

Boundary Layer Relaxation Effects with Actuator Line Turbulence Correction

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Boundary Layer Relaxation Effects with Actuator Line Turbulence Correction

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The turbulence in the viscous wake of propeller blades impinging on the wing affects the laminarity of the wing. A turbulence correction term is implemented for the actuator line method in the DLR-TAU code, adding turbulence kinetic energy due to propeller into the flow-field. The propellers are, then, able to trigger transition on the wing surfaces with available correlation-based transition models. To validate the approach and investigate the transition scenario, a propeller-wing configuration in a wind tunnel is investigated using the DLR- γ 1-equation model and experiments. We show that the numerical predictions of the calmed regions and extended laminar runs on a wing due to a propeller are possible. Intermittent shifts in transition location indicate periodic boundary layer relaxation (calmed regions) between blade passage. Some sections show delayed transition until 80% chord, whereas the central sections benefit less. Boundary layers temporarily attain a laminar-like state until about 75% chord, which is downstream of the natural transition location without the propeller. The shape and behavior of the predicted unsteady transition front agrees well with the measured intermittency profiles. Similarly, the measured aftmost transition locations agree well with the CFD predictions showing that the maximum laminar flow benefit can be reasonably predicted.

Nomenclature

α	=	angle of attack
c	=	chord [m]
C_1	=	turbulence correction coefficient [-]

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C_D	=	drag coefficient
C_f	=	skin friction coefficient
C_L	=	lift coefficient
C_μ	=	turbulent viscosity constant
C_T	=	thrust coefficient
D	=	propeller disc diameter [m]
δ_1	=	displacement thickness [m]
δ_2	=	momentum thickness [m]
Δr	=	blade section length in radial direction [m]
d.c.	=	drag count
DP	=	distributed propulsion
γ	=	intermittency variable
h	=	height [m]
H_{12}	=	shape factor
J	=	advance ratio [-]
k	=	turbulence kinetic energy [m^2/s^2]
k^*	=	blade generated turbulence kinetic energy [m^2/s^2]
ξ	=	calibration coefficient
MAC	=	mean aerodynamic chord [m]
n_{Prop}	=	propeller number of revolutions [RPM]
ω	=	specific dissipation rate [1/s]
Ω	=	Propeller rotational speed [RPM]
$p(t, \phi)$	=	measured static pressure signal [Pa]
$\bar{p}(\phi)$	=	phase averaged static pressure signal [Pa]
$p'(t, \phi)$	=	residual static pressure fluctuations [Pa]
ψ	=	propeller azimuthal angle [deg]
Q_k	=	turbulence due to propeller [m^2/s^2]
R	=	propeller disc radius [m]
ρ	=	density [kg/m^3]
$\text{Re}_{\theta t}$	=	momentum thickness Reynolds number
S	=	sample standard deviation
TKE	=	turbulent kinetic energy [m^2/s^2]

U	=	velocity magnitude [m/s]
U_∞	=	inflow freestream velocity [m/s]
u_τ	=	friction velocity [m/s]
x, y, z	=	cartesian coordinates [m]
y^+	=	non-dimensional wall distance

Subscripts

av	=	average
e	=	quantity at the end of the boundary layer
norm	=	normalized
ref	=	reference
s	=	sectional quantity on one side

I. Introduction

AIRCRAFT energy usage is set to reduce as much as 50% by 2035 [1]. Considering the fact that aviation sector forms a pillar of the global trade and transportation, this target requires continuous improvements in technology, which could be a combination of various factors. Distributed propulsion (DP) systems have recently caught researchers' attention. In these configurations, smaller propulsion systems are distributed throughout the span of the wing, rather than bulkier conventional propulsors. The spanwise distribution of the propulsive elements is aimed to benefit the aircraft mission in the form of efficiency increase (e.g. aerodynamic, propulsive, structural, and so on) [2].

A distributed propulsion system which has a spanwise distribution of propellers in a tractor arrangement will have most of its wing submerged in the propeller slipstream. The effect of propellers on the trailing wing can be beneficial, but also detrimental. The benefits include the increased high-lift performance of DP, as shown by Firnhaber-Beckers *et al.* [3]. The lift augmentation can range between 50% to 60% while the propulsive efficiency decreases. Similarly, Keller [4] has shown that the DP systems are more effective during take-off than in cruise. Ribeiro *et al.* [5] has carried out very large eddy simulations of a propeller-wing-flap configuration to characterize the propeller-wing interactions at high-lift. DP under high-lift conditions are also investigated experimentally by Duivenvoorden [6] and Duivenvoorden *et al.* [7, 8]. The slipstream starts to deform significantly before physically interacting with the wing under high-lift conditions [8]. Additionally, DP systems improve the swirl recovery of the wing due to larger slipstream (due to multiple propellers), and allow reduction of the wing sizes [4]. However, the downsides must also be considered. Viscous drag, for example, increases in the propeller slipstream. Schollenberger *et al.* [9] show that for a full spanwise distribution of propellers yield higher viscous drag than that of partial DP for constant disk area. Another reason is the non-uniform lift distribution overcompensating for swirl recovery [10]. Thus, it is expected to have higher viscous drag increase for

larger slipstreams, which is the case for distributed propellers.

The effect of slipstream of a propeller on the wing is an active research topic since the early days of flight. Elevated levels of turbulence in the slipstream is observed by Hood and Gaydos [11] for an airfoil in wind tunnel as early as 1939. The change in the transition onset is measured using flight tests by Young and Morris [12] using total pressure probes. They concluded that the (mean) transition point moves closer to the leading edge in the region submerged in propeller slipstream.

Thus, the propeller slipstream is initially thought to be fully-turbulent due to increased turbulence levels & upstream transition point. Zalovcik and Skoog [13] have measured the transition location and its movement from 50% chord to 25% chord in the slipstream region for a NACA 66 series airfoil, which indicated a significant loss of laminar flow.

The mean effect of the propeller causes earlier transition when a wing is submerged in the propeller slipstream [14]. Holmes and Obara [15], and Holmes *et al.* [16] have experimentally determined the boundary layer transition locations using sublimating chemicals. They have observed the movement of the transition point from 40% chord to 28% chord in the slipstream region of Bellanca Skyrocket II aircraft. The authors have also shown that the flow is not entirely turbulent, but also spends some time in laminar regime. That is, the boundary layer becomes turbulent as the turbulent blade wakes impinge on the wing, indicating a cyclic behavior in the boundary layer.

Holmes and Obara [15] posit that there exist regions with *thickened* laminar boundary layers which appears to be turbulent due to propeller slipstream. However, these regions are determined to be not turbulent by their analysis. They point out that comparing the velocity profile shapes and the boundary layer thicknesses are not enough on their own. The "turbulent-appearing" time-averaged boundary layer profile [15] in the laminar region of the propeller slipstream is explained by cyclic impingement of the turbulent wake shed by the propeller trailing edge which travel downstream in a coherent fashion. They hypothesized that the area enclosed by turbulent regions could be "nominally" laminar, which is also confirmed with the experiments carried out with a T-34C airplane. Their analysis suggests that the drag rise due to propeller slipstream is significantly less than that of complete loss of the laminar flow.

The laminar region between turbulent packets was observed over a laminar wing glove that was designed as part of a joint venture between DLR and Dornier Aircraft for Do228 turboprop aircraft [17]. The authors present time histories of boundary layer states obtained with hot-film signals in flight. These signals suggest that the laminar regions are separated by turbulent wave packets traveling downstream. The laminar flow is shown to extend until 33% and 30% chord on upper and lower surfaces on the laminar glove, respectively, as opposed to 55% chord outside the slipstream.

In the general transition process, turbulent spots are produced and propagated downstream within the laminar boundary layer. Calmed regions (also known as *becalmed regions*) form behind the turbulent spots. The calmed region is a laminar-like region that is present immediately after the turbulent spot [18] or strips [19] with laminar-like velocity profile. Calmed regions are identified as regions of decreasing wall shear stress behind the turbulent spot. The wall shear levels for calmed regions are slightly higher than the laminar state, but more importantly, lower than the turbulent

state [20]. The boundary layers relax (called boundary layer relaxation in this manuscript) into a laminar-like state following the turbulent spot, with higher shape factors [20]. Calmed regions are more resilient to flow separation due to higher shape factors; where the reduction in shear stress levels (from turbulent to laminar) makes the boundary layer more stable than its surroundings. The drag reduction effect (even below than that of undisturbed laminar values) is also reported by Howard and Miley [21] for a propeller slipstream. Moreover, the calmed regions are inaccessible to the Tollmien–Schlichting (T-S) waves originating upstream of the turbulent spot inception point [20]. The T-S waves convect about 30% of the local freestream velocity. The turbulent spot leading edge and trailing edge move about 80% and 50% of the local freestream velocity, respectively. In the end, calmed regions eliminate any tendency for flow separation and significantly delays transition [20]. The delayed transition effect is also reported by Miley and Howard [22].

Gostelow *et al.* [23] have experimentally investigated the calmed region behavior behind a turbulent spot. The extended laminar runs over a compressor stator suction surface are observed. The shape factor (also the integral parameters) vary as the turbulent spot passes a point in space and a calmed region is formed. In the experiment, H_{12} decreased from values above 3 to values below 1.8 as the spot passed. Subsequently, the H_{12} values gradually relax back to their unperturbed values.

The existence of laminar flow between traveling turbulent regions is also shown experimentally by Miley *et al.* [22] and Howard *et al.* [24] for NACA 0012 airfoil at various angles of attack. Reynolds number based on the chord length was 6×10^5 . With propeller on, laminar flow regions were intermittently detected as downstream as 60 to 70% chord, as opposed to transition occurring at early as 40% chord with propeller off. Extended laminar runs due to the propeller wake passage cycle is observed. The growth of the turbulent part of the cycle increases as the laminar stability decreases in the chordwise direction. The authors indicate that the boundary layer goes through transition (laminar to turbulent) and then reverse-transitions (turbulent to laminar) in the wake passage cycle. Miley *et al.* [22] showed similar H_{12} correlations between their measurements and that of re-laminarizing boundary layers and low-Reynolds-number laminar boundary layers under external turbulence.

Howard and Miley [21] have extended the work of Miley *et al.* [22] with wind-tunnel and flight tests. They indicated that the slipstream could "carry" laminar flow between highly turbulent disturbances which could also reduce the drag. The authors have used Squire and Young's method to determine the profile drag until a specified station using integral boundary layer parameters. The obtained drag values had cyclic behavior for a propeller rotation cycle. The propeller wake traveling through a point on airfoil is accompanied by drag increase, followed by the return to lower levels of drag. The profile drag time history had lower values than that of fully turbulent flow, and similar to laminar flow. Their conclusion was that the flow in slipstream provides an *energizing effect*, which favorably affects the stability of the wing boundary layer. Not only that, but boundary layers exposed to high external turbulence levels can become stable boundary layers energized by the slipstream effects depending on the adversity of the mean pressure gradient [21].

In 1990, Renoud and Howard have studied the boundary layer response to a periodically turbulent flowfield [25]. They have investigated a wing with a circular rod acting as a freewheeling propeller in front. The wing sections were of a helicopter tail rotor, and the chord based Reynolds number was 5×10^5 . The rod was spun at 1500 RPM with 0.18% ambient turbulence intensity. Even though the flow field is three-dimensional, the crossflow velocity components were found to be about 10% of the streamwise mean, hence a quasi-two-dimensional analysis is carried out by the authors. A turbulent pulse passage is followed by a recovery period to the undisturbed state. The laminar to turbulent transition is observed, which is followed by formation of a calmed region. The laminar flow is observed as downstream as 70% chord, which is not expected on this type of airfoil. During the turbulent wake passage, the boundary layer thickens by as much as three times, and thins to 40% of its undisturbed value during the transition and re-transition periods. Momentum thickness Reynolds number sharply increases to about twice its original value during the wake passage, indicating the tendency of the flow to transition into a laminar state.

The setup of [25] is numerically replicated by Sarikaya *et al.* [26] using a two-dimensional setup which has a moving rod in front of an airfoil. The authors have shown that the boundary layer relaxation (calmed regions) can be predicted using transition models (DLR γ and $\gamma\text{-Re}_{\theta t}$) already available in the DLR TAU code, which is developed by German Aerospace Center (DLR) [27]. There, the disturbed inflow caused by the moving rod causes the transition into turbulence, then the passage of the wake leaves behind a calmed region. They also showed that the boundary layers relax after the passage of the wake, i.e. calmed regions show laminar-like velocity profiles. It was shown that the transition can be delayed up to approximately 75% chord location as opposed to a natural transition location of $\approx 45\%$.

At the Institute of Aerodynamics and Gas Dynamics (IAG), joint numerical and experimental studies on the impact of distributed tractor propellers on the laminarity of the wing boundary layers are conducted in the frame of the SynTrac collaborative research center* funded by the German Research Foundation (DFG). In this paper, an actuator line turbulence correction method is implemented in DLR-TAU code for propellers solving URANS equations, based on an already published work for wind turbines [28]. The implemented turbulence correction increases the turbulent kinetic energy level in the slipstream, whereas the turbulence levels do not increase without the correction. Currently, all the studies available in the literature use fully-turbulent flow-field assumption for *propeller-wing interaction cases*. However, a transition model must be used in RANS solutions to resolve the boundary layer relaxation / calmed region effects. To the authors' best knowledge, this paper constitutes the first numerical work about propeller-wing interactions using a transition model for the investigation of the calmed regions/boundary layer relaxation effects. We aim to answer whether an actuator line method, adequately enriched with the modeled turbulence, is sufficient to predict the calmed regions using a correlation-based transition model. In Section II, we discuss the methodology used such as numerical modeling, modifications to the γ transition model and turbulence correction approach. In Section III, we present the results for the implementation of the turbulence correction in unsteady cases and discuss the boundary layer relaxation

*<https://www.trr-syntrac.com>

effects. Additionally, we compare the original DLR- γ model and the modified γ model; and their response to elevated levels of turbulence in slipstream. Finally, we compare the numerically predicted unsteady transition behavior to that of the measurements carried out in the IAG Laminar Wind Tunnel. Finally, the conclusions drawn and the outline of the future work are presented in Section IV.

II. Method

We introduce the experimental setup briefly in the Section II.A. The numerical methods employed in the present work are introduced next in Section II.B. The modifications proposed to the DLR- γ 1-equation transition model are briefly summarized in Section II.C. Then, a brief discussion of actuator disk/line methods take place in Section II.D. Finally, in Section II.E, the turbulence correction approach for actuator disk and actuator line methods are explained.

A. Experimental setup

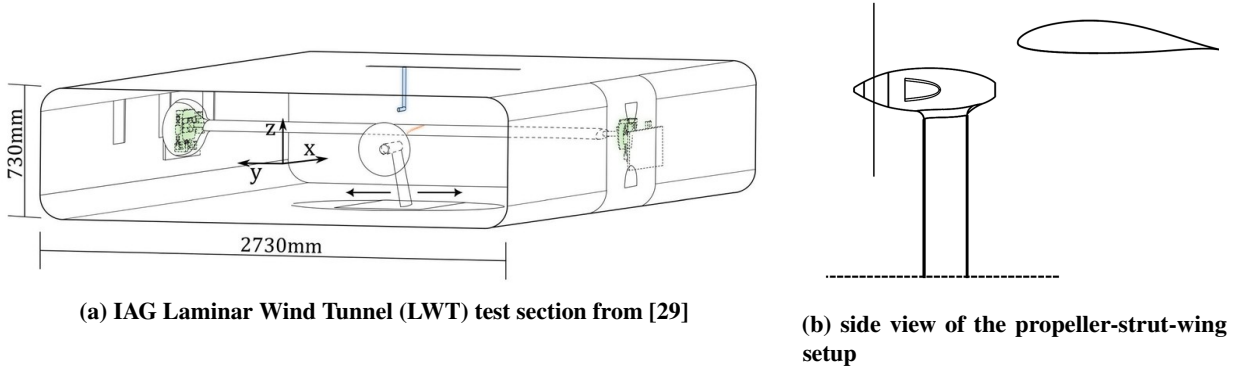


Fig. 1 The experimental setup

Experimental measurements were conducted at the Laminar Wind Tunnel (LWT) of the Institute for Aerodynamics and Gas Dynamics at the University of Stuttgart parallel to the numerical investigations. The LWT test section has a width of 2730 mm and a height of 730 mm. The chord length of the model was $c = 350$ mm and the angle of attack of the airfoil was $\alpha = 2^\circ$. The measurements were conducted at Reynolds numbers in the range of $1.264 \times 10^6 < Re < 1.275 \times 10^6$ which at the given weather conditions corresponded to a freestream velocity of $54.16 < U_\infty < 56.42$ m/s. The airfoil profile used in the experiments was the DP-FR-1924-MODLWT [30] NLF airfoil. A two-bladed propeller, driven by an electric motor, was mounted on a pylon attached to a traversing base in front of the airfoil, as seen in Fig. 1. The propeller has a diameter of $D = 300$ mm corresponding to a radius of $R = 150$ mm. The advance ratio, defined as

$$J = \frac{U_\infty}{nD} \quad (1)$$

was maintained in the range of $2.11 < J < 2.17$ which corresponded in this case to a rotational speed of $5110 < n_{Prop} <$

5327 RPM. The propeller rotation, seen from upstream, looking towards the downstream direction was clockwise, therefore a positive spanwise position on the airfoil where $y/R > 0$ corresponds to a position behind the upward moving or rising propeller blade whereas a position of $y/R < 0$ corresponds to a position behind the downward moving blade. The rotational position of the propeller was measured at all times by a Megatron HTA36X hall-effect rotation sensor. By measuring the angular position, a specific propeller phase angle can be obtained for each data sample afterwards, and thus phase averaging of the data is possible.



Fig. 2 The propeller used in the experiments

Static surface pressures were measured on the airfoil's suction side surface in the areas submerged in and affected by the propeller slipstream. For the steady and unsteady pressure measurements, 30 pressure taps connected to high-sensitivity Kulite XCS-SL-062- ± 5 D pressure sensors in an infinite-tube configuration were used. The positions of the 30 pressure taps are shown as red circles in Fig. 3. To avoid disturbing the following sensors downstream, the tap locations were varied in the spanwise direction while advancing in the streamwise direction. In addition to the pressure taps, one sensor was flush-mounted to the airfoil surface in order to obtain more detailed information about the frequency-response and damping characteristics of the other, infinite-tube connected sensors. The flush-mounted sensor sits at the same streamwise location as another pressure tap, highlighted in blue in Fig. 3.

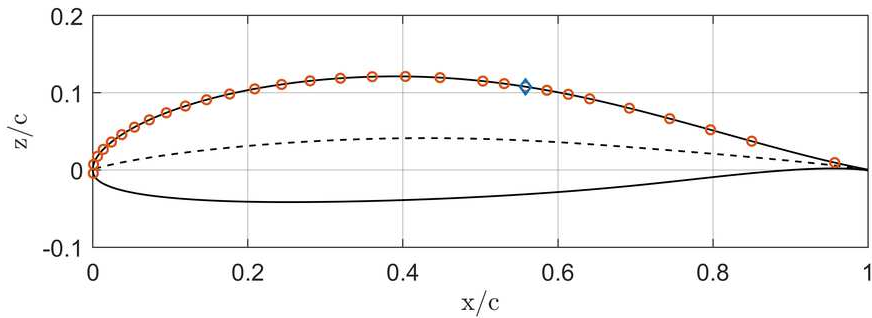


Fig. 3 Pressure tap positions on the airfoil

Although only one row of sensors exists, an array of sensors in the spanwise direction can be emulated by conducting multiple measurements with the propeller fixed at different spanwise positions. That way, measurement data at different spanwise offsets relative to the propeller centerline from $y/R = -1 \dots 1$ can be obtained. The increment in normalized spanwise position was $\Delta y/R = 0.1$. In order to obtain the information from each sensor, the propeller has to be moved to

an individual position for each pressure tap and for each radius, which results in a total grid of $31 \times 21 = 651$ (counting also the flush-mounted sensor) measurement points, each point with an individual measuring time of 30 s. In addition to the surface pressures, a Vectoflow FRAP-Pro five-hole probe traversing in y- and z-direction was mounted downstream of the wing to gain information about the pressures, velocities and flow directions in the wake region. In order to measure lift and drag, a force balance system was connected to the wing tunnel model. The acquired data from the force measurement system was not reliable enough to be used for serious quantitative analysis. The sampling rate was set to 100 kHz for each measurement and the data were collected using three synchronized DT9857E-16-XAO Sigma-Delta-ADCs. For post-processing, the data were downsampled to 20 kHz by block-averaging groups of five consecutive samples. More details about the setup can be read in [29].

In order to obtain information about the intermittency γ of the boundary layer of the airfoil surface the value of the moving sample standard deviation

$$S = \sqrt{\frac{1}{N-1} \sum_{i=1}^N |x_i - \bar{x}|^2} \quad (2)$$

is used. In a first step, the pressure signal for each sensor is phase averaged with a resolution of 1° . In the next step, the phase averaged signal is used to interpolate the phase average at the angle of each measured sample $\bar{p}(\phi)$. This value is then subtracted from the directly measured value $p(t, \phi)$. The new value is now referred to as the residual pressure $p'(t, \phi)$:

$$p'(t, \phi) = p(t, \phi) - \bar{p}(\phi) \quad (3)$$

In theory, assuming perfect periodicity, only the turbulent fluctuations of the signal should remain, whereas deterministic periodic pressure components induced directly by the propeller are eliminated from the new residual signal. The moving sample standard deviation of this residual pressure is then calculated around each sample with a window of 11 samples. Values above a threshold of 1.25 Pa are considered to indicate a turbulent or transitional boundary layer and therefore assigned to $\gamma_{unsteady} = 1$ whereas values below the threshold are considered to indicate a laminar boundary layer and assigned to a value of $\gamma_{unsteady} = 0$. Finally, these binary local and temporary intermittency values of one for turbulent/transitional and zero for laminar boundary layer are again phase averaged with a reduced resolution of 6° . The resulting values represent the intermittency γ over the full measurement duration for a specific propeller phase angle. The values for the threshold were established beforehand in reference measurements without the propeller in a clean and a tripped boundary-layer configuration. The experimental intermittency measurements will be used to validate the numerical approach in Section III.H.

B. Numerical methodology

The numerical setup replicates the complete experimental set-up. The simulations are carried out using the DLR-TAU code (version 2021), which is developed by German Aerospace Center (DLR) [27]. TAU can solve Euler or Reynolds-Averaged Navier Stokes (RANS) equations on hybrid grids utilizing a dual-grid approach. The solutions can be obtained for steady and unsteady cases. We solve the unsteady RANS equations, except where noted otherwise. In the unsteady simulations, dual-time stepping is employed to advance time. LUSGS iterations are utilized in each time loop.

The air is modeled as an ideal gas and the closure is obtained using the 2-equation SST $k-\omega$ 2003 model [31], and the transitional effects are modeled using the DLR γ model already implemented in the TAU code [32]. The DLR γ model is a 1-equation transition model that provides increased accuracy in the prediction of transition onset location compared to the $\gamma-Re_{\theta t}$ model as shown in François *et al.* [32]. Sarikaya *et al.* [26] have also demonstrated the capabilities of the DLR- γ model for a 2-D airfoil configuration under disturbed inflow with elevated turbulent kinetic energy levels, where the calmed region / relaxed boundary layer phenomena are presented for a 2-D case.

We use second-order accurate spatial fluxes (central differencing) and second-order Roe formulation on turbulent quantities. Multigrid is employed to accelerate convergence. Cauchy convergence criteria is used to assess convergence. A timestep is assumed to be converged when the drag and lift values change less than 0.01% in the last 50 iterations. We note that the density residual drops by 6 orders of magnitude for the steady case.

The Reynolds number based on the chord is 1.25 million, and the turbulence intensity is set to 0.034% (N-factor between 11 and 12). The atmospheric conditions at the entrance are taken equal to an altitude of $h = 465$ meters at the inlet.

We use a hex-dominant unstructured grid with about 77 million cells and 51 million points is generated using Cadence Pointwise meshing software[†]. Structured surface grids are used for actuator disk, wing and propeller strut surfaces as well as applicable regions on the wind tunnel. Propeller spinner and its connection to strut, wing-test section junctions are meshed using unstructured surface grids. The wing is discretized with 350 points in the spanwise direction and with 200 points in each surface to have a reasonable amount of resolution in streamwise direction. Leading and trailing edges have fixed spacings of about 0.7% MAC. The actuator disk surface consists of 361 points in azimuthal and 225 points in the radial direction.

The targeted y^+ on the wing surface is 0.25, with a first layer spacing of 0.0015 mm. The same first layer spacing is used to advance prisms from the wind tunnel walls, the strut, and the wing with 70 layers employing 1.15 growth rate. T-Rex algorithm is employed to fill the whole volume using hexa-dominant cells (voxel algorithm). This allows to resolve the boundary layer effects with sufficient accuracy as well as the transition effects.

There is a refinement block downstream of the propeller to refine the slipstream as depicted in Fig. 4. The spacing in the slipstream is set to 1.75% of the propeller radius (≈ 2.6 mm). Previous experiences in the research group indicate

[†]<https://www.pointwise.com/>

that slipstream voxel spacing should be close to 1.5% radius for sufficient vorticity resolution in the slipstream. Beckers *et al.* [33] utilizes a spacing of about 1.6% radius, and Schollenberger *et al.* [10] similarly utilizes $\approx 1.5\%$ radius for the grid-converged solutions. Since the aim of this study is to show the implementation effects of turbulence correction, the grid refinement level (voxel spacing of 1.75% radius) is considered sufficiently fine; hence, a grid convergence study is not carried out.

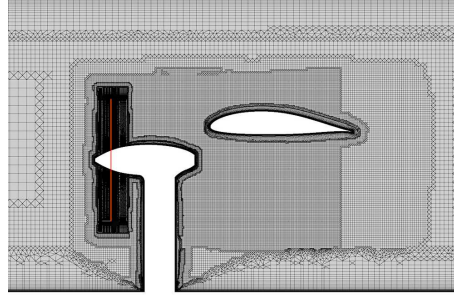


Fig. 4 Centerline cut of the mesh, propeller is highlighted in orange

C. Modifications to DLR γ transition model

The modification proposed by Soliman *et al.* [34] to the DLR- γ model is used. The goal of the modification is to fix the transient response of the original DLR- γ model [32] to the changes in the flow-field. Generally speaking, the 'changes' are the transient response of the transition front to events such as turning the propeller-off. The following paragraphs describe the modifications without going into too much detail.

Before we proceed with the summary, we note that the DLR- γ model was initially designed to only take the free-stream turbulence intensity into account [32] and not the local turbulence intensity. However, Sarikaya *et al.* [26] have previously shown that disturbed inflow can "bypass" this limitation, and the model can form laminar regions which immediately follow the transition front. In [26], it was theorized that the turbulent kinetic energy and pressure gradients interact with the boundary layer, and the intermittency variable to trigger transition, which then produce boundary layer relaxation effects.

The proposed modifications aim to fix the so-called "artificially fixed" transition locations in some cases. The imbalance in destruction and production terms cause the transition location to be stuck in an upstream location, despite the transition criterion indicating a more downstream transition location. The destruction term in the intermittency equation is modified as a remedy.

The new destruction formulation is activated when the transition criterion is not satisfied (i.e. $F_{\text{onset}} < 1$). The new formulation is deactivated as the criterion is satisfied ($F_{\text{onset}} \geq 1$), and the original destruction formulation is recovered. Thus, the modified model proposes an improved ability to show changes in the intermittency field due to changes in flow-field. For further details see [34]. Henceforth, this model is called the "modified γ model" in this study.

D. Actuator disk and actuator line methods

The effect of the propellers on the flow-field can be modeled in TAU code using three approaches. The first method is the actuator disk method (ACD), where a steady-state force reaction can be directly specified or calculated based on the blade element momentum theory approach [35]. The details of the actuator disk implementation in TAU code are given in Raichle *et al.* [36]. The actuator disk method can reasonably capture the lift, thrust and power coefficients for propeller-wing systems. However, there are some interactions which cannot be resolved by ACD alone, such as the unsteady propeller-wing interactions.

When unsteady interactions of propeller with the wing are of interest, the actuator line method (ACL) is more suitable. The unsteady effects such as blade tip vortices and hub vortices due to unsteady motion of the propeller can be modeled this way. ACL is an inherently transient method which incurs more computational cost compared to ACD. Both ACD and ACL requires the prescription of propeller polars to benefit from the blade element momentum theory. In the present study, propeller polars are generated using the methodology given in Refs. [37, 38]. To briefly summarize the method, 3-D, single blade simulations are conducted for different inflow velocities using periodic boundary conditions. This method has the advantage of including 3-D effects like Himmelskamp effect at the root, and the tip effect. Then, the local inflow angles are extracted for various radial sections. After that, the local aerodynamic coefficients (sectional C_L and C_D) are determined. The details regarding the polars are given in Sarikaya *et al.* [39].

We use the new actuator line implementation by Beckers *et al.* [35] for present work. The new ACL implementation eases the communication between nodes in an HPC environment compared to the implementation by Schollenberger *et al.* [38] which requires additional communication between nodes. The implementation by Schollenberger *et al.* is a more "traditional" ACL approach whereas the implementation by Beckers *et al.* has some simplifications. Instead of a spatially-averaged propeller inflow, a temporally-averaged propeller inflow is calculated, and used for the force computations. This approach has the downside of limiting the time-accurate response of the propeller to the unsteady inflow conditions. Since the propeller operates under constant inflow in the present study, the approach by Beckers *et al.* [35] is valid. Symmetric Gauss or skewed Gumbel functions can be used as the distribution functions. These functions are then superimposed for each blade, which effectively capture the blade motion as they rotate [35].

Finally, the third method is the fully-resolved propeller blade simulations using overset (Chimera) technique. In this method the propeller blades are physically resolved, but the computational setup is more involved. The overset setup will not be discussed further in this study.

E. Turbulence correction for actuator lines

The actuator disks/lines in TAU code cannot increase the turbulent kinetic energy in the slipstream, and do not contain the turbulence from the blade boundary layers, which cause the transition models to misbehave. Thus, a model to increase the turbulent kinetic energy in the slipstream is proposed in this part.

This method is initially proposed by Kang *et al.* [28] for horizontal axis wind-turbines, assuming the flow is attached over the blades. However, we apply this method to propellers provided that the propeller operates similarly in attached regime. We follow the derivation of Kang *et al.* [28] and then propose a non-dimensional form as well as a calibration coefficient. The rate of generation of turbulence from the blade section is equal to the sum of total turbulence kinetic energy flux from the boundary layer around the airfoil [28]. For a small strip of length Δr in the radial direction,

$$Q_k = 2\Delta r \int_0^\infty k^* U dy \quad (4)$$

where k^* is the blade generated TKE flux and U is the velocity in the boundary layer. The integral is estimated as

$$Q_k = 2\Delta r k_{av} U_0 \delta_1 \quad (5)$$

where k_{av} is the average TKE over the boundary layer, U_0 is the averaged velocity magnitude of the blade section and δ_1 is the displacement thickness at the end of the boundary layer. The average k in the log-law region can be approximated by assuming a balance in turbulent production and dissipation [40]

$$k_{av} = \frac{u_\tau^2}{\sqrt{C_\mu}} \quad (6)$$

where C_μ is the turbulent-viscosity constant in the k - ε model. In a simple turbulent shear flow, on the regions where turbulent production and dissipation are reasonably balanced, $C_\mu = 0.09$ according to Pope [40]. The friction velocity, u_τ , can be re-written as

$$u_\tau^2 = \frac{\tau_w}{\rho} = \frac{1}{2} U_0^2 C_f \quad (7)$$

yielding

$$k_{av} = \frac{1}{2} \frac{C_f}{\sqrt{C_\mu}} U_0^2 \quad (8)$$

For a boundary layer approximation C_f and δ_1 are rewritten

$$C_f = \frac{6}{7} C_{D,s} \quad (9)$$

where $C_{D,s}$ is the approximated drag coefficient corresponding to one side of the blade [28] from flat plate relations [41].

The definition of the shape factor yields

$$\delta_1 = H_{12} \delta_2 \quad (10)$$

where $H_{12} = 1.3$ is the shape factor ($H_{12} = \delta_1/\delta_2$). According to Schlichting [41], the momentum thickness δ_2 is

estimated by

$$C_{D,s} = \frac{2}{c} \delta_2 \quad (11)$$

Substituting Eq. (11) in Eq. (10), we get

$$\delta_1 = H_{12} C_{D,s} \frac{c}{2} \quad (12)$$

where c is the local blade chord length.

Expression in Eq. (9) can then be substituted in Eq. (8). The overall drag coefficient of a section is estimated by two times the drag coefficient of a single side,

$$C_D = 2C_{D,s} \quad (13)$$

Finally, plugging in Eq. (12) in Eq. (5), then, yields the TKE correction as per Kang *et al.* [28],

$$Q_k = C_b C_D^2 U_0^3 c \Delta r \quad (14)$$

where $C_b = 0.46$ given originally by Kang *et al.* [28] and C_D is the sectional drag coefficient. Equation 14 cannot be directly implemented in the TAU code due to its dimensional nature. The implementation in TAU code requires non-dimensionalization of the variables given in Eq. 14, which are the local velocity and the local lengths. The dimensional local velocity U is non-dimensionalized by Eq. 15,

$$\tilde{U}_0 = U_0 \sqrt{\frac{\rho_{\text{ref}}}{P_{\text{ref}}}} \quad (15)$$

where variables with $\sim$ indicate non-dimensional variables and subscript ref indicates a reference quantity. Additionally, local chord and radial distances are also non-dimensionalized using a suitable reference length x_{ref} ,

$$\tilde{c} = \frac{c}{x_{\text{ref}}}, \quad \tilde{\Delta r} = \frac{\Delta r}{x_{\text{ref}}} \quad (16)$$

Ultimately, the non-dimensional form of Eq. 14 is obtained,

$$\tilde{Q}_k = C_b \left(\tilde{U}_0 \sqrt{\frac{p_{\text{ref}}}{\rho_{\text{ref}}}} \right)^3 (\tilde{c} x_{\text{ref}}) (\tilde{\Delta r} x_{\text{ref}}) C_D^2 \quad (17)$$

Re-arranging Eq. 17 slightly we get,

$$\tilde{Q}_k = C_b \left(\frac{p_{\text{ref}}}{\rho_{\text{ref}}} \right)^{3/2} x_{\text{ref}}^2 \tilde{U}_0^3 \tilde{c} \tilde{\Delta r} C_D^2 = C_b \xi C_1 \tilde{U}_0^3 \tilde{c} \tilde{\Delta r} C_D^2 \quad (18)$$

We combine the all the reference terms (i.e. constant terms) given in Eq 18 to a term denoted with C_1 . Additionally, we

add ξ as a calibration coefficient. In the present work, the C_1 is called the turbulence correction coefficient and given by,

$$C_1 = \left(\frac{p_{\text{ref}}}{\rho_{\text{ref}}} \right)^{3/2} x_{\text{ref}}^2 \quad (19)$$

The addition of ξ allows for the fine-scale calibration of the model. It is desired that the turbulence levels near the wing are similar to that of a fully-resolved blade simulation. We consider the similar levels of turbulence kinetic energy near the wing for the actuator line and for the blade-resolved simulations to be realistic. The turbulence from the actuator line rapidly decays, but without any further control it may fail to reach similar levels as the turbulence approaches the wing leading edge. Thus, we introduce ξ as a calibration coefficient to finely control the strength of the turbulence kinetic energy and its dissipation until the wing leading edge to have realistic values. The turbulence and its dissipation until the leading edge is discussed in Section III.I.

The exact value of ξ depends on the actuator line subroutine non-dimensionalization approach (x_{ref}) in the CFD codes. As an example, the appropriate atmospheric values and $x_{\text{ref}} = 0.5D_p$ yields $C_1 = 22.7 \times 10^6 \times x_{\text{ref}}^2 \approx 1 \times 10^6$. Thus, it is easy to see the pressure to density ratio term dominates Eq. 19, which increases the magnitude of C_1 to $O(10^6)$. Thus, ξ must be varied depending on the scale of the problem as well as the CFD code reference values. We note that the pressure to density ratio term given in Eq. 19 starts at about 24×10^6 at sea-level to approximately 16×10^6 at about 32000 ft.

Non-dimensionalized Q_k ($\tilde{Q}_k$) is computed for all the nodes in the propeller outflow. For actuator disks, a full, circular distribution of TKE is obtained on the propeller plane. For actuator lines, the same total force distributions and *the same total TKE* are obtained by means of Gumbel or symmetric Gauss distribution functions over the disk area, which will be discussed in the Section III.I. The TKE is, then, only shed at the "actuation lines".

As a final remark, we note that the magnitude of the C_1 will depend on the propeller scale (as shown in Eq. (19)), as well as the code structure and how it is implemented. The current implementation in the TAU code directly involves computing the $\tilde{Q}_k$ values at the exit of the propeller outflow boundary, where the computed non-dimensional values are patched into the flow-field variables array. Thus, sufficient ξ ensures meaningful (i.e. physically sound) values are attained, which is the reason the present study has "large" ξ values, which somewhat depends on the problem scale. Additionally, for larger propellers and for wind turbines, the computed $\tilde{Q}_k$ values will have a larger order of magnitude with smaller values of ξ . The justification and realizability of this approach is discussed in Section III.I.

III. Results and Discussion

The results section start with the steady transition location of the clean wing in the wind tunnel (Sec. III.A) without the propeller effect. The cases to be analyzed in detail are presented (Sec. III.B). Boundary layer relaxation effects are assessed in Sec. III.C, where the transition is intermittently delayed and laminar-like velocity profiles are obtained. Then,

various aspects of the turbulence correction effects are discussed in the remaining sections, as well as the modifications of the γ model. The numerical predictions are validated using the experiments in Sec. III.H. Finally, the physical behavior of the turbulence correction in propeller slipstream is analyzed.

A. Steady, propeller-off transition location assessment of the wing

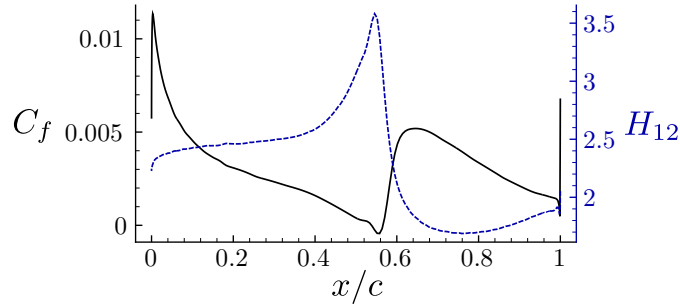


Fig. 5 Skin friction coefficient and shape factor distribution for the clean wing, $Re = 1.25 \times 10^6$

The steady transition behavior of the DLR-FR-1924-MODLWT airfoil, as predicted by the DLR- γ transition model, is shown in Fig. 5. The figure also shows that the transition region spans between $\approx 55\%$ and 60% , and the transition is not instantaneous. The *transition onset* is numerically predicted at 55% chord. The experimental transition onset for $Re = 1.25 \times 10^6$ is measured to be 55% . The transition location is then determined by taking the midpoint of the C_f ramp-up, where $H_{12} = 2.3$.

It is also assumed that the transition process ends when $H_{12} = 2.0$ is attained, resulting in a fully-turbulent flow downstream. Thus, by this classification the transition point would be around 58% chord. The point where the $H_{12} = 2.3$ is then taken as the basis to determine the unsteady transition location. Nevertheless, the numerical predictions agree well with the measured transition onset.

B. Cases definition

Table 1 Overview of the simulations

Case	Model	ξ , turbulence correction	Explanation
Case 1	DLR- γ	0	ACL with no TKE addition
Case 2	modified DLR- γ	0	ACL with no TKE addition
Case 3	DLR- γ	48.78	ACL with TKE addition
Case 4	modified DLR- γ	48.78	ACL with TKE addition
Case 5	modified DLR- γ	97.56	ACL with even more TKE addition

The overview of the numerical simulations, which have slight differences, are presented in Table 1. Numerically predicted boundary layer relaxation effects are presented, and the related physics are explained in Section III.C using

case 3. The first case and the third case are compared in Section III.D to see the effects of the turbulence correction using the original DLR- γ model. Then, Section III.E compares the original and modified γ models under no influence of the turbulence correction. This allows to assess the modification effects of the transition model with no other influence. Afterwards, Section III.F compares the effects of the turbulence correction for both transition models. After that, Section III.G assesses the effects of elevated turbulence level in the slipstream and compares the transition characteristics. Finally, a validation is performed comparing the experiments carried out in IAG Laminar Wind Tunnel and numerical simulations in Section III.H.

C. Boundary layer relaxation effects

We investigate the boundary layer relaxation, or, calmed region effects in this section. We use the turbulence correction on actuator disk, and the original γ model, which is the case 3 in Table 1.

In Fig. 6a, the tip vortex from the up-going blade side is visible as a traveling, turbulent packet. This packet causes local transition with low shape factors around it ($H_{12} = 2.0$). Downstream of this packet, the calmed region (created by the tip vortex of the previous blade passage) is visible with delayed transition at around 75% chord. On the up-going blade tip vortex side the following states are visible: *laminar* $\rightarrow$ *turbulent* $\rightarrow$ *calmed* $\rightarrow$ *turbulent* (from LE to TE). Premature transition due to passage of the tip vortex is visible on the down-going side, where the transition occurs at 35% chord.

After about 15 degrees of rotation, the state in Fig. 6b is attained. The tip vortex on the up-going side expands in streamwise direction. This causes early transition as it merges and terminates the calmed region. The transition front on the down-going side moves downstream. Fig. 6c indicates that the early transition observed in the up-going blade side retreats downstream while also moving toward down-going blade side due to the swirl of the slipstream. Additionally, the small calmed region, located near the wing centerline, moves toward down-going blade side, as well. Finally, calmed regions are about to arise on this side due to the blade tip vortex passage.

The calmed regions start to form as shown in Fig. 6d. Additionally, the spanwise movement of the turbulent region from the up-going blade side are visible. The extent of the calmed region is around 72% chord. After that, a packet of a turbulent region appears on Fig. 6e on the down-going blade side. This causes immediate transition in its vicinity as visible by the dashed lines. Similar to Fig. 6a, we see *laminar* $\rightarrow$ *turbulent* $\rightarrow$ *calmed* $\rightarrow$ *turbulent* regions (in this order) as we move downstream. Fig. 6f shows that the leading part of the tip vortex is around midchord, which then interacts with the moving transition front near midspan. The transition front moving toward down-going side stretches in streamwise direction, and merges with the blade tip vortex. The blade tip vortex also stretches in the spanwise and streamwise directions to merge with the oncoming sweeping transition region.

Fig. 6g shows the calmed region is cut-off from the remainder of the laminar region upstream. Calmed region is subsequently terminated when the transition moves upstream. Now we see that calmed regions enlarge in size and

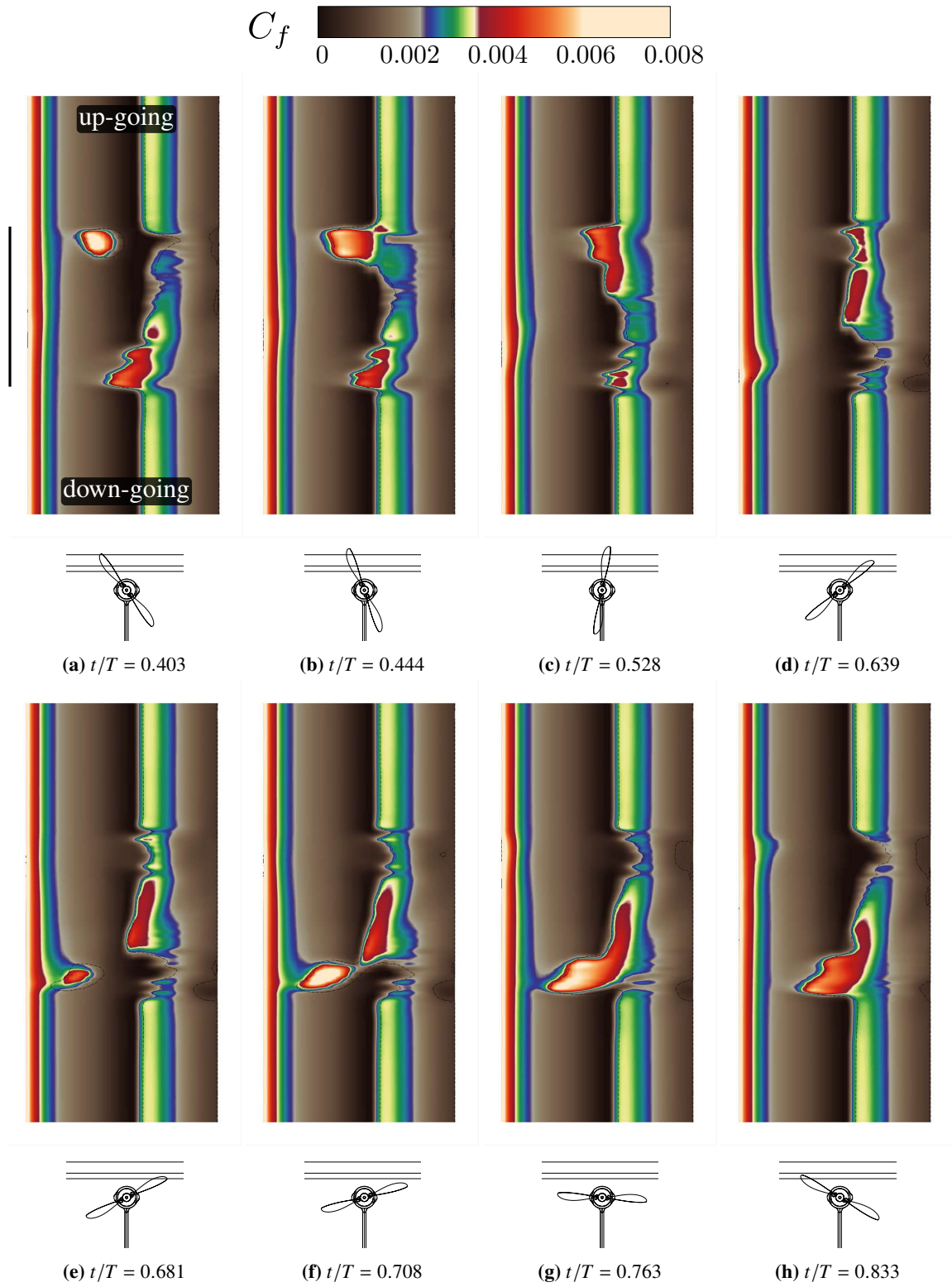


Fig. 6 Skin friction coefficient (flow from left to right), dashed lines $H_{12} = 2.0$

especially the reduction of C_f is visible on the up-going blade side (c.f. Figs. 6f to 6g) as transition moves downstream. Delayed transition is visible, which occurs around 74% chord. Finally, we see the maximum extent of the calmed region benefit, where the flow is laminar up to $\approx 80\%$ of the chord on the up-going side on Fig. 6h. The turbulent front on the down-going blade side starts to retreat downstream as the tip vortex passes. The sequence from Fig. 6e to 6h posits that the calmed region advances rapidly after the C_f decreases below that of transitional values on the down-going blade side. The same behavior is also observed for the up-going blade in Figs. 6a to 6e, since the phase difference is 180 degrees between two blades.

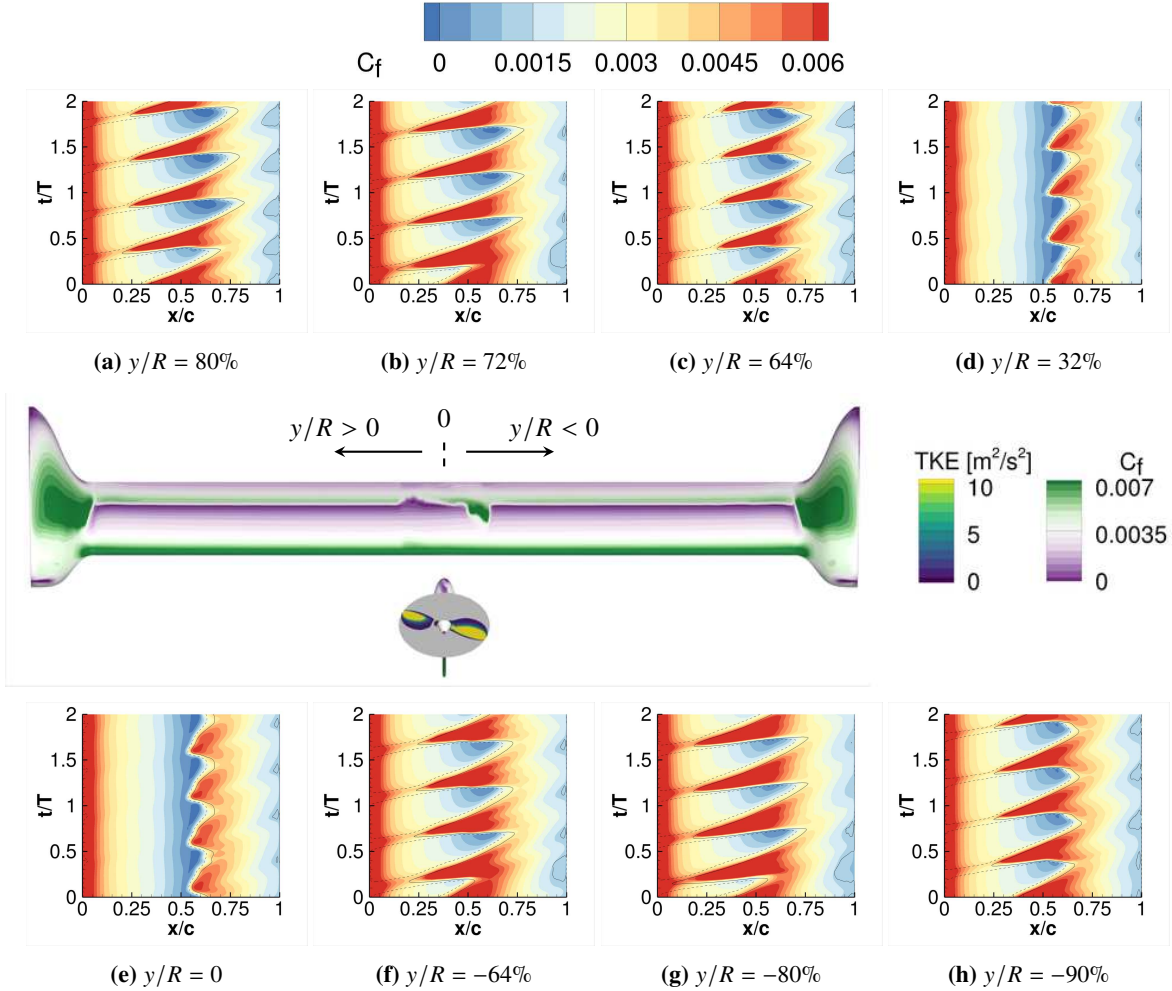


Fig. 7 Spatio-temporal variation of C_f for different spanwise locations, (— : $H_{12} = 2.0$, - - - : $H_{12} = 2.3$).

For a more quantitative look, various spanwise stations are investigated next. The spatio-temporal variations in skin friction coefficient for various spanwise locations are presented in Fig. 7. The plots show the C_f contours and the shape factor lines. First, the section at $y/R = 80\%$ is investigated (on the up-going blade side). The effect of the propeller is visible in the form of periodic wake strips with high C_f as well as upstream transition onset location. Premature transition at about 25% chord is observed when the propeller wake with its high turbulence levels impinge on the wing

as seen in Fig. 7a. Then, the calmed region occurs after the passage of the wake with delayed transition. The transition occurs at about 80% chord. Calmed region is formed and terminated periodically when the vortices shed from the propeller interact with the wing. For the $y/R = 72\%$ section presented in Fig. 7b, the behavior is similar and the extent of the calmed region (between 75-80% chord) is also comparable to that of 80 percent spanwise location. Laminar separation characteristic of the airfoil is suppressed as the turbulent wake passes, and as long as the calmed region exists. After the termination of the calmed region, the flow immediately goes back to a laminar separation state to transition into turbulence.

Moving inboard we see that the transition onset due to wake shifts slightly downstream at $y/R = 64\%$, which is attributed to the lower turbulence kinetic energy level in this radial position impinging on the wing as visible in Fig. 7c. The termination of the calmed region sees a stronger negative slope compared to Figs. 7a and 7b and the extent of the calmed region reaches $\approx 75\%$ chord. The effect of the propeller wake on calmed region is reduced for 32% and 0% spanwise locations as evident in Figs. 7d and 7e. One explanation is the reduced levels of incoming turbulence and its interaction with the laminar separation type of transition observed on this airfoil. Since the incoming TKE is lower than that of outboard sections, it is not able to trigger transition upstream and cannot "carry" the slipstream further along the wing. Instead the flow periodically transitions between 55 to 65% chord, with no extended laminar runs due to calmed regions. For the down-going blade side, the observations are similar to that of the up-going blade side outboard sections. For $y/R = -64\%$ (Fig. 7f), the upstream transition at 25% chord is visible (similar to the Fig. 7c) but the termination of the calmed region has less negative slope than the up-going blade side counterpart. Calmed region here extends to about 70-75% chord. The calmed region can be identified at $\approx 75\%$ chord for $y/R = -80.2\%$ location. At even more outboard positions such as $y/R = -90\%$, it is seen that the calmed region somewhat weakens. The calmed region, then, produces a negative slope at its most downstream termination location as visible in Fig. 7h. The extent of it in this spanwise location is about 65% chord. One thing to note for down-going blade side is that the wake strips/wedges are 'thicker' (c.f. Fig. 7a and Fig. 7g) indicating a slightly higher level of incoming turbulence than up-going blade side.

Next, we take a look at normalized drag coefficient (i.e. $C_D/C_{D,y/R=5}$) for fully-turbulent and transitional case, given in Fig. 8. The first subplot on the left corresponds to $t/T = 1.875$ and all the subsequent subplots are separated in time by 10 degrees of propeller rotation. Initially, the spanwise distribution of the normalized drag coefficient shows that the drag increase on the down-going side is greater on transitional case than that of the fully-turbulent case. The presence of the vortex shed by the propeller is visible around $y/R = 80\%$. In the second timestep (i.e. after 10 degrees of propeller rotation), we see the calmed region affecting the drag coefficient in action. The spanwise movement of the vortex leaves behind a calmed zone (shaded zone) in the transitional case where the drag is rapidly reduced. On the other hand, the fully turbulent case sees an increase in drag. The drag coefficient change is around -1.3% relative to the fully turbulent case (averaged over the shaded region).

Similarly, on the third subplot, we see a further reduction in the drag coefficient for the γ model, particularly around

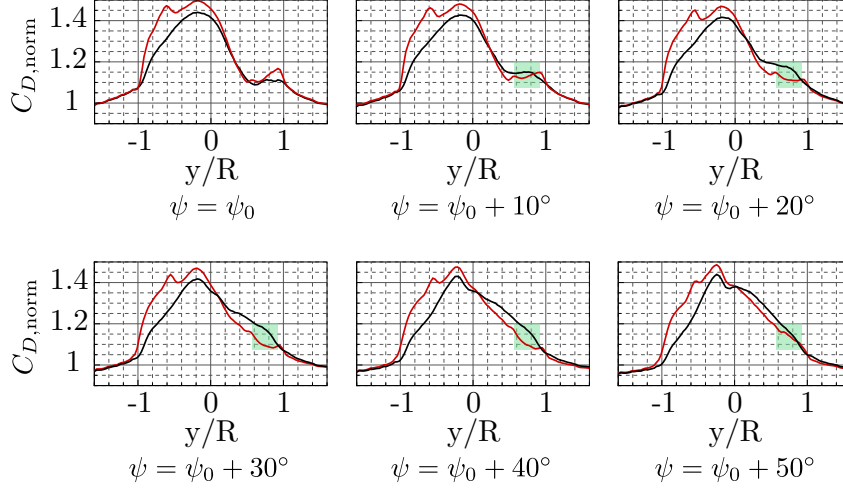


Fig. 8 Normalized drag coefficient distribution for fully-turbulent (black) and DLR- γ model (red), evenly spaced at 10 degree propeller rotation intervals

$y/R = 0.8$ as supported by Fig. 7a. Fully turbulent case shows further increase in drag coefficient around 60% spanwise location. The instantaneous drag coefficient decrease is about 4.2% compared to that of the fully-turbulent case. In terms of absolute values this change corresponds to $\Delta C_D = -0.00082$, which is 8.2 drag counts (d.c.).

For the fourth timestep, fully-turbulent case keeps somewhat a constant drag distribution around the shaded area, where the transitional case attains its minimum value of drag coefficient. The drag coefficient change is around -6.0% relative to the fully-turbulent case, or approximately 11 d.c. ($\Delta C_D = -0.0011$). Both cases show increased drag coefficients between $y/R = 0$ and 60%. In the fifth timestep, the transitional case has relatively constant drag as well as the fully-turbulent case. The increase between 0 and 60% spanwise location start to affect the innermost part of the calmed region. The drag change on the transitional case relative to the fully turbulent case is -5.5%. Finally, on the last subplot, we see that the calmed region is now terminated, and the transitional case predicts a drag trend similar to that of fully-turbulent case with similar values at outboard part.

To sum this part up, we have seen a monotonic, rapid increase in the drag coefficient for the fully-turbulent case due to passage of a wake of a propeller blade. In contrast, the drag coefficient first decreases and then keeps constant for a while due to formation of a calmed region and only after that reaches levels similar to the fully-turbulent levels for the transitional case.

Thus, the next topic of discussion is the evolution of the boundary layer profiles over the airfoil. Boundary layers are sampled and plotted every 15 degrees over the last propeller rotation for the transitional and the fully-turbulent cases in Fig. 9 for $y/R = -80\%$. The transitional case shows a variation between laminar-like and turbulent profiles as shown in Fig. 9a. We limit our discussion to the upper surface boundary layer profiles only, but the findings are also valid for the lower surface. The boundary layers start as laminar, and the turbulent pulses from the propeller wakes are

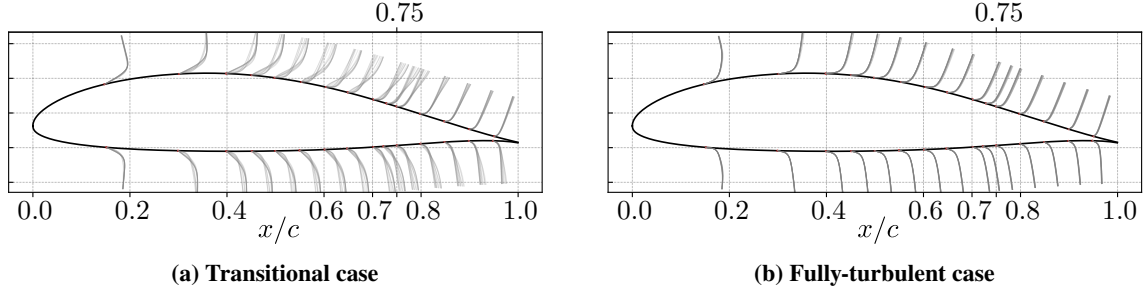


Fig. 9 Boundary layers over the airfoil for the last propeller revolution

also visible (albeit faint) at 30 and 40% chord locations. After that, the turbulent profiles become less faint and more common, although the laminar and turbulent (and transitional) states coexist. The presence of the calmed regions are the most visible after 60% chord (downstream of the natural transition point). The boundary layers at 65 to 70% show such regions of laminar-like velocity profiles, indicating reduced instantaneous wall shear stress after the passage of the turbulent wake. The "relaminarization" effect is clearly visible even after the downstream of the natural transition location. The effect is observed as downstream as 75% chord, agreeing with the wall shear stress distribution and the conclusions of the Fig. 7g. Downstream of the 75% chord location, the velocity profiles resemble fully-turbulent case. The fully-turbulent case shows the boundary layer development over the airfoil and little variance is observed, especially near the boundary layer edges (Fig. 9b). The differences are stark with the transitional case upstream of the 75% chord. Downstream of that the profiles are similar for both cases.

D. Turbulence correction effects using original γ model

In this part, the turbulence correction effects will be investigated using the original γ model. Thus, the aim is to compare the response of the original γ model to the turbulence correction. Hence, ξ is set to 0 for comparison purposes. The solutions for the $\xi = 0$ case (i.e. no propeller turbulence) are warm-restarted from $\xi = 48.78$, and thus the effect is only visible after the second blade passage. The slipstream without turbulence correction only can only cause *limited* delayed transition (due to tip vortex impingement) as visible in Fig. 10a. However, it cannot move the transition location upstream. The transition in its most upstream position is around 60% chord, which is similar to that of the clean wing. However, the delayed transition only occurs until 70% chord as opposed to roughly 80% chord. The 'calmed region' effect without turbulence correction only occurs at the outer edges, such as $y/R = -80\%$. Toward the inboard sections, it is seen that the calmed regions get weaker and so does the laminar run as depicted in Fig. 10b for $y/R = -64\%$. With turbulence correction, the transition line extends until 75% chord but it only moves up to roughly 65% chord without it. The upstream transition also suffers where it moves from about 30% to about 60%, again close to the natural transition position of the wing. The effect worsens at 48% span on the down-going blade side as shown in Fig. 10c as the response without turbulence correction flattens. The transition onset stays constant at 60% chord. The transition onset for the

turbulence correction is moving, but its most upstream position is detected at 50% chord. The extended laminar run is still visible, intermittently reaching 67-68% of the chord. Therefore, the benefits of the turbulence correction are evident. Without the corrections, the response of the transition model is basically non-existent. The DLR- γ model, without correction, cannot predict calmed regions in the most of the spanwise sections. Due to the effects of the actuator line on the flow-field (e.g. lower c_p in the helical blade tip vortex), the boundary layer indeed relaxes; but this effect is contained to a tiny region only downstream of the helical vortex trajectory. Turbulence correction, in this case, allows the transition location to move upstream as the helical vortex impinges on the wing and causes premature transition (compared to the clean wing). The turbulent packet then interacts with the downstream boundary layer and creates regions of laminar flow separated by turbulent regions (similar to Fig. 6f). Without correction, there is little movement of the transition front due to impinging wake from the actuator line. After the passage of the wake, the boundary layers turn to a laminar state. The case with turbulence correction, then, retains the laminar state for longer. One such example is already provided in Fig. 9a and the intermediate boundary layer states. The ability of the DLR- γ transition model to predict the relaxed boundary layers under disturbed inflow is already presented by Sarikaya *et al.* [26] for a 2-D configuration with intermittently delayed transition until 75-80% chord location. In [26] it was also shown that using the DLR- γ transition model, the boundary layers can go through laminar-to-turbulent (transition) and turbulent-to-laminar (reverse transition) states intermittently. Finally, the ability to intermittently suppress the laminar separation characteristics of the airfoil is diminished without the turbulence corrections.

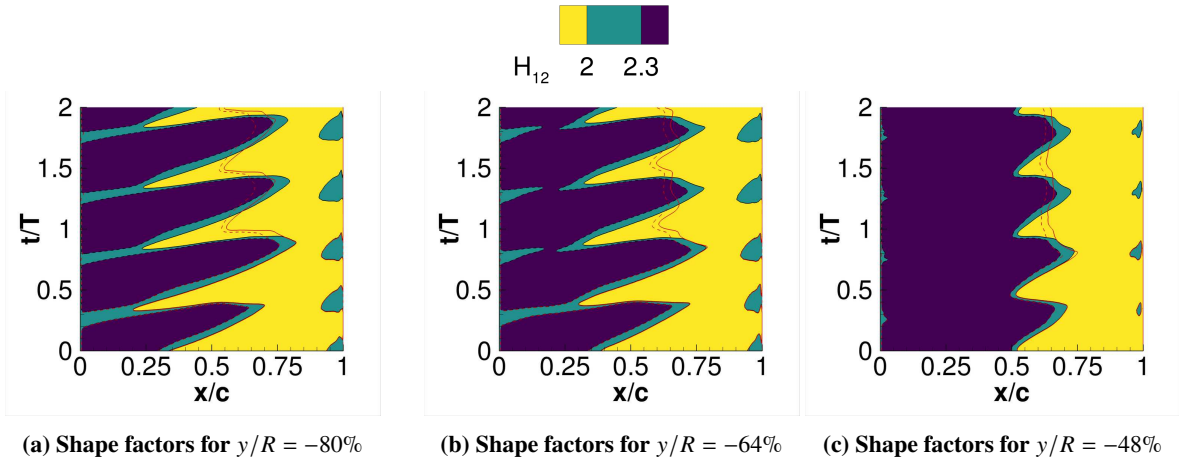


Fig. 10 Shape factor comparisons for original γ models, $\xi = 0$ to 48.78, dark: laminar, light: turbulent. Contour: turb. correction on, lines: turb. correction off, (— : $H_{12} = 2.0$, - - - : $H_{12} = 2.3$).

E. Comparison of original and modified γ model for no added TKE

In the previous part, the boundary layer relaxation effects are assessed for the original γ model with turbulence correction ($\xi = 48.78$). In this subsection, the original γ model and modified γ model will be compared with no

turbulence correction.

We now disable the turbulence correction off, which allows us to investigate the response of both models, as well as their resulting periodic behavior. Fig. 11 shows the skin friction coefficient and shape factors for the original and modified γ model for $\xi = 0$. The profound effect of the turbulence correction, or lack thereof, is visible after $t/T > 0.5$, due to the second blade of the propeller. The upstream transition effect is not observed, the transition line oscillates between 55% and 65-70% chord for the original model (Fig. 11a) and for the modified model (Fig. 11b). This section is the only section (also +80%) where the relaxation effects are somewhat observed despite no turbulence correction being present. The shape factors as seen in Fig. 11c indicate that after the initial adaptation due to turbulence correction (until $t/T \approx 0.5$), both models predict more or less the same transition characteristics. This is explained due to propeller tip vortex interacting with the transition models (such as pressure gradient) and triggering transition near this spanwise section. However, moving inboard shows that between $\pm 64\%$ and centerline, we see a disruption in the calmed region predictions for both models. For brevity, only the centerline (i.e. $y/R = 0\%$) is shown in Fig. 12. Similar to the previous case, due to the calibration of the modified γ model, we see similar C_f distributions and H_{12} distributions. Fig. 12a and Fig. 12b are behaving similarly. Particularly, the last revolution of the propeller implies both models, without the turbulence correction, are unable to distinguish the propeller slipstream from the free-stream. A more or less constant transition onset is attained for both models, with no rise in C_f due to propeller vortices as opposed to first revolution ($t/T < 1$). The shape factors confirm this (Fig. 12c) where the transition front is relatively constant showing the benefit of the turbulence correction in these simulations. Apart from the fact that transition is triggered near high y/R values with no upstream transition onset, the inner parts of the slipstream are unable to trigger transition into turbulence.

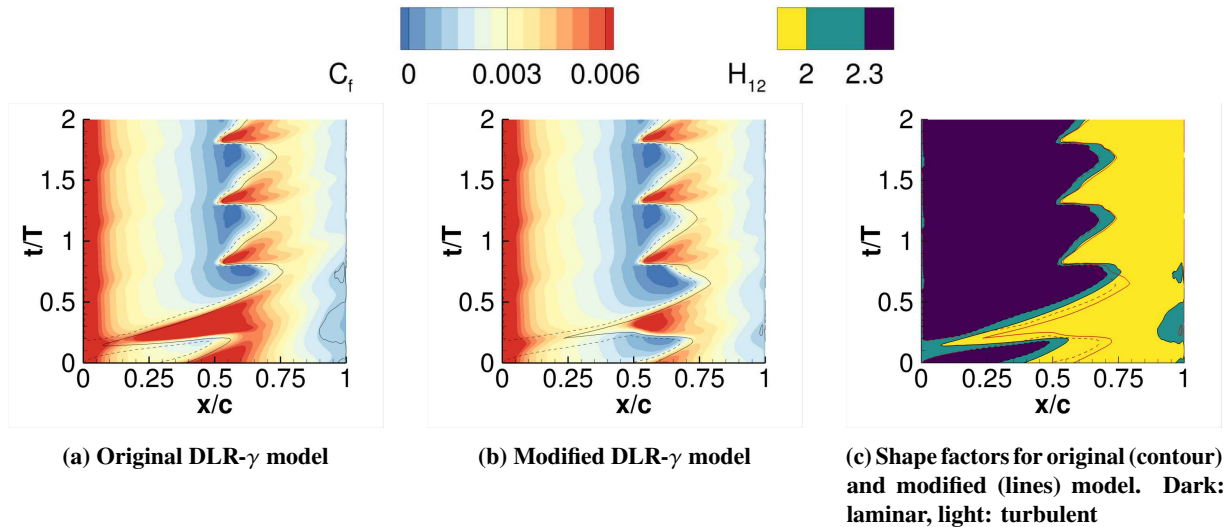


Fig. 11 Skin friction coefficient and shape factor comparisons for original and modified γ models. $\xi = 0$, down-going side, $y/R = -80\%$, (— : $H_{12} = 2.0$, - - - : $H_{12} = 2.3$).

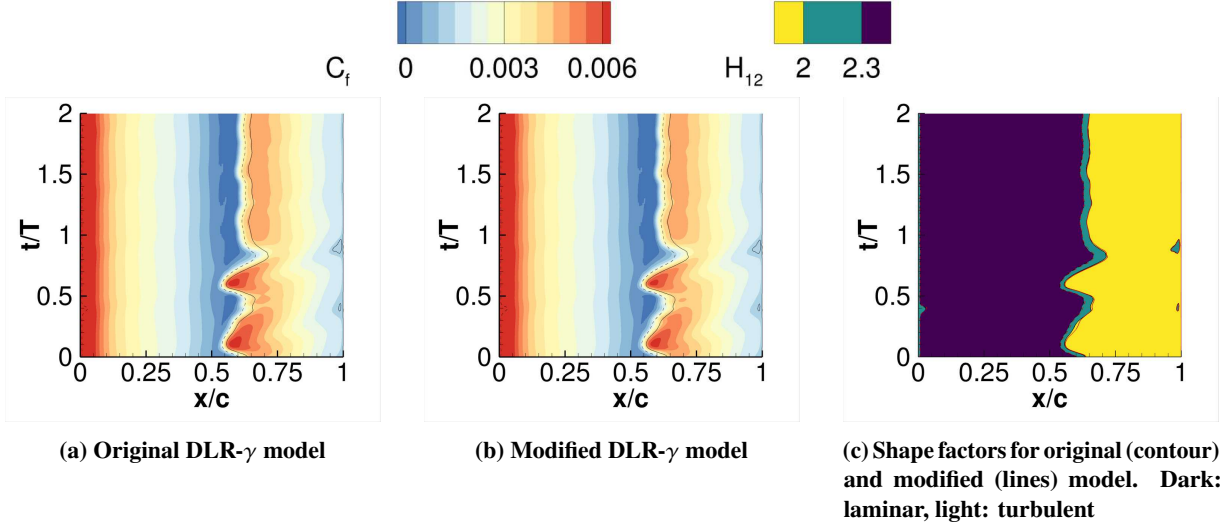


Fig. 12 Skin friction coefficient and shape factor comparisons for original and modified γ models. $\xi = 0$, centerline, $y/R = 0\%$, (— : $H_{12} = 2.0$, - - - : $H_{12} = 2.3$).

F. Comparison of original and modified γ model with added TKE

The aim here is to compare original γ model and the modified γ model with actuator line turbulence correction ($\xi = 48.78$). Some of the results for γ with actuator line turbulence correction are already presented in Section III.C. We investigate the shape factor distributions over propeller revolutions without re-presenting the C_f distributions.

In Fig. 13, we present the shape factor distributions for both models spanning between $y/R = -90\%$ and $y/R = 90\%$. Fig. 13a shows the shape factor distributions for the down-going side at $y/R = -90\%$. Modified γ model is observed to predict upstream transition slightly more downstream (about 5 to 10%). After that the extent of the calmed regions are very similar, located at $\approx 75\%$. Due to slightly downstream initiation of the transition due to wakes, the spacing between the wakes is slightly larger. At $y/R = -72\%$, shown in Fig. 13b, the conclusions are not too different than that of -90% section. Here, the turbulent regions start to form with a delay of 25% for the modified γ model, although the extent of laminar regions are similar. On the centerline (Fig. 13c), we see relaxation effects unlike the case without the turbulence correction. There a flat transition response is seen (c.f. Fig. 12c) whereas here a periodically moving transition front is visible. The transition moves between midchord and 65% chord. The calmed region / boundary layer relaxation effects are the weakest here. Then, Fig. 13d shows that even at 18% span, meaningful relaxation effects are possible. Here, the transition front moves between 50 and 70% chord. Moving further outboard to $y/R = 72\%$ (Fig. 13e), we see the wedge-like structures appear again due to stronger wake interactions. Again, there is about a 25% chord difference between the wedge-like transition locations for original and modified γ model. Finally, the most outboard location at $y/R = 90\%$ for the up-going blade side shows that the calming effect is slightly weakened at this section. Fig. 13f shows the original and modified models still have comparable gap between their upstream transition locations but the calmed region extent is similar.

Overall, we see that the transition on the down-going blade side happens more upstream than the up-going side (c.f. Figs. 13b and 13e). The turbulent, wedge-like structures are slightly larger. One important aspect is that the modified γ model keeps its upstream transition location relatively constant throughout the spanwise locations (except $y/R = -90\%$) at about midchord, whereas the original γ model shows more movement of the transition front. Thus, it can be said that the modified γ model is relatively less sensitive to the incoming freestream turbulence (*of this level*) than the original model. This movement of the transition front subsequently affects the time spent in calmed region but not the extent of the calmed region. Both models predict similar extents but show differences in time spent in the "calmed" state. The modified γ model shows slightly more time passes between the wakes, hence slightly more time may be spent in the calmed state.

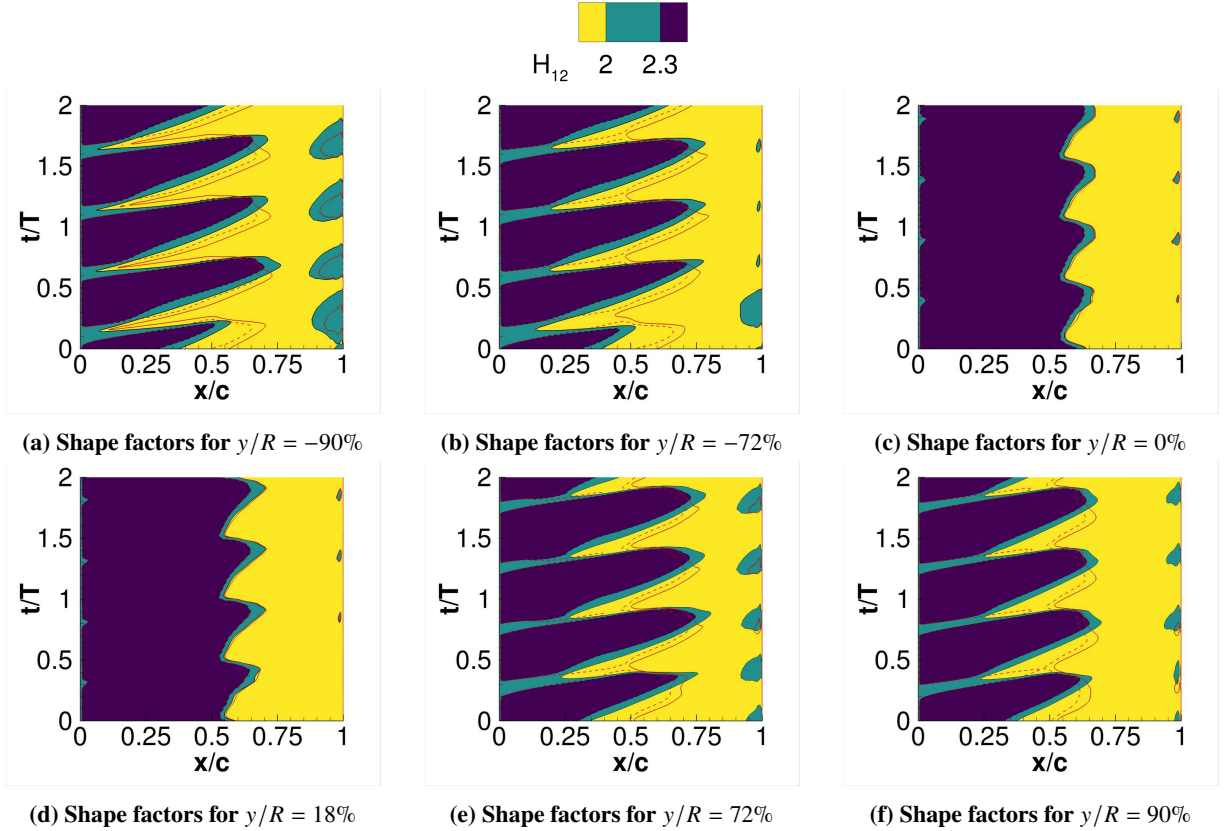


Fig. 13 Shape factor comparisons for original and modified γ models. $\xi = 48.78$, dark: laminar, light: turbulent. Contour: original, lines: modified γ model, (— : $H_{12} = 2.0$, - - - : $H_{12} = 2.3$).

G. Increased ξ for modified γ model

We increase the ξ (i.e. turbulence calibration coefficient) to 97.56 (twice of its previous value) to investigate further levels of elevated turbulence for the modified γ model. In Section III.F, the transition predictions for original and modified γ model are compared. There it was observed that the modified γ model yields similar extents of calmed

region similar to that of original γ model. However, the upstream transition location due to wakes was generally found to be around midchord (with the exception of $y/R = -90\%$) as presented before in Fig. 13. Now, we increase the ξ coefficient to move the transition due to wake further upstream. Here, the upstream transition onset is seen to be moving further upstream attributed to the increased ξ . However, that does not yield a meaningful change in the calmed region envelope, but the changes in the calmed region termination location are on the order of 5% chord for some locations. The outer locations (Figs. 14a and 14d) benefit from slightly extended calmed regions. However, the sections between $y/R = -48\%$ and 64% show little to no benefit. The transition locations, as well as the calmed region length stay constant at elevated levels of incoming turbulence. In the midsection of down-going side ($y/R = -48\%$, Fig. 14b) increased shape factors are visible which initially move transition location due to wake upstream, but then the transition moves back to about midchord. However, the worst example of this is the centerline shown in Fig. 14c. There is no movement for increased ξ , so the predicted transition locations are equal. It is noted that due to earlier transition at outer sections, the leading boundary of the turbulent wake and calmed region boundary get closer together, which implies the time spent on the calmed state will be slightly lower for higher ξ case than that lower ξ .

When TKE is introduced into the propeller slipstream, the standard DLR- γ model exhibits a pronounced upstream shift of transition during wake passage. At these locations, both the intermittency and the viscosity ratio increase rapidly to values that are representative of a turbulent boundary layer. This behavior is associated with the transport (through convection and diffusion) of TKE and intermittency from the slipstream into the boundary layer, potentially accompanied by increased local TKE production as the intermittency increases. With the modified DLR- γ formulation, the growth of intermittency is substantially reduced at these upstream locations where the local transition criterion is not satisfied, owing to the modified destruction term. In this case, the intermittency remains close to zero within the boundary layer and reaches only moderate values near the boundary-layer edge. This confirms that the objective for modifying the destruction term to limit the growth of intermittency within the boundary layer in situations where the local transition criterion is not fulfilled, works as intended. When the TKE level that is introduced into the propeller slipstream is increased further, however, an upstream transition shift is also observed within the modified formulation. This shows that sufficiently high levels of TKE (or eddy viscosity) in the slipstream can eventually overcome the effect of the modified destruction mechanism. Thus, elevated levels of freestream turbulence are able to numerically by-pass the transition criterion requirement for both original and the modified γ -models. A more detailed analysis of the relevant transport and source terms is planned to identify the underlying mechanisms.

H. Experimental validation

We compare the original γ model in combination with the propeller turbulence correction ($\xi = 48.78$) and modified γ model also with the propeller turbulence correction ($\xi = 97.56$) with the experimental intermittency measurements, based on our findings from Section III.E to Section III.G.

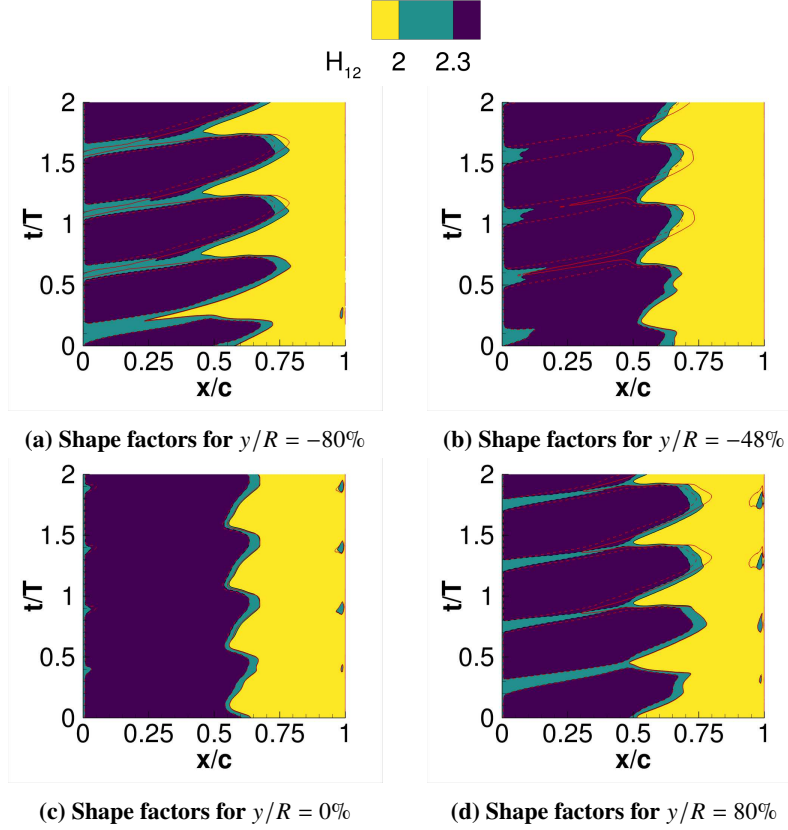


Fig. 14 Shape factor comparisons for the modified γ model. $\xi = 48.78$ to 97.56 , dark: laminar, light: turbulent. Contour: low ξ , lines: high ξ , (— : $H_{12} = 2.0$, - - - : $H_{12} = 2.3$).

The mean pressure coefficient distribution in the slipstream (mean of $y/R = -1$ to 1) is plotted in Fig. 15. It is seen that the mean flow is predicted accurately, with largest deviation of $\Delta C_p \approx 0.07$ around $x/c = 0.3$. The distribution of the pressure coefficient values indicate the numerical and the experimental setup are in good agreement. Both numerical cases (the original γ and the modified γ model) yield overlapping mean C_p distributions, hence only one of them is plotted.

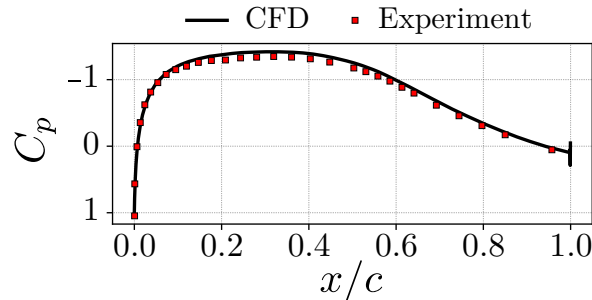


Fig. 15 Measured mean pressure coefficient vs. numerical predictions

Fig. 16a shows the upstream transition location is predicted more downstream compared to the experiments, but

the slopes of leading and trailing boundaries of the high C_f region are matched. The extended laminar run is also visible on the experiments, the predicted downstream transition location is in good agreement. The beginning of the predicted laminar separation region ($C_f < 0$, encircled in green) also agrees with the measured intermittency ramp-up region (with negative slope) where the calmed region (magenta shaded region) is terminated. The extent of the laminar separation region is predicted slightly downstream whereas in experiments the transition location is about 5% chord upstream. Similarly in Fig. 16b ($y/R = 80\%$), the aftmost transition location agrees well with the experiments, but the transition due to laminar separation region after the termination of the calmed region is predicted to have a gradual change, whereas the measured profiles show a steeper change, similar to $y/R = 60\%$. The transition strip due to wake is also well predicted.

For the 70% radius at the down-going blade side (Fig. 16c), we see good agreement between predictions and the experiments, on both the shape and the extent of the laminar run. The transitional wake strip seems to travel slightly slower than the measurements (its slope is smaller) initially, but then it catches up. Fig. 16d depicts the 80% radius at the down-going blade side. The CFD predicted transition location agrees with the experiments, but the calmed region termination is predicted differently similar to Fig. 16b.

For the modified γ model, we see a behavior similar to the original γ model for 60% radius as depicted in Fig. 17a. The predicted extent of the laminar run is in good agreement with the measurements at about 75% chord. The sharp ramp-up with negative slope -after termination of the calmed region- is also visible. One difference between the original γ model and the modified γ model is near the laminar separation region. A slightly bigger laminar separation region is observed for the modified γ model is predicted, which begins slightly more upstream than the original γ model; although by about half-length of this region the flow is measured to be fully turbulent for both cases. Fig. 17b shows the blade up-going side 80% radius comparison between the CFD and the experiments. The aftmost transition location is slightly overpredicted by CFD compared to the measurements. Similar to the $y/R = 60\%$, a negative slope transition into the fully-turbulent state is observed due to the wake. This negative slope region is located slightly upstream on the measurements implying slight overprediction by CFD. Additionally, the laminar separation region begins slightly more upstream than original γ model, which generally holds for other sections, also. The early transition due to wakes is not predicted like the original γ model, where C_f peaks are much more downstream than the high intermittency zones. For the down-going blade side, at 70% radius, a thinner transitional strip is seen, but the aftmost transition location agrees well with the experiments. The measured negative slope region also has a very good agreement with the predicted movement of the transition front (Fig. 17c). One difference here is that the laminar separation region encloses the intermittency ramp-up in the negative slope region, as opposed to being concentrated around half-length of it. For the outer section at 80% down-going blade side, a stronger transitional strip is observed. The extended laminar run length is slightly overpredicted by CFD. The movement of the transition line after the termination of the calmed line is faster and sudden on the experimental case whereas it is more gradual on CFD.

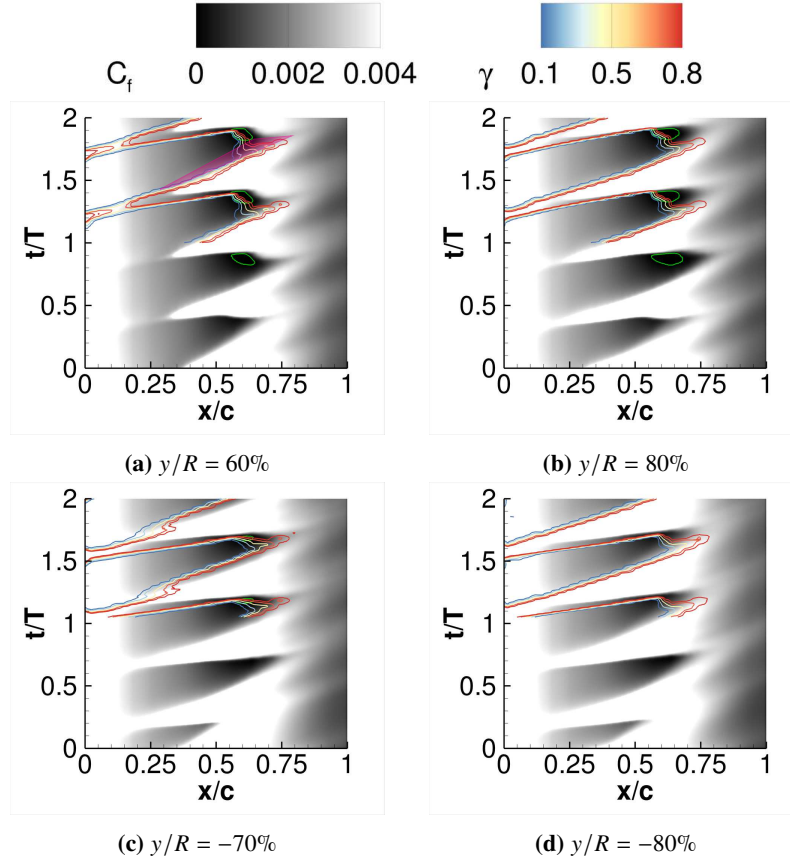


Fig. 16 Predicted C_f (contours) vs. measured intermittency (lines) for the original γ model, $\xi = 48.78$.

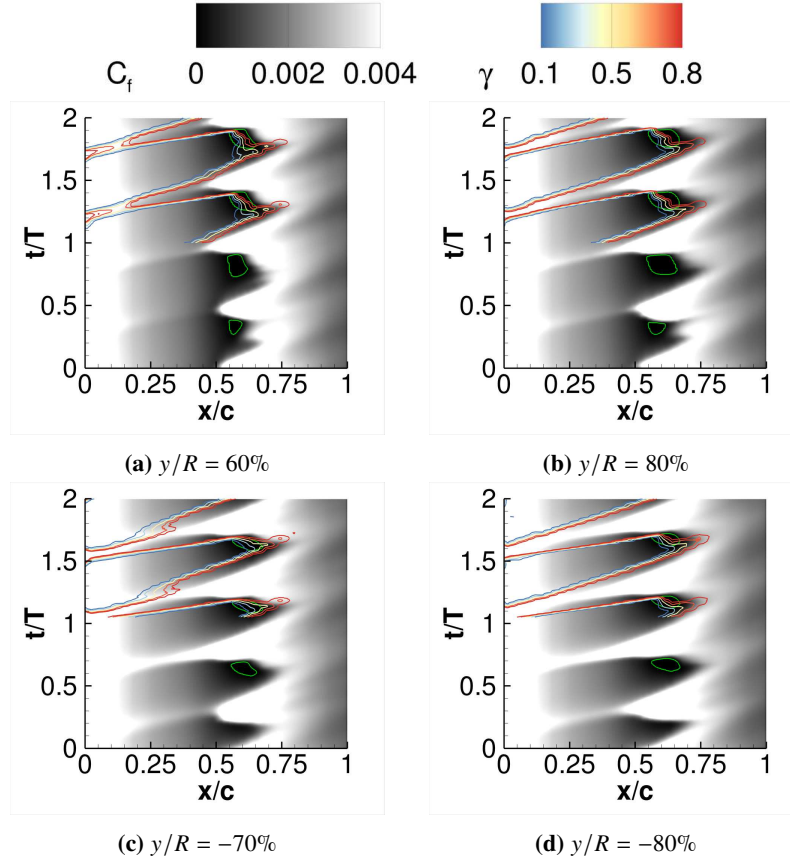


Fig. 17 Predicted C_f (contours) vs. measured intermittency (lines) for the modified γ model, $\xi = 97.56$.

The spanwise sections that are located between the centerline and the $\pm 30\%$ of the propeller radius show worse agreement than the outer sections. The turbulence level is generally not enough to trigger transition on both cases, presumably the lower angle of attack induced by the propeller flow-field playing a role, too. Because of this reason, the extended laminar runs predicted by CFD (60 – 65%) are as downstream as the measurements (70 – 75%). However, the lower intermittency regions (blue and light blue lines) indicate the predicted separation region boundary aligns reasonably with them. The "two-headed" transition fronts predicted by both models in Fig. 18 are not experimentally observed. It is suspected that elevated levels of turbulence may result in a better prediction in this section.

For 30% radius at the blade up-going side, the agreement between the experiments and the measurements are slightly better (Fig. 19). For both models, the two-headed distribution obtained on the other side is not obtained. The edges of the laminar separation region align closely with the measurements. However, the predicted extended laminar region distance resembles the experiments but due to lack of 'upstream' transition effects, the slopes are not aligning like outer radii. The convection of the predicted C_f faster than the measurements, likely causing a very small calmed region rapidly being terminated. The calmed regions are expected to enlarge should the slopes align better. For wing-mounted propeller configurations, it is possible that the inner sections (between $y/R = -30\%$ and 30%) are affected by nacelle interference [6, 39], which makes comparing the very inner sections less relevant.

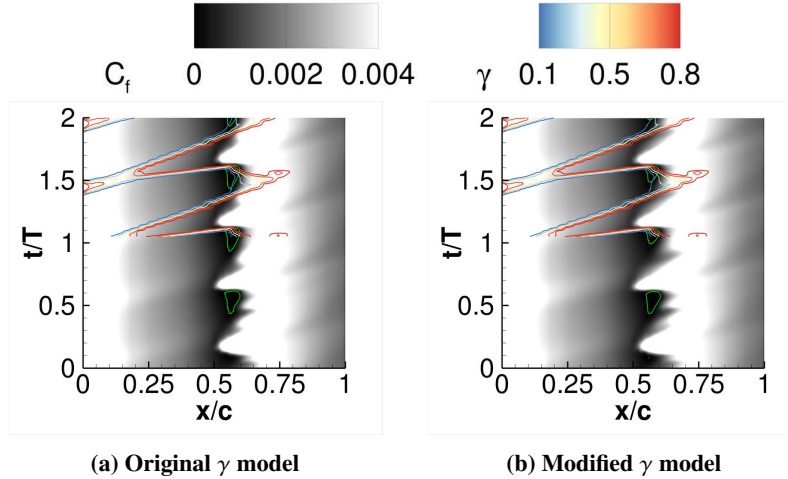


Fig. 18 Predicted C_f (contours) vs. measured intermittency (lines) at $y/R = -30\%$.

I. Realizability and justification of the turbulence correction

The following discussion is about the physical meaning of the turbulence correction implementation. For this purpose, we compare the actuator disk, actuator line (both with turbulence correction), and single blade simulations. The turbulence kinetic energy level, its dissipation, and its behavior in the slipstream is assessed. We only consider the cases with turbulence correction.

We start by comparing the k distribution and its evolution in the streamwise direction for actuator disk and actuator

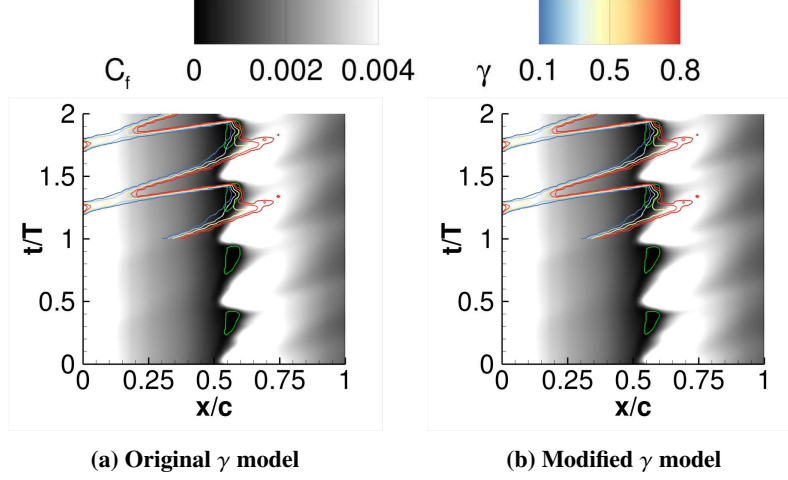


Fig. 19 Predicted C_f (contours) vs. measured intermittency (lines) at $y/R = 30\%$.

line simulations. The actuator disk and the *mean* actuator line (not shown) k distributions are identical by definition [35], though their evolution downstream are different.

It can be seen that the added TKE on the ACD simulations do not dissipate as quickly as the ACL simulation in Fig. 20. ACD simulation shows that the TKE distribution is relatively constant in the slipstream whereas the 'propeller blade vortices' on ACL dissipate quicker. We explain this by the magnitude and concentration of the TKE. There exists an increased magnitude of the k for ACL (by Gumbel distribution) which implies two concentrated k spots will be shed from the propeller. These two concentrated spots are more likely decay due to high local k gradients in the tangential direction (and up to some degree, in the radial direction as well). Additionally, as the propeller rotates, some spots will not be producing k until the next blade passage, which also implies finite time rate of change of k . For the ACD, the magnitudes of the k gradients are lower, which causes slower dissipation. This is confirmed by the Fig. 21 where mean specific dissipation rate in the slipstreams (ω) of ACD and ACL are compared against each other. The (mean) ACL clearly has much higher dissipation in the slipstream due to the factors mentioned above (higher local k gradients, and higher k magnitude). The instantaneous dissipation of ACL looks very similar to that of mean dissipation, where the higher dissipation regions in the wake of individual propeller blades are visible, confirming the previous findings.

We note that the *instantaneous* total k contained on the propeller for ACL simulations are similar in magnitude to that of the ACD, as shown in Fig. 22. However, the magnitude of the turbulence kinetic energy depends on the propeller azimuthal position, ψ . Even though the instantaneous turbulence kinetic energy levels of ACL fluctuate due to propeller position, the ACL still produces the same *mean* turbulence kinetic energy as the ACD. This is confirmed by nearly overlapping dashed lines in Fig. 22.

Next, we qualitatively analyze the propeller slipstream by comparing the total turbulent kinetic energy contained within. Currently, we have only shown that similar levels of k are added to the flow-field, but their evolution is not

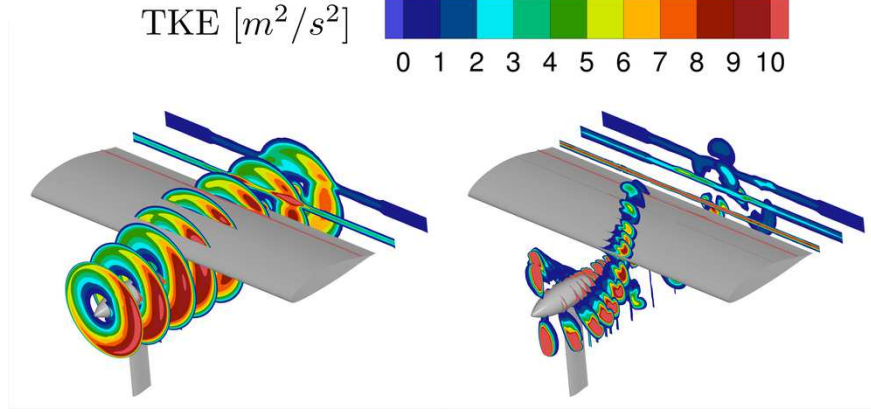


Fig. 20 Evolution of turbulence kinetic energy (k) in the slipstream for ACD (left) vs. ACL (right, instantaneous), using the same ξ . $k < 0.5 [m^2/s^2]$ is masked for better visibility.

yet qualitatively analyzed. We now compare the total k in the slipstream for ACL with the single blade simulations. Figure 23 shows the change of the total k in the slipstream for the actuator line and the single blade simulations, normalized by the freestream value. The total TKE values are compared against decaying grid turbulence as given in Pope [40] with exponent 1.1. We also note that the presence of wind tunnel walls and the strut boundary layer contaminate the integrals, which are subsequently blanked (removed from the integration domain) and not included in the integrals.

The TKE downstream of the propeller is initially lower, although becomes larger than that of single blade case for the ACL, with comparable decay. Around 1.5 propeller radii upstream of the leading edge, the TKE build-up of the strut becomes significant and start to contaminate the integral up to the end of the strut (strictly speaking, the strut also includes the nacelle). This also highlights the importance of blanking such regions which generate additional TKE that is not related to the propeller. Thus, the TKE due to strut and the TKE due to propeller are separated; which is the difference between the two curves. The initial decay of the ACL looks similar to that of decaying grid turbulence with an exponent of $n = 1.1$. When compared against the single blade simulations, we have seen that the initial jump in the TKE is larger than the ACL simulations. The initial decay is somewhat fast, but the remainder of the decay remains reasonable at small distances such as the present case (0.24 m). The higher levels of k added by ACL or propeller blades dissipate quickly, and the wing effectively sees very similar levels of turbulence kinetic energy at $(x - x_{LE})/R = 0$ (wing leading edge). Thus, we conclude that the added TKE should produce similar effects to that of fully-resolved propeller blades using local-correlation based transition models.

IV. Conclusion

A turbulence correction method is implemented in DLR-TAU code in the scope of SynTrac collaborative research center. With the implementation, the propellers are able to elevate the turbulence kinetic energy level in the slipstream

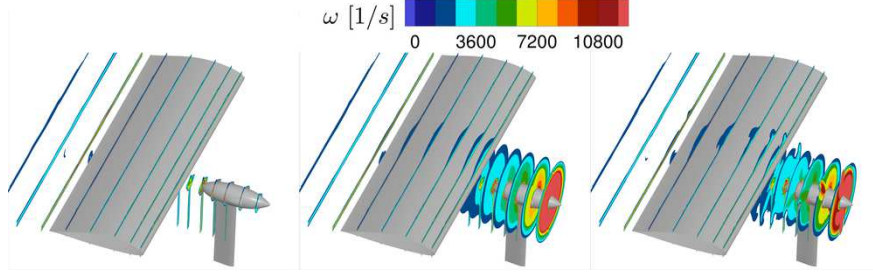


Fig. 21 Evolution of specific dissipation rate (ω) in the slipstream for ACD (left) vs. ACL (mean at the center, instantaneous at the right). $\omega < 1500[1/s]$ is masked for better visibility.

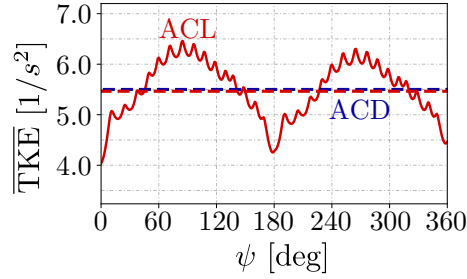


Fig. 22 Mean turbulent kinetic energy per unit area on the propeller. Red: ACL, blue: ACD, dashed line: time averaged quantity

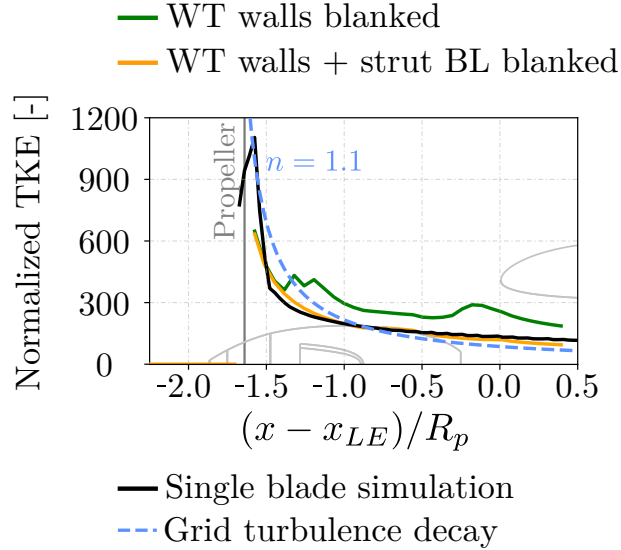


Fig. 23 Evolution of the normalized turbulence kinetic energy (k/k_∞) in the slipstream for the ACL and single blade, compared against the grid turbulence decay [40]

higher than the freestream values. The propellers modeled as actuator disks/actuator lines in DLR-TAU code cannot influence the transition characteristics within the slipstream without the turbulence correction. The implemented turbulence correction method changes the transition characteristics in the slipstream for a local, correlation-based transition transport model, DLR γ . The method uses variables that are strictly local, and does not increase the computational cost considerably. The turbulence correction works by relating the drag coefficient, sectional inflow

velocity and sectional blade parameters to the sectional turbulence production. The non-dimensionalization yields a constant term, C_1 . To better control the dissipation in the slipstream (subsequently the turbulence near the wing leading edge), a calibration coefficient ξ is also proposed.

The experiments carried out in the Laminar Wind Tunnel of Institute of Aerodynamics and Gas Dynamics (at University of Stuttgart) are numerically simulated considering the wind tunnel environment. The 2-bladed propeller designed to maximize the wake spacing is modeled using an actuator line method. Original DLR γ model and a modified variant is used to compare their effects on possible boundary layer relaxation (calmed region) effects. Boundary layer profiles are investigated at a section ($y/R = -80\%$) which showed relaxed boundary layers. Relaxed boundary layer occur after the natural transition point with laminar-like velocity distributions with slightly elevated levels of shear stress than the laminar profiles (but lower than turbulent profiles). These boundary layers are visible up to 75% on this spanwise section. The passage of the wake leaves a calmed region behind it, which temporarily reduces the sectional drag up to 6%. The calmed regions suppress the laminar separation characteristics as long as they exist.

Both transition models show little response when there is no turbulence in slipstream (turbulence correction off), which is noted as a problem of the γ model, in general. The transition in the slipstream mimics that of the clean wing, which is physically not correct. Transition location only shifts meaningfully once the turbulence correction is applied. The foremost transition is located as upstream as 20-25% chord whereas the aftmost transition point is located as downstream as 75 to 80% depending on the spanwise location. Some temporal movement in the transition front is observed when the turbulence correction is off for few sections. These are attributed to the highly localized, high pressure gradients imposed by the blade tip vortices on their trajectory causing a premature transition. The effects without turbulence correction diminish as outboard as $y/R = -64\%$ with a more or less flat response to the propeller slipstream.

The original γ model and the modified γ model generally predict similar laminar runs, but the C_f movement near the termination of the calmed region (local negative slope region) are slightly different. The modified γ model shows less sensitivity to the turbulence correction, where the upstream transition points shift downstream to about 45-50% chord location. Increasing the calibration coefficient ξ allows the foremost transition location to move upstream, and strengthens the formed calmed regions (which are moved downstream ever-so-slightly by increased TKE).

The findings regarding extended the laminar runs are validated against the experiments. The numerical predictions agree well with the measured intermittency data. The extent of laminar runs are predicted with good accuracy. The convection speeds which show up as slopes in the spatio-temporal plots match the numerical predictions. The original γ model and modified γ model generally predict similar aftmost transition locations like those of experiments, but the predicted foremost transition locations are more downstream on the modified γ model. This results in thinner transitional strips convecting for the modified γ model. The negative slope regions near the calmed region termination is more pronounced on the modified γ model, which seems to agree better with the measured values than the original.

These negative slope regions are noted to arise when laminar separation-type transition is present. The predictions worsen for inner sections such as $y/R = -30\%$ and 30% . These sections receive less turbulent kinetic energy from the propeller due to their central location. In a realistic configuration, the wing-mounted nacelle is expected to interfere with the flow between these spanwise sections, which would also disturb the boundary layers. In the present measurements, the nacelle occupies $y/R = \pm 28\%$, which has the potential to affect the flow-field.

Predicted slipstreams for ACD and ACL simulations are compared against each other with turbulence correction. The magnitude of the mean k at the propeller face is identical, but the dissipation in the slipstream is much higher for ACL due to local k gradients in tangential and radial direction, and the highly concentrated k over the propeller blade. The ACL with turbulence correction is shown to initially add slightly lower levels of turbulence when compared to the single blade simulations. The added turbulence rapidly decays, resembling decaying grid turbulence. ACL slipstream has similar levels of TKE, which are comparable to the single blade simulations. Thus, the wing effectively receives similar levels of TKE for both the ACL and the single blade simulations.

Overall, both original γ and the modified γ model are observed to show good performance in boundary layer relaxation / calmed region predictions due to propeller wake modeled by actuator lines. Both the turbulence correction methodology and the transition models are validated against the measurements. Both models show the boundary layer's interaction with the incoming high turbulence regions, where the sudden growth of intermittency acts like a *numerical by-pass* with respect to the transition criterion. With the proposed approach it is possible to account for propeller induced transition, and it is recommended to consider this for investigating propeller-wing interactions, as it influences the overall efficiency. As a future work, fully-resolved propeller blades will be compared against the turbulence correction coupled with the actuator line method, and the measurements. Additionally, as further measurements from the wind tunnel become available, such as boundary layer profiles, detailed validation studies will be carried out.

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