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An Experimental and Numerical Evaluation of the Aerodynamic Performance of a UAV Propeller Considering Pitch Motion

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Abstract: Considering the vibration generated by a propeller-driven UAV or encountering gust, the propeller will perform a very complex follower motion. A pitch and rotating coupled motion is proposed in the present work that can take more complex unsteady performance of follower force than a regular fixed-point rotating motion. In order to evaluate the unsteady follower force and conduct parametric study, an extensive ground test bench was designed for this purpose where the whole test system was driven by a linear servo actuator and the follower force was measured by a 6-component balance. For CFD simulation, coupled motion in particular needs detailed unsteady aerodynamic model; therefore, a high-fidelity CFD-based study integrated with the overset mesh method was complemented to solve the unsteady fluid of varying conditions. The results suggest that a significant influence on unsteady follower force is observed, and the mean value of in-plane force does not equal to zero during the coupled motion process. Compared with the regular fixed-point rotation of propeller, the fluctuation frequency of follower force in present work couples the rotation and pitch motion frequencies. In addition, the oscillation amplitude of out-plane force and torque is positively related with the pitch frequency, pitch amplitude, and relative length from leading edge of wing to the rotation center. For example, the oscillation amplitude of 1-blade’s out-plane force and torque increases by 57.122% and 66.542% for the 5 Hz-5 deg case compared with the 5 Hz-3 deg case, respectively. However, the torque is not sensitive to frequency of pitch motion. The generally excellent agreement evident between the ground test and numerical simulation results is important as guidance for our future investigation on “dynamic” aerodynamic performance of a propeller-driven UAV.

Keywords: UAV propeller; ground test; CFD; overset mesh method; pitch motion; follower force

1. Introduction

The interest in the research conducted on propeller-driven UAVs (unmanned aerial vehicles) for a variety of military and civilian applications has increased rapidly over the past decade [1–4]. In the previous literature [5–7], the research performed on the unsteady aerodynamic performance of an isolated propeller has been limited to studying the situation where a propeller only rotates around the center of rotation at a fixed point so did the propeller–wing aerodynamic interference [8]. However, it is common for the propeller system and the UAV airframe to encounter gust [9,10], or for the propeller to be affected by the vibration of an elastic wing [11,12]. To address this situation, the present work proposes a propeller with a coupled motion form, namely, fixed-point rotating motion coupled with pitch motion. Specifically, the pitch motion originates from the vibration of the wing, but a real wing model is not included in this paper. Furthermore, the direction of the propeller thrust and torque changes with the pitch motion phase angle, which
makes the propeller aerodynamic force an unsteady “follower” force. The unsteady follower effect makes predicting the propeller aerodynamics more difficult.

With these considerations in mind, the effects of coupling motion on the propeller’s aerodynamic performance warrant consideration. Hodges et al. modeled the propeller thrust as a follower force in order to explore the thrust effect on the bending–torsion flutter boundary of very flexible wings [13]. The result showed that the flutter speed increased by 11% due to the propeller follower force. Similarly, Teixeira et al. [14] also considered propeller loads (forces and moments) as follower forces and studied the influence of the follower forces on the unsteady aerodynamic performance and dynamic response of a very flexible wing. In the work of Ostuka et al. [15], the propeller-induced axial velocity caused a wing deflection change of 5.4% and the propeller decreased the vibration amplitude of the wing. In the above literature, there are two common points: the spatial position of the propeller changed with the vibration of the wing, and the propeller follower force that was generated changed with time and position. Thus, the propeller follower force influences the vibration amplitude, dynamic aeroelasticity response, and aerodynamic performance of the wing significantly, but there is little research on the propeller follower force itself. Therefore, in the present work, we paid more attention to the follower force performance of the propeller itself.

Static and wind tunnel experiments are common to measure propeller loads and investigate the aerodynamic characteristics. During the experiment by Gamble et al., a static test bench was built to characterize the forces and moments acting on the motor and on a micro air vehicle, in which the influence of axial and vertical wing placements on propeller and wing loads was collected [16]. As observed from the wind tunnel experiments of Airbus A400M military transport aircraft [17], four 6-component Rotating Shaft Balances were used to measure the thrust and torque plus the forces and moments contained in the propeller rotating plane (so called 1P-force). In addition, Garofano et al. [18] and Yang et al. [19] applied the 6-component balance to the ground effect experiment and aeroacoustics experiment of the propeller, which indicated that the balance provided an effective way to understand the aerodynamic characteristics of various configurations of propellers. Therefore, we designed a set of kinematic mechanisms to generate the coupled motion of the propeller in order to simulate the conditions of random wing vibration or encountering gust, and then measured the propeller follower forces with a 6-component balance. It is worth declaring that the balance was used to measure the “dynamic” aerodynamic forces of the propeller in the present work, which was a significant difference from the literature. In addition to this, the test of dynamic performance of the linear servo actuator was also an important content for the UAV.

Meanwhile, a high-fidelity CFD (computational fluid dynamics) work based on the overset mesh method was also conducted to study the complex aerodynamic performance. In general, several numerical approaches have been proposed for CFD computation, mainly including DNS (direct numerical simulation), LES (large eddy simulation) and RANS (Reynolds-averaged Navier-Stokes). DNS can simulate a fully turbulent flow without approximations, whereas the method is not available for unsteady rotational flows around the rotating propeller due to the exhaustive computational cost [20]. LES outperforms DNS in terms of computational cost, but it is still insufficient for propeller aerodynamic simulations in engineering application. RANS and unsteady RANS have developed into wide functions to solve the complex rotating flow phenomena around the propeller [21–23]. Stokkermans et al. concluded that the RANS CFD with a relatively simple S-A (Spalart–Allmaras) turbulence model was capable of modeling the aerodynamic performance of wingtip-mounted propellers [24]. Another turbulence model using SST (shear stress transport) k-ω combined with RANS CFD was chosen to solve the influences of non-axial freestream conditions on aerodynamic force of an isolated propeller and ducted configuration at different inflow angles [25]. Although there was a large amount of research about propellers, this study only examined the “static” performance of a fixed-point rotation. A significant different was that the present work carried out the “dynamic”
performance of a coupled motion based on the overset mesh method, and the “dynamic” performance was critically important for a propeller-driven UAV.

On the basis of the above-mentioned analysis, the goal of the research presented in this paper is twofold. The first goal is to investigate the capability of the RANS CFD based on the overset mesh method for the simulation of a rotating UAV propeller under the coupled motion, by comparison of a static ground test specifically design for a better fundamental understanding of the unsteady performance of the follower force. The second goal is to provide guidance for our next investigation on the influence of the propeller follower force on the aeroelasticity of the wing. The remainder of the paper is organized as follows: Section 2 details the geometry of the carbon fiber propeller model used throughout this work, the ground test setup, and the fundamentals of CFD. The turbulent model, computational domain, aerodynamic model, mesh generation, and a grid independence study are introduced. Section 3 presents the results of the pitch motion effect obtained from both the ground test cases and numerical simulations. Finally, we present the conclusions from the analysis of the pitch motion effect and provide ideas for future work along the same lines.

2. Materials and Methods

2.1. Propeller Geometry

The propeller used in the ground test and CFD simulation was a carbon fiber propeller with a diameter of 330.2 mm and a pitch of 165.1 mm; its 3D geometry is shown in Figure 1. The evolution of the chord and the pitch angle distributions of the propeller is shown in Figure 2.

![Figure 1. Geometric model of the propeller used in experimental and numerical analysis.](image1)

![Figure 2. The evolution of the chord and pitch angle of the propeller.](image2)
affected by the mechanical vibration due to the high-speed rotation of propeller. The bottom of the support rod was fixed on the shelf by bearings. The actuating mechanism was connected with the support rod through a connecting rod. The vibration mechanism drives the propeller unit and support rod through a servo actuator and the hydraulic system to perform horizontal reciprocating motion through the connecting rod. By adjusting the vibration frequency of the actuating mechanism, a controllable vibration of the model between 0 Hz and 7 Hz can be achieved, which meets the requirements for both this ground test and the wind tunnel experiment. However, although the servo actuator has enough energy to drive the test motion mechanism, there is a loss of partial amplitude during high-frequency movements.

![Figure 3. Ground test bench to investigate the pitch motion effect with an isolated propeller: (a) side-view; (b) top-view.](image)

**Figure 3.** Ground test bench to investigate the pitch motion effect with an isolated propeller: (a) side-view; (b) top-view.

### 2.2.1. Propeller Measurement System

The propeller unit measurement system was composed of two different subsystems. The first one was a 6-component balance (Figure 3) that was used to measure the thrust and torque on the propeller blades. The front end of the 6-component balance was connected to the motor through four screws, and the rear end was fixed on the support rod through a flange. There was a fairing outside the propeller unit to ensure the stability of the flow field. The range of forces and moments in the six components are ($\pm10 \text{ Kg}$, $\pm5 \text{ Kg}$, $\pm5 \text{ Kg}$, $\pm4 \text{ N} \cdot \text{m}$, $\pm1\text{N-m}$, $\pm1\text{N-m}$), respectively. The corresponding values of output sensitivity are (1.485 mv/V, 1.412 mv/V, 0.840 mv/V, 1.609 mv/V, 1.371 mv/V, 1.843 mv/V), respectively. In addition, at each test point, the balance data were acquired at a sampling frequency of 1000 Hz for 30 s, which provided enough time to produce adequate data for time-averaged forces and moments. The repeatability error of the sensor was within 0.15% and the hysteresis was also within 0.15%.

The second subsystem was responsible for providing power and rotation speed measurement. The speed of the electric motor was determined from the feedback of an electronic speed controller connected to a 580 kW DC power supply (Figure 3). The motor PWM input was sent from a remote control to achieve a rotational speed of 8000 rpm. To measure the rotational speed of the propeller, an optical sensor was considered. The red light emitted by the optical sensor was reflected by the propeller blades to the receiver, causing the sensor to generate a switch signal. Counting was performed to complete the rotational speed measurement. Then, the readouts from the tachometer were used to manually adjust the control signal sent to the electric speed controller. The error between the actual rotational speed and the ideal value was always within 0.1%, which meets the rotational speed measurement requirements of this test.
2.2.2. Support Rod

The support rod was designed as a “T” type. The horizontal rod was a carbon fiber tube with a length of 200 mm, where the outer diameter was 26 mm and inner diameter was 22 mm. This ensured that the overall structure had sufficient stiffness. The front end of the horizontal rod was connected to the 6-component balance through a flange, and the tail was connected to the vertical support rod through a T-pipes. The vertical support rod was also a carbon fiber tube with an outer diameter of 30 mm, an inner diameter of 26 mm, and a length of 270 mm. The tail of the vertical support rod was connected to actuating mechanism.

2.2.3. Actuating Mechanism and Data Acquisition System

The power source of the vibration mechanism was a linear servo actuator, which can achieve the reciprocating motion with a stroke of 42 mm and can output a thrust of at least 2000 N. The bottom of the steering gear was installed on the support, and the front end was connected with the support rod through the crank rocker mechanism. The active part of the crank rocker mechanism was connected with the front end of the servo actuator, the length was 50 mm, and the driven part was connected with the bottom of support rod. Figure 4 depicts the process of the static ground test for an isolated propeller under the coupled motion state. The propeller loads data were acquired in real time by a high-speed data acquisition system (DAQ). The system dedicated to the 6-component balance consisted of one PXI card with 32 analog input channels of 16-bit resolution and 625 Ks/s sampling frequency, along with a PXI controller to record and process the data. Data from the PXI controller were processed via software implemented with a MATLAB code after completion of each test to display the frequency domain distribution of the measured aerodynamic forces. Another PC is devoted to controlling and monitoring the servo actuator through data sent over an RS-232 serial interface with a 5 ms interval.

![Diagram](image)

**Figure 4.** The process of the ground test.

2.3. CFD Settings

As previously described within Section 1, the double-precision SST \( k-\omega \) turbulence model [26,27] was selected to solve the incompressible unsteady RANS (Reynolds-averaged Navier–Stokes) governing equations [28], as illustrated in Equation (1).

\[
\begin{align*}
\frac{\partial \mathbf{u}}{\partial t} + \frac{\partial}{\partial x_j}(\mathbf{u} u_j) &= -\frac{1}{\rho} \frac{\partial p}{\partial x_j} + \frac{\partial \sigma_{ij}}{\partial x_j} + \tau_{ij}, \\
\mathbf{u} &= (u_1, u_2, u_3)
\end{align*}
\]
where $t$ is time, the $\rho$ and $p$ donate the air density and air pressure, respectively; The Reynolds stress tensor $\tau_{ij}$ in the viscous flow is defined by Stokes’s assumption. Equation (2) is mathematical representation of $\sigma_{ij}$.

$$\sigma_{ij} = \mu \left( \frac{\partial u_i}{\partial x_j} + \frac{\partial u_j}{\partial x_i} - \delta_{ij} \frac{\partial u_l}{\partial x_l} \right)$$ \hspace{1cm} (2)

where $u_i$, $u_j$ and $u_l$ represent the freestream velocity vector in a Cartesian coordinate system. The viscosity coefficient and Kronecker delt are denoted by $\mu$ and $\delta_{ij}$.

$$\mu = \mu_l + \mu_t$$ \hspace{1cm} (3)

where $\mu_l$ and $\mu_t$ stands for the molecular laminar viscosity and turbulent eddy viscosity, respectively.

The SST $k-\omega$ turbulence model is a widely used two-equation turbulence model, which has great advantages in simulations of rotating flows [29–31]. The finite volume method based on unstructured grids was used to discretely solve the above equations, in which the coupled algorithm was used for the pressure–velocity coupling. The second-order upwind scheme was used for the spatial discretization method. Dual-time discrete and advancing unsteady calculations were performed with the implicit LU-SGS method. Each time step contains a maximum of 20 sub-iterations and a residual reduction of 5 orders of magnitude to ensure the convergence of the solutions. In the unsteady calculation process, a time step size corresponds to the physical time required for the propeller to rotate 2 deg [24], so 180 time steps were needed to simulate a complete propeller revolution.

Figure 5 depicts the schematic diagram of the pitch motion, which is controlled by some basic definitions: the origin rotation center of propeller is $O(-10 \text{ mm}, 0, 0)$ and the center of pitch motion is consistent with the direction of the freestream, directly behind the propeller, named $P$. The propeller creates the pitch motion around the $Z$ axis.

The pitch motion can be expressed as:

$$A = A_0 \cdot \sin(2\pi ft)$$ \hspace{1cm} (4)

where $A_0$ and $f$ are the amplitude and frequency of the pitch motion, respectively. The tangential velocity of the pitch motion can be described as follows:

$$V = L \cdot A_0 \cdot 2\pi ft \cdot \cos(2\pi ft)$$ \hspace{1cm} (5)

where $L$ is relative length from the leading edge of wing to the rotation center of the propeller.

The user defined function was used in the specified propeller motion control equation, including the rotation and pitch motion. According to Equations (1) and (2), the coordinate of the rotation center $O$, the vector of the rotation axis $PO$ and the velocity of the propeller $V_o$ at any time can be written as:

$$O(x, y, z) = (L - 0.01 - L \cdot \cos A, L \cdot \sin A, 0)$$ \hspace{1cm} (6)

$$PO = (-L \cdot \cos A - 0.01, L \cdot \sin A, 0)$$ \hspace{1cm} (7)

$$V_o = (V \cdot \sin A, V \cdot \cos A, 0)$$ \hspace{1cm} (8)
In order to formulate the movement of the propeller following the wing’s vibration, the aerodynamic modeling of the propeller in the present work used the OMM (overset mesh method). In the past, many researchers have used this method to analyze the aerodynamic performance of the propeller in both isolated propeller and propeller–wing aerodynamic interference situations [20,32,33]. Due to its excellent dynamic performance, OMM allows for the analysis of the aerodynamic performance of a rotated propeller coupled with pitch motion. In OMM, the information is exchanged across the overset interface between the background and component domains. The mesh dimension of the background and component domains should be kept as consistent as possible in order to reduce calculation errors [34].

Figure 6 illustrates the geometry dimension and boundary conditions of the computational domain. It has two different domains, and they are generated individually. One is a conventional cylindrical external stationary domain with a radius of 18 \( R \), which is composed of the inlet, outlet, and far-field. The distance from the center of the rotation of the isolated propeller to the inlet boundary is 18 \( R \) and 72 \( R \) to the outlet boundary. These offsets from the propeller guarantee that the field space around the blade is not perturbed by the boundary conditions on the external boundaries. The other domain, recorded as a rotation domain with a radius of 1.2 \( R \) to enclose the propeller, contains the propeller and a small cylinder with thickness. A solid half-shuttle hub was created from the inflow to the outflow. It is worth noting that a transition region was generated in the computational domain, where the mesh was refined. This not only made the mesh size of the background and component domains as uniform as possible, but also reduced the number of meshes for the background domain.

The boundary conditions for the entire simulations were as follows:

(a) The velocity inlet for the inlet boundary was established via the freestream angle of attack (\( \alpha \)) and freestream velocity of \( V_\infty \). In the present work, \( V_\infty = 0 \) m/s stands for the static conditions. However, when the freestream angle of attack and freestream velocity are present in the calculation, as shown in Section 2.2.1, the freestream velocity was decomposed into \( V_x \), \( V_y \), and \( V_z \).

(b) The condition of the pressure outlet was set at the domain of the far-field and outlet, where the operating pressure was set to \( 1.01 \times 10^{-5} \) Pa.

(c) The solid surface of the propeller and half-shuttle hub were modeled as a stationary wall with no-slip boundary condition, that is to say, the normal and tangential velocities between solid surface and inflow were 0 m/s.
Figure 6. Schematic view: (a) 2-D diagram of the entire computational domain; (b) boundaries and boundary conditions of the CFD settings.

The grid generation was carried out in ANSYS ICEM software. Presented in Figure 7a is an O-grid used for the entire computational domain (background mesh). In addition to this, the propeller rotation domain and surface were modeled with structured mesh, and the mesh topology is shown in Figure 7. In order to restore the highly twisted surface of the blade, it can be noted that the surface of the propeller was divided into 14 regions along the radial direction. The height of the first layer of the grid near the propeller wall was set to \(1 \times 10^{-5}\) m. On this basis, a 19-layer mesh was generated through O-block topology with a height ratio of 1.2, and finally, a rotation domain enclosing the propeller was generated. The averaged wall \(y^+\) value of medium mesh model was less than one. Additionally, in order to capture the development of the blade tip vortices and improve the resolution of the flow simulation, the mesh was refined toward the leading and trailing edges of the blade and blade tip, as shown in Figure 8a. Especially at the area of blade tip, the O-block topology was applied once again, which improved the quality of the blade mesh. Figure 8b presents a close-up view of the propeller boundary layer mesh at 0.7 R. During the simulation process, the grid in the rotation domain performed rigid body rotation motion in this area to simulate the rotation of the propeller. When performing the mesh independence verification in the next section, the \(y^+\) and growth rate near the blade wall remained unchanged, and the total amount of meshes in the propeller rotation area was changed by changing the number of grid nodes in the chord and span directions of the propeller blade.
2.3.1. Mesh Convergence Study

In order to verify the irrelevance of an isolated propeller mesh size to the calculation results, three sets of isolated propeller models with different mesh sizes were generated (a coarse mesh model; a baseline mesh model; a fine mesh model), as shown in Table 1 and Figure 9. The calculation conditions of the verification of mesh convergence were as follows: the freestream velocity was 12 m/s, the freestream angle of attack was 2 deg, and the propeller rotational speed were set to 6000 rpm (revolutions per minute), 7000 rpm, and 8000 rpm. Sea-level conditions were assumed, with the reference velocity and length selected as the tangential velocity at three different rotational speeds, and a chord length of the propeller at \( r/R = 70\% \), where the corresponding Reynolds number was based on \( 9.7 \times 10^4 \), \( 1.1 \times 10^5 \), and \( 1.3 \times 10^5 \), respectively.

Table 1. Mesh convergence study of the isolated propeller.

<table>
<thead>
<tr>
<th>Mesh Model</th>
<th>Total Cell Number/10^4</th>
<th>Node Number along the Radius</th>
<th>Node Number along the Chord</th>
</tr>
</thead>
<tbody>
<tr>
<td>Coarse</td>
<td>309</td>
<td>115</td>
<td>20</td>
</tr>
<tr>
<td>Baseline</td>
<td>441</td>
<td>252</td>
<td>30</td>
</tr>
<tr>
<td>Fine</td>
<td>610</td>
<td>340</td>
<td>40</td>
</tr>
</tbody>
</table>
In Figure 10, the aerodynamic characteristics of the time-averaged thrust and torque of an isolated propeller of the three mesh models at different rotational speeds are graphically displayed. It can be noted that the time-averaged aerodynamic performance increases with the increase in rotational speed, and with the increase in the mesh size, the gap between the time-averaged aerodynamic characteristic values gradually narrows. Between the baseline mesh and fine mesh models, there was a difference of 1.69 million grids, but the simulated propeller thrust increased by only 0.515%, 0.461%, and 0.439%, respectively, under the three rotational speeds. The difference between the coarse mesh and baseline mesh models was only 1.32 million, but the propeller thrust increased by 3.724%, 3.965%, and 4.168% under three different rotational speeds, respectively. The instantaneous values of the single blade thrust with different mesh models in two full revolutions are shown in Figure 11, where the thrust amplitude and peak values indicate the baseline mesh model will be the suitable choice. In addition, the cost was calculated for the three sets of simulation models under the same working conditions at 8, 12, and 19 h. Therefore, considering the calculation accuracy and calculation cost, all the subsequent CFD simulations in this paper were conducted with the baseline mesh model.
Although the SST $k$-$\omega$ turbulence model has been proven to accurately capture the unsteady aerodynamic performance of the rotating propeller, several additional simulations were conducted to determine the effect of the turbulence models on the CFD simulation results. For this investigation, the S-A and laminar models were selected. The relatively simple S-A model was chosen due to its wide application in the aerospace engineering field, and the laminar model was chosen in order to illustrate the separations and rotating slipstreams of the propeller. A comparison of the pressure distribution located at the $r/R = 0.7$ station can be seen in Figure 12, in which the baseline mesh model was selected for simulation. For the fixed revolution-per-minute simulation, a similar trend in the pressure distribution on the blade surface was observed across the slices. However, for the result obtained from the 3-D laminar model, a greater range of low-pressure and high-pressure regions were present on the blade surface; therefore, the thrust force was greater than seen from the remaining two cases, which coincides with the phenomenon in Figure 13a. However, from Figure 13c, a significant difference for torque simulation was found between S-A and SST $k$-$\omega$ turbulence models. Comparing the relative values of the three simulation cases in Figures 12 and 13, and based on other scholars’ experiences [29–31], the SST $k$-$\omega$ turbulence model performed best with unsteady aerodynamic performance observed.
2.3.2. Validations

In this section, the propeller thrust and torque from a propeller-wing static test for the rotational speeds of 6000 rpm, 7000 rpm, and 8000 rpm were used to evaluate the accuracy of the CFD method. Figure 14 compares the thrust and torque of the propeller obtained from the experiment and the CFD simulation. The thrust results agree well with the static ground test in the case of 6000 rpm and 7000 rpm, and reasonably well, with some discrepancies, in the case of 8000 rpm. The maximum propeller thrust difference between the test and CFD simulation was 3.52%. A possible physical source of this difference is that the 6-component balance is affected by the temperature and mechanical vibration when the propeller rotates at a higher speed. The corresponding difference in propeller torque at 8000 rpm is 4.44% and the difference is almost ignored at 7000 rpm. Therefore, the results of the static test indicate that the CFD method and simulated strategy used in this paper can evaluate the aerodynamic performance of the propeller accurately.

3. Results and Discussion

Six static ground test data points were used to assess the CFD numerical results. These data points, highlighted in Table 2, were split into the effects of frequency, amplitude, and $L$. $L$ is the relative length from the leading edge of the wing to the rotation center of the propeller.
Table 2. Data points of the selected static ground test.

<p>| | | | | |</p>
<table>
<thead>
<tr>
<th></th>
<th></th>
<th></th>
<th></th>
<th></th>
</tr>
</thead>
<tbody>
<tr>
<td>$V_{\infty}$/m/s</td>
<td>rpm</td>
<td>Frequency/Hz</td>
<td>Amplitude/deg</td>
<td>L/mm</td>
</tr>
<tr>
<td>CASE-1</td>
<td>0</td>
<td>8000</td>
<td>3</td>
<td>4</td>
</tr>
<tr>
<td>CASE-2</td>
<td>0</td>
<td>8000</td>
<td>5</td>
<td>3</td>
</tr>
<tr>
<td>CASE-3</td>
<td>0</td>
<td>8000</td>
<td>5</td>
<td>4</td>
</tr>
</tbody>
</table>

3.1. Effect of the Pitch Frequency

This section studies the influence of pitching motion frequency on the unsteady follower aerodynamic characteristics of the propeller, and is divided into the case of freestream velocity $V_{\infty} = 0$ m/s and freestream velocity $V_{\infty} \neq 0$ m/s. The rotational speed was 8000 rpm, the length $L$ was 330 mm, the amplitude of the pitch motion was 5 deg, and the frequencies of pitch motion were set to 3 Hz, 4 Hz, and 5 Hz. The frequency of the pitch motion simulates the structural torsional mode of the wing. In addition to this, a regular fixed-point rotation case was designed for comparison with the unsteady aerodynamic performance of coupled motion that was the focus of the present work.

Figure 15 depicts the comparison of the unsteady aerodynamic force along the X-axis of an isolated propeller at different pitch frequencies and regular case. It can be observed that, although there is no freestream, the propeller force also presents periodic fluctuations. In terms of the force magnitude, the peaks and troughs in the amplitudes between two adjacent waves are not the same. The variation of amplitude (max and min values) with azimuth angle was found in Figure 15a,b, and this correlates with the pitch motion. This is completely different from the results obtained from the regular case without pitch motion (Figure 15c), in which the fluctuation amplitudes were almost identical. The underlying reason is that the local angle of attack and local velocity of the propeller can change at any moment, driven by the pitch and rotation motion. It is clear that the difference between coupled motion and the regular case provides guidance for studying the dynamic aerodynamic performance of a rotor UAV or a fixed-wing UAV after encountering gust. It can be concluded from Figure 15 that the number of fluctuations is independent of the pitch frequency, because no matter what the pitch frequency is, the thrust of the propeller along the X-axis fluctuates four times in a rotational period. According to what was mentioned above, the performance of the propeller follower force is highly dependent on the coupling property of the pitch motion and rotation motion.
Figure 15. Comparison of the unsteady aerodynamic force along the X-axis in several rotation revolutions: (a) coupled motion: 0–1080 deg; (b) coupled motion: 1080–2160 deg; (c) only fixed-point rotation motion: 0–1080 deg.

As shown in Figure 5, the propeller force $T$ along the X-axis at each moment is decomposed into the force perpendicular to the propeller disk, named out-plane force, $T_{out}$, and the force parallel to the propeller disc, named in-plane force, $T_{in}$. The response of the in-plane force, $T_{in}$, with time is displayed in Figure 16. As is clearly visible, the full propeller, 1-blade, and 2-blade (1-blade represents the NO.1-blade and 2-blade represents the NO.2-blade for a two-bladed propeller) have exactly the same fluctuation period related to pitch motion, and this fluctuation period only depends on the pitching frequency, which potentially changes the aerodynamic performance of the 1P-force (in-plane propeller shaft force). Taking the pitching frequency of 5 Hz as an example, within 0.5 s of the physical time, the propeller completed 2.5 pitching cycles. This coincides with the force $T_{in}$ fluctuation period presented in Figure 16b. From Figure 16a, b, the mean value of in-plane force related to pitch motion equals to zero. However, a significant difference was found that the corresponding value related to rotation motion does not equal to zero. Figure 16a, b also shows that the $T_{in}$ of 1-blade and 2-blade have the same fluctuation amplitude. For the fixed-point rotation case (Figure 16c), the in-plane force amplitude for each blade oscillates at a frequency of 133.33 per revolution around a constant mean value of zero with a constant periodic amplitude. As can be seen for both CFD computations, periodic fluctuation of in-plane force was present for all simulation cases. However, once the propeller is driven by the pitch motion, there is an angle between the propeller rotation axis and the slipstream behind the propeller disk at all points, so the 1P-force curve demonstrates more complex unsteady periodic fluctuations than the regular one, which may be a major discovery for the study of 1P-force.
For the pitch frequency $f = 5\text{ Hz}$, the $T_{\text{out}}$ time domain response of the 1-blade and 2-blade in a complete pitch oscillation cycle is presented in Figure 17. For each blade, the $T_{\text{out}}$ experienced two large fluctuations overall during a completed pitching oscillation process, where the blade force evolved from minimum to maximum per half-cycle. The simulation data show that, when the blade moves to the position of maximum amplitude $A_0$, that is, when it moves from equilibrium position to 5 deg or −5 deg, the largest blade force is clearly observed. The reason might be that there is an angle between the direction of propeller slipstream and rotation axis, and the angle gradually increases; therefore, the blade force is found to increase at the same time. However, the velocity of pitch motion decreases to 0 m/s when the propeller reaches its maximum amplitude. This indicates that the sum local velocity of the propeller reaches the minimum, and this correlates with a decrease in blade force. So, the largest blade force is not observed at the position of maximum amplitude. Theoretically, in one pitch period (0.2 s), the propeller should complete 27 rotating revolutions, which is consistent with the number of fluctuation peaks for each single blade shown in Figure 17, and the result further proves the reliability of the CFD simulation. Additionally, the out-plane force of the two blades rotates at the same frequency, but there is also a phase difference of 180 deg between the fluctuation curves.

The response of the time and frequency domains of the out-plane force obtained from the ground test are shown in Figure 18. The results for both conditions show the significant effect of pitch motion frequency on the out-plane force of the propeller. The aerodynamic fluctuation frequency is consistent with the pitch motion frequency, which is in good agreement with Figure 17. However, for the case of 3 Hz-5 deg, the time-averaged
thrust difference between the ground test and CFD is 4.765%, and the difference for the torque is 1.085%. When the pitch frequency improves to 5 Hz, the corresponding difference in the thrust and torque is 6.618% and 1.961%, respectively. As can be seen from the results, there is a significant variance between the low frequency and high frequency operating conditions. Fortunately, the reason might be found in the feedback signal from the controller and the recorded test video. According to the prompt of the controller, the working condition of 5 Hz-5 deg is actually almost the same as that of 5 Hz-4.2 deg, where the differences of propeller thrust and torque are reduced to less than 3.50% by comparing the ground test and CFD results. Additionally, from the test videos, it is clearly observed that the structure vibration is more severe when the test conditions are at high frequencies, which may affect the measurement of the propeller loadings. As a further verification work, an analysis of the structural mode of the entire mechanism will be carried out in future work.

Figure 18b shows that a force level appears at the main frequency of 3 Hz, and the frequency is exactly equal to the pitch motion, indicating the measurement equipment and servo actuator are working properly, whereas in Figure 18d, non-negligible peaks occur at the harmonic frequencies (10 Hz and 15 Hz). The reason why there are obvious peaks in the frequencies plot might be the aerodynamic nonlinearity characteristics for rotating propeller and structural nonlinearity, such as structure gaps. The results show that the ground test data not only reflect the frequency of pitch motion, but also show the rotational frequency of the propeller rotation, which is consistent with the information described in Figure 17.

Figure 18. The response of the ground test out-plane force for difference frequencies: (a) time-domain response for the case of 3 Hz-5 deg; (b) frequency-domain response for the case of 3 Hz-5 deg; (c) time-domain response for the case of 5 Hz-5 deg; (d) frequency-domain response for the case of 5 Hz-5 deg.
Figure 19 shows the distribution of the pressure contours on the downstream section of the propeller when the propeller completed 52 full revolutions under the pitching motion with the frequencies of 3 Hz and 5 Hz. The corresponding time stamp in pitch motion was 52× cycle, which was conducted to assess the local changes to the blades loading. It can be observed that the high-pressure region appears at the 70–100% R of the blade. Compared with the blade with no pitch motion, the blade tip at the pitched state has a more evident high-pressure region. This shows that the propeller can absorb more energy from the flow field and, ultimately, generate larger blade loads. Although the phase angle of the two blades presented in this paper are the same, the contours of pressure distribution on the sliced plane do not coincide in reality. From the pitch cycle, it can be deduced that the spatial location of the propeller shown in Figure 19a moves away from the equilibrium position at this time, and the relative pressure magnitude of the two blades has two different behaviors: one larger and one smaller. However, the spatial location shown in Figure 19b approaches the equilibrium position, and at this moment, the pressure distribution of the two blades is more consistent. This also verifies the phenomenon shown in Figure 17: the more severe amplitude of blade load changes is clearly found as the propeller oscillates to the position of maximum amplitude, and the corresponding value is lower near the equilibrium position.

Figure 19. Pressure distribution behind the propeller: (a) f = 3 Hz; (b) f = 5 Hz.

Figure 20 shows the influence of the pitch frequency on the torque produced by a single blade in the direction of the rotation axis. In Figure 20a, it can be seen that the difference in the value of torque affected by the two pitch frequencies is not prominent, but the number of fluctuations generated in the same time period is obviously related to the frequency. As is clearly visible, the torque absolute value of torque is the smallest when the propeller is at n × 1/2 cycle. When the propeller moves to the maximum amplitude, which is called the (2n + 1) × 1/4 period, the torque value is the largest. The underlying reason might be that the force arm is the longest at maximum amplitude. Therefore, the blades take the largest torque on the X-axis in this state. Presented in Figure 20b is the time domain response of the torque of the 1-blade and 2-blade when the pitch frequency is f = 5 Hz. A similar response was found for the two blades; however, there is an obvious phase difference in the torque generated by a single blade. After observing the change curve around the n × 1/2 period, it was found that there is no phase difference between the change in 1-blade and 2-blade; in fact, the change trend is completely opposite. Figure 20c,d shows the response of the torque generated by two blades around the n × 1/2 period with time when pitch frequency is f = 3 Hz and 5 Hz, respectively. Within a specified period of time, the two blades assume different trends in the direction of the rotation axis, and the curves are just opposite. The blades are in a very short process from the +Y direction to the −Y direction through the equilibrium position. At this time, it may be
considered that the 1P-force of the two blades in the Y direction are equal, but the directions are opposite.

\[ f = 3 \text{ Hz}; 1\text{-blade} \]
\[ f = 5 \text{ Hz}; 1\text{-blade} \]
\[ f = 3 \text{ Hz}; 1\text{-blade} \]
\[ f = 5 \text{ Hz}; 1\text{-blade} \]

Figure 20. Influence of the pitch frequency on the time domain response of the torque: (a) for 1-blade, \( f = 3 \) Hz and 5 Hz; (b) for 1-blade and 2-blade, \( f = 5 \) Hz; (c) for 1-blade and 2-blade within a specified period of time, \( f = 3 \) Hz; (d) for 1-blade and 2-blade within a specified period of time, \( f = 5 \) Hz.

In the present study, an investigation of the unsteady follower force of the propeller at identical pitch frequencies was conducted when the velocity freestream was not equal to 0 m/s. The velocity freestream of 18 m/s, angle of attack of 0 deg, rotational speed of 8000 rpm, and pitch amplitude of 5 deg were set as the default values, while the two different pitch frequencies were set as 3 Hz and 5 Hz. As previously described, another two sets of the regular case were calculated based upon the different angles of attack, 0 deg and 2 deg, respectively. A comparison of the out-plane force of 1-blade at the frequencies of 3 Hz and 5 Hz is plotted in Figure 21a, where a significant difference of fluctuation amplitude is found between the two cases. The local velocity of the blade is equal to the sum of the freestream velocity, the propeller rotational speed and the velocity of pitch motion. It is well known that the large pitch frequency corresponds to the larger velocity of pitch motion. In addition, the maximum and minimum value occur at the same phase angle for the frequencies of 3 Hz and 5 Hz, which means the only factor determining the magnitude of the fluctuation is the pitch frequency. Therefore, the blade force corresponding to the pitch of 5 Hz is larger than that of 3 Hz. Based upon our knowledge and experience, it is clear that the fluctuation amplitude of force has a significant influence on structure design and aeroelasticity design for a UAV. In terms of the most previous studies, hot points were focused on the unsteady aerodynamic performance generated by the fixed-point rotation of a propeller in a given inflow, as shown in Figure 21c,d. Looking at
the out-plane force, an oscillating response of 6.67 revolutions within 0.5 s around a constant mean value was observed for the case of 2 deg, whereas the corresponding value of 0 deg remained unchanged. Although the angle of attack of the freestream was 0 deg, the coupled motion results in increased fluctuation of the propeller surface loads, causing variations in the periodic amplitude of the out-plane force response. However, for the torque performance, the fluctuation amplitude corresponding to the two frequencies was equal due to the sum of local angle of attack relative to the rotation axis and angle of pitch motion (Figure 5) maintained equality at the same spatial location.

In Figure 21a, it is clear that the fluctuation period of out-plane force still related to the frequency of pitch motion. Within every 1/2 period, the blade force corresponding to 5 Hz fluctuates 13 times, and the value is 22 times for 3 Hz. Naturally, it can be concluded that the number of fluctuations for the unsteady follower force is related to the pitch and rotation frequencies. The 1/2 period (0.167 s) of the 3 Hz-pitch motion is shown as a black vertical dash line, and the 1/2 period (0.1 s) of the 5 Hz-pitch motion is plotted as a red vertical dash line. The dash line indicates that the maximum and minimum values of the time response curve of the out-plane force of the 1-blade corresponding to the two pitching frequencies do not appear at the equilibrium position or the maximum amplitude, which is exactly the opposite of the trend obtained from the static conditions. However, the max (min) torque in Figure 21b remains at the equilibrium position or the location of maximum amplitude.

![Figure 21](image)

**Figure 21.** The loads time domain response of the 1-blade: (a) out-plane force of coupled motion; (b) torque of coupled motion; (c) out-plane force of fixed-point rotation motion; (d) torque of fixed-point rotation motion.

Figure 22 presents the 2-D and 3-D flow structure for the static simulation and freestream exited simulation cases. During the static case, the pressure difference that was
more significant led to an enhanced flow around the blade tip; as a result, a reverse circulation phenomenon was observed in Figure 22a, and this unsteady aerodynamic performance was common when a rotary-drone was in hovering mode. However, it was found that the rotating streamlines behind the propeller were extended and the flow field changed more smoothly when it exited freestream, as shown in Figure 22b. Therefore, the force time domain response of each blade is more regular (Figure 21) compared with the static case (Figure 17). In addition to this, the flow around the blade tip was weakened due to the lower pressure difference.

![Figure 22](image)

**Figure 22.** The flow structure: (a) 2D: static case; (b) 2D: freestream exited case; (c) 3D: static case; (d) 3D: freestream exited case.

### 3.2. Effect of the Pitch Amplitude

In order to investigate the influence of the pitch amplitude in detail, three working conditions were set in this section, namely, 3 deg, 4 deg, and 5 deg. The common calculation conditions for the three cases were as follows: the velocity of freestream and propeller rotational speed were kept at 0 m/s and 8000 rpm, respectively, and the pitch frequency was 5 Hz. Figure 23 plots a comparison of the effect of the three pitch amplitudes on the out-plane force, $T_{out}$, and in-plane force, $T_{in}$, responses of the 1-blade. Although the fluctuation curves have a similar tendency, the fluctuation amplitude of the blade force, which is of greater concern for this paper, is clearly different depending on the three pitch amplitudes. For the case with the pitch amplitude of $A = 5$ deg, this is reflected in a clear increase in the peak and the load on the 1-blade, which is the largest increase. The larger the amplitude, the greater the translational velocity of the propeller that is obtained according to Equation (2), and the slipstream velocity behind the propeller increases. Therefore, the out-plane force $T_{out}$ of the propeller increases with the increase in pitch amplitude. The in-plane force $T_{in}$ response curves under different amplitude conditions are plotted in Figure 23b, which shows that the in-plane force $T_{in}$ is also dependent on the amplitude of
the pitch motion, but the oscillation frequency of the in-plane force $T_{in}$ is equal to the pitch frequency. The maximum or minimum amplitude appears at the place with $(2n + 1) \times 1/4$ period. This is a consequence of the fact that, when the propeller moves to the maximum angle in the $+Y$ direction, the in-plane load has the maximum positive value. When the propeller moves to the maximum angle in the $-Y$ direction, the in-plane load has the minimum negative value.

![Figure 23. Comparison of the effect of the pitch amplitudes on the $T_{out}$ and $T_{in}$ time domain response of the 1-blade: (a) $T_{out}$; (b) $T_{in}$.](image)

Figure 24 presents the specific analysis of the pitch-amplitudes effect on the torque variation trend along the rotation axis, which is a frequency based on the pitch frequency and rotational speed of the propeller. The figure clearly shows that, when the amplitude of the pitch angle is 5 deg, the propeller is subject to a significantly larger torque than that when the amplitude of pitch angle is 3 deg or 4 deg. When the system operates at different pitch angle amplitudes, the distance between the propeller and X-axis is not equal at identical times, which leads to an unequal force arm. The larger the amplitude of the pitch angle, the longer the moment arm, so there is a positive correlation between the torque and the pitch length. The comparison of the first two rows of the out-plane force and torque of the propeller listed in Table 3 shows a good agreement between the static tests and CFD simulations. The results of the 6-component balance corresponding to the test are time-averaged. For the case of 5 Hz-3 deg, the time-averaged propeller thrust difference between ground test and CFD was 3.013%. Although this case was also a high-frequency pitch motion, due to the lower pitch motion amplitude, the thrust difference between the test measurement and CFD simulation was reduced. By contrast, for the case of 5 Hz-5 deg, the difference increases. Based on this investigation, it is clear that a proper design point of the pitch motion amplitude is an essential requirement of the CFD analysis to address the issues of follower force and vibrations for propeller propulsion systems.
3.3. Effect of the Relative Length $L$

It is well known that the propeller placement with respect to the wing is a key design point for a propeller aircraft. In turn, the relative length from wing to the propeller might influence both the follower aerodynamic performance of the propeller and the torque budget for the aircraft [16,35]. Therefore, in this section, the relative length $L$ from the wing’s leading edge to the rotation center of the propeller was considered as a variable, and the values were 230 mm, 280 mm, 330 mm, and 380 mm, denoted as $L_{230}$, $L_{280}$, $L_{330}$, and $L_{380}$, respectively. Figure 25 presents a schematic diagram of the pitching motion of the propeller around different pitch centers. The simulations were conducted at a propeller rotational speed of 8000 rpm, a pitch frequency of 5 Hz, a pitch amplitude of 5 deg, and a freestream velocity of 0 m/s.

A quantitative comparison of the response of the total propeller thrust over a period of time under the conditions of the three relative lengths is plotted in Figure 26a, which shows that the total thrust of propeller corresponding to the four relative lengths is essentially equal and has the same fluctuation period. The relative length $L$ has no obvious effect on the propeller thrust. On the contrary, as shown in Figure 26b, the amplitude of the out-plane force is positively correlated with $L$ which is greatly meaningful for studying the vibration of the UAV. Interestingly, the propeller out-plane force values in Table 4 show that the force decreases with the increase in the relative length. In the current
measurement, the force was further overestimated in comparison to CFD. This might be due to the dissipation of structural vibration, whereas the CFD simulation presents the ideal environment.

The effect of $L$ on the 1-blade and 2-blade force along the rotation axis plotted in Figure 27 shows that the four relative lengths have no obvious influence on the variation trend of the torque, and almost the same fluctuation center in time domain response for four curves is found, which is also reflected in Table 4. That is to say, the time-averaged torque obtained from the static ground test remains almost unchanged as the relative length $L$ increases. A similar torque response is observed in the CFD simulations. In terms of the more concerned magnitude of the vibration amplitude, as shown in Figure 27, a significant effect can be seen on the amplitude, and the larger magnitude of the torque is obtained by increasing the relative length. This is due to the fact that, although there is the amplitude of pitch motion is identical, the actual distance between the rotation axis of the propeller and the X-axis are not equal at the same moment for the different pitch lengths. The greatest value of the torque for the CFD simulation was found in the $L_{380}$ simulation case.

![Figure 26. Variation curves of propeller thrust corresponding to the four values of relative lengths.](image)

![Figure 27. Influence of the relative length on the torque time domain response of each blade: (a) 1-blade; (b) 2-blade.](image)
Table 4. Comparison between the ground test and the CFD simulation results under the different relative lengths.

<table>
<thead>
<tr>
<th>Working Conditions</th>
<th>Out-Plane Force/N</th>
<th>Torque/N-m</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Ground Test</td>
<td>CFD</td>
</tr>
<tr>
<td>230-5Hz-4°</td>
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<td>20.018</td>
</tr>
<tr>
<td>280-5Hz-4°</td>
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<td>20.016</td>
</tr>
<tr>
<td>330-5Hz-4°</td>
<td>20.693</td>
<td>20.015</td>
</tr>
</tbody>
</table>

Figure 28 shows a comparison of the averaged velocity contours in the X direction and Y direction when the propeller completes the 27 rotation revolutions for the downstream location (X/R = 0.03). In Figure 28a,b, the CFD results show that the distribution of the axial averaged velocity behind the propeller has no obvious relationship with the relative pitch length. It is well known that the velocity in the X direction is mainly caused by the propeller rotating at a high-speed, which is closely related to the rpm of the propeller. The selected transient case shows a wider high-velocity region along the span of the blade in the X direction, indicating that the blade loads are larger. For the distribution of the Y velocity in the red box plotted in Figure 28b,c, more notable differences were found. In this case, the CFD result shows a sliced plane that locates at the downstream of the propeller disk, and the blades also have a lateral speed of motion, which directly depends on the pitch length, thus causing the difference in the mean velocity in the Y direction. This could also explain why the pitch length mainly affects the laterally related aerodynamic loads, which again shows the influence of the relative length on the propeller torque, as presented in Figure 26.

![Figure 28](image_url)
Nevertheless, the excellent agreement between the ground test and the CFD results, particularly in terms of the calculation accuracy and research cost, is an encouraging sign that the CFD numerical simulations are in fact a useful approach to explore in detail the challenging aspects of the unsteady follower force and aerodynamic performance of propellers in detail. However, for such complex coupled motion adequate validation with appropriate ground and wind tunnel experiments remains necessary.

4. Conclusions

A series of investigations incorporating both a ground test and numerical simulation were conducted in this paper, with the primary goal of shedding light on the effect of pitch motion on the unsteady follower aerodynamic performance of a UAV propeller. An actuator servo system drove the propeller to achieve pitch motion, and a 6-component balance was designed to collect high-quality instantaneous follower forces on the blades. The CFD simulation results are in good agreement with the results obtained from the ground test, except for the working conditions of 5 Hz-5 deg. This might be due to the loss of performance of servo actuator during high frequency-amplitude movement; therefore, this highlighted the need to improve the capabilities of the experimental system for propeller loads predictions. It was proven that the overset mesh method is sufficient to describe the specified motion and can capture the propeller loads accurately.

The frequency, amplitude of pitch motion, and propeller placement with respect to the wing, have significant impacts on the form and amplitude of follower forces. An outstanding feature was that the performance of the follower force possessed the rotating motion and pitch motion frequencies. This directly leads to changes in the 1P-force compared with a regular fixed-point rotating case, where the amplitude was found to be approximately 1.662 times of the regular one. Although similar profiles were observed, the oscillation amplitude of torque increased by 33.328% (5 Hz-4 deg) and 66.542% (5 Hz-5 deg) compared with the case of 5 Hz-3 deg. As freestream velocity existed, it was clear that the location of max and min value of out-plane force changed during the coupled motion process, whereas the corresponding location of torque remained the same.

In general, the major limitations of the present study are the ground test instruments used to collect our data due to a high probability of structural nonlinearity, such as structure gaps. In addition to this, the selection of the turbulence models and the setting of the boundary conditions can also have an influence on the CFD simulation results. Future research should be undertaken to explore the unsteady follower aerodynamic performance under the influence of a uniform freestream and gust in a low-speed wind tunnel experiment.

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