Numerical Studies on a Rotor with Distributed Suction for Noise Reduction

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Abstract. Minimizing the flow-induced noise is an important issue in the design of modern onshore wind turbines. There is a number of proven passive means to reduce the aeroacoustic noise, such as the implementation of serrations, porous trailing edges or the aeroacoustic airfoil design. The noise emission can be further reduced by active flow control techniques. In the present study the impact of distributed boundary layer suction on the noise emission of an airfoil and a complete rotor is investigated. Aerodynamic and aeroacoustic wind tunnel tests were performed for the NACA 64-418 airfoil and supplemented by numerical calculations. The aeroacoustic analyses have been conducted by means of the institute’s Rnoise prediction scheme. The 2D studies have shown that noise reductions of 5 dB can be achieved by suction at moderate mass flow rates. To study the impact of three-dimensional effects numerical investigations have been conducted on the example of the generic NREL 5MW rotor with suction applied in the outer part of the blade. The predictions for the complete rotor provided smaller benefits compared to those for the isolated airfoil, mainly because the examined suction configurations were not optimized with respect to the extent of the suction patch and suction distribution.

1. Introduction
The expansion of onshore wind energy by re-powering or exploitation of new sites being potentially closer to residential areas requires wind turbines with minimized noise emission. The consideration of aeroacoustic aspects is therefore an integral part in the design process of modern onshore turbines. Besides mechanical noise, different flow-induced noise sources can be observed [1]. Several studies on the noise emission of commercial turbines suggest that turbulent boundary layer trailing-edge interaction noise (TBL-TEN) is the dominant aeroacoustic noise source of modern turbines [2]. This is a broadband noise source caused by interaction of the turbulent pressure field in the blade boundary layer with the trailing edge. The main drivers for this noise source are both the boundary layer mean velocity profile, the turbulence integral length scale and the amplitude of the turbulent velocity fluctuations across the blade boundary layer near the trailing-edge [3] as well as the effectiveness of the scattering at the trailing edge. Consequently, there are two distinct strategies to reduce this noise source. One approach aims to reduce the effectiveness of the scattering by modifications in the trailing-edge region. The other approach is to reduce the amplitude of the oncoming pressure fluctuations.

The effectiveness of the noise radiation can for example be reduced by introducing a porous trailing-edge yielding reasonable noise reduction [4]. Because the effective length of the trailing-edge perpendicular to the flow direction constitutes a measure for the efficiency of the scattering,
shortening of the effective spanwise length by means of trailing-edge serrations (sawtooth shape) depicts another promising approach. Significant noise reductions of 3.2 dB have been achieved in field tests [5] although the gain is smaller compared to theoretical predictions [6]. Similar to serrations, the use of brushes has been extensively studied and turned out to be a promising approach to reduce the broadband noise for symmetric and cambered airfoils [7], [8].

The turbulent fluctuations in the vicinity of the trailing-edge as cause of TBL-TEN can be reduced either by passive or by active methods. As a passive mean the development of the boundary layer and the associated turbulent pressure fluctuations can be controlled and reduced by adequate shaping of the airfoil pressure distribution [9], [10], [11]. Active methods aim for a reduction of the noise driving properties by means of active flow control demanding the supply of external energy. Within the frame of a bilateral project, it has been shown [3] by detailed measurements in the Mixing-Layer Facility (Tel Aviv Univ.) and in the Laminar Wind Tunnel (Univ. of Stuttgart) as well as by CFD-based numerical investigations that the trailing-edge noise emission can be effectively reduced by means of adequate distributed suction in the rear part of an airfoil. The 2D investigations were supplemented by numerical studies on the impact of distributed suction on the noise emission of the generic NREL 5MW turbine [12]. The present paper is dedicated to the discussion of relevant findings of these studies. In Sec. 2 the experimental set-up used for the wind tunnel investigations will be described along with a brief presentation of the flow solver and the applied aeroacoustic prediction model. In Sec. 3 results of the wind tunnel tests on an airfoil with suction are presented along with comparative numerical results. Thereafter, selected results for the predicted noise reduction of the NREL rotor with distributed suction in the outer part of the blades are highlighted.

2. Numerical and Experimental Set-Up

2.1. Flow Solver and Noise Prediction Model

The aerodynamic calculations required as basis for the acoustic analyses were performed by means of the CFD code FLOWer [13]. The finite-volume code FLOWer developed at the German Aerospace Centre (DLR) solves the two- or three-dimensional RANS equations in integral form on structured meshes. A specific boundary condition to account for boundary layer suction and blowing has been implemented by the Institute of Aerodynamics and Gas Dynamics (IAG), allowing the specification of constant pressure, constant suction velocity or a user-defined suction profile along the suction regime. FLOWer provides the related turbulence input parameters for the subsequent noise prediction.

The prediction of TBL-TEN is performed with the institute’s acoustic code Rnoise [14] which is based on the model developed by TNO-TBD [15]. It firstly post-processes the FLOWer results in a manner to achieve the noise-related input parameters for predicting the wave-number-frequency spectrum of the wall pressure fluctuations \( \tilde{P}(k_1, k_3, \omega) \) at the trailing edge:

\[
P(k_1, k_3, \omega) = 4\rho^2 \left( \frac{k_1^2}{k_1^2 + k_3^2} \right) \int_0^\infty \Lambda_2(x_2) \left[ \frac{dU_1(x_2)}{dx_2} \right]^2 \cdot \tilde{\phi}_{22}(x_2, k_1, k_3) \cdot \phi_m(\omega - k_1 U_c) \cdot \langle u_2^2(x_2) \rangle \cdot e^{-2|\text{k}|x_2 dx_2} \tag{1}
\]

where \( k_i \) denotes the wave number in \( i \)-direction and \( \Lambda_2, \frac{dU_1(x_2)}{dx_2} \) and \( \langle u_2^2(x_2) \rangle \) represent the distribution of the vertical integral length scale, wall-normal gradient of streamwise velocity and Reynolds stress across the boundary layer, respectively. The prediction scheme is completed by the involvement of the moving axis spectrum \( \phi_m \) and the normalized spectrum of the wall-normal velocity fluctuations \( \tilde{\phi}_{22} \). Since the Menter \( k-\omega \) SST turbulence model [16] was used in the present study, anisotropy effects were not directly accounted for in the aerodynamic calculation. Therefore, the semi-empirical scaling model according to Kamruzzaman [14] was applied to derive \( \Lambda_2 \) from the isotropic RANS results.
The mechanism of far-field noise propagation is modelled through a frequency spectrum which depicts a solution of the diffraction problem [17] and underlies simplifying assumptions such as perpendicular flow to the trailing-edge and a non-compact noise source:

\[ S_{ff} = \frac{1}{2\pi R^2} \frac{\omega L}{c_0} \int_{-\infty}^{\infty} \frac{P(k_1,0,\omega)}{|k_1|} dk_1 \]  

(2)

with \( L \) being the wetted length in spanwise direction and \( c_0 \) representing the speed of sound. The observer is located at a distance \( R \) directly above the trailing edge. Note that Eq. (2) differs from the original TNO-TBD model by a factor of 1/2, which is due to the single to double sided spectrum convention problem as discussed in [18].

Since the Rnoise-related acoustic theory is derived for two-dimensional flow, it is applied in a large number of blade sections to determine the noise emission from a complete rotor. The boundary properties in these sections are determined from a 3D RANS calculation. The total noise of the blade at a certain observer position is then calculated by logarithmically summing up the sound emission of the sections considering directivity and Doppler effects. This prediction scheme is denoted Rnoise3D.

2.2. Wind Tunnel and Experimental Set-Up

To study the fundamental impact of distributed suction on turbulent boundary layer properties being relevant for TBL-TEN emission (compare Sec. 2.1), experimental studies have been performed at Tel Aviv University in a mixing-layer facility [19]. Measurements on an airfoil with adverse pressure gradient performed in the Laminar Wind Tunnel (LWT) of the IAG served to verify the aerodynamic and aeroacoustic predictions. This facility is a tunnel of Eiffel type with closed test section of 0.73 m in height, 2.73 m width and 3.15 m in length. The airfoil model is mounted vertically between two rotary tables. The maximum tunnel speed amounts to 90 m/s enabling REYNOLDS numbers up to \( Re = 5 \cdot 10^6 \) for typical chord lengths examined in this facility. The LWT is characterized by a very low turbulence level of \( Tu_x = \sqrt{u'^2_x/U_\infty} = 2 \cdot 10^{-4} \) (20 – 5000 Hz, \( U_\infty = 30 m/s \)) providing a realistic picture of the natural transition scenario of subsonic airfoils operating under atmospheric conditions. To determine the boundary layer mean profile and the turbulent fluctuations, measurements with a single hot-wire of 5 \( \mu m \) diameter were performed. The hot-wire probe was located at midspan position and traversed perpendicular to the inflow direction in a distance of 1 mm downstream of the trailing-edge.

To enable trailing-edge noise measurements in the present low turbulence facility, the Coherent Particle Velocimetry (CPV) method was developed at the LWT [21], [22]. This method is based on the evaluation of signals obtained by two highly sensitive hot-wires located upwards and downwards of the trailing-edge respectively. To measure the particle velocity and the velocity fluctuations two 45° slanted hot-wire probes with 2.5\( \mu m \) wire diameter were used. The probes are mounted on two cantilevers which are equipped with serrations. Since the noise emitted from the trailing edge towards the upper side of the airfoil is 180° out of phase with the sound emitted to the lower side, a cross-spectral analysis of the
two sensor signals can be applied to separate the trailing-edge from the background noise or noise generated by wall reflections. The velocity fluctuations are finally converted to sound pressures at a standard observer position assuming monopole sources aligned along the trailing edge. The experimental set-up including CPV system and wind tunnel model with suction device is shown in Fig. 1. For the present 2D investigations a NACA 64-418 airfoil was selected as it is a public domain geometry and considered to be representative for the outer part of a blade that is dominant for the overall noise emission of the rotor. The wind tunnel model has a chord length of 0.6m and was manufactured by two glass-fibre reinforced shells laminated in CNC milled negative moulds. A trailing edge thickness 0.3mm was chosen to avoid any blunt trailing edge noise contribution because this particular noise source can effectively be reduced by other established means like a bevelled trailing edge shape. The model is equipped with a suction device covered by a bended porous stainless steel plate with the outer side representing the airfoil contour. Suction was applied to the upper surface solely because of its dominant impact on noise emission for the relevant regime of the lift coefficient. The plate has a porosity of 25% (25% of the panel surface are permeable, 75% impermeable) obtained by conical holes of 250μm diameter (see magnified picture in Fig. 1). The device is located in the turbulent main pressure recovery region of the airfoil and covers a suction area from 55% to 75% chord (Fig. 1). It features five separated chambers beneath the suction plate, each spanning 5% in chordwise direction. Each chamber is connected to a pump via flexible tubes with integrated valves to enable an individual adjustment of the mass flow rate which was measured by taking the differential pressure across a standard orifice plate.

3. Results

3.1. Airfoil With Distributed Suction

Comprehensive wind tunnel tests were performed in the LWT (Sec. 2.2) on the example of the NACA 64-418 airfoil section [20]. The results served to validate the present noise prediction scheme and to examine the impact of relevant parameters of the suction configuration on the achievable noise reduction. Besides CPV measurements of the noise spectra, aerodynamic coefficients were determined and detailed hot-wire boundary layer experiments were performed. The inflow velocity was kept constant at $U_\infty = 70 m/s$ for all tests because the emitted trailing edge noise is highly sensitive towards variations of $U_\infty$. The chosen inflow speed corresponds to a Reynolds number of $Re \approx 2.5 \cdot 10^6$ for the model of 0.6m chord. In all measurements adequate turbulators were attached at the upper and lower side of the model to force by-pass transition at 5% chord.

Numerical calculations were conducted using the Rnoise code described in Sec. 2.1. The underlying FLOWer CFD simulations were performed on a structured C-type grid with 518 cells in circumferential and 128 cells in wall normal direction. The discretization in wall normal direction was chosen such that the height of the first grid cell is in the order of $y^+ \approx 1$ and the boundary layer is resolved by at least 40 cells. The distance of the far field boundaries amounts to 70 chord lengths in each direction. A grid sensitivity study was performed to ensure that the chosen discretization is sufficiently fine for accurate prediction of the aerodynamic and aeroacoustic properties [20]. For the present calculations the two-equation $k$-$\omega$ Shear Stress Transport (SST) turbulence model by Menter [16] was chosen. As suction boundary condition a patch-wise constant pressure beneath the surface was prescribed for the actuator “on” cases with the level of the pressure being iterated until the intended mass flow rate was achieved (compare Sec. 2.1). In the calculations transition was fixed at 5% chord on upper and lower side consistently to the turbulator position in the experiments.

All in all, the impact of angle-of-attack $\alpha$, total mass flow coefficient $c_Q$, chord length, actuator position and configuration on the aerodynamic characteristics and the achievable noise reduction was examined [20]. Exemplarily, the impact of $c_Q$, $\alpha$ and actuator location is subsequently
discussed. First the impact of the mass flow coefficient \( c_Q \) shall be discussed which is defined as \( c_Q = \frac{\dot{m}}{\rho \infty U_\infty A_s} \), with \( \dot{m} \) representing the mass flow rate, \( A_s \) the suction area, \( \rho_\infty \) the air density and \( U_\infty \) the inflow velocity. Fig. 2 shows the wind tunnel results for \( \alpha = 6^\circ \) which corresponds to a lift coefficient of \( c_l \approx 0.95 \) for the actuator “off” configuration (baseline). In the experiments the first four suction chambers \( (x/c = 0.55 \sim 0.75) \) were activated with the same value for \( c_Q \). The leftmost graph in Fig. 2 depicts the upper side mean boundary layer profiles measured 1\( \text{mm} \) downstream of the trailing edge. The red curve gives the result for the baseline configuration (actuator “off”) while the green and blue lines hold for activated suction with \( c_Q = 0.0114 \) and \( c_Q = 0.0171 \) respectively. For \( c_Q = 0.0114 \) this relates to an average suction velocity of \( v_s = 3.19 \text{m/s} \) and an absolute value of the mass flow rate of \( \dot{m} = 0.08 \text{kg/s} \). The diagram in the middle displays the turbulence kinetic energy \( k_T \) normalized by \( U_\infty^2 \). As a single hot-wire was used in the present experiments \( k_T \) was obtained from the rms value of the measured streamwise velocity fluctuations \( \langle u_1^2 \rangle \) assuming the flat plate anisotropy ratios \( \langle u_1^2 \rangle : \langle u_2^2 \rangle : \langle u_3^2 \rangle = 4 : 2 : 3 \) yielding \( k_T = \frac{9}{8} \langle u_1^2 \rangle \). The impact of suction is clearly visible in Fig. 2. The boundary layer thickness is reduced and the velocity profile is enriched near the wall. At the same time the maximum of \( k_T \) significantly decreases. Because the maximum of \( k_T \) is shifted towards the wall, higher local values can be observed in direct proximity to the surface. The observed smaller boundary layer thickness will result in reduced turbulent length scales and, along with the smaller \( k_T \) level, the amplitude of the turbulent pressure fluctuations is expected to decrease, particularly in the low frequency domain. As the oncoming turbulent pressure field represents the source of the trailing-edge noise, the noise emission will consequently be reduced by suction. This is confirmed by the measured trailing-edge noise spectra shown in the rightmost graph of Fig. 2. The depicted \( L_p \) spectra hold for a spanwise trailing edge extent of 1\( \text{m} \) and a standard observer distance of 1\( \text{m} \) above the trailing-edge. No weighting function was applied. It is obvious that the \( L_p \) level in the low frequency domain is significantly reduced by suction while some increase is observed for higher frequencies. The \( L_p \) increase at higher frequencies can be attributed to the reduced length scales and the higher \( k_T \) in the vicinity of the wall. As the maximum of \( L_p \) in the low frequency range dominates the overall noise level a significant reduction can be achieved by suction. Actually, the measured overall noise level (without weighting) was reduced by 2.8\( \text{dB} \) for \( c_Q = 0.0114 \) and by 5.4\( \text{dB} \) for \( c_Q = 0.0171 \) with the \( L_p \) level being integrated between \( f = 500 \text{Hz} \) and 5\( \text{kHz} \). For this turbine also remarkable reductions of the \( A \)-weighted noise level were predicted. It shall be noted that the noise contribution of the suction system itself is not considered in the CPV measurements but measurements with a microphone suggest that the noise directly emitted from the suction device is lower than the minimum trailing edge noise level achievable with activated suction [19]. These measurements were performed at zero tunnel speed and were negatively affected by the very noisy suction fan used in the present set-up.

Fig. 3 shows the FLOWer / Rnoise results for the same airfoil, inflow velocity and (uncorrected) angle-of-attack. In general, a good agreement with the measured boundary layer profiles and \( k_T \) distributions (Fig. 2) can be observed. The differences in boundary layer thickness and \( k_T \) level near the wall can be explained by the fact that the profiles were measured slightly (1\( \text{mm} \)) downstream of the trailing-edge whereas the calculations were evaluated above the airfoil surface at \( x/c = 0.995 \). Also the predicted shapes of the trailing-edge noise spectra are in good agreement with the experiments although the absolute level is underpredicted.

For real wind turbines of the \( \text{MW} \) class, chord lengths and \textsc{Reynolds} numbers are noticeably larger than examined in the present wind tunnel tests. Therefore, the influence of the chord length on the noise reduction achievable by suction was investigated by means of Rnoise calculations. Again, the NACA 64-418 airfoil was considered with transition fixed to 5% chord at upper and lower side. Four different chord lengths were examined, namely \( c = 0.3\text{m}/0.6\text{m}/1.2\text{m}/2.4\text{m} \). The inflow velocity was set to a constant value \( U_\infty = 70\text{m/s} \)
Fig. 2. Measured boundary layer profiles past the upper side of the NACA 64-418 airfoil and noise spectra for different mass flow coefficients $c_Q$, $Re \approx 2.5 \cdot 10^6$, $\alpha = 6^{\circ}$, $x_{transition}/c = 0.05$ at upper and lower side.

Fig. 3. Predicted boundary layer profiles at the trailing-edge of the upper side and noise spectra for different mass flow coefficients $c_Q$, NACA 64-418, $Re = 2.5 \cdot 10^6$, $\alpha = 6^{\circ}$, $x_{transition}/c = 0.05$ at upper and lower side.

yielding REYNOLDS numbers of $Re = 1.25/2.5/5.0/10.0 \cdot 10^6$.

Fig. 4 shows the boundary layer profiles and noise spectra for the baseline airfoil without suction and an exemplary AoA of $\alpha = 3^{\circ}$. In general, increasing REYNOLDS number yields smaller relative boundary layer thickness $\delta/c$ if the transition location is unchanged. The absolute boundary layer thickness $\delta$, however, increases (see left picture of Fig. 4) because here the increase of $Re$ was obtained by enlarging the chord. At the same time the predicted maximum level of $k_T$ decreases. The thicker boundary layer for larger chord lengths is associated with larger turbulent length scales and this, in turn, results in a reduced peak frequency of the noise spectrum and increased maximum amplitude (see right picture of Fig. 4). Fig. 5 depicts the predicted reduction of the overall noise level vs. AoA for the same airfoil and REYNOLDS numbers but with four activated suction chambers ($x/c = 0.55 \sim 0.75$) and $c_Q = 0.0171$. For small REYNOLDS number and higher angles-of-attack flow separation upstream of the trailing edge was predicted. Because Rnoise does not allow reliable noise prediction in this case [20] no results are shown if flow separation was encountered. As a general trend the noise reduction achieved by suction increases with AoA for all examined chord lengths. This can be explained by
the fact that the boundary layer thickness at the upper side and therefore its noise contribution increases with AoA while the opposite is true for the lower side. For this situation the overall noise emission can effectively be reduced by upper side suction. This effect successively abates with larger chord because the relative boundary layer thickness decreases and is less prone to separation with higher $Re$ and its thickness cannot be reduced as strongly by means of suction.

Figure 4. Predicted boundary layer profiles at the trailing edge of the upper side and noise spectra for different chord lengths, NACA 64-418, $U_\infty = 70 m/s$, $\alpha = 3^\circ$, $x_{transition}/c = 0.05$ at upper and lower side.

Figure 5. Predicted overall noise reduction for different chord lengths and AoA, NACA 64-418, $U_\infty = 70 m/s$, $x_{transition}/c = 0.05$ at upper and lower side.

Figure 6. Predicted overall noise reduction for different locations of suction patches with 5% chord, NACA 64-418, $U_\infty = 70 m/s$, $\alpha = 6^\circ$, $x_{transition}/c = 0.05$.

In order to design an efficient suction system for a real wind turbine the impact of the suction location on the achievable noise reduction and the related power consumption is of interest. In general a position of the suction device just downstream of the spar cap is desirable because this allows large pipe diameters associated with smaller losses. On the other side the local pressure above the device is low in this region and higher pump power is required to overcome the pressure increase towards the air outlet (e.g. in the hub region). A position more downstream decreases the pressure difference between inlet and outlet but the losses in the pipes are higher because of their smaller diameter. To give an idea of the achievable noise reduction the impact of the
suction location shall be examined here without consideration of the power requirements. Again, Rnoise calculations were performed for the NACA 64-418 airfoil examined at \( Re = 2.5 \cdot 10^6 \), \( \alpha = 6^\circ \) and forced transition at 5% chord. Four different locations of single suction patches with 5% chord were analyzed. The beginning of the patch was located at \( x/c = 0.55 \), 0.65, 0.75 and 0.85 respectively. The mass flow coefficient was chosen to \( c_Q = 0.0342 \) which correlates to half of the absolute mass flow rate of the previous examples with four suction chambers. Fig. 6 shows the achieved reduction of the overall noise level vs. beginning of the suction patch. It is obvious that the impact of the suction location is quite small. Only for the most downstream position a noticeable smaller gain can be observed. Examination of the predicted boundary layer properties reveals that the strong turbulent fluctuations present at the most downward suction location can be reduced less effectively within the small distance to the trailing edge. As a preliminary conclusion it is expected that medium suction locations \( x/c \approx 0.8 \) are promising for efficient active noise reduction.

### 3.2. Wind Turbine With Boundary Layer Suction

In a second part of the study numerical investigations to examine the impact of three-dimensional effects on the effectiveness of distributed suction were performed for the rotor of a generic wind turbine. The rotor is based on the NREL 5MW turbine [12] that was used as reference turbine in the European UpWind and OFFWINDTECH projects. This three-bladed turbine features a rotor radius of \( R = 63m \). In the present study a wind speed of 12m/s, a rotational speed of 12.1rpm, corresponding to 80m/s tip speed, was examined for a blade pitch angle of \(-2.3^\circ\). To reduce the computational demands and to enable a mesh refinement in the suction area a 1/3 model of the rotor was considered, i.e. rotor tilt angle and tower were neglected.

The complete computational setup consists of several different structured sub-meshes that are connected using the Chimera overlapping mesh technique. The C-type blade mesh features 196 cells in circumferential, 97 in radial and 69 cells in wall normal direction fulfilling the usual requirements with regard to boundary layer resolution (compare Sec. 3.1). A supplementary grid block with 25 layers was introduced downstream the base of the finite-thickness trailing-edge. A further Chimera mesh was introduced for locally refined discretisation in the suction area which is located on the upper side of the outer blade region. In total the blade meshes contain \( 3.26 \cdot 10^6 \) grid cells. A simple 1/3 cylinder with \( 2.26m \) radius \((\equiv 0.035R)\) and \( 4m \) length \((\equiv 0.065R)\) is introduced to approximate the displacement effect of a nacelle. Finally a separate hub mesh serves to connect the blade with the nacelle mesh. All the blade and nacelle meshes are embedded in a background mesh which is a 1/3 cylinder with 6 blade radii in length and 4.8R in diameter and consists of \( 5 \cdot 10^6 \) grid cells. To simulate the steady flow of the rotor periodic boundary-conditions are applied. As for the 2D simulations (Sec. 3.1) the Menter SST turbulence model was applied and the blade boundary layer was assumed fully turbulent. Because trailing-edge noise level scales with the inflow velocity to the \( 5th \) power, the suction actuator should be located in the outboard region of the blade. In the present study two different configurations were examined, \textit{config}. 1 ranging 58% to 94% of the blade radius and \textit{config}. 2 from 82% to 94%. From 72% of the radius outwards the NREL rotor features the NACA 64-618 airfoil that shows increased camber but its pressure distribution has the same characteristics as the NACA 64-418 considered in the present 2D studies (Sec. 3.1). Therefore, for the 3D rotor studies the same chordwise extent of the suction area was chosen that has proven to be reasonable in the 2D investigations \((55 \sim 75\%)\). The suction area was discretized by 4 patches in chordwise and 9 patches in radial direction for \textit{config}. 1 while 3 spanwise patches were chosen for \textit{config}. 2. The segmentation in radial direction is considered to be necessary because the blade rotation yields a spanwise pressure gradient and the under-pressure in the chambers has to be adapted to avoid unwanted crosswise flow streams. In a practical implementation the chambers will be connected to the same main pipe with the pressure differences being compensated by
valves. A constant value of the mass flow coefficient \( c_\text{Q} \) was prescribed within the suction area as boundary condition for the actuator “on” cases. Since for the 3D case \( c_\text{Q} \) was referenced to the inflow velocity \( U_\infty \) superimposed with the local rotational speed, the absolute suction velocity increases towards the blade tip. It has to be noticed that no optimization of suction area, segmentation or suction distribution was performed within the present study.

Subsequently, the impact of the spanwise extent of the suction area and the mass flow rate on the achievable noise reduction shall be discussed. First, the effectiveness of the two different suction configurations was compared. For both configurations the same overall mass flow rate of 4.5 kg/s per blade was chosen, which means that the suction velocity for config. 2 (\( r = 82\% \sim 94\% R \)) is three times higher than for config. 1 (\( r = 54\% \sim 94\% R \)). For config. 1 this corresponds to \( c_\text{Q} = 0.0171 \), the value that has shown to effectively reduce noise emission in the 2D studies (Sec. 3.1). Fig. 7 shows the predicted noise emission for different blade sections evaluated in the same way as the 2D calculations presented in Sec. 3.1, i.e. the noise emission per \( m \) span in 1 m distance above the trailing-edge is given without consideration of directivity or Doppler effects. While the noise reduction obtained with suction config. 1 is up to 3 dB in the outer part of the blade, the gain achieved by config. 2 is only increased to 4 dB although the suction velocity is three times higher. This can be explained by the fact that the lower mass flow coefficient of config. 1 is already sufficient to reduce the suction side noise contribution to such a small level that the lower side contributes significantly to the overall noise level. A further increase of the local suction velocity at the upper side has only a limited impact on the overall noise emission (config. 2). As a consequence config. 1 with a larger radial extent provides a stronger reduction of the overall blade noise emission compared to config. 2. The overall blade noise emission was examined for various observer positions located along a horizontal circle around the rotor using the Rnoise3D tool (Sec. 2.1) [20]. The circle has a radius of 100 m and its center coincides with the hub of the rotor. At a position 100 m downstream of the hub, an overall noise reduction of 1.5 dB was calculated for config. 1. This moderate gain can be explained by the fact that the predicted strength of the local noise emission is highest outboards of the suction area considered in the present study, compare Fig. 7. The regime near the tip, therefore, significantly contributes to the overall noise level of the blade. The blade noise reduction achievable with the present suction configuration is therefore noticeably smaller than the gain in local noise emission displayed in Fig. 7. For a stronger impact on the overall blade noise the suction area should be further extended towards the tip. If the blade region outboard of the suction area is neglected in the evaluation of the overall noise the gain achieved by config. 1 amounts to 1.7 ∼ 2.5 dB while 0.7 ∼ 2.0 dB were predicted for config. 2 depending on the observer position.

4. Conclusion and Outlook
The present paper describes studies on active trailing-edge noise reduction of airfoils and wind rotor blades by means of distributed boundary layer suction. Detailed examination of relevant
boundary layer properties that represent the source of the noise emission served to understand
the mechanism of the noise reduction. Numerical predictions were performed using the
RANS-based inhouse noise prediction method Rnoise and aerodynamic as well as aeroacoustic
measurements were performed in the institute’s Laminar Wind Tunnel. The combined numerical
and experimental investigations on a NACA 64-418 airfoil showed that an overall noise reduction
up to about 5\text{dB} is possible for mass flow coefficients as low as \(c_Q = 0.01 \sim 0.02\) at reasonably
high \text{REYNOLDS} numbers (\(Re = 2.5 \cdot 10^6\)) and lift coefficients. Extensive parametric studies
were performed to investigate the impact of mass flow rate, angle-of-attack, actuator location
and chord length on the achievable gain. Comparisons to the experimental results showed that
Rnoise is capable of predicting the impact of suction on noise emission and can be used for
future design of active noise mitigation systems.

The findings of the 2D studies served as basis to define a suction configuration for the generic
NREL 5MW rotor. The impact of suction on the noise near rated wind speed was examined
by numerical predictions for two different suction configurations located in the outer part of the
blade. These first studies provided overall noise reductions in the order of 1 \sim 2\text{dB} for observer
positions of 0.8 rotor diameter away from the hub. The gain is smaller than the benefit observed
for an airfoil mainly because the suction configuration on the blade was not optimized and did
not sufficiently extent towards the noise dominating near tip region. Upcoming investigations
aim on the optimisation of the suction system for the 3D rotor. The ultimate goal is to maximize
the noise reduction at minimum power consumption.

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