Aero-Structural Performance of Multiplane Wind Turbine Blades

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In current wind turbine blade designs, the inboard region suffers performance losses from competing structural and aerodynamic requirements. The objective of this effort is to develop multiplane inboard configurations that provide attractive aero-structural performance for wind turbine blades. A biplane approach may be sufficient to realize the full benefits of this approach. To compare the performance of a conventional inboard section with a biplane inboard section, cross-sectional properties of a thick monoplane and a biplane were measured to obtain their approximate structural and aerodynamic characteristics. Numerical simulations were used to explicitly compare the aerodynamic performance of a thick monoplane to a biplane. Then, several model beams were designed to be simple approximations of a conventional wind turbine blade ("monoplane beam") and the biplane blade approach ("biplane beam"). Three canonical bending loads were applied to each of these model beams and the deflection of each beam was compared. Numerical simulations show that the lift-to-drag ratio of the biplane is significantly greater than the lift-to-drag ratio of the thick monoplane for the angles of attack investigated (0–15.5°). A parametric analysis of different biplane beam configurations shows that tip deflections of the biplane beam configurations are smaller than those of monoplane beams of the same length. For example, a nominal 50 m biplane blade has less than 30% of the deflection of a 50 m monoplane blade. Thus, for a monoplane beam of fixed length, it is possible to construct a longer biplane beam with an equal tip deflection. These combined aerodynamic and structural benefits can lead directly to greater turbine power by three important advantages: (1) improved aerodynamic performance, (2) improved spacing due to the potential for decreased vortex shedding, and, most importantly, (3) potential increases in blade length due to improved inboard blade strength. Therefore, these results suggest that the biplane blade approach is an attractive design for the next-generation of large wind turbine blades.

Nomenclature

<table>
<thead>
<tr>
<th>Symbol</th>
<th>Description</th>
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<tbody>
<tr>
<td>( f )</td>
<td>externally applied force vector</td>
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<tr>
<td>( q )</td>
<td>degree of freedom vector</td>
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<tr>
<td>( V )</td>
<td>velocity vector</td>
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<td>( c )</td>
<td>chord length</td>
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<tr>
<td>( E )</td>
<td>Young’s modulus</td>
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<td>( F_o )</td>
<td>magnitude parameter for load profile</td>
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<td>( g )</td>
<td>gap distance</td>
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<tr>
<td>( h )</td>
<td>cross-section height</td>
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<tr>
<td>( i )</td>
<td>mass moment of inertia</td>
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<tr>
<td>( I_x )</td>
<td>principal area moment about the x-axis</td>
</tr>
<tr>
<td>( I_y )</td>
<td>principal area moment about the y-axis</td>
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<tr>
<td>( K )</td>
<td>kinetic energy density</td>
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<tr>
<td>( M_{bending} )</td>
<td>bending moment</td>
</tr>
<tr>
<td>( p )</td>
<td>pressure</td>
</tr>
<tr>
<td>( p_{atm} )</td>
<td>atmospheric pressure</td>
</tr>
<tr>
<td>( R )</td>
<td>length from blade root to tip</td>
</tr>
<tr>
<td>( r_b )</td>
<td>length of biplane with constant gap</td>
</tr>
<tr>
<td>( r_{MR} )</td>
<td>length from blade root to merging region</td>
</tr>
<tr>
<td>( Re )</td>
<td>Reynolds number</td>
</tr>
<tr>
<td>( S )</td>
<td>generalized Timoshenko stiffness matrix</td>
</tr>
<tr>
<td>( t_b )</td>
<td>wall thickness, biplane cross-section</td>
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<tr>
<td>( t_m )</td>
<td>wall thickness, monoplane cross-section</td>
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<tr>
<td>( U )</td>
<td>strain energy density</td>
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<tr>
<td>( V )</td>
<td>linear velocity</td>
</tr>
<tr>
<td>( V_\infty )</td>
<td>freestream velocity</td>
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<tr>
<td>( V_{relative,wind} )</td>
<td>relative wind velocity</td>
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\( x \) distance from the root  \( \alpha \) angle of attack  
\( y \) distance from the leading edge  \( \gamma \) strain  
\( z \) distance from the chord line  \( \kappa \) curvature  

**Subscripts**  
1 beam reference line  
2 chord line in cross-sectional plane  
3 thickness line in cross-sectional plane  
\( \tilde{m} \) center of mass  

**Symbols**  
\( \alpha \) angle of attack  
\( \gamma \) strain  
\( \kappa \) curvature  
\( \mu \) dynamic viscosity  
\( \nu \) Poisson’s ratio  
\( \Omega \) angular velocity  
\( \rho \) density  
\( \sigma_{x,\text{bottom}} \) maximum stress on bottom airfoil  
\( \sigma_{x,\text{top}} \) maximum stress on top airfoil  
\( \tilde{\mu} \) mass per unit length

## I. Introduction

Structural loads in wind turbine blades have increased dramatically as commercial turbines have grown in size. This most affects the design of the inboard region of the blade, where thick airfoil cross-sections are needed to support these demanding loads\(^1\) (figure 1). Current inboard blade designs suffer performance losses from competing structural and aerodynamic requirements. While many have developed airfoils for the mid- and outboard regions of wind turbine blades,\(^1\) \(^6\) relatively little work has been done to design blades with an improved inboard region in order to meet both structural and aerodynamic requirements. Recent efforts in the development of the inboard region have examined flatback or blunt trailing-edge airfoils.\(^7\) While an improvement over standard thick airfoils, flatback airfoils still suffer from high drag and noise from vortex shedding;\(^8\) \(^10\) this vortex shedding also decreases the quality of the wind for downstream turbines. Other efforts focused on flow control, attempting to increase aerodynamic efficiency (lift-to-drag ratio) by controlling separation of the boundary layer. This is achieved to a certain extent by means of synthetic jets, trailing edge flaps and wedges, stall strips, and vortex generators.\(^11\) However, none of these approaches have sufficiently addressed the structural loading challenge for turbine blade growth.\(^12\)

UCLA researchers have developed a new multiplane approach to inboard wind turbine blade design.\(^13\) This multiplane approach has the potential to improve the aerodynamic and structural performance of the inboard region. Most importantly, this improvement to the aerostuctural performance of the inboard region can allow increased overall blade length and, thus, improved overall power output for a given blade mass or tip deflection constraint. This approach can improve the performance and levelized cost of energy\(^a\) of wind turbines of all sizes, and will likely be of particular significance for large (3-7 megawatt) and ultra-large (8-10 megawatt) turbines for both land-based and offshore applications.\(^14\)

An artist’s conception of this approach is shown in figure 2 for a biplane inboard region. Early trade-off studies at UCLA have shown that a biplane approach may be sufficient to realize the full benefits of this approach;\(^13\) however, different multiplane configurations are being considered. The design shown in figure 2 is for a retrofit biplane blade that can be integrated with an existing conventional wind turbine hub. The aero-structural advantages of this design are summarized in figure 3. The svelte shape of the biplane cross-section allows optimal aerodynamic performance while also providing structural rigidity.

The objective of this effort is to develop multiplane inboard configurations that provide attractive aerostructural performance for wind turbines. This paper reveals the basic aerodynamic and structural benefits separately using simple techniques.

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\(^a\)levelized cost of energy: the total system cost divided by the total energy produced over the system’s lifetime
II. Approach

To compare the performance of a conventional inboard section with a biplane inboard section, the structural and aerodynamic performance of a thick monoplane cross-section was compared to a biplane cross-section. As discussed in section II.A, cross-sectional properties were measured to approximately characterize the structural and aerodynamic performance. Next, as presented in section II.B, computational fluid dynamics (CFD) simulations were used to explicitly compare the aerodynamic performance of a thick monoplane to a biplane. Finally, section II.C describes how several model beams were designed to be simple, first-order approximations of a conventional wind turbine blade and a biplane blade. Three canonical bending loads were applied to each of these model beams and the deflection of each beam was compared.

A. Cross-sectional Properties of a Thick Monoplane and Biplane

To approximately characterize the structural and aerodynamic characteristics of a thick monoplane and a biplane cross-section, several cross-sectional properties were measured for two model airfoils. An FFA-W3-301 airfoil (figure 4) was chosen as the thick monoplane cross-section. This airfoil has a thickness-to-chord ratio of 30.1% and is similar to those used near the root in conventional wind turbine blades. An SC(2)-0714 airfoil (figure 4) was used in the biplane cross-section. This airfoil has a thickness-to-chord ratio of 14.0%; thus, an SC(2)-0714 biplane using two of these airfoils has a total thickness-to-chord ratio of 28.0%, roughly equal to that of an FFA-W3-301 monoplane. In this preliminary analysis, however, the NASA SC(2)-0714 airfoil was not chosen for its aerodynamic properties, but instead for its structural shape. This airfoil is
Three cross-sectional properties were measured for the thick monoplane and the biplane cross-sections: the principal area moments of inertia ($I_x$ and $I_y$), the projected frontal area, and the wetted surface area. Because a cross-section’s bending stiffness is proportional to its mass moment of inertia, the principal area moments give an approximate measure of the structural stiffness of a cross-section when subjected to bending loads. Similarly, a cross-section’s pressure and viscous drag are roughly proportional to its projected frontal area and wetted surface area, respectively. Thus, the projected frontal area and wetted surface area give an approximate measure of the pressure and viscous aerodynamic drag of a cross-section. The chord length of each cross-section was $c = 1$ m. The airfoils in the biplane cross-section were not staggered and were separated by a gap-to-chord ratio of $g/c = 0.5$. The SolidWorks 2010 software was used to calculate these properties, assuming the airfoils in each cross-section were made of a solid material with density $\rho = 2.0 \times 10^3$ kg/m$^3$. Results from this analysis are given in section III.A.

B. Aerodynamic Comparison of a Thick Monoplane and Biplane Cross-section

In order to quantify the comparative aerodynamic performance of the thick monoplane cross-section and the biplane cross-section, a two dimensional (2D) CFD analysis was performed using O-grid computational domains (figure 5). As before, the chord length of each cross-section was $c = 1$ m; the airfoils in the biplane cross-section were not staggered and were separated by a gap-to-chord ratio of $g/c = 0.5$. Both domains had a diameter of 21 chord lengths, so that incompressible viscous calculations could accurately model the flow far away from the cross-section. A velocity-inlet boundary condition was used along the left edge of each domain to define the upstream flow velocity. An outflow boundary condition was used along the right edge of each domain to extrapolate the downstream flow properties from the domain interior. Wall boundary conditions were used along each airfoil.

Two structured grids were generated for each domain with Gridgen 15.06 (figure 6). The thick monoplane used an O-grid with 131,072 cells. The biplane used an H-grid between each airfoil and an O-grid for the surrounding farfield region; the entire grid used 151,500 cells. In both grids, cells were concentrated near the airfoil walls, where large gradients in the flow exist from the boundary layer.

Steady-state, incompressible viscous CFD calculations were performed by the Fluent 6.3.26 code. CFD was used to implicitly solve the pressure-based Navier-Stokes equations in 2D, such that

$$\nabla \cdot \mathbf{V} = 0 \quad (1)$$

$$\rho (\mathbf{V} \cdot \nabla \mathbf{V}) = -\nabla p + \mu \nabla^2 \mathbf{V} \quad (2)$$

Turbulent viscosity was modeled with the one-equation Spalart-Allmaras model. The solution scheme was second-order accurate in space.

All flow conditions in the numerical simulation were for air at atmospheric pressure $p_{atm} = 1.01325 \times 10^5$ Pa, density $\rho = 1.225$ kg/m$^3$, and viscosity $\mu = 1.7894 \times 10^{-5}$ kg/(m·s). All simulations had a Reynolds number of $Re = 1.479 \times 10^6$ and a freestream velocity of $V_{\text{in}} = 21.6$ m/s. The flow over the thick monoplane was computed at angles of attack between $0^\circ \leq \alpha \leq 15.5^\circ$; the flow over the biplane was computed between $0^\circ \leq \alpha \leq 15.3^\circ$. Convergence criteria of $10^{-3}$ were used for all computed solutions.
Figure 5. Geometry of the biplane cross-section, shown with numerical boundary conditions. Note: figure not to scale.

Figure 6. Computational grids for preliminary aerodynamic analysis

(a) O-grid for FFA-W3-301 monoplane

(b) Clustered grid points near FFA-W3-301 monoplane

(c) O-grid for SC(2)-0714 biplane

(d) Clustered grid points and H-grid between SC(2)-0714 biplane
C. Structural Comparison of Monoplane Beams and Biplane Beams

Several model beams (figure 7) were designed to be simple, first-order approximations of a conventional wind turbine blade (“monoplane beam”) and a biplane blade (“biplane beam”). Three canonical types of bending load profiles were applied to the model beams, and the deflection of each beam was calculated. These deflections were calculated with a 1D beam finite element approach, and validated with a fully 3D finite element analysis. The 1D approach was used to quickly explore the effect of three design parameters (referring to figure 7(c): \( r_{MR}/R, r_b/r_{MR}, \) and \( g/c \)) on the tip deflection of the biplane beam. The 1D approach was also used to find the tip deflection of monoplane beams under various loads to then find the equivalent biplane beam length with an equal tip deflection.

1. Design of Model Beams

In order to quickly evaluate the effect of several design parameters on structural performance, simplified representations of a biplane blade were designed. For this initial study, tapering of the cross-section along the span of the beam was not considered (the cross-sectional area, chord length \( c = 1 \) m, and height \( h = 0.35 \) m were kept constant from root to tip). Composite materials were also not considered. An isotropic material that approximated aluminum was used for all beams (\( E = 5.0 \times 10^{10} \) Pa, \( \nu = 0.3, \) and \( \rho = 2.0 \times 10^3 \) kg/m\(^3\)). Because weight is one of the important limiting parameters that determines the length of wind turbine blades,\(^12\) all beams had equal cross-sectional areas, and hence, equal mass per unit length. This allowed for a self-consistent comparison between their structural performance.

A monoplane beam with a hollow rectangular cross-section (figure 7(a)) was constructed to mimic the box-beam structure of the shear web in conventional blades. The rectangular cross-section had a wall thickness-to-chord ratio of \( t_m/c = 11.08\% \). Another monoplane beam (figure 7(b)) with a hollow circular cross-section near the root \((0 \leq x \leq R/4)\) and the same rectangular cross-section in the outboard region \((R/4 \leq x \leq R)\) was constructed to mimic the cylindrical root section of conventional blades. The circular cross-section had a wall thickness-to-chord ratio of \( t_{m,1}/c = 8.72\% \); the rectangular cross-section had \( t_{m,2}/c = 11.08\% \). A biplane beam (figures 7(c) and 7(d)) was also constructed to mimic the box-beam structure that would be used in a biplane blade. For this analysis, the beam was symmetric about the \( xy\)-plane. The outboard monoplane region \((r_{MR} < x < R)\) used the same rectangular cross-section \((t_m/c = 11.08\%)\) as the first monoplane beam. The inboard biplane region \((0 < x < r_{MR})\) used a rectangular cross-section with thinner walls \((t_b/c = 5.0\%\) wall thickness-to-chord ratio\). The area of this thinner-walled cross-section was half of the area of the thicker-walled cross-section. This kept the weight of the biplane beam equal to the weight of both monoplane beams.

2. Load Profiles and Boundary Conditions

To approximate the flap-wise bending moment (the primary load on wind turbine blades\(^30\)), three canonical bending load profiles were applied to the model beams: a point load, a constant load distribution, and a triangular load distribution (figure 8). The point load magnitude was 10,000 N \((F_o = 1000)\). The maximum magnitude of the constant and triangular load distributions was 1,000 N/m. In particular, the triangular load distribution was chosen to approximate the load distribution that develops on a wind turbine blade during operation (largest loads near the tip and smallest loads near the root). To load the monoplane beams and the biplane beam in an equivalent manner, it was assumed that the loads on the inboard biplane region of the biplane beam were equally distributed among the upper and lower beams (figure 8(f)). All beams were cantilevered at the root \((x = 0 \text{ m})\) and free at the tip \((x = 50 \text{ m})\).

3. Validation of 1D Beam Finite Element Approach

Two approaches were used to carry out a linear static analysis on both monoplane beams and one biplane beam configuration. The biplane beam had design parameter values of \( r_{MR}/R = 0.6, r_b/r_{MR} = 0.4, \) and \( g/c = 2.0 \). Each of the load profiles described above was applied to each beam. In the first approach, the 3D structure of each beam was approximated with 1D beam finite elements. In the second approach, the 3D structure of each beam was approximated with 3D tetrahedral finite elements.

Using the first approach, each beam was modeled with 3rd-order 1D Timoshenko beam finite elements (figure 9(a)) in DYMORE 3.0, a flexible multibody dynamics finite element program.\(^{21,22}\) For this linear static analysis, DYMORE solved the equation...
\[ [S] \{q\} = \{f\} \] (3)

The 2D cross-sectional properties of these beam elements were calculated with the VABS 3.4 code, also known as Variational Asymptotic Beam Sectional analysis. VABS uses the variational asymptotic method to accurately calculate the mass and stiffness matrices for arbitrary beam cross-sections. The 6 \times 6 mass matrix \([M]\) can be determined from the equation for the kinetic energy density \(K\), such that

\[
K = \frac{1}{2} \begin{bmatrix}
V_1 & V_2 & V_3 & \Omega_1 & \Omega_2 & \Omega_3 \\
V_1 & 0 & 0 & 0 & 0 & 0 \\
V_2 & 0 & \hat{\mu} & 0 & 0 & 0 \\
V_3 & 0 & 0 & \hat{\mu} & 0 & 0 \\
\Omega_1 & 0 & -\hat{\mu} & 0 & -i_{22} & -i_{23} \\
\Omega_2 & 0 & 0 & -i_{22} & \hat{\mu} & 0 \\
\Omega_3 & 0 & -i_{23} & 0 & 0 & \hat{\mu}
\end{bmatrix}
\] (4)

VABS uses a generalized Timoshenko beam model, whose 6 \times 6 stiffness matrix \([S]\) can be determined from the equation for the strain energy density \(U\), such that
Figure 8. Load profiles applied to model beams

(a) point load, monoplane beam

(b) point load, biplane beam

(c) constant load distribution, monoplane beam

(d) equivalent constant load distribution, biplane beam

(e) triangular load distribution, monoplane beam

(f) equivalent triangular load distribution, biplane beam

Figure 9. Computational meshes used for finite element analysis

(a) Biplane beam discretized with 1D beam finite elements

(b) Biplane beam discretized with 3D tetrahedral finite elements
$U = \frac{1}{2} \begin{bmatrix} \gamma_{11} \\ 2\gamma_{12} \\ 2\gamma_{13} \\ \kappa_1 \\ \kappa_2 \\ \kappa_3 \end{bmatrix}^T \begin{bmatrix} S_{11} & S_{12} & S_{13} & S_{14} & S_{15} & S_{16} \\ S_{12} & S_{22} & S_{23} & S_{24} & S_{25} & S_{26} \\ S_{13} & S_{23} & S_{33} & S_{34} & S_{35} & S_{36} \\ S_{14} & S_{24} & S_{34} & S_{44} & S_{45} & S_{46} \\ S_{15} & S_{25} & S_{35} & S_{45} & S_{55} & S_{56} \\ S_{16} & S_{26} & S_{36} & S_{46} & S_{56} & S_{66} \end{bmatrix} \begin{bmatrix} \gamma_{11} \\ 2\gamma_{12} \\ 2\gamma_{13} \\ \kappa_1 \\ \kappa_2 \\ \kappa_3 \end{bmatrix} \tag{5}$

DYMORE and VABS were selected as structural analysis tools because they are fast, and they have successfully modeled helicopter blades and wind turbine blades. Furthermore, Sivaji et al. also used a 1D beam approach for the preliminary structural analysis of a joined wing structure.

Using the second approach, each beam was modeled with 3D tetrahedral finite elements (figure 9(b)) in the Structural Mechanics module of COMSOL Multiphysics 4.1.0.88. For this linear static analysis, COMSOL also solved Eq. (3). This 3D analysis was conducted to validate the 1D beam approach, so that DYMORE and VABS could be confidently used to quickly analyze several different biplane beam configurations.

### III. Results

#### A. Cross-sectional Properties of a Thick Monoplane and Biplane

The FFA-W3-301 thick monoplane cross-section has principal area moments of inertia $I_x = 8.202 \times 10^{-4}$ m$^4$ and $I_y = 7.848 \times 10^{-3}$ m$^4$, a projected frontal area of 0.301 square meters per unit span, and a wetted surface area of 2.168 square meters per unit span. The SC(2)-0714 biplane cross-section has principal area moments of inertia $I_x = 1.165 \times 10^{-2}$ m$^4$ and $I_y = 1.288 \times 10^{-2}$ m$^4$, a projected frontal area of 0.280 square meters per unit span, and a wetted surface area of 4.134 square meters per unit span. Comparatively, the principal area moments of inertia for the biplane are about one order of magnitude greater than those for the thick monoplane. The projected frontal area of the biplane is slightly less than the frontal area of the thick monoplane. Finally, the wetted surface area of the biplane is nearly twice that of the thick monoplane. Two of the three cross-sectional properties for the biplane are better than those for the thick monoplane; this motivates further comparisons of the two cross-sections with CFD, as well as a structural comparison of monoplane and biplane beams.

#### B. Aerodynamic Comparison of a Thick Monoplane and Biplane Cross-section

Lift and drag coefficients of both the thick monoplane and biplane were calculated from the integrated pressure force along each of the airfoil walls. The lift and drag coefficients calculated with CFD for the FFA-W3-301 thick monoplane were then compared to experimental data reported by Fuglsang et al. The pressure and viscous parts of the lift and drag data were also reported from CFD to study the effect of the biplane’s increased surface area on viscous drag. Finally, the lift-to-drag ratio at each angle of attack was calculated to compare the aerodynamic efficiency of the thick monoplane and the biplane.

Numerical and experimental results for the FFA-W3-301 thick monoplane match well for angles of attack between $0^\circ < \alpha \lesssim 10^\circ$ (figure 10). Above this range, CFD calculations did not predict lift and drag coefficients measured in wind tunnel experiments by Fuglsang et al. The CFD results predict that the FFA-W3-301 thick monoplane would stall near $\alpha \approx 14^\circ$, while experimental measurements show an earlier stall near $\alpha \approx 10^\circ$. Thus, the CFD results overpredict the lift and underpredict the drag when $\alpha \geq 10^\circ$. CFD also slightly overpredicts the drag when $3^\circ \lesssim \alpha \leq 10^\circ$.

Overall, CFD calculations show that the SC(2)-0714 biplane outperforms the FFA-W3-301 thick monoplane for angles of attack between $0^\circ \leq \alpha \lesssim 15.5^\circ$ (figure 11). The lift generated by the biplane for each angle of attack is always greater than the lift generated by the thick monoplane (figure 11(a)). The pressure drag on the biplane is always smaller than the pressure drag on the thick monoplane. Although the viscous drag on the biplane is always greater than the viscous drag on the thick monoplane, the total drag for the biplane is always less than the total drag for the thick monoplane. As a result, the lift-to-drag ratio of the biplane is always greater than the lift-to-drag ratio of the thick monoplane (figure 11(b)). However, the results for angles of attack $\alpha \gtrsim 10^\circ$ in figure 11 may be inaccurate because, as mentioned earlier, numerical and experimental results for the thick monoplane only match well for angles of attack between $0^\circ < \alpha \lesssim 10^\circ$ (figure 10).

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C. Structural Comparison of Monoplane Beams and Biplane Beams

1. Validation of 1D Beam Finite Element Approach

Both monoplane beams and one biplane beam configuration \( (r_{MR}/R = 0.6, r_b/r_{MR} = 0.4, g/c = 2.0) \) were used to validate the 1D beam finite element analysis against a 3D finite element analysis for each of the load profiles described in section II.C.2. Figure 12(a) shows the deflections calculated by both approaches. For the point load, the tip deflections calculated by each approach for the point load differ by 0.27% (monoplane beam, hollow rectangular cross-section), 8.48% (monoplane beam, 25% circular - 75% rectangular cross-section), and 2.76% (biplane beam). For the constant load distribution, the tip deflections differ by 0.64% (monoplane beam, rectangular cross-section), 10.17% (monoplane beam, 25% circular - 75% rectangular cross-section), and 3.42% (biplane beam). For the triangular load distribution, the tip deflections differ by 0.38% (monoplane beam, rectangular cross-section), 9.79% (monoplane beam, 25% circular - 75% rectangular cross-section), and 3.31% (biplane beam). Although the tip deflections of the monoplane beam with 25% circular - 75% rectangular cross-section have higher errors than the other two model beams, the results still show decent agreement between the 1D and 3D approaches. The disagreement is likely due to the sharp gradient in this beam’s cross-sectional properties at \( x = R/4 \), which cannot be resolved well by the mesh of 1D beam elements. For a triangular load distribution, which approximates the loading on a wind turbine blade during operation, the tip deflection of the biplane blade is less than 30% of the tip deflection of a monoplane blade (figure 12(b)).

2. Parametric Analysis of Different Biplane Beam Configurations

Several biplane beam configurations were constructed by varying three design parameters \( (r_{MR}/R = 0.2, 0.3, \ldots 0.8; r_b/r_{MR} = 0.2, 0.3, \ldots 0.8; g/c = 0.5, 0.6, \ldots 2.0) \), while the total beam length \( R = 50 \) m was held constant. From all the permutations of these three parameters, 784 biplane beam configurations were constructed. Three load profiles were applied to each biplane beam configuration, and DYMORE was used to calculate the tip deflections. The effect of three design parameters \( (r_{MR}/R, r_b/r_{MR}, \text{and } g/c) \) on the tip deflection of all 784 biplane beam configurations was evaluated (figure 13).

Overall, tip deflections decrease as \( r_{MR}/R \) increases. For a given \( r_{MR}/R \), tip deflections always decrease as \( g/c \) increases. When \( 0.2 \leq r_{MR}/R \leq 0.4 \), tip deflections decrease as \( r_b/r_{MR} \) increases. However, for \( 0.5 \leq r_{MR}/R \leq 0.8 \), tip deflections show a dependence on both \( r_b/r_{MR} \) and \( g/c \). When \( g/c \) was small (\( \approx 0.5 \)), tip deflections decrease as \( r_b/r_{MR} \) increases. When \( g/c \) was large (\( \approx 2.0 \)), tip deflections sometimes increase as \( r_b/r_{MR} \) increases. As \( r_{MR}/R \) increases, the tip deflection is more likely to increase with \( r_b/r_{MR} \) across a wider range of \( g/c \) values. For example, at \( r_{MR}/R = 0.5 \), this behavior is seen when \( 1.9 \leq g/c \leq 2.0 \), but at \( r_{MR}/R = 0.8 \), this behavior is seen for a much wider range of \( 0.75 \leq g/c \leq 2.0 \). Only results from the triangular load distribution are shown in figure 13. Results from other load profiles gave similar results and are not shown.
3. Monoplane Beams and Biplane Beams with Equal Tip Deflections Under Various Loads

Out of the 784 biplane beam configurations investigated earlier (all of length $R = 50$ m), one configuration with small tip deflections was chosen for further analysis ($r_{MR}/R = 0.6$, $r_b/r_{MR} = 0.4$, and $g/c = 2$). In dimensional quantities, the inboard biplane region of this configuration had dimensions of $r_{MR} = 30$ m, $r_b = 12$ m, and $g = 2$ m. In order to determine how much longer this biplane beam could be until its tip deflection was equal to a monoplane beam, several new biplane beam configurations were constructed. Each configuration had identical inboard dimensions ($r_{MR} = 30$ m, $r_b = 12$ m, and $g = 2$ m), while the outboard monoplane region ($r_{MR} < x < R$) was lengthened.

Using the 1D approach, tip deflections were calculated for both monoplane beams (each had length $R = 50$ m). For this study, the “equivalent” biplane beam length was defined as the length $R$ at which the biplane beam tip deflection was equal to the monoplane beam tip deflection under the same load profile. For both monoplane beams and all load profiles, the equivalent biplane beam length is always longer than the monoplane beam length of 50 m (table 1). The 25% circular - 75% rectangular cross-section monoplane beam have smaller tip deflections than the hollow rectangular cross-section monoplane beam. The equivalent biplane beam length for the 25% circular - 75% rectangular cross-section monoplane beam is also smaller than the equivalent biplane beam length for the hollow rectangular cross-section monoplane beam.

Using the values from table 1, the power benefit of using a biplane blade is potentially very large because the blade length can be increased. Hence, the rotor swept area of a turbine would also increase. Even small increases in blade length are significant, because a rotor’s power scales with the square of its radius. For example, a rotor with radius $R = 55$ m would yield on average 21% more power than a rotor with radius $R = 50$ m.
(a) Deflection of three model beams under equivalent load profiles. 1D results are shown with points, whereas 3D results are shown with lines. 1D DYMORE results: monoplane beam with a rectangular cross-section under a point load (■), constant load distribution (▲), and triangular load distribution (▲); monoplane beam with 25% circular - 75% rectangular cross-section under a point load (■), constant load (■), and triangular load (△); biplane beam with \( r_{MR}/R = 0.6, r_b/r_{MR} = 0.4, \) and \( g/c = 2.0 \) under a point load (○), constant load (○), and triangular load (△). 3D COMSOL results: monoplane beam with rectangular cross-section (bold dashed line), monoplane beam with 25% circular - 75% hollow rectangular cross-section (half-filled triangles), biplane beam with \( r_{MR}/R = 0.6, r_b/r_{MR} = 0.4, \) and \( g/c = 2.0 \) (△). 3D COMSOL results: monoplane beam with rectangular cross-section (bold dashed line), monoplane beam with 25% circular - 75% hollow rectangular cross-section (medium dashed line), and biplane beam (thin dashed line).

(b) Deflection of three model beams under equivalent triangular load distributions. 1D results are shown with points, whereas 3D results are shown with lines. 1D DYMORE results: monoplane beam with a rectangular cross-section (▲), monoplane beam with 25% circular - 75% hollow rectangular cross-section (half-filled triangles), biplane beam with \( r_{MR}/R = 0.6, r_b/r_{MR} = 0.4, \) and \( g/c = 2.0 \) (△). 3D COMSOL results: monoplane beam with rectangular cross-section (bold dashed line), monoplane beam with 25% circular - 75% hollow rectangular cross-section (medium dashed line), and biplane beam (thin dashed line).

Figure 12. Comparison of beam deflections under various load profiles, calculated with DYMORE (1D approach) and COMSOL (3D approach)
A 10,000 N load was applied at the tip of the beam ($a$ = 10 m), and a 1,000 N/m load was applied along the entire length of the beam ($b$ = 50 m). The monoplane region of the biplane beam ($r_{MB} < x < R$) was lengthened until the biplane beam and monoplane beam tip deflection were equal.

For the equivalent biplane beam with tip deflection equal to the monoplane beam (25% circular - 75% rectangular cross-section), the load linearly increased from zero at the root ($x = 0$) to 1,251 N/m at the tip ($x = R$). For the equivalent biplane beam with tip deflection equal to the monoplane beam (hollow rectangular cross-section), the distributed load linearly increased from zero at the root to 1,403 N/m at the tip. Both distributed loads applied 1,000 N/m to the beams at $x = 50$ m.

Table 1. Monoplane beams and biplane beams with equal tip deflections under various loads

<table>
<thead>
<tr>
<th>Load profile</th>
<th>25% circular - 75% rectangular cross-section, monoplane beam (length $R = 50$ m)</th>
<th>Hollow rectangular cross-section, monoplane beam (length $R = 50$ m)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Deflection at tip, m</td>
<td>Equivalent$^a$</td>
</tr>
<tr>
<td>-----------------------------------</td>
<td>----------------------</td>
<td>----------------</td>
</tr>
<tr>
<td>Point load$^b$</td>
<td>1.205</td>
<td>65.254</td>
</tr>
<tr>
<td>Constant load distribution$^c$</td>
<td>1.842</td>
<td>65.511</td>
</tr>
<tr>
<td>Triangular load distribution$^d$</td>
<td>1.435</td>
<td>62.562</td>
</tr>
</tbody>
</table>

$^a$This biplane configuration had three design parameters fixed (see figure 7(c)): $r_{MB} = 30$ m, $r_b = 12$ m, and $g = 2$ m. The monoplane region of the biplane beam ($r_{MB} < x < R$) was lengthened until the biplane beam and monoplane beam tip deflection were equal.

$^b$A 10,000 N load was applied at the tip of the beam ($a = R$).

$^c$A 1,000 N/m load was applied along the entire length of the beam ($0 < x < R$).

$^d$For the equivalent biplane beam with tip deflection equal to the monoplane beam (25% circular - 75% rectangular cross-section), the load linearly increased from zero at the root ($x = 0$) to 1,251 N/m at the tip ($x = R$). For the equivalent biplane beam with tip deflection equal to the monoplane beam (hollow rectangular cross-section), the distributed load linearly increased from zero at the root to 1,403 N/m at the tip. Both distributed loads applied 1,000 N/m to the beams at $x = 50$ m.
IV. Conclusion

The results show that the biplane blade significantly improves both the aerodynamic and structural characteristics of the blade. As discussed in section III.A, it was expected that the smaller thickness-to-chord ratio of the SC(2)-0714 airfoil would create less pressure drag than the thicker FFA-W3-301 airfoil. This aerodynamic benefit was confirmed in section III.B, where CFD calculations show that the lift-to-drag ratio of the biplane is much greater than the lift-to-drag ratio of the thick monoplane for the angles of attack investigated (0–15.5°). Although the projected frontal areas of the biplane and thick monoplane are about equal, the biplane has less pressure drag than the thick monoplane because the biplane configuration splits the frontal area between two separate airfoils. The biplane also has less total drag than the thick monoplane, despite having nearly twice the wetted surface area of the thick monoplane and more viscous drag. As presented in section III.A, the principal area moments of inertia for the biplane were about one order of magnitude greater than those for the thick monoplane. This structural benefit was confirmed in section III.C through a comparison of monoplane and biplane beams. For a triangular load distribution, which approximates the loading on a wind turbine blade during operation, the tip deflection of the biplane blade is less than 30% of the tip deflection of a monoplane blade. A parametric analysis of different biplane beam configurations shows that tip deflections of biplane beams are reduced overall as each of the design parameters (r_{MR}/R, r_{b}/r_{MR}, and h/c) are increased. Thus, for a monoplane beam of fixed length, it is possible to construct a longer biplane beam with an equal tip deflection.

The aerodynamic and structural benefits described above suggest that the biplane blade is an attractive design for the next generation of large wind turbine blades. Aerodynamic performance improvements to the inboard region can lower the cut-in wind speed at which a needed to start rotating a wind turbine rotor. These improvements could also decrease vortex shedding and improve the spacing of turbines in wind farms. Most importantly, structural performance improvements can increase the length of wind turbine blades. Hence, the rotor swept area of a turbine would also increase. Even small increases in blade length are significant, because a rotor’s power scales with the square of its radius. For example, a 55 m radius rotor would yield on average 21% more power than a 50 m radius rotor. Thus, it is likely that this approach will be significant for large (3-7 megawatt) and ultra-large (8-10 megawatt) turbines for both land-based and offshore applications.

A. Future Work

Aerodynamic work will continue with CFD to obtain validated results up through high angles of attack. In the future, wind tunnel experiments will also be conducted on biplane cross-sections up through high angles of attack. Structural work will be extended to consider more realistic geometries for monoplane beams and biplane beams under actual operating loads. These loads can be obtained from wind turbine Blade Element Momentum codes. The finite element analysis can also be extended to measure internal stresses, perform dynamic simulations, and analyze buckling modes of the biplane beam.

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