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Hydrodynamic Propulsive Performance of a Hybrid Aquatic-Aerial Rotor-Wing System under Collective Pitch and Rotational Speed Matching

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Abstract: Achieving efficient underwater propulsion with aerial rotors is a challenge for hybrid aquatic-aerial vehicles (HAAVs), further complicated by complex hydrodynamic interactions with the downstream wing. This study employs numerical simulations based on the improved delayed detached-eddy simulation (IDDES) to investigate the hydrodynamic performance of a rotor-wing system under matched collective pitch and rotational speed conditions during underwater cruising. Our findings reveal that coordinated adjustment of these parameters can improve rotor propulsive efficiency by up to 16.2% under prescribed cruising speed and thrust requirements. Among the thrust-loading conditions examined, the collective pitch corresponding to the highest rotor efficiency decreases as the thrust requirement increases. The intricate rotor-wing interactions, driven by wing thickness and loading effects, as well as rotor loading and wake effects, exhibit dynamic effects on the propulsion system and vary with the combinations of collective pitch and rotational speed. Increasing collective pitch while reducing rotational speed weakens the wing thickness, wing loading, and rotor loading effects but strengthens the rotor wake effect. Although the interaction has limited influence on rotor efficiency, it improves the net propulsive efficiency of the system by up to 5.2% by reducing the wing drag. This benefit increases with collective pitch but still does not offset the loss in rotor efficiency at excessively high pitch. Hence, both effects should be considered when matching collective pitch and rotational speed. These findings clarify the role of rotor-wing interaction in evaluating the benefits of collective pitch and rotational speed matching for HAAV underwater propulsion.

Key words: Propulsion efficiency; Hydrodynamic interaction; Hybrid aquatic-aerial vehicles; CFD.

0. Introduction

Hybrid aquatic-aerial vehicles (HAAVs) combine aerial flight with underwater operation and offer a versatile platform for both military and civil applications. Among existing configurations, vehicles with amphibious rotors and fixed wings can support extended endurance of aerial and underwater cruising within a common platform^[1-3]. However, achieving efficient propulsion in both media requires accounting for rotor-wing interaction and adapting rotor operation to the different propulsion requirements in air and water.

For vehicles with a rotor mounted upstream of a wing, rotor-wing interaction can affect the performance

of the propulsion system. Aerodynamic studies have provided insights into this interaction. Using computational fluid dynamics (CFD), Lim^[4] investigated the unsteady characteristics of rotor-wing interactions. Aref et al.^[5] showed that the rotor slipstream can delay local flow separation over the downstream wing. Experiments by Ananda et al.^[6] demonstrated that the increased wing lift and reduced drag are induced by the rotor effects at low Reynolds numbers. Within HAAV research, recent studies have demonstrated platforms combining rotors and wings, and examined their performance across different operating conditions^[1-3]. However, the contribution of underwater rotor-wing interaction to mean loads, load

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fluctuations, and propulsive efficiency remains insufficiently quantified. This limits the assessment of how the integration of rotor and wing affects propulsion performance during underwater cruising.

The existing HAAVs that use amphibious rotors generally adopt aerial rotor directly for underwater propulsion^[1-3,7-12]. A practical advantage of this approach is that it preserves aerial propulsion capability while avoiding the additional design and integration effort required to develop a dedicated hybrid aquatic-aerial propulsor. However, changes in rotor loading and motor-rotor matching can reduce underwater propulsion efficiency without suitable adaptation^[13]. Several approaches have been investigated to address this challenge. Optimization of blade chord and pitch distributions seeks a compromise between aerial and aquatic performance^[14,15], while adjustments to motor-propeller matching and transmission ratios improve the suitability of aerial propulsion systems for underwater operation^[13,16]. Variable pitch control, traditionally used to adjust rotor loading, has also been investigated as a means of adapting a common rotor for operation in both air and water^[17]. For rotors with collective pitch control, collective pitch and rotational speed can be matched to adjust the rotor load and improve the propulsive efficiency while retaining the blade geometry. Different combinations not only change the rotor efficiency but also produce different wake characteristics. Furthermore, these wake changes can affect the downstream wing, motivating further investigation of their influence on the performance of the coupled rotor-wing system.

To examine how collective pitch and rotational speed matching affects the underwater propulsive performance of a rotor-wing system, this study numerically investigates a configuration comprising an XV-15 rotor and a rectangular wing with a NACA4412 cross-section. At a fixed cruising speed and prescribed thrust output, different combinations of collective pitch and rotational speed are evaluated using the improved delayed detached-eddy simulation (IDDES). The numerical results reveal the underlying mechanisms of rotor-wing interactions and explain how the matching of collective pitch and rotational speed modifies rotor-wing interaction. This analysis provides a basis for assessing the hydrodynamic characteristics of HAAV rotor-wing system.

The structure of this paper is as follows: *Section 1* provides a description of the statement of the problem. *Section 2* presents the numerical method and numerical model employed in detail. *Section 3* presents the numerical results and analyzes the performance of the

isolated rotor and the rotor-wing system under different combinations of collective pitch and rotational speed. *Section 4* concludes the study.

1. Statement of the Problem

The problem considered is the selection of collective pitch and rotational speed to improve the rotor-wing system propulsive efficiency of an HAAV operating at a prescribed underwater cruising speed. The aim is to identify the most efficient combination among the cases examined and to determine how parameter matching affects rotor-wing interaction.

With a focus on the propulsion module of the HAAV, a semi-span rotor-wing system is considered without the fuselage. Although this approach excludes the changes in local flow caused by the fuselage, it allows the mechanisms and characteristics of rotor-wing interaction to be examined in isolation within the simplified configuration^[18]. This approach also reduces the computational cost of the present study. Since the fuselage is not included in the computational model, the HAAV self-propulsion problem, which requires a balance between the rotor thrust and vehicle resistance, is simplified here to meeting a given rotor thrust at a prescribed velocity. On this basis, rotor-wing performance under different combinations of collective pitch and rotational speed can be economically compared without introducing a complete vehicle resistance model or solving the self-propulsion problem of a specific HAAV. Moreover, prescribing different target thrusts can extend the analysis easily to a range of propulsion requirements associated with HAAVs showing different drag characteristics.

The rotor-wing system considered is shown in Fig. 1(a). The rotation axis of the rotor is aligned with the incoming flow, and the rotor is located upstream of the wing. The rotor geometry is based on the three-bladed XV-15 documented by Felker et al.^[19], with a diameter $D=7.62\text{m}$, a twist of -40.25° , a pre-cone angle of 2.5° and an outward rotor rotation direction. The collective pitch P_c is defined as the angle between the rotor disk and the chord line of the blade section at $0.75R$, where $R=D/2$ is the rotor radius. The investigated collective pitch ranges from 26° to 58.5° . The rectangular wing has a NACA4412 section, a modeled span $B=5\text{m}$, a chord $c=1.6\text{m}$, and an installation angle of 2° . It is positioned $0.5c$ downstream of the rotor disk. The rotor-wing, isolated rotor, and isolated wing configurations in Fig. 1 are used to assess rotor-wing interaction by comparison with the isolated configurations. Additionally, the blade flapping and lead-lag motion are neglected in the present study.

Two coordinate systems are used in this study. The Cartesian coordinate system (x, y, z) , shown in Fig. 1, has its origin at the rotor disk center. The x -axis points downstream along the rotor axis, the z -axis points upward, and the y -axis completes a right-handed system. The cylindrical coordinate system (x, r, φ) shares the same origin and axial direction. Here, r is the distance from the rotor axis, and φ is measured clockwise from the positive z -axis when viewed from upstream looking downstream. The rotor phase θ is defined by the azimuthal position of a reference blade, using the same angular convention as φ . The reference blade is aligned with the positive z -axis at $\theta = 0^\circ$.

The hydrodynamic performance of the rotor is characterized by the advance coefficient J , thrust coefficient K_T , torque coefficient K_Q , and propulsion efficiency η . The wing performance is characterized by the drag coefficient K_F and lift coefficient K_L . The parameters are defined as follows:

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

$$K_T = \frac{16T}{\rho n^2 D^4} \quad (2)$$

$$K_Q = \frac{32Q}{\rho n^2 D^5} \quad (3)$$

$$\eta = \frac{TU}{2\pi nQ} \quad (4)$$

$$K_F = \frac{F}{\rho U^2 c^2} \quad (5)$$

$$K_L = \frac{L}{\rho U^2 c^2} \quad (6)$$

where T and Q are the thrust and torque of the rotor, F and L are the drag and lift of the wing, and ρ is the fluid density, $\rho=997.561\text{kg/m}^3$. The thrust is directed along the rotor axis in the propulsion direction, while the torque acts opposite to the rotor rotation. The wing drag acts opposite to the propulsion direction, and the lift is perpendicular to the incoming flow. In the following analyses of Section 3, the subscripts R&W, R, and W are used to distinguish forces and torques obtained from the coupled rotor-wing, isolated rotor, and isolated wing configurations, respectively.

With respect to the strategy of P_c - n matching, n is adjusted in the isolated rotor configuration to meet the target thrust for each P_c at a given inflow speed.

Accordingly, the matching condition for different P_c - n combinations can be expressed in a normalized form as

$$\frac{K_T}{J^2} = \frac{16T}{\rho U^2 D^2} \quad (7)$$

For fixed D and ρ , K_T/J^2 defines the target thrust at different inflow speeds. For the self-propulsion problem, this thrust approximately equals the vehicle resistance. Thus, K_T/J^2 can also be interpreted as a nondimensional form of the resistance for a self-propelled vehicle at a cruising speed U . On this basis, three operating conditions with $K_T/J^2=0.2, 0.3$, and 0.4 at the same inflow speed of $U=0.679\text{m/s}$ are considered in the present study. These three conditions represent different resistance requirements associated with HAAVs showing different resistance characteristics, which may result from different fuselage shapes, vehicle attitudes, towing conditions, etc. The resulting matched combinations for these three thrust conditions are reported in Section 3.2. The same P_c - n combinations are applied to the rotor-wing configuration without further adjustment.

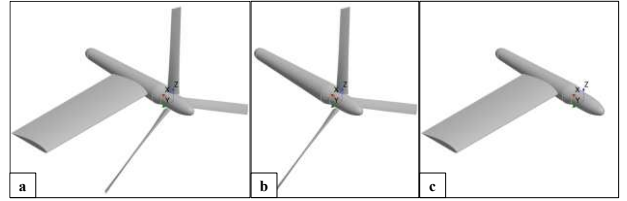


Fig. 1 Schematic of the simulation geometry. (a) Rotor-wing system; (b) Isolated rotor; (c) Isolated wing

2. Methodology

2.1 Numerical Method

In this study, a finite volume solver based on CFD is employed to solve the unsteady, incompressible Navier-Stokes (N-S) equations. To ensure high resolution of the flow field around the rotor-wing system, this study employs the SST k - ω IDDES method for turbulence simulation. The SST k - ω IDDES model adopted in this paper follows the formulation developed by Gritskevich et al.^[20] within the general IDDES framework proposed by Shur et al.^[21]. It utilizes the SST k - ω model to simulate near-wall flow fields in RANS regions and incorporates concepts from Wall-Modeled Large Eddy Simulation (WMLES) through a modified grid scale definition function, which addresses the logarithmic layer velocity mismatch problem. A brief description of the IDDES is provided below. The transport equations for k and ω are expressed as:

$$\frac{\partial \rho k}{\partial t} + \nabla \cdot (\rho \mathbf{V} k) = \nabla \cdot [(\mu + \mu_T \sigma_k) \nabla k] + P_k - \rho \beta^* f_\beta (k \omega \frac{\sqrt{k}}{l_{\text{Hybrid}} f_\beta \beta^*} - k_0 \omega_0) \quad (8)$$

$$\frac{\partial \rho \omega}{\partial t} + \nabla \cdot (\rho \mathbf{V} \omega) = \nabla \cdot [(\mu + \mu_T \sigma_\omega) \nabla \omega] + P_\omega - \rho \beta f_\beta (\omega^2 - \omega_0^2) \quad (9)$$

where ρ is the fluid density, t is time, $\mathbf{V}$ is the velocity vector, μ is the dynamic viscosity, μ_T is the turbulent dynamic viscosity, P_k and P_ω are production terms, f_β^* is free shear correction factor, f_β is vortex stretching modification factor, σ_k , σ_ω , β and β^* are other factors in this model, l_{Hybrid} is the turbulent length scale in the IDDES methodology informed by Wall-Modeled Large Eddy Simulation (WMLES) principles and is implemented as:

$$l_{\text{Hybrid}} = \tilde{f}_d (1 + f_e) l_{\text{RANS}} + (1 - \tilde{f}_d) C_{\text{DES}} \Delta_{\text{IDDES}} \quad (10)$$

where the blending function $\tilde{f}_d$ governing the RANS-to-LES transition is formulated as a grid-dependent parameter determined by the local grid size and grid wall distance, the elevating function f_e prevents excessive Reynolds stresses reduction in near-wall regions, l_{RANS} is the RANS turbulent length scale. The expressions for the relevant functions are shown as follows:

$$\tilde{f}_d = \max((1 - f_{dt}), f_B) \quad (11)$$

$$f_e = \max((f_{e1} - 1), 0) f_{e2} \quad (12)$$

$$C_{\text{DES}} = 0.61(1 - F_1) + 0.78 F_1 \quad (13)$$

$$\Delta_{\text{IDDES}} = \min(\max(0.15d, 0.15\Delta, \Delta_{\min}), \Delta) \quad (14)$$

where f_{dt} is coupled with the local flow states, f_B , f_{e1} and f_{e2} are grid-dependent functions, blending function F_1 inherited from the SST k - ω model, Δ and $\Delta_{\min}$ are the maximum and minimum distances between the cell center under consideration and those of its adjacent cells, respectively. Detailed formulations of these functions are provided in previous studies and software documentation^[20-22] and thus are not repeated here.

2.2 Numerical Model

In this study, the commercial CFD software STAR CCM+ is used as the simulation solver. The hybrid-BCD scheme, which blends second-order upwind and bounded-central differencing, is applied for the convective term. The pressure-velocity coupling is

solved by SIMPLE algorithm. The computational domain and boundary conditions of the numerical model are shown in Fig. 2. The computational domain consists of a stationary rectangular region and a rotating cylindrical region. The sliding mesh method is employed to simulate the rotation of the rotor. The stationary domain is $9D$ in length, $4B$ in width and $4D$ in height. The rotating domain has a diameter of $1.1D$ and a length of $0.225D$. The rotor disk center is positioned $3D$ from the upstream boundary, $6D$ from the downstream boundary, and B from the fuselage symmetry plane. Pressure outlet boundary condition and symmetry plane boundary condition are applied at the downstream and fuselage symmetry boundaries of the stationary domain, while velocity inlet boundary conditions are employed at other boundaries of the stationary domain with the velocity direction along the x -direction. The surfaces of the rotor and wing are treated as no-slip wall conditions. Interfaces between the stationary and rotating domains are generated to facilitate the data exchange among them.

The computational domains are discretized using the trimmed mesher with hexahedral elements for simulations in the present work, as depicted in Fig. 3. The minimum grid sizes on the rotor and wing surface are $0.0002D$ and $0.0078D$, respectively. In the near wall area of the rotor and wing, prismatic grids, whose height in the first layer adjacent to the wall is restricted to 0.05mm based on the near-wall integration requirement of $y^+ < 1$ for the SST k - ω IDDES model, are adopted. With grid sizes of $0.0125D$ for the wing refinement region and wake refinement region 1, $0.025D$ for the wake refinement region 2, grids around the surfaces of the rotor and wing as well as in the wake regions are refined to capture the complicated vortex interactions. Moreover, adaptive mesh refinement (AMR) is implemented in the rotating domain, wake refinement region 1 and wing refinement region where

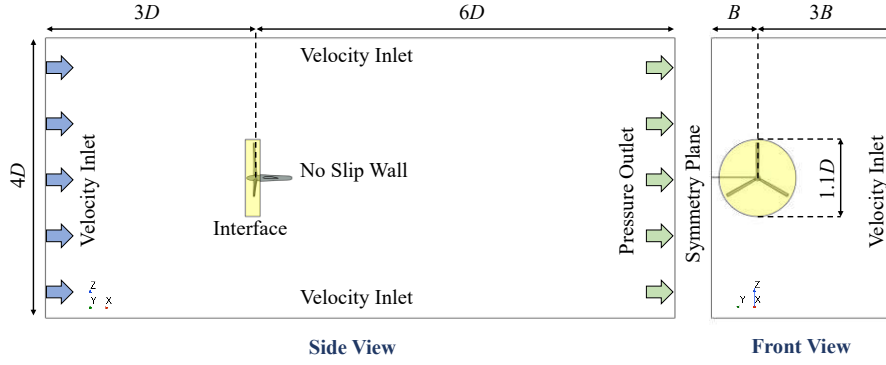


Fig. 2 Schematic of the computational domains and boundary conditions

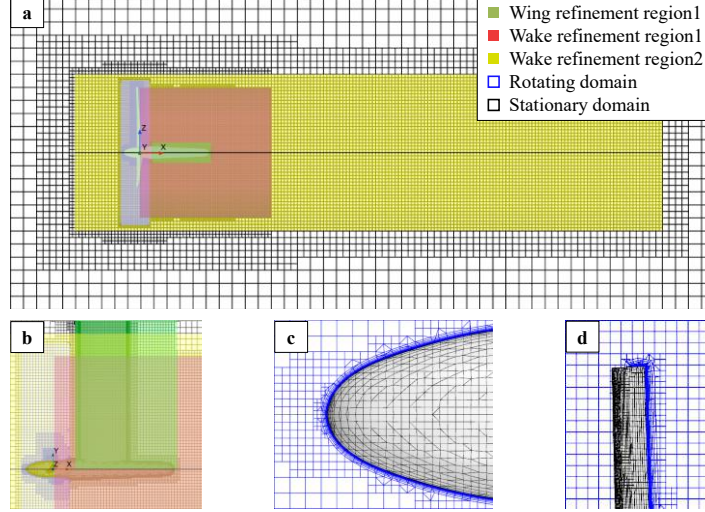


Fig. 3 Schematic of the grid refinement strategy for the computational domains. (a) xoz section of the computational domain; (b) near field of the rotor and wing; (c) nacelle area; (d) rotor tip area

Ω criterion^[23] exceeded 0.5 to enhance the resolution of the vortices of the rotor and wing. The refinement is performed every 2 time steps, with a maximum of 2 refinement levels and a minimum cell size of $0.0031D$. Overall, the numerical model consists of a total of 7.67 million grids in the computational domains.

The time advance scheme employs a second-order implicit method with a time step corresponding to 2° of rotor rotation. Each time step is subjected to 5 inner iterations to ensure the convergence of the numerical results.

3. Results and Discussion

3.1 Verification and Validation

Given the significant influence of the grid size and time step on the numerical results, in this section, the spatial and temporal discretization schemes are verified. The verification simulations are based on the rotor operating condition with $P_c=26^\circ$, $U=0.679$ m/s, and advance coefficient $J=1.07$.

3.1.1 Grid Convergence Study

The grid discretization scheme described in

Section 2.2 is designated as the medium-density baseline grid G2. Two additional grids, G1 and G3, are generated with a refinement ratio of $\sqrt{2}$ while preserving the same prismatic layer configuration. Table 1 presents the time-averaged results of the simulations employing G1, G2 and G3. The grid convergence ratio R_G is adopted to verify the effect of grid size on the numerical convergence with reference to the ITTC's recommended procedures and guidelines for CFD verification and validation methodology^[24], which is defined as:

$$R_G = \frac{s_{G3} - s_{G2}}{s_{G2} - s_{G1}} \quad (15)$$

where s is the numerical result and the subscripts indicate different grid schemes.

The results for the grid convergence ratio for K_T , K_Q and η are calculated as -0.49, -0.93 and -0.19, respectively. All values satisfy the oscillatory convergence criterion $-1 < R_G < 0$, demonstrating that the numerical solutions converge with oscillations as the grid size decreases.

Table 1 Numerical results of the G1, G2 and G3

id	Grid density	Grid number	K_T	K_Q	η
----	--------------	-------------	-------	-------	--------

G1	Coarse	4.507M	0.342	0.149	0.784
G2	Medium	7.676M	0.346	0.149	0.789
G3	Fine	13.735M	0.344	0.149	0.788

Sufficient grid density is critical for the accurate resolution of wake vortices. Vortex structures calculated from different grid schemes are visualized using the $\Omega=0.52$ isosurface following Liu et al.^[23], as shown in Fig. 4. In comparison to the G2 and G3 with denser grid distributions, the G1 exhibits inferior resolution of vortex. This is manifested in two aspects: First, unexpected cloud-like structures are observed near the interface region due to the numerical errors caused by poor grid quality. Second, vortex structures dissipate prematurely due to the coarse grid in the wake region. Therefore, in order to ensure flow prediction accuracy while maintaining computational efficiency, the G2 is selected for subsequent simulations and analysis in this study.

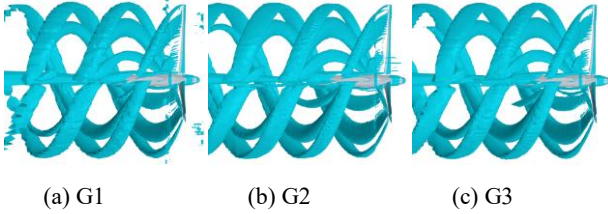


Fig. 4 Vortex structures calculated from schemes with different grid density

3.1.2 Time Step Convergence Study

In this section, three time step schemes (T1, T2 and T3) corresponding to 3° , 2° and 1° of the rotor's rotation at 5r/min are employed to verify the time step convergence. The time-averaged results for the K_T , K_Q and η are presented in Table 2.

Table 2 Numerical results of the T1, T2 and T3

id	Time step	K_T	K_Q	η
-	s	-	-	-
T1	0.1	0.345	0.149	0.787
T2	0.067	0.346	0.149	0.789
T3	0.033	0.347	0.150	0.790

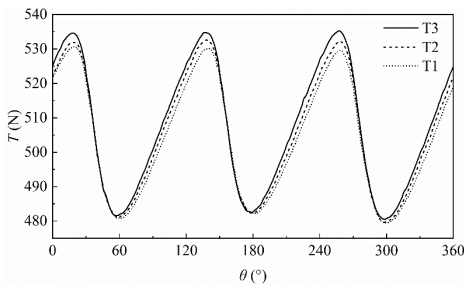


Fig. 5 Thrust variations with rotor azimuth angle θ in one revolution of the rotor for time step schemes T1, T2 and T3

With a similar definition to R_G , the time step convergence ratios R_T evaluated using K_T , K_Q and η are

calculated as 0.46, 0.90 and 0.15, respectively. All values satisfy the monotonic convergence criterion $0 < R_T < 1$, demonstrating that the numerical solutions converge monotonically as the time step decreases. Additionally, Fig. 5 illustrates the thrust variations with rotor azimuth angle θ in one rotor revolution for three time step schemes, which shows that they all exhibit similar three-per-revolution variations. The T2 time step scheme is selected for subsequent analyses, considering the computational accuracy and efficiency.

3.1.3 Validation

Following the verification of the numerical model at full-scale, this section presents the validation of the numerical approach against experimental results. To this end, the verified numerical model is applied to the test model and the corresponding operating conditions. The propeller open-water test was conducted in the towing tank of Wuhan University of Technology. The test model is a scaled XV-15 rotor with a diameter of 0.18m. Fig. 6 shows the open-water tank and the test rotor. At a rotor rotational speed of 1030r/min and towing speed of 0.62-1.54m/s, the open-water performance was measured over a range of $J=0.2-0.5$.

As shown in Fig. 7, the CFD results exhibit consistency with the experimental data, capturing the monotonic decreasing trend of both K_T and K_Q as J increases. Quantitatively, however, an underestimation by the CFD method is observed across the entire tested range, and this discrepancy becomes slightly more pronounced at lower J . Specifically, the relative deviations for K_T and K_Q at minimum advance ratio J of 0.2 are -9.46% and -8.81%.

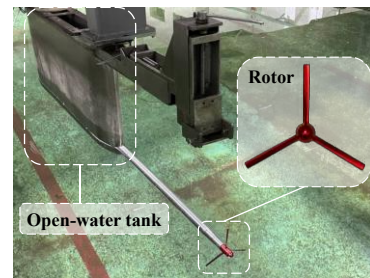


Fig. 6 Photograph of the open-water tank and test XV-15 rotor

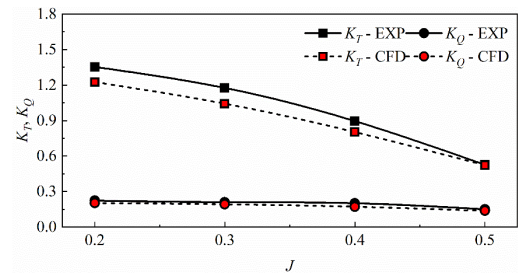


Fig. 7 XV-15 Open-water performance by experiment and CFD Heinke et al.^[25] demonstrated that, for model-

scale propellers with short chord lengths, the blade surface flow becomes more complex and can be strongly influenced by transition and laminar separation effects that are highly sensitive to the turbulence modelling approach. Hence, these discrepancies are likely attributed to the limitations of the turbulence model in capturing complex flow state across the surface of a model-scale XV-15 rotor with extremely short chord length. Given the acceptable level of deviation below 10% and the good agreement in overall trends, the numerical model is considered validated and reliable for predicting the open-water performance of the rotor in subsequent analyses.

3.2 Effects of P_c - n matching on isolated rotor

Fig. 8 illustrates the influence of the coordinated adjustment of collective pitch P_c and rotation speed n on the blade inflow conditions. The black color in Fig. 8 represents the original inflow velocity condition, while the red color represents the adjusted condition. Under constant cruising speed U , decreasing rotation speed n reduces the local velocity V_R and elevates inflow angle β , whereas increasing collective pitch P_c augments the geometric angle of attack α . Coordinated adjustment of these two parameters allows the rotor operating conditions to be adjusted under constant thrust conditions, thereby making improvements in hydrodynamic efficiency possible.

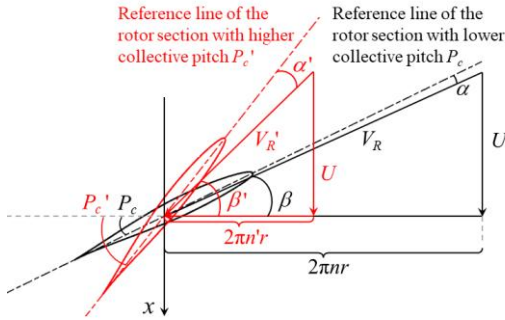


Fig. 8 Schematic of the velocity triangle at $0.75R$ under the same cruising speed for different collective pitch P_c and rotation speed n combinations, where V_R is the inflow velocity of the blade section excluding the induced velocity caused by rotor rotation, β is the inflow angle, and α is the geometric angle of attack

In the present study, the matching of P_c and n is performed to satisfy three prescribed thrust-loading conditions of $K_T/J^2=0.2$, 0.3 and 0.4 , which correspond to target thrusts of approximately 334N, 500N and 666N, respectively, at a cruising speed of 0.679 m/s. The resulting P_c - n combinations in these conditions are presented in Fig. 9. As shown in Fig. 10, the combinations with increasing P_c elevate the rotor K_T and K_Q . The propulsive efficiency η also changes. With

the increasing P_c , η first rises to a maximum around 0.85 and then decreases under all three loading conditions. When using $P_c=26^\circ$ as the reference condition, the coordinated P_c - n matching achieves 16.2% , 8.7% , and 5.1% at $K_T/J^2=0.2$, 0.3 and 0.4 , respectively, with $P_c=52^\circ$, 43.6° , 39° . The results suggest that the P_c associated with peak efficiency shifts to a lower value as the prescribed thrust requirement increases. This trend is in line with the traditional marine propeller pitch design, in which a lower pitch-to-diameter ratio is preferred at a higher thrust loading to maximize propulsive efficiency^[26].

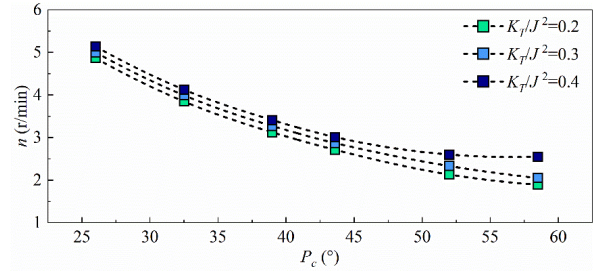


Fig. 9 P_c - n combinations in three different thrust conditions

Fig. 11 shows the FFT results of the rotor thrust and torque with different P_c - n combinations at $K_T/J^2=0.3$. At low collective pitches, the load fluctuation is mainly characterized by low-amplitude discrete spectral lines. The fluctuation soars once the P_c reaches 52° , accompanied by the emergence of a broad and irregular low-order spectral distribution. To further clarify the causes of the severe fluctuation and better understand the differences in rotor wake characteristics between the combinations, proper orthogonal decomposition (POD) analysis is conducted using the velocity field at a plane located $0.675D$ downstream of the rotor disk. As shown in Fig. 12, although the first two modes account for the main flow structures in all three cases with $P_c = 26^\circ$, 39° and 52° , the cumulative energy contribution of the first two modes drops obviously at $P_c = 52^\circ$, which means the wake energy is distributed over a larger number of modes and that the flow becomes less dominated by the leading coherent structures. More importantly for the irregular fluctuation, strong localized fluctuations are observed near the rotor tip in mode 6 at $P_c = 52^\circ$. Combined with the tip vortex structures as illustrated in Fig. 13, these observations suggest that the increasing P_c raises the effective angle of attack and loading of the blade, which may promote flow separation and destabilize the tip-vortex structures. These unsteady flow structures are consistent with the enhanced low-order load fluctuations observed in the FFT results.

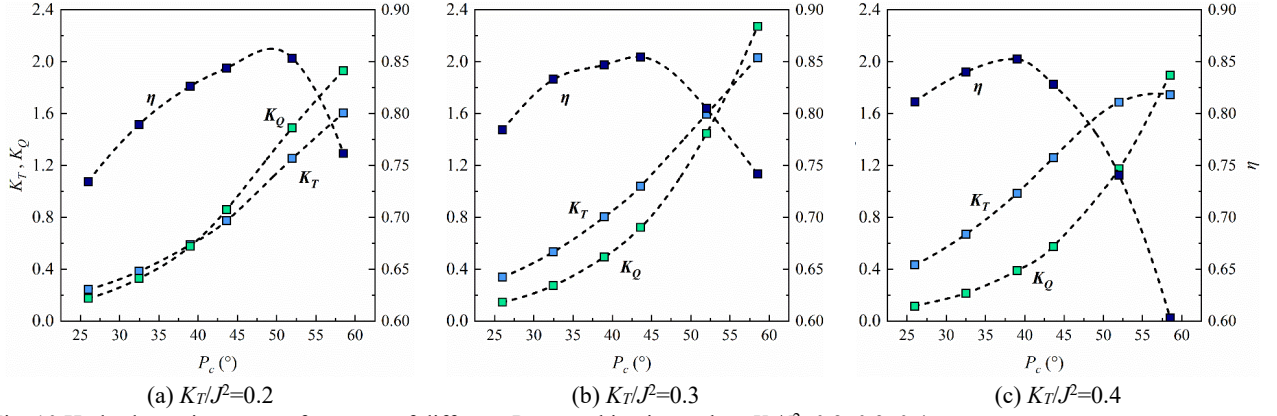


Fig. 10 Hydrodynamic rotor performance of different P_c - n combinations when $K_T/J^2=0.2, 0.3, 0.4$

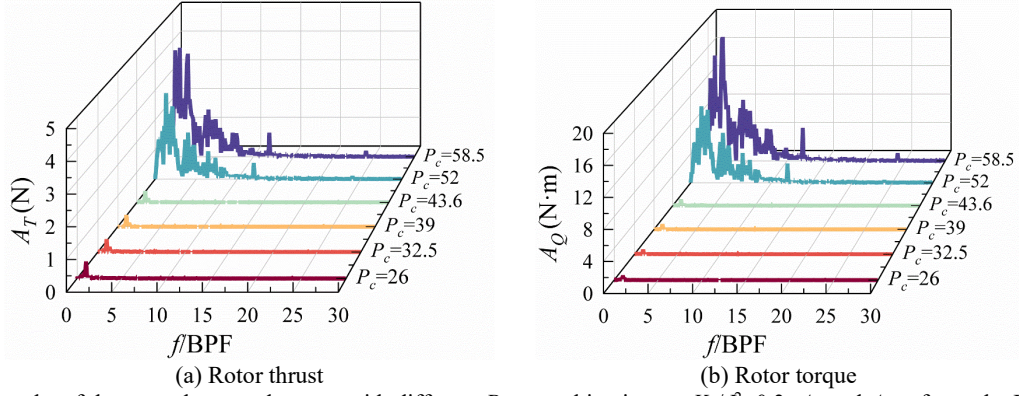


Fig. 11 FFT results of the rotor thrust and torque with different P_c - n combinations at $K_T/J^2=0.3$. A_T and A_Q refer to the FFT amplitude of rotor thrust and torque, f refers to frequency, and BPF refers to blade passing frequency of the rotor

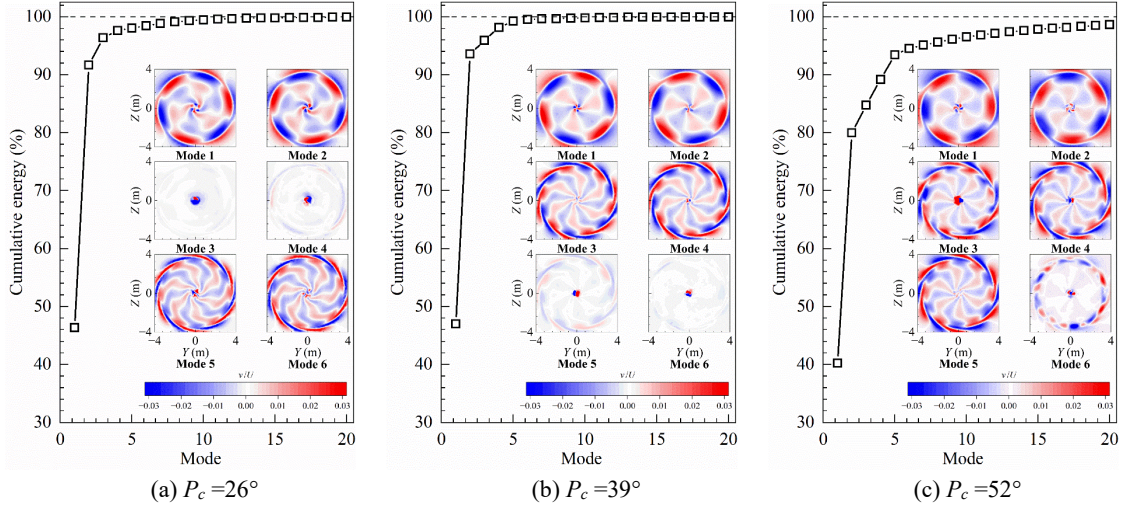


Fig. 12 Cumulative energy contribution and spatial distribution of the velocity POD modes at $K_T/J^2=0.3$

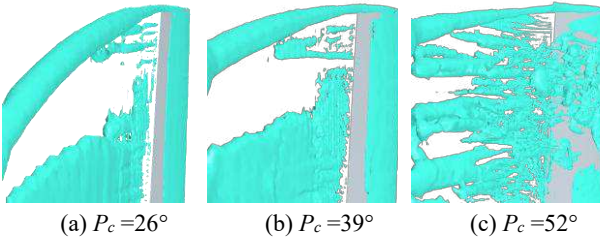


Fig. 13 Tip vortex structures visualized using the $\Omega=0.52$ isosurface at different P_c - n combinations

To assess the potential for cavitation under different P_c - n combinations, Fig. 14 presents the

instantaneous pressure distributions on the rotor blade at $K_T/J^2=0.3$, with $P_c=26^\circ, 39^\circ, 52^\circ$. The pressure is reported relative to the reference pressure as $\Delta P=P-P_0$, where P is the absolute pressure and P_0 is the reference pressure. The results in Fig. 14 (a) and (b) show that the low-pressure areas are concentrated on the blade suction side over the outer radial range $0.8 < r/R < 1.0$. Chordwise pressure profiles at the representative radial position $r/R=0.9$ are therefore compared in Fig. 14(c). The profile shapes vary noticeably across the changing P_c - n combinations. Among the three conditions, the

$P_c=39^\circ$ case exhibits the strongest pressure drop near

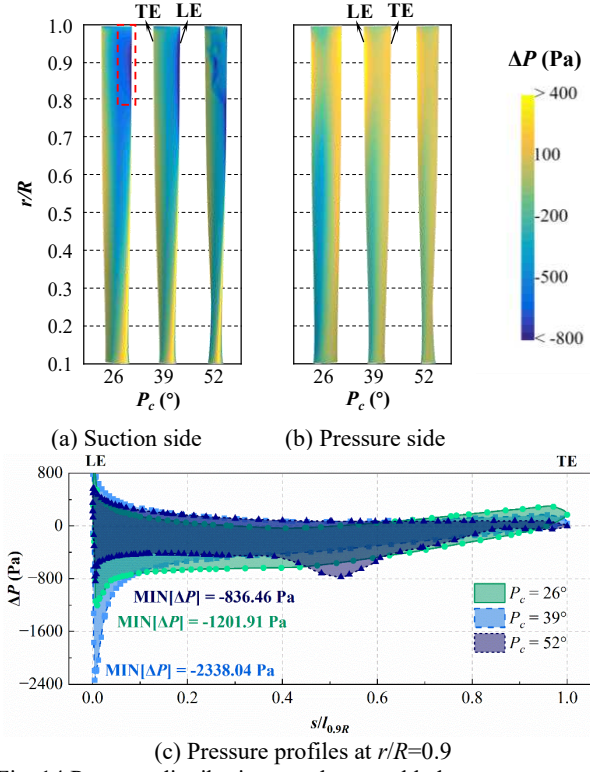


Fig. 14 Pressure distributions on the rotor blade the leading edge, with $\text{MIN}[\Delta P] = -2.338$ kPa, compared with -1.202 and -0.836 kPa for $P_c = 26^\circ$ and 52° , respectively. This condition therefore has the smallest local pressure margin against cavitation among the cases examined. However, under the present low-speed

cruising condition where $U = 0.679$ m/s and $K_T/J^2 = 0.3$, the pressure drop remains insufficient to reduce the local absolute pressure to the vapor pressure. Even conservatively neglecting the hydrostatic pressure contribution and taking P_0 as the standard atmospheric pressure of 101.325 kPa, the corresponding minimum absolute pressure is still 98.987 kPa. This value remains approximately 96.65 kPa above the saturation vapor pressure of water at 20°C ($P_v = 2.337$ kPa). Accordingly, the subsequent discussion of rotor performance and rotor-wing interaction focuses on single-phase hydrodynamic effects, without further consideration of cavitation.

Fig. 15 compares the time-averaged axial and tangential velocity distributions at a plane located $0.675D$ downstream of the rotor disk. At $P_c = 26^\circ$, the axial velocity field exhibits a pronounced radial variation, characterized by a low-velocity region near the hub and a high-velocity region near the rotor tip. With the P_c increasing, the axial velocity distribution is redistributed, with the high-velocity region extending over a broader portion of the wake. A more pronounced change is observed in the tangential velocity. As the collective pitch increases, the tangential velocity is significantly enhanced in both the inner and outer radial regions, with more pronounced peaks at larger radii. These results indicate that the rematching of the P_c and n substantially changes the rotational motion of the wake and alters its momentum distribution.

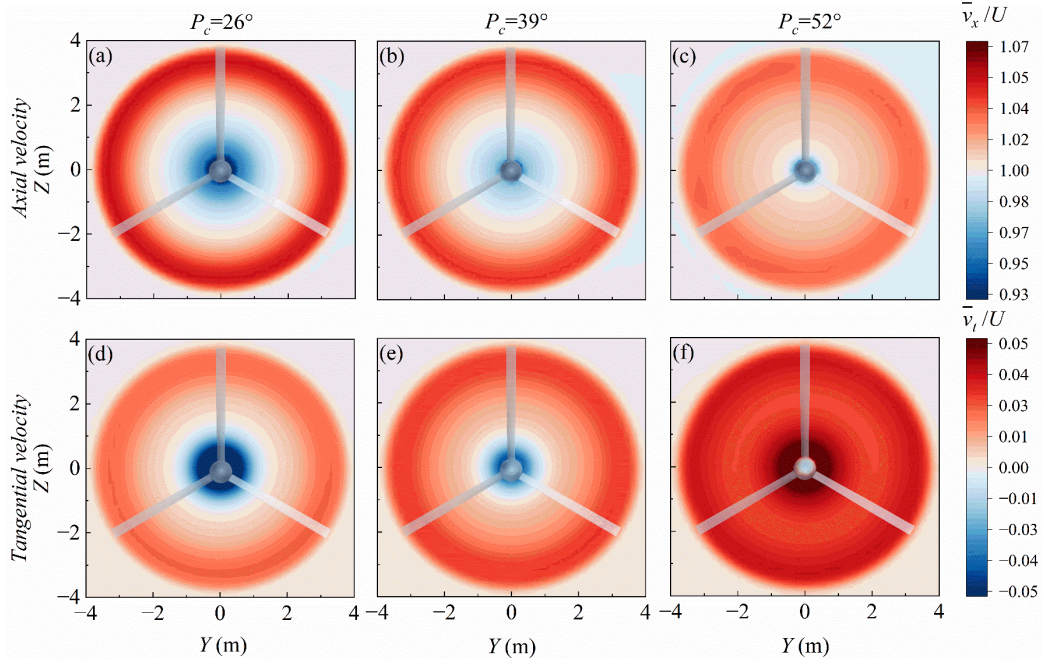


Fig. 15 Time-averaged axial and tangential velocity distribution in the rotor wake at different P_c - n combinations

3.3 Effects of P_c - n matching on rotor-wing system

This section analyzes the rotor-wing interaction

mechanisms and examines the load characteristics and propulsive efficiency of the rotor-wing system under

the P_c - n combinations matched to $K_T/J^2=0.3$ at $U=0.679$ m/s.

3.3.1 Rotor-wing interaction mechanisms

As shown in Fig. 16, under the configuration of the rotor-wing system described in *Section 1* and with the rotor operating condition with $P_c=26^\circ$ and $K_T/J^2=0.3$, the mean performance of both the rotor and wing changed because of the interaction between each other. Specifically, the rotor thrust and torque coefficients rise by 2.26% and 1.65%, and the wing lift coefficient and drag coefficient rise by 2.19% and 3.41%. More importantly, time histories of the rotor and wing performance exhibit strong phase-locked fluctuation feature, which helps to further understand the rotor-wing interaction mechanisms. The rotor thrust and torque exhibit similar variations, following a 3-per-revolution waveform and reaching their maxima at $\theta_1=18^\circ$ and minima at $\theta_4=60^\circ$ within each fluctuation cycle. Similarly, the wing drag coefficient K_F reaches its maximum at $\theta_2=28^\circ$ and then decreases to a minimum at $\theta_5=64^\circ$. The situation is more complex for the wing lift, exhibiting two pairs of local maxima and minima within each fluctuation cycle. Notably, the two lift maxima occur at phases approximately corresponding to the drag maximum and minimum at θ_2 and θ_5 , respectively. These characteristic phase angles are therefore selected for the following flow field analysis to clarify the interaction mechanisms responsible for the load variations.

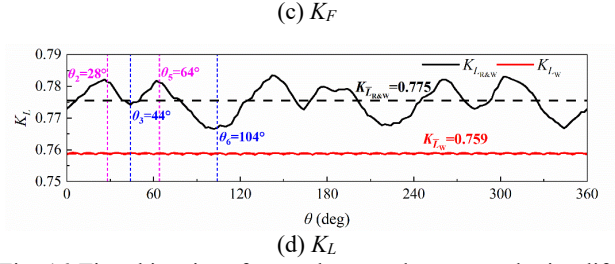
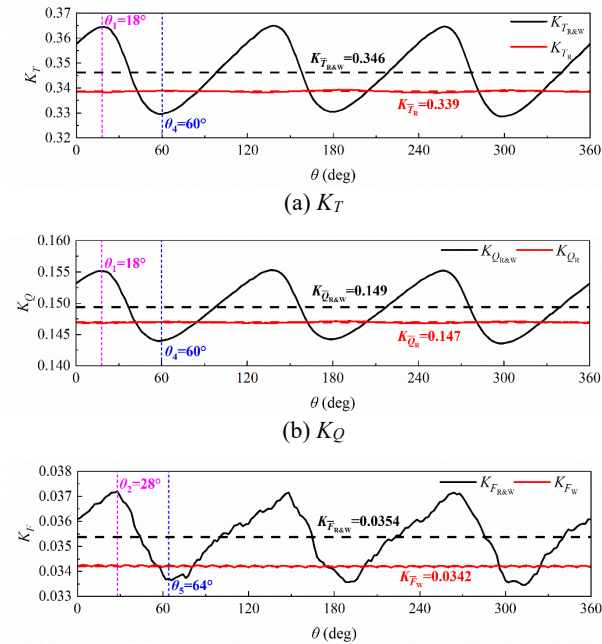


Fig. 16 Time histories of rotor thrust and torque and wing lift and drag over one rotor revolution. Black and red lines denote the cases with and without rotor-wing interaction, respectively, while the horizontal dashed lines indicate the corresponding mean values

Fig. 17 shows the contours of pressure coefficient C_p on the cross-section of the flow field located between the rotor and wing. The C_p is defined as

$$C_p = \frac{P - P_0}{0.5\rho U^2} \quad (16)$$

where P is the local pressure in the flow field, and P_0 is the reference static pressure in the undisturbed inflow. As illustrated in Fig. 17(a), the blockage caused by the thickness of the wing produces a high-pressure region in proximity to the wing leading-edge, referred to here as the Thickness Effect Region. Meanwhile, the hydrodynamic circulation surrounding the wing generates a pressure difference between its suction and pressure sides, forming the low-pressure Loading Effect Region I and the high-pressure Loading Effect Region II, respectively. Consequently, these pressure disturbances extend upstream toward the rotor and impose a spatially nonuniform background pressure field on the rotor disk. As shown in Fig. 17(b) and (c), when the rotor blades sweep through these regions, the pressure difference across the blade surfaces varies periodically with rotor azimuth. At θ_1 , the relative positions of the blades with respect to Loading Effect Region II result in comparatively strong blade loading, corresponding to the maxima of rotor thrust and torque. At θ_4 , this spatial relationship changes substantially, reducing the blade loading and corresponding to the thrust and torque minima. The rotor load fluctuation is therefore closely associated with the combined influence of the wing thickness effect and wing loading effect on the pressure field experienced by the rotor blades.

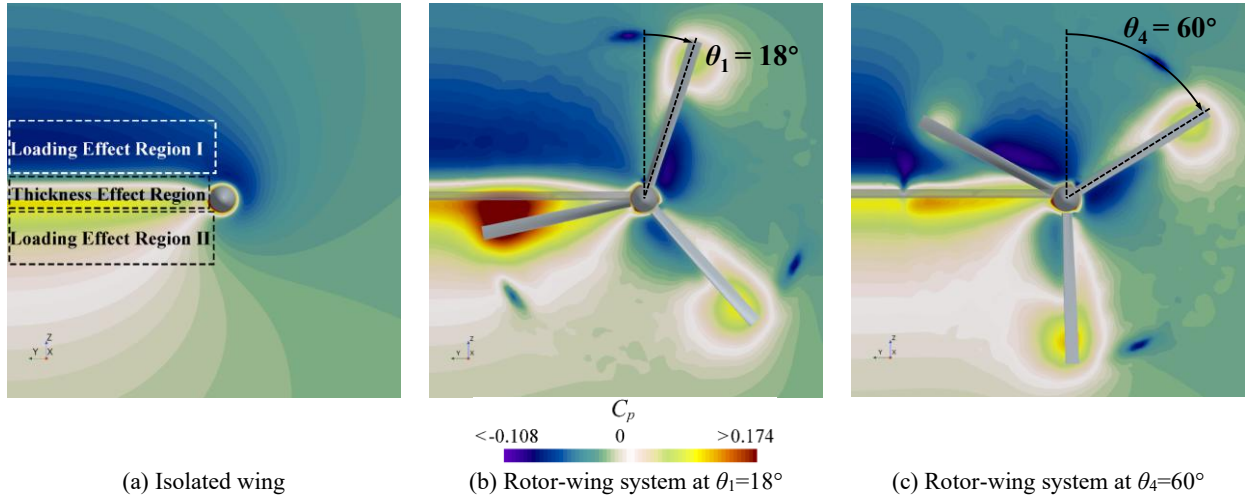


Fig. 17 Pressure contours on the cross-section of the flow field located $0.25c$ downstream of the rotor disk, between the rotor and wing. (a) isolated wing case; (b) rotor-wing interaction case at $\theta_1=18^\circ$, corresponding to the maxima of rotor thrust and torque; (c) rotor-wing interaction case at $\theta_4=60^\circ$, corresponding to the minima of rotor thrust and torque

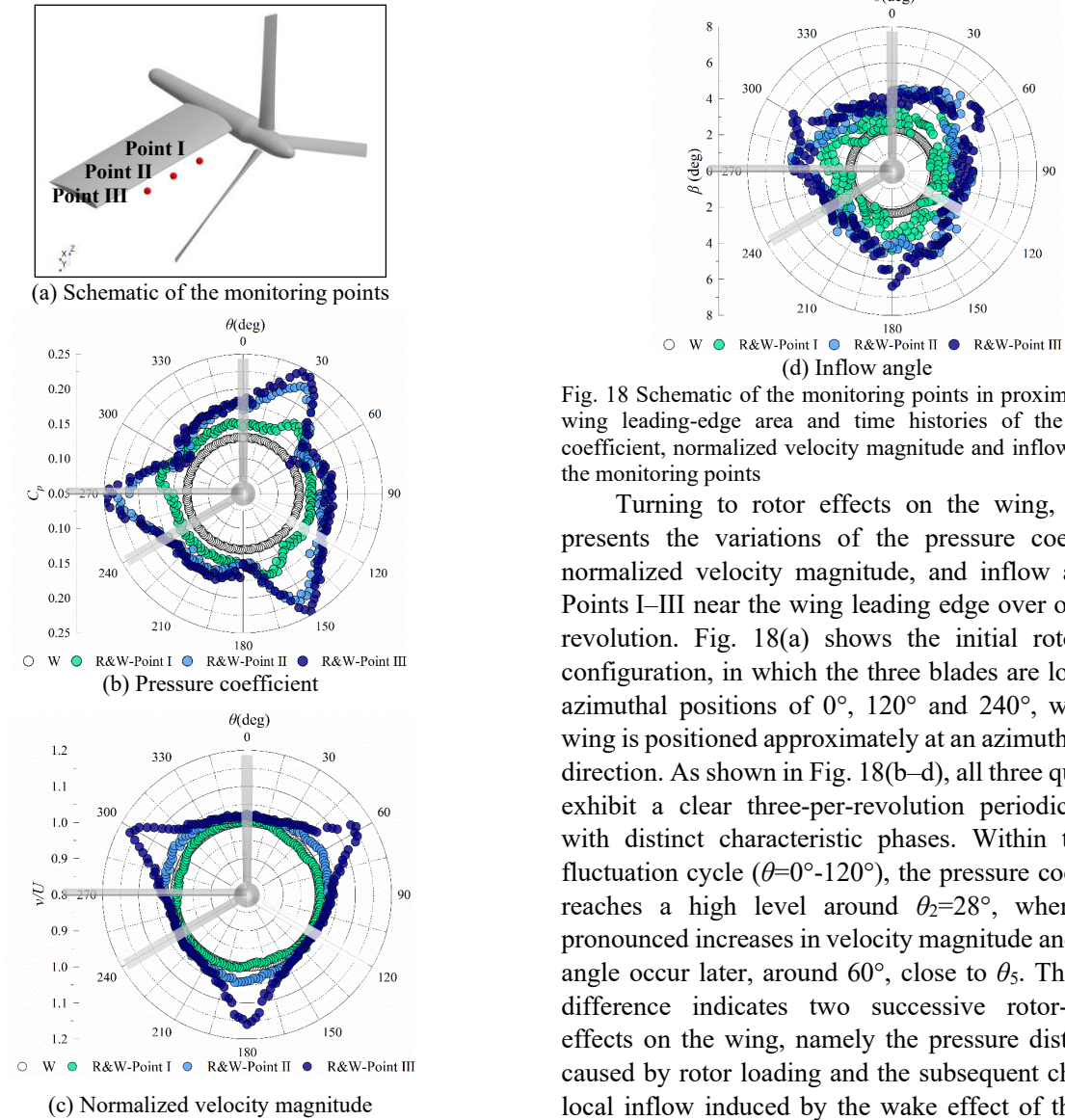


Fig. 18 Schematic of the monitoring points in proximity to the wing leading-edge area and time histories of the pressure coefficient, normalized velocity magnitude and inflow angle at the monitoring points

Turning to rotor effects on the wing, Fig. 18 presents the variations of the pressure coefficient, normalized velocity magnitude, and inflow angle at Points I–III near the wing leading edge over one rotor revolution. Fig. 18(a) shows the initial rotor–wing configuration, in which the three blades are located at azimuthal positions of 0° , 120° and 240° , while the wing is positioned approximately at an azimuth of 270° direction. As shown in Fig. 18(b–d), all three quantities exhibit a clear three-per-revolution periodicity, but with distinct characteristic phases. Within the first fluctuation cycle ($\theta=0^\circ$ – 120°), the pressure coefficient reaches a high level around $\theta_2=28^\circ$, whereas the pronounced increases in velocity magnitude and inflow angle occur later, around 60° , close to θ_5 . This phase difference indicates two successive rotor-induced effects on the wing, namely the pressure disturbance caused by rotor loading and the subsequent change in local inflow induced by the wake effect of the rotor. When a rotor blade passes in front of the wing near θ_2 ,

the high-pressure field induced by blade loading increases the local pressure near the wing leading edge, contributing to the simultaneous increase in wing lift and drag as shown in Fig. 16(c) and (d). As the blade-induced high-pressure region moves above the wing, the pressure difference across the wing is reduced, corresponding to the first lift minimum near θ_3 . Subsequently, at approximately θ_5 , the rotor wake reaches the wing leading-edge region as further illustrated in Fig. 19, producing a marked increase in local velocity and inflow angle. Under the present direction of rotor rotation, the wake induces an upwash component to the wing, corresponding to the second lift maximum, while the accompanying pressure reduction near the leading edge is consistent with the minimum in wing drag. These results indicate that the load variations of the wing arise from the successive effects of rotor load and wake.

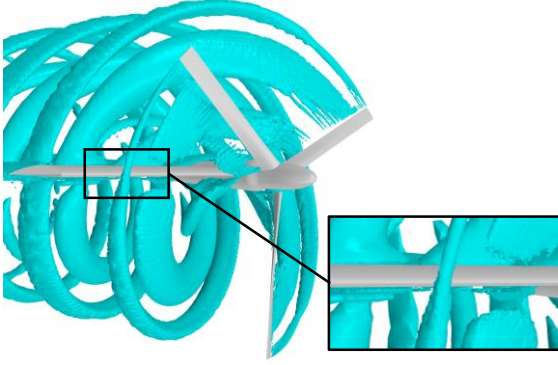


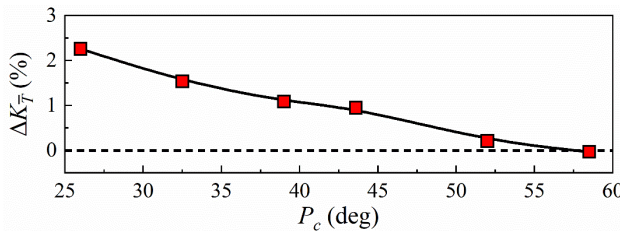
Fig. 19 Instantaneous rotor tip-vortex interaction with the wing at $\theta_5=64^\circ$, visualized using the $\Omega=0.52$ isosurface

3.3.2 Load distributions

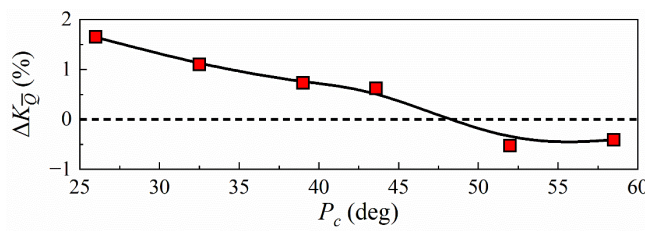
Fig. 20 presents the relative changes in mean thrust and torque induced by the wing effects with different P_c - n combinations. Although wing effects influence the rotor thrust and torque across various P_c - n combinations, these effects remain constrained within a limited level. Compared to isolated rotor performance, variations in thrust and torque persist below 3% for all P_c - n combinations. Specifically, in the lowest pitch condition of $P_c=26^\circ$, there is a 2.26% increase in time-averaged rotor thrust and a 1.65% increase in time-

averaged rotor torque compared with the isolated rotor. As the P_c increases, the interactions from the wing diminish, resulting in reduced augmentation of the thrust and torque. In the highest pitch condition of $P_c=58.5^\circ$, the influence of the interactions on the rotor thrust and torque reverses, resulting in slight reductions in both quantities. Fig. 21 presents the frequency characteristics of rotor thrust and torque under rotor-wing interactions for different P_c - n combinations. The results show that fluctuations of rotor thrust and torque are dominated by the 1st BPF for all P_c - n combinations. Given that these P_c - n combinations yield approximately the same thrust under isolated rotor conditions, the decreasing BPF amplitude of the thrust fluctuation indicates that the wing effects on the rotor diminish as P_c increases and n decreases. Hence, although the torque fluctuation increases with P_c , this opposite trend does not imply stronger interaction, but mainly results from the substantial increase in the isolated rotor torque with P_c , which raises the baseline magnitude of the torque fluctuations.

Fig. 22 illustrates the radial and azimuthal distributions of the thrust variation per unit radial length for a single blade over one rotor revolution at different P_c - n combinations, where the radial segment width $\Delta r=0.1R$ and azimuthal increment $\Delta\theta=2^\circ$. The radial coordinate indicates the radial position, while the circumferential coordinate indicates the azimuth angle. A distinct paired positive and negative variation appears as the blade passes in front of the wing, reflecting the combined effects of wing thickness and wing load identified in Section 3.3.1. As P_c increases, the variation induced by the wing gradually weakens, consistent with the weakening trend observed in Fig. 20 and Fig. 21. Since this paired pattern originates from the thickness and loading effects, its progressive attenuation indicates that both effects become less pronounced at higher P_c . In addition to this, localized oscillations associated with the flow separation shown in Fig. 13 are also observed near the blade tip ($r/R>0.8$) at $P_c=52^\circ$ and therefore represent an additional source of unsteady blade loading.



(a) ΔK_T



(b) ΔK_Q

Fig. 20 Relative changes in the mean thrust and torque due to wing interaction at different P_c - n , where positive values indicate an increase relative to the isolated-rotor condition

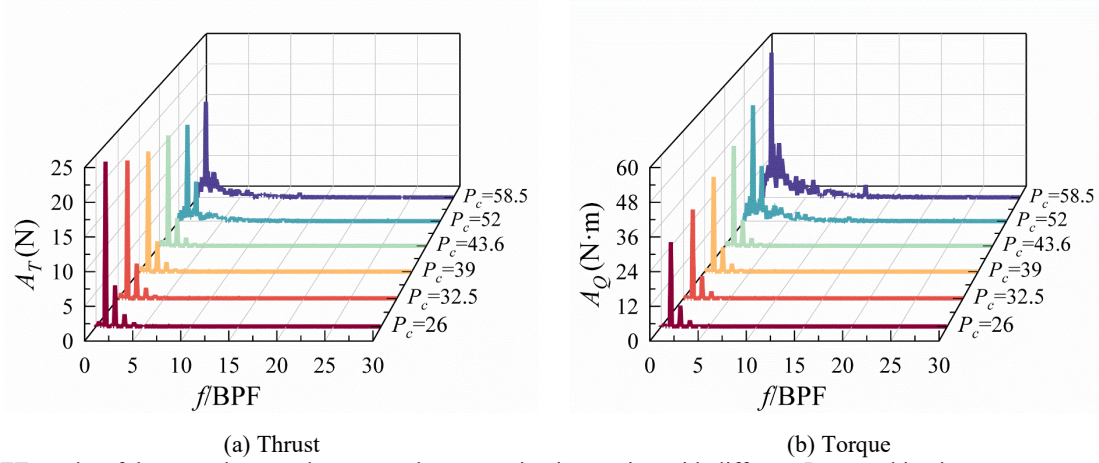


Fig. 21 FFT results of the rotor thrust and torque under rotor-wing interaction with different P_c - n combinations

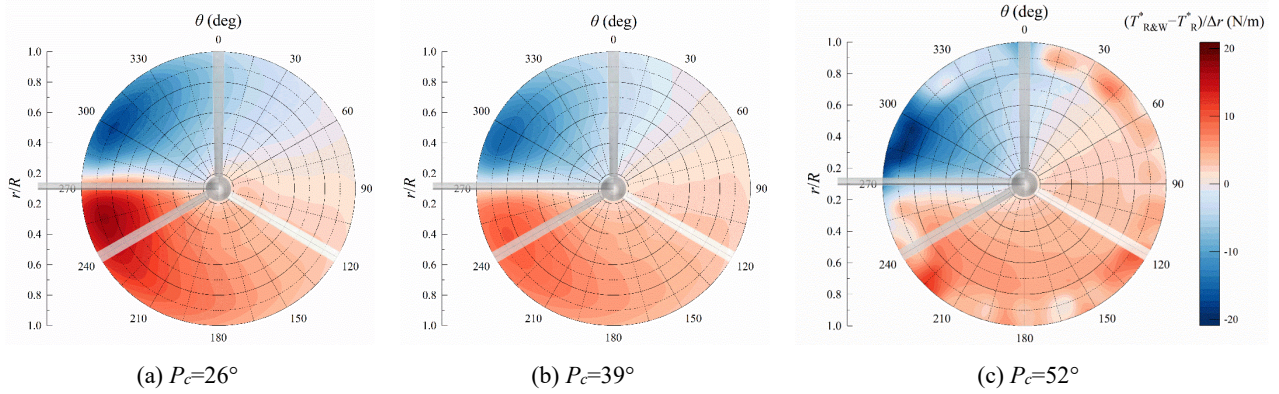


Fig. 22 Radial and azimuthal distributions of the thrust variation per unit radial length for a single rotor blade at different P_c - n combinations

Fig. 23 presents the relative changes in mean drag and lift induced by the rotor effects with different P_c - n combinations. In contrast to the decreasing wing effects on the rotor, the rotor effects on the wing become increasingly pronounced as P_c increases and n decreases. The lift increment increases continuously, while the drag variation changes from a slight increase at low P_c to a substantial reduction at high P_c . Specifically, under the highest P_c condition of 58.5° , the wing achieves a 31.4% lift augmentation paired with a 54.7% drag reduction compared to the isolated wing condition. The spectral results in Fig. 24 further show that the wing load fluctuations vary markedly with increasing P_c . At $P_c=26^\circ$, the BPF and 2BPF components of the lift fluctuation are comparable, reflecting the two lift peaks illustrated in Fig. 16(d) occurring within each fluctuation cycle. As P_c increases, the contribution of the 2BPF component gradually weakens, while the overall fluctuation level increases. A similar increase is observed for the drag fluctuation, although its mean value continuously decreases as shown in Fig. 23. This difference between the mean and fluctuating loads suggests that the variation in wing performance is governed by changes in the unsteady

interaction, which are further examined through the detailed wing load distributions in Fig. 25.

Fig. 25 presents the spanwise distributions of the lift and drag variations per unit span for the wing over one rotor revolution under rotor-wing interactions, where the spanwise segment width $\Delta y=0.1B$ and time increment is 2° . The vertical coordinate indicates the spanwise position of the wing, with $y/B=0$ corresponding to the wing tip, $y/B=1.0$ corresponding to the wing root, and $y/B=0.762$ corresponding to the region near the rotor tip, while the horizontal coordinate represents the rotor azimuth angle. As identified in Section 3.3.1, the loading effect is mainly associated with the response near θ_2 , whereas the wake effect becomes dominant around θ_5 . This distinction is more clearly reflected in Fig. 25. At $P_c=26^\circ$, the loading induced response forms a weak local increase in both the drag and lift around θ_2 , while the wake effect near θ_5 produces the drag trough and the second lift peak, respectively. As P_c increases, the load variation near θ_2 gradually weakens, whereas the response near θ_5 becomes substantially stronger, indicating that the rotor loading effect weakens while the wake effect strengthens. For drag, the weakening positive variation

associated with rotor loading effect, together with the strengthening negative variation induced by rotor wake effect, leads to a reduction in the mean drag but an increase in its periodic fluctuation. For lift, the weakening of the first peak and strengthening of the

second peak explain both the reduced relative importance of the 2BPF component and the overall increase in fluctuation amplitude at higher P_c . Thus, increasing P_c shifts the dominant rotor influence on the wing from the loading effect toward the wake effect.

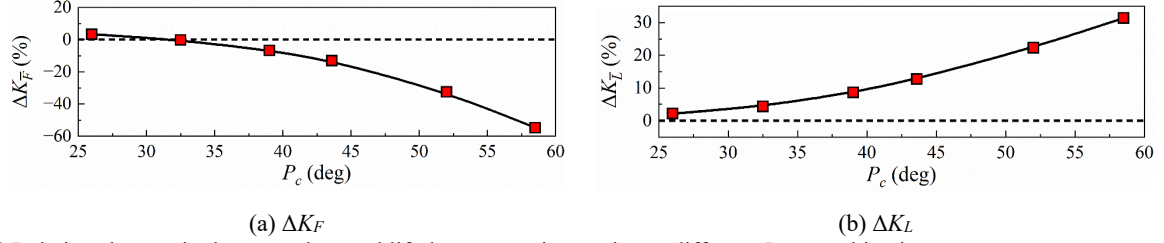


Fig. 23 Relative changes in the mean drag and lift due to rotor interaction at different P_c - n combinations

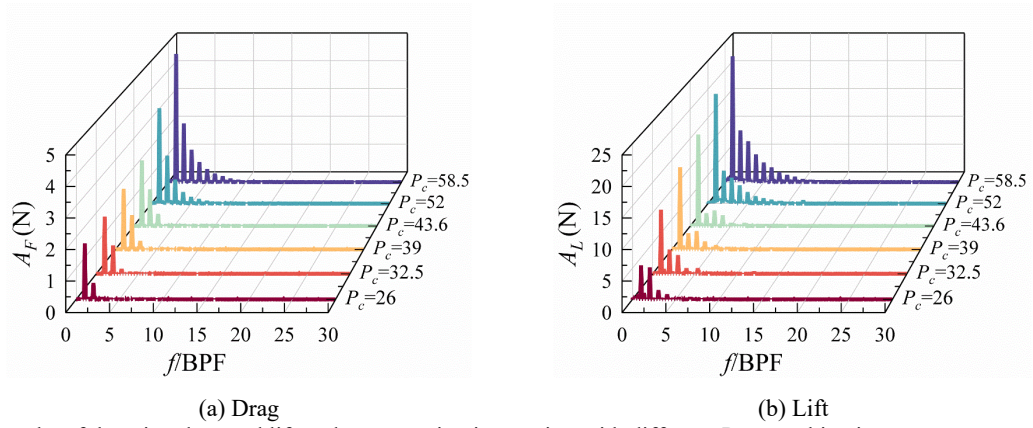
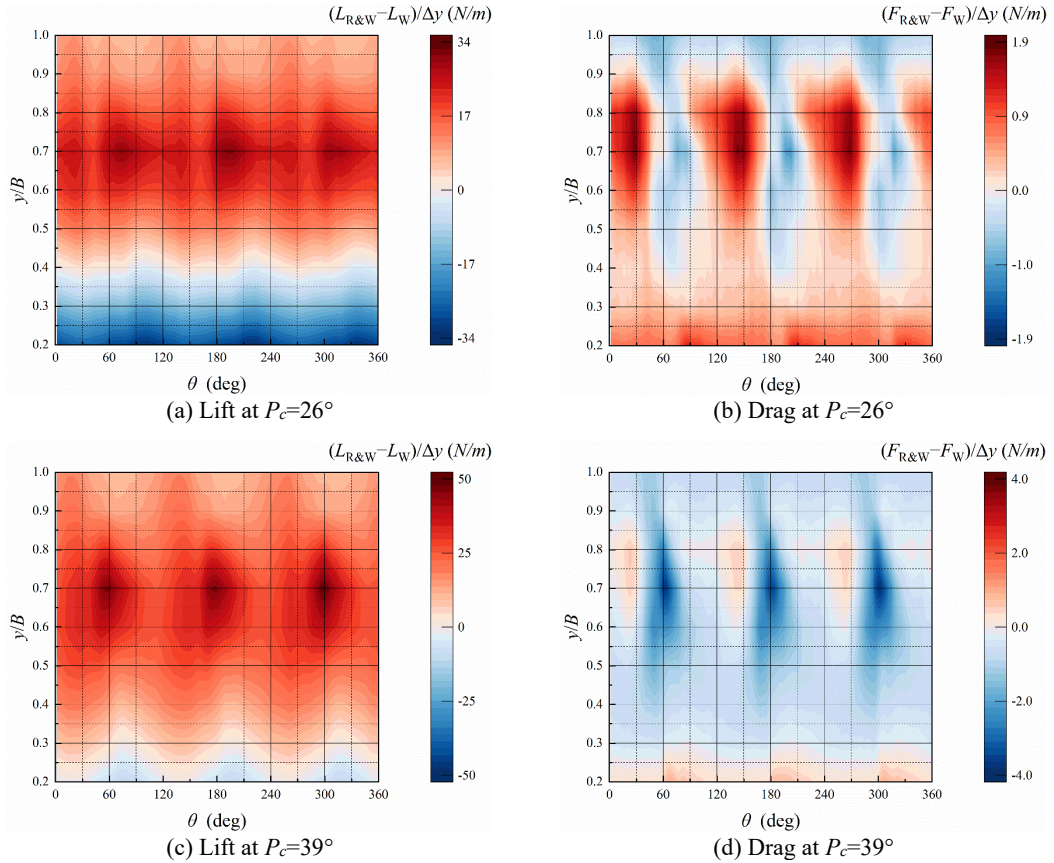


Fig. 24 FFT results of the wing drag and lift under rotor-wing interaction with different P_c - n combinations



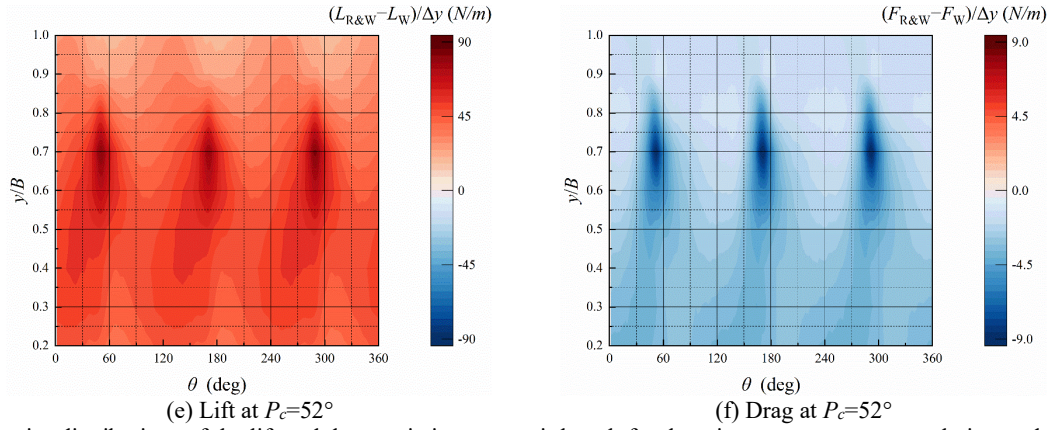
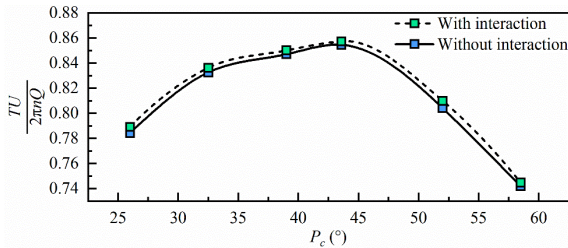


Fig. 25 Spanwise distributions of the lift and drag variations per unit length for the wing over one rotor revolution under rotor-wing interactions

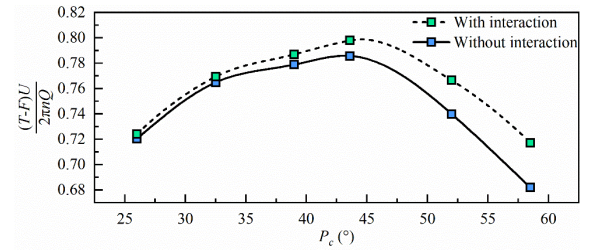
3.3.3 Propulsion efficiency of the rotor-wing system

Fig. 26(a) compares the rotor propulsive efficiency with and without rotor-wing interaction. The two curves remain very close over all investigated P_c - n combinations. Although the interaction changes both rotor thrust and torque, their effects on the efficiency largely offset each other. As a result, the change in rotor efficiency remains limited. The efficiency trend obtained from the isolated rotor is also retained, with the maximum occurring near $P_c=43.6^\circ$, indicating that the interaction has little influence on the efficiency characteristics of the rotor itself.

A different trend is observed when the rotor and wing are considered as an integrated propulsion module and the wing drag is accounted for in the net propulsive efficiency. As shown in Fig. 26(b), rotor-wing interaction increases the system efficiency for all investigated conditions, and the difference becomes more pronounced as P_c increases. At low P_c , the interaction causes only a small change. With increasing P_c and decreasing n , the rotor wake effect becomes stronger and the wing drag is progressively reduced, as discussed in Section 3.3.2. Consequently, the benefit of the interaction becomes increasingly apparent at the system level. At $P_c=58.5^\circ$, the increase in net propulsive efficiency reaches 5.2% compared with the non-interacting reference.



(a) Rotor propulsive efficiency



(b) Rotor-wing system propulsive efficiency

Fig. 26 Propulsive efficiencies of the rotor and rotor-wing system with and without rotor-wing interactions at different P_c - n combinations

It should also be noted that the increasing benefit from rotor-wing interaction at high P_c does not compensate for the efficiency deterioration caused by an excessively large collective pitch. Both curves in Fig. 26(b) still reach their highest values near $P_c=43.6^\circ$ and then decrease. Therefore, rotor-wing interaction mainly changes the magnitude of the system efficiency rather than the location of the highest-efficiency operating condition. Together with the load fluctuations discussed above, these results suggest that the selection of P_c and n should consider not only the isolated rotor efficiency, but also the mean and unsteady performance of the complete rotor-wing system.

4. Conclusion

This paper numerically investigated the hydrodynamic performance of a hybrid aquatic-aerial rotor-wing system considering the P_c - n matching, aiming to identify the most efficient combination among the cases examined and to determine how parameter matching affects rotor-wing interaction. The main conclusions are summarized as follows:

(1) The rematching of rotor collective pitch and rotational speed can improve propulsion efficiency by changing the rotor load and wake characteristics under a prescribed thrust output condition. For $K_T/J^2=0.2, 0.3$ and 0.4 , the collective pitch corresponding to the highest efficiency decreases as the thrust requirement

increases. The most efficient combinations at each thrust-loading condition yield rotor efficiency improvements of approximately 16.2%, 8.7% and 5.1%, respectively, relative to the $P_c=26^\circ$ baseline.

(2) The hydrodynamic influence of the wing on the rotor originates from the wing thickness effect and loading effect. These mechanisms alter the pressure distribution downstream of the rotor, causing periodic rotor load oscillations as the blades pass in front of the wing, while changes in the mean thrust and torque remain below 3% in magnitude relative to the isolated rotor.

(3) The hydrodynamic influence of the rotor on the wing is dominated by the rotor wake and loading effects. The rotor wake effect increases lift and reduces drag by modifying axial and circumferential velocity distributions in the wake field, whereas the rotor loading effect elevates both lift and drag through pressure field modulation near the wing leading edge. In addition to amplifying wing load fluctuations, the coupling of these two effects increases the mean lift and reduces the mean drag by up to 31.4% and 54.7% relative to the isolated wing, respectively.

(4) The rotor-wing interactions vary with P_c - n combinations. With a higher P_c and a lower n , the wing thickness effect, wing loading effect, and rotor loading effect are weakened, while the rotor wake effect is enhanced. Although the resulting changes in mean rotor thrust and torque remain limited, the increase in wing lift and reduction in drag become more pronounced.

(5) The rotor-wing interactions have limited influence on rotor efficiency but improve the net propulsive efficiency by up to 5.2% relative to the non-interacting reference through reduced wing drag. This benefit increases with P_c but does not offset the deterioration in rotor performance at excessive pitch. Consequently, at $K_T/J^2=0.3$, the highest system efficiency among the combinations examined remains near $P_c=43.6^\circ$, indicating that interaction mainly changes the efficiency level rather than the most efficient operating combination.

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Author Contributions

Jingyu Li: Writing – original draft, Validation, Methodology, Formal analysis. Wei He: Writing – review & editing, Supervision, Resources, Project administration, Funding acquisition, Conceptualization.

Xiangning Huang: Writing – review & editing, Formal analysis. Ziru Li: Writing – review & editing, Supervision, Resources, Project administration.

Data Availability

The datasets generated or analyzed during the current study are available from the corresponding author upon reasonable request.

Ethics Approval and Consent to Participate

Not applicable.

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Conflict of Interest

All authors have no relevant financial or non-financial interests to disclose.

References

- [1] Weisler, W., Stewart, W., Anderson, M. B., Peters, K. J., Gopalathnam, A., & Bryant, M. (2018). Testing and characterization of a fixed wing cross-domain unmanned vehicle operating in aerial and underwater environments. *IEEE Journal of Oceanic Engineering*, 43(4), 969–982.
- [2] Wang, Z., Jiang, Y., Zou, Z., & Zhen, Z. (2025). Design and implementation of a multimodal tilt-rotor unmanned aerial-aquatic vehicle. *Journal of Field Robotics*, 42(5), 1766–1782.
- [3] Xia, M., Wang, B., & Luo, Z. (2025). Design and experiment of a multimodal aerial aquatic vehicle with morphing wing and tilting rotors. *Advances in Mechanical Engineering*, 17(2), 1–17.
- [4] Lim, J. W. (2019). Fundamental investigation of proprotor and wing interactions in tiltrotor aircraft. In *Vertical Flight Society 75th Annual Forum & Technology Display*. Vertical Flight Society.
- [5] Aref, P., Ghoreyshi, M., Jirasek, A., Satchell, M. J., & Bergeron, K. (2018). Computational study of propeller–wing aerodynamic interaction. *Aerospace*, 5(3), Article 79.
- [6] Ananda, G. K., Selig, M. S., & Deters, R. W. (2018). Experiments of propeller-induced flow effects on a low-Reynolds-number wing. *AIAA Journal*, 56(8), 3279–3294.
- [7] Maia, M. M., Mercado, D. A., & Diez, F. J. (2017). Design and implementation of multirotor aerial-underwater vehicles with experimental results. In *2017 IEEE/RSJ International Conference on Intelligent Robots and Systems (IROS)* (pp. 961–966). IEEE.
- [8] Alzu'bi, H., Mansour, I., & Rawashdeh, O. (2018). Loon Copter: Implementation of a hybrid unmanned aquatic–aerial quadcopter with active buoyancy control. *Journal of Field Robotics*, 35(5), 764–778.
- [9] Moore, J., Fein, A., & Setzler, W. (2018). Design and analysis of a fixed-wing unmanned aerial-aquatic vehicle. In *2018 IEEE International Conference on Robotics and*

- Automation (ICRA)* (pp. 1236–1243). IEEE.
- [10] Pranto, M. R. I., Dodul, M. S. H., Inam, M. I., & Hossain, K. A. (2020). CFD analysis of flow characteristics over air-propellers used in a VTOL AquaUAV. In *Proceedings of the 6th International Conference on Mechanical, Industrial and Energy Engineering (ICMIEE 2020)*. Khulna, Bangladesh.
 - [11] Wei, Z., Teng, Y., Meng, X., Yao, B., & Lian, L. (2022). Lifting-principle-based design and implementation of fixed-wing unmanned aerial–underwater vehicle. *Journal of Field Robotics*, 39(6), 694–711.
 - [12] Wu, S., Wu, S., Shao, M., He, Z., Liu, Y., & Zhang, J. (2025). Fault-tolerant control strategies for tilt-rotor aerial-aquatic vehicles: Design and implementation. *Defence Technology*, 51, 274–293.
 - [13] Tan, Y. H., Siddall, R., & Kovac, M. (2017). Efficient aerial–aquatic locomotion with a single propulsion system. *IEEE Robotics and Automation Letters*, 2(3), 1304–1311.
 - [14] Dang, Z., Wu, M., He, X., Huang, Z., Ying, Z., Chen, Z., & Zheng, C. (2025). An amphibious propeller design optimization framework based on deep neural network surrogate model. *Aerospace Science and Technology*, 159, Article 109967.
 - [15] Dang, Z., Wu, M., He, X., Huang, Z., Ying, Z., Chen, Z., & Zheng, C. (2025). Aquatic–aerial propeller optimization based on a multi-fidelity neural network and transfer learning. *Physics of Fluids*, 37(10), Article 107121.
 - [16] Tan, Y. H., & Chen, B. M. (2019). Motor-propeller matching of aerial propulsion systems for direct aerial-aquatic operation. In *2019 IEEE/RSJ International Conference on Intelligent Robots and Systems (IROS)* (pp. 1963–1970). IEEE.
 - [17] Copaci, A., Dzwigalo, M., Kowalczyk, P., Sierocki, K., Bram, M. V., & Durdevic, P. (2025). A variable-pitch propeller system for dual-medium UAVs: Design, modeling, and validation. In *2025 29th International Conference on Methods and Models in Automation and Robotics (MMAR)* (pp. 106–111). IEEE.
 - [18] Schollenberger, M., Kirsch, B., Lutz, T., Krämer, E., & Friedrichs, J. (2024). Aerodynamic interactions between distributed propellers and the wing of an electric commuter aircraft at cruise conditions. *CEAS Aeronautical Journal*, 15, 255–267.
 - [19] Felker, F. F., Betzina, M. D., & Signor, D. B. (1985). Performance and loads data from a hover test of a full-scale XV-15 rotor (NASA Technical Memorandum 86833). National Aeronautics and Space Administration.
 - [20] Gritskevich, M. S., Garbaruk, A. V., Schütze, J., & Menter, F. R. (2012). Development of DDES and IDDES formulations for the $k-\omega$ shear stress transport model. *Flow, Turbulence and Combustion*, 88, 431–449.
 - [21] Shur, M. L., Spalart, P. R., Strelets, M. Kh., & Travin, A. K. (2008). A hybrid RANS-LES approach with delayed-DES and wall-modelled LES capabilities. *International Journal of Heat and Fluid Flow*, 29(6), 1638–1649.
 - [22] Siemens Digital Industries Software. (2024). *Simcenter STAR-CCM+ 2402 user guide*.
 - [23] Liu, C., Wang, Y., Yang, Y., & Duan, Z. (2016). New omega vortex identification method. *Science China Physics, Mechanics & Astronomy*, 59(8), Article 684711.
 - [24] International Towing Tank Conference. (2021). Uncertainty analysis in CFD verification and validation methodology and procedures (Recommended Procedures and Guidelines No. 7.5-03-01-01, Revision 04).
 - [25] Heinke, H. J., Hellwig-Rieck, K., & Lübke, L. (2019). Influence of the Reynolds number on the open water characteristics of propellers with short chord lengths. In *Proceedings of the Sixth International Symposium on Marine Propulsors (SMP'19)*, Rome, Italy.
 - [26] Fitriadhy, A., Adam, N. A., & Abu Bakar, A. (2023). Computational investigation into the effect of pitch ratio on B-series propeller performance. *Journal of Naval Architecture and Marine Engineering*, 20(2), 143-154.