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Darrieus Wind Turbine Blade Unsteady Aerodynamics: a Three-Dimensional Navier-Stokes CFD assessment

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Abstract

Thanks to the recent rapid progress in high-performance computing and the growing availability of large computational resources, computational fluid dynamics now offers a cost-effective, versatile and accurate means to improve the understanding of the unsteady aerodynamics of Darrieus wind turbines, increase their efficiency and delivering more cost-effective structurally sound designs.

In this study, a Navier-Stokes CFD research code featuring a very high parallel efficiency was used to thoroughly investigate the three-dimensional unsteady aerodynamics of a one-blade Darrieus rotor. Highly spatially and temporally refined time-dependent simulations were carried out using up to 16,000 processor cores per simulation on an IBM BG/Q cluster. The study aims at providing a detailed description and quantification of the main three-dimensional effects associated with the cyclical motion of this turbine type, including tip losses, dynamic stall, vortex propagation and blade/wake interaction. On one hand, the results corroborate the findings of several carefully designed two-dimensional studies. On the other hand, they reveal that the three-dimensional flow effects affecting Darrieus rotor blades are much more complex than assumed by the conventional lower-fidelity models often used for design applications, and strongly vary during the rotor revolution.

Keywords

Darrieus wind turbine, unsteady Navier-Stokes simulations, CFD, tip-effects, 3D flows

Nomenclature

Latin symbols

AoA angle of attack

42	AR	aspect ratio	[-]
43	c	blade chord	[m]
44	C_m	moment coefficient	[-]
45	C_p	pressure coefficient on airfoils	[-]
46	BEM	Blade Element Momentum	
47	CFD	Computational Fluid Dynamics	
48	H	turbine height	[m]
49	k	turbulent kinetic energy	[m ² /s ²]
50	NS	Navier-Stokes	
51	p	static pressure	[Pa]
52	R	turbine radius	[m]
53	T	torque per unit length	[Nm]
54	TKE	turbulent kinetic energy	
55	TSR	tip-speed ratio	[-]
56	U	wind speed	[m/s]
57	U_z	vertical velocity component	
58	VAWTs	Vertical-Axis Wind Turbines	
59	w_{th}	theoretic relative wind speed	[m/s]
60	x, y, z	reference axes	
61	y^+	dimensionless wall distance	[-]
62			
63	<u>Greek symbols</u>		
64	ϑ	azimuthal angle	[deg]
65	μ_t	turbulent viscosity	[Kg/m/s]
66	ρ	air density	[kg/Nm ³]
67	Φ	computational domain diameter	[m]
68	Ψ	computational domain height	[m]
69	ω	specific turbulence dissipation rate	[1/s]
70	Ω	turbine revolution speed	[rad/s]
71			
72	<u>Subscripts</u>		
73	∞	value at infinity	
74	ave	averaged value	
75			

76 1. Introduction

77 1.1 Background

78 After most research projects on vertical-axis wind turbines (VAWTs) came to a
79 standstill in the mid 90's [1], the Darrieus wind turbine [2] is receiving rapidly increasing
80 attention of both researchers and manufacturers [3-5]. For distributed wind power generation
81 in the built environment [6], inherent advantages of this turbine type, such as performance
82 independence on wind direction, generator often positioned on the ground, low noise
83 emissions [7], enhanced performance in skewed flows [8] may outweigh disadvantages such
84 as lower power coefficients and more difficult start-up with respect to typical horizontal axis
85 machines. Moreover, in densely populated areas VAWTs are often preferred to other turbine
86 types because they are perceived as aesthetically more pleasant and thus easier to integrate in
87 the landscape [9]. The applicability of Darrieus wind turbine for utility-scale power
88 generation making use of floating platforms also appears to present important benefits in
89 terms of overall dynamic stability [10].

90 Historically, the aerodynamic performance analysis of these rotors has been carried out
91 with low-fidelity methods, like the Blade Element Momentum (BEM) theory [1,11-13] or
92 lifting line methods [14-15]. More recently, however, the intrinsic limitations of these models
93 made clear that higher-fidelity tools are needed in order to understand in greater depth the
94 complex physical phenomena taking place during the revolution of Darrieus rotors [16], like
95 the interaction of the blades with macro vortices [17] or dynamic stall [18].

96 If experimental testing is often extremely difficult and expensive, Navier-Stokes (NS)
97 Computational Fluid Dynamics (CFD) can provide versatile and accurate means to improve
98 the understanding of VAWT unsteady aerodynamics and achieve higher-performance,
99 structurally sound and more cost-effective Darrieus turbine designs. The use of NS CFD for
100 simulating time-dependent Darrieus turbine aerodynamics is rapidly increasing due to both
101 the ongoing development and deployment of more powerful high-performance computing
102 hardware, such as large clusters of multi- and many-core processors [19], and also the
103 development of computationally more efficient algorithms.

104 *1.2 Previous CFD studies on Darrieus VAWTs*

105 Early assessments of the Reynolds-Averaged Navier-Stokes (RANS) CFD technology
106 for Darrieus rotor aerodynamics, aiming primarily at thoroughly investigating the complex
107 fluid mechanics of these machines, made use mainly of a two-dimensional (2D) approach
108 (e.g. [20-21]); an extensive literature review on these studies has been recently provided by
109 Balduzzi et al. [22]. The use of a 2D approach was motivated by the need of maintaining the
110 computational cost of the simulations within acceptable limits, since the fully-unsteady
111 solution of the three-dimensional (3D) flow field past rotating Darrieus rotors requires very
112 large computational resources due to the very large temporal and spatial grid refinement
113 needed for obtaining reliable results [22-24]. Unfortunately, the use of 2D simulations results
114 in some important aerodynamic features being discarded (e.g. tip flow effects, secondary
115 flows, etc.). Notwithstanding this, recent work [25-28] showed that properly-set 2D approach
116 can provide estimations of the power curve of a rotor relatively close to the experimental
117 value and, thus, usable for preliminary design.

118 Since the early 2D studies of Darrieus rotor aerodynamics based 2D NS CFD,
119 researchers have longed to perform 3D simulations of these machines in order to fully
120 understand some phenomena that are presently modeled on the basis of a relatively small
121 amount of data from existing turbines and assumptions based on overly simplistic analytical
122 models.

123 In the past few years, thanks to the growth of available computational resources, 3D NS
124 CFD analyses have received increasing attention, and some preliminary studies have been
125 published. First, comparisons between two-dimensional simulations and three-dimensional
126 ones have been carried out (e.g. [29]). Purely 3D studies have been also carried out to
127 characterize the turbine wake [29], the flow field around the blades [30-31], the start-up of
128 small rotors [32], the effects of the finite aspect ratio [33-34] or the influence of supporting
129 arms [35] and different blade shapes [36]. Finally, further studies were also focused on the
130 performance variation in skewed flow conditions [37-38].

131 Although many of the aforementioned analyses have indeed highlighted new important
132 aerodynamic phenomena, in almost all cases limited computational resources imposed the use
133 of fairly coarse spatial and temporal refinements, which often did not match the requirements
134 indicated by proper sensitivity analyses. In particular, the common approach found in the
135 literature was to progressively coarsen the meshes when moving to 3D analyses, in order to
136 limit the total number of cells in the range between 1,000,000 and 10,000,000.

137 Most recent 2D parametric CFD analyses of Darrieus rotors (e.g. [22-24]) showed that
138 the simulation reliability is tremendously affected by the quality of the meshing and time-
139

stepping strategies. In particular, it has been shown that the minimum temporal and spatial refinement levels required to obtain grid-independent solutions is quite high, due to the aerodynamic complexity of these unsteady rotor flows. Therefore, when using 3D unsteady NS CFD for Darrieus rotor aerodynamics, the computational cost of the simulation becomes very large due to necessity of maintaining high levels of time-refinement and a high level of spatial refinement both in the grid planes normal to the rotor axis and the axialwise direction. Refinement in the latter direction is mandatory to reliably resolve 3D flow features. Failing to maintain suitably high grid refinement levels in all three direction results in losing the potential of 3D simulations of improving the design of these machines by properly resolving 3D effects. As an example, one of the previous studies based on 2D RANS CFD showed that temporal and spatial grid-independent solutions are obtained provided that grids with at least 400,000 elements are used [22]. To preserve the same accuracy level in a 3D RANS simulation of the same turbine (modelling only half of the rotor making use of symmetry boundary conditions on the plane at rotor midspan) the mesh would consist of about 90,000,000 cells, which is almost ten times the size of the finest meshes used in the 3D RANS studies of Darrieus rotor flows published to date.

1.3 Study aim

In this study, a RANS CFD research code featuring a very high parallel efficiency is used to investigate the detailed features of 3D flow effects of a rotating Darrieus rotor blade and the impact of such effects on the power generation efficiency of the blade. To accomplish this, a time-dependent 3D simulation using very high levels of spatial and temporal refinements and yielding a fairly reliable assessment of the phenomena under investigation is carried out using a large 98,304-core IBM BG/Q cluster. The scope of the study is to analyze the main 3D effects occurring during the cyclic motion of the considered one-blade rotor configuration, including tip vortices, dynamic stall and downstream vortex propagation, and to assess the impact of these phenomena on the overall performance of this rotor.

The paper is organized as follows: sections 2 and 3 summarize the main features of the case study and the numerical framework, respectively. Section 4 presents the main results of the 3D analysis and compares them to those of the 2D analysis of the same case study, in order to highlight the impact of 3D effects by comparing the integral performance of a finite-length rotor and that of the infinite blade counterpart. A summary of the study and concluding remarks are finally provided in section 5.

2. Case study

The case study selected for the 3D simulation is a one-blade H-Darrieus rotor using a NACA 0021 airfoil. The chord ($c=0.0858$ m) and radius ($R=0.515$ m) of this virtual rotor were taken equal to those used in the case-study of [21]; the blade was attached at midchord. In order to reduce the computational cost of the simulation, the central symmetry of H-Darrieus rotors was exploited, allowing to simulate only one half of the blade rather than the entire blade length of $H=1.5$ m. Thus, the aspect ratio (AR) of the simulated blade portion is 8.74 which is half that of the actual blade. The blade was contained in a cylindrical computational domain (Fig. 1) having a radius $\Phi=240R$, a value chosen to guarantee a full development of the wake [26]. The domain height was instead set to $\Psi=2.53H$, corresponding to half the height (due to the central blade symmetry) of the wind tunnel where the original model was tested [21,39]; data of these tests were used for the validation of the numerical RANS CFD approach [26-28], that can be viewed as the 2D counterpart of the 3D methodology used in the present study.

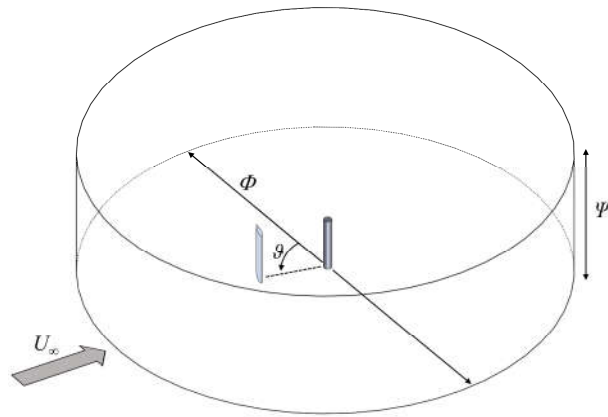


Figure 1 - Computational domain.

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The 3D mesh (details are reported in Fig. 2) was obtained by first generating a 2D mesh past the airfoil using the optimal mesh settings identified in [26-27], and then extruding this mesh in the spanwise (z) direction and filling up with grid cells the volume between the blade tip and the circular farfield boundary.

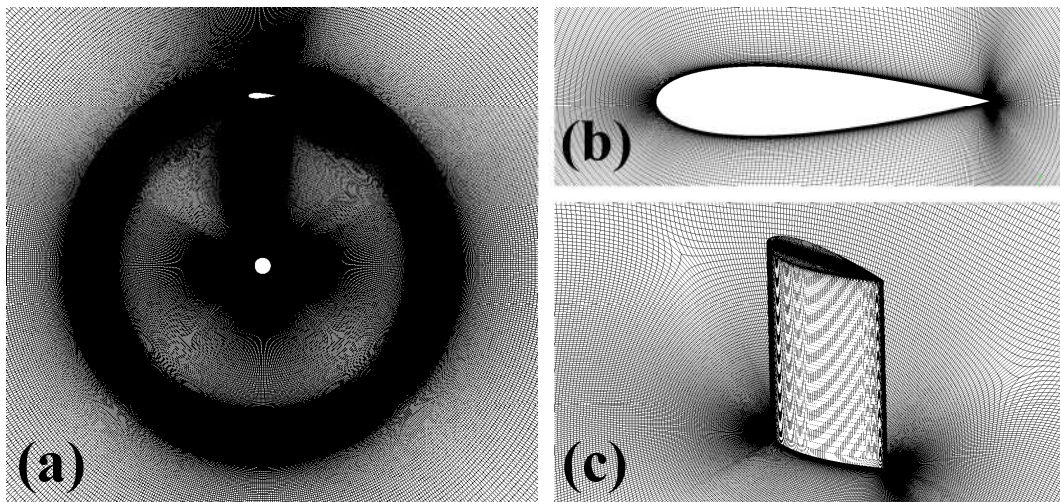


Figure 2 - Some details of the computational mesh.

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The 3D grid is structured multi-block. Its 2D section normal to the z -axis and in the z -interval occupied by the blade (Fig. 2(a)) consisted of 4.3×10^5 quadrilateral cells. The airfoil was discretized with 580 nodes and the first element height was set to $5.8 \times 10^{-5}c$ to guarantee a dimensionless wall distance y^+ lower than 1 throughout the revolution. As recommended in [22], a proper refinement of both leading and the trailing edge regions was adopted (Fig. 2(b)), as well as a globally high refinement in the region around the airfoil within one chord from the walls in order to properly resolve the detached flow regions at high angle of attack (AoA) [40]. After extrusion in the z direction, 80 layers in the half-blade span were formed (Fig. 2(c)), with progressive grid clustering from midspan to the tip in order to ensure an accurate description of tip flows. Moreover, a high grid refinement level was provided in the whole tip region above the blade in order to properly capture the flow separation and the tip vortices. The final mesh was made of 64×10^6 hexahedral cells and able to fulfill the numerical requirements prescribed in [24].

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Due to the large burden associated with running the 3D time-dependent simulation, only a single operating condition was simulated, corresponding to a tip-speed ratio (TSR) of 3.3. The free stream wind speed was $U=9$ m/s. The turbulence farfield boundary conditions

216 were a turbulent kinetic energy (k) based on 5% turbulence intensity and a characteristic
217 length of 0.07 m (according to [27], production limiters were used in the simulations on k and
218 ω with a setting ratio of 10).
219

220 **3. Computational framework**

221 All RANS simulations were carried out with the COSA research code.

222 COSA is a compressible density-based structured multi-block finite volume RANS
223 code featuring a steady flow solver, a time-domain (TD) solver for the solution of general
224 unsteady problems [41-42], and a harmonic balance solver for the rapid solution of periodic
225 flows [43-45]. The RANS equations are obtained by time-averaging the Navier-Stokes
226 equations on the characteristic turbulent time-scales of the problem at hand. The RANS
227 equations are formally similar to the Navier-Stokes equations, and differ from those for the
228 fact that all thermodynamic and kinematic variables are mean rather than instantaneous
229 values, and for the presence of an additional term, the Reynolds stress tensor accounting in a
230 mean fashion for the effects of turbulence. Making use of Boussinesq's approximation, the
231 Reynolds stress tensor is given by the product of an eddy viscosity μ_t and the strain rate
232 tensor of the mean velocity field. COSA determines μ_t with Menter's k - ω shear stress
233 transport turbulence model [46]. The second-order space discretization of the convective
234 fluxes of both the RANS and the SST equations uses an upwind scheme based on Van Leer's
235 MUSCL extrapolations and Roe's flux difference splitting. The second order discretization of
236 all diffusive fluxes is instead based on central finite-differencing. The space-discretized
237 RANS and SST equations are integrated in a fully-coupled fashion with an explicit solution
238 strategy based on full approximation scheme multigrid featuring a four stage Runge-Kutta
239 smoother. Convergence acceleration is achieved by means of local time-stepping and implicit
240 residual smoothing. For general time-dependent problems, the TD equations are integrated
241 using a second order dual time-stepping approach.

242 Comprehensive validation analyses of COSA are reported in [43,45] and other
243 references cited therein. For unsteady problems involving oscillating wings and cross-flow
244 open rotors such as the Darrieus rotor configuration investigated in this paper, COSA solves
245 the governing equations in the absolute frame of reference using an arbitrary Lagrangian-
246 Eulerian formulation and body-fitted grids. In the case of Darrieus rotors this implies that the
247 entire computational grid rotates about the rotational axis of the turbine. The suitability of
248 COSA for the simulation of Darrieus wind turbines has been recently assessed through
249 comparative analyses with both commercial research codes and experimental data [26-27].

250 Present simulations were run on an IBM BG/Q cluster [47], featuring 8,144 16-core
251 nodes with a total of 98,304 cores. Thanks to the excellent parallel efficiency of the COSA
252 code, the simulations yielding the results presented in this paper could be performed using
253 about 16,000 cores. This required partitioning the grid into 16384 blocks, making use of in-
254 house utilities. Using a time-discretization of 720 steps per revolution, the simulation needed
255 12 revolutions to achieve a fully periodic state. The flow field over the consider period was
256 assumed to be periodic once the difference between the mean torque of the last two
257 revolutions was smaller than 0.1% of the mean torque in the revolution before the last. The
258 wall-clock time required for the complete simulation was about 653 hours (27.2 days).
259

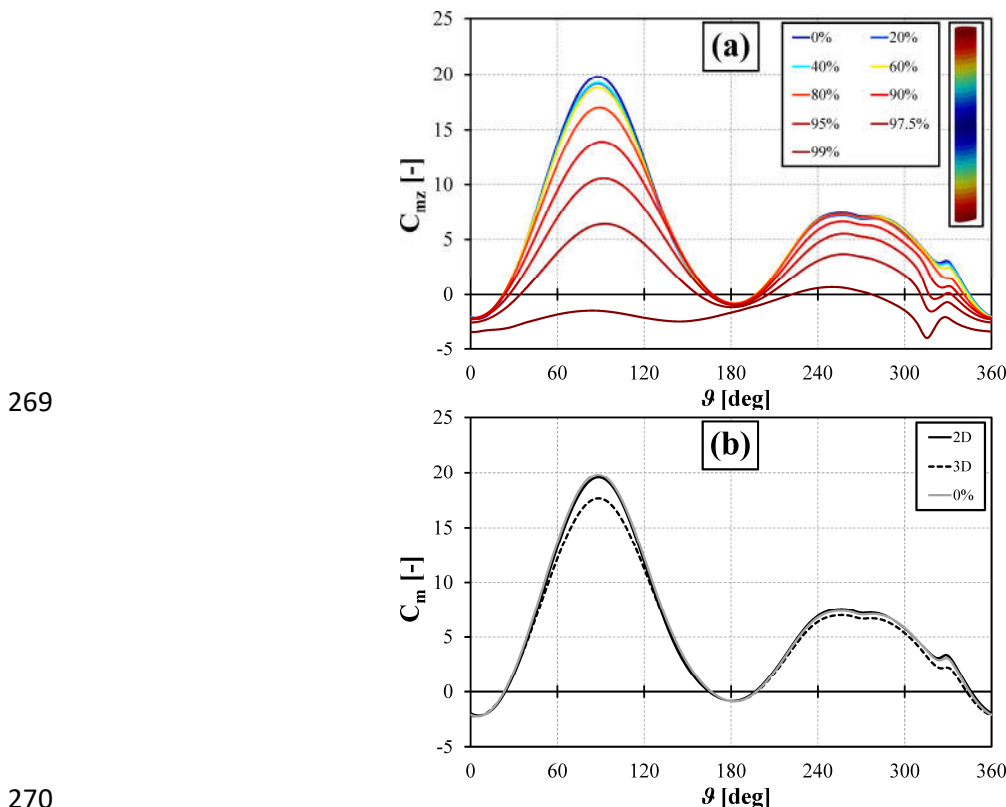
260 **4. Results**

261 Figure 3(a) reports the periodic profile of the torque coefficient per unit blade length at
262 different span lengths along the blade (0% and 100% correspond to midspan and tip,

263 respectively). The angular position $\vartheta=0^\circ$ corresponds to the blade leading edge facing the
 264 oncoming wind and entering the upwind half of its revolution.

265 The instantaneous torque coefficient per unit length C_{mz} is defined by Eq. (1). Here T_z
 266 denotes the instantaneous torque per unit blade length at the considered z position, U_∞ and ρ_∞
 267 denote the wind speed and the air density, respectively, and c denotes the blade chord.

$$268 \quad C_{mz} = \frac{T_z}{\frac{1}{2} \rho_\infty U_\infty^2 c^2} \quad (1)$$



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270
 271 **Figure 3 - Moment coefficient vs azimuthal angle: (a) variation at different span lengths; (b) 2D**
 272 **simulations compared to the 3D profile at midspan and average 3D profile.**

273

274 Figure 3(b) reports three torque profiles. One, that labelled *2D*, refers to the results of a
 275 2D simulation of the same rotor, and corresponds to the “ideal” torque of a blade with infinite
 276 span, i.e. without any secondary effects at the blade tip. This 2D simulation was carried out
 277 using a mesh equal to the midspan section of the 3D mesh and the same numerical parameters
 278 of the 3D simulations. The torque profile labelled *0%* is the torque per unit blade length at the
 279 midspan position of the finite-length rotor, whereas the torque profile labelled *3D* is the
 280 overall torque coefficient C_m of the 3D rotor defined as:

$$281 \quad C_m = \frac{2}{H} \int_0^{\frac{H}{2}} C_{mz} dz \quad (2)$$

282 Examination of these profiles reveals several important facts. Firstly, the ideal 2D
 283 torque and the 3D torque profiles are characterized by similar patterns, including the
 284 occurrence of two relative maxima, one in the upwind the other in the downwind regions, and
 285 also similar blade azimuthal positions of both maxima: the maximum torque in the upwind
 286 portion of the revolution is located at $\vartheta \approx 88.5^\circ$ and the maximum torque in the downwind

287 portion of the revolution is located at $\vartheta \approx 257^\circ$ in both cases. An almost perfect match is
288 visible between the 2D curve and the curve at midspan of the 3D rotor, highlighting that 3D
289 flow effects due to tip flows do not reach this position.

290 Examination of all profiles of Fig. 3(b) shows that the effects of blade finite-length
291 effects are very small when the blade loading is low, i.e. when the angle of attack on the
292 airfoil is low (azimuthal positions between $0^\circ < \vartheta < 40^\circ$, $130^\circ < \vartheta < 210^\circ$): in these portions of the
293 revolution, the 2D and both 3D curves (i.e. the local profile at 0% span and the average one)
294 are indeed almost superimposed. When the incidence increases, the blade load also increases.
295 Consequently, torque reduction due to tip effects also increases both in the upwind and the
296 downwind zones. This is because the strength of tip vortex flow increases with the flow
297 incidence. By further looking at the comparison between the 2D curve and the mean 3D
298 curve, one can notice that these effects are strongest in the upwind region of the period,
299 where a maximum difference of 9.7% between the torque peaks occurs.

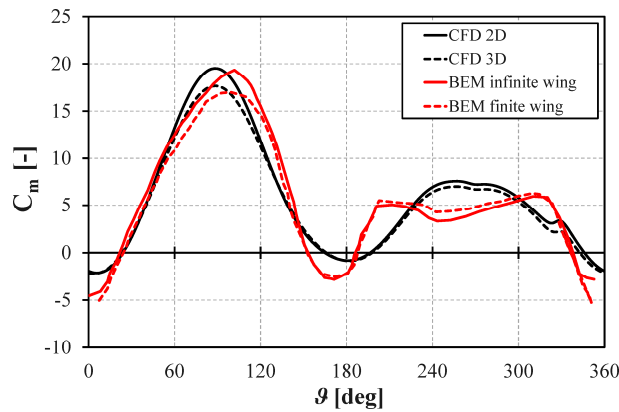
300 Focusing now on the torque profiles at different semispan lengths reported in Fig. 3(a),
301 some additional observations can be made, more specifically:

- 302 • The torque profiles of the blade sections at 20%, 40% and 50% of the semispan are
303 almost identical, indicating that at least half of the blade is characterized by a
304 predominantly 2D flow with negligible impact of tip flow effects;
- 305 • The torque profiles of the blade sections at 60%, 70% and 80% start showing a
306 progressive reduction of the torque peak, down to -14% with respect to the midspan
307 section. The remainder of the torque curve is less affected, especially in the downwind
308 zone;
- 309 • The torque profiles of the blade sections at 90%, 95% and 97.5% show that at these
310 distances from the blade tip the 3D effects are strong throughout the whole revolution.
311 In particular, in the regions of positive torque production, the efficiency is remarkably
312 reduced;
- 313 • In proximity of the blade tip (99%) almost no positive contribution to the torque output
314 is given, due to the large load reduction;
- 315 • In general, the azimuthal position of the torque peak occurs later in the cycle as one
316 moves towards the tip, with a 5° shift between the 0% and 97.5% sections. This can be
317 explained with a reduction of the incidence angle (downwash), as will be shown in
318 continuation of the study.

319 To quantify the predicted impact of tip effects in comparison to existing knowledge,
320 Fig. 4 reports the comparison of the 2D and mean 3D torque profiles obtained with the CFD
321 analyses and the corresponding estimates obtained with the VARDAR research code, a state-
322 of-the-art BEM code developed at the University of Florence [6,13-14] using the ubiquitous
323 Leicester-Prandtl model for the finite-wing correction [48].

324 The two BEM profiles of Fig. 4 differ in that one includes tip flow corrections and the
325 other does not.

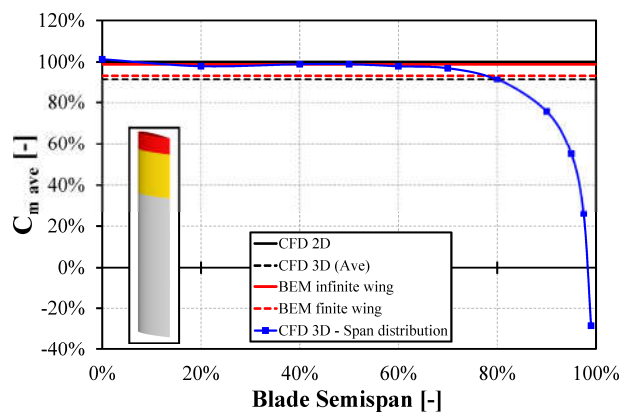
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328 **Figure 4 - Moment coefficient profiles: 2D and 3D CFD data vs. BEM simulations either including or**
329 **neglecting the finite-wind effects.**
330

331 Examination of these profiles highlights, that the extent of the upwind moment peak
332 reduction predicted by the CFD analyses is in good agreement with that estimated with the
333 simplified tip flow model included in the BEM theory. On the other hand, the lumped
334 parameter model appears not to be able to properly capture the moment coefficient reduction
335 in the downwind portion of the revolution. Conversely to the CFD results, the BEM model in
336 fact predicts a slight moment increase downwind, since the lower energy extraction predicted
337 in the upwind zone leads to a lower induction factor and then to a higher attended wind speed
338 in the downwind flow. This comparative analysis highlights the potential of using CFD for
339 further improving the analysis, and ultimately the design, of Darrieus rotor aerodynamics
340 since it may provide a new insight for the calibration of proper corrections in lower-order
341 models.

342 To provide a different quantitative perspective of the impact of tip losses, Fig. 5 reports
343 the profile of mean moment coefficient per unit length. The mean value for each blade height
344 is obtained by averaging the profiles of Fig. 3(a) over one revolution. The figure also reports
345 the constant mean torque values of the 2D and 3D simulations for both the CFD and BEM
346 models. All curves are normalized with respect to the mean 2D moment coefficient.
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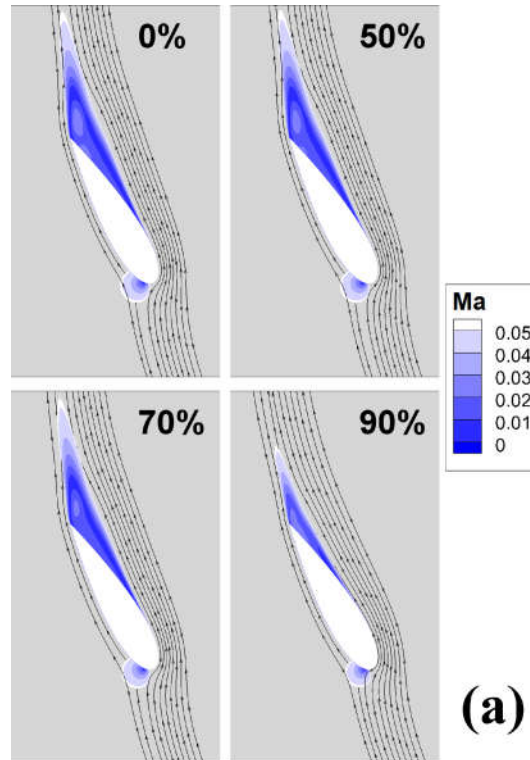


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349 **Figure 5 - Moment coefficient distribution along the semispan.**
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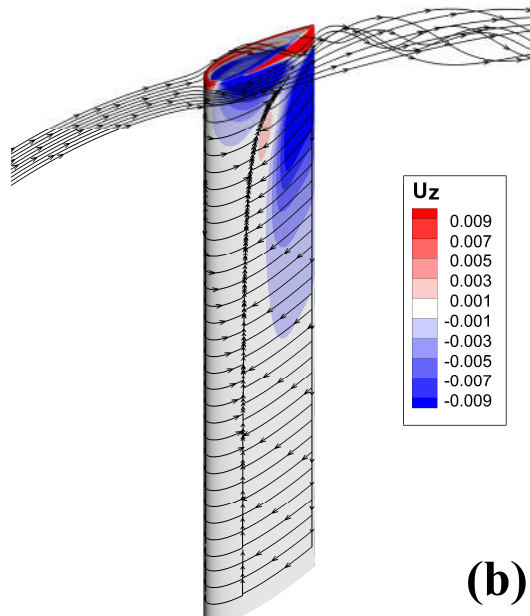
351 One sees that the average blade performance is almost unaffected by tip-effects up to
352 approximately 70% of its semispan. Expressing these results with reference to the blade
353 chord and aspect ratio, it is found that tip flow effects adversely affect the performance of the
354 blade for a span length of approximately $2.6c$ (yellow zone in Fig. 5). In terms of aggregate
355 data, the tip effects yield a reduction of the rotor torque of 8.6% with respect to the
356 theoretical 2D calculation with virtually infinite span. This can be globally seen as an
357 equivalent reduction of the actual blade's height by $0.75c$ (red colored zone in Fig. 5) and

358 such a correction factor needs to be accounted for when estimating the turbine's performance
359 by means of 2D CFD simulations.

360 To further investigate the phenomena that lead to the efficiency reduction, the Mach
361 contours and streamlines at the angular position of maximum separation (i.e. $\vartheta=120^\circ$) are
362 reported in Fig. 6(a). Different spanwise sections are reported to analyze the flow pattern
363 alterations as one moves towards the blade tip.
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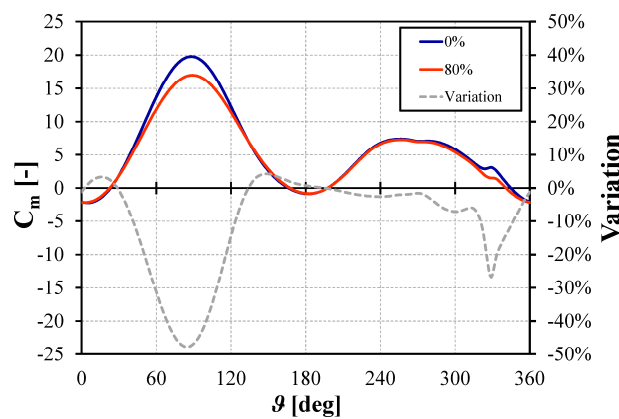
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Figure 6 – Downwash effect at $\vartheta=120^\circ$: (a) Mach contours and streamlines at different semispan locations; (b) flow streamlines in the tip region, skin friction lines and z velocity component on the blade surface.

372 In the central portion of the blade (i.e. from midspan to approximately 70% of the
 373 semispan) the flow streamlines lay on a 2D plane and a region of extensively detached flow is
 374 present along the entire blade. When moving closer to the tip (90% of the semispan), the
 375 discussed effect of the downwash field is apparent: the effective AoA on the airfoil is reduced
 376 with respect to that at midspan. As a consequence, the extension of the stall region is also
 377 reduced due to the decrease of the incidence angle.

378 The skin friction lines and contours of the z velocity component on the blade surface
 379 reported in Fig. 6(b) show the extension of the region affected by downwash on the suction
 380 side of the blade. Near the tip, the flow on the pressure side is no longer able to follow the
 381 blade profile, and travels over the tip due to the pressure difference between the pressure side
 382 and the suction side. The tip vortex flow is responsible for the downwash velocity component
 383 and therefore for the incidence variation along the span, in accordance with the theory of
 384 finite wings [48]. It is noted that the finite wing effects occurring in Darrieus rotors are more
 385 complex than those encountered in fixed finite wings. This is primarily because of the flow
 386 curvature associated with the circular trajectory of the blade, and also the flow nonlinearities
 387 due to dynamic stall. To quantify the impact of these effects, relevant outcomes can be
 388 obtained by examining the torque coefficient trends per unit length produced by two relevant
 389 slices in Fig. 7, e.g. midspan and 80%. The percentage difference between the two curves (i.e.
 390 the moment coefficient difference between the curves at each azimuthal angle divided by the
 391 average moment coefficient at midspan) is also reported to quantify the dependence of the
 392 torque variation on the azimuthal position.

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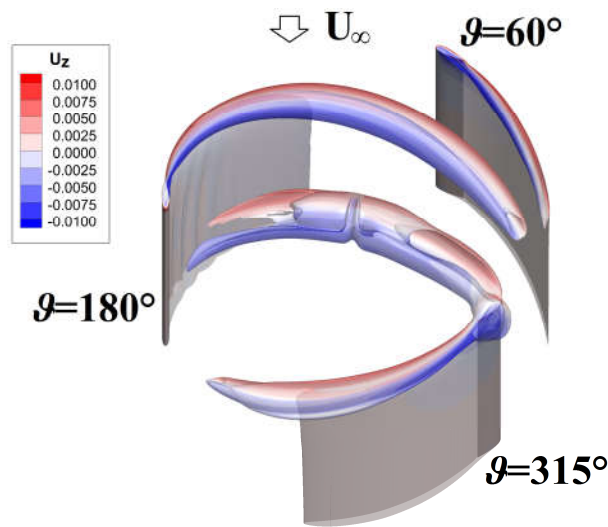
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Figure 7 - Comparison of moment coefficient curves at 0% and 80% of the semispan.

397 It is observed that a notable torque reduction occurs between $40^\circ < \theta < 130^\circ$. In addition,
 398 large and sudden torque reduction is obtained at the end of the revolution, i.e. when the
 399 incidence angle is progressively reducing towards values lower than the stall one. On the
 400 other hand, an inversion in the expected trend was noticed close to $\theta=150^\circ$, while the torque
 401 output is positive. Even if not apparent from the figure, a higher torque production (relative
 402 difference of +11% between 80% and midspan) was noticed when getting closer to the tip.
 403 According to the finite wing theory, the lift should be in fact always reduced in proximity of
 404 the tip and the reduction is assumed to be proportional to the lift itself. Therefore, the
 405 inversion at $\theta=150^\circ$ cannot be justified with this theory only, as well as the significant torque
 406 reduction at the end of the revolution, since the reduced load on the blades should not
 407 generate such a large downwash effect. Detailed flow analyses will be shown in the
 408 continuation of the study to comprehend this phenomenon.

409 First, the reasons of the sudden torque reduction at the blade tip between $315^\circ < \theta < 340^\circ$
 410 were investigated. To this purpose, Fig. 8 shows the evolution of the turbulence kinetic
 411 energy (TKE) field at selected azimuthal positions based on TKE iso-surfaces. The color

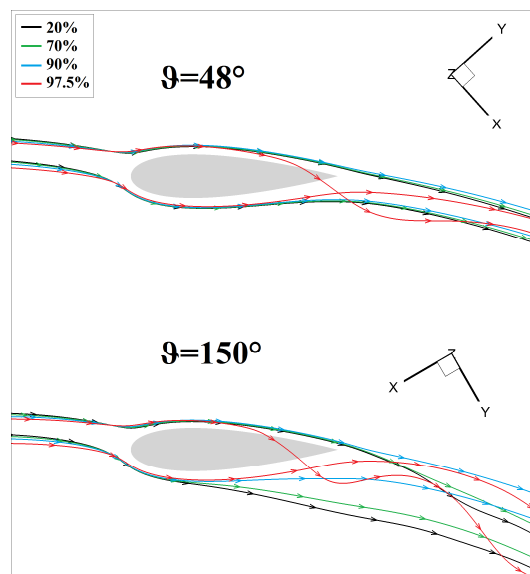
412 scale is based on the intensity of the velocity component along the z-axis (U_z). Three
 413 azimuthal positions of the blade are considered in Fig. 8, i.e. $\vartheta=60^\circ$ (upper right), 180° (left)
 414 and 315° (lower right).
 415



416 **Figure 8 - Iso-surfaces of TKE colored with the contour of U_z .**

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 419 During the upwind half of the revolution ($\vartheta=60^\circ$) the tip vortex is strong, since the
 420 vertical component of velocity is very intense. A high turbulence region is then generated in
 421 the tip's wake. For $\vartheta=180^\circ$, U_z is reduced around the blade tip, but the region of the blade
 422 wake characterized by high TKE is increased in size and length, and is still associated with
 423 large values of U_z . This generates a strong vortex, which is then detached from the blade,
 424 after being convected by the wind, is re-encountered by the blade itself at $\vartheta=315^\circ$. The
 425 interaction with this macro-vortex induces a more pronounced modification of the torque
 426 curve with respect to the 2D case, where this effect is totally absent.

427 Focusing now on the azimuthal position where the torque inversion takes place, i.e.
 428 $\vartheta=150^\circ$, Fig. 9 reports a top view of the flow streamlines at two azimuthal positions. The
 429 comparison is carried out with $\vartheta=48^\circ$, which is the other position during the upwind half of
 430 the revolution experiencing the same AoA of $\vartheta=150^\circ$. Streamlines on both the pressure and
 431 suction sides of the blade are visualized at four different span locations.
 432



433 **Figure 9 - Streamlines at different span lengths: $\vartheta=48^\circ$ and $\vartheta=150^\circ$.**

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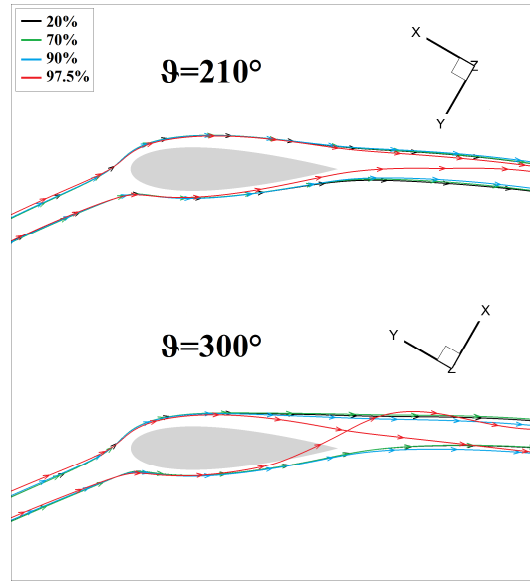
436 For $\vartheta=48^\circ$ the “conventional” downwash effect is clearly visible: moving from midspan
437 to the tip, the incidence of the oncoming flow is progressively reduced and the velocity past
438 the trailing edge is shifted towards the pressure side. This phenomenon is not very
439 pronounced due to the low load on the blade at this angular position and the deflection occurs
440 only in proximity of the tip.

441 For $\vartheta=150^\circ$, moving from midspan to the tip, the incidence of the oncoming flow is
442 progressively reduced similarly to what is seen at $\vartheta=48^\circ$ and no evident difference with
443 respect to the $\vartheta=48^\circ$ degrees case can be observed. Conversely, the flow condition on the
444 suction side is completely different: at midspan, even if the AoA is similar to that obtained at
445 $\vartheta=48^\circ$, a large separation region is evident at $\vartheta=150^\circ$ due to dynamic stall. As a result of the
446 finite wing effect, a strong modification of the flow is then observed when moving across the
447 blade span towards the tip: starting from 70% semispan, the flow re-starts to be deflected
448 towards the pressure side. At 90% semispan, the flow direction is completely changed with
449 respect to midspan: the separation region is indeed almost absent, making the flow become
450 very similar to that measured at $\vartheta=48^\circ$.

451 This can be explained by a combined effect of downwash and dynamic stall. During the
452 advancing 90-degree sector of the blade trajectory, only the sections at the tip do not reach
453 stall conditions since they experience a lower AoA. When the incidence decreases in the
454 retreating 90-degree sector, the central portion of the blade suffers from dynamic stall, with a
455 marked reduction of lift production. Conversely, the flow remains attached for the sections at
456 the tip, which are still able to produce the theoretical lift. It can be summarized that, even if
457 the incidence is lower close to the tip, the absence of dynamic stall guarantees higher lift and
458 therefore higher torque production than at midspan in this specific angular position. The
459 explanation is straightforward: when the incidence is maximum, the sections at the tip do not
460 reach stall conditions since they experience lower AoAs and when the incidence decreases
461 they are still able to produce the theoretical lift. It can be summarized that, even if the
462 incidence is lower close to the tip, the absence of dynamic stall guarantees, in this specific
463 condition, higher lift and therefore higher torque production.

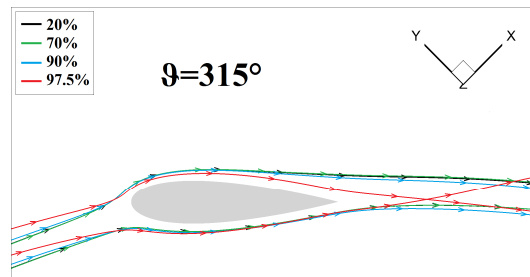
464 Fig. 10 shows an analogous comparison for the angular positions of $\vartheta=210^\circ$ and
465 $\vartheta=300^\circ$, which again are characterized approximately by the same AoA value. It can be
466 noticed that the behavior at $\vartheta=210^\circ$ is the same of that at $\vartheta=48^\circ$, being an angular position in
467 the downwind half of the rotation where the incidence is increasing (in magnitude). At
468 $\vartheta=300^\circ$ the incidence (decreasing) is again the same of $\vartheta=210^\circ$ but in this case no evident
469 modification of the streamlines can be observed from midspan to the tip. Indeed, the suction
470 side is characterized by just a slight separation and the downwash effect is very similar to that
471 at $\vartheta=210^\circ$.

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473
474 **Figure 10 - Streamlines at different span lengths: $\vartheta=210^\circ$ and $\vartheta=300^\circ$.**
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476 In addition, Fig. 11 shows the streamlines at $\vartheta=315^\circ$ to see the effect of the turbulent
477 vortex on the flow field that was described in Fig. 8: it is apparent that a sudden distortion of
478 the incoming flow in the tip region takes place due to the blade-vortex interaction, with a
479 large variation of the incidence angle, which is now not related to the downwash effect.
480



481
482 **Figure 11 - Streamlines at different span lengths: $\vartheta=315^\circ$.**
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484 All aforementioned results can be more quantitatively described by directly evaluating
485 the pressure coefficient (C_p) distributions and the vorticity contours along the blade span.

486 Fig. 12 reports the pressure coefficient distributions at different span lengths for three
487 of the previously discussed critical angular positions: maximum load ($\vartheta=80^\circ$), inversion of
488 load between midspan and tip ($\vartheta=150^\circ$) and maximum load in the downwind half of the
489 rotation ($\vartheta=240^\circ$). The pressure coefficient used in this study is defined by Eq. (3), where p
490 denotes the static pressure at the airfoil surface. It has to be noted that, due to the complexity
491 of properly defining the actual relative wind speed on the airfoil, the pressure profiles were
492 here normalized by the theoretic relative wind speed (w_{th}) among a null induction factor (i.e.
493 the vectorial sum between the wind speed at infinity U_∞ and the peripheral speed ΩR).

494

$$C_p = \frac{p - p_\infty}{\frac{1}{2} \rho_\infty w_{th}^2} \quad (3)$$

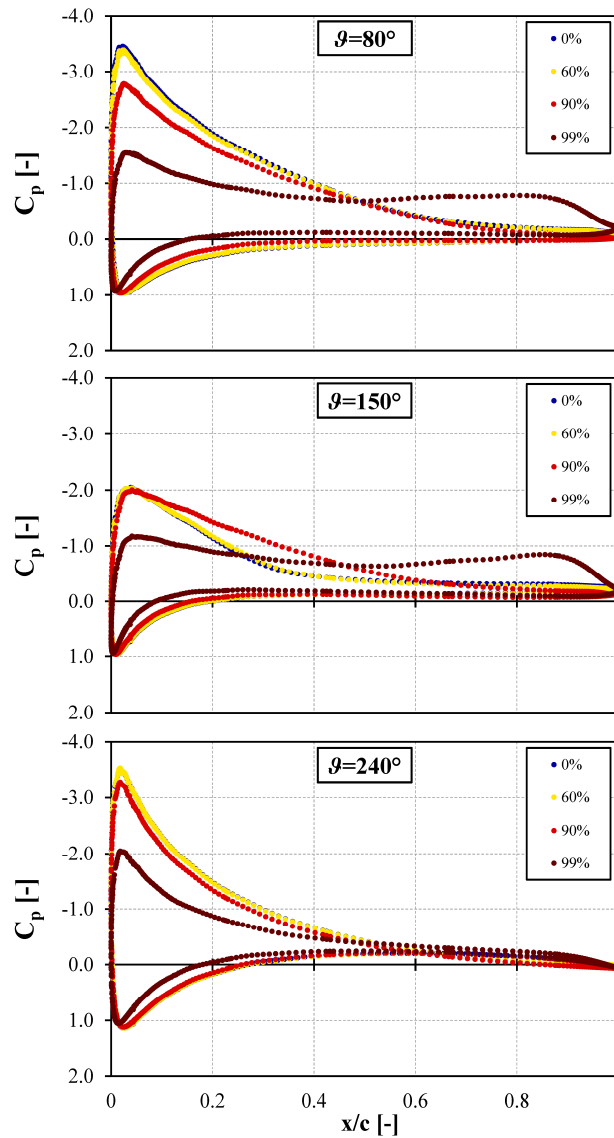


Figure 12 - Pressure coefficient profiles at different span lengths: $\vartheta=80^\circ$, $\vartheta=150^\circ$ and $\vartheta=240^\circ$.

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In conditions of high load (high AoA and high relative speed, like at $\vartheta=80^\circ$), it is confirmed that the 3D effects can reach up to 40% of the semispan from the tip, since moving from midspan to the tip, the C_p profile at 60% already shows a slight reduction of the load with respect to midspan. Close to the tip, the suction side of the blade is characterized by an almost constant pressure, indicating that this section does generate a very small lift force. As a result, a negative torque production is noticed at this location. When the AoA is high but the relative speed magnitude is lower ($\vartheta=240^\circ$), the 3D effects extend only to the last 20% of the semispan (i.e. from 80% semispan to tip): significant differences in the C_p distribution can be appreciated, however, only above 90% of the semispan. Close to the tip, only a reduction of the load can be observed, without any additional effect. The position $\vartheta=150^\circ$ is instead characterized by stalled flow, as shown by the large separation in the pressure distributions at 0% and 60%. In this case, the effect of the tip vortex is to slightly reduce the load, inducing the flow to remain attached to the blade, with a lower separation and even a small improvement of produced lift.

The evolution of the vorticity contours at different span locations along the blade is finally presented in Fig. 13.

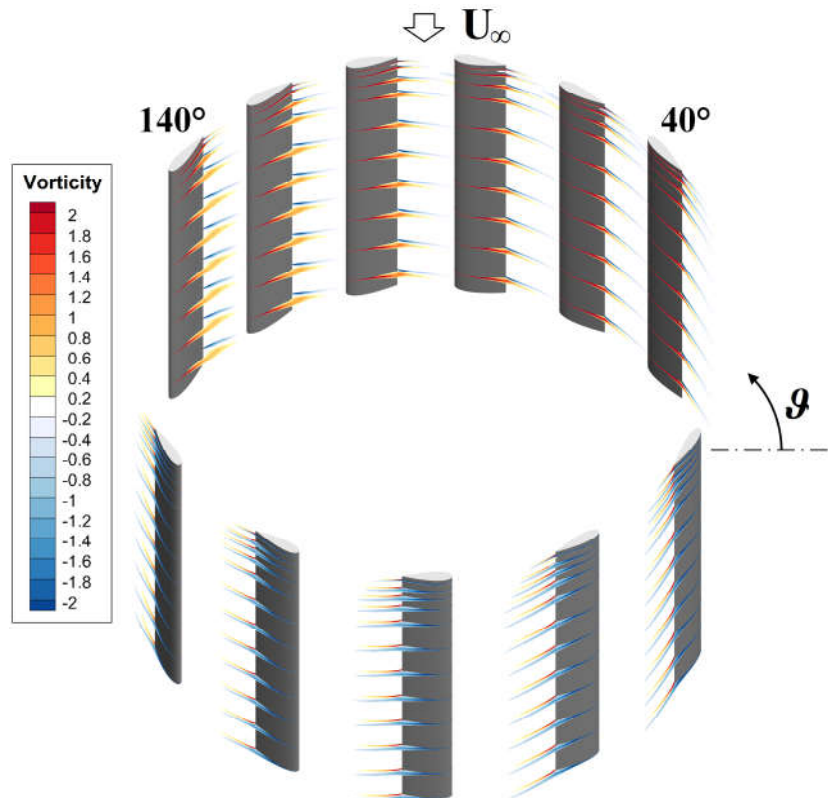


Figure 13 - Vorticity contours at different span lengths during the revolution.

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518 Focusing on the upwind half of the rotation, the two extreme positions of $\vartheta=40^\circ$ and
519 $\vartheta=140^\circ$, are in fact of particular interest. It can be noticed that, although (as expected) the
520 behavior in terms of torque distribution along the span is comparable, with analogous values
521 at each span location (see Fig. 3(a) and Fig. 3(b)), the 3D flow field in these two positions is
522 remarkably different. Indeed, for $\vartheta=40^\circ$ the vorticity contours are very similar, moving from
523 midspan to the tip, while for $\vartheta=140^\circ$ the large separation region due to stall is clearly present
524 along the largest portion of the blade but tends to disappear when getting close to the tip due
525 to tip effects. Moving to the downwind half of the revolution, it is apparent that the vorticity
526 contours are now very similar, moving from midspan to the tip, at all azimuthal positions.
527 This behavior is in agreement with previous analyses of streamlines and pressure coefficient
528 profiles.

529

530 5. Concluding remarks

531 In the study, a 3D, time accurate CFD study of a rotating Darrieus wind turbine blade
532 was carried out. Particular attention was given to the description of 3D flow effects and to the
533 modifications induced by them in comparison to the “infinite-wing” ideal case, which is the
534 one generally optimized by both 2D numerical calculations and low-fidelity simulation
535 models. The main outcomes of the analysis can be summarized as follows:

536 a) 3D effects do not modify the general trend of torque extraction over the revolution
537 (with the only exception of the near-tip region): indeed, maximum and minimum relative
538 values of torque take place at analogous blade azimuthal positions.

539 b) The impact of tip effects is strongly dependent on the azimuthal position of the
540 blade, according to oncoming relative speed in terms of incidence angle and modulus. In this
541 view, the accuracy of a low-fidelity model like the BEM theory could be remarkably
542 increased by re-correcting the aerodynamic coefficients for finite-wing effects at each
543 azimuthal position (or streamtube).

544 c) In the present study case, the average torque reduction due to finite-blade effects
545 corresponded to a reduction of the effective overall blade length of $1.5c$ ($0.75c$ for each half
546 blade).

547 d) A strong interaction between the tip-vortex released in the upwind portion of the
548 blade trajectory and the blade moving in the downwind region was noticed in correspondence
549 to an azimuthal position of $\vartheta=315^\circ$.

550 Future work will include the investigation of 3D flow effects at different tip-speed
551 ratios, particularly the lower ones, at which the impact of dynamic stall is thought to be more
552 relevant, and should also extend the analysis to a three-blade turbine, in order to describe in
553 detail all aspects of wake/blade interactions.

554

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566

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