Numerical investigation of vortex dynamics in an H-rotor vertical axis wind turbine

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We study the vortex dynamics of a two-dimensional H-rotor wind turbine using a Navier-Stokes solver. The k-\(\varepsilon\) turbulence model with the wall function is used as the turbulence closure. A sliding mesh technique is employed to handle the blade rotation. The vortex-blade interaction is systematically investigated and its influence on the force generation is discussed. Our simulations show that the vortex-blade interaction largely depends on the solidity and tip speed ratio. We further study the impact of solidity on the turbine performance. Our simulations show that the peak torque per blade decreases with the solidity while the peak torque azimuthal angle increases with the solidity. Our simulations also show that the increase in the azimuthal angle is more significant at low tip speed ratios than at high tip speed ratios. The impact of blade thickness is studied. Our simulations show that a thicker airfoil has a higher torque coefficient than a thinner airfoil. However, because for the thinner airfoil its peak torque occurs at a high tip speed ratio, the thinner airfoil has an overall higher power coefficient than the thicker airfoil.

Keywords: H-rotor; CFD; vortex dynamics; dynamic stall

1. Introduction

Wind powered energy generation has experienced tremendous growth worldwide and it is considered as an environmentally friendly and economically competitive means of electric power generation. World Wind Energy reported that by the end of 2010 all installed wind turbines can generate 430 terawatthours per year, equaling 2.5% of the global electricity consumption (World Wind Energy Report, 2010). Wind energy is generated using energy conversion system to convert kinetic energy from the wind into electricity. Wind energy conversion systems generally can be categorized into two groups based on the orientation of their rotation axis: the horizontal axis wind turbine (HAWT), and the vertical axis wind turbine (VAWT). The VAWT mainly has three kinds: the Savonius, the Darrieus, and the H-rotor. Figure 1 shows one horizontal axis wind turbine and three vertical axis wind turbines.

The VAWT presents several advantages over the HAWT. First, the VAWT has low sound emission due to its low operating speed (Simão Ferreira, Van Zuijlen, Bijl, Van Bussel, & Van Kuik, 2010). Recent studies (Liu, Ouyang, Wu, Tian, & Du, 2014) are putting efforts on reducing noise of the rotor. Iida, Mizuno, and Fukudome (2004) predicts that the noise level of VAWTs is about 70 dB which is 10 dB lower than HAWTs. Second, the VAWT is insensitive to the wind direction (Eriks-son, Bernhoff, & Leijon, 2008). Third, the VAWT has increased power output in skewed flows while the HAWT has decreased power output, making the VAWT more favorable on top of a roof. Mertens, Van Kuik, and Van Bussel (2003) numerically studied the performance of H-rotors in skewed flows. In their work computational fluid dynamics (CFD) was used to calculate the wind velocity over a rectangular model building. The calculated velocity was passed to a low fidelity model, the double multiple streamtube model (DMSM), to evaluate the turbine performance. They showed that the skewed flow increased the power coefficient of the H-rotor. This phenomenon was further confirmed by their wind tunnel experiments (Mertens et al., 2003) and hotwire measurement (Simão Ferreira, Van Bussel, & Van Kuik, 2006). Last, the VAWT can accept wind from any direction and can potentially be used in lieu of HAWT designs in urban and mountainous areas and gusty area (Riegler, 2003).

The H-rotor shown in Figure 1 is a promising wind energy conversion system suitable for the built environment. Its small dimension ensures that it can be installed in urban areas. It has excellent energy conversion efficiency and has low manufacturing cost due to its simple structure. Despite its simple geometry, flow phenomena involved in H-rotor are complex and simplification cannot be easily made. These challenges are summarized in recent studies (Simão Ferreira et al., 2010). First, the blade rotating motion implies a variation of angle of attack which causes variation of force on the blade. As a result, it requires...
a time-accurate model to analyze the unsteady behavior. Second, wind turbines often operate beyond their individual static stall angle and experience dynamic stall. The dynamic stall is more severe at low tip speed ratio (TSR) and is a challenging topic by itself.

Both experimental and numerical studies have been reported in the study of H-rotors. In their wind tunnel experiment Oler, Strickland, Im, and Graham (1983) observed a large vortex on a single-bladed H-rotor. Simão Ferreira, Kuik, Bussel, and Scarano (2009) used the PIV method to visualize vortex shedding from a single-bladed H-rotor. Vortex shedding was observed at a low TSR of 2 but not at high TSRs of 3 and 4. Mohamed (2013) experimentally investigated the effect of solidity on the turbine performance to check the self-starting capability of Darrieus turbine.

Numerical methods with various fidelities have been proposed to study VAWTs. For example, Templin (1976) introduced the single streamtube model which is derived from blade element momentum theory without considering the solidity and TSP. Templin showed that the model can predict the power but the predicted value is higher than experimental results. Wilson and Lissaman (1974) proposed an improved model: the multiple streamtube model. In this improved model, the swept area of the turbine is divided into several parallel streamtubes and the blade element momentum theory is applied in each streamtube separately. The improved model predicted the instantaneous aerodynamic blade forces and the induced velocities better than the single streamtube model. Paraschivoiu (1988) presented a double multiple streamtube model to predict the performance of Darrieus wind turbine. The DMSM gave a better prediction than the original streamtube model. One drawback of most streamtube models is that they are limited to low TSRs and small solidities since the blade element momentum theory is not valid at high TSRs and solidities (Paraschivoiu, 2002).

Vortex models have also been applied to study VAWTs. Vortex models calculate the velocity field through the influence of vorticity in the wake of the blades (Islam, Ting, & Fartaj, 2008). Larsen proposed a vortex model to study a single bladed VAWT (1975). In his model the angle of attack is assumed to be small and the stall effect is neglected. Strickland, Webster, and Nguyen (1980) improved the vortex model to include the dynamic stall, pitching circulation and added mass effect. Cardona (1984) added a modified form of dynamic stall effects. His model showed improvement in the prediction of the instantaneous blade force.

With the advancement of computing power and numerical algorithms, computational fluid dynamics become a reliable tool in the study of H-rotor. Tchon and Paraschivoiu (1994) simulated two-dimensional H-rotors using a laminar Navier-Stokes solver. In their study the Reynolds number is 6700 which is one tenth of the experimental condition. Their results matched the experimental normal force but not the tangential force. Guerri, Sakout, and Bouhadef (2007) investigated the aerodynamic performance of a small rotating VAWT by using a Reynolds Averaged Navier-Stokes (RANS) solver and moving meshing technique. In their study the SST k-omega turbulence model was used. Their study showed that the RANS solver can capture the dynamic stall phenomenon and gave a better prediction of power coefficient than the DMSM model. Besides, the RANS model considers the airfoil section data while the DMSM model does not. Hamada etc. compared 2D and 3D results using the unsteady RNG k-ε turbulence model and claimed that an appropriate time step is required to obtain the accurate solution. Horiuichi, Ushiyama, and Seki (2005) examined the H-rotor performance using the detached eddy simulations (DES). Comparing the calculated and experimentally measured wind velocity, they concluded that the numerical simulation was applicable to the flow analysis for H-rotors. They also noticed that at the distance of 10 times of turbine radius from turbine center, flow could not recover its freestream condition, indicating a large spacing is needed when installing wind turbine in large quantity. Mohamed (2012) numerically investigated the influence of airfoil shape on H-rotor performance. He
studied 20 different airfoils using the realizable $k - \varepsilon$ turbulence model. He was able to identify one airfoil that provides better performance than the conventional ones.

Among all the phenomena involved, dynamic stall plays a critical role in determining the H-rotor performance especially at low TSRs. The understanding of dynamic stall can help the design of more efficient and durable turbines. Ferreira et al. have done extensive research to understand the dynamic stall phenomenon on an H-rotor. They used PIV to visualize the shedding and convection of vortices (Simão Ferreira, 2009). They also numerically investigated dynamic stall using different turbulence models and further compared the influence of turbulence models on the accuracy of dynamic stall prediction. They found that the k-epsilon model and the Spalart Allmaras model give similar cycled averaged tangential and normal forces over the upwind semi-rotation. But the spatial distribution of vorticity is different among the two turbulence models (Simão Ferreira, Van Bussel, & Van Kuik, 2007). They also studied the same problem using large eddy simulations (LES) and detached eddy simulations. They found DES overall performances better than LES. The impact of dynamic stall on torque is also studied numerically by Hamada with the RNG k-epsilon turbulence model (Hamada, Smith, Durrani, Qin, & Howell, 2008).

To improve the efficiency, Hwang optimized a 2D H-rotor using a RANS solver and the k-epsilon turbulence model (Hwang, Lee, & Kim, 2009). Hwang showed that the performance can be improved by 25% with an optimized pitching angle motion. Paraschivoiu, Trifu, and Saeed (2009) computed the optimal variation of the blades’ pitch angle to maximize the power coefficient at given conditions. The optimization was conducted by combining a genetic algorithm and a DMSM code. The optimized blade pitch was able to enhance the turbine performance at the operating wind speed. Takao et al. (2009) reported that an H-rotor can be improved by adding a directed guide vane row because of the guide vane row changes the airflow inlet angle to the rotor.

In this paper we study the aerodynamics of a 2D H-rotor using the RNG $k - \varepsilon$ turbulence model with a focus on the vortex dynamics and its impact on force histories. The influence of solidity is investigated. The blade thickness effect will also be studied. In the next we first introduce the aerodynamics of H-rotors, and then we present the numerical methods and computational set-up. Last we present the numerical results and discussion.

### 1.1. Aerodynamics of H-rotor VAWT

An H-rotor consists of straight blades with an airfoil cross section profile. The strut and supporting arms serve as the structural support. A typical H-rotor VAWT is shown in Figure 2. When wind blows toward the turbine, both lift and drag are generated on the blade. The force component in the tangential direction generates torque that spins the turbine.

The blade rotation changes the velocity magnitude and direction perceived by the blade. Hence the angle of attack (AoA) varies with the azimuthal position of the blade. As shown in Figure 3 the AoA ($\alpha$) is defined as the angle between the airfoil chord line and the effective wind velocity ($W$) perceived by the blade. The effective velocity is composed by the wind velocity ($V_\infty$) and the blade rotating velocity ($\omega R$). Figure 3 illustrates the variation of the effective velocity at different azimuthal positions of the blade. The maximum and minimum effective velocity is achieved when the blade is at the azimuthal angle of 180˚ and 0˚, respectively. Figure 4 shows the effective wind velocity perceived by the blade at different azimuth angles. In the figure the effective wind velocity is normalized by the freestream wind speed.
For an H-rotor the effective AoA is not only a function of the azimuthal position but a function of the tip speed ratio which is defined as the ratio between the blade rotating speed ($\omega R$) and the freestream speed ($V_\infty$). The relationship between the effective AoA ($\alpha$), the azimuthal angle, $\theta$, and the tip speed ratio, $\lambda$, is defined as follows:

$$\alpha = \tan^{-1} \frac{\sin \theta}{\cos \theta + \lambda}$$

Figure 5 plots the effective AoA versus the azimuthal angle at different tip speed ratios. The static stall angle of NACA 0015 airfoil at the Reynolds number of 70,000 is also plotted. It can be seen that as the tip speed ratio decreases more portion of the rotation cycle exceeds the static stall angle.

Figure 6 shows the directions of the lift, drag and their normal and tangential components on a blade. Here the lift is the force that is perpendicular to the relative wind velocity ($W$) and the drag is the one parallel to $W$. In the study of H-rotor, tangential and normal forces are usually used. The tangential force is resultant force parallel to the rotational direction and the normal force is the one perpendicular to the rotational direction. The tangential force $F_T$ and the normal force $F_N$ can be expressed as below:

$$F_T = L \sin \alpha - D \cos \alpha$$

$$F_N = L \cos \alpha - D \sin \alpha$$

where $L$ and $D$ are the lift and drag, respectively. The tangential and normal force coefficients can be expressed as:

$$C_T = \frac{F_T}{1/2 \rho c V_\infty^2}$$

$$C_N = \frac{F_N}{1/2 \rho c V_\infty^2}$$

The averaged tangential force ($F_{Ta}$) over one rotational cycle is

$$F_{Ta} = \frac{1}{2\pi} \int_0^{2\pi} F_T(\theta)d\theta$$

The torque ($Q$) on the blade is generated by the tangential force and it is calculated as follows:

$$Q = NF_{Ta}R$$

where $N$ is the number of blades and $R$ is the turbine rotation radius as shown in Figure 3. And the torque coefficient can be expressed as:

$$C_Q = \frac{Q}{1/2 \rho R A V_\infty^2}$$

where $A$ is the cross-sectional area of the wind turbine. In 2D simulations, the height of the turbine blade is considered as unit length, therefore,

$$A = 2R$$

The total power ($P$) is

$$P = Q \cdot \omega$$

and the power coefficient is defined:

$$C_P = \frac{P}{1/2 \rho A V_\infty^2}$$
2. Numerical methods

2.1. Fluid solver

We solve the unsteady incompressible Navier-Stokes equations to describe the flow fields. The least squares cell-based method is applied to evaluate all the gradients. The convection terms are discretized using a second-order upwind scheme. The diffusion terms are discretized using a second-order central difference scheme and the pressure term is by using second-order central difference scheme. Time marching is achieved using a second-order backward implicit method. The SIMPLE (Semi Implicit Method for Pressure Linked Equations) method is used to couple the velocity and pressure (Patankar, 1980). The following RNG k-ε turbulence model is used as the turbulence closure (Yakhot, Orszag, Thangam, Gatski, & Speziale, 1992):

\[
\frac{\partial (\rho k)}{\partial t} + \frac{\partial (\rho \mathbf{u} k)}{\partial x_i} = \frac{\partial}{\partial x_j} \left( \alpha_k \mu_{\text{eff}} \frac{\partial k}{\partial x_j} \right) + G_k - \rho \varepsilon
\]  

(12)

\[
\frac{\partial (\rho \varepsilon)}{\partial t} + \frac{\partial (\rho \mathbf{u} \varepsilon)}{\partial x_i} = \frac{\partial}{\partial x_j} \left( \alpha_\varepsilon \mu_{\text{eff}} \frac{\partial \varepsilon}{\partial x_j} \right) + C_{1_\varepsilon} \frac{\varepsilon}{k} G_k - C_{2_\varepsilon} \rho \frac{\varepsilon^2}{k} - R_{\varepsilon}
\]  

(13)

where \( k \) is turbulent kinetic energy, \( \varepsilon \) is turbulent dissipation rate, \( \mu_{\text{eff}} \) is effective turbulent viscosity, \( G_k \) represents the generation of turbulent kinetic energy due to the mean velocity gradients. The quantities \( \alpha_k \) and \( \alpha_\varepsilon \) are the inverse effective Prandtl numbers for \( k \) and \( \varepsilon \). The RNG k-ε turbulence model was proposed by Yakhot. In this model, the rigorous statistical technique is applied to the unsteady Navier-Stokes equations to capture the effect of smaller-scale motions (Pezzinga, 1994). Compared with the standard k-ε model, the RNG k-ε model uses an additional term (\( R_{\varepsilon} \)) in its dissipation equation to model the interaction between the turbulence dissipation and the mean shear. Tests showed that the additional term can significantly improve the model accuracy for problems with rapidly strained flows (Ansys, 2009). Furthermore, the effect of swirl on turbulence is included in \( \mu_{\text{eff}} \), enhancing the accuracy for swirling flow simulation.

2.2. Sliding mesh

To handle the blade rotation we used the sliding mesh technique to update the mesh around the rotating wind turbine blades. In this method, cell zones are surrounded by at least one interface zone where it meets its neighboring cell zone. The interface zones are associated with one another to form a mesh interface. And both zones will move relative to each other along the mesh interface. One advantage is that the sliding mesh technique does not require a point-to-point match at the interface which is often required for other moving grid methods (Lian, Steen, Trygg-Wilander, & Shyy, 2003). With the sliding mesh technique, the computational domain is divided into stationary areas and rotating areas. The rotating areas consist of the moving objects. As shown in Figure 7, the rotating area consists of the blades.

In the rotating region, the governing equations need to be modified to incorporate the rotation effect. The modified continuity and momentum equations are as follows:

\[
\nabla \cdot (\vec{u} - \vec{V}) = 0
\]  

(14)

\[
\frac{\partial \bar{u}}{\partial t} + \bar{u} \cdot \nabla (\bar{u} - \vec{V}) = -\nabla p + \frac{1}{\rho} \nabla \cdot (\mu \nabla \bar{u})
\]  

(15)

Here \( \bar{u} \) is the fluid velocity in the stationary frame and \( \vec{V} \) is the velocity due to mesh motion, \( p \) is the pressure and \( \mu \) is the dynamic viscosity. At the sliding interface a conservative interpolation is used for both mass and momentum based on a set of fictitious control volumes.

3. Results and discussions

3.1. Computational setup

Figure 7 illustrates the computational setup. The annular region (including the blades) rotates with a prescribed speed defined by the TSR. In this rotating region the modified Navier-Stokes equations (Equations 13 and 14) are solved. The rest regions are stationary. In the simulation, the Dirichlet boundary condition is applied on the left boundary where the velocity is set as the freestream velocity; slip boundary condition is applied to the top and bottom surfaces; the pressure is prescribed on the right boundary. The airfoil surfaces are defined as nonslip walls. The inner boundary of the annulus (the red line) is defined as interface zones with the inner region boundary. The outer boundary of the annulus (the blue line) is defined as interface zones with the overlapping boundary of outer region.

![Figure 7. Computational setup of the problem.](image)
3.2. Grid sensitivity analysis

A sensitivity analysis is carried out. The flow conditions and geometry are based on experiment of Oler et al. (1983). They tested a straight one-bladed rotor with NACA0015 profile in a water tow tank. The tow tank has a depth of 1.25 meters, a width of 5 meters, and a length of 10 meters. The solidity is 0.25 which is the ratio of the sum of chord lengths to the turbine rotation radius. In the experiment they maintained a constant angular velocity but varied the incoming flow velocity to get different TSRs. The case we simulate here has a TSR of 2.5. The Reynolds number is 67,000 based on the effective tip speed and the chord length.

The baseline grid has 400 points on the airfoil in the circumferential direction. The medium grid has 600 points and the fine grid has 800 points in the circumferential direction. In the radial direction the grid distribution is carefully adjusted to ensure the $y^+$ is in the range of 15 and 30 for turbulent flow simulations.

Figure 8 shows the tangential force coefficient ($C_T$) versus the azimuth angle with different grid densities. From 0° to 70° all the grids predict almost the same results. After 70° the coarse grid predicts a lower coefficient than the medium and fine grids. But there is almost no disparity between the medium and fine grids from 70° to 360°, indicating a grid-independent solution is obtained with the medium grid. At $\theta = 70\degree$ the effective angle of the airfoil is 20.6°, which is beyond its static stall angle shown in Figure 5. Figure 9 presents snapshots of vorticity contours with streamlines at $\theta = 85\degree$ with different grids. There is a slight difference in the recirculation zone within the separation bubble between the coarse and medium grids but there is almost no difference between the medium and fine grids. The medium grid is therefore used in the following study.

In Figure 10 we compare the simulated tangential and normal force coefficients with experimental data (Oler et al., 1983). The numerical results match the trend of the measured ones but the magnitudes are different. Simulations by various other groups show similar disparity in force coefficients (Deglaire, Engblom, Ågren, & Bernhoff, 2009).

3.3. Tip speed ratio effect

As shown in Figure 5, at the same azimuth angle the effective AoA encountered by the airfoil changes with the TSR. The higher the TSR, the less the airfoil exceeds its static stall angle. In Figure 11 we compare the tangential force coefficient histories at different TSRs. It is found the coefficient peak decreases with the increase of TSR. It is also observed that from $\theta = 0\degree$ to $\theta = 90\degree$ the coefficient increases with the TSR. At around 90°, the tangential force coefficient begins to drop at TSR = 2 but the coefficient does not reach its peak until at around 100° for TSR of 3 and 4.

Figure 12 compares the snapshots of vorticity contours at $\theta = 100\degree$ at different TSRs. It is clear that a high TSR
can defer the shed of the leading edge vortex. For $\text{TSR} = 2$, a large vortex has already shed into the wake; for $\text{TSR} = 3$ the vortex starts to shed but still attaches to the surface; for $\text{TSR} = 4$ the vortex attaches to the surface.

The torque coefficients of a three-bladed H-rotor at different TSRs are plotted in Figure 13. The peak torque coefficient is achieved at 2.5. Figure 14 plots the power coefficients. The peak occurs at TSR of 2.5.

Figure 10. Comparison of instantaneous tangential and normal force coefficients between experiment (Oler et al., 1983) and simulation.

Figure 11. Comparison of instantaneous tangential force coefficient of three bladed H-rotor at different TSRs.

Figure 12. Comparison of vorticity contours at different TSRs at $\theta = 100^\circ$. A high TSR defers the shedding of leading edge vortex.

Figure 13. Torque coefficients of the three-bladed H-rotor at different TSRs.

Figure 14. Power coefficients of the three-bladed H-rotor at different TSRs.
3.4. Solidity effect

Figure 15 compares the tangential force coefficient histories of the one-bladed, two-bladed, three-bladed, and four-bladed H-rotors at different TSRs. Figure 16 compares their cycle averaged per blade torque coefficients at different TSRs. It is clear that for all the tested TSRs increasing the number of blades reduces the tangential force coefficient magnitude but the reduction level highly depends on the TSRs: the higher the TSRs the more reduction the coefficient. This is also true for the cycle averaged torque coefficient (Figure 16). At TSR = 2 all the rotors have the nearly identical per blade torque coefficients but their coefficients are quite different at the TSR of 4. From Figure 16 we can also see that increasing the number of blades delays the appearance of tangential force peak, which is more obvious at low TSRs than at high TSRs.

We compare the vorticity contours at different solidities in Figure 17 and Figure 18. In each case four representative instants are selected (θ = 57°, 140°, 230° and 333°). In all cases strong vortex-blade interactions are observed but the interaction behavior varies with the azimuthal angle, solidity, and TSR.

Figure 17 is the case of TSR = 2. We compare the vorticity contours around the circled blades. At θ = 57° and 140°, the blade is in the upstream and no vortex-blade interaction is observed in all cases. At θ = 230°, no interaction is observed in the one-bladed case; in the two-bladed case the blade is about to interact with the wake of the previous blade; in the three-bladed case vortex-blade interaction is observed. At θ = 333° the interaction is seen in both the two- and three-bladed cases. In the one-bladed case, the airfoil goes through a weak vortex shed from itself. In Figure 15, we can find the resulting force histories are very similar between the two- and three-bladed cases while they are different from the one-bladed case.

The vorticity contours at TSR = 3 are shown in Figure 18. From Figure 5 we can see that at TSR = 3 the effective AoA is lower than that at TSR = 2. This lower effective AoA leads to different vortex dynamics. At θ = 140° a much smaller LEV is observed at TSR = 3 than at TSR = 2. At θ = 230° the blade already interacts with the wake in the case of TSR = 3 but not in the case of TSR = 2.

Figure 19 compares the cycle averaged power coefficients of turbines with different numbers of blades. Except for the one-bladed rotor, the power coefficient increases with the number of blade at low TSRs and decreases with the number of blades at high TSRs. Overall the
Figure 17. Comparison of vorticity contours at different solidities (TSR = 2).

Figure 18. Comparison of vorticity contours at different solidities (TSR = 3).
three-bladed rotor has the highest coefficient at TSR of 2.5.

3.5. Airfoil thickness effects
The performance of two three-bladed rotors with different cross sectional profiles is further studied. One has the NACA0015 profile and the other one has the NACA0022 profile. Figure 20 compares the vorticity contours near their tangential force efficient peaks at different TSRs. Overall they look alike each other.

Figure 21 compares the cycle averaged torque coefficients at different TSRs. At low TSRs, the NACA0022 blade gives a better performance than the NACA0015 blade. The NACA0022 blade reaches its peak torque at a lower TSR than the NACA0015 blade. The peak torque of the NACA0022 blade is slightly higher than the NACA0015 blade. But the NACA0015
blade has an overall higher peak power coefficient (Figure 22).

4. Conclusion

We have studied the aerodynamic performance of an H-rotor using a Navier-Stokes solver. A sliding mesh method is used to handle the rotation motion of the blades. The transient RNG k-ε turbulence model is used as the turbulence closure. Detailed sensitivity analysis is presented. Due to the massive flow separation, we need a very fine grid to capture the flow phenomenon. Comparisons are made with available numerical and experimental data. Discrepancies exist between experimental data and simulation data. It is found that the power performance is increasing with TSR increasing at low TSRs. At high TSRs, our study shows that vortex and blade interactions have an obvious impact on the blade performance. The power performance continues increasing until the number of blades equal to three. The four bladed H-rotor shows a lower power coefficient than three bladed H-rotor. The power performance is also significantly influenced by the airfoil thickness. Our results show that increasing the airfoil thickness improves the blade performance at the low tip speed ratio.

Since H-rotors are characterized with massive flow separation, vortical flows, and laminar-to-turbulent transition, high fidelity models such as detached eddy simulation or large eddy simulation is recommended for further work.

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