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On the Influence of Virtual Camber Effect on Airfoil Polars for Use in Simulations of Darrieus Wind Turbines

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Abstract

Darrieus vertical-axis wind turbines are experiencing renewed interest from researchers and manufacturers, though their efficiencies still lag those of horizontal-axis wind turbines. A better understanding of their aerodynamics is required to improve on designs, for example through the development of more accurate low-order (e.g. blade element momentum) models. Many of these models neglect the impact of the curved paths that are followed by blades on their performance. It has been theorized that the curved streamlines of the flow impart a virtual camber and incidence on them, giving a performance analogous to a cambered blade in a rectilinear flow.

To test the extent of this effect, wind tunnel experiments have been conducted in a rectilinear flow to obtain lift and drag for three airfoils: a NACA 0018 and two conformal transforms of the profile. The transformed airfoils exhibit the virtual camber that the theory predicts is imparted to a NACA 0018 when used in a Darrieus turbine with blade chord-to-turbine radius ratios, c/R , of 0.114 and 0.25. A parallel computational fluid dynamics campaign has been conducted to study the

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30 aerodynamic behavior of the same blades in curvilinear flow in Darrieus-like motion with $c/R =$
31 0.114 and 0.25, at tip-speed ratios of 2.1 and 3.1, using novel techniques to obtain blade effective
32 angles of attack. The analysis confirms that the theory holds, with the wind tunnel results for the
33 NACA 0018 being analogous to numerical results for the relevant cambered airfoils.

34 In addition, turbine performance is calculated using computational fluid dynamics and a blade
35 element momentum code, for each of the blades in turn. The computational fluid dynamics results
36 for the NACA 0018 agree closely to blade element momentum results for the equivalent cambered
37 airfoil where $c/R = 0.25$, for both turbine power and blade tangential forces. Agreement between the
38 two methods using geometrically identical blades is poor at both the blade and turbine level for c/R
39 $= 0.25$.

40 It is concluded that when modeling a Darrieus rotor using blade element momentum methods,
41 applying experimental data for the profile used in the turbine will yield inaccurate results if the c/R
42 ratio is high, in such cases it is necessary to select a profile based on the virtual shape of the blades.

43

44 **Keywords:** Darrieus, vertical axis wind turbine, flow curvature, virtual camber, experiments, CFD,
45 blockage tolerant wind tunnel

46 **1. Introduction**

47 Most installed wind energy capacity is provided by large wind farms comprised of horizontal
48 axis wind turbines (HAWTs) [1]. Turbines are becoming ever more efficient and their diameters
49 ever bigger. While these large installations are a valuable addition to grid capacity, such designs are
50 not suitable for building integration, and they do not benefit people and communities without a grid
51 connection.

52 Installed capacity comprised of smaller turbines, both on- and off-grid, is growing in the U.S.
53 [2] with similar trends notable in other developed countries. Research into novel small designs is
54 also on the increase. Building integration forms a large part of this, with studies looking at the

55 challenges presented by the pre-existing built environment [3,4] or aiming to design new buildings
56 that produce favorable conditions for wind energy production [5].

57 Vertical axis wind turbines (VAWTs) have been identified as suitable for small-scale
58 installations due to their mechanical simplicity and ease of installation and maintenance afforded by
59 the positioning of generation equipment at ground level [6]. Recent studies have focused on the
60 integration of VAWTs into the built environment, e.g. in rooftop installations [7] or in skewed
61 flows caused by urban infrastructure [8,9], and on improvement of VAWT energy yields [10],
62 which still lag conventional HAWTs.

63 VAWTs are also suited to urban installations due to their good handling of turbulent and
64 unstructured flows, with low noise emissions and high reliability [11]. Darrieus rotors are the most
65 popular VAWT design used, as they are the only VAWT able to reach power coefficients
66 comparable to HAWTs [12].

67 To improve efficiencies further, a more in depth understanding of the physical phenomena that
68 govern Darrieus turbine behavior is needed. For example, both dynamic stall [12] and flow
69 curvature effects [13] affect turbine performance, but are not completely understood. Approximate
70 corrections, or no correction at all, are applied for them when using low-order models (e.g. blade-
71 element momentum, BEM models). Low-order models still represent an industry standard for the
72 analysis of wind turbines. Whether used as a first step in the design process, or in coupled codes for
73 the analysis of aero and other dynamics simultaneously [14], they are used ahead of more advanced
74 analyses due to their robustness and speed. Reasonably accurate results have been produced for
75 time-dependent studies on acceleration [15] and for power and operating range calculations [16].

76 Studies have demonstrated that so-called “flow curvature effects” have a large impact on small
77 Darrieus turbine performance. These effects, caused by the curved paths that VAWT blades follow
78 in operation, were first proposed by Migliore [17]. They manifest in a “virtual” blade camber and
79 incidence, giving blade performance characteristics analogous to those of a cambered blade at
80 incidence in a rectilinear flow. Migliore never went beyond his theoretical approach to verify his

81 proposals experimentally. The current authors have conducted several numerical and experimental
82 studies in an attempt to verify Migliore's theory.

83 Initial research compared BEM output, using polars for symmetrical airfoils and cambered
84 airfoils, to experimental turbine data [18]. The cambered airfoil data was taken from literature and
85 gives only an approximate representation of virtual camber effects. Later, a technique was
86 developed to find the effective incidence of VAWT blades in computational fluid dynamics (CFD)
87 simulations [13]. This allowed plots of airfoil lift and drag against incidence to be processed from
88 the turbine CFD data. CFD results for a turbine with virtually cambered blades were compared to
89 experimental results for a symmetrical NACA 0018 from literature.

90 This paper documents a new approach to the problem. Rather than relying on existing blade
91 polars for symmetrical airfoils, or cambered profiles that represent, at best, an approximation to
92 virtual camber, new wind tunnel experiments have been conducted. Lift and drag forces have been
93 obtained for three airfoils: a NACA 0018 and two modified profiles based on the NACA 0018. The
94 modified profiles have been conformally transformed to fit their camber lines to the arc of a circle,
95 such that the ratio of the airfoil chord to the circle's radius, c/R , is 0.114 or 0.25. See Fig. 3 for the
96 three profiles. Wind tunnel testing has used a new, blockage tolerant test section specifically
97 developed for VAWT blade testing [19].

98 The NACA 0018 was chosen as it is commonly used in VAWT research, while the c/R ratios of
99 the transformed airfoils were chosen for comparability to those used in Migliore's original paper of
100 0.114 and 0.26 [17]. The airfoil with c/R of 0.114 has a maximum camber of 1.42% at 50% of
101 chord, while the c/R of 0.25 has 3.11% maximum camber, again at 50% of chord.

102 Based on Migliore's proposal, in the flow of the wind tunnel the transformed airfoils should
103 perform as the unmodified NACA 0018 would in VAWTs with similar c/R ratios and conversely
104 the NACA 0018's tunnel results should be similar to those of the transformed airfoils when used in
105 the VAWTs. Obtaining force data from a rotating VAWT blade would be challenging
106 experimentally. Instead, CFD simulations have been conducted at several tip-speed ratios, TSRs.

107 Results for TSRs of 2.1 and 3.1 are presented in this paper. Forces calculated have been processed
108 into plots of lift and drag against effective incidence using the aforementioned technique [13].

109 Preliminary findings, comparing data obtained in a conventional wind tunnel (for just the
110 NACA 0018 and the c/R of 0.25 transform) to CFD results (for turbines with c/R of 0.25), have
111 been published previously [20] with limited consideration of BEM modeling. This paper extends
112 the work to two transformed airfoil and turbine c/R ratios, using new wind tunnel results from the
113 blockage tolerant test section and higher quality CFD results. It also provides an analysis of what
114 these findings mean for BEM modeling of VAWTs, including recommendations of how best to
115 account for the virtual camber effect in such codes.

116 The experiments and simulations documented in this paper were conducted at a Reynolds
117 number of 300,000. This is a typical operating blade Reynolds number for small-scale,
118 commercially available VAWTs. For example, the Urban Green Energy Visionair 3 turbine [21],
119 has a 1.8 m diameter and 0.38 m blade chord. In normal operating conditions, at a TSR of 3 in a
120 wind speed of 6 m/s, it will experience blade Reynolds numbers of 300,000 – 600,000.

121 In the range of Reynolds numbers a VAWT blade could be expected to encounter (less than $3 \times$
122 10^6 for even the largest VAWTs in high winds), other than at very low Reynolds numbers (of less
123 than 80,000), the addition of camber to a profile has the same effects on airfoil performance. These
124 can be seen in Fig. 8, namely a shift in the lift/incidence curve to higher lifts, an increase in positive
125 stall angle (and the maximum lift generated at this point) and a corresponding decrease in negative
126 stall angle (and the minimum lift generated at this point). See for example the low Reynolds number
127 work of Selig [22] and Althaus [23]. This has been confirmed in the range 80,000 – 300,000 for the
128 profiles used in this study in wind tunnel tests conducted by the current authors that are yet to be
129 published. Thus, testing the validity of Migliore's theory at one Reynolds number in this range
130 gives confidence in its applicability to all relevant Reynolds numbers.

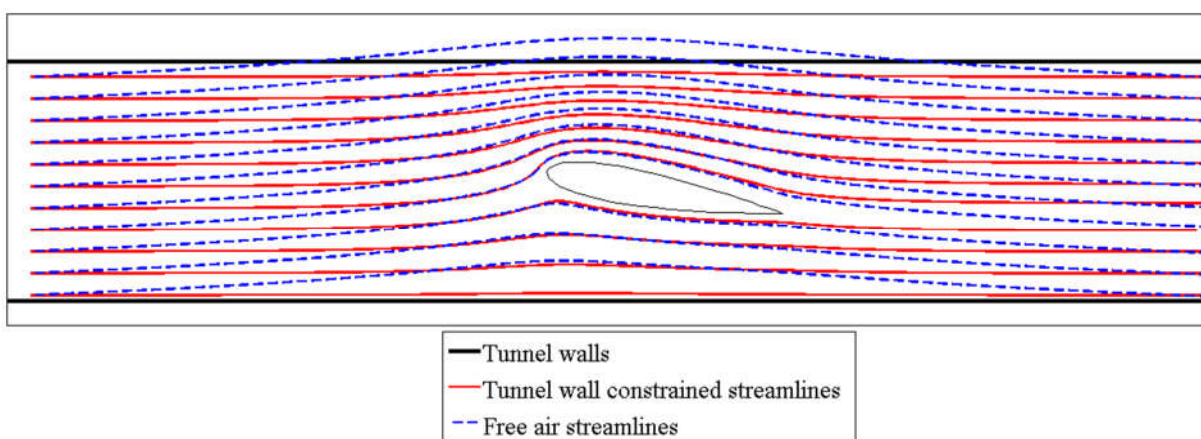
131 The c/R ratio used here, after Migliore [17], is a measure of virtual camber added to a blade
132 undergoing VAWT motion. It is subtly different to the more common turbine solidity ratio, Nc/R ,

133 where N is the number of blades on the rotor, since it is independent of blade number. This paper
134 shows that while virtual camber does not greatly impact turbines with a c/R of 0.114, it does those
135 with a c/R of 0.25, proving similar for higher c/R turbines. As a point of reference, the Visionair 3
136 turbine has a c/R of around 0.4, the highest of the Urban Green Energy range.

137 2. Methods: experiments

138 Conventional wind tunnels do not provide an accurate reproduction of unconstrained steady
139 flow, since they are conducted using test sections with solid walls which affect the flow around a
140 model under test. The walls constrain any streamline curvature induced by the model. Further,
141 model and walls together block the flow through the tunnel, causing it to speed up. Fig. 1 shows
142 streamline development around an airfoil in a wind tunnel and in free air. The figure was prepared
143 using a panel method with airfoil panels represented by distributed vortices and sources and tunnel
144 wall panels represented by point vortices. The streamlines in both cases are released from the same
145 points. Those constrained by the tunnel show less curvature than the free air equivalents, and are
146 forced closer together by the blockage of the airfoil and tunnel walls.

147



149 **Figure 1 – Effects of wind tunnel walls on streamlines around an airfoil.**

150 2.1 Blockage reduction

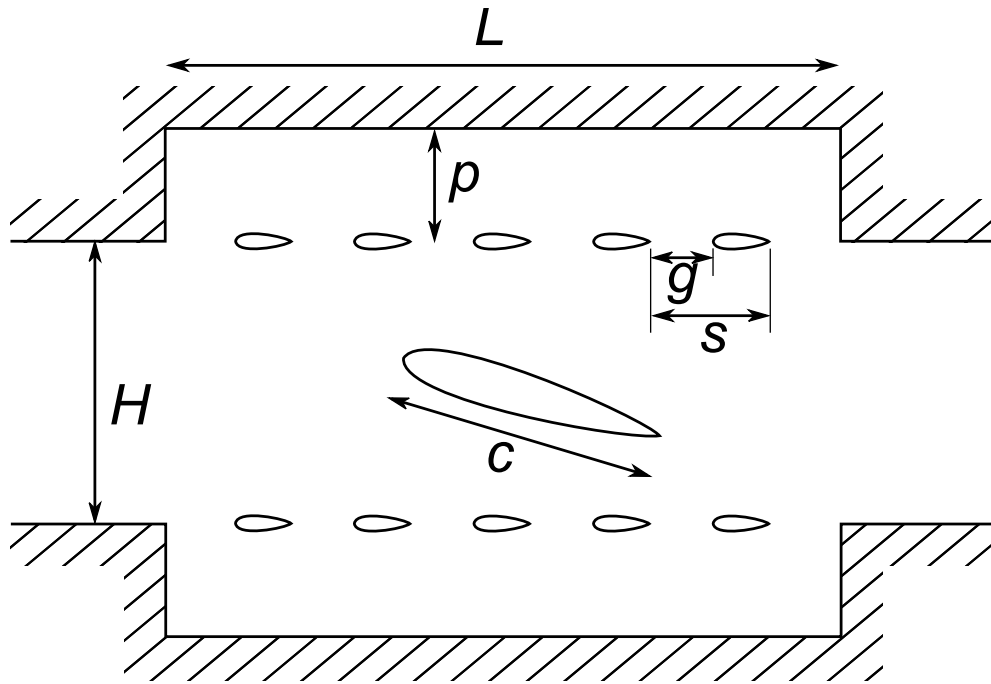
151 Blockage corrections are used to compensate for the effect of blockage and streamline
152 curvature. Derived using potential flow theory for streamline constraints and through potential flow
153 and empirical methods for blockage, they are applicable only to attached flows, though with caution
154 they can be applied to flows with “some degree of separation... with caution” [24].

155 Though most testing for this study has been conducted at incidences between the stall angles of
156 the airfoils where corrections perform well, it has extended beyond stall. Tunnel constraints can
157 affect stall itself, with the blockage-accelerated flow resulting in a shift of the stall angle [25].
158 Blockage corrections cannot account for this. Even small differences in airfoil polars have a large
159 impact on VAWT analyses prepared using them [26], so better ways of limiting the effects of
160 blockage are needed for VAWT blade testing. Blockage tolerant wind tunnels offer an alternative
161 means of reducing blockage that is not reliant on corrections. Since corrections for solid-walled and
162 open jet wind tunnels are of opposite signs [24] one would expect that free-air conditions could be
163 approximated using semi-permeable walls.

164 **2.2 The Imperial College Parkinson blockage tolerant tunnel**

165 Parkinson’s tunnel design [27] has been used in this study, whose configuration is depicted in
166 Fig. 2. Slotted walls comprised of a regular array of evenly spaced airfoils perpendicular to the flow
167 allow flow to exit and re-enter the main channel, the shape of the array components avoiding
168 separation around them. The slatted wall regions are enclosed by plenum chambers of depth p and
169 length L to maintain mass conservation in the flow along the tunnel. An appropriate open area ratio
170 (OAR, a measure of open to slatted wall areas, defined as g/s) must be settled on through
171 experiments on models of different sizes but with like shapes. The OAR that gives the most similar
172 results for the models is that which provides the closest approximation of unconstrained steady
173 flow, since in free-air results would be identical.

174



175

176

Figure 2 – Schematic diagram of the Parkinson blockage-tolerant wind tunnel.

177

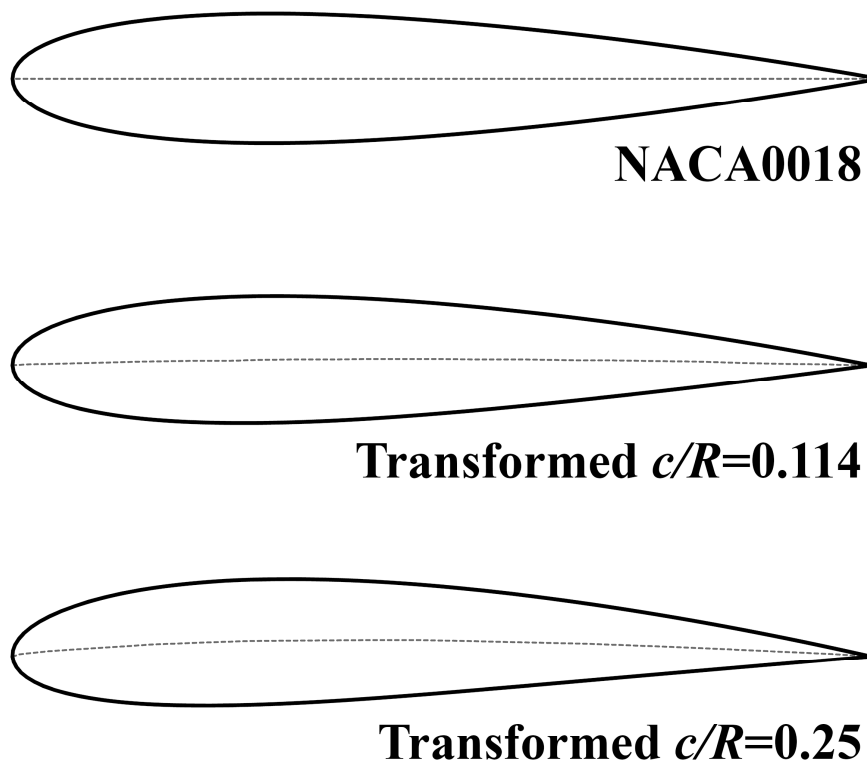
178 An OAR of 71% was found to work best for airfoils tested in the Imperial College Parkinson
 179 tunnel. The tunnel has a square cross-section with sides of 915mm, a plenum length of 2178mm and
 180 depth of 350mm. Slat airfoils have NACA 0015 profiles and 90mm chords.

181 For details on the design and build of this tunnel, experimental set up, and the experiments
 182 performed to calibrate the OAR, see [19]. For convenience, a summary of the experimental set up
 183 and methods is given below. The tunnel was found to reduce the effects of blockage better than
 184 corrections applied to results taken in a solid-walled tunnel [19]. Data from the tunnel requires no
 185 additional processing to achieve low blockage results.

186 2.3 Airfoils

187 The three airfoils (the NACA 0018 and transformed airfoils $c/R = 0.114$ and 0.25 , see Fig. 3 for
 188 the profiles used) all span the width of the tunnel of 915mm and have 183mm chords to give a
 189 chord-to-tunnel height ratio, c/H , of 0.2. They were 3D-printed using nylon laser sintering, in four
 190 span-wise parts due to print chamber limitations.

191 The printed parts were assembled around two 10mm thick 915mm steel rods and were finished
192 with filler and paints to achieve an accurate profile, checking against laser-cut female profile
193 templates produced to an accuracy of $< 100 \mu\text{m}$. All of the airfoils had their trailing edges blunted
194 to a radius of 0.75 mm to ease the 3D printing process. Note that chord measurements are to this
195 blunted trailing edge.
196



197
198 **Figure 3 - The three airfoils used in this study.**

199 **2.4 Airfoil mounting and force measurement**

200 Airfoils are mounted vertically at the half-chord, between two end plates that sit flush with the
201 tunnel walls. The wind tunnel boundary layer was found to have a negligible impact on results,
202 making this arrangement acceptable.

203 Force transducers are mounted at both ends of the airfoil, one connected to a bearing, the other
204 to a bearing and a stepper motor to control incidence. The transducers rotate with the airfoil and
205 measure normal and tangential forces along with moment about the half chord.

206 **2.5 Data acquisition and experiment control**

207 Tunnel velocity is measured across the contraction with a manometer and controlled, via outputs
208 on a National Instruments USB-6229 data acquisition board, using a PC running a proportional-
209 integral-derivative controller (PID). The incidence stepper motor is also controlled through the
210 board, with incidence checks taken using an optical encoder to ensure the stepper motor does not
211 slip under load. Outputs from the force transducers are digitized using a pair of NI PCI-6220 data
212 acquisition boards, with simultaneous acquisition from both. These are then processed into lift, drag
213 and moment about the quarter-chord.

214 The process is fully automated and run from a Matlab script. Required incidences and Reynolds
215 number are input, the apparatus does the rest, rotating to each incidence in turn, checking the speed
216 of the tunnel with the PID, allowing settling time, then recording forces and flow conditions.

217 **2.6 Accuracy of measurements**

218 The force transducers used are factory rated to a 95% confidence level to within 1% for force
219 measurements and 1.5% for torque measurements, with no significant deterioration in performance
220 noted.

221 A small amount of play remains in the system when the stepper motor is holding a steady
222 incidence. This is around $\pm 0.25^\circ$ when forced by hand, though no significant incidence play was
223 noted during experiments over the range of incidences presented in this paper.

224 The largest source of error results from creep in the force transducers. The number of readings
225 taken between re-zeroing of the transducers has been limited to reduce the impact of this. Offsets
226 are taken at the beginning and end of each run of the experiment, with the differences between the
227 two offsets time apportioned across readings to give a local zero from which forces are calculated.
228 The maximum cumulative error in lift or drag readings is estimated at 3%. No differences were
229 noted between repeated runs of the experiments greater than this.

230 **3. Methods: CFD simulations**

231 CFD simulations were used to investigate the aerodynamic behavior of the selected airfoils
232 when rotating in a Darrieus turbine. Four single-bladed rotors were considered, the two c/R ratios of
233 0.114 and 0.25, fitted with both the NACA 0018 and the relevant transformed airfoil.

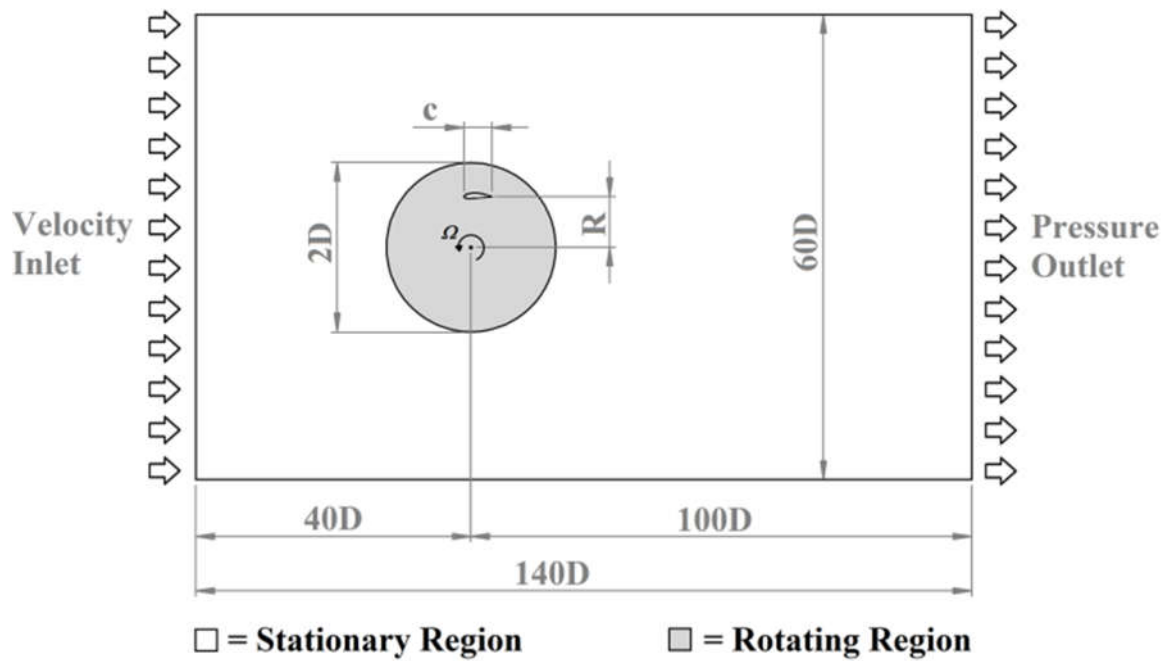
234 **3.1 Numerical settings and test plan**

235 The commercial code ANSYS Fluent [28] was used to solve the time-dependent unsteady
236 Reynolds-averaged Navier-Stokes (U-RANS) equations in their two-dimensional form. Based on
237 previous studies using the same commercial software [29], the Coupled algorithm was employed to
238 handle the pressure-velocity coupling. It was proved that this algorithm ensured more stable results
239 when adopting different meshes, timesteps, or rotating speeds. The second order upwind scheme
240 was used for spatial discretization of the whole set of RANS and turbulence equations, as well as
241 the bounded second order for time differencing, to obtain good resolution.

242 Air was modeled as an ideal compressible gas with standard ambient conditions, i.e. a pressure
243 of 1.01×10^5 Pa and a temperature of 300 K. The authors have recently presented an assessment of
244 the main settings that have been applied to the CFD simulations [29], which have also been
245 validated against experimental data obtaining very good agreement. The results of the sensitivity
246 analyses on the main simulation parameters are here reported.

247 Exploiting the sliding-mesh model of the solver, the simulation domain was divided into two
248 subdomains in order to allow the rotation of the turbine, as proposed by Maître et al. [30] and Raciti
249 Castelli et al. [31]. Fig. 4 shows a circular zone containing the turbine, with a diameter ($2D$) twice
250 that the turbine itself (D). R represents the turbine radius. The circular zone rotates with the angular
251 velocity of the rotor while a rectangular fixed outer zone determines the overall domain extent.

252



253

254

Figure 4 – Simulation domain.

255

256 The use of a sliding interface is of particular interest for unsteady simulations of rotating
 257 machines, thanks to the possibility of differentiating the discretization requirements between the
 258 two subdomains. The rotating region around the turbine has strict requirements in terms of spatial
 259 discretization, in order to correctly describe the flow gradients in the proximity of the blades. The
 260 outer region, conversely, often does not need extremely fine discretizations, allowing one to enlarge
 261 its overall dimensions to avoid undesired disturbances induced by the boundary conditions.

262 Focusing on this latter aspect, all the boundary distances of Fig. 4, selected after the sensitivity
 263 studies reported in [29], are given as a function of the rotor diameter. The velocity is imposed at the
 264 inlet section, which is placed 40 rotor diameters upwind of the rotating axis. The ambient pressure
 265 condition is imposed at the outlet boundary, located 100 rotor diameters downwind, while a
 266 symmetrical condition is defined for the lateral boundaries at a distance of 30 rotor diameters. The
 267 symmetry condition for lateral boundaries is the most common solution for this type of simulation
 268 (e.g. [32]). An alternative option could be to represent the lateral boundaries with “opening-type”
 269 conditions (i.e. able to support simultaneous inflow and outflow over a single region), which could

270 enable a reduction of domain width. Due to possible instabilities generated by this type of setting
271 the conservative choice of symmetry conditions was maintained here.

272 Table 1 reports the main geometrical features of the four simulated models. The airfoil chord
273 was kept constant in all the simulations, while the revolution radius was changed to achieve the two
274 desired chord-to-radius ratios.

275

276

Table 1 – Test cases.

	Case 1	Case 2	Case 3	Case 4
c/R	0.114		0.25	
airfoil	NACA0018	Transformed ($c/R=0.114$)	NACA0018	Transformed ($c/R=0.25$)
c [m]	0.2	0.2	0.2	0.2
R [m]	1.75	1.75	0.8	0.8
U [m/s]	8	8	8	8

277

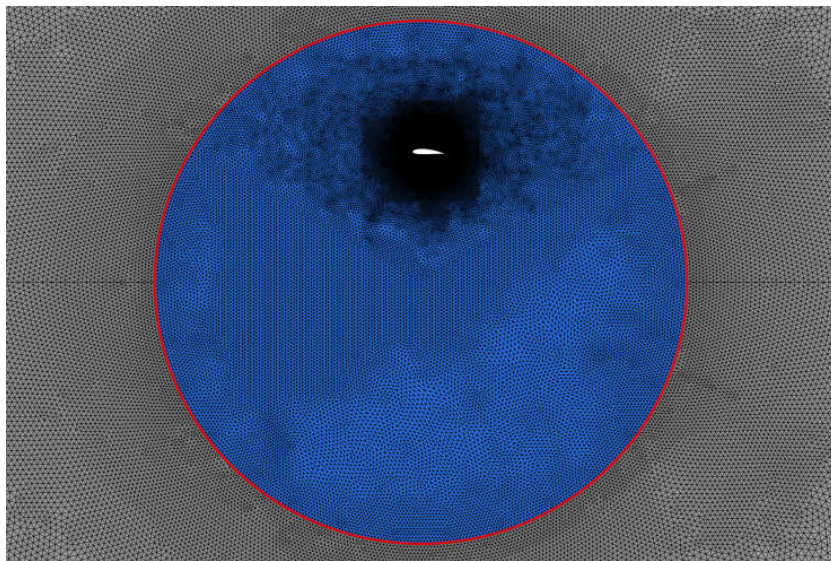
278 To correctly describe the flow around each airfoil, six different levels of refinement of the
279 mesh and three angular time-steps were considered for both c/R ratios, in order to identify the
280 required number of nodes in the mesh surrounding the airfoil and the total number of mesh
281 elements of the computational grid. The mesh settings were defined accordingly to the results of
282 the grid-independency analysis reported in [29], since they were deemed to guarantee the same
283 level of accuracy.

284 An unstructured mesh composed by triangular elements was used for the discretization of the
285 core flow region, except for the boundary layer region, where a structured O-grid was generated
286 with a row of 50 inflated layers to include the entire boundary layer height. The requirement in
287 terms of near wall refinement is very strict: the near-wall cell size is determined by imposing the
288 condition that its first nodal point has a distance from the wall that does not exceed the limit
289 required by a ω -based turbulence model for a proper resolution of the boundary layer. This was
290 achieved by ensuring that the values of the dimensionless wall distance (y^+) during the rotor

291 revolution did not exceed the limit of 1, necessary to ensure that the first computational node
292 falls in the linear region of the boundary layer.

293 The expansion ratio for the growth of elements starting from the surface was kept below 1.1
294 to achieve good mesh quality in proximity of the airfoil. It was proven that a grid-independent
295 behavior can be obtained by using a discretization of the airfoil surface with approximately 600
296 nodes. The mesh size of the rotating region, for the single-bladed configuration, results in
297 approximately 1.4×10^5 elements, while the stationary region is discretized with 2.0×10^5
298 elements. Figs 5-7 show some details of the grids. The rotating domain, containing the rotating
299 blade, is characterized by a progressive coarsening of the elements with the distance from the
300 blade. The mesh is refined in the region surrounding the blade due to the higher complexity of
301 the flow field. As suggested by [31], a *control circle* (Fig. 6), with a diameter equal to twice the
302 airfoil's chord, was defined around the blade in order to have a better capability to control the
303 elements size in the region closer to the blade itself. The use of quadrilateral elements in the
304 near-wall region is clearly distinguishable in Fig. 7 for the blade leading edge. The chosen mesh
305 topology requires a grid-clustering in order to have a smaller spacing between the nodes near
306 both the leading and trailing edges, being the regions experiencing the highest gradients.

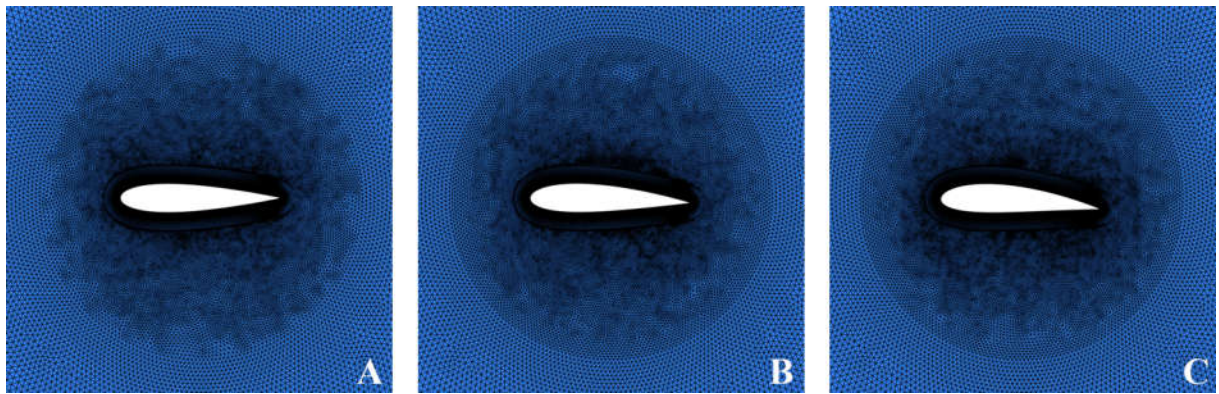
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308

309 **Figure 5 – Computational grid for the rotating domain (e.g. transformed airfoil with $c/R=0.25$).**

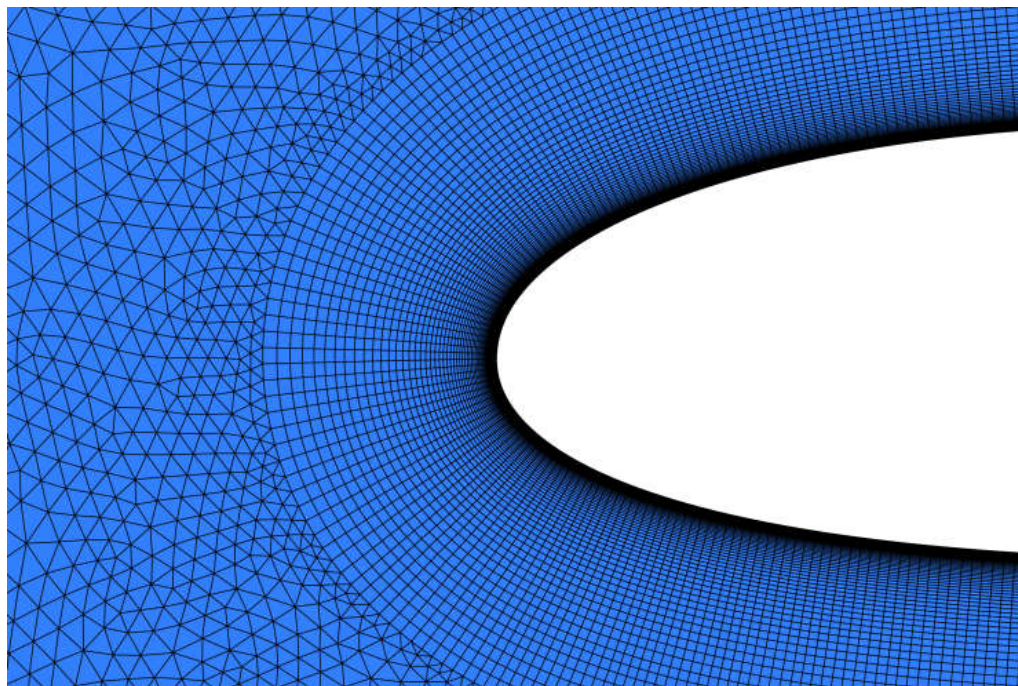
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311

312 **Figure 6 – Control circle details for the NACA0018 (A), the transformed airfoil with $c/R=0.114$ (B)**
313 **and $c/R=0.25$ (C).**

314



315

316 **Figure 7 – Computational grid: boundary layer discretization at the leading edge (e.g. transformed**
317 **airfoil with $c/R=0.25$).**

318

319 Recent work [13] demonstrated that different functioning TSRs require specific minimum
320 time steps in order to ensure accurate results. In the present analysis angular time steps in the

321 range between 0.135° and 0.42° were used, corresponding to the cases with the lowest and the
322 highest TSR respectively.

323 As suggested by many authors (e.g. Howell et al. [33] or Rossetti et al. [34]), the global
324 convergence of each simulation was monitored comparing the average value of the torque
325 coefficient (c_T) over a complete revolution. After a specific sensitivity analysis [29], the selected
326 threshold for convergence was identified in a variation lower than the 0.1% of the torque
327 coefficient value between two subsequent revolutions. This value was by far lower than the limit
328 commonly adopted in literature, i.e. 1%. The required number of revolutions is not a priori
329 known, being dependent on the rotating speed of the turbine: in the present analyses, it ranged
330 between 30 and 50 revolutions.

331 Concerning the turbulence closure problem, Balduzzi et al. [35], showed the effectiveness of
332 Menter's shear stress transport (SST) model in performance simulations involving unsteady
333 aerodynamics for VAWTs, as also confirmed by wide use in literature.

334 In the present study attention has been focused on a more detailed examination of the
335 aerodynamic behavior of a single airfoil in motion by analyzing equivalent static pressure
336 coefficients on the blade profiles. Since the prediction of the boundary layer evolution becomes a
337 critical issue and the blade Reynolds number for the considered cases cannot guarantee a fully
338 turbulent condition, the $\gamma-Re_\theta$ transition model (derived by Menter and Langtry from the SST
339 model [36]) was implemented, despite its increased computational cost. Lanzafame et al.
340 recently showed good agreement between experimental data and numerical results obtained with
341 the transition turbulence model for two different types of H-Darrieus turbines [37].

342 The CFD methodology used in this study has been assessed and validated in the recent past
343 through direct comparisons with experimental data. Simulations have been shown to accurately
344 predict the experimental power curves of a full-scale rotor tested with variations of the pitch
345 angle [13]. The methods also succeeded in correctly predicting blade torque profiles [29], as
346 measured experimentally by Vittecoq and Laneville [38].

347 **3.2 Data analysis**

348 Once each simulation had reached full convergence, an additional revolution was simulated
349 acquiring the pressure distribution over the airfoils and the flow field in proximity of the blades
350 approximately every 2 degrees.

351 In order to reconstruct VAWT blade polars and evaluate the virtual camber effect, a robust
352 procedure to extrapolate the incidence angle was needed. The concern of defining the angle of
353 attack from CFD simulations of rotating blades has been addressed by wind turbine specialists in
354 case of HAWTs [39]. More recently, a method for VAWTs was proposed by Balduzzi et al. [13]
355 and then further improved by Bianchini et al. [20].

356 The method of Balduzzi et al. adapted the averaging technique of Hansen [40] for use in
357 Darrieus turbines. In this method, the velocity triangles of the blades are reconstructed by
358 evaluating the relative wind speed in a properly positioned area in front of the airfoil and
359 applying an inverse BEM method to estimate the induction factor. In an inverse BEM method
360 applied to VAWTs the effect of velocity reduction and distortion generated by the blade-flow
361 interaction is globally modeled by a variation of the induction factor [13], with no information
362 on the distortion of the absolute wind speed.

363 To overcome this intrinsic uncertainty, the novel approach developed by Bianchini et al. [20]
364 was adopted, making use of the virtual camber concept, the main topic of the present study. The
365 method, a four-step process, is briefly summarized here:

- 366 1. Based on the chord-to-radius ratio of the rotor (c/R) and the tip-speed ratio (TSR), the virtual
367 airfoil due to flow-curvature effects is defined based on the conformal transformations of
368 Migliore et al. [17].
- 369 2. The pressure coefficient distributions over the virtual airfoil are calculated for a wide range
370 of AoAs with fine intervals of 0.25° between each. This is performed in XFOil [41] using a
371 Reynolds number compatible with that attended on the airfoil. Then, all the pressure
372 coefficient distributions are normalized within -1 and +1 by scaling them by their maximum

373 and minimum values. This solution allows comparison between pressure distributions,
374 depending only on the incidence angle, with a negligible error on the exact relative speed,
375 which can be hard to define from CFD calculations [20].

376 3. The pressure coefficient distributions calculated from CFD are acquired from calculations at
377 different azimuthal positions and compared to those previously obtained for an airfoil with
378 horizontal chord [13]. They are again normalized within -1 and +1 by scaling them by their
379 maximum and minimum values.

380 4. For every azimuthal position, the pressure coefficient distribution from CFD is compared to
381 all those calculated for the airfoils. By doing so, the distribution that best fits that from CFD
382 can be highlighted. In particular, the position along the chord of the pressure peak is mainly
383 used to define the incidence as the influence of flow speed has been discarded by
384 normalizing the distributions. This comparison directly provides the estimation of the
385 incidence on the airfoil. Moreover, by normalizing the pressure profile the relative speed can
386 be evaluated *a posteriori*. This velocity value appeared fully compatible with that predicted
387 based on the “reference zone estimation” of Ref. [13].

388 As discussed in literature [39], the validity of this approach unfortunately ceases as soon as
389 the flow is separated around the turbine blades. In these conditions, no reliable blade pressure
390 distribution can be obtained with XFOil and therefore no comparison can be made from CFD to
391 XFOil to define the flow incidence on the airfoil.

392 **4. Results**

393 **4.1 Wind tunnel experiments**

394 Lift and drag coefficients for the three airfoils, tested at a Reynolds number of 300,000, are
395 presented in Figs 8 and 9 respectively. The plots show coefficients for both increasing and
396 decreasing incidence in the vicinity of stall. Results for the earlier NACA 0018 study of Timmer

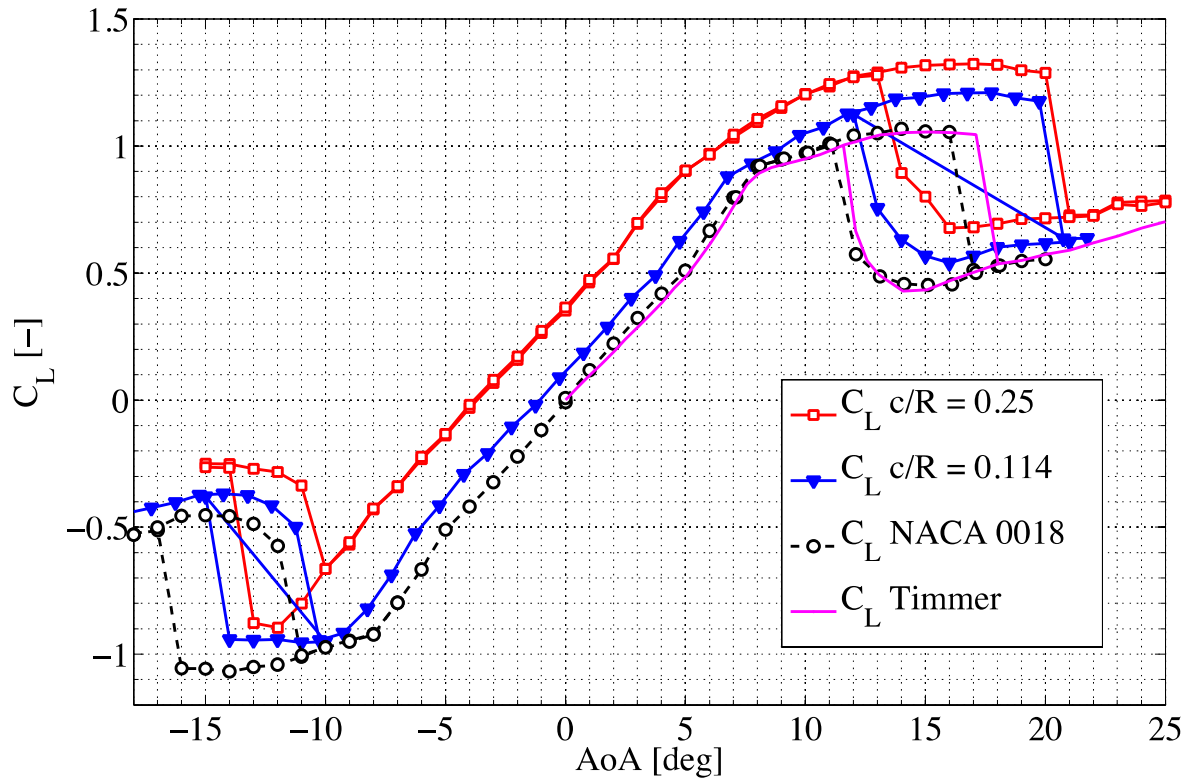
397 [42], taken at the same Reynolds number, are also included. There is excellent agreement
398 between the two NACA 0018 datasets, other than at stall points. Stall occurs at an incidence of
399 16° in the current study, with flow reattaching at 11° with decreasing incidence, with equivalent
400 values in Timmer's study of 17° and 11.5° respectively. There are two possible causes for this
401 difference:

402 Blockage effects – Timmer's data was taken in a conventional solid-walled tunnel and post-
403 processed with blockage corrections. Though these corrections do adjust lift and drag to account
404 for the acceleration of flow around the model caused by blockage, they do not account for the
405 delay of stall to higher angles also caused by the faster flow. Since the tolerant tunnel used in this
406 study physically reduces blockage, this problem is not encountered.

407 Airfoil surface finish – Both studies allowed free transition, were Timmer's models rougher
408 than our own, higher stall/reattachment angles would be expected. Those of the current study
409 were finished with 1000-grit sandpaper and paints (an accurate surface roughness measurement
410 has not been obtainable). Timmer does not disclose surface roughness in his paper [42].

411 The "bump" in the lift coefficients of the two studies at around 8° is caused by a laminar
412 separation bubble which Timmer was able to remove with application of zig-zag strips to initiate
413 turbulent boundary layer transition.

414



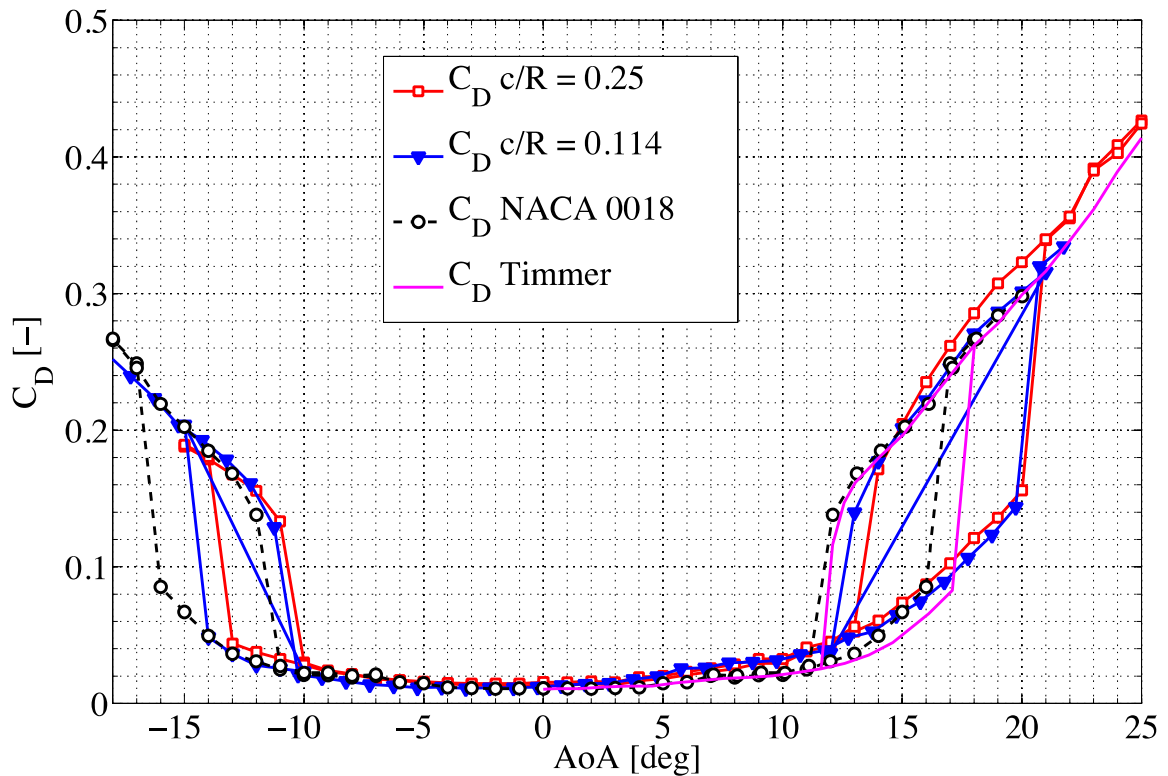
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Figure 8 – Lift against incidence for the NACA0018, $Re = 300,000$ (this study and Timmer [42]),

417

and for the $c/R = 0.114$ and $c/R = 0.25$ airfoils, $Re = 300,000$ (this study only).



418

419

Figure 9 - Drag against incidence for the NACA0018, $Re = 300,000$ (this study and Timmer [42]),

420

and for the $c/R = 0.114$ and $c/R = 0.25$ airfoils, $Re = 300,000$ (this study only).

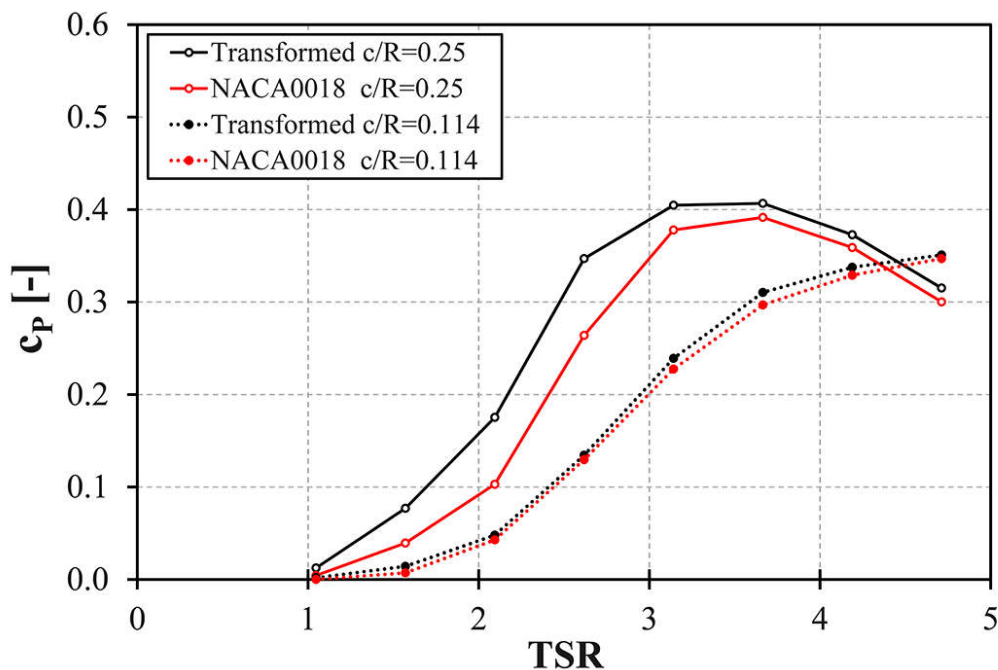
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422 Since the two cambered airfoils were designed specifically for this study, there are no results
423 in literature to compare them against. Figs 8 and 9 show them alongside results for the NACA
424 0018. The relationships between the aerodynamic characteristics and camber are as one would
425 expect in this range of Reynolds numbers and blade thickness and cambers: the greater the
426 camber, the higher the stall angle and C_l max at positive incidences, while the opposite is true at
427 lower incidences, and the angle at which zero lift occurs decreases as camber increases [43].

428 4.2 CFD turbine simulations

429 For both chord-to-radius ratios, seven rotating speeds were considered for the simulation of
430 the airfoils (the NACA0018 and the relevant transformed equivalent). An undisturbed wind
431 speed of 8 m/s was imposed at the inlet boundary, leading to an investigated operating range of
432 TSRs from 1.0 to 4.7. The comparison of power coefficients (c_P) trends is reported in Fig. 10 as
433 a function of the tip speed ratio (TSR).

434



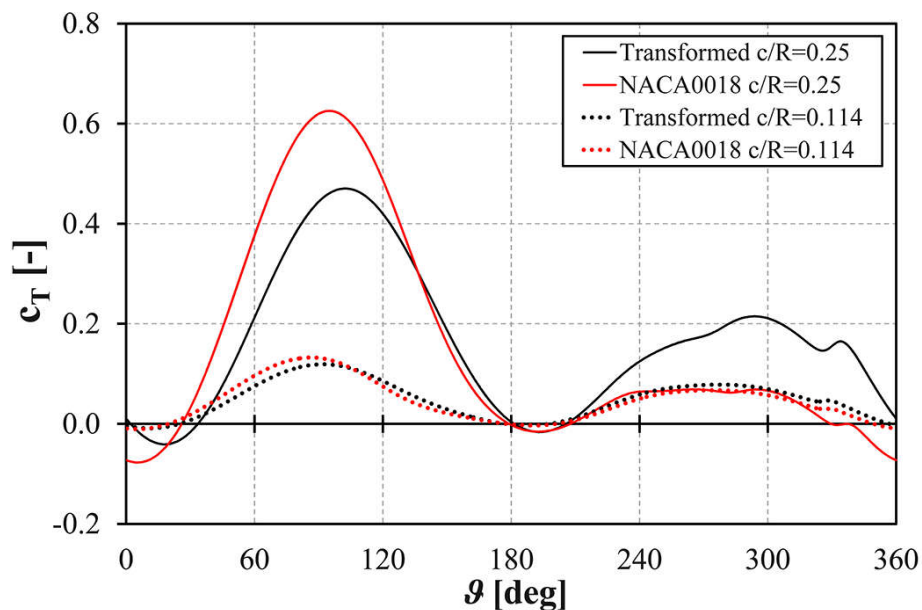
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Figure 10 – Comparison of power coefficient curves for all the simulated airfoils.

437

438 As discussed in Bianchini et al. [20] and confirmed here, blades having a higher chord-to-
439 radius ratio exhibit a shift of the curve peak towards lower TSRs. The behavior of the two
440 airfoils at $c/R = 0.114$ are quite similar, while the two airfoils at $c/R = 0.25$ behave quite
441 differently, stressing the importance of a proper blade design criteria for high c/R ratios [13].
442 These differences are also visible in torque extraction over the revolution, recently discussed by
443 Bianchini et al. [20], who showed that the energy extraction with the transformed airfoil
444 (arranged with its camber curving towards the center of rotation, and expected to behave like the
445 symmetric NACA0018 in curvilinear flow) is more balanced between the upwind and the
446 downwind halves of the revolution. Conversely, the geometrical NACA0018 (which in turn is
447 expected to behave like the transformed airfoil with camber curved outward away from the
448 center of rotation) concentrates the torque extraction in the upwind zone, providing higher local
449 torque coefficients. Fig. 11 shows the tangential force coefficient over a revolution at $TSR=3.1$
450 for all the investigated airfoils, demonstrating this difference in upwind/downwind balance
451 between the airfoils for $c/R = 0.25$.



452

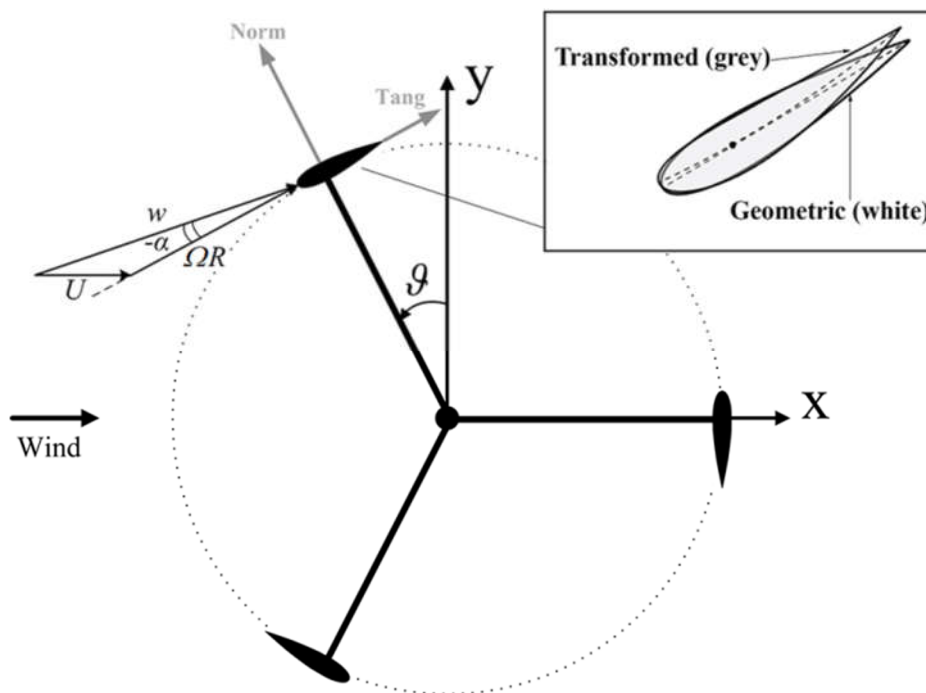
453 **Figure 11 – Comparison of tangential force coefficient profiles over a revolution @ $TSR=3.1$ for all**
454 **the simulated airfoils.**

455 **5. Discussion**

456 To assess the impact of virtual camber effects on the aerodynamics of the airfoils in motion,
457 two TSRs were selected for each curve and analyzed in detail. For both the $c/R = 0.25$ and the
458 $c/R = 0.114$ cases, TSRs of 2.1 and 3.1 were selected. They are characterized by local blade
459 Reynolds numbers between 2.5×10^5 and 3.0×10^5 , comparable to those of the new experimental
460 data collected in the wind tunnel.

461 For all the above operating conditions, the analysis was restricted to a range of azimuthal
462 angles between approximately $\vartheta = -10^\circ$ to $\vartheta = +70^\circ$. In this range, the AoA is generally small
463 enough to ensure attached flow on the airfoil, allowing the procedure for AoA estimation by
464 means of comparisons with XFOIL data to be used. For the convention of signs and reference
465 systems please refer to Fig. 12.

466



467

468 **Figure 12 – Signs and reference systems convention.**

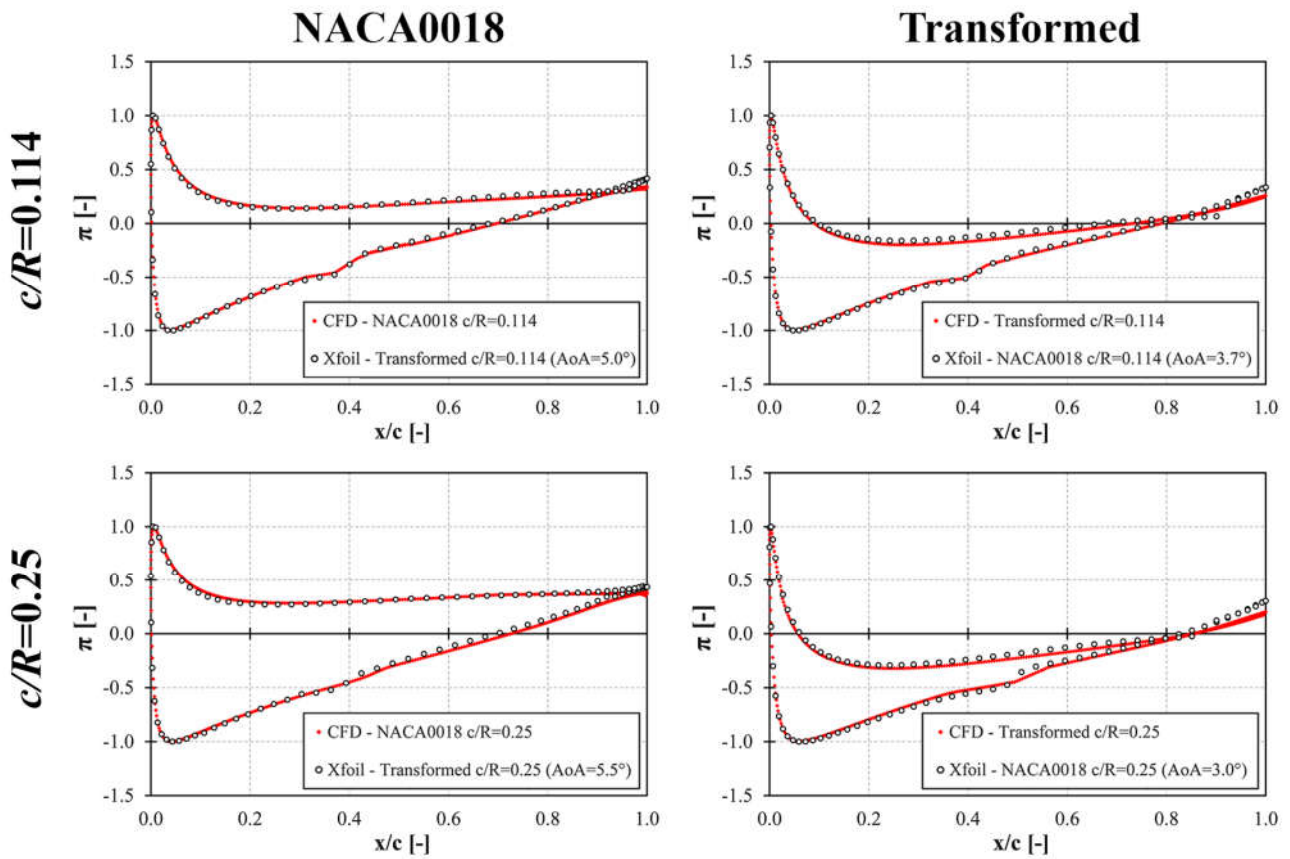
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470 The aforementioned procedure was applied to the selected azimuthal positions for all the
471 tested airfoils. Based on previous results shown by Balduzzi et al. [13] and Bianchini et al. [18],
472 the pressure coefficient distributions over the airfoils in every condition were expected to
473 reproduce those of the corresponding virtual airfoil obtained from experiments on the conformal
474 transformed airfoils. As mentioned, the transformed airfoil arranged with its camber inward
475 becomes a virtual NACA 0018 when rotated about a radius four times its chord length, while the
476 NACA 0018 becomes a virtual transformed airfoil with its camber outward in similar conditions
477 (see Fig. 12).

478 The matching between the pressure profiles from CFD with those related to the virtual airfoil
479 based on conformal transformation was excellent. As an example (comparable agreement was
480 found in almost all the other considered azimuthal positions), Fig. 13 compares the pressure
481 coefficient profiles at an azimuthal position of $\vartheta=32.8^\circ$ of the four simulated airfoils with the
482 correspondent distribution obtained with XFOil for the their equivalent transformed airfoils. With
483 both the c/R ratios the virtual camber effect is verified. Also, the incidence angle (i.e. the angle
484 of the pressure distribution over the virtual airfoils which best matched the CFD data) differs for
485 the two configurations, as the two c/R ratios induce different virtual incidence effects, with a
486 smaller incidence for the smaller c/R , again in line with virtual camber and incidence theory.

487 Finally, it is apparent that good agreement was also found between CFD and XFOil on the
488 position of the transition on the airfoil.

489



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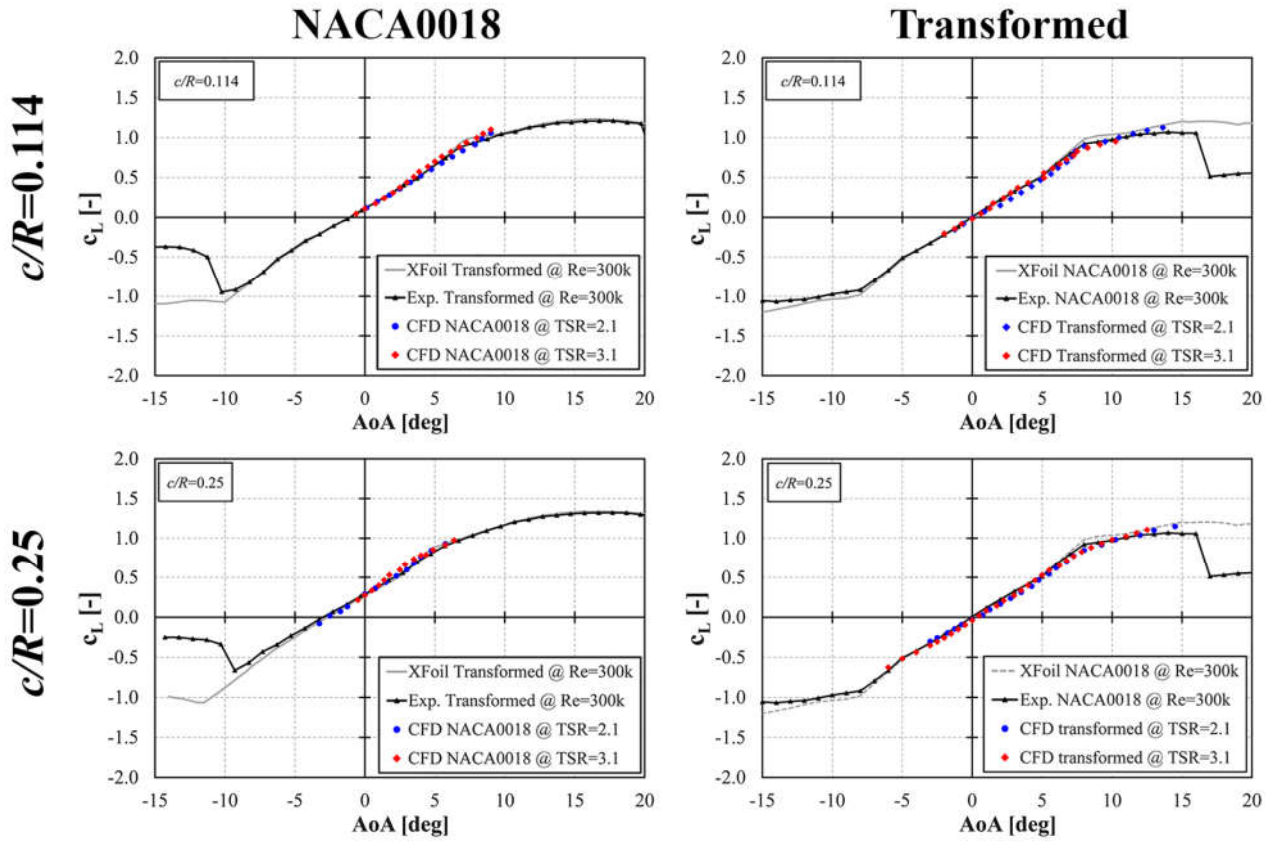
491 **Figure 13 – Pressure profiles comparison @ $\vartheta=32.8^\circ$, TSR=3.1 for all the simulated airfoils: CFD**
 492 **data vs. matching profile over the transformed airfoil due to the virtual camber effect.**

493

494 Once all the azimuthal positions were processed, the equivalent polars (i.e. the lift and drag
 495 outputs) of the airfoils during their revolution were reconstructed from the numerical evaluation
 496 of the tangential and normal forces exerted by the airfoils themselves.

497 Figs 14 and 15 show the trends for lift and drag coefficients, respectively, for all four airfoils
 498 in motion.

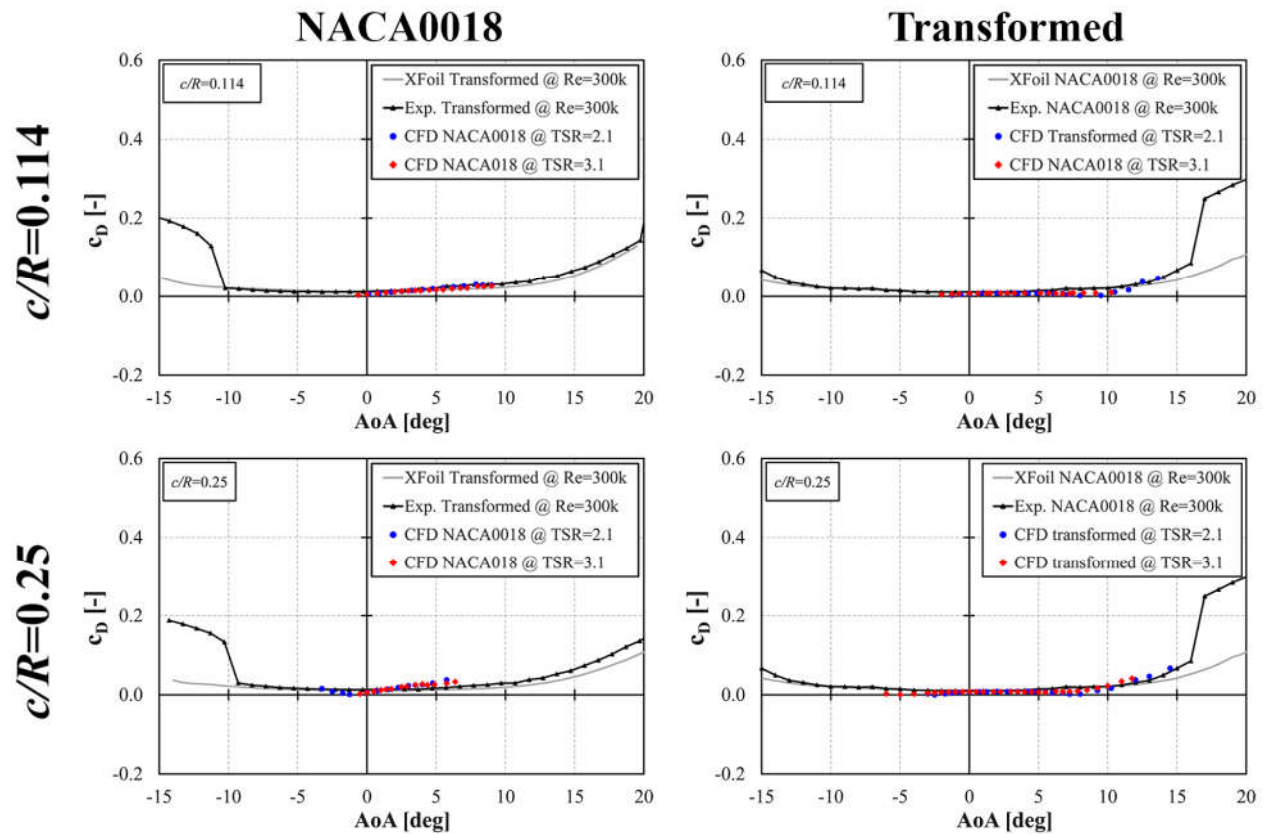
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Figure 14 – Reconstructed lift polars for the four simulated airfoils at TSR=2.1 and TSR=3.1.



502

503

Figure 15 – Reconstructed drag polars for the four simulated airfoils at TSR=2.1 and TSR=3.1.

504

505 There is excellent agreement between reconstructed data from CFD and blade polars based
506 on the equivalent virtual airfoil, whether calculated using XFOIL or obtained experimentally. The
507 lift coefficients are particularly well reproduced for both c/R ratios, in terms of both the slope of
508 the linear region and the lift coefficient at $\text{AoA} = 0^\circ$. Small discrepancies can be noticed in the
509 drag coefficient produced by the CFD computed NACA0018 airfoil at the higher c/R ratio,
510 which seems to increase more rapidly than that of the virtual transformed airfoil, although the
511 very low absolute value of the drag is more sensitive to small errors produced by the analysis
512 (e.g. in the correct estimation of the relative speed). Also, experiments used force transducers
513 rather than a wake traverse to measure drag, which can be inaccurate where drag forces are very
514 low.

515 It should be noted that since the incidence calculation technique works by matching CFD
516 airfoil pressure distributions to the nearest match produced in XFOIL, the incidences produced are
517 inclusive of any virtual incidence effects. Thus, as mentioned, in Fig. 13 different incidences are
518 calculated at different c/R ratios for the same airfoil, in spite of TSR and azimuth being the same,
519 and the effects of virtual incidence have no impact on Figs 14 and 15.

520 All the above results clearly demonstrate that the virtual camber effect originally postulated
521 Migliore et al [17] strongly affects the aerodynamic behavior of a Darrieus turbine with a
522 medium-high chord-to-radius ratio. They also confirm the blade design criteria proposed by
523 Balduzzi et al. [13] and demonstrate the suitability of the presented experimental data to the
524 simulation of this type of wind turbine.

525 **6. Potential benefits on BEM analyses**

526 The main use of the results obtained in the present work is connected to their potential impact
527 on BEM models. Though more advanced prediction models are available (e.g. CFD or vortex
528 models), these simplified theories can still provide some advantages under defined

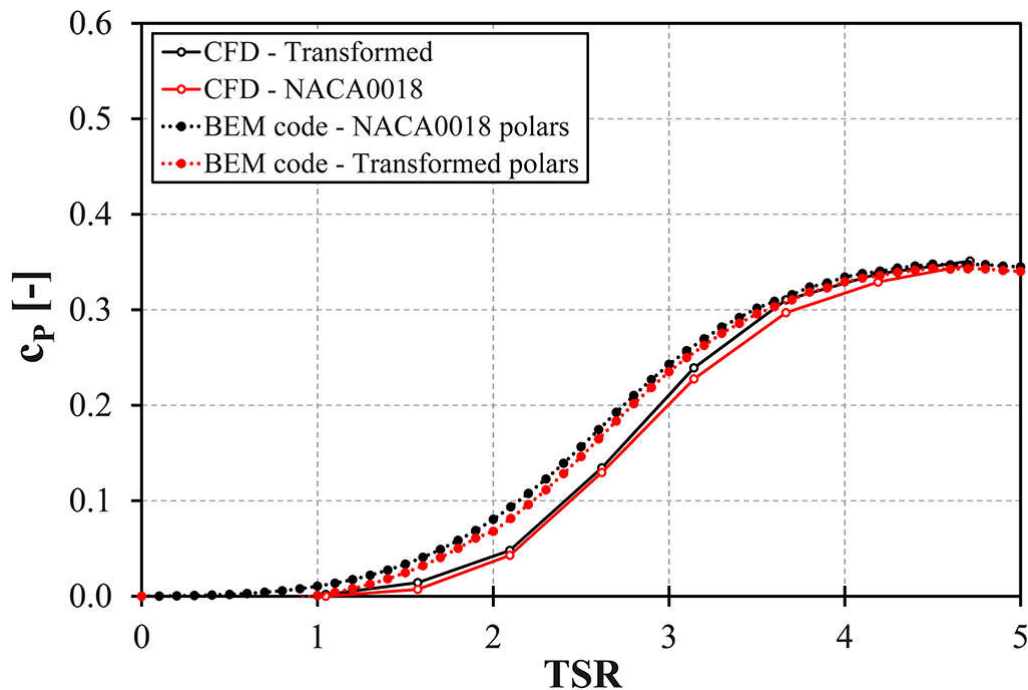
529 circumstances, especially concerning the general design of a machine (e.g. overall dimensions
530 and attended power) and particularly when a reduction of the computational cost is needed [18].

531 Based on a lumped parameters approach to aerodynamics, BEM models are intrinsically very
532 sensitive to the accuracy of input blade data in terms of lift and drag coefficients [44].
533 Discrepancies in tabulated data, even small, can substantially affect the reliability of the
534 predictions, both in terms of peak efficiency and of optimal TSR.

535 To quantify the effect of neglecting virtual camber, experimental coefficients collected in the
536 wind tunnel (and matched by CFD) were used in the VARDAR BEM code of the University of
537 Florence, which has previously been validated against experimental results for a Darrieus turbine
538 taken in a wind tunnel [18] and is considered a robust design tool for H-Darrieus rotors [45].

539 Using the code, two turbine geometries were simulated either with the lift and drag
540 coefficients of the NACA 0018 or with those of the equivalent transformed airfoil at each c/R
541 ratio. Figs 16 and 17 report the comparison in terms of power coefficient curves between CFD
542 and BEM with turbine $c/R = 0.114$ and $c/R = 0.25$ respectively.

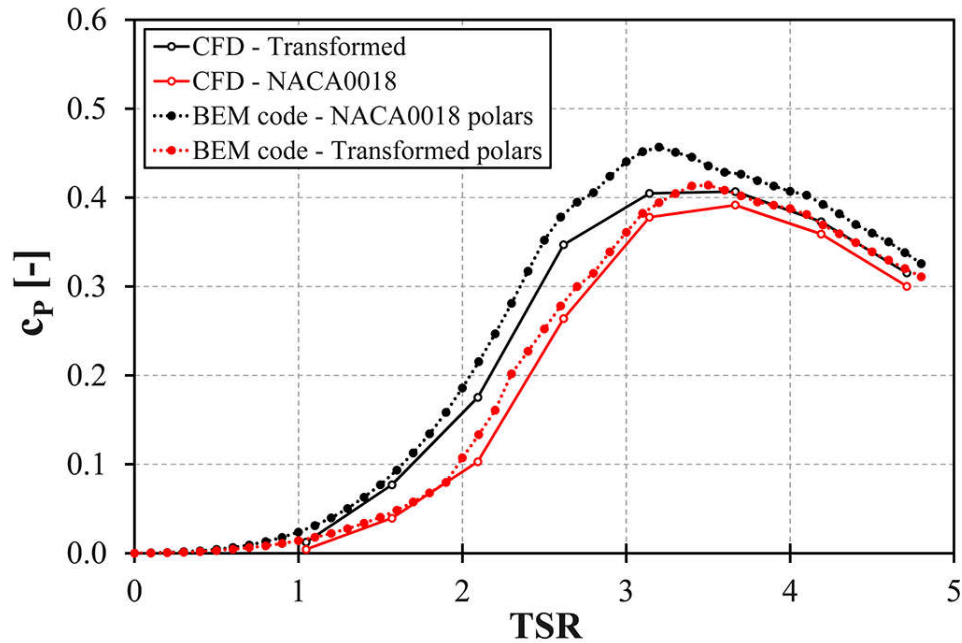
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Figure 16 – Comparison between BEM and CFD based power coefficient curves at $c/R=0.114$.



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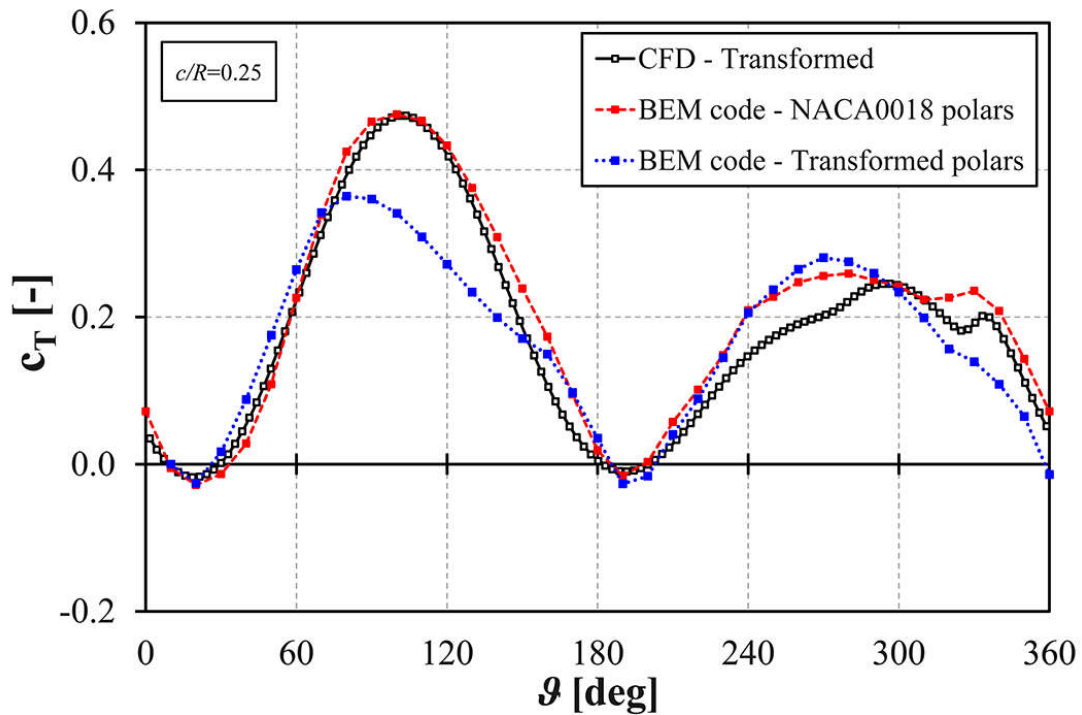
548 **Figure 17 – Comparison between BEM and CFD based power coefficient curves at $c/R=0.25$.**

549

550 For the $c/R = 0.25$ case, BEM predictions using the polar of the virtually transformed airfoil
 551 fit the 2D CFD simulation of the NACA 0018 well, and similarly BEM results for the NACA
 552 0018 fit the CFD simulation of the transforms. Agreement between BEM and CFD simulations
 553 using the same blade profiles is clearly worse. Agreement for the $c/R = 0.114$ case is strong
 554 between the two CFD simulations and the two BEM simulations, suggesting the impact of virtual
 555 camber is less extreme on the outputs of the two codes at this lower c/R .

556 The impact of virtual camber on the reliability of the code for the $c/R = 0.25$ case is more
 557 pronounced if the predictions of blade torque production as a function of the azimuthal position
 558 of the blade is analyzed. Fig. 18 compares the torque profile of the CFD transformed airfoil at
 559 $TSR = 3.1$ with the predictions of the BEM code using the coefficients of the transformed airfoil
 560 and the NACA 0018. The agreement is far stronger between the transformed airfoil and the
 561 virtually equivalent NACA 0018 than with its geometrical equivalent.

562 The use of the aerodynamic coefficients of the equivalent transformed airfoil appears
563 necessary for an accurate prediction of behavior using a BEM model when the c/R ratio is high,
564 with a remarkable improvement in the agreement of this theory to CFD.
565



566
567 **Figure 18 – Tangential force coefficient trends @ TSR=3.14 and $c/R=0.25$: CFD simulation of the**
568 **transformed airfoil vs. BEM predictions using different polars.**

569 7. Conclusions

570 Results have been presented in a study analyzing the impact of virtual camber effects on the
571 performance of Darrieus VAWT blades. The curved paths blades follow have been hypothesized to
572 impart virtual camber and incidence on them, making their behavior analogous to cambered airfoils
573 in rectilinear flow. Comparisons have been made between results from wind tunnel tests in
574 rectilinear flow on a NACA 0018 and two blades modified to exhibit the virtual camber expected in
575 VAWTs with $c/R = 0.114$ and 0.25 , and CFD simulations of the curvilinear flow of VAWT blades
576 for the same three profiles.

577 Curves of lift and drag against incidence computed from the CFD results match not to the same
578 profile from the wind tunnel work, but to the relevant equivalent virtually transformed profile. This
579 suggests that virtual camber is a significant contributor to VAWT performance and as such must be
580 considered when making use of low order models such as BEM codes.

581 An analysis is made of VAWT performance in terms of power coefficient against TSR using
582 BEM with the experimental polars of the three airfoils, alongside a similar analysis using CFD. For
583 higher c/R ratios, there is again good agreement between relevant transformed pairings and not
584 between geometrically identical airfoils. This agreement extends to a blade-level analysis prepared
585 using the BEM and the CFD in the form of blade tangential force coefficients against azimuth.

586 Findings show that consideration of curvature effects is necessary to obtain accurate results
587 from BEM codes that are comparable to those of CFD. When simulating a VAWT using BEM
588 methods, blade data for input should be selected based not on the physical geometry of the blade,
589 but on that of a transformed profile. The profile should have camber such that its chord aligns with
590 an arc of the circumference of the turbine. Such profiles can be calculated using conformal
591 transformation theory. The incidence of this transformed blade should be adjusted in line with
592 Migliore's virtual incidence. If experimental data is not available for the transformed profile, it can
593 be obtained using the methods described in this paper, or estimated (for attached flows) by
594 simulating the transformed airfoil shape using panel methods.

595 **8. Acknowledgments**

596 We would like to thank Prof. Ennio Antonio Carnevale of the University of Florence for
597 supporting this research program. Experimental work was supported financially by the
598 Environmental Services Association Education Trust.

599 **9. Nomenclature**

600 Acronyms

601	AoA	Angle of Attack
602	BEM	Blade Element Momentum
603	CFD	Computational Fluid Dynamics
604	OAR	Open Area Ratio
605	SST	Shear Stress Transport
606	TSR	Tip-Speed Ratio
607	U-RANS	Unsteady Reynolds-Averaged Navier-Stokes
608	VAWTs	Vertical Axis Wind Turbines

609

610 Greek symbols

611	α	Angle of Attack (symbol)	[deg]
612	γ	Intermittency	
613	ϑ	Azimuthal Angle	[deg]
614	π	Dimensionless Pressure Coefficient	[-]
615	ω	Specific Turbulence Dissipation Rate	[1/s]
616	Ω	Revolution Speed	[m/s]

617

618 Latin symbols

619	A	Turbine's Swept Area	[m ²]
620	c	Blade Chord	[m]
621	c_D	Drag Coefficient	[-]
622	c_L	Lift Coefficient	[-]
623	c_P	Power Coefficient	[-]

624	c_T	Tangential Force Coefficient	[-]
625	D	Rotor Diameter	[m]
626	F_t	Tangential Force	[N]
627	g	Slatted Wall Spacing	[m]
628	k	Turbulence Kinetic Energy	[m ² /s ²]
629	L	Plenum Chamber Length of the Tolerant Tunnel	[m]
630	p	Plenum Chamber Depth of the Tolerant Tunnel	[m]
631	R	Rotor Radius	[m]
632	Re	Reynolds Number	[-]
633	Re_θ	Momentum Thickness Reynolds Number	[-]
634	s	Slatted Wall Distance	[m]
635	U	Undisturbed Wind Speed	[m/s]
636	w	Relative Speed	[m/s]
637	y^+	Dimensionless Wall Distance	[-]
638			

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