max(F_{\text{onset}_2} - F_{\text{onset}_3}, 0), \\ F_{\text{onset}_1} &= \frac{Re_v}{2.193 Re_{\theta_c}}, \\ F_{\text{onset}_2} &= \min\left(\max(F_{\text{onset}_1}, F_{\text{onset}_1}^4), 4\right), \\ F_{\text{onset}_3} &= \max\left(2 - (0.25 R_T)^3, 0\right). \end{aligned} \quad (25)$$

The F_{onset_1} term triggers transition when the ratio of vorticity Reynolds number Re_v and critical Reynolds number Re_{θ_c} exceeds a certain threshold. Min/max limiters ensure the function outputs are bounded. Here, the vorticity Reynolds number Re_v and the characteristic turbulent Reynolds number R_T are calculated as:

$$Re_v = \frac{\rho d^2 S}{\mu}, \quad (26)$$

$$R_T = \frac{\mu_t}{\mu}. \quad (27)$$

At a given streamwise location, the function G_{onset} activates the intermittency production term when the F_{onset_1} criterion is satisfied at any point within the boundary layer height, while simultaneously deactivating the destruction term:

$$G_{\text{onset}} = \begin{cases} 1 & \text{if } \max(F_{\text{onset}_1}) > 1, \\ 0 & \text{otherwise.} \end{cases} \quad (28)$$

The correlations for the critical Reynolds number Re_{θ_c} and the intermittency production strength F_{length} are given by:

$$Re_{\theta_c} = \alpha \overline{Re_{\theta_t}}, \quad \alpha = 0.62, \quad (29)$$

$$F_{\text{length}} = 10, \quad F_{\text{length}, \min} = 2.5. \quad (30)$$

In the above equations, ρ is the density, $\mathbf{u}$ is the velocity vector, μ is the molecular viscosity, μ_t is the eddy viscosity, Ω is the vorticity magnitude, S is the strain rate magnitude, and d is the wall distance.

The governing transport equation that describes the evolution of $\overline{Re_{\theta_t}}$ is:

$$\frac{\partial(\rho \overline{Re_{\theta_t}})}{\partial t} + \nabla(\rho \mathbf{u} \overline{Re_{\theta_t}}) = \underbrace{P_{\theta_t}}_{\text{Production}} + \underbrace{\nabla \cdot [\sigma_{\theta_t}(\mu + \mu_t) \nabla \overline{Re_{\theta_t}}]}_{\text{Diffusion}}, \quad (31)$$

The production term is designed to force the transported scalar $\overline{Re_{\theta_t}}$ to match the freestream value of Re_{θ_t} outside the boundary layer. The blending function F_{θ_t} turns off the source term inside the boundary layer to allow the convection and diffusion of the conserved quantity:

$$P_{\theta_t} = 0.03 \frac{\rho}{t} (Re_{\theta_t} - \overline{Re_{\theta_t}}) (1 - F_{\theta_t}), \quad (32)$$

with:

$$\begin{aligned} F_{\theta_t} &= \min\left(e^{(-\frac{d}{\delta})^4}, 1\right), \\ \theta_{\text{BL}} &= \frac{\overline{Re_{\theta_t}} \mu}{\rho U}, \quad \delta_{\text{BL}} = 7.5 \theta_{\text{BL}}, \quad \delta = \frac{50 \Omega d}{U} \delta_{\text{BL}}. \end{aligned} \quad (33)$$

Here, t is a timescale present for dimensional reasons that scales with the convective and diffusive terms of the transport equation:

$$t = \frac{500 \mu}{\rho U^2}. \quad (34)$$

The freestream value of Re_{θ_i} outside the boundary layer is obtained from experimental correlations:

$$Re_{\theta_i} = \begin{cases} \left(1173.51 - 589.428Tu_{\infty} + \frac{0.2196}{Tu_{\infty}^2}\right)F(\lambda_{\theta}) & \text{if } Tu_{\infty}(\%) \leq 1.3, \\ 331.50(Tu_{\infty} - 0.5668)^{-0.671}F(\lambda_{\theta}) & \text{if } Tu_{\infty}(\%) > 1.3, \end{cases} \quad (35)$$

where the $F(\lambda_{\theta})$ term includes the effects of freestream turbulence Tu_{∞} and streamwise pressure gradient λ_{θ} :

$$F(\lambda_{\theta}) = \begin{cases} 1 - (-12.986\lambda_{\theta} - 123.66\lambda_{\theta}^2 - 405.689\lambda_{\theta}^3)e^{-\left(\frac{Tu_{\infty}}{1.5}\right)^{1.5}} & \text{if } \lambda_{\theta} \leq 0 \\ 1 + 0.275(1 - e^{-35\lambda_{\theta}})e^{\frac{Tu_{\infty}}{1.5}} & \text{if } \lambda_{\theta} > 0. \end{cases} \quad (36)$$

The streamwise pressure gradient parameter λ_{θ} is calculated as:

$$\lambda_{\theta} = \frac{\rho\theta^2}{\mu} \frac{dU}{ds}, \quad (37)$$

where dU/ds represents the streamwise acceleration:

$$\begin{aligned} \frac{dU}{ds} &= \frac{u}{U} \frac{dU}{dx} + \frac{v}{U} \frac{dU}{dy} + \frac{w}{U} \frac{dU}{dz}, \\ \frac{dU}{dx} &= \frac{1}{2\sqrt{U}} \left(2u \frac{du}{dx} + 2v \frac{dv}{dx} + 2w \frac{dw}{dx} \right), \\ \frac{dU}{dy} &= \frac{1}{2\sqrt{U}} \left(2u \frac{du}{dy} + 2v \frac{dv}{dy} + 2w \frac{dw}{dy} \right), \\ \frac{dU}{dz} &= \frac{1}{2\sqrt{U}} \left(2u \frac{du}{dz} + 2v \frac{dv}{dz} + 2w \frac{dw}{dz} \right), \\ U &= \|\mathbf{u}\| = \sqrt{u^2 + v^2 + w^2}. \end{aligned} \quad (38)$$

The boundary conditions for equations (22) and (31) are:

$$\gamma_{\infty} = 1, \quad \overline{Re}_{\theta_i\infty} = Re_{\theta_i\infty}, \quad \left. \frac{\partial \gamma}{\partial y} \right|_{\text{wall}} = 0, \quad \left. \frac{\partial \overline{Re}_{\theta_i}}{\partial y} \right|_{\text{wall}} = 0, \quad (39)$$

where $Re_{\theta_i\infty}$ can be obtained from the above experimental correlations (35) and (36) by setting λ_{θ} to zero.

To provide an interface with the crossflow model of Müller and Herbst [14], an additional destruction term is added to equation (22):

$$P_{CF} = -\min \left(\max \left[0, \left\{ \frac{\rho}{1000t} \left(\frac{Re_H}{6} \right)^{c_1} Re_{\Omega}^{c_2} \left(\frac{12\theta}{d} \right)^{c_3} - c_4 \right\} c_5 \right], c_6 \right). \quad (40)$$

The intensity of crossflow is quantified by three main components weighted by the exponents c_1 to c_3 : (1) the Reynolds number Re_H based on the momentum thickness θ and local helicity H , (2) the vorticity Reynolds number Re_{Ω} , and (3) the ratio of the momentum thickness to wall distance θ/d . The Re_H term captures the alignment of vortices with the main flow, the Re_{Ω} term uses the vorticity magnitude Ω instead of the strain rate tensor S to more accurately reflect the flow physics near the leading edge, and the θ/d term provides an additional way to independently vary the characteristic length scales. The Reynolds number terms read:

$$\begin{aligned} Re_H &= \frac{\rho\theta}{\mu} \sqrt{\theta H}, \\ Re_{\Omega} &= \frac{\rho d^2 \Omega}{\mu}. \end{aligned} \quad (41)$$

The additional constants c_4 to c_6 control the onset, magnitude, and upper limit of the disturbances. The model constants are:

$$c_1 = 0.548, \quad c_2 = 0.1912, \quad c_3 = -0.298, \quad c_4 = 0, \quad c_5 = 60, \quad c_6 = 1666.5. \quad (42)$$

Note that this model does not include the effect of surface roughness.

Finally, the MB model is coupled with the SA turbulence model by scaling the production term of the transported variable $\tilde{\nu}$ with the intermittency solution in equation (2):

$$P_{\tilde{\nu}} = \gamma c_{b1} \tilde{S} \tilde{\nu}. \quad (43)$$

This implementation uses the SA-neg-noft2 version [32] of the SA turbulence model, which does not use the f_{t2} term. The reader should refer to Medida [31] and Müller and Herbst [14] for a comprehensive description of this model.

III. Results

A. NLF(2)-0415 45° Swept Airfoil

The NLF(2)-0415 45° swept airfoil provides a test case to study the crossflow transition mechanism in swept airfoils. At an angle of attack of $\alpha = -4^\circ$, this airfoil is designed to have a favorable pressure gradient on the upper surface up to a chordwise location of $x/c \approx 0.71$. This minimizes the formation and growth of Tollmien-Schlichting waves, isolating the crossflow transition mode.

The NLF(2)-0415 45° swept airfoil was tested by Dagenhart et al. [33] and Radeztsky et al. [34] in the Arizona State University Unsteady Wind Tunnel. The experimental facility is a closed-return wind tunnel with a 16 ft long, 4.5 ft squared test section, capable of reaching velocities up to $V_\infty = 35$ m/s. The airfoil model, with a chord length of $c = 1.83$ m, was tested at an angle of attack of $\alpha = 4^\circ$ under the flow conditions listed in table 1. The freestream turbulence intensity was reported to be $Tu = 0.09\%$. These efforts resulted in well-documented reports of the transition locations, obtained via the naphthalene sublimation technique, as a function of the Reynolds number. Additionally, the work of Radeztsky et al. [34] analyzed the influence of the surface roughness. This study was used to calibrate the SST- γ - $\overline{Re}_{\theta_i}$ +CF model [15], establishing a logarithmic dependence of the stationary crossflow Reynolds number Re_{SCF} with the surface roughness parameter h . However, for the common set of measurements, corresponding to $h = 3.3 \mu\text{m}$, the results of Radeztsky et al. [34] appear to be shifted to the left on the X-axis —by approximately $\Delta Re = 0.2 \times 10^6$ — compared to those of Dagenhart et al. [33]. We decided to use the latter for validation purposes since the flow conditions were clearly specified.

Table 1 Flow conditions for the NLF(2)-0415 45° swept airfoil.

Reynolds number Re [1]	Mach number M [1]
1.92×10^6	0.123
2.19×10^6	0.140
2.37×10^6	0.151
2.73×10^6	0.174
3.27×10^6	0.209
3.73×10^6	0.239

The computational model of the NLF(2)-0415 45° swept airfoil uses a C-type grid. The wrap-around direction contains 497 points, with 201 points placed on the upper and lower surfaces of the airfoil, respectively, and the remainder placed along the wake cut. Grid point distribution is clustered near the leading and trailing edges, with spacings of 0.01% and 0.02% . The wall normal direction contains 85 points. The initial wall spacing is chosen to target $y^+ \approx 1/3$ at $Re = 2.7 \times 10^6$. The grid progressively stretches until reaching the far-field at a distance of $50c$ from the leading edge of the airfoil. The stretching ratio in all directions is maintained below 1.2. To simulate the infinite wing, two spanwise stations are added at $+1/-1$ with the required offset to create a geometric sweep of $\Lambda = 45^\circ$. Periodic boundary conditions are applied on the first and last spanwise planes. The total number of grid points is 1.26×10^5 . Fig. 1 shows close and far views of the grid system, along with the boundary conditions applied.

Fig. 2a plots the transition location x_{tr}/c on the upper surface of the airfoil against the Reynolds number Re . It shows a comparison of the computational results, with and without crossflow correlations, and the experimental data from Dagenhart et al. [33]. The transition location in each simulation is determined as the first chordwise position where the turbulence index exceeds a threshold value of 0.5. The baseline transition models consistently predict

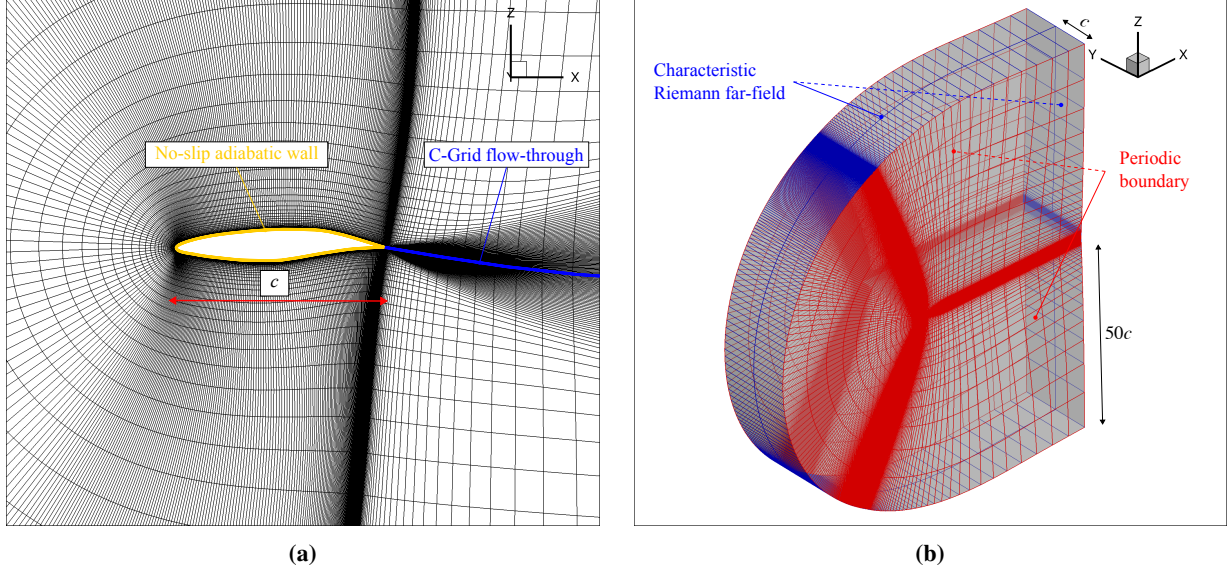


Fig. 1 Structured overset grids for the NLF(2)-0415 45° swept airfoil: (a) close-up view and (b) far-field view.

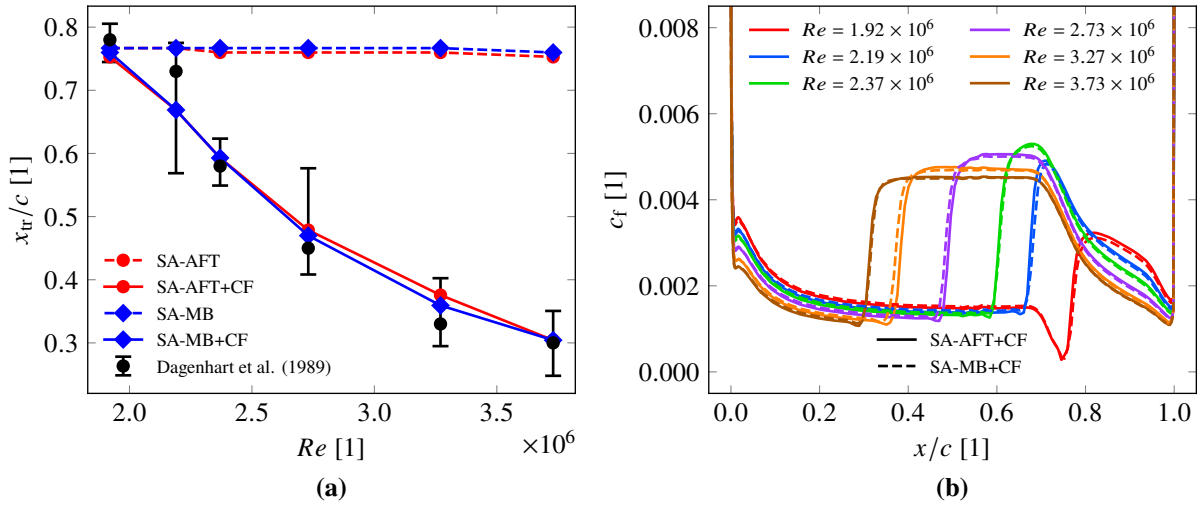


Fig. 2 Validation of transition prediction for the NLF(2)-0415 45° swept airfoil: (a) chordwise transition location as a function of Reynolds number for various turbulence models; and (b) chordwise distribution of the skin friction coefficient at different Reynolds numbers for the crossflow transition models. Experimental data from Dagenhart et al. [33].

laminar-to-turbulent transition in the vicinity of $x/c \approx 0.75$, driven by the growth of Tollmien-Schlichting instabilities—recall the flow is subjected to a favorable pressure gradient up until $x/c \approx 0.71$. In contrast, the extended models with crossflow effects reveal an earlier transition onset, with the transition locations moving progressively upstream as the Reynolds number increases. Note that, in the majority of the cases, transition occurs within the favorable pressure gradient region and is attributed to the crossflow vortices. However, for the lowest Reynolds number $Re = 1.92 \times 10^6$, all models predict transition shortly after the minimum pressure point, indicating that this phenomenon is still dominated by streamwise instabilities. To offer more insight into the transition process, Fig. 2b shows the chordwise distribution of skin friction coefficient c_f on the upper surface of the airfoil, computed with the crossflow models. For $Re = 1.92 \times 10^6$, the sudden drop in c_f preceding transition suggests that the flow initially separates in laminar state, as a result of the adverse pressure gradient. This phenomenon is not observed for higher Reynolds numbers, where an accelerated

transition occurs due to secondary spanwise flows. Transition models with built-in crossflow correlations can predict streamwise and crossflow transition independently, greatly improving the agreement with experimental data.

B. TU Braunschweig Sickle Wing

The sickle wing serves a test case to study the effect of spanwise varying mean flow. The geometry consists of a small unswept segment near the root, followed by three sections of varying sweeps of $\Lambda = 30^\circ$, 45° , and 55° , respectively. At each of the sweep breaks, strong spanwise gradients amplify stationary crossflow vortices and accelerate the transition process. The airfoil cross-section was designed to isolate crossflow-induced transition on the upper surface, allowing for a mixed amplification of crossflow and Tollmien-Schlichting waves on the lower surface.

The sickle wing was tested by Petzold and Radespiel [35] in the low speed wind tunnel of Technische Universität Braunschweig. The experimental facility is a closed-loop atmospheric wind tunnel with a 3 m long, 1.3 m squared test section that can be operated at a maximum velocity of $V_\infty = 60$ m/s. The wing model, with a chord length of $c = 0.8$ m and an average surface roughness of $h = 9.78 \mu\text{m}$, was tested at an angle of attack of $\alpha = -2.6^\circ$. The average turbulence intensity was reported to be $Tu = 0.17\%$, corresponding to a freestream velocity $V_\infty = 55$ m/s and a Reynolds number based on the chord length of $Re = 2.75 \times 10^6$. Experimental transition locations were obtained via infrared thermography.

The computational model of the sickle wing consists of two structured overset NB grids: the main platform of the wing and the tip cap. The main platform of the wing features a C-grid topology. The wrap-around direction contains 557 points, with 496 points placed on the wing surface and 61 points placed on the wake cut. The spanwise direction contains 233 points. To resolve strong gradients, grid points are densely distributed in the vicinity of the kinks. The wall-normal direction contains 59 points. The tip cap consists of 279 points from the leading edge to the trailing edge, 73 points from upper to lower surface, and 59 points in the wall-normal direction. The initial wall spacing is chosen to target $y^+ \approx 1/3$. A collection of Cartesian off-body grids expands the computational domain to a far-field distance of $50c$. In total, 43 off-body grids are generated with progressively coarser levels of refinement. The grid spacing of the first grid level is $5\%c$. To leverage the symmetry of the geometry, we only simulate a half-span version of the wing. The total number of grid points is 9.1×10^6 , with 8.8×10^6 corresponding to the NB grids and 0.3×10^6 corresponding to the OB grids. Fig. 3 shows a top view of the NB grids and a perspective view of the background mesh system. For completeness, the boundary conditions applied are also indicated.

Figs. 4 and 5 illustrate the transition characteristics obtained with the AFT and MB models, respectively. Each figure compares the predicted transition locations of the baseline (left) and crossflow (right) models with experimental data [35] for the upper and lower surfaces of the wing. The transition line is visualized by the change of skin friction coefficient c_f in the contour plot. Surface streamtraces are also indicated. Based on the infrared camera measurements, Petzold and

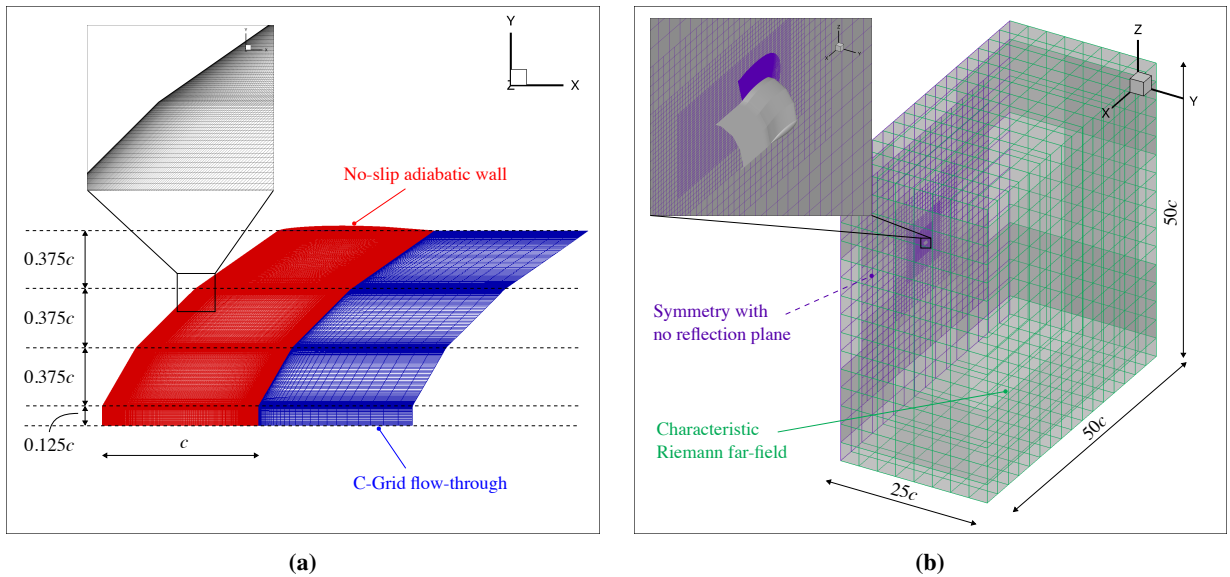


Fig. 3 Structured overset grids for the TU Braunschweig sickle wing: (a) near-body grids and (b) off-body grids.

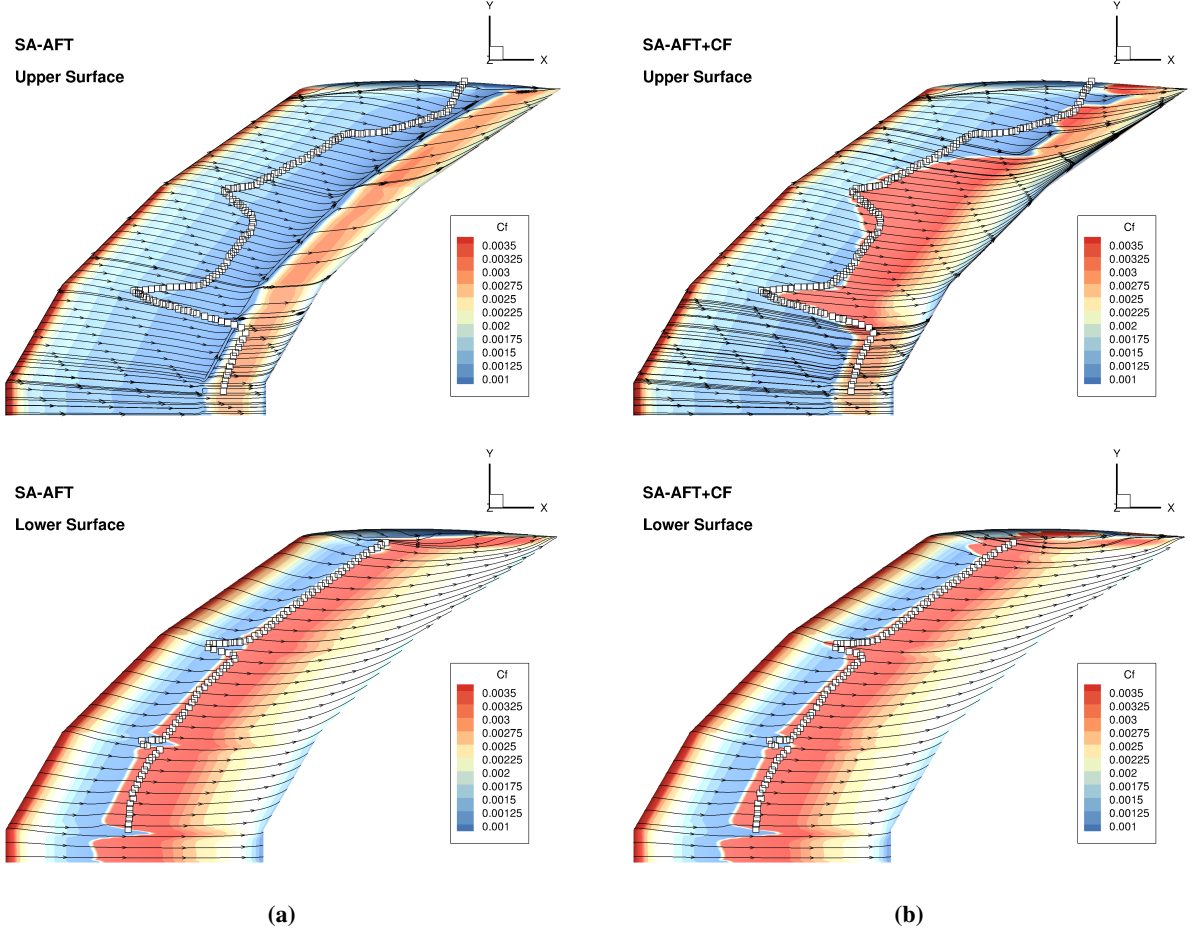


Fig. 4 Validation of the transition prediction of the TU Braunschweig sickle wing. Surface streamlines and contours of skin friction coefficient on the upper and lower surfaces of the wing for (a) SA-AFT model and (b) SA-AFT+CF model. Experimental data from Petzold and Radespiel [35].

Radespiel [35] identified the following transition mechanisms on the upper surface of the wing: (1) Tollmien-Schlichting-dominated transition on the $\Lambda = 30^\circ$ segment, characterized by a straight transition line; (2) crossflow-dominated transition on the $\Lambda = 45^\circ$ segment, characterized by sawtooth pattern transition line; and (3) a change in the dominant transition mechanism from stationary crossflow to unsteady waves—either Tollmien-Schlichting or traveling crossflow waves—on the $\Lambda = 55^\circ$ segment, as depicted by an initial sawtooth line that moves downstream and becomes straight. The lower surface of the wing showed a straight transition line, characteristic of Tollmien-Schlichting-dominated transition, with upstream shifts near the kink regions. In Fig. 4a, we observe that the SA-AFT model predicts streamwise transition at approximately $3/4c$ on the upper surface of the wing. Near-parallel streamtraces, located slightly upstream of the transition line, delimit a laminar separation bubble region, which suggests that the flow initially separates in laminar state, followed by a reattachment in turbulent state. The predicted and experimental transition locations compare poorly. In Fig. 4b, the SA-AFT+CF model shows a significant improvement in the predicted transition locations, which align closely with experimental data. The surface streamtraces only show a laminar separation bubble in a portion of the $\Lambda = 55^\circ$ segment, where the predicted transition onset is delayed. On the lower surface, where Tollmien-Schlichting-dominated transition is expected, both models compare relatively well with the experiment. The transition onset is predicted in a more upstream location in the initial half of the span. Notably, the SA-AFT+CF model only picks up the forward movement of the transition line at the last sweep break. In Fig. 5a, we observe that the SA-MB model predicts similar results to the SA-AFT model. In contrast, the SA-MB+CF model fails to capture the key features observed in the experimental transition line, as seen in Fig. 5b. On the upper surface, the simulation picks up the trend of an accelerated transition process, but it does not capture the distinctive sawtooth pattern of crossflow transition.

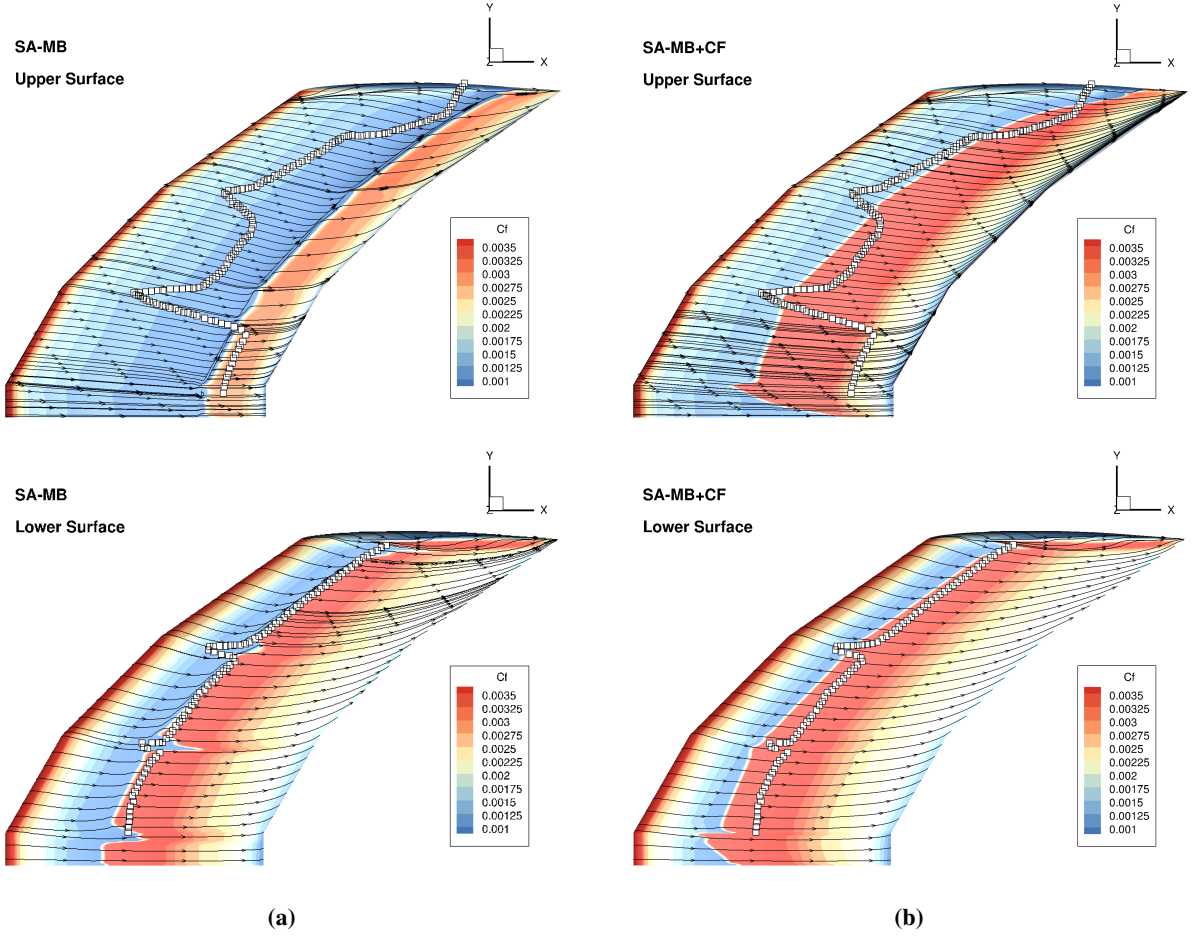


Fig. 5 Validation of the transition prediction of the TU Braunschweig sickle wing. Surface streamlines and contours of skin friction coefficient on the upper and lower surfaces of the wing for (a) SA-MB model and (b) SA-MB+CF model. Experimental data from Petzold and Radespiel [35].

Additionally, it appears that the model overpredicts the influence of crossflow on the $\Lambda = 30^\circ$ segment. Through additional simulations, we confirmed that this overestimation of the crossflow instability is insensitive to the freestream turbulence level, hinting that it may be related to the model formulation. Further analysis is required to characterize this behavior. On the lower surface, the SA-MB+CF model moves the transition line ahead of the experimental one and is not able to predict the peaks originating at the sweep breaks. For this particular case, it appears that the current implementation of the SA-AFT+CF model outperforms the SA-MB+CF model.

C. Full-Scale XV-15 Rotor

The full-scale XV-15 rotor is simulated to assess the impact of crossflow-induced transition in the prediction of rotor performance. The rotating motion of the blades induces strong spanwise flows that may accelerate transition. An accurate prediction of the skin friction drag is essential to accurately compute the figure of merit FM.

Over the years, this rotor has been the focus of numerous experimental analyses conducted in the National Full-Scale Aerodynamics Complex (NFAC) 80-by-120 ft Wind Tunnel at the NASA Ames Research Center. The NFAC test section is the largest in the world and is capable of testing full-sized aircraft to velocities up to $V_\infty = 100$ kt. The experimental campaigns resulted in a well-documented collection of performance and load measurements [36, 37], as well as valuable skin friction data to evaluate laminar-to-turbulent transition on rotary wings [5].

The XV-15 rotor configuration consists of three highly-twisted blades and a simplified hub. Table 2 lists the rotor geometric properties and airfoil shapes at several radial stations. In the absence of data, we assumed a surface roughness

Table 2 XV-15 rotor geometric properties and airfoil distribution.

Parameter	English	Metric	Radial station r/R	Airfoil
Rotor radius R	150 in	3.81 m	0.09	NACA 64-935
Blade-tip chord c_{tip}	14 in	0.356 m	0.17	NACA 64-528
Rotor solidity σ		0.089	0.51	NACA 64-118
Non-linear blade twist θ_{tw}		-40.9°	0.80	NACA 64-(1.5)12
Hub precone β_p		1.5°	1.00	NACA 64-208

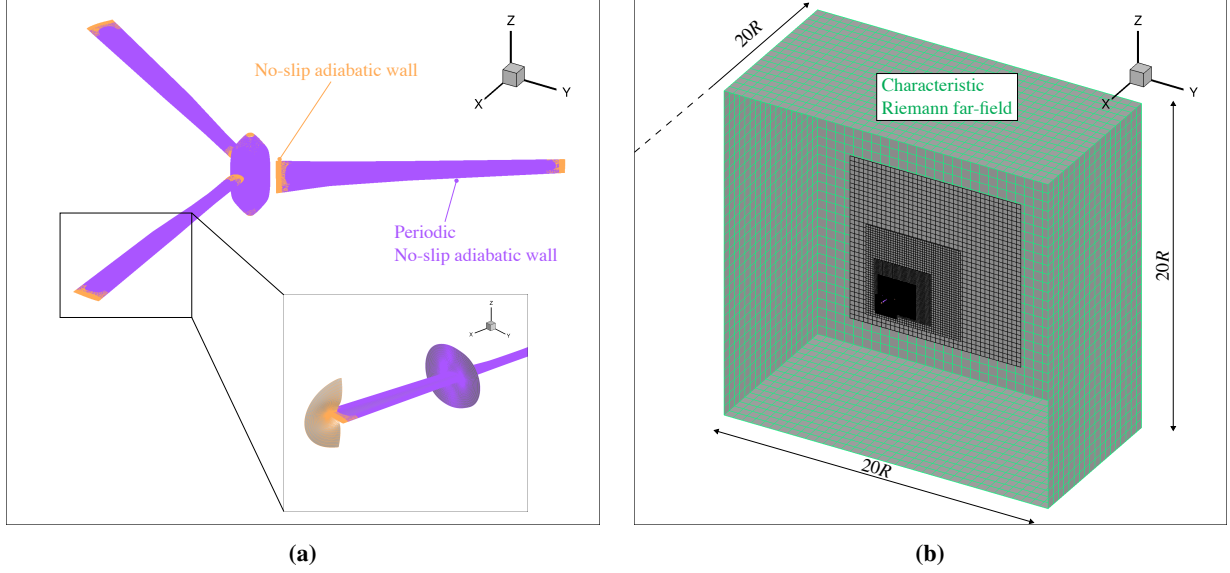


Fig. 6 Structured overset grids for the full-scale XV-15 rotor: (a) near-body grids and (b) off-body grids.

of $h = 3.3 \mu\text{m}$. To efficiently reutilize the grid system for various collective angles, we leave a small gap between the blades and the hub. As shown in Fig. 6, three NB curvilinear grids are utilized to resolve the flow near the blades: one O-grid for the main surface and two cap grids for the root and the tip. The main grid contains 263 points around the airfoil, 121 points in the spanwise direction, and 95 points in the wall-normal direction. To resolve strong pressure gradients, we densely distributed points in the leading and trailing edges, with spacings of $0.01 \%c$ and $0.04 \%c$; and in the vicinity of the root and tip, with a spacing of $0.01 \%b$, where c is the local chord length and b is the span length. Grid spacing near the walls maintains $y^+ \leq 1$ to achieve sufficient boundary layer resolution. The blade tip cap grids consist of 171 points from leading edge to trailing edge, 89 points from upper to lower surface, and 95 points in the wall-normal direction. Similarly to the blade, the hub's primary surface and singular axes are covered with three grids of the same topology. The NB volume grids are extruded so that the outside limits lie outside of the boundary layer. The NB grids are embedded within a Cartesian OB grid system with uniform spacing. The first level of resolution, aimed at resolving the rotor wake, has a grid spacing of $10 \%c_{\text{tip}}$. Successive levels of refinement, each twice as coarse as the preceding, efficiently expand the grid system to the far-field, which is $20R$ away from the center of the rotor in all directions. The various grids are interfaced through hole cutting, grid blanking, and donor stencil search operations. The resulting grid system consists of 47.2×10^6 grid points in total, of which 18.6×10^6 correspond to the NB grids.

In this work, we simulate the XV-15 rotor in hover for low- and high-thrust conditions —determined by collective pitch angles of $\theta_0 = 3^\circ$ and 10° , respectively— at sea level in standard atmosphere. The Reynolds number based on the tip chord and blade tip velocity is $Re = 4.925 \times 10^6$, and the blade tip Mach number is $M_{\text{tip}} = 0.69$. The computations are initialized by a quick-start procedure to establish the rotor flow, advecting the initial transient out of the solution domain. The initial start-up is run for 2880 steps with a physical time step corresponding to 2.5° of rotor rotation, allowing for 20 rotor revolutions. The simulation is then reverted to the time-accurate set of parameters, with a physical time step of 0.25° of rotor rotation, for which each revolution consists of 1440 steps. The simulations are stopped when

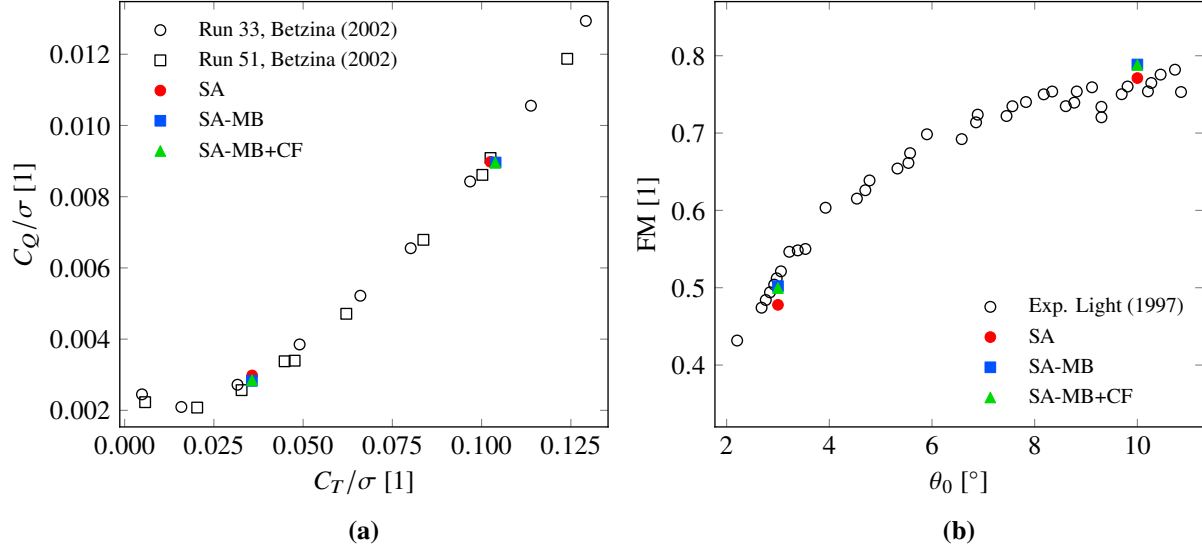


Fig. 7 Aerodynamic performance of the full-scale XV-15 rotor in hover at $M_{tip} = 0.69$: (a) solidity-normalized torque coefficient versus solidity-normalized thrust coefficient and (b) figure of merit versus collective pitch angle. Experimental data from Betzina [37] and Light [36].

the forces and residual convergence are satisfied, which usually occurs after 15 or 20 rotor revolutions in time-accurate mode. In this case, we only consider the solution of the MB models and we leave the AFT models for a future study. It should be noted that the transition correlations utilize variables derived from the local velocity. Consequently, this approach violates Galilean invariance. However, OVERFLOW provides an option to activate the Galilean invariance modification, which uses the velocity relative to the grid as an approximation to the velocity relative to the wall.

Fig. 7 compares the predicted and experimental aerodynamic characteristics and performance of the XV-15 rotor. Fig. 7a shows a plot of torque coefficient C_Q/σ versus thrust coefficient C_T/σ , both normalized by rotor solidity σ . The computational results are obtained with the SA, SA-MB, and SA-MB+CF turbulence models, whereas the experimental data is taken from Betzina [37]. The predicted aerodynamic coefficients show similarity between the fully-turbulent and transition models, aligning relatively well with experimental observations. For a fixed collective pitch angle θ_0 , the use of transition models results in a slight reduction of C_Q/σ , as the profile drag decreases in the laminar regions of the blade. Fig. 7b shows a plot of the figure of merit FM against the collective pitch angle θ_0 . In this case, the experimental data is taken from Light [36]. The FM is highly sensitive to variations in C_Q/σ and C_T/σ , as even small changes in these coefficients can lead to significant differences in rotor performance. Chaderjian [38] reported that an error of 0.5 % in the prediction of FM could result in a reduction of the helicopter's payload by one passenger. The use of transition models results in a higher FM prediction due to the lower C_Q/σ . However, we only observe significant improvements when the rotor operates in the low-thrust condition, where the fully-turbulent model tends to underpredict FM. In the high-thrust condition, the predicted FM appears to be slightly overestimated. At higher collective angles, we expect to see smaller laminar regions with a less pronounced effect on performance prediction. In both cases, for both the prediction of aerodynamic loads and rotor performance, the crossflow correlations seem to have minimal influence.

Wadcock et al. [5] applied the oil-film interferometric skin friction technique to obtain the chordwise skin friction coefficient distribution on the upper surface of the blade at six radial stations. Their work revealed laminar leading edge separation bubbles, laminar-to-turbulent transition, and reverse flow regions. In Figs. 8 and 9, we plot the skin friction coefficient c_f versus the chordwise coordinate x/c for the radial stations $r/R = 0.83$ and $r/R = 0.94$. Here, the skin friction coefficient c_f is normalized with the local blade velocity Ωr . The computational results obtained with the SA, SA-MB, and SA-MB+CF turbulence models are compared to the experimental measurements of Wadcock et al. [5]. For the lower collective pitch angle $\theta_0 = 3^\circ$, at $r/R = 0.83$ and $r/R = 0.94$, laminar flow extends over a large portion of the blade, where c_f monotonically decreases with the distance from the leading edge. Experimental measurements indicate that the flow becomes turbulent between $x/c = 0.5$ and $x/c = 0.6$, resulting in a sudden rise of c_f , followed by a steady decrease towards the trailing edge. This behavior is only captured by the SA-MB and SA-MB+CF models, which provide similar estimations of c_f . For the higher collective pitch angle $\theta_0 = 10^\circ$, stronger pressure gradients

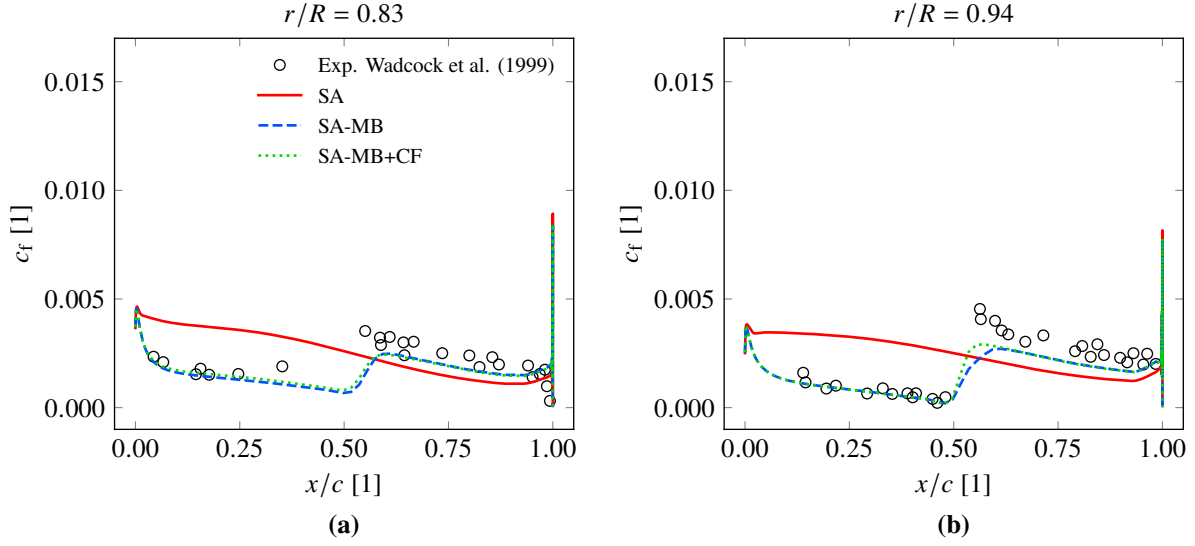


Fig. 8 Distribution of chordwise skin friction coefficient along the upper surface of the XV-15 rotor blade in a low-thrust hovering condition, $\theta_0 = 3^\circ$ and $M_{\text{tip}} = 0.69$: (a) $r/R = 0.83$ and (b) $r/R = 0.94$. Experimental data from Wadcock et al. [5].

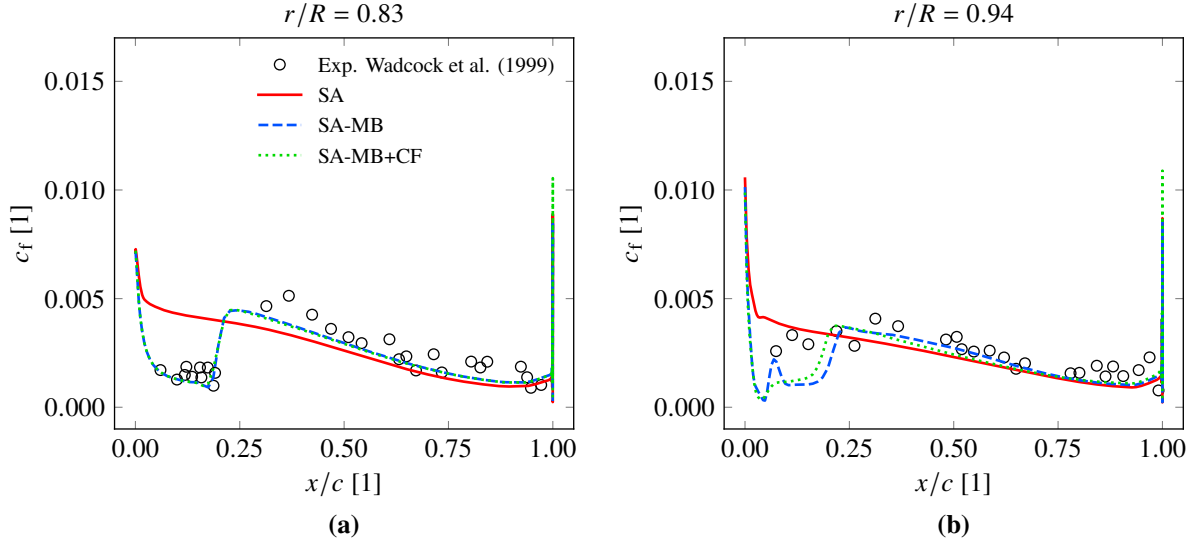


Fig. 9 Distribution of chordwise skin friction coefficient along the upper surface of the XV-15 rotor blade in a high-thrust hovering condition, $\theta_0 = 10^\circ$ and $M_{\text{tip}} = 0.69$: (a) $r/R = 0.83$ and (b) $r/R = 0.94$. Experimental data from Wadcock et al. [5].

result in earlier boundary layer transition. At $r/R = 0.83$, experiments indicate that transition occurs at $x/c \approx 0.25$. The laminar flow region in the initial quarter chord is well predicted by the SA-MB and SA-MB+CF models, whereas in the turbulent flow region all models provide similar c_f estimations. At $r/R = 0.94$, the flow is presumably turbulent from the leading edge, and the c_f evolution is better captured by the SA model. The SA-MB and SA-MB+CF models predict laminar regions up to $x/c \approx 0.20$. The crossflow correlations appear to have a minimal impact on the transition location—specifically, a slight forward movement of the transition line in the $r/R = 0.94$ section, possibly due to the influence of the tip vortex—, suggesting that the transition phenomenon is dominated by streamwise instabilities in the boundary layer.

IV. Concluding Remarks

This paper explored the use of high-fidelity CFD to simulate laminar-to-turbulent boundary layer transition in the full-scale XV-15 rotor in hover. In a rotor blade, the combination of geometric sweep, pressure gradients, and rotor rotation creates a crossflow component of the velocity within the boundary layer, which can lead to an accelerated transition. To assess the impact of crossflow-induced transition on rotor performance prediction, the Amplification Factor Transport transition model and Medida-Baeder’s local correlation-based transition model were coupled with the algebraic crossflow models of Langtry et al. and Müller and Herbst, respectively. The crossflow models rely on the helicity parameter to measure the alignment of the vortices with the main flow, quantifying the crossflow strength within the boundary layer. The implementation of these models into NASA’s OVERFLOW CFD code is described. A validation study was conducted for standard test cases of crossflow-dominated transition: NLF(2)-0415 45° swept airfoil and TU Braunschweig sickle wing. From the results presented, the following conclusions can be made:

- For the validation cases, the crossflow-extended transition models provided an accelerated path for boundary layer transition in regions with strong secondary flows. When compared to experimental data, the transition front predictions of the crossflow-extended models significantly improved those of the baseline models.
- The crossflow models follow a local formulation approach to extend their applicability to arbitrarily complex three-dimensional geometries. However, no single transition model is ideal for the full spectrum of applications, as observed in the discrepancies for the TU Braunschweig sickle wing. This underscores the challenge of developing a universally applicable model across diverse scenarios.
- For the XV-15 rotor, the impact of crossflow-induced transition appeared to be minimal, suggesting that laminar-to-turbulent transition occurs fundamentally due to streamwise instabilities. Baseline and crossflow-extended transition models provided a similar estimation of rotor performance, which improved the fully-turbulent solution, especially at the low-thrust condition. Similarly, both predicted the same extent of laminar flow over the blade surface.

In summary, crossflow-induced transition predictive capabilities were added to NASA’s OVERFLOW. The discrepancies in the results when different turbulence models were applied to the same case indicate that opportunities for improvement still exist. Future work will consider the application of these models to different rotor geometries.

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