



Passive control of wing-tip vortices through a grooved-tip design

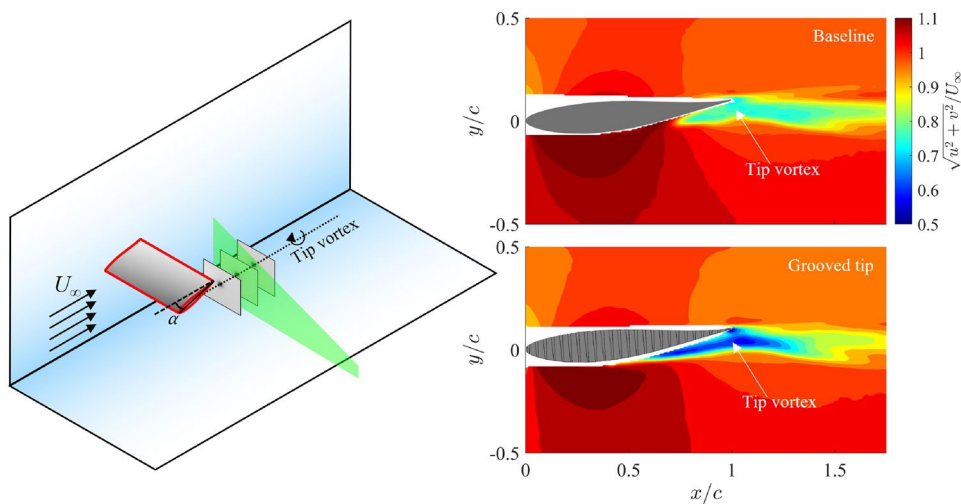
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Abstract

This paper investigates the characteristics and control of tip vortices generated by a finite wing, focusing on the impact of the novel grooved-tip designs. Tip vortices can lead to flow loss, noise, vibration and cavitation in hydrodynamic systems. We propose and develop a grooved-tip design, featuring multiple grooves distributed along the wing tip to alter the tip vortex structure and dynamics. Four grooved-tip designs, including tilted and shrinking grooves, were experimentally investigated. Streamwise and cross-flow particle image velocimetry (PIV) measurements were employed to visualise the flow fields near the wing tip and along the primary tip vortex trajectory. The PIV results demonstrate that the grooved-tip designs significantly reduce the velocity magnitude within the primary tip vortex. This velocity deficit is attributed to the decreased suction within the vortex core. Furthermore, cross-flow PIV measurements reveal that the tip separation vortex is substantially suppressed, and the strength of the primary tip vortex is significantly mitigated. Downstream of the wing, the grooved tips lead to a reduction in vortex swirling strength and an enlargement of the vortex dimensions, suggesting enhanced diffusion and a reduction of the pressure drop of approximately 40%, based on the estimation from a reduced-order model linking pressure to vortex swirling strength. Our findings highlight the potential of these grooved-tip designs to effectively modify tip vortex behaviour and mitigate the pressure drop within the tip vortex region, with negligible changes to the lift and drag performance. This work can inform advanced passive vortex control strategies in wing- and blade-based systems, with potential applications in hydrofoils of marine vessels and underwater vehicles, as well as in turbines and propellers.

Graphical abstract



List of symbols

sR Semi-aspect ratio
 c Chord length
 C_D Drag coefficient

C_L Lift coefficient
 d Diameter
 $O(x, y, z)$ Cartesian coordinates: streamwise, cross-stream, spanwise

Extended author information available on the last page of the article

$O(r, \theta, X)$	Cylindrical coordinates: radial, azimuthal, axial
p	Pressure
r_c	Viscous core radius
Re_c	Chord-based Reynolds number
$\mathbf{u} = (u, v, w)$	Fluid velocity
U_∞	Free stream velocity
α	Angle of attack
Γ	Circulation
λ_c	Swirling strength
ρ	Fluid density
ω	Vorticity

Abbreviations

PIV	Particle image velocimetry
PTLV	Primary tip leakage vortex
PTV	Primary tip vortex
TEV	Trailing-edge vortex
TLV	Tip leakage vortex
TSV	Tip separation vortex

1 Introduction

The formation of tip vortices, driven by the pressure difference between the pressure and suction sides of a finite wing or blade tip, generate a swirling flow in the wake. These vortices and associated problems, such as induced drag, wakes and noise, have been a long-standing concern in wing- and blade-based systems over the past decades. From an aerodynamic point of view, wing-tip vortices contribute to induced drag, accounting for up to 40% of the total drag (Margaris and Gursul 2006). Furthermore, tip leakage vortices are responsible for nearly one-third of the aerodynamic loss in gas turbine cascades (Denton 1993). In hydrodynamic applications such as propellers, pumps and tidal turbines, tip vortices and tip leakage vortices are often linked to cavitation due to the pressure drops within the vortex core (Pereira et al. 2004; Liu et al. 2018), which is detrimental due to increased noise (Wittekind and Schuster 2016; Pennings et al. 2016), structural vibrations (Cheng et al. 2025; Liu and Tan 2019) and reduces efficiency (Aktas et al. 2016; Capone et al. 2023). Additionally, applications in marine environment face a higher risk of cavitation erosion due to the corrosive nature of seawater (Hou et al. 2014). Hydrofoils are increasingly used in sailing (Ng et al. 2025) and commercial vessels (Godø and Steen 2023a) for their efficiency and performance benefits, though cavitation remains a key challenge in their design and operations (Godø and Steen 2023b; Godø et al. 2024).

With the growing emphasis on renewable energy, tidal stream power is expected to play a vital role in achieving the net-zero transition by 2050. Tidal turbines, however, also

face persistent challenges related to tip vortices and cavitation, limiting their efficiency and reliability when operating at higher tip speed ratio (TSR) (Adcock et al. 2021). Attenuating tip vortices can lead to enhanced power output and reduced mechanical stress on powertrain components (Ning and Dykes 2014; Wimshurst et al. 2018). Moreover, the efficiency of tidal and wind turbines is often constrained by turbine-to-turbine wake interactions, which can result in power losses of up to 20% (Barthelmie et al. 2011). A key contributor to those losses is tip vortices, which significantly influences the generation and propagation of the turbine wake (Fischereit et al. 2022). Reducing the intensity of the tip vortex, such as by lowering the axial-to-circumferential momentum ratio of the swirling vortex, can trigger earlier vortex breakdown (Robinson et al. 1994; Liu et al. 2024). This earlier breakdown has been shown to facilitate faster wake recovery (Lignarolo et al. 2015), which is beneficial for improving turbine array performance. Therefore, effective control of tip vortices and associated pressure drops and wakes is critical for the further development of the tidal energy sector. Given the widespread occurrence of vortex-induced cavitation across various underwater applications, including ship propellers, underwater vehicles, tidal stream turbines, hydropower turbines and pumps, developing advanced approaches for controlling tip vortices and mitigating the cavitation risks can also inform novel technologies in transformative engineering applications.

In essence, primary tip leakage vortex (PTLV), which occurs in the presence of a small gap between the blade tips and the casing wall, and primary tip vortex (PTV), which forms in the absence of a casing wall, share the same underlying flow physics. Due to the pressure difference between the pressure and suction sides of a wing or a blade, the flow near the tip rolls up into a swirling vortex, resulting in a low-pressure region at its core (Liu et al. 2025a; Bi et al. 2024). Various active and passive strategies have been proposed to mitigate the PTLV and PTV.

Active strategies to control tip vortices mainly focused on wing-based systems, such as active trailing-edge tab to displace or enhance diffusion of a tip vortex (Panagakos and Lee 2006) and oscillating control surfaces to vary the degree of boundary layer separation by means of “zero mass-flux perturbations” (Greenblatt et al. 2005), synthetic jet to produce vortex diffusion and stretching (Margaris and Gursul 2006; Dghim et al. 2020; Zaccara et al. 2022) and plasma actuation to displace the tip vortices (Dong et al. 2022). For helicopter rotor blades, active twist actuation has demonstrated the ability to control both the strength and trajectory of the blade tip vortices (Bauknecht et al. 2017). Other active strategies have focused on controlling the leading-edge vortex (LEV) during unsteady motions such as plunging to enhance aerodynamic performance. For instance, Feng et al. (2020) demonstrated that suction can inhibit the

grow of the secondary vortex and delay LEV detachment, thus prolonging the duration of transient high lift. Building on this, Wang et al. (2021) proposed a combined blowing and suction strategy to both strengthen the LEV and delay its detachment, achieving further improvements in unsteady lift. However, these active strategies substantially introduce additional structural and system complexity and require external power.

Passive strategies primarily include geometric modification to the tip region. For example, Ji et al. (2021) numerically investigated the thickened and raked blade tips for a pump-jet propulsor, reporting a decrease in the tip leakage vortex (TLV) strength and significantly improved negative pressure peaks; however, the rotor efficiency decreased due to reduced thrust and lift-to-drag ratio at outer radii. Wang et al. (2023) found that a wavy blade tip divided the single large-scale PTLV into smaller-scale vortices along the tip chord, reducing the PTLV swirling strength but increasing the swirling near the suction side due to secondary tip leakage vortices.

One of the most promising passive approaches to control tip vortices is the C-shaped single-groove design by Liu and Tan (2018). The groove generates a jet that impinges on the PTLV, suppressing its development across a range of inflow velocities. However, a follow-up study by Jiang et al. (2022) experimentally observed that the shrinking C-shaped groove at the blade tip could induce extra cavitation due to high local velocity inside the groove. A recent study by Bi et al. (2024) demonstrated the potential of mitigating cavitation due to TLV through the application of a thin layer of porous metal at the blade tip. However, the study primarily analysed the TLV behaviour across different tip gap sizes without examining the role of permeability. More recently, Liu et al. (2025a) proposed a permeable tip treatment to tidal turbine blade tips, inspired by the findings of Cummins et al. (2018) who demonstrated the role of permeability in controlling vortical structures in the separated vortex ring behind a dandelion seed. Their analysis covered a range of permeability and suggested that an optimal permeability for controlling the tip vortices exists, which can most effectively mitigate the associated pressure drop through enhanced vortex diffusion and enlarged vortex dimensions. In a similar vein, Liu et al. (2025b) conducted a numerical study on a compressor cascade, demonstrating significant effects of permeable tip treatment in controlling TLV.

Building upon our previous work (Liu and Tan 2018, 2020; Liu et al. 2025a, 2025b), this study aims to develop a novel passive approach that combines the benefits of groove-jet impingements and permeable tip treatment to mitigate the tip vortices and associated pressure drop, while achieving an effective two-dimensional (2D) permeability. Advancing from our earlier single-groove design (Liu and Tan 2018, 2020; Han et al. 2022), the present approach utilises multiple

grooves distributed along the entire tip chord. Unlike the single-groove design, which may introduce additional local pressure minima due to concentrated high-speed jet flow within the groove, the distributed multi-groove configuration approximates uniform permeability, thereby avoiding the undesirable effects. Experiments were performed on a wing model in a water tunnel, with time-averaged flow fields on streamwise and cross-flow planes visualised using particle image velocimetry (PIV) measurements for a baseline case and four grooved-tip designs. In this paper, we have demonstrated that the fundamentally novel concept of controlling tip vortices through tip permeability can be achieved by practical designs.

The rest of the paper is organised as follows. The lift and drag performance of the baseline and modified wings are compared in Sect. 3.1. In Sect. 3.2, we examine the velocity deficit within the tip vortices, highlighting the alleviation of pressure drop around the tip vortex core. In Sect. 3.3, the vortex structures with and without the grooves are compared to elucidate changes in the flow patterns. In Sect. 3.4, the change in the vortex swirling strength is discussed and an analytical relationship between the swirling strength and pressure drop is derived to estimate the pressure change with the tip vortex core.

2 Methodology

2.1 Experimental setup

The experiments were conducted in a water tunnel located at the University of Edinburgh (Robinson et al. 2015). The water tunnel is 8 m long, 0.4 m wide, and filled with water to a depth of 0.4 m; flow is preconditioned by curving vanes before entering and exiting the channel. The half-wing model has a thickened NACA-63-415 hydrofoil profile, consistent with the blade tip geometry in the UK Supergen ORE Tidal Benchmarking Project (Harvey et al. 2023). The wing features a chord length of $c = 0.1$ m and a span of $b = 0.2$ m, resulting in a semi-aspect ratio (b/c) of $sR = 2$. The coordinate system $O(x, y, z)$ indicates the streamwise, cross-stream and spanwise coordinates (see Fig. 1). The origin of the coordinate system is located at the leading edge at the wing root. The velocity components in each direction are u , v and w , respectively. The free stream velocity was calibrated using a Nortek Vectrino acoustic Doppler velocimeter at the centre of the measurement plane, in the absence of the wing and underwater cameras. Experiments were performed at a nominal velocity of $U_\infty = 0.3$ m s⁻¹, equivalent to a Reynolds number based on the chord length of $Re_c = 3.22 \times 10^4$, with a streamwise turbulence intensity of approximately 4.5%. Lift and drag were measured over a wide range of angles of attack, while for flow visualisations

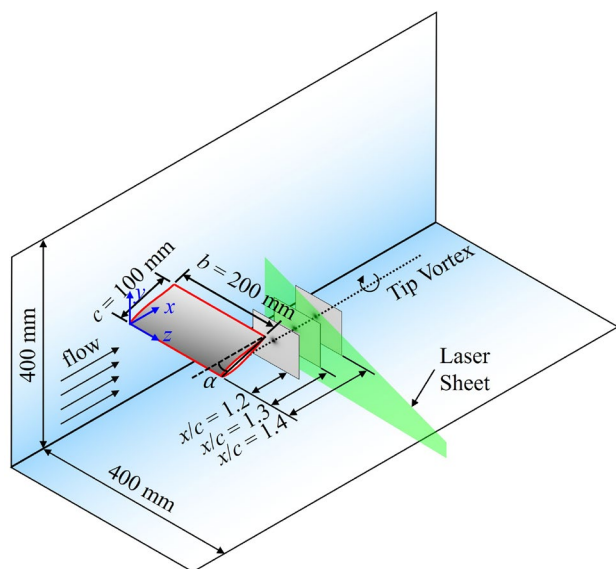


Fig. 1 Dimensions of the water tunnel and coordinate system used in the present study. The displayed contours are for illustrative purposes

the angle of attack was set at $\alpha = 6^\circ$, which is close to the hydrofoil's optimal lift-to-drag ratio (Harvey et al. 2023). At $\alpha = 6^\circ$, the leading edge was positioned 190 mm below the free water surface.

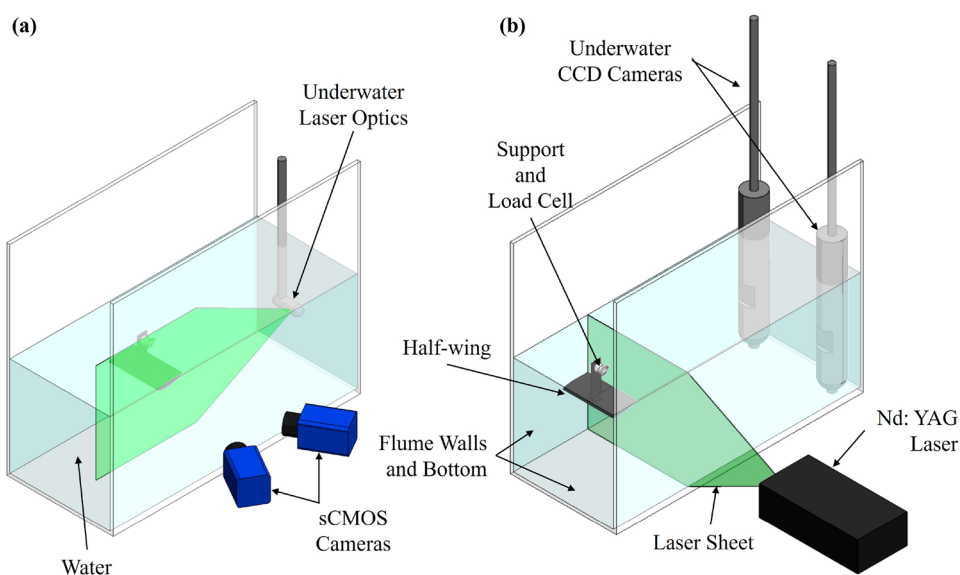
2.2 Particle image velocimetry measurements

A schematic diagram of the inverted half-wing model, along with the experimental setup for PIV measurements, is shown in Fig. 2. Model inversion was employed to minimise the flow disturbance on the suction surface from the support structure and the load cell. Quantitative flow field

measurements were performed using a LaVision FlowMaster stereoscopic PIV (2D3C) system. Schematic diagrams of the experimental setup for the streamwise (on $x - y$ planes) and cross-flow (normal to free stream, on $y - z$ planes) PIV measurements are shown in Fig. 2a, b, respectively. Streamwise measurements were conducted at a fixed plane slightly inboard of the tip, at $x/c = 1.95$, whereas the cross-flow measurement planes were evenly spaced between $x/c = 0.6 - 1.5$ with a $0.1c$ increment.

A NewWave Solo 200XT double-pulse Nd:YAG laser, with a maximum energy of 200 mJ/pulse was used to illuminate the desired planes. Water was seeded with silver-coated hollow glass spheres with a mean diameter of $10 \mu\text{m}$ and a density of 1.4 g cm^{-3} . PIV images were captured by two Imager sCMOS cameras placed on the side of the water tunnel for the streamwise measurements, or two Imager ProSX 5 M cameras in sealed torpedo-shaped compartments placed downstream of the wing for cross-flow measurements. Scheimpflug adapters were used to compensate for the oblique viewing angle from the perspective view. Cameras and laser pulses were triggered simultaneously by a PTU X synchroniser. For each measurement, 500 instantaneous flow fields over 850 convecting periods (c/U_∞) were captured at a rate of 1.75 Hz to accurately account for the mean flow statistics. The commercial software package LaVision DaVis 10.2 was used to calculate the velocity vectors. The interrogation window size was 64×64 pixels, which was then reduced to 32×32 pixels with a 75% overlap. The effective grid size was approximately $1\%c$. The uncertainty of the velocity measurements was reported by the DaVis 10.2 software to be within 2% of the mean free stream velocity, where the uncertainties in the 3D velocity components (u, v, w) are calculated by propagating the known uncertainties of the 2D displacements from each camera, namely

Fig. 2 Experimental setup for the particle image velocimetry (PIV) measurements: **a** streamwise, **b** cross-flow



(u_1, v_1) and (u_2, v_2) , through the calibration mapping function (LaVision 2020).

It is important to note that the underwater camera casings have a diameter of 89 mm and span across the entire water depth, which unavoidably introduced blockage to the flow (see Fig. 2b). Consequently, the measured free stream velocity in the presence of the underwater PIV cameras was reduced to $U_\infty = 0.28 \text{ m s}^{-1}$, corresponding to an updated chord-based Reynolds number of $Re_c = 2.80 \times 10^4$ for cross-flow measurements. A similar underwater flow visualisation technique was previously used in our laboratory (Muir et al. 2017).

2.3 Force measurements

Lift and drag measurements were conducted over a range of angles of attack from $\alpha = 0^\circ$ – 14° , in increments of 1° . The chordwise (F_x) and chord-normal (F_y) forces were collected using an ATI Industrial Automation Nano17 IP68 six-axis load cell with a full-scale load (FSL) of 25 N and a resolution of 0.00625 N, and logged to a computer running the ATIDAQ .Net software through a National Instruments USB-6210 data acquisition device. Force data were recorded with a sampling frequency 1000 Hz. For each measurement, force signals were recorded for 120 s over 360 convecting periods (c/U_∞), which is sufficiently long for the mean and root-mean-square (r.m.s.) of the signals to converge. Buoyancy forces and load cell factory offset were accounted for in the water tunnel for all measured angles of attack; the load cell was zeroed in quiescent flow before each measurement to obtain the hydrodynamic forces. Axis rotation was performed to convert F_x and F_y into the hydrodynamic lift and drag. The maximum uncertainty in the force measurements are estimated to be approximately $B_{T_{\max}}(C_L) = \pm 0.090$ and $B_{T_{\max}}(C_D) = \pm 0.030$, respectively, based on the methods of Moffat (1985). The relatively large uncertainties are attributed to the fact that the measured force was a small percentage of the FSL. For example, the maximum recorded lift was 2.65% of the FSL, while the maximum recorded drag was only 0.41% of the FSL. The quantification of force measurements uncertainties is detailed in “Appendix 1”.

Note that, the half-wing model was supported by a small NACA 0010 symmetric aerofoil. The drag coefficient of the supporting NACA 0010 foil at zero angle of attack was estimated to be approximately $C_D \approx 0.02$ based on a chord length of 40 mm and a span of 125 mm. The drag contribution from the support structure introduced an overhead of $C_D \approx 0.005$ to the overall drag coefficient for all cases at all angles of attack. Tare drag is included in the presented data.

2.4 Models with grooved tips

Liu et al. (2025a) suggests that an optimal range of tip permeability exist for suppressing the pressure drop due to tip vortices for a model-scale tidal turbine. The optimal mitigation effect is achieved when the non-dimensional permeability, Darcy number $Da = \kappa/\bar{\tau}^2$ (here, κ is the permeability and $\bar{\tau} = 0.0952c$ is the average tip thickness), falls between 10^{-6} and 10^{-5} . Accordingly, we propose multiple-groove configurations, with twenty-five evenly distributed grooves. Each groove having a nominal width of $0.01c$ (in the chordwise direction) and a fixed depth of $0.02c$ (in the spanwise direction), designed to produce an equivalent 2D permeability within this range. For the determination of the 2D permeability of the grooved tips, please refer to “Appendix 2”. Four different groove geometries are investigated, as illustrated in Fig. 3. These designs vary in terms of the contraction ratios from the pressure side (PS) to the suction side (SS) and the orientation for each groove. Overall, five half-wing models were manufactured, comprising one baseline case without any tip modification and four grooved-tip designs, as shown in Fig. 3b–e. The numerically-derived chord-normal non-dimensional permeability Da for the four grooved-tip designs are summarised in Table 1.

3 Results and discussion

3.1 Lift and drag performance

This section evaluates the influence of the grooved-tip design on the aerodynamic performance of the wing,

Fig. 3 NACA 63-415 hydrofoils with grooved tip: **a**, **b** 25G-R-CN, **c** 25G-C-CN, **d** 25G-R-P, **e** 25G-C-P

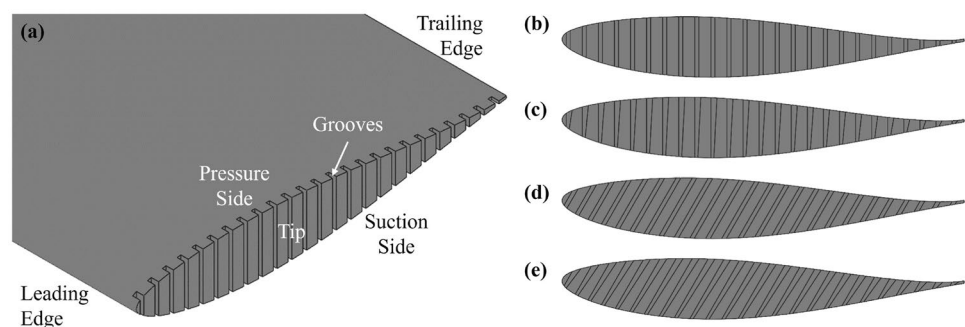
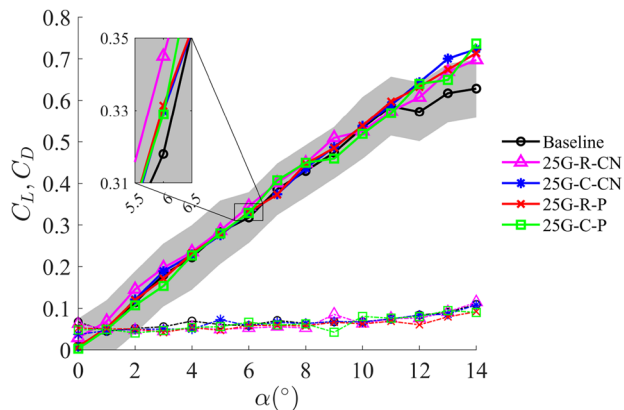


Table 1 Comparison of Da values for different tip designs

Tip configuration	Da ($\times 10^{-5}$)
Baseline	–
25G-R-CN	2.20
25G-C-CN	1.70
25G-R-P	1.59
25G-C-P	1.19

**Fig. 4** Variations of the lift coefficient C_L (solid lines) and drag coefficient C_D (dashed line) as a function of angle of attack α . The shaded region indicates the interval within one standard deviation from the mean C_L of the baseline case

alongside primary objective of controlling tip vortices and mitigating the pressure drops within the PTV region. Figure 4 presents the variations of the lift (C_L) and drag (C_D) coefficients as a function of the angle of attack (α) for the baseline and grooved-tip designs. The lift coefficients (solid lines) of the baseline and all grooved-tip designs are found to be comparable, particularly within the pre-stall angles of attack that all mean C_L values for the grooved-tip designs fall within one standard deviation of the baseline case indicated by the shaded region in Fig. 4. A moderate delay in onset of stall is observed at higher angles of attack for wings with grooved tips, suggesting that these designs may contribute to delaying flow separation in the near-tip region. The drag coefficients (dashed lines) generally follow a similar trend, although they exhibit a greater degree of scattering due to the relatively higher percentage uncertainty of drag measurements.

The inset in Fig. 4 highlights the differences in the lift coefficient between the baseline case and grooved-tip designs at $\alpha = 6^\circ$. At this specific angle of attack, the grooved tips yield slightly higher lift coefficients than the baseline case; however, these differences are relatively small and fall within the range of measurement uncertainties. Overall, these results indicate that the grooved-tip

designs have a positive, albeit limited, impact on the wing's lift and drag performance.

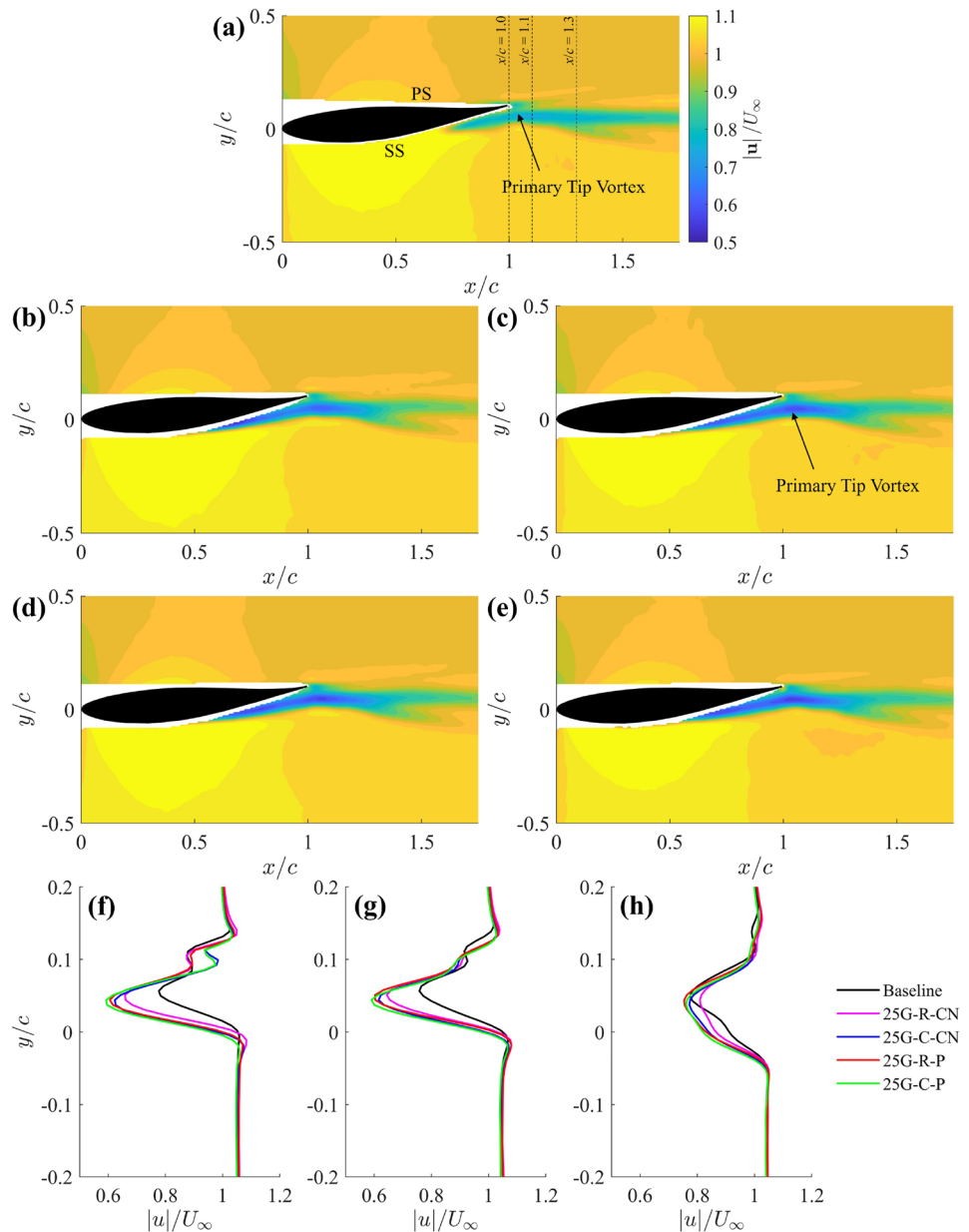
For the current experiments, the grooves at the wing tip have a depth of 2% of the tip chord length and 1% of the wing span. It is, therefore, anticipated that when such a grooved-tip design is scaled for application in systems like tidal turbine blades, where the grooved-tip structure contributes a much smaller proportion of the turbine diameter [e.g. 0.1% of the turbine diameter in Liu et al. (2025a)], the grooved-tip design is unlikely to result in a significant change in the overall power output or thrust in normal operating TSR.

3.2 Velocity deficit within the primary tip vortex

3.2.1 Velocity magnitude deficit

This section presents the velocity field results obtained from the PIV measurements, primarily demonstrating how grooved-tip designs effectively reduce the velocity magnitude within the PTV region, leading to a significant alleviation of the associated pressure drop. Figure 5 shows the time-averaged velocity magnitude contours on a streamwise plane at $z/c = 1.95$ (see Fig. 2a for experimental setup) for both the baseline case the four different grooved-tip designs. While the free stream velocities remain constant across all cases due to repeatable inflow conditions, a notable velocity deficit is observed within the PTV region for the grooved tips. For instance, the minimum velocity magnitude within the PTV significantly reduced from 0.76 m s^{-1} for the baseline case (Fig. 5a) to 0.60 m s^{-1} for the 25G-C-CN tip (Fig. 5c), representing a 21% reduction. The velocity deficit within the PTV core is better illustrated in Fig. 5f–h with a more direct comparison at three streamwise locations. At $x/c = 1.0$ and 1.1 , the baseline tip consistently present a higher velocity than all grooved-tip designs. Later at $x/c = 1.3$, no noticeable difference between the baseline case and grooved tips can be seen, except the 25G-R-CN case which exhibited a slightly faster wake recovery. This observation is meaningful as it suggests that the grooved-tip design can substantially alleviate the pressure drop inside the tip vortex. This inference aligns with the pressure distribution of a steady axisymmetric vortex based on the governing equations (Batchelor 1964), which is qualitatively valid for the low-to-moderate Reynolds number (in the present study, $Re_c = 2.80 \times 10^4$) and incompressible flow conditions in the water tunnel, where inertial effects are dominant and viscous dissipation is moderate. In addition, Zhao et al. (2024) verified the pressure–velocity relation, $\bar{p} \sim \frac{1}{2}\bar{u}^2$, derived from simplified Reynolds-averaged Navier–Stokes (RANS) equation using three-dimensional experimental

Fig. 5 Time-averaged velocity magnitude contours at $z/c = 1.95$ streamwise plane for **a** baseline, **b** 25G-R-CN, **c** 25G-C-CN, **d** 25G-R-P, **e** 25G-C-P. Time-averaged velocity magnitude as a function of cross-stream distance (velocity profile) at **f** $x/c = 1.0$, **g** $x/c = 1.1$, **h** $x/c = 1.3$. SS: suction side; PS: pressure side



data for a tip vortex. The derived relation signifies that the mean static pressure inside a tip vortex is governed by the local mean axial velocity, resembling the relationship described by Bernoulli’s principle.

An in-depth assessment of the individual velocity component, including the streamwise (u/U_∞), cross-stream (v/U_∞) and spanwise (w/U_∞) velocities, on the streamwise plane $z/c = 1.95$ is shown in Fig. 6 for the baseline case and 25G-C-CN design as an example. Figure 6 reveals that the velocity magnitude deficit in the PTV region is primarily dominated by the streamwise velocity change. In particular, the noticeable reduction in streamwise velocity within the PTV region is similar to the observation in the velocity magnitude contours in Fig. 5.

As shown in the top row of Fig. 6, the streamwise velocity contours illustrate a larger region of relatively high velocity near the trailing edge for the baseline case (Fig. 6a), which is significantly reduced for the 25G-C-CN design (Fig. 6b). This reduction in streamwise velocity within the PTV region is critical as it is a direct implication that the grooved-tip design exhibits a relatively higher local static pressure and a reduced pressure drop, according to the pressure–velocity relation derived by Zhao et al. (2024). Furthermore, for the grooved-tip design, the more concentrated low-velocity region (indicated by the blue colour) shifts upstream, suggesting the groove-jet impinges on and mixes with the PTV.

The cross-stream velocity contours further highlight the impact of the grooved tip. Figure 6c for the baseline case

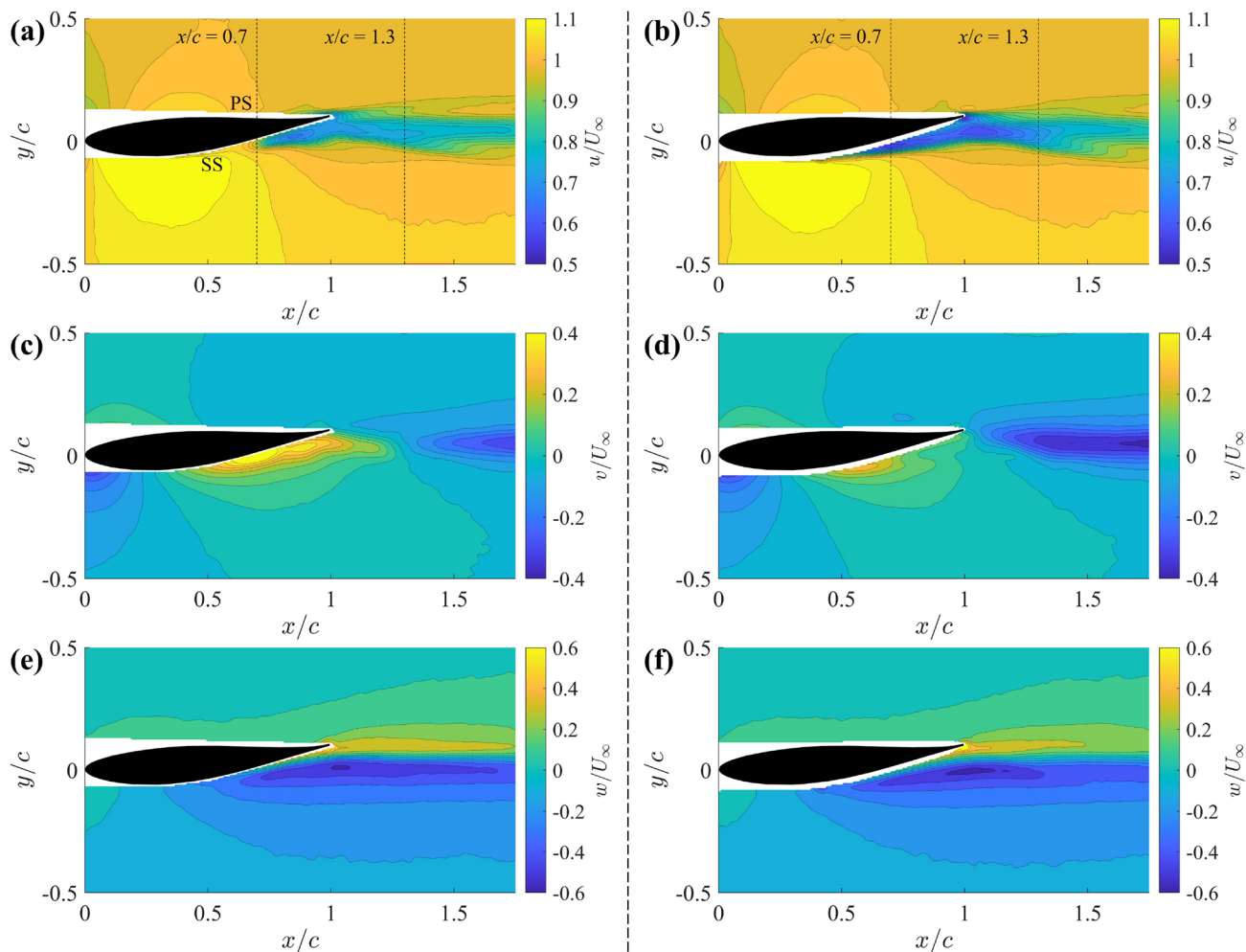


Fig. 6 Time-averaged streamwise (a, b), cross-stream (c, d), and spanwise (e, f) velocity contours at $z/c = 1.95$ plane for baseline (a, c, e) and 25G-C-CN (b, d, f)

shows a steeper velocity gradient on the suction side compared to the 25G-C-CN case in Fig. 6d, where the grooves effectively reduce this velocity gradient. This reduction in pressure gradient on the suction side may contribute to delaying boundary layer separation on wings with grooves. Additionally, the upward cross-stream velocity region near the suction side extends further downstream for the baseline case, beyond the trailing edge, due to the sweeping effects from the tip separation vortex (TSV). In contrast, the 25G-C-CN case exhibits a strong downward wake immediately after leaving the trailing edge (Fig. 6d), indicating reduced impact from the TSV and enhanced mixing of the wake with the free stream. In the bottom row, while the out-plane (yellow) velocities are comparable between the two cases, the 25G-C-CN case (Fig. 6f) shows the in-plane (blue) velocity extending further from the wing surface compared to the baseline (Fig. 6e), implying a more diffused tip vortex structure. These detailed velocity components analysis provide further

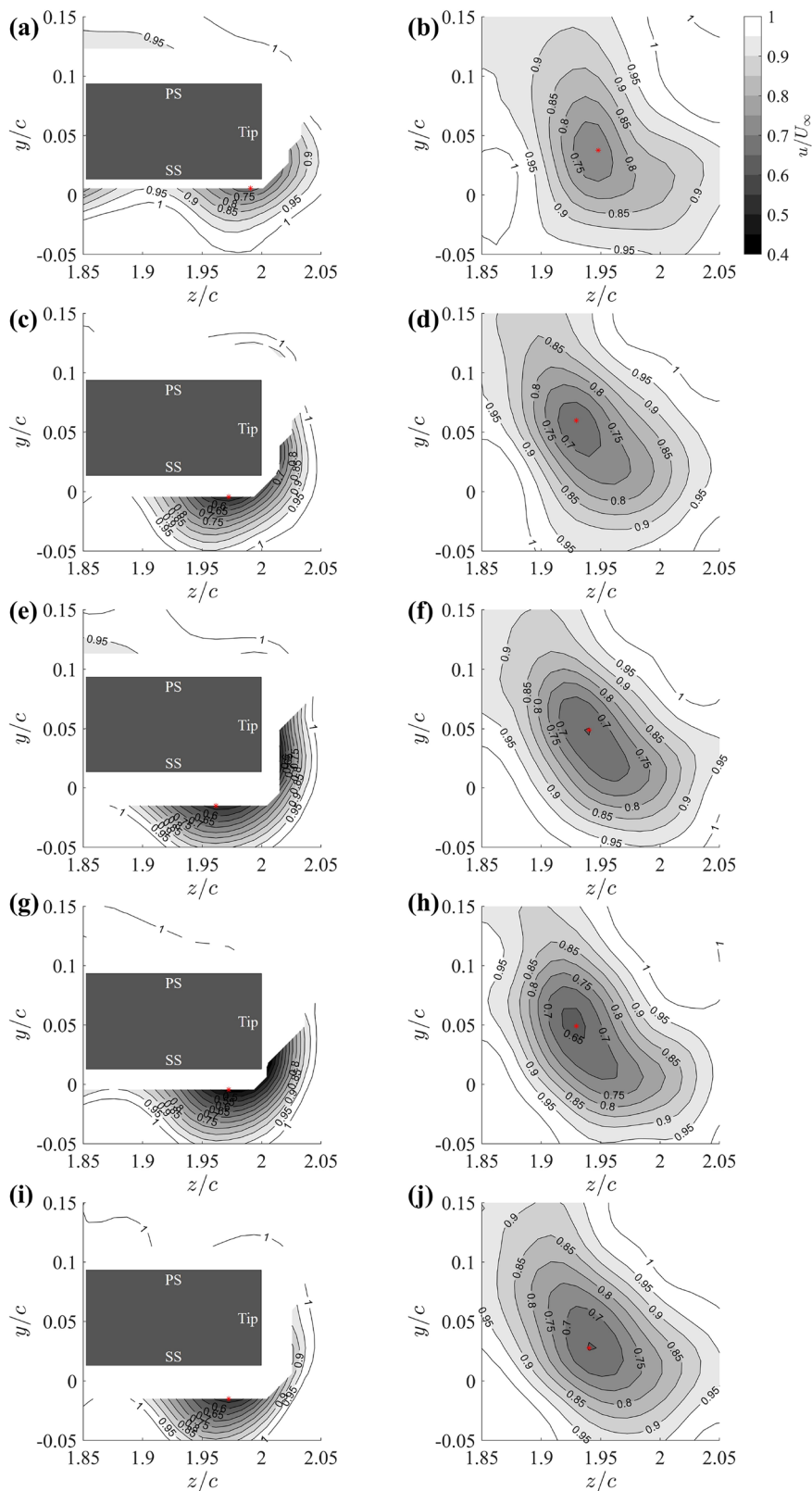
evidence of the effectiveness of the grooved-tip design in modifying the flow field around the wing tip.

3.2.2 Axial velocity deficit

Figure 7 provides a comparison of the time-averaged streamwise velocity contours at two cross sections for the tip vortices, namely $x/c = 0.7$ (left column, on-wing) and $x/c = 1.3$ (right column, in-wake), for the baseline and four grooved-tip configurations. In these plots, darker shades correspond to lower velocities, indicating a greater velocity deficit, while lighter colours represent velocities closer to the free stream velocity.

For the baseline case (Fig. 7a, b), a prominent region of velocity deficit is observed in the PTV region near the wing tip, with the vortex core reaching a minimum value of approximately $u/U_\infty = 0.75$. In contrast, all grooved-tip designs exhibit noticeable changes compared to the baseline.

Fig. 7 Time-averaged stream-wise velocity contours at $x/c = 0.7$ (left column) and $x/c = 1.3$ (right column) cross-flow plane for **a, b** baseline, **c, d** 25G-R-CN, **e, f** 25G-C-CN, **g, h** 25G-R-P, **i, j** 25G-C-P. The red asterisk (*) indicates the location of minimum velocity



Notably, a more significant streamwise velocity deficit is consistently observed in the vortex cores during the tip vortex roll-up process (left column) and after the vortex is fully

developed (right column). The spanwise stretching of the low-velocity region at $x/c = 0.7$ suggests an earlier onset of vortex roll-up and increased entrainment, potentially due to

the enhanced mixing between the groove jets and the PTV. Assuming the vortex axis is almost parallel with the free stream, the streamwise velocity component u serves as a reasonable proxy for the vortex axial velocity v_x . Consequently, the observed reduction in the streamwise velocity provides further evidence that grooved-tip designs are effective in mitigating the pressure drop with the tip vortex.

To further analyse this phenomenon and account for the PTV's trajectory, we extract and present the minimum streamwise velocity magnitude along the tip vortex trajectory on all cross-flow planes, ranging from near the mid-chord ($x/c = 0.6$) to half chord-length downstream of the trailing edge ($x/c = 1.5$), as shown in Fig. 8. Particular attentions were paid to ensure the minimum velocity was extracted within the PTV region. The velocity profiles for all configurations consistently show an increasing trend downstream, signifying the expected diffusion and decay of tip vortices as they convect downstream from the wing tip. Compared to the baseline case, the grooved-tip designs generally exhibit lower minimum streamwise velocity, despite some data scattering is observed. These results, again, reinforce that grooved-tip designs effectively increase the

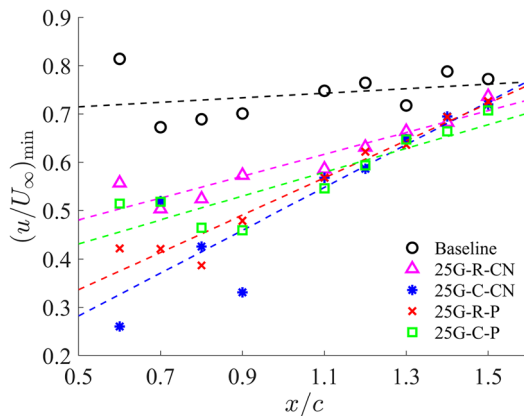
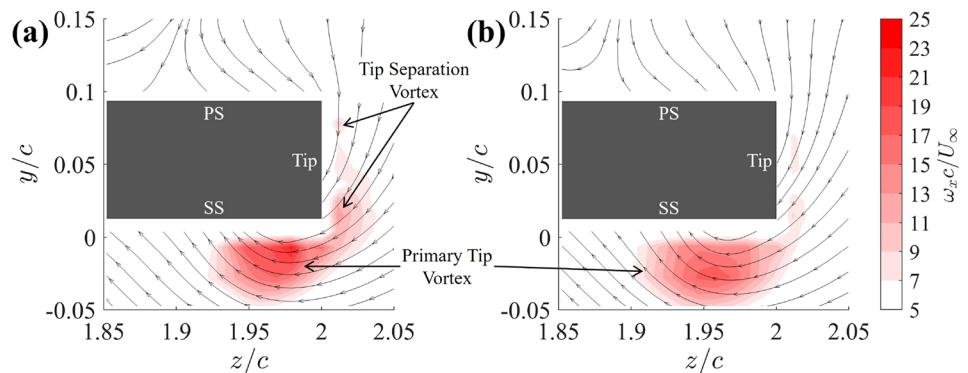


Fig. 8 Comparison of streamwise velocity on each cross section along the vortex core trajectory between baseline and grooved tips

Fig. 9 Time-averaged vorticity contours superimposed with time-averaged streamlines for cross-flow plane at $x/c = 0.7$ for **a** baseline, **b** 25G-C-CN



pressure within the PTV core, thereby reducing the pressure drop and lowering the risk of tip vortex cavitation.

Among the four grooved-tip designs, they all demonstrate comparable velocity deficits in the PTV region, as evidenced in Fig. 5. This consistency is attributed to their similar effective 2D permeability, which governs the interaction between the grooves and the flow. Overall, considering both the amplitude of velocity deficits (Fig. 8) and the lift-drag performance (Fig. 4), the 25G-C-CN design appears to be the most promising configuration among those tested. Consequently, this design has been selected for a more detailed comparative analysis against the baseline case in subsequent sections.

3.3 Vortex pattern

This section aims to investigate how the grooved-tip designs fundamentally alter the vortex structures, consequently affect the intensity of the tip vortices. The key finding is that the grooved-tip design significantly suppresses the TSV and leads to a weaker, more diffused PTV. Figure 9 presents a comparison of the time-averaged streamwise vorticity, $\omega_x = \partial w / \partial y - \partial v / \partial z$, and streamlines between the baseline case and the 25G-C-CN design at the cross-flow plane of $x/c = 0.7$. In the baseline case (Fig. 9a), two distinct components of tip vortices are identified: the PTV, located just below the suction side, and the TSV which is observed to grow in size from the pressure side to the suction side. There is also evident entangling and mixing of the PTV and TSV in the baseline configuration. In contrast, Fig. 9b demonstrates a significant reduction in the magnitude of vorticity for the TSV due to the groove-tip design, indicating that the grooves are highly effective in weakening this vortex. Furthermore, the grooved-tip design results in a noticeable reduction in vorticity magnitude of the PTV, accompanied by an enlarged vortex size, which is a clear sign of enhanced vortex diffusion.

To further understand the underlying physics of how grooved-tip treatments weaken tip vortices and mitigate the associated pressure drop, we examined key vortex parameter

such as vortex circulation. Figure 10 presents the non-dimensional vortex circulation, Γ/cU_∞ , obtained by integrating the streamwise vorticity ω_x at each streamwise station from cross-flow PIV measurements. A threshold was applied to include only the positive vorticity, accounting for the PTV’s contribution along its trajectory while excluding negative vorticity from the trailing-edge vortex (TEV). Upstream of the trailing edge, the circulation grows along the streamwise direction with little variation in its rate, and it tends to plateau beyond the trailing edge. Notably, the circulation values for each groove-tip design and the baseline are close at each cross section, especially upstream of the trailing edge. This suggests that the total circulation generated by the tip vortices remains largely unaffected by the grooves, which aligns with the slight impact of the grooved-tip designs on the lift and drag performance, as shown in Fig. 4.

3.4 Vortex strength mitigation

This section quantifies the attenuation of the PTV strength due to the grooved-tip design. In the preceding section, Fig. 9 suggests that the grooved-tip design leads to both an attenuation in the vorticity and an enlargement of vortex size. To qualitatively assess the vortex strength, the non-dimensional vortex swirling strength ($\lambda_c c/U_\infty$) is employed. This parameter, defined as the imaginary part of the complex eigenvalues of the velocity gradient tensor (Chong et al. 1990; Zhou et al. 1999), is effective in identifying the vortex core regions and representing vortex intensity (Jeong and Hussain 1995; Chakraborty et al. 2005).

Figure 11 displays contours of the non-dimensional vortex swirling strength at successive cross-sections downstream of the trailing edge for the baseline case (Fig. 11a–f) and the 25G-C-CN design (Fig. 11g–l). At the $x/c = 1$ plane, both tip designs show the PTV with a small “tail,” indicative

of the tip vortex’s early-stage rolling-up process. The size of this “tail” is more prominent in the 25G-C-CN design, potentially due to the shearing effect from the groove channel flows. As the vortex convects downstream, it becomes more circular and axisymmetric. A well-defined vortex core is visible in each cross section for both cases, confirming that the tip vortex remains coherent in the near-wake region from $x/c = 1$ to at least $x/c = 1.5$ (which is the most downstream plane measured). As anticipated, the strongest swirling appears at the vortex core centre, with weaker rotation observed at increasing radial distance from the centre. The relatively high peak magnitudes in each slice suggests the absence of a drastic vortex breakdown as the vortex convects downstream. A gradual shift of the vortex centre towards the wing root direction is observed.

It is also noticed that Fig. 11g–l clearly demonstrates the PTV core of 25G-C-CN grooved-tip design exhibiting larger dimensions and lower maximum swirling strength at the vortex centre compared to the baseline case (Fig. 11a–f). This visual evidence strongly suggests that the grooved-tip treatment leads to a weaker PTV, which has significantly enlarged dimensions. The increased vortex dimensions suggest that the energy contained in the vortex is dissipated over a larger area, given Fig. 10 has shown that the circulation for different tip designs remain close, which will contribute to the reduced pressure drops.

The inception of cavitation is directly linked to local pressure drops, for example, within the core of a swirling vortex. Therefore, understanding the pressure change at the vortex core along the PTV trajectory is critical for assessing the mitigation effect of the grooved-tip design on cavitation risks. Due to the limitations of the stereoscopic PIV setup, which lacks the third dimension of velocity vectors and their derivatives, direct pressure reconstruction for highly three-dimensional flow features like tip vortices is not feasible. Consequently, a reduced-order model was adopted to estimate the pressure change within the PTV core relative to the free stream, as detailed below.

3.4.1 Reduced-order model for pressure estimation

To assess the local pressure change within the tip vortex core, an analytical model based on a cylindrical polar coordinate system $O(r, \theta, X)$ with its origin located at the vortex centre and velocity components (v_r, v_θ, v_X) was adopted. Here, X represents the tip vortex axial direction, which is almost parallel with the streamwise direction. Radial velocity v_r and swirl velocity v_θ approach zero in the free stream far from the tip vortex, while axial velocity v_X approaches U_∞ at infinity. The cylindrical polar coordinate system is illustrated in Fig. 12. For a steady, incompressible, axisymmetric vortex, the equation of motion over a cross section

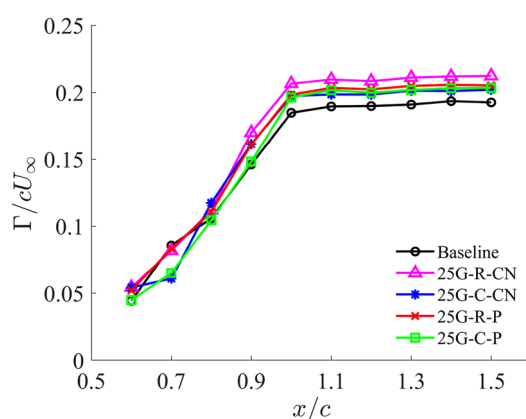
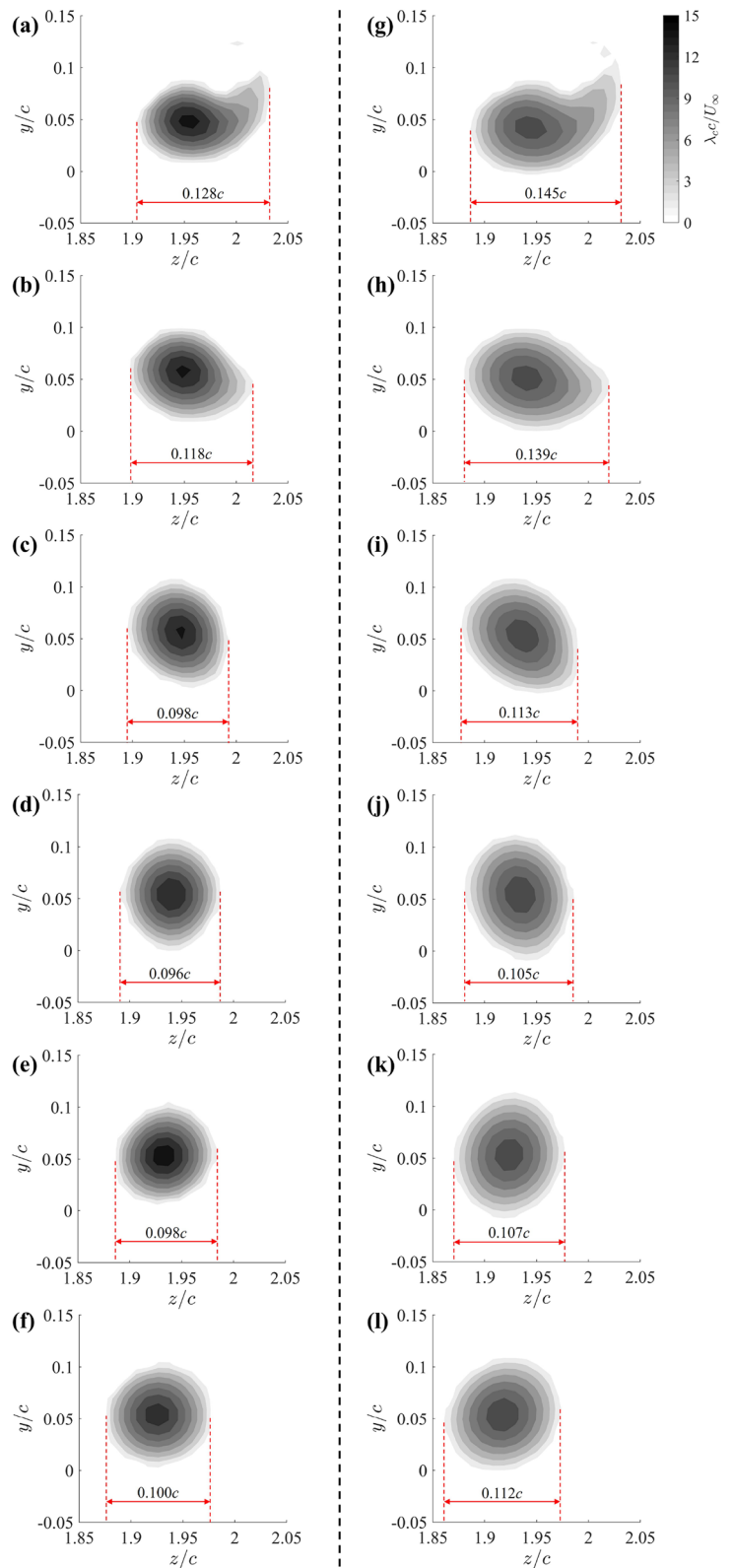


Fig. 10 Comparison of the non-dimensional vortex circulation Γ/cU_∞ on each cross section along the vortex core trajectory between baseline and grooved tips

Fig. 11 Contours of non-dimensional swirling strength $\lambda_c c/U_\infty$ on each cross section along the vortex core trajectory downstream of the trailing edge for baseline tip (a–f) and 25G-C-CN grooved tip (g–l). **a, g** $x/c = 1$; **b, h** $x/c = 1.1$; **c, i** $x/c = 1.2$; **d, j** $x/c = 1.3$; **e, k** $x/c = 1.4$; **f, l** $x/c = 1.5$



$$p_\infty - p_0(X) = \rho \int_0^\infty \frac{v_\theta^2}{r} dr, \tag{1}$$

of the PTV at $X = \text{constant}$ yields the pressure defect on the axis as (Morton 1969)

where p is pressure, r is radial distance from the axis, and ρ is the fluid density. Assuming the radial velocity of the

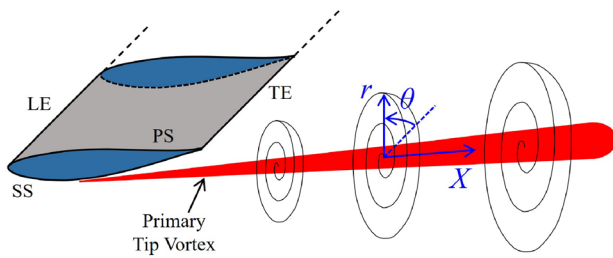


Fig. 12 Cylindrical polar coordinate system used for simplified pressure analysis model; not to scale. LE: leading edge; TE: trailing edge

vortex core is zero, the main driver of the radial pressure gradient simplifies to v_θ^2/r , establishing a direct link between the swirl intensity and radial pressure gradient.

For simplicity, the vortex is considered two-dimensional on a 2D measurement plane, i.e. neglecting the axial velocity, and further making the assumption that the vortex behaves like a Rankine vortex. We selected the simplified Rankine vortex model because it provides a direct link between swirling strength and maximum pressure drop (which is demonstrated below), enabling a straightforward interpretation of the PIV results. It is understood that an idealised Rankine vortex model differs from the experimental data, such as the tangential velocity profile. However, it can be used in the case of tip vortices (Franc and Michel 2005). For example, Boulon et al. (1999) and Dreyer (2015) applied the Rankine vortex model for qualitative analysis of tip vortices. While the velocity profile of the Rankine vortex model differs from experimental data and other more advanced models (e.g. Lamb-Oseen (Lamb 1993), Vatisstas (Vatisstas et al. 1991) and VM2 (Fabre and Jacquin 2004)), the differences are more prominent towards the edge of the vortex core, i.e. $r/r_c \rightarrow 1$ (see Figure 2.1 in Dreyer (2015) for comparison). In the present study, we are interested in the region where the pressure drop is maximum, namely near the tip vortex centre where $r/r_c \rightarrow 0$. In this region, the differences in the velocity profiles for various vortex models are insignificant. Rankine vortex model offers a practical and widely accepted approximation. Thus, we deem the use of this simplified vortex model is appropriate and sufficient, given that the vortex swirling strength is computed from the velocity vectors.

In a 2D Rankine vortex model, the velocity profile in cylindrical polar coordinates $O(r, \theta, X)$ is given by

$$v_\theta(r) = \begin{cases} \Omega r, & \text{for } r \leq r_c \quad (\text{solid-body rotation}) \\ \frac{\Gamma}{2\pi r}, & \text{for } r > r_c \quad (\text{potential vortex}) \end{cases} \quad (2)$$

where Γ is the circulation of the vortex, r_c is the vortex viscous core radius, and $\Omega = \Gamma/2\pi r_c^2$ is the constant angular velocity of the rigid core. Substituting Equation (2) into Equation (1) and integrating from the vortex centre ($r = 0$, where the pressure drop is maximum), to infinity, yields

$$p_\infty = p_{r_c} + \rho \frac{\Gamma^2}{8\pi r_c^2}, \quad \text{and} \quad (3)$$

$$p_0 = p_{r_c} - \frac{1}{2} \rho \Omega^2 r_c^2.$$

Hence, the pressure drop at the vortex core centre is

$$\Delta p_{\max} = p_\infty - p_0$$

$$= \rho \frac{\Gamma^2}{8\pi^2 r_c^2} + \frac{1}{2} \rho \Omega^2 r_c^2. \quad (4)$$

Given the circulation of a Rankine vortex is $\Gamma = 2\pi \Omega r_c^2$, Eq. 4 simplifies to

$$\Delta p_{\max} = \rho \Omega^2 r_c^2. \quad (5)$$

For a purely rotating flow, the vortex swirling strength λ_c can be shown equal to the angular velocity Ω of the rotating core. Therefore, based on this idealised model, the pressure drop is directly proportional to the square of the swirling strength

$$\Delta p \propto \lambda_c^2. \quad (6)$$

3.4.2 Pressure mitigation by the grooved tip

The reduced-order model demonstrates that the pressure drop within the vortex core is directly proportional to the square of the vortex swirling strength. Consequently, the vortex swirling strength contours presented in Fig. 11 suggest a significant mitigation in the pressure drop inside the tip vortex core, as the maximum vortex swirling strength is notably reduced by the grooved-tip design. While this simplified vortex model neglects the axial velocity, Sect. 3.2 also shows a significant deficit in the axial velocity caused by the grooved tip, suggesting an even greater mitigation in the pressure drop within the PTV that indicated by the swirling strength alone.

Figure 13 presents the variation of the maximum swirling strength λ_c (normalised by the chord length c and free stream velocity U_∞) as a function of streamwise distance x/c for the baseline and the four grooved-tip designs. Near the mid-chord, maximum λ_c values are relatively low and comparable for the baseline and grooved tips. As the PTV develops towards the trailing edge, the maximum swirling strength increases, indicating that the vortical structures intensify along the tip chord. This growth is driven by the

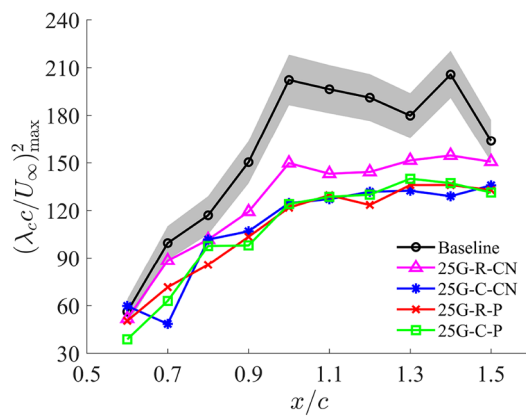


Fig. 13 Comparison of maximum vortex swirling strength $\lambda_c c / U_\infty$ on cross sections along the vortex core trajectory before the trailing edge between baseline and grooved tips. The shaded region represents the measurement uncertainty propagated from the velocity measurements for the baseline case calculated using the same approach outlined in “Appendix 1”

continuous entrainment of tip flows due to the pressure difference between the pressure and suction sides of the wing. Beyond the trailing edge, the baseline case shows to decay gradually, signifying vortex diffusion and dissipation as the vortex convects downstream. In contrast, the grooved-tip cases level off, suggesting that the diffusion and dissipation are expedited by the groove structures even before the trailing edge.

Consistently, the baseline case exhibits the highest swirling strength at each cross section, confirming that the unmodified tip generates the strongest vortex. The 25G-R-CN case achieved moderate mitigation of vortex strength, remaining below the baseline curve but relatively high compared to other grooved-tip designs; a reduction of 27% was observed at $x/c = 1.1$. Importantly, the remaining three designs, including 25G-C-CN, 25G-R-P and 25G-C-P, demonstrate similar and more substantial reductions in vortex strength. The most significant pressure-drop mitigation, nearly 40%, is always achieved at $x/c = 1.0$ by these three grooved-tip designs. This data strongly supports the effectiveness of the proposed grooved-tip designs in mitigating tip vortex-induced pressure drops and, consequently, cavitation risk.

Although direct pressure reconstruction was not possible with the present stereoscopic PIV technique, our data showed consistency between different flow quantities, providing indirect support for the pressure analysis. In particular, both the axial velocity deficit in Sect. 3.2 and the reduced vortex swirling strength in Sect. 3.4 indicate a mitigation of pressure drop within the PTV core. This agreement between independent velocity-based analyses provides confidence in the reduced-order model.

4 Conclusions

The present work investigates the effects and underlying physics of introducing tip permeability, realised by a novel multiple-groove design at the tip, on the characteristics of tip vortices. The experiments were performed on a NACA 63-415 wing in a water tunnel at a chord-based Reynolds number of 3.22×10^4 , with a semi-aspect ratio of 2. Lift and drag were measured across a wide range of angles of attack, and flow fields near the wing tip and along the PTV trajectory were measured using PIV at $\alpha = 6^\circ$.

The proposed groove-tip designs demonstrate negligible differences in lift and drag coefficients within the typical range of angles of attack, compared to the baseline wing with an impermeable tip. Notably, these designs also exhibit a moderate delay in stall for the aspect ratio tested ($sR = 2$), which will be further investigated in future work.

Streamwise velocity measurements near the wing tip ($z/c = 1.95$) consistently showed a significant velocity deficit within the PTV region for all grooved-tip designs tested, compared to the baseline case. This velocity reduction, predominantly attributed to the changes in the streamwise component, suggests a modification of the tip vortex structure, which was caused by the impingement and mixing of the groove-jets with the PTV.

Analysis of the cross-flow PIV measurements at $x/c = 0.7$ revealed that the baseline case exhibited entanglement between the TSV and the concentrated PTV, which contributed to intensified tip vortex structures. In contrast, the grooved-tip design almost eliminated the TSV and resulted in a more diffused PTV, evidenced by the weakened vorticity and enlarged vortex dimensions. Vortex parameter analysis indicated only a minimal change in the vortex circulation for cross-sections of the PTV before the trailing edge, irrespective of the tip design. However, the grooved-tip structures increased the size of the PTV.

A reduced-order model was employed to qualitatively compare vortex strengths, addressing the limitations of experimental measurements. This model established that for a 2D Rankine vortex, the pressure drop at the vortex core is proportional to the square of the vortex swirling strength. Consequently, all four grooved-tip designs showed reduced maximum swirling strength along the PTV trajectory compared to the baseline case. This indicates that the proposed designs are effective in modifying the flow field within the tip vortex, resulting in an estimated pressure-drop reduction of approximately 40% compared to the baseline case at the tested Reynolds number, based on a reduced-order model linking pressure to vortex swirling strength. Such a reduction can mitigate cavitation

risks induced by tip vortices in wing- and blade-based systems, though confirmation across higher Reynolds numbers and more representative operating conditions will be needed.

The demonstrated passive flow control technology shows potential for modifying tip vortex characteristics through grooved-tip treatment. While our findings are limited to model-scale experiments, such designs could inspire future applications in hydrofoils on marine vessels and underwater vehicles, as well as turbines, propellers and pumps. Furthermore, the development of advanced permeable structures may also contribute to noise suppression in wind turbines and aerial vehicles, as similar structures at the trailing edge featuring local permeability have demonstrated their ability to suppress trailing-edge noise (Deng et al. 2022, 2023; Fiscaletti et al. 2024).

Constrained by the current experimental setup and capabilities, the two-dimensional data are insufficient to fully understand these highly three-dimensional flow features as well as internal flow fields inside the grooves. Further investigation could benefit from more advanced volumetric measurement techniques, such as the time-resolved three-dimensional Lagrangian Particle Tracking Velocimetry or tomographic PIV, coupled with pressure reconstruction algorithms. Due to the inherent constraints of the present water tunnel facility, the experiments were limited to a relatively low Reynolds number. To extend the analysis, future numerical simulations, validated against the present experiment, will be useful to understand the physics and effects of vortex control with grooved-tip designs across a broad range of Reynolds numbers.

Appendix 1: Force measurement uncertainties

The force coefficient C_F , which can either be the lift or drag coefficient, is written as

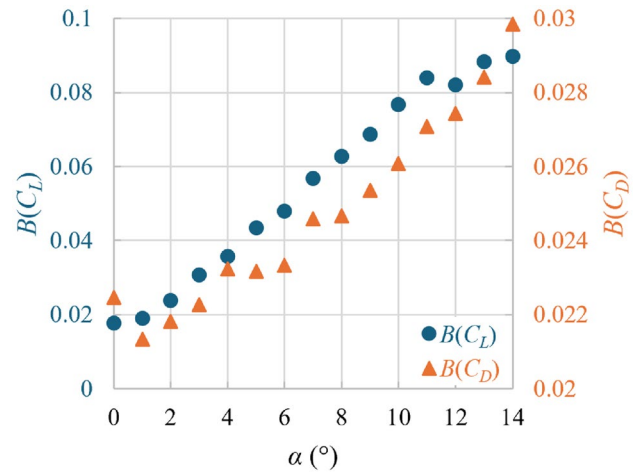


Fig. 14 Bias limits of force coefficients as a function of angle of attack α

$$C_F = \frac{F}{0.5\rho U_\infty^2 S} \tag{7}$$

Therefore, the total bias of C_F is written as

$$B_T(C_F) = \left[\left(\frac{\partial C_F}{\partial F} B(F) \right)^2 + \left(\frac{\partial C_F}{\partial \rho} B(\rho) \right)^2 + \left(\frac{\partial C_F}{\partial U_\infty} B(U_\infty) \right)^2 + \left(\frac{\partial C_F}{\partial S} B(S) \right)^2 \right]^{1/2} \tag{8}$$

where $B(F)$, $B(\rho)$, $B(U_\infty)$ and $B(S)$ are the bias limits of the force, fluid density, free stream velocity and area, respectively. Each term is estimated as below.

$B(F)$: The half-wing model was rigidly attached to the load cell in the experiments, so that the inclination of the load cell is dependent on the wing’s angle of attack α . Axis rotation of the measured force (F_x, F_y) in the load cell’s reference frame to the global reference frame yields

Table 2 Summary of bias limits for force measurement

Variable	Magnitude	Unit
$B(L)$	0.0159	[N]
$B(D)$	0.0204	[N]
$B(\rho)$	0.2960	[kg m ⁻³]
$B(U_\infty)$	0.021	[m s ⁻¹]
$B(S)$	2.5×10^{-7}	[m ²]
$B_{T_{\max}}(C_L)$	0.090	–
$B_{T_{\max}}(C_D)$	0.030	–

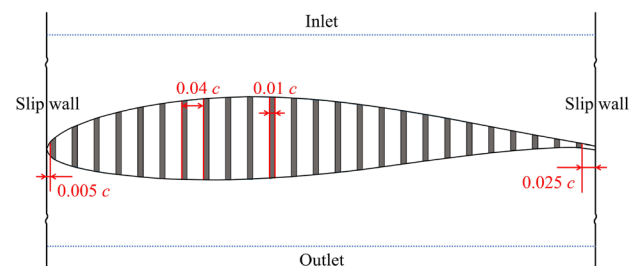


Fig. 15 Schematic of 2D channel simulation with a multi-groove configuration

$$\begin{aligned} L &= F_x \sin(\alpha) + F_y \cos(\alpha), \quad \text{and} \\ D &= F_x \cos(\alpha) - F_y \sin(\alpha). \end{aligned} \quad (9)$$

As per calibration report of the ATI Industrial Automation Nano17 IP68 load cell, the bias limits in F_x and F_y force components are 0.07% and 0.06% of the sensing range (25 N). Therefore, the bias limits of the measured forces are $B(F_x) = 0.0183$ N and $B(F_y) = 0.0158$ N, respectively. The load cell and the half-wing model were rigidly connected to a beam above the free water surface. The beam was parallel with the load cell's horizontal axis as well as the wing's chord. Therefore, the angle of attack of the wing α is equivalent to the rotation angle η of the beam. The rotation angle η was measured by a digital inclinometer with an accuracy of $\pm 0.025^\circ$, so that $B(\alpha) = B(\eta) = 0.025^\circ$. The lift and drag forces each has three uncorrelated uncertainties, namely $B(F_x)$, $B(F_y)$ and $B(\alpha)$. The bias limits $B(L)$ and $B(D)$ for the lift and drag can be written as

$$\begin{aligned} B(L) &= \left[(\sin(\alpha) B(F_x))^2 + (\cos(\alpha) B(F_y))^2 \right. \\ &\quad \left. + ((F_x \cos(\alpha) - F_y \sin(\alpha)) B(\alpha))^2 \right]^{1/2}, \quad \text{and} \\ B(D) &= \left[(\cos(\alpha) B(F_x))^2 + (\sin(\alpha) B(F_y))^2 \right. \\ &\quad \left. + ((F_x \sin(\alpha) + F_y \cos(\alpha)) B(\alpha))^2 \right]^{1/2}, \end{aligned} \quad (10)$$

respectively.

$B(\rho)$: Before and after each set of force measurement, the variation of the temperature T measured by a handheld digital thermometer (RS PRO 206-3738) with an accuracy of 1°C was between 0.1 and 0.5°C . Consequently, the minimum and maximum bias limits of the temperature measurement are $B_{\min}(T) = 1.1^\circ\text{C}$ and $B_{\max}(T) = 1.5^\circ\text{C}$. The bias limit of water density as a function of temperature is (ITTC 2011)

$$B(\rho) = \left| \frac{\partial \rho}{\partial T} \right| B(T), \quad (11)$$

where the sensitivity coefficient is

$$\left| \frac{\partial \rho}{\partial T} \right| = \left| 0.0552 - 0.0154T + 0.000120T^2 \right|. \quad (12)$$

Equations 11 and 12 yield the following bias limits: $B_{\min}(\rho) = 0.2171$ kg m⁻³ and $B_{\max}(\rho) = 0.2960$ kg m⁻³, respectively. The density change due to the seeding particles for flow visualisation is neglected.

$B(U_\infty)$: The free stream velocity of the flow was not measured during the measurements to avoid flow disruptions from the measurement device. However, the water tunnel was calibrated before the experiments by a Vectrino Acoustic Doppler Velocimetry (ADV). The turbulence intensity

in the x , y and z directions are $0.0480U_\infty$, $0.0426U_\infty$ and $0.0267U_\infty$, respectively. Therefore, the overall bias limit of the free stream velocity is estimated as $0.0695U_\infty$, giving $B(U_\infty) = 0.021$ m s⁻¹. This is consistent with previously reported experiments conducted in the same water tunnel (Arredondo-Galeana et al. 2023).

$B(S)$: The dimensions of the wing was measured after sanding, so that the accuracy of the 3D printer is not used here. The ruler has resolution of 1 mm and an accuracy of ± 0.5 mm. Therefore, the bias limit for the area, $S = bc$, considering the bidimensionality of the area, is $B(S) = 2.5 \times 10^{-7}$ m for all wings.

The bias limits for all inputs are summarised in Table 2, together with the maximum total bias limit for the lift $B_{T_{\max}}(C_L)$ and drag $B_{T_{\max}}(C_D)$ coefficients at $Re_c = 3.22 \times 10^{-4}$ computed using Eq. 8. The bias limits $B(C_L)$ and $B(C_D)$, as functions of α are shown in Fig. 14.

Appendix 2: Configuration of grooves through 2D permeability estimation

To inform the practical design of multi-groove configurations, a two-dimensional (2D) channel simulation is carried out. As shown in Fig. 15, the setup features a foil embedded within the channel that replicates the tip profile of a turbine blade. Two simulation scenarios are considered: (i) the foil is modelled as a porous medium with uniform permeability, characterised by the non-dimensional Darcy number (Da); and (ii) the foil incorporates multiple grooves that permit fluid passage, while the remaining sections remain impermeable. The groove configuration is considered to exhibit equivalent 2D (chord-normal) permeability to the porous tip when both cases yield the same pressure drop between the inlet and outlet of the channel. Based on prior work identifying the optimal permeability range as $Da = 10^{-6}$ to 10^{-5} (see Liu et al. 2025a), the corresponding groove geometries are selected to fall within this effective permeability range.

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Author Contributions JT—co-conceived the project, designed, prepared and performed the experiments, processed and analysed the experimental data, and wrote the first draft of this manuscript. SO—advised on the experiments and data analysis, and edited the manuscript. IMV—co-supervised the project, advised on the experiments and 2D simulations, and edited the manuscript. YL—conceived

and supervised the project, performed the simulations, advised on the experiments and data analysis, secured funding, and edited the manuscript.

Data Availability The data that support the findings of the study are available from the corresponding author upon reasonable request.

Declarations

Conflict of interest The authors have no conflict of interest to report.

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