Performance Evaluation of a Forward Swept Blade for Vertical Axis Wind Turbine through CFD Simulation

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Abstract. With high power density wind farms, reduced environmental impact, and logistical requirements, vertical axis wind turbines offer a good value proposition for the challenging topography of the Philippines. In this study, the forward-swept wing was incorporated as a VAWT blade; its swept angle is defined along the axis of rotation and evaluated its performance through CFD simulation utilizing SST k-ω turbulence model with curvature correction function validated against experimental data. The forward-swept blade exhibited improved performance over a limited tip speed ratio (λ) with up to a 7.2% increase in peak performance occurring at λ = 2.6 and with the highest increase of 14% in λ = 2.387. The increase is due to an increased peak torque output characterized by stall delay attributed to forward swept high angle of attack.

1. Introduction

Although traditional horizontal axis wind turbines are getting cheaper, the logistical component will still account for most of the developmental costs [1]. With the topographical makeup of the Philippines, this can be challenging. Alternatively, to address this logistical challenge and maximize the wind resource that can be harnessed, vertical axis wind turbines can be employed as these turbines are relatively small in comparison. What it lacks in individual turbine efficiency it compensates with higher power density. In a field experiment, a potential increase in wind farm power output by up to ten times its equivalent HAWT farms per land area was achieved by strategically clustering the turbines, synergistically enhancing performance. However, the turbines used in the experiment were reportedly unreliable, and they encountered many turbine-related failures [2].

It can be said that VAWT is lagging behind HAWT for it did not receive continuous development, unlike HAWT in the past [3]. Although relatively simpler, it is aerodynamically complex. VAWT dynamic loading was not well understood in the past, which led to poor design choices and premature failures [4]. The most common failures were fatigue failure from cyclic stress, which can be exacerbated by dynamic stall as it induces load fluctuations [3]. There are methods to mitigate these, both passive and active. However, the current study is particular about the Gorlov, also known as helical VAWT, which has been shown to alleviate dynamic stall effects resulting in the reduced magnitude of torque fluctuations [5]. However, due to reduced peak torque, helical designs have reduced start-up acceleration but have a more consistent output [6]. If the sweep angle and the blade...
length are further increased, helical blades will be more susceptible to uneven blade loading, based on simulation results [5].

Forward swept wing has the characteristics that may offer an alternative solution. It has a high angle of attack, and the stall starts at the midplane [7]. Forward swept design was already investigated with traditional HAWT with mixed results, i.e., increased root bending stress that can be detrimental to pitched controlled turbines [8]. Another study achieved an up to 2.9% increase in power coefficient ($C_p$) in a CFD simulation [9]. Recently its application for VAWT was investigated and was termed inverted V-shaped blade, attaining a 15% increase in the coefficient of performance while retaining the desirable characteristics of helical design, but this is limited at a single tip speed ratio $\lambda$, and other flow characteristics were not evaluated yet [10]. A known problem with a forward-swept wing is its susceptibility to static divergence as per wind tunnel tests [11]. This issue occurs when the wing structure starts to yield under aerodynamic moments that originate from wingtips due to increasing lift, explaining the increased root bending stress observed in the HAWT blade application [8].

1.1 Swept blade and helical designs considerations

The airflow against a swept blade is divided into two components a velocity component perpendicular to the blade at a sweep angle called chordwise velocity component and the other called spanwise component that runs across the blade [12]. The increased spanwise flow may explain why helical design with steep angles tends to perform poorly at low wind speeds and angular velocity due to the blade stalling much earlier, where the peak $C_p$ and torque is way below that of the straight-bladed design as the blade requires higher airflow to provide sufficient lift as seen on [6,13]. It was observed that as the sweep angle increases, its optimal powerband would further shift towards the higher wind speed and angular velocity, which coincides with [14,15] findings. There are consequences entailed in such operation. Higher sweep angles will inevitably experience uneven blade loading [5]. Also, inertial forces will be dominant at a high rotation speed, leading to the blade deflecting.

1.2 Observations on existing simulations and Numerical considerations

In turbulence modeling for VAWT for this particular case, it was observed that the SST k-ω [16] tends to result in an overestimated power coefficient values with fine meshes, especially with 2D mesh, particularly at high and low $\lambda$ [17]. On the other hand, severe underestimation in moderate $\lambda$ for coarse meshes but has good accuracy in low and high $\lambda$ was observed [18]. Two factors are consistent with these observations: mesh density and the effect of isotropic assumption by RANS [19]. Due to local isotropic assumption, the model can have difficulty with flow features with high variations in mean strain rate, which is a known reason for the insensitive nature of eddy-viscosity models to curvature [20]. The model tends to over or underestimates turbulence effects under concave and convex curvatures, respectively, including inaccuracy with highly anisotropic flows. To account for this, a curvature correction function was utilized [21]. $C_{curv}$ is the parameter that influences the strength of the correction and, by default, $C_{curv}=1$ [22]. However, for $\lambda$ range where non-corrected SST k-ω exhibited accurate results, this may not be necessary.

2. Methodology

For conciseness, the straight and forward-swept blades were abbreviated to SB and FS-B, respectively. These were all conducted with ANSYS Fluent 2019 software in a workstation with AMD Ryzen 9 3950X 3.7Ghz 16-core processor and 64GB of RAM. Results are from the prescribed monitors of the instantaneous values of blade torque, and force components (axial, longitudinal, and transversal thrust). All data presented are sampled from the last revolution after reaching a time-periodic solution. The duration of a stable transient simulation for one revolution requires approximately 12 hours. Initial flow field calculation requires longer calculation time due to unsteady conditions; thus, mesh-to-mesh solution interpolation was incorporated in the methodology, substantially reducing the time required to obtain a periodic solution.
2.1 Numerical Model Validation Study
The model was based and validated against two experimental data [23,24]. The current simulation did not intentionally account for the struts or spokes to obtain an undisturbed flow for the straight blade so that the validation model can also be used in performance comparison. This approach is commonly employed in the literature to obviate additional cells imposed by the boundary layer mesh [10,15,18]. A blockage effect of approximately 10% and a correction coefficient of 1.5% were reported but were not applied in the experimental results. Therefore, the data presented in the experiment was also treated as is, including the reproduction of the wind tunnel dimensions to provide a direct comparison and preserve the effects of blockage in 3D for mesh independence analysis. The rest of the simulation was conducted at \( U = 9 \text{ m/s} \) with varying angular velocity, and the domain was enlarged to reduce the blockage effects as shown in figure 1.

2.2 Meshing
Two separate cell zones were generated from the whole fluid domain in ANSYS Fluent Meshing: a rotating sub-grid and a stationary one is shown in figure 2. The whole domain consists of polyhedral mesh, a first layer height of 0.025mm was specified for the boundary layer near the blade walls with 18 layers, and a growth rate of 1.25 was deemed sufficient to ensure a \( y^+ \leq 1 \). The polyhedral mesh was chosen as its quality is not severely affected by very high aspect ratio cells at the boundary layer and size change due to multiple faces compared to tetrahedral mesh and is much easier to generate than structured mesh [25].

2.3 Numerical model specification
The transient simulation was done in ANSYS Fluent, the flow conditions were assumed to be incompressible, and Shear-Stress Transport (SST) k-\( \omega \) turbulence model [16] was employed in the study accompanied by curvature correction function [21] to obtain an accurate solution in known \( \lambda \) range that the model reportedly underestimates the results [18]. The default curvature correction value of \( C_{curv} = 1 \) was applied.

2.4 The computational domain
The blade walls are non-slip, while the domain walls have free-slip conditions applied. Specified air density is \( \rho = 1.155 \text{ kg/m}^3 \) inlet and outlet were set to velocity and pressure, respectively, with 1% turbulence intensity [23]. The wall zones between the rotating and stationary domain were interfaced together, prescribed with a matching option to account for non-conformal boundary walls for the sliding mesh method. The sliding mesh assumes a rigid mesh and is extensively used as this is more
efficient and less demanding compared to dynamic mesh [10,15,18]. Blade chord Reynolds number considered for this study ranges from \( \text{Re}_c = 1 \times 10^5 \sim 1.62 \times 10^5 \).

### 2.5 Wind turbine calculations

Time step size \( \Delta t \) can be obtained by dividing the azimuthal angle increment \( \Delta \theta \) with angular velocity \( \omega \) in radians:

\[
\Delta t = \frac{\Delta \theta}{\omega}
\]  

(1)

The force and torque are calculated every timestep and used to monitor convergence aside from residuals to ensure accuracy. The total force \( F \) are calculated from the blade face and is given by the sum of the dot product of a specified force vector \( \bar{a} \) with pressure \( \bar{F}_p \) and viscous \( \bar{F}_v \) forces, while torque is the sum of the cross product of these forces with moment vector \( \bar{r} \). \( \bar{r} \) is defined from the moment center to the specified blade wall [26].

\[
\begin{align*}
F_a &= \bar{a} \cdot \bar{F}_p + \bar{a} \cdot \bar{F}_v \\
T_b &= \bar{r} \times F_v + \bar{r} \times F_p
\end{align*}
\]  

(2)

(3)

The coefficients are expressed as:

\[
\begin{align*}
C_x &= \frac{F_x}{0.5 \rho A U^2} \\
C_y &= \frac{F_y}{0.5 \rho A U^2} \\
C_q &= \frac{T}{0.5 \rho U^2 A r} \\
C_p &= \frac{T \omega}{0.5 \rho U^3 A_r}
\end{align*}
\]  

(4)

(5)

(6)

(7)

### 2.6 Solution settings

PISO exhibited better performance than SIMPLE for pressure-velocity coupling in this study. Iterations per timesteps take longer but reach convergence much faster and is stable even at higher under-relaxation values. Only the neighbor correction is enabled to shorten the iteration time. A value of 4-5 is sufficient to provide stable simulation from initialization with second-degree accuracy. After reaching a stable solution, the neighbor correction factor was reduced to 1. Spatial discretization and transient formulation are all set to second-order accuracy. Convergence criteria based on residuals was set to \( 1 \times 10^{-4} \) [18], and a maximum of 20 iterations per time step was deemed sufficient [10]. For \( \lambda = 2.035 \) due to considerable flow variations, residuals fluctuate between \( 1 \times 10^{-3} \sim 1 \times 10^{-4} \).

### 2.7 Mesh independence analysis

All simulations were run until a stable instantaneous torque was obtained, dictated by the relative change in adjacent peaks (less than 1%) of up to 4-5 rotations after a periodic solution was achieved [18]. The analysis was run at \( U = 8 \text{ m/s}, 400 \text{ rpm} \), and \( \text{Re}_c = 1.18 \times 10^5 \) with an initial 0.5° azimuthal increment where the highest performance \( C_p \) as recorded [23]. The mean coefficient of performance measured in the experiment at 8m/s was \( C_p = 0.285 \), with an error at around \( \pm 8\% \). The medium mesh was selected as it is well within error and accounts for the increase in cell count for the low blockage case (increase in domain size).

The azimuthal increment was increased to 2° [10], and a temporal analysis was carried out to further ascertain accuracy. Only two increments were compared Table 2 since the slight increase in peak
torque has similarity to Rezaeiha et al. [27]. This implies that a small azimuthal increment does not readily equate to better accuracy and will still depend on the conditions.

**Table 1.** Mesh independence analysis.

| Minimum specified scoped size at the wall | Total number of cells generated | Simulated $C_p$ | Relative Error % |
|------------------------------------------|---------------------------------|-----------------|------------------|
| Fine 0.8 mm                              | $18 \times 10^6$               | 277             | -2.81            |
| Medium 0.9 mm                            | $10 \times 10^6$               | 271             | -4.91            |
| Coarse 1 mm                              | $7 \times 10^6$                | 258             | -9.47            |

**Table 2.** Temporal resolution analysis at 8m/s for medium mesh.

| Increment | Simulated $C_p$ | Relative Error % |
|-----------|-----------------|------------------|
| Case 1 0.5° | 0.271           | -4.91            |
| Case 2 2°   | 0.277           | -2.81            |

2.8 Model validation at varying operating condition

For the enlarged domain, the SB was divided in half through symmetry to decrease the cell count. Due to the reduced blockage effect, a decrease in power coefficient was observed compared to the experimental but is well within error. The validation results at varying $\lambda$ with low blockage are shown in figure 4 and are in line with Su et al.[10]. The observed overestimation is attributed to significant gradients near-wall from the increased angular velocity at high $\lambda$ and dynamic stall at lower $\lambda$. Note that curvature correction was disabled in lower $\lambda$ as it causes overestimated results, and the uncorrected SST $k$-$\omega$ is sufficiently accurate in this range.

![Figure 3. Turbine averaged instantaneous torque history of varying mesh density comparison.](image1)

![Figure 4. Comparison of simulated $C_p$ against two experimental data [24,23]. The percentage indicates blockage.](image2)

2.9 Simulation of the Forward Swept Blades

Symmetry condition for FS-B was not applied similar to Su et al., (2020), for there was an observed in-plane interaction of the vortices due to spanwise flow. The symmetry plane acts like a fence prohibiting the propagation of dynamic stall vortex, which significantly affects results. An initial simulation was done involving FS-B in two forward swept configurations $\theta_{fs} = 6^\circ$, $\theta_{fs} = 10^\circ$ and SB (from validation) at peak performance in single $\lambda$ based from experiment [23] to characterize its
effects. This comparison was made due to the excellent accuracy exhibited by the model in this condition, ensuring confidence in the results. $\theta_{FS} = 6^\circ$ is approximately equivalent to 0.6c inverted V-shaped blade of Su et al.\cite{10} shown in figure 5 $\theta_{FS} = 6^\circ$ will be referred to as FS-B 6$^\circ$ which will be used in further analysis at varying $\lambda$ range and compared it with the simulated SB based from Castelli et al.\cite{24}. The initial simulation will use the experiment-based domain, and the varying $\lambda$ simulation will use the enlarged domain to decrease any potential interference in wake due to high blockage.

2.10 Forward swept blade schematic

The general dimensions of the model are the same as the experiment\cite{23}. The forward-swept is defined by the circular phase shift of the blade tips in a leading forward position. The shape emulates a diverging forward-swept wing and a mirrored Gorlov/helical blade with a low sweep as shown in figure 5 superimposed on the inverted V-shaped blade\cite{10} in addition to the conceptual turbine and its potential support configuration.

![Figure 5. Forward swept blade superimposed on an inverted V-shaped blade with the reference azimuthal line & swept angle $\theta$ top left) as shown. A conceptual turbine illustration (right), with airflow & rotation indicated by the arrow (bottom left).](image)

3. Performance evaluation and analysis of the results

The results are illustrated in torque and thrust as these are the most straightforward in evaluating the performance characteristics of the blade. The initial simulation consists of a comparison between FS-B having two different sweep angles against SB. One revolution is represented by subdividing it into four sub-regions: windward ($315^\circ \leq \theta < 45^\circ$) upwind ($45^\circ \leq \theta < 135^\circ$) leeward ($135^\circ \leq \theta < 225^\circ$) and downwind region ($225^\circ \leq \theta < 315^\circ$).

3.1 Sweep effects on performance

Simulation of two sweep angles was done to observe sweep effects on the performance shown in figure 6, which reveals increased peak torque at low sweep angle in the upwind region but lower torque at downwind region as seen from the instantaneous moment coefficient contrary to Su et al.\cite{10} inverted V-shaped blade, but has the similar trend in reduced peak torque as the sweep angle increases but has a wider band. This observed characteristics with increasing sweep angle resemble helical blades in a simulation conducted by Alaimo et. al \cite{13}, particularly at the downwind region but will require further evaluation as this is only done for a single $\lambda$ and two angles.

3.2 Vortex formation

In figure 7, FS-B skin friction lines originating from the leading-edge representing attached flow take a chordwise path which may look horizontal but obliquely shifts towards midplane (inboard) at the line of separation (vertical line) after that, exhibits a more pronounced shift upon separation. Due to
the stall primarily occurring at the midplane seen in figure 8, the peak aerodynamic forces are also situated there. Considering only the half of the blade, representing a helical counterpart, the load is biased towards the trailing end of the blade similar to Scheurich et al.[5] findings. This was also reflected in the skin friction lines in figure 7 where separation line (vertical) is not visible indicating full separation. From the trailing edge, the lines representing the reverse flow are highly skewed compared to the angle of the blade, indicating a longer travel path thus, causing a delay in separation which increases with sweep angle, as seen in 120° post-stall in figure 7, this is reflected in prior figure 6 showing a higher torque retained for FS-B 10°. The increase in peak torque output at the windward region is characterized by the delay of separation as described, while torque in the downwind region has a slight reduction. Increasing the sweep angle further delays the separation, which should reflect a higher peak torque; however, it was reduced instead, shown with the FS-B 10° case figure 6. The likely cause is the variation in the angle of attack (AoA) of the blade section similar to Scheurich et al. [6]. Observe the increase in the incidence of reverse flow in figure 7 for FS-B 10° which is slightly larger compared to low sweep angle of 6°; this interferes with the propagation of dynamic stall vortex due to the increased spanwise flow consequently reducing its strength thus the reduction in peak torque as observed at the current λ. For the helical equivalent, this could mean that it is convected away from the blade, reducing its interaction which explains the higher degree of angle of attack at the trailing section from the spanwise flow and the high degree of wake asymmetry originating from the trailing section observed by Scheurich et al. [6].

Table 3. Average torque and coefficient of performance for SB and FS-B with two sweep angles.

| Blade  | Average Torque [N-m] | \(C_p\) | \(\Delta C_p [%]\) |
|--------|----------------------|--------|------------------|
| SB 0°  | 2.932                | 0.277  | -                |
| FS-B 6° | 3.163                | 0.299  | 7.942            |
| FS-B 10° | 3.152               | 0.298  | 7.581            |

Figure 6. Instantaneous torque coefficients of blades with sweep angles of 0°, 6°, 10° at 8m/s vs azimuthal angle position within one revolution.

Figure 7. Skin friction lines at 120° left group). Post-stall close-up at 90° and 120° of FS-B 10° right group).

3.3 Performance at varying tip speed ratios \(\lambda\)
For the proceeding simulations, only the FS-B 6° was continued and was compared with the simulated experimental equivalent, SB. The averaged values in figure 9 of torque and power suggest an increase with \(\lambda\) until peak efficiency then declines afterward. In terms of power coefficient, the highest increase is at \(\lambda = 2.387\) (14%) and for peak efficiency at \(\lambda = 2.6\) (7.2%). The overall effect of instantaneous
loading asymmetry can be seen from the average thrust values. At $\lambda = 2.035$ comparing the two blades, a slight reduction of transversal thrusts was observed for FS-B but had consistently higher longitudinal thrust for the rest of $\lambda$ range. In figure 10 & 11 of the instantaneous plots, FS-B 6° exhibited increased peak torque in a limited $\lambda$ range from $\lambda = 2.387$ to $\lambda = 3.082$. At $\lambda = 2.035$ where the dynamic stall is more pronounced, FS-B has slightly reduced peak torque and averaged torque. At the downwind region, the magnitude of blade torque and force fluctuation compared to SB was reduced. The most significant increase in peak torque and loads was observed at $\lambda = 2.387$, equating to a much higher average torque output and efficiency. Due to a relatively low sweep FS-B, instantaneous torque and thrust are almost similar with SB at $\lambda = 3.082$ to $\lambda = 3.292$. Peak longitudinal thrust continually increases with $\lambda$ in the upwind region caused by the increasing pressure gradients as the angular speed increases.

**Figure 8.** Q-criterion viewed from upwind (left) & downwind (right). SB (top), FS-B 6° (middle), & 10° (bottom).

**Figure 9.** Comparison of averaged power coefficient (top), power (middle), & thrust force (bottom) at varying tip speed ratios $\lambda$. 
Figure 10. Comparison of blade instantaneous torque coefficients $C_q$ at varying tip speed ratios $\lambda$.

Figure 11. Comparison of blade instantaneous longitudinal $C_x$ & transversal $C_y$ thrust coefficients.
3.4 Blade section loads

Based on the analysis of flow visualizations, it is apparent that loads on the blade are uneven. Further investigation was done to determine the magnitude of these load variations to understand loading conditions and mitigate these in the design stage. The analysis requires the blade face to be subdivided into different sections as illustrated in figure 12 which is from the half portion of the blade. This was conducted in two tip speed ratios where there is a significant difference in loading conditions. The results were not normalized to give the reader a better outlook of the magnitude of the loads. In figure 12, in line with the previous observation from vortex formation, FS-B peak load is situated at the midplane section (c), which is even higher than SB in $\lambda = 2.035$ while producing lower total blade torque. A noticeably reduced tip loading for FS-B can be seen at section (a), a large difference from section (c) compared with SB. The section (b) and (c) loads for SB are similar but have a more pronounced difference at $\lambda = 2.035$ leeward and downwind regions. The favorable reduction of the fluctuation magnitude at the downwind region for FS-B previously observed in figure 10 can be attributed to reduced tip stall at section (a) while SB undergoes a higher magnitude of a secondary stall. In $\lambda = 2.387$ section (a) of FS-B has good stall characteristics where the torque load is distributed at a much wider band due to sweep effects. It can be said that the reduction is caused by the induced blade tip vortices, which hinder reverse flow as shown in prior figure 7 & 8, which has a similar effect to a vortex generator as demonstrated by Choudhry et al. [28].

![Figure 12. Blade section torque loads for at two tip speed ratios $\lambda = 2.035$ (top) & $\lambda = 2.387$ (bottom).](image)

3.5 Wake interference

Analysis of near wake was done to assess the influence of wake shed, particularly at the downwind region. Prior results suggest a clear relationship between the magnitude of dynamic stall upwind in peak torque and torque recovery at the downwind, consistent with results from other research [10,17]. Vorticity contours from FS-B at $\lambda = 2.035$ were taken due to a much higher observed dynamic stall at midplane to visualize and analyze this. Keep in mind that this represents the current $\lambda$ and some wake feature may or may not be present and varies with other cases. It can be seen from figure 13 that the lifting face switched from inside to outside, which is also reflected in prior figure 8 (middle figure). The increase in torque output in sections (b) and (c) in the leeward region previously shown in figure 12 is due to the secondary stall indicated by the formation of C2. A1 shed by the previous blade appears to interact with the stall vortex in full separation at $240^\circ$ C2 and A1 are counter-rotating and dissipate slowly. The source of A1 is from the separated dynamic stall vortex as indicated by A2 in the...
leeward region and A3 at upwind from the following blades. It is likely that at higher $\lambda$ the development of stall vortex (e.g., C2) is stunted due to much lower torque values that can be seen from instantaneous torque at downwind seen in figure 10 as $\lambda$ increases.

![Figure 13](image.jpg)

**Figure 13.** Non-dimensionalized vorticity contours revealing switched lifting face at 200° (A) then wake interaction at 240° (B). Vortex shed history are labeled alphanumerically to track its origin and formation.

**4. Conclusion**

The forward-swept blade design performance was evaluated through computational fluid dynamics (CFD) simulation. The results are represented by plots of power, torque, and thrusts in addition to flow patterns and vortex formation. The forward-swept blade increased peak torque output and efficiency of up to 7.9% at 8m/s, $\lambda = 2.695$. In 9m/s at $\lambda = 2.387$ and $\lambda = 2.6$ a 14% and 7.2% increase was achieved, respectively. Peak loads are situated midplane where stall predominantly occurs, which is higher than the straight blade. Flow separation is delayed; thus, the torque curve of the forward-swept
blade is much wider. Dynamic stall formation is influenced by the spanwise flow induced by the swept blade, and its intensity is reduced with increasing sweep and higher $\lambda$. Inhibiting dynamic stall formation reduces peak torque; its magnitude at the upwind can influence torque output downwind from wake interference.

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