Active stall control for large offshore horizontal axis wind turbines; a conceptual study considering different actuation methods.

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Abstract.

The increasing size of Horizontal Axis Wind Turbines and the trend to install wind farms further offshore demand more robust design options. If the pitch system could be eliminated, the availability of Horizontal Axis Wind Turbines should increase. This research investigates the use of active stall control to regulate power production in replacement of the pitch system. A feasibility study is conducted using a blade element momentum code and taking the National Renewable Energy Laboratory 5 MW turbine as baseline case. Considering half of the blade span is equipped with actuators, the required change in the lift coefficient to regulate power was estimated in $\Delta C_l = 0.7$. Three actuation technologies are investigated, namely Boundary Layer Transpiration, Trailing Edge Jets and Dielectric Barrier Discharge actuators. Results indicate the authority of the actuators considered is not sufficient to regulate power, since the change in the lift coefficient is not large enough. Active stall control of Horizontal Axis Wind Turbines appears feasible only if the rotor is re-designed from the start to incorporate active-stall devices.

Nomenclature

| Symbol | Description | Units |
|--------|-------------|-------|
| $A$    | Area        | $m^2$ |
| $A_p$  | Plasma force field amplitude | $Nm^{-3}$ |
| $B$    | Number of Blades | [-] |
| $C_d$  | Drag Coefficient | [-] |
| $C_l$  | Lift Coefficient | [-] |
| $C_p$  | Power Coefficient | [-] |
| $C_p0$ | Canonical Pressure Coefficient | [-] |
| $C_n$  | Normal Force Coefficient | [-] |
| $C_n0$ | Normal Force Coefficient at $\alpha = 0$deg | [-] |
| $C_T$  | Thrust Coefficient | [-] |
| $C_T^*$| Jet Momentum Coefficient | [-] |
| $D$    | Drag       | $N$  |

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Tip loss factor [-]

Plasma actuator force [N]

Force normal to the rotor plane [N]

Force tangential to the rotor plane [N]

Force exerted on the air by the wall [N]

Plasma body force density [Nm⁻³]

Boundary layer shape factor [-]

Empirical constant for separation criterion [-]

Lift [N]

Spanwise actuation length [m]

Power [W]

Power for boundary layer transpiration [W]

Power for dielectric barrier discharge [W]

Power for trailing edge jets [W]

Torque [Nm]

Turbine radius [m]

Reynolds number [-]

Thrust [N]

Wind Speed [ms⁻¹]

Local velocity external to the boundary layer [ms⁻¹]

Jet velocity [ms⁻¹]

Maximum air velocity over the airfoil contour [ms⁻¹]

Relative blowing velocity [ms⁻¹]

Effective Velocity [ms⁻¹]

Axial induction factor [-]

Tangential induction factor [-]

Chord [m]

Chord with porosity [m]

Separation location [-]

Empirical coefficient for tip-correction [-]

Force field height [m]

Chordwise width of the jet exit [m]

Force field length [m]

Jet mass flow rate [kgs⁻¹]

Boundary layer transpiration mass flow rate [kgs⁻¹]

Pressure [Pa]

Minimum pressure at the airfoil [Pa]

Local radius [m]

Airfoil contour coordinate [m]

Velocity in the x direction [ms⁻¹]

Velocity in the y direction [ms⁻¹]

Horizontal coordinate [m]

Laminar coordinate [m]

Suction peak coordinate [m]

Transition coordinate [m]

Turbulent coordinate [m]

Effective boundary layer length [m]

Vertical coordinate [m]

Greek Letters
\( \phi \) inflow angle [deg]
\( \alpha \) angle of attack [deg]
\( \beta \) pressure gradient parameter [-]
\( \theta \) twist angle [deg]
\( \theta \) momentum thickness [m]
\( \delta^* \) displacement thickness [m]
\( \delta \) boundary layer thickness [m]
\( \Omega \) rotational speed [s\(^{-1}\)]
\( \lambda \) tip speed ratio [-]
\( \rho \) air density [kgm\(^{-3}\)]
\( \nu \) kinematic viscosity [m\(^2\)s\(^{-1}\)]
\( \eta_{\text{pump}} \) pump efficiency [-]

**Acronyms**

- AOA: Angle of Attack
- BEM: Blade Element Momentum
- BL: Boundary Layer
- BLT: Boundary Layer Transpiration
- DBD: Dielectric Barrier Discharge
- HAWT: Horizontal Axis Wind Turbine
- LE: Leading Edge
- LHS: Left Hand Side
- NREL: National Renewable Energy Laboratory
- RHS: Right Hand Side
- TE: Trailing Edge
- TEJ: Trailing Edge Jet

1. Introduction

The tendency to increase the size of offshore Horizontal Axis Wind Turbines (HAWT), together with the trend of installing wind farms further offshore, drives the search for more robust designs. Modern HAWTs deployed offshore are variable speed and pitch-controlled [1]. However if the pitch system can be eliminated the maintenance costs are expected to decrease and the availability will increase, which might end up in a lower cost of energy. The power harvested by a wind turbine rotor is given by:

\[
P = \Omega Q
\]  
(1)

where \( \Omega \) is the rotational speed and \( Q \) represents the aerodynamic torque. Usually wind turbines are designed to produce electrical power at different wind speeds, \( U \), and power control strategies are required to regulate power production. In this respect, modern HAWTs can be divided in two main types, fixed-pitch and variable-pitch machines. For wind speeds below rated, both control solutions impose a constant tip-speed-ratio, \( \lambda = \Omega R/U \), by increasing the rotational speed as the wind speed increases. Such a control strategy keeps the HAWT at the optimum operational point, maximizing aerodynamic efficiency. This maximizes the power coefficient, \( C_P \), defined as:

\[
C_P = \frac{P}{0.5\rho_\infty \pi R^2 U^3}
\]  
(2)

where \( \rho_\infty \) is the air density and \( R \) represents the turbine’s radius. In other words, for low wind speeds HAWTs harvest as much power as they are capable of. Near and above rated wind speeds the control strategy differs. The rotational speed is usually limited above rated wind speed since
the aerodynamic forces are proportional to $\Omega^2$. Designing HAWTs for larger rated power means designing the blades to withstand higher loads, which is more expensive, and it is not profitable since very large wind speeds do not occur often. This means that at wind speeds above rated it becomes necessary to "waste" some aerodynamic power. In variable-pitch machines the blades are pitched for wind speeds above rated [2], usually decreasing the angle of attack (AOA). This is done in such a way that the rotor aerodynamic torque is kept constant, and equal to the rated generator torque. The rotational speed is also kept constant and thus the power output is constant above rated wind speed. For fixed-pitch machines, the most efficient way of regulating power at wind speeds above rated is to decrease the rotational speed [15], increasing the AOA beyond the stall angle. However, keeping the power constant above rated wind speeds means that the generator must handle larger torque magnitudes than at rated wind speed. Comparing both solutions, pitch-controlled HAWTs are expected to require more maintenance, because of the pitch mechanism. According to [15] the pitch mechanism and bearings have a typical failure rate which amounts to 14.3% of the total components failure. Fixed-pitch machines however require a larger, more expensive generator, to cope with the large torques experienced above rated wind speeds. For the same rated power, the generator in a fixed-pitch HAWT must be 40% larger, and more expensive [15], than for a variable-pitch machine.

The challenge is thus to come to a solution that combines the advantages of both designs, i.e. eliminating the pitch system while keeping the torque bounded below the value occurring at rated wind speed. This means that circulation at the blade section, and hence the loading, must be controlled without pitching the blade, i.e. without changing the angle of attack. The present study investigates the feasibility of using active stall control rotors as an alternative for pitch controlled rotor blades. Active stall control in the context of the present research means the application of add-ons that actively provoke stall. The National Renewable Energy Laboratory (NREL) 5MW machine [2] is used as a benchmark. A Blade Element Momentum (BEM) code is used to evaluate three different actuation technologies for active stall control: Boundary Layer Transpiration (BLT), Trailing Edge Jets (TEJ), and Dielectric Barrier Discharges (DBD).

2. Blade Element Momentum Method

The BEM method is used to simulate the HAWT aerodynamics. For each radial section, the local forces acting on the blade are decomposed considering figure 1.

$$\Omega (1 + a')$$

$$U (1 - a)$$

where $V_{eff}$ is the effective speed at the blade section at radius $r$, and $a$ and $a'$ are the axial...
and tangential induction factors, respectively. The inflow angle is expressed by $\phi$, and the local twist and AOA are represented by $\theta$ and $\alpha$ respectively. The aerodynamic forces acting on the blade section can be decomposed into components parallel and normal to the rotor plane, according to

$$\begin{align*}
\{ & dF_{n_b} = dL \cos \phi + dD \sin \phi \\
& dF_{t_b} = dL \sin \phi - dD \cos \phi \}
\end{align*}$$

(3)

where $dL$ and $dD$ represent the section’s lift and drag, $dF_n$ is the force component normal to the rotor plane and $dF_t$ is the force component tangential to the rotor plane. The subscript ’$b$’ denotes the force decomposition refers to a single blade. Integrating the axial force over the entire blade and summing for the ’$B$’ blades, the thrust force is obtained, $T$. Integrating the tangential force multiplied by the local radius over the whole blade and summing for the ’$B$’ blades, the aerodynamic torque is obtained, represented by $Q$. BEM models divide the streamtube of the HAWT rotor in concentric annuli, assuming each ring at a given radial position is not influenced by the others. For each radial position, BEM models assume values of the axial and tangential induction factor, and based on the geometry of the blade, they compute the local angle of attack. Lift and drag of each blade section are then calculated using look-up tables for the lift and drag coefficient as a function of AOA. These look-up tables take into account rotational augmentation affecting the inboard blade sections. By considering all blades the axial force at each annulus is obtained. Assuming axisymmetric operation, this axial force corresponds to the thrust at each radial section:

$$dT = B (dL \cos \phi + dD \sin \phi)$$

(4)

Finally the axial force is inserted in the momentum equation and the axial induction factor of each radial station is iterated until convergence is reached, using:

$$C_t(r) \equiv \frac{dT}{0.5 \rho_\infty U^2 2 \pi r \, dr} = 4a(r)(1 - a(r))$$

(5)

where $C_t$ is the thrust coefficient. The present study does not consider heavily loaded rotors since the focus is on wind speeds above rated. Accordingly expression 5 is not corrected for the turbulent wake state[17]. The tangential induction factor is derived from the axial induction factor at each radial section by matching the power production from the actuator disk and from the blade element decomposition, according to [17]. The present BEM model includes the so called ”tip-correction”, from Shen et al [10], which accounts for the fact there is a finite number of blades using a factor $F$:

$$F = \frac{2}{\pi} \cos \left[ \exp \left( -g \frac{B(R - r)}{2R \sin \phi} \right) \right]$$

(6)

where $g$ is an empirical coefficient dependent on the number of blades and tip-speed-ratio, expressed by:

$$g = \exp \left[ -0.125(B\lambda - 21) \right] + 0.1$$

(7)

The empirical coefficients of Shen’s model were derived using experimental data from two distinct rotors at different tip-speed ratios [10]. Finally the sectional blade aerodynamic coefficients are calculated using:

$$\begin{align*}
C_{l,corr}^t &= FC_t \\
C_{d,corr} &= FC_d
\end{align*}$$

(8)
where $C_l$ and $C_d$ are the lift and drag coefficients respectively, without the tip-correction, and the '$corr'$ superscript denotes the coefficient with the tip-correction. The lift and drag coefficients are defined with:

\[
\begin{align*}
C_l &= \frac{dL}{0.5\rho cV_{eff}^2} \\
C_d &= \frac{dD}{0.5\rho cV_{eff}^2}
\end{align*}
\]  

(9)

where $c$ represents the airfoil section’s chord. The implemented BEM code has been validated (not shown) against the commercial wind turbine design tool GH Bladed\[29\].

3. Required Authority

Regardless of the actuation type employed, one must assess how much actuator authority is necessary to keep the power output constant at wind speeds above rated. The larger the actuated area the larger the authority of the whole control system will be, for a given authority of the individual actuators. The actuated section of the HAWT blades is considered to start from the tip of the blades because outboard sections provide greater control over the blade loads, since they contribute more to $T$ and $Q$ due to the larger dynamic pressure and larger radius. This choice is also made because near the root stall delay is expected \[16\], since the local magnitude of the Coriolis force hampers flow separation. In other words, any actuator with the purpose of promoting flow separation, and thus decrease circulation, is less effective in the root region, and it is thus justified to consider actuation in the outboard region of the blade. The NREL 5 MW machine \[2\] was used as a baseline turbine, and three different actuated lengths of the blade were considered, namely $L = \{21; 29; 37\}$ m, measured from the tip of the blade. These lengths match the transition of the airfoil sections in the NREL 5 MW blade, as shown in figure 2.

![Figure 2: Considered Actuated Regions](image)

It is assumed that actuators change the sectional lift, and associated circulation, but not drag, i.e. it is assumed the drag is the same as obtained with an airfoil without actuation. In a fixed-pitch machine, increasing the wind speed beyond rated increases the AOA and consequentially drag; however, larger wind speeds also mean a larger inflow angle and thus a decreased contribution of the drag to the rotor torque. The sensitivity of the local power coefficient, $dC_P$, to changes in the lift and drag coefficient is expressed by:

\[
\Delta(dC_P) = \frac{\partial(dC_P)}{\partial C_l} \Delta C_l + \frac{\partial(dC_P)}{\partial C_d} \Delta C_d
\]

(10)

Assuming the rotational speed and wind speed at the rotor plane remain constant, regardless of the actuation employed, the partial derivatives are estimated:

\[
\begin{align*}
\frac{\partial(dC_P)}{\partial C_l} &= \frac{\Omega^3 \beta(r)^2}{2 \pi U^3} \sin\phi \\
\frac{\partial(dC_P)}{\partial C_d} &= \frac{\Omega^3 \beta(r)^2}{2 \pi U^3} \cos\phi
\end{align*}
\]

(11)
Clearly $C_P$ is more sensitive to changes in the drag coefficient since it is expected that $\phi < 45 \text{ deg}$ at the outboard sections, even at above-rated wind speeds. The absolute change in the drag coefficient should however be smaller than the absolute change in the lift coefficient. The magnitude of these changes is estimated by looking at the lift and drag polar of the outboard sections subjected to largest AOA, i.e. at cut-out wind speed, and comparing it with the optimum AOA. This is illustrated in table 1, where $\Delta C_{l_{\text{max}}}$ and $\Delta C_{d_{\text{max}}}$ are the maximum values of the aerodynamic coefficients occurring within the wind speed envelope considered:

|           | $C_{l_{\text{opt}}}$ | $C_{d_{\text{opt}}}$ | $C_{l_{\text{max}}}$ | $C_{d_{\text{max}}}$ | $\Delta C_l$ | $\Delta C_d$ |
|-----------|-----------------------|-----------------------|-----------------------|-----------------------|---------------|---------------|
| NACA64618 | 0.898                 | 0.005                 | 1.453                 | 0.118                 | 0.555         | 0.113         |
| DU93-W-210| 0.888                 | 0.007                 | 1.402                 | 0.108                 | 0.514         | 0.101         |

Table 1 shows $\Delta C_l$ is much larger than $\Delta C_d$. Still, the effect of changes in $C_d$ in the local power coefficient may be comparable to the effect of the variation in $C_l$, depending on the actuator and airfoil employed. Nevertheless, because $\Delta C_d$ is small and since no concrete information is available for the actuator-induced drag, it is assumed only the sectional lift changes. An actuator which decreases circulation also increases drag, since flow separation is provoked; this means assuming the drag remains the same with/without actuation overestimates the required $\Delta C_l$ to be imposed by the actuator.

Different values of $C_l$ are imposed over the actuated portion of the blades and the total aerodynamic power is computed for each combination of sectional $C_l$ and actuated length of the blade. This is done for different wind speeds above rated, $U = \{13; 17; 21; 25\}$ m/s, covering the above rated envelope of operation. For each of the wind speeds considered, results in figure 3 show the aerodynamic power obtained is varying linearly with imposed $C_l$. This is expected since the torque produced by a blade section is proportional to the local $C_l$, and also because the contribution of the unactuated inboard sections to the total aerodynamic torque is very small, compared to the actuated outboard region of the blade. The required $C_l$ to reduce the power produced to the rated value, for a given wind speed and actuated blade portion, is found at the intersection with the 'Rated' power line. It is clear that a substantial part of the blade needs to be actuated if the power is to be kept at the rated value, which is understandable since in a pitching HAWT circulation changes over the entire blade as it is pitched. The $C_l$ required to keep the aerodynamic torque at the rated value does not change significantly as different wind speeds are imposed, e.g. for $L = 29$ m we have $C_l \in [0.58; 0.7]$. It is also interesting to investigate what is the required $\Delta C_l$, with respect to the blade without actuation, and what AOA occur. Figure 4 illustrates this for an actuated length $L = 29$ m, for the considered wind speeds. The actuator must be able to decrease the lift coefficient considerably if power regulation is to be achieved. For the considered baseline turbine at the mid-span airfoil $\Delta C_l = 0.65$ with $\alpha \in [7.5; 22]$ , and at the tip region $\Delta C_l \in [0.52; 0.8]$ with $\alpha \in [7.5; 19]$ . One should keep in mind that these values are obtained for the baseline turbine, which was designed to be a variable pitch machine; nevertheless, the required change in the lift coefficient is quite large and is to be obtained at very large $\alpha$.

4. Actuator Simulation

In the past, several actuator types were contemplated for application in HAWTs. Distinct actuation objectives have been considered, such as emergency braking [30] or fatigue mitigation...
Figure 3: Power vs $C_l$ for different actuated lengths, at different wind speeds, $U = 13m/s$ (a), $U = 17m/s$ (b), $U = 21m/s$ (c), $U = 25m/s$ (d)

[31]. A good review of available actuation technologies is given in [32]. The present study focuses on 3 types of actuators, namely BLT, TEJ and DBD plasma actuators. These technologies were selected because they include no moving parts, which should reduce the maintenance required and hence contribute to increased actuation system robustness.

4.1. Boundary Layer Transpiration

The first option of active stall control considered is boundary layer with transpiration, i.e. with air being blown/sucked, perpendicularly to the airfoil surface. Because the goal is to decrease the circulation, in the present study only blowing is considered. The aerodynamic code $RFOIL\_suc\_V2$ is used to simulate BLT: this program is an adaptation of Drela’s XFOIL [4], using a viscous-inviscid algorithm in which the Euler equations are coupled to an integral boundary layer (BL) formulation. $RFOIL\_suc\_V2$ accounts for the stall delay caused by the HAWT blade’s rotation, based on user-provided radial pressure gradient and local solidity. In the present case however rotational effects are neglected since only outboard blade sections are considered. The code also allows for different porous chordwise regions and transpiration velocities to be imposed. $RFOIL\_suc\_V2$ is described and experimentally validated in [5], for different suction velocities and aft porous lengths. The transpiration velocities are modelled as perturbations, while the BL hypothesis is still assumed. More specifically, the closure relationship used for the skin friction coefficient to solve von Kármán’s integral BL equation
assumes that the turbulent structures are unchanged by the imposed transpiration [5]. In two dimensional incompressible flows, introducing the BL approximation in the continuity equation yields:

\[
\frac{\partial u}{\partial x} + \frac{\partial v}{\partial y} = 0 \implies O[v] = \frac{V_{\text{eff}}}{\sqrt{Re}}
\] (12)

where \(u\) and \(v\) are the velocities in \(x\) and \(y\) direction, respectively, and \(Re\) represents the Reynolds number of operation. Considering BLT, at each chordwise station the transpiration velocity coincides with \(v\). In the present case \(Re \approx 10 \times 10^6 \implies O[v] \approx V_{\text{eff}} \times 10^{-3}\), meaning the code from [5] should not be used while imposing large transpiration speeds since its validity becomes questionable. Also, the ability of this tool to predict airfoil flows with large separated regions is limited. This is because the effect of the BL on the potential flow is modelled only to first order effects, meaning that only small equivalent airfoil shape changes may be captured. Moreover the shape of the wake is calculated based on the inviscid solution, which again is only valid when the flow is separated over a small portion of the airfoil. Despite these limitations, this tool was considered adequate to estimate the potential for sectional lift coefficient manipulation through BLT. Employing more accurate methods such as direct numerical simulation implied a very large computational effort, and was not suitable for a preliminary study on active stall control of HAWTs.

Simulations are performed at \(Re = 10 \times 10^6\), which matches the conditions found at a 5MW machine at rated wind speeds and above. Different airfoils, different porous regions and various transpiration velocities are considered. Results show that when BL blowing is applied near the trailing edge (TE) the reduction in \(C_l\) is small, when compared to the clean configuration. As the AOA increases, TE blowing has practically no-effect since the flow in the TE region is anyhow
separated. When blowing is applied in the leading edge (LE) area considerable changes in the aerodynamic loading are obtained, and the $C_l$ remains practically constant for high angles of attack. Figure 5 shows the results obtained for the DU93-W210 and NACA 64618 profiles, which are used in the baseline turbine. The lift curves are obtained imposing a porous region located on the airfoil suction side, ranging from $x/c_{por} \in [0.05; 0.2]$. As the blowing speed increases

![Figure 5: $C_l$ vs $\alpha$ for different blowing speeds $V_b$ applied at $x/c \in [0.05; 0.2]$. Left- DU93 - W - 210 and Right - NACA64618](image)

the lift decreases, which is expected since separation takes place further upstream. The blowing velocities indicated, $V_b$, are relative to the free stream velocity; The relation of the lift decrease with the blowing speed is identical for both airfoils, e.g. $V_b = 0.006 \Rightarrow \Delta C_l \approx 0.2$. Also, the lift decrease compared to the unactuated case is much smaller before stall occurs naturally, i.e. for small AOA. The effect of the porous region location and length was tested. Prolonging the porous region further downstream did not decrease the lift coefficient, possibly because flow separation is anyhow taking place at chordwise positions more upstream than $x/c = 0.2$. When the porous region started before $x/c = 0.05$ convergence was only obtained for small angles of attack, meaning that the decrease in the lift coefficient provoked by the transpiration could not be quantified.

It appears that, for large angles of attack, the decrease in lift coefficient varies linearly with imposed blowing speed. Extrapolating this trend, if one assumes half the blade is actuated and the porous region ranges from $x/c_{por} \in [0.05; 0.2]$ , power regulation should be achieved imposing $V_b = 0.021$. Earlier separation could perhaps be obtained at a more upstream location, with a smaller mass flow, but this cannot be predicted with the aerodynamic tool used. Unfortunately, literature review did not find experimental data to support the computed values of $\Delta C_l$. This is because, to the authors’ knowledge, experiments with airfoils subjected to BLT were only performed for the suction case, since usually lift augmentation is aimed, rather that lift decrease. Experimental data was found [17] for both suction and blowing, but only for the flat plate case. Additionally, and to complicate the matters further, the expression ‘boundary layer blowing’ is often used in literature to refer to flow injection parallel to the free stream [18], energizing the BL, instead of what it is describing in the present text.

4.2. **Trailing Edge Jets**

There is interest in TE devices since they are capable of producing a significant change in lift [31]. As hinted by its designation, trailing edge jets (TEJ) are jets of air which exit the airfoil
contour located at the TE region. TEJs are usually placed at a chordwise position around \( x/c \in [0.9 - 0.95] \), and the blown air jet creates a stagnation streamline exiting the airfoil, different than the one obtained without actuation; this changes the Kutta condition and thus the circulation and aerodynamic loading of the airfoil section. If the TEJ is placed on the suction surface it will act to decrease the circulation, and vice-versa. When compared to other TE devices such as flaps, TEJ have fewer moving parts and should thus be more robust [6]. It is usual to characterize TEJs using the jet momentum coefficient, defined as:

\[
C_\mu = \frac{m_{\text{jet}} U_{\text{jet}}}{0.5 \rho_\infty U_\infty^2 A}
\]

where \( m_{\text{jet}} \) is the jet mass flow rate, \( U_{\text{jet}} \) is the jet velocity, \( \rho \) is the air density and \( A \) is the blade section area; this quantity expresses the ratio of the net momentum of the jet to the dynamic pressure of the free stream flow. For an incompressible fluid \( \rho_{\text{jet}}=\rho_\infty \), and in two dimensions expression 13 reduces to:

\[
C_\mu = \frac{U_{\text{jet}}^2 h_{\text{jet}}}{0.5 U_\infty^2 c}
\]

in which \( h_{\text{jet}} \) is the chordwise width of the jet exit. Results from [8] show \( Re \) has negligible influence on the \( \Delta C_l \) obtained with a given TEJ configuration. Figure 6 shows the effect of TEJ on the sectional lift of the NACA0018 airfoil. The change in lift is not symmetric for the lower and upper surface jets. According to [8], this is because the BL development is different at the two sides. For large angles of attack, the BL is thicker at the suction surface, which means TEJ will have a larger effect on pressure surface (which decreases \( C_l \)). It is clear that \( \Delta C_l \alpha \sqrt{C_\mu} \),

Figure 6: \( C_l \) of a NACA0018 airfoil at \( Re = 6.6 \times 10^5 \) and \( Ma = 0.176 \) with TEJ located at \( x/c = 0.9 \) and \( h_{\text{jet}} = 0.006c \) from [8]. Left- \( C_l \) vs \( \alpha \) for \( C_\mu = 0.012 \) and Right - \( C_l \) vs \( C_\mu \) for \( \alpha = 0 \)

which is also reported in[28]. Even though figure 6 illustrates the influence of TEJ for a specific airfoil, comparable results are found for different airfoils [8],[7]. Accordingly the present study assumes the same \( C_l = f(C_\mu) \) dependence applies. It is also verified that the authority of TEJ is reduced for large AOA since flow separation naturally occurs in the TE area. At AOA beyond stall the TEJ has no practically effect.

4.3. Dielectric Barrier Discharge Actuators
DBD actuators consist of two electrodes with a thickness in the order of \( 10^{-5} m \), separated by a dielectric with a thickness of up to a few mm. The air is ionized by applying a large cyclic
electric potential difference at the electrodes, and the local electric field transfers momentum to the air. The actuator is represented in figure 7 and the interested reader is referred to [9], [13]. These devices have no moving parts or pneumatic systems, which makes them attractive in applications where robustness is important, such as offshore wind turbines. The main issue associated with using the present DBD state of the art is that at large $Re$ there is not sufficient authority. However, by optimizing the electric signal and geometry of the actuator it is possible to produce a body force that is one order of magnitude larger [9] than previous studies indicate. A scheme of the working configuration of the DBD actuator on the HAWT blade is also indicated in figure 7.

In the present study the authority of the DBD actuator is analyzed by investigating whether the device is able to provoke separation under the flow conditions of the baseline turbine. In aerodynamics, separation is considered to take place when there is flow reversal, which implies there is a streamline exiting the airfoil surface. At this chordwise point the flow detaches from airfoil surface, i.e. it separates. If an airfoil operates at large $\alpha$, the flow on the suction-side naturally separates, leading to a decreased suction length and consequentially lift reduction. Accordingly, determining whether or not an actuator induces or delays separation enables the estimation of how much authority, in terms of $\Delta C_l$, is achieved. Over the years several criteria have been put forward to determine if separation occurs, different for laminar and turbulent flow. A good review is given in [19]. For simplicity in the current study it is initially assumed that the DBD acts as a point force by considering the total force transferred to the air. It is assumed that the actuator force $F_{act} = 0.2 \, N/m$, since this is the maximum value obtained in experimental studies dedicated to maximizing the actuator body force [9]. The actuator is assumed to operate at $V_{eff} = 60 \, m/s$, corresponding to the effective velocity at rated rotational speed with $r/R = 0.75$.

Laminar Flow
There is no consensus among the wind energy community on how much of the flow experienced by HAWT blades is laminar. While rotational effects tend to delay transition [23], blade contamination cannot be avoided [24] and leads to early transition. Moreover, wind turbulence intensity also influences the transition process [25]. Consequentially, in HAWT environment laminar flow should also be considered. In the current study Thwaites’ method [11] is used to predict separation in the laminar regime, according to which there is separation if
where $\beta$ is a pressure gradient parameter, $\theta$ is the BL momentum thickness, $\nu$ is the kinematic viscosity and $U_e$ is the local velocity external to the BL, obtained in inviscid flow. More complex methods of predicting laminar separation exist, but Thwaites’ approach is used in a straight-forward fashion since the momentum produced by the actuator can be supposed to modify the momentum thickness directly. The BL momentum thickness at each chordwise position translates the decrease in momentum the BL flow has sustained; this thickness is the distance perpendicular to the wall that, in inviscid flow, yields the momentum flux that matches the momentum deficit. By multiplying this distance with the density and local external velocity, the total force exerted on the air by the wall up to the considered chordwise position is determined[11]. This is expressed by:

$$F_{wall} = \rho \theta U_e^2$$

Assuming the density and external velocity do not change as actuation is employed, the new local momentum thickness is obtained from equation 16 by adding the actuator force to the wall force. Thwaites’ criterion is then employed to see whether separation is provoked.

Simulations were performed for the laminar regime, for $\alpha = \{8; 11; 14; 17\}$ deg. Figure 8 shows the value of the pressure gradient parameter $\beta$ obtained by applying the actuator at each chordwise position, and also the separation threshold. The actuator will increase the momentum thickness locally and not over the whole laminar BL. However, figure 8 provides insight on where it is more advantageous to place the actuator since it shows, for each chordwise station, what is the maximum increase (in absolute value) in $\beta$ obtained by employing a DBD actuator. Results are shown only for the laminar regime, i.e. upstream of the transition point since the criterion is only valid for laminar flow. Thwaites’ criterion was derived for a flat plate, but in the present case $\frac{dU_e}{dx}$ was replaced with $\frac{dU_e}{ds}$, where $ds$ represents the elemental contour of the airfoil in the LE area. Using more than one actuator yields a larger force of actuation, but it does not add linearly [9]. Different actuator forces were considered, ranging from 0.2 to 0.8 N/m. The aerodynamic code RFOIL was used to estimate the evolution of the BL variables along the contour of the NACA64618 airfoil $Re = 10 \times 10^6$. Minding figure 8 it is clear that the angle of attack of operation has a small influence on the location of the suction peak, indicated in the figures above by a zero value of $\beta$. The suction peak is moving upstream with increasing angles of attack. More importantly, the figures indicate it is not possible to provoke LE separation with DBD actuators operating in laminar flow since $\beta > -0.09$. This is the case for all the simulated $\alpha$, even considering rather optimistically an actuator force of 0.8 N/m.

**Turbulent Flow**

For turbulent flow, separation is predicted with the Stratford criterion[14]. Other turbulent separation criteria exist, mostly based on the local shape BL factor $H$. Such methods however cannot be used straightforwardly because only the total thrust of the actuator is known from experiments, which translates into a change in $\theta$; the change in BL displacement thickness $\delta^*$ is not known, and thus the change in $H$ cannot be directly estimated. According to Stratford separation takes place when:
where $C'_p$ is the canonical pressure coefficient and $x'$ is the effective BL length and $K$ is an empirical constant. The canonical pressure coefficient is defined based on the maximum speed over the airfoil, $U_{max}$, occurring at the suction peak. This is expressed by:

$$C'_p = \frac{p - p_{min}}{0.5 \rho U_{max}^2} \quad (18)$$

where $p_{min}$ is the minimum pressure, occurring at the suction peak. The effective BL length is introduced to account for the development of the BL prior to the suction peak. 'Effective length' is the length the BL would need to develop in a zero-pressure-gradient such that approximately the same values of the integral BL quantities are obtained. In the present case, the effective length is smaller than the actual length, since up to the suction peak the pressure gradient is favourable, and thus the boundary rate of growth is smaller than in a flat-plate configuration.
For the cases under consideration the suction peak is located upstream of the transition point, as seen in figure 8. According to [12], the effective laminar length at the suction peak is given by:

$$x'_{lam,suc} = \int_{0}^{x_{suc}} \left( \frac{U_e(x)}{U_{max}} \right)^5 dx$$  \hspace{1cm} (19)

where $x_{suc}$ denotes the chordwise position where the suction peak is located. From that point up to the transition location, the effective BL length is calculated with:

$$x'_{tr} = x'_{lam,suc} + (x_{tr} - x_{suc})$$  \hspace{1cm} (20)

Assuming that during transition the momentum thickness $\theta$ remains constant[11], the effective turbulent BL length is computed by equating it with the laminar momentum thickness and reversing the expression for the flat plate solution:

$$\theta_{tr,lam} = \theta_{tr,turb} = x'_{turb, tr} \left( \frac{0.036}{Re_x} \right)^{-\frac{1}{5}}$$  \hspace{1cm} (21)

where $Re$ is calculated using the free-stream velocity. Downstream of the transition point, the effective turbulent BL length is calculated using

$$x'_{turb} = x'_{turb, tr} + (x - x_{tr})$$  \hspace{1cm} (22)

One should note the position of the stagnation point changes as different $\alpha$ are imposed. This is taken into account by changing the lower limit of the integral in expression 19.

For the turbulent case it is necessary to assume more of the actuator’s characteristics than in the laminar regime. The geometric parameters of the DBD actuator are chosen to match the configuration yielding the largest thrust [9]. The dielectric material is Teflon with a thickness of 6.3 mm, the exposed electrode length is 12.7 mm and covered electrode length is 25 mm. By applying 25 kV$_{rms}$ at 2.2kHz in a positive sawtooth waveform this actuator should yield $F_{act} = 0.2N/m$. The body force volume is characterized by a height $h = 2$ mm, measured perpendicular to wall, and a length $l = 25$ mm, which is the same as the covered electrode. Regarding the spatial distribution of the force field, the body force density in the streamwise direction, with units N/m$^3$, is estimated with:

$$F_x = A \sin \left( \frac{\pi x}{l} \right) \sin \left( \frac{\pi y}{h} \right)$$  \hspace{1cm} (23)

where the origin of the coordinate system is situated at the upstream side of the exposed electrode, and $x \in [0;l]$ and $y \in [0;h]$. An illustration of the implemented body force spatial distribution is shown in figure 9. This body force distribution yields ellipse-like lines of iso-magnitude, and it is assumed based on the experimental results of [13]. The largest magnitude of the body force is obtained at ($x,y$) = ($l/2,h/2$) and it takes the value $A$, which can be calculated by integrating in $x$ and $y$ and substituting the values of the specific actuator under consideration:

$$F_{xTotal} = 0.2 = A \int_{0}^{h} \int_{0}^{l} \sin \left( \frac{\pi x}{l} \right) \sin \left( \frac{\pi y}{h} \right) dxdy = \frac{4A lh}{\pi} \Rightarrow A = 9870N/m^3$$

The turbulent separation criterion does not depend explicitly of the local velocity profile, and accordingly the BL is characterized by integral parameters. The actuator’s force distribution in the $y$ direction is thus collapsed into a single point, and the body force varies only in the $x$ direction according to:

$$F_x(x) = \frac{2A}{\pi} \sin(\pi x/l)$$  \hspace{1cm} (24)
The influence of the actuator on the flow is thus introduced in the separation criterion by noticing that the body force density, in $N/m^3$, is equivalent to a pressure gradient, $dp/dx$, in Pa/m. The effect of the DBD actuator is introduced in the LHS of equation 17. Different actuator chordwise positions and $\alpha$ are considered. The RHS of equation 17 is the threshold for separation to occur. Figure 10 shows that without actuation, the separation point moves upstream as $\alpha$ increases. This is indicated by the intersection of the case 'No Actuation' with 'Separation Threshold'. The influence of the actuator on the flow is signalled by the bulges at the different chordwise positions. The maximum value of each bulge is obtained at the centre of the encapsulated electrode, since at this point the body force and thus pressure gradient are largest, leading to a local maximum in the LHS of the separation criterion. The large drop immediately downstream of the actuator is explained because the pressure rise provoked by the DBD is spatially confined. Accordingly, the model assumes a localized pressure decrease just after the actuator, leading to a decrease in the separation criterion. It is clear that the actuator brings the flow closer to separation, i.e. closer to the dashed line. Results indicate the DBD is capable of provoking separation as $\alpha$ increases; for $\alpha = 11deg$ and $\alpha = 17deg$ separation is predicted at $x/c = 0.4$ and $x/c = 0.3$ respectively. As smaller $\alpha$ and more upstream positions are considered it appears the actuator does not separate the flow.

From Separation to Actuator Authority

Even though results indicate separation could not always be provoked by DBD actuators, it is important to estimate the $\Delta C_l$ obtained in case separation is provoked. This is done by introducing the Kirchhoff-Helmholtz trailing edge separation law, reviewed in [27]. According to this expression the suction side separation point, $f$, is related to the airfoil normal force coefficient, $C_n$:

$$C_n = C_{n0} + \frac{dC_n}{d\alpha} \left( \frac{1 + \sqrt{f}}{2} \right)^2 \alpha$$

(25)

where $C_n$ is the sectional force in the direction perpendicular to the airfoil chord, defined as $C_n = C_l \cos \alpha + C_d \sin \alpha$. The slope of $C_n(\alpha)$ at small $\alpha$ is represented by $dC_n/d\alpha$, and $C_{n0}$ is value the normal coefficient takes when $\alpha = 0$. These constants are adjusted for the airfoil under consideration. The separation point is measured from the LE, meaning that when the suction side flow is completely attached $f = 1$, and when the flow is separated from the LE we have $f = 0$. 

Figure 9: Assumed DBD Body Force Spatial Distribution
In the past DBD actuators were used successfully to control separation, namely reattaching the flow in the LE region [21]. However, the momentum transferred by DBD actuators appears to be too small to consider circulation control through Kutta condition manipulation, even as suitable TE geometries are employed [22]. Accordingly, the expected changes in the lift curves when imposing DBD actuators should resemble what is obtained with BLT, rather than what is obtained with TEJ. In other words, one expects that DBD are capable of altering the $C_l$ curve at large AOA, while not affecting the sectional lift for small AOA. Figure 11 illustrates the $\Delta C_l$ obtained for different separation positions and considering different AOA, for the NACA64618 airfoil. The change in the sectional lift is obtained from the change in $C_n$ resulting from the Kirchoff law, by considering that the drag is not altered by the actuator. If separation occurs near the LE considerable decrease in the sectional lift coefficient should be possible, e.g. $\Delta C_l = 0.4$ for $f = 0.1$ at $\alpha = 11$ deg. Moreover, the larger $\alpha$ is, the smaller $f$ should be to produce given change in the lift coefficient. This is expected since the separation point moves upstream with increasing $\alpha$ when no actuation is applied. Combining figures 10 and 11 the authority of DBD actuators is estimated in $\Delta C_l \approx 0.13$ for $\alpha = 11$ deg. One should note $V_{eff}$ has a large influence on the achieved $\Delta C_l$ since the absolute pressure gradient depends on the square of the effective
velocity. This implies larger $\Delta C_l$ should be obtained at the mid-span area. Figure 11 also shows that $\Delta C_l < 0.7$ for the considered range of $\alpha$. This result is obtained for a particular airfoil, but nevertheless it indicates the required decrease in sectional lift is not achieved even if separation is provoked at the LE. Recalling figure 4, to regulate power through active stall above rated speeds it is necessary to have $\Delta C_l \approx 0.7$, considering half the span is actuated. Separation would have to be provoked before the LE to obtain such a change in sectional lift, but this situation cannot be modelled with Kirchhoff’s law.

5. Discussion

The different actuation types are now compared. The power consumption of each actuator is analysed considering $\Delta C_l = \{0.2 ; 0.4\}$ is imposed over half the blade $L = 30 \text{ m}$. This is not sufficient to regulate the aerodynamic power captured by the HAWT, but may be used to compare the energy consumption of the different actuation types.

Considering BLT, the pressure difference between the flow in the suction peak region and the air inside the blade should be sufficient to drive BLT. This is assumed because at the outer half of the HAWT blade the dynamic pressure is large, since $V_{\text{eff}}$ is large, and consequently the absolute pressure difference is also large. Moreover, the desired transpiration velocity can be obtained by tailoring the porous material characteristics [26]. Accordingly, the power required to employ BLT is estimated by looking at the turbine as a centrifugal pump. The required air mass rate per unit span is estimated with:

$$\dot{m}_{\text{BLT}} = V_b V_{\text{eff}} x \frac{c_p}{c_{\text{por}}} \rho$$  \hspace{1cm} (26)

The power consumed per unit span is then

$$dP_{\text{BLT}} = \frac{1}{2} \dot{m}_{\text{BLT}} (\Omega r)^2 dr$$  \hspace{1cm} (27)

which under the assumption $V_{\text{eff}} \approx \Omega r$ is integrated over the considered blade span yielding:

$$P_{\text{BLT}} = 0.5 V_b x \frac{c_p}{c_{\text{por}}} \rho \Omega^3 \int_{r=30}^{r=60} r^3 c(r) dr$$  \hspace{1cm} (28)
For the TEJ case, expression 14 is inverted to obtain the required air mass per unit span:

\[ U_{jet} = U_{\infty} \sqrt{\frac{0.5cC_\mu}{h_{jet}}} \Rightarrow \dot{m}_{TEJ} = U_{jet} h_{jet} \rho \]  

(29)

The power consumption of the pneumatic system involved in providing the jet is computed by calculating the amount of energy required to accelerate the air flow from still-stand to the speed at which it is ejected in the jet. The expression for the power per unit span is written as:

\[ dP_{TEJ} = \frac{1}{2} \dot{m}_{TEJ} U_{jet}^2 \eta_{Pump} \]  

(30)

where \( \eta_{Pump} \approx 0.95 \) represents the efficiency of the pump used to drive the pneumatic system. Integrating over the span and assuming \( h_{jet} = 0.006 \) [8] we come to:

\[ P_{TEJ} = 0.5 \times \frac{0.006\rho \Omega^2}{\eta_{Pump}} \left( \frac{0.5C_\mu}{0.006} \right)^{3/2} \int_{r=30}^{r=60} r^3c(r)dr \]  

(31)

Regarding DBD actuators, power consumption is estimated based on the actuator thrust, according to the empirical relationship from [9]:

\[ dP_{DBD} = \frac{F_{act}}{0.2} \rho_0 \eta_{DBD} \]  

(32)

with the actuator force in N/m and power consumption in W/m. Given a \( \Delta C_l \) and AOA of operation, the required separation location \( f \) is determined according to figure 11. The actuator is placed at this chordwise position and \( F_{act} \) is increased until separation is provoked (following Stratford’s criterion). The required DBD power depends on several parameters, namely \( V_{Eff} \), \( \alpha \), \( \Delta C_l \) and airfoil employed. In the present study we consider the NACA64618 with \( \alpha = [11; 14] \) and with \( V_{eff} \approx \Omega r \).

Results obtained for the different actuators are presented in table 2. The actuation parameters used in each case are also shown. For \( \Delta C_l = 0.2 \) TEJ require the least power of all actuators.

| \( \Delta C_l \) | Actuator Parameter | BLT | TEJ | DBD\(\alpha=11\) | DBD\(\alpha=14\) |
|---|---|---|---|---|---|
| 0.2 | Actuator Parameter | \( V_b = 0.006 \) | \( C_\mu = 0.0013 \) | \( f = 0.3 \) | \( f = 0.17 \) |
| Power (kW/Blade) | 19.9 | 4.9 | 32.6 | 94.7 |
| 0.4 | Actuator Parameter | \( V_b = 0.012 \) | \( C_\mu = 0.015 \) | \( f=0.12 \) | \( f=0.07 \) |
| Power (kW/Blade) | 39.8 | 194.9 | 60.9 | 144.4 |

On the other hand BLT appear to consume the least power for \( \Delta C_l = 0.4 \). This is explained because for TEJ \( \Delta C_l \propto \sqrt{C_\mu} \), while for BLT figure 5 shows \( \Delta C_l \propto V_b \). The estimated power for the DBD actuators is dependent on the AOA. For \( \alpha = 11 \) it is comparable to BLT, while for \( \alpha = 14 \) it is considerably larger. For all actuation technologies, power consumption is significant compared with the total turbine power production; considering a 3 bladed HAWT employing BLT such that \( \Delta C_l = 0.4 \), actuation would consume approximately 2.4% of the turbine’s power. Regarding DBD actuators, results indicate separation can not always be provoked. The actuator was modelled in a simple way and the criteria used were not developed to incorporate localized flow perturbations such as DBD actuators; nevertheless it appears the momentum transferred by the actuator is not enough to provoke separation in all considered cases. CFD simulations could
provide more insight on the actual effect of DBD actuators at large Reynolds numbers, since the present modelling used integral BL parameters, while the force field generated by DBD actuators varies both in space and time. The DBD actuator considered was optimized to yield the largest thrust force, since usually these actuators are used to delay separation and thus energize the BL. However the key parameter in provoking separation is not the total thrust force, in this case opposite to the flow direction, but rather the maximum body force obtained, which translates into a localized adverse pressure gradient. Experiments show [9] that by decreasing the exposed electrode length, while keeping all other parameters constant, the total thrust decreases but the thrust per meter increases. The maximum pressure gradient is thus increased, indicating this option should be explored in the future to design DBD actuators to provoke separation.

The present study indicates the required $\Delta C_l$ to regulate power is not attained with any of the actuators considered. The authority of each actuation type was assessed using uncomplicated models, but nonetheless the required change in circulation associated with the airfoil section is extremely large, and it seems none of the actuation mechanisms considered is suitable. This indicates airfoil redesign is necessary if power regulation through active stall control is to be accomplished. Airfoils designed to be sensitive to actuation could achieve the required $\Delta C_l$. This however needs to be done prudently to avoid a point-design type of airfoil, since it is not suitable for HAWT applications given the wind stochastic nature and spatial gradients. Ultimately, airfoil design should be coupled to blade planform design while curtailling actuator power consumption such that a cost-effective solution is found.

6. Conclusion

A preliminary study on the feasibility of active stall control for HAWT was performed based on the NREL 5 MW baseline turbine. Results show a large portion of the blades must be actuated, and the actuators must be effective especially at large AOA. Different actuation technologies have advantages and drawbacks. LE blowing is able to produce LE stall, and thus has a large authority. TEJs can yield significant changes in the lift coefficient, but only in the linear region of the lift curve. DBD have no moving parts but transfer a limited amount of momentum to the air and it is not clear whether they provoke separation at the Reynolds numbers being considered. All in all, active stall control of HAWT could be feasible only if the blade and the airfoils were designed from the beginning to be active stall controlled.

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