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LARGE EDDY SIMULATION OF WIND FARM FLOWS: IMPROVED ACTUATOR DISK MODEL AND INVESTIGATIONS OF WAKE PHENOMENA

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Chapter 1

Introduction

1.1 Wind energy and its challenges

Wind energy is one of the fastest growing energy sources, with a worldwide installed capacity increase from less than 20 GW in 2000 to more than 500 GW in 2017 [1]. This market is currently dominated by Asia with 42.4% of the global market. Then comes Europe with 33%, North America with 19.5% and the rest of the world [1]. Unquestionably, in terms of wind power per capita, the European Union is the world leader. Nearly 170 GW are currently installed, producing in 2017 more than 350 TWh of electricity, which is more than 12% of all the electricity needs of the continent [1].

The development of offshore wind farms is particularly important. Indeed, the wind speeds over the ocean are typically much higher, and the wind resource is also more consistent than onshore. The implantation of offshore wind farms also meets less resistance from inhabitants than the installations of onshore wind turbines. In Belgium, for example, the current offshore capacity reaches 877 MW with 4 wind farms (C-Power (325 MW), Belwind (171 MW), Northwind (216 MW) and Nobelwind (65 MW)) and this will be increased up to 2 300 MW by 2020, through several short- and medium-term projects, i.e. Rentel, Mermaid, Seastar and Northwhester [2].

In offshore configurations, of a purpose of cost reduction, the rotors are often clustered in large wind farms; this leads to high power densities in the limited space of concessions. This also happens for onshore wind farms, but to a lesser extent. This grouping of wind turbines introduces two major issues, i.e. a reduction of power production per wind turbine and a diminution of the

lifetime of the rotor, coming from the interaction between the wind turbines and the wakes of the generators lying upstream. The wake is very inherent to the process of power extraction from wind, and consists in the flow region downstream of the wind turbine rotor, characterized by:

- a high level of turbulence,
- a lower wind speed,
- unsteadiness and large-scale oscillations (so-called “wake meandering”).

The wake dynamics is a very complex phenomena, that can be traced back to the vortex sheets, which are shed by the blades. These instantaneous flow structures are clearly visualized in Fig. 1.1, for a single wind turbine wake. The shed vortex sheets roll up into vortices at the tip and at the root of the blade, which are advected downstream and interact, break down and lose their coherence. This transition happens through hydrodynamic instabilities and perturbations by incoming turbulence. We can thus distinguish the near-wake, where vortices are still identifiable and coherent, and the far-wake, where the flow is fully turbulent [112].

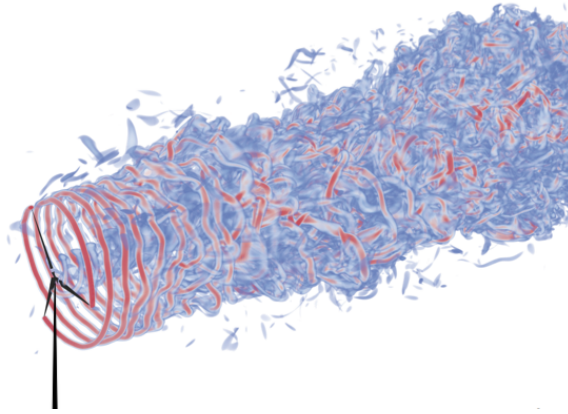


Figure 1.1 – Volume rendering of the vorticity magnitude in the wake [111]; these results are obtained with a Vortex Particle-Mesh method, coupled to immersed lifting lines [32].

When the rotors are clustered in a wind farm arrangement, the complexity increases, as shown in Fig. 1.2 for the wakes of the Horns Rev 1, an offshore wind farm with 80 wind turbines, located 14 km away from the west coast of Denmark [4]. There are non-linear wake-wake and wind turbine-wake interactions, causing the actual yield of a wind farm to be hard to predict. It can also

lead to higher-than-predicted failure rates for some machines and/or for specific seasonal wind conditions. In addition to these wake effects, uncertainties and intermittency due to the wind resource itself accentuate the issues arising from energy production by means of wind turbines. All these issues affect the attractiveness of wind energy and has a direct impact on the Levelized Cost of Energy.



Figure 1.2 – Aerial view from the Southeast of wake clouds at Horns Rev on February 12, 2008 (© Vattenfall, Horns Rev 1 owned by Vattenfall. Photographer Christian Steiness).

In order to reduce these negative effects, it is important to have tools which are able to accurately predict the power production, including its possible variations, and to give useful information for the design of future wind farms: it is here more a role of prediction. It is also relevant that these tools can be useful for global control of wind farms, a most recent and promising avenue that aims at maximizing the total farm production and mitigating problems related to the intermittency and the wake effects.

Several tools for prediction and estimation have been developed in academia and industry. Commercial wind farm design tools involve simple wake models [22, 59] and are known to suffer from large uncertainties in the prediction of energy output. They are often based on a time averaged approach, unable to capture the highly complex behavior of the wake. In parallel, more accurate tools have been developed in universities and research centers [31, 32, 37, 106, 125] and use Large Eddy Simulation (LES) combined to a wind turbine model. The LES approach can capture the unsteadiness of wind turbine wakes but a

high fidelity LES has the disadvantage of increasing the cost of simulations. The current challenge thus consists in finding a compromise between accuracy and efficiency.

The thesis aims at achieving this compromise with a numerical tool able to accurately capture the physics of rotors and wakes, the unsteady nature of a wind farm production while keeping a reasonable CPU cost to allow fast-time and near real-time studies. The first two features are important for the power prediction or the study of the wind farm response to a transient (change of the wind direction,...) while the last one is particularly relevant for control. Indeed, in the long term, the tool may be used for studying global farm control schemes as the existing tool efficiency and the trends in CPU performance clearly hint at real time computations in the near future. This would allow to couple the tool to real wind farms, and to pass predictions to controllers; such a high-fidelity information could be very valuable in the search for maximized wind farm power and alleviated loads on wind turbines. Moreover, in a short term, there is a definite need for an efficient tool which can give accurate predictions and which is able to model the largest wind farms even in unsteady conditions.

1.2 State of the art

As mentioned, several numerical tools able to model wind farms are available: they can be sorted according their level of fidelity.

1.2.1 Wake models

The low fidelity time-averaged models have the advantage to have short runtime and numerical wind farm studies have often relied on these computationally efficient approaches. Among these tools, we find the WAsP (Wind Atlas Analysis and Application Program) [100] using the Jensen or Park models, which account for the wakes as velocity deficit and expanding zone regions [71, 76] or more complex farm model, as FUGA [22, 122]; WindFarmer, a two-dimensional axisymmetric RANS solver, using the Ainslie model [13]; WindFarm [12], that also uses the Ainslie model; OpenWind [8], based on the Deep-Array Wake model (DAWM) [28] where the presence of the rotors are modeled by virtually increasing the local surface roughness [51]; FarmFlow [3], a three-dimensional RANS approach that uses the wake model called WakeFarm [103] and that is an intermediate version between simple models and refined Computational Fluid

Dynamic (CFD) approaches. Reviews of these are available in [22, 137, 59]. The wake models are often very crude and are known to suffer from large uncertainties in the prediction of the power production, explained in Section 1.1. Many research efforts have been made in order to address this short-coming, e.g. the Dynamic Wake Meandering model of [78] or [38], FLORIDyn [52]. These models still lack strong physical grounds, i.e. by assuming passive advection of the wake for the former or by lacking the transition from distinct tip vortices to far wake turbulence for the latter. More advanced approaches are thus required for the support in the elaboration of more accurate physics-based wake models; in that respect one can mention the efforts towards a vorticity-based model of [91, 90].

1.2.2 Computational fluid dynamics

The resort to more complex Computational fluid dynamics (CFD) approaches, coupled to a more or less advanced wind turbine representation, can help in capturing these unsteady effects and allows to obtain a more accurate wake behavior. At wind farm scales, Large Eddy Simulation (LES) is often used; indeed, it allows the representation of the inherent unsteadiness of wind farm flows with acceptable computational costs.

The full geometry of the blades is never represented because the resolution required to perform a true LES near the surface is currently computationally infeasible. Simpler methods are thus privileged. The two most widely used approaches are the Actuator Line (AL) and the Actuator Disk (AD). Both approaches involve a representation of the rotor effect in the flow through equivalent body forces.

The first concept is relatively recent and was introduced by Sorensen and Chen in [125]. It models the blades as rotating lines, with the aerodynamic forces computed based on the local velocities at the line position, blade geometry (chord, ...) and the two-dimensional aerodynamic data. They are often coupled to a LES approach, and are aimed at an accurate capture of the rotor behavior and its resulting near- and far-wake. This requires a fine mesh, though, with between 32 points and 64 points along the blade [73], and it constrains the time step, as the AL cannot travel more than one mesh cell over a time step. The Actuator Line method is thus most widely used for the simulations of one or several wind turbines (see [36, 68, 72, 123, 132] for several examples). In the same spirit, we find immersed Lifting Lines (LL) coupled to

a Vortex Particle-Mesh method [32, 33, 34] (see Fig. 1.1). This approach represents the blades as sources of vorticity while the vortex method is known to give very accurate results for turbulent flows. As the lifting lines and the shed vorticity are both handled by Lagrangian tracers [32], the VPM approach allows large time steps unconstrained by the blade tip speed, unlike an Actuator Line technique. The time step is rather adapted according to, among others, Lagrangian stability constraints [141]. The time step constraint is thus relaxed; but this technique still requires a fine resolution. The resulting instantaneous streamwise velocity for a discrete blade type approach is shown in Fig. 1.3(a).

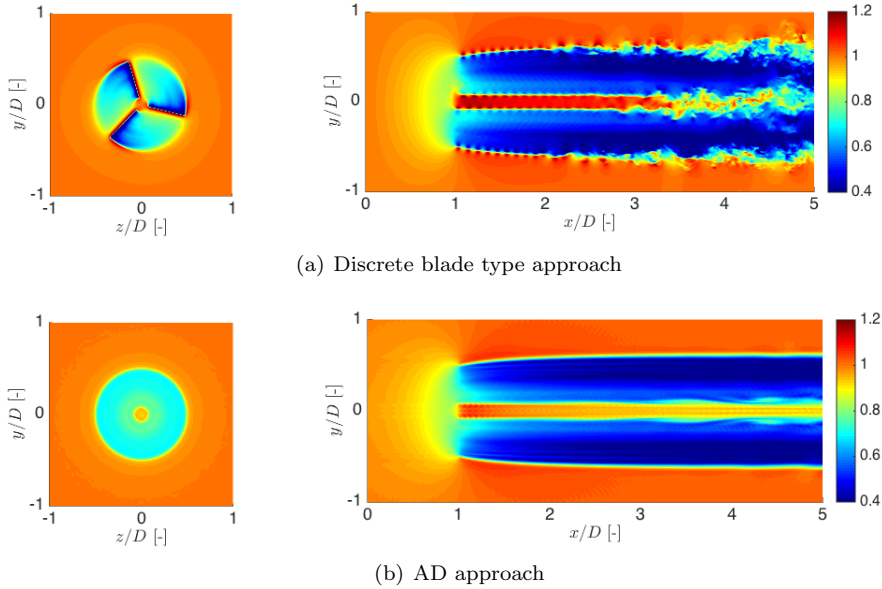


Figure 1.3 – Instantaneous streamwise velocity, normalized by the mean inflow, in the rotor plane (left) and in a vertical plane passing through the rotor hub (right) for two different wind turbine approaches: top, a discrete blade tip approach (here a Vortex Particle-Mesh method with LL) and bottom, an AD method. The results are obtained from simulations performed in Chapter 3, for verification purposes.

These models can produce very accurate results in terms of rotor modeling and wake behavior, but are unaffordable for production simulations at the wind farm scale. For example, Churchfield *et al.* [37] used AL approaches for simulating the Lillgrund wind farm, an operational wind farm comprising 48 wind turbines. This required one million processors-hours for simulating ten minutes of wind plant flow. These computational resources are unavailable in many institutions.

For wind farm simulations, another wind turbine modelization is thus privileged: the AD approach. Basically, the AD method consists in representing the blades as equivalent forces, distributed on a regularized disk swept by the rotor. Figure 1.3(b) highlights the differences resulting in the flow and in the wake development with the use of an AD method. The simplest version consists in representing the wind turbine as a drag disk, with a thrust production estimated based either on an unperturbed upstream velocity [74, 107, 144], or on velocities prevailing at the disk position [31, 95, 101, 126, 127, 145]. The distribution is generally uniform over the disk surface. More advanced AD models account for the rotating movement and imply non-uniform forces repartition across the disk surface. They also account for the flow at the disk position, which allows the rotor to react with the incoming flow fluctuations, and removes the delicate definition of unperturbed upstream velocity. Ivanell [67] used advanced AD methods for simulating two central lines of the Horns Rev wind farm; Martinez Tossas et al. [93] (with the tool called Simulator for Wind Farm Applications (SOWFA)) used AD approaches for performing simulations of the Lillgrund wind farm and compared the power production with that obtained in simulations with Actuator Lines approaches [37]; Wu and Porté-Agel [143] performed LES of the same operational wind farm as Ivanell [67] with rotating disks. In that case, all the 80 machines were represented, to study the downstream development and power production in aligned and staggered configurations. A review of several of these tools and works is available in [94]. Accounting for rotation and non-uniform repartition allows a more accurate rotor behavior but also better wake statistics [144].

The actuator disk appears to be the most appropriate approach to model large wind farms: indeed, it accepts coarse resolutions and it is not constrained by the tip speed of the blades, allowing larger time steps. However it is not the strategy that gives the most realistic rotor behavior. They only account for averaged forces effects and the flow experienced by a disk differs from that felt by actual individual blades, mainly at the blade tips and roots [118]. The tip region being responsible for most of the torque production, this may be critical in the resulting power. The disk approach is also known to suffer of weaknesses in the resulting wake: the wake expansion and decay are governed by vortex instabilities, which cannot be captured by such a basic approach and coarse mesh resolutions. This incorrect estimation has repercussions on the mixing process across the wake and on the rate at which the wake recovers momentum

from the ambient flow [83, 84, 115].

Still, in turbulent flows and wind farm flows, the AD model appears to produce acceptable wake statistics as close as five rotor diameters downstream of the wind turbine; as shown in several works [93, 106, 127, 144]. However, the comparisons between AD and AL methods in those works have often been carried out at comparable resolutions; this brings a bias in the assessment. At a coarse resolution, an AL method cannot hold significant advantage over the AD approach; at a fine resolution, it is unaffordable for wind farm simulations. Conversely, it is difficult to highlight what is the actual lack of accuracy in resulting wake statistics between a disk in coarse resolutions and a more accurate representation at finer resolution.

With this spectrum of methodologies in mind, it becomes apparent that today's challenge, in the race to reliable and affordable wind farm predictive tools, precisely consists in striking a compromise between high fidelity and computational efficiency.

1.3 Objectives

We propose to achieve high-fidelity levels through an efficient approach based on classical, structured mesh, Computational Fluid Dynamics. Based on what we learned about existing tools, we employ LES on coarse meshes together with the most appropriate approach for wind turbine modeling at wind farm scale: the actuator disk.

The used code is here an in-house developed fourth-order finite differences code [27, 53, 48], formulated in primitive variables, i.e. in velocity and pressure. It employs a cartesian structured grid, which allows fast computational time, contrary to some tools based on unstructured meshes [9]. It also allows an easier handling of the boundary conditions, contrary to some pseudo-spectral codes used in [95, 126, 143]. Thiry [131] recently improved the code with wall modelling procedures, allowing the simulations with ground effects and an accurate representation of the wind farm environment.

As our main objective is the large-scale and accurate simulation of wind farms in unsteady scenarios, we select an advanced version of the AD for the wind turbine representation [92, 143]. This specific variant involves the discretization of the disk into a certain number of elements where the normal and tangential aerodynamic forces are evaluated from the local disk velocities. The

rotor will also be improved with the addition of control schemes, in order to reproduce realistic responses of the rotor through wind fluctuations, upstream wake oscillations and potential grid events.

However, as discussed above, the AD is not the strategy that gives the most realistic behavior in terms of rotor aerodynamics. As we want to achieve high fidelity despite the coarse resolutions, we will improve the disk approach with a tip-loss correction, typically used in classical Blade Element Momentum method [124]. This correction and its alternative versions [26, 116] are well adapted for the BEM implementation and often applied on the “induction factors” that are naturally available in this theory. This implies an estimation of an unperturbed velocity, which becomes problematic to define when the rotor is impacted by turbulent winds, wind shear or wakes of upstream machines. The tip-loss corrections are thus often neglected in wind farm simulations. We here want to enable the use of tip loss corrections on an AD model aimed at LES of wind farms.

We will verify our disk approach at typical coarse resolutions used in wind farm simulations with results obtained with a higher-fidelity approach. We choose here a Vortex Particle-Mesh method, coupled to immersed lifting-lines. This indeed accounts for a more accurate representation of the rotor and near wake development, and it presents little dissipation and dispersion errors, which makes it very well suited for investigating the rotor production and the wake statistics. This will allow to highlight any impact of the lack of accuracy in the AD in terms of rotor behavior and wake statistics.

As our final objective is the simulations of very large wind farms, we will also simulate wind farms flows in order to (i) validate the tool against available observations, (ii) investigate wake statistics and their impact on the downstream machine, (iii) highlight the performances of the tool in terms of computational costs.

1.4 Outline

This work is concerned with the implementation and assessment of an Actuator Disk model in a fourth-order finite differences code, aiming at accurate LES of wind farm flows. The thesis is structured as follows:

Chapter 2

This chapter briefly describes the different tools and techniques used in this work. The fourth-order finite difference flow solver in which the Actuator Disk is implemented is first presented. The AD model then thus described. The different techniques for turbulent inflow generation that are added and used in the code are briefly addressed; those vary according the objective of the considered simulation. Finally, a Vortex Particle-Mesh method, coupled to immersed Lifting Lines [32, 33, 34] is briefly described: this last tool will be used to verify and compare the AD method in terms of aerodynamic data and wake statistics.

Chapter 3

The implemented AD method is here verified for simulations at coarse resolutions, in terms of disk-level velocities and aerodynamic loads. In order to have the most accurate response of the AD (despite its coarse resolution), a tip-loss correction is added, here the Glauert tip-loss factor, commonly used in the Blade Element Momentum method. As the implementation of this correction requires a reference upstream velocity, a methodology is here proposed to infer this effective upstream velocity locally to each disk element, based on the one-dimensional Momentum (M1D) theory and using only the local data at the rotor. This estimation is first verified through a set of simulations with drag-only disks, on which a constant loading is imposed. The AD supplemented with the tip-loss correction is compared to the VPM method, through the simulations in uniform wind of a constant circulation wind turbine and of a more realistic machine. *This chapter is a slightly modified version of a paper entitled “An actuator disk method with tip-loss correction based on local effective upstream velocities”, published in Wind Energy [98]. A part of these results was also presented at the 68th Annual Meeting of the American Society, Division of Fluid Dynamics (APS-DFD68).*

Chapter 4

The AD method supplemented with its additional tip-loss correction is then verified for turbulent inflows; again, only the data at the disk level are investigated. The estimation of this effective upstream velocity, required for the tip-loss correction, is first verified. The power and thrust produced by the AD

model are then compared to those produced by the VPM approach. Finally, the root bending moments are investigated, to assess if finer information relevant to fatigue loads can be recovered from a simple disk approach at a coarse resolution. *A part of these results was presented at the 68th Annual Meeting of the American Society, Division of Fluid Dynamics (APS-DFD68).*

Chapter 5

In this chapter, the AD method at a coarse resolution is verified in terms of wake statistics for a large panel of operational cases. As the AD model is aimed at wind farm simulations, it is important that it produces realistic velocity deficits and levels of turbulence where another machine can potentially be located. We here compare the results to those obtained with the VPM method.

Chapter 6

A first wind farm simulation is here performed, implying a transient scenario in a small wind farm with 15 machines. We here consider an emergency stop for one rotor. Control schemes are implemented, in order for the each rotor to accurately respond to the incoming flow, as well as to realistic emergency shut-down procedure. The wakes are not investigated in details, but this scenario will highlight the physics and the transient in the redistribution of power production within a farm subjected to such an incident. *This chapter is a slightly modified version of a paper entitled “LES of wind farm response to transient scenarios using a high fidelity actuator disk model”, published in a special issue of the Journal of Physics: Conference series [97], containing the Proceedings of The Science of Making Torque from Wind conference (Torque 2016).*

Chapter 7

This last chapter is dedicated to the simulation of an operational large offshore wind farm, the Horns Rev wind farm (Denmark), for which measurements and observations are available and treated in many works. The objectives are multiple: (i) verify and validate the code by comparing the power production with the observations, (ii) investigate wake statistics and their impact on the production and loads on the downstream machines, and, (iii), study the performances in terms of computational costs for the simulations of large wind farms. Several wind sectors around one main direction are investigated; the final objective consists in running several simulations to reach a wind sector

that covers 30° ; but a smaller wind sector of about 20° is presented in this chapter. *This chapter is to be submitted as a paper, including the simulations performed over the whole wind sector (30°).*

Chapter 8

In this final chapter, we collect our conclusions and draw some perspectives for future works in terms of the uses of the developed method and its potential extensions.

Chapter 2

Methodology

This chapter briefly describes the different tools and techniques used in this work. The first section presents the fourth-order finite differences flow solver in which the Actuator Disk model has been developed; the wall modelling procedure, as well as the subgrid-scale models are also briefly presented. The Actuator Disk method is described in the second section. The turbulent inflow generation is addressed in the third section. In the last section, another simulation code is presented; this code will be used for verifying the disk approach in terms of rotor production and resulting wakes.

2.1 Flow solver

We consider the Large Eddy Simulation of incompressible flows. It is performed using an in-house developed fourth-order finite differences code [46, 47], formulated in primitive variables (i.e. in velocity and pressure) and using a staggered mesh arrangement. This code is denoted “FD4” code in this work. Using a Cartesian coordinate system, the Navier-Stokes equations (supplemented by a subgrid scale (SGS) model) are formulated as

$$\frac{\partial u_i}{\partial x_i} = 0, \quad (2.1)$$

$$\frac{\partial u_i}{\partial t} + u_j \frac{\partial u_i}{\partial x_j} = -\frac{1}{\rho} \frac{\partial p}{\partial x_i} + \frac{\partial \tau_{ij}}{\partial x_j} + \frac{\partial \tau_{ij}^{sgs}}{\partial x_j} + \frac{f_i}{\rho}, \quad (2.2)$$

where t is the time, $i, j \in (1, 2, 3)$ correspond to the three directions, (x , y and z) and the three velocity components (u , v and w); p is the pressure and f_i is the external force (per unit volume); ρ is the fluid density; τ_{ij} is the viscous stress tensor, written as (in the case of a Newtonian fluid):

$$\tau_{ij} = 2\nu S_{ij} \quad (2.3)$$

where ν is the molecular viscosity and S_{ij} is the strain rate or deformation tensor:

$$S_{ij} = \frac{1}{2} \left(\frac{\partial u_i}{\partial x_j} + \frac{\partial u_j}{\partial x_i} \right) \quad (2.4)$$

For incompressible flow, $\frac{\partial \tau_{ij}}{\partial x_j} = \nu \frac{\partial^2 u_i}{\partial x_j \partial x_j}$. τ_{ij}^{sgs} is the SGS stress tensor model (detailed in Section 2.1.1). For this work, x , y and z are respectively the streamwise, vertical and spanwise directions.

The flow solver has been initially developed by Georges [53] and Bricteux [27], and then rewritten by Duponcheel [48]. It was originally designed to preserve the discrete kinetic energy exactly. The equations are thus discretized in space using the fourth-order finite differences scheme developed by Vasilyev [136]. Thiry [131] further developed the code with the addition of wall modeling strategies. For this purpose, the convective term has been rewritten (see [131] for details), which allows an easy implementation of wall shear stress boundary conditions. This version, called here the “flux version”, thus preserves the discrete kinetic energy up to fourth order (in the absence of wall modeling procedure, see [131] for details) and it conserves exactly the momentum on uniform grids. In this work, both versions will be used; when wall modelling is not required, the first version, denoted “energy-conserving version”, is used.

The unsteady Navier-Stokes equations for incompressible flows are solved using a fractional-step method [80] which consists in a projection of a predictor velocity field, obtained by a classical time integration, onto a divergence-free space. The projection is obtained by solving a Poisson equation for the pressure increment. When the energy-conserving version is used, both convective and diffusive terms of the equations are integrated using an explicit second-order Adams-Bashforth. For the flux version, the integration is carried using an second-order Adams-Bashfort scheme for the convective term, while an explicit Euler scheme is used for the viscous terms. The Poisson equation is solved using a multigrid solver (details about the procedure can be found in Duponcheel [48])

and Georges [53]).

2.1.1 Subgrid-scale modeling

Different Sub-Grid Scale models are available in the code (see Duponcheel [48]). In this work, two main models are used: a classical Smagorinsky model [120] and a Regularized Variational Multiscale (RVM) variant of this model [69, 142]. The Smagorinsky model reads:

$$\tau_{ij}^{sgs} = 2\nu^{sgs} S_{ij} \text{ with } \nu^{sgs} = C\Delta^2 (2S_{ij}S_{ij})^{1/2} \quad (2.5)$$

where S_{ij} is the strain rate tensor defined in Eq (2.4); C is the model coefficient. For a general non-uniform mesh, Δ is defined as $(\Delta x \Delta y \Delta z)^{1/3}$, where Δx , Δy and Δz are the local grid spacings in each direction.

The principle of a RVM model consists in decomposing the LES field into large scales and resolved small scales:

$$u_i = \bar{u}_i + u_i^s \quad (2.6)$$

and the SGS viscosity computation is based on the small scales $\nu^{sgs}(\mathbf{u}^s)$. The small scales are obtained using an iterated discrete 3-point filter (details can be found in [70]). We here use the “small-small” version (noted RVMs), implying:

$$\tau_{ij}^{sgs} = 2\nu^{sgs}(\mathbf{u}^s) S_{ij}^s \quad (2.7)$$

where S_{ij}^s is the strain rate tensor of the small scales. The RVMs-Smagorinsky model thus reads:

$$\tau_{ij}^{sgs} = 2C\Delta^2 (2S_{mn}^s S_{mn}^s)^{\frac{1}{2}} S_{ij}^s. \quad (2.8)$$

The use of the RVM model is motivated by its negligible dissipation in a purely rotational flow [39] and its small-small variant additionally offers an improved spectral behavior [40]. The RVMs-Smagorinsky, however, was shown to have spurious interactions with wall models (see Thiry [131]) and the first simulations with rough wall modeling performed in this work led to similar observations. The classical Smagorinsky model is thus selected when wind farm simulations are considered.

2.1.2 Wall modeling

We here consider simulations at very high Reynolds number (Re), for which wall resolved LES are simply not affordable for the current computational resources. For wall-bounded flows, in the vicinity of the wall, the larger eddies are still locally very small; their size indeed scales with the distance to the wall. As the Re increases, those energy-carrying eddies become smaller and require a finer mesh to resolve them all the way to the wall; the cost reduction that LES allows thus becomes marginal [131]. The objective of wall modeling is to provide a model for the inner layer, to remove the need of resolving this layer and to alleviate the mesh requirement linked to capturing the eddies it contains. The principle thus consists in using coarser meshes, and in imposing a surface shear stress at the ground boundary. This wall stress is directly determined from the resolved velocity field at the first grid points by assuming a local equilibrium law. Thiry [131] has implemented and investigated different wall modeling strategies in the FD4 code. We here use a wall stress model for a rough wall, as it is the classical law of the wall used for simulations of neutral Atmospheric Boundary Layers and wind farm simulations [143]. This law reads:

$$|\langle \mathbf{u} \rangle| = \frac{u_\tau}{\kappa} \log\left(\frac{y}{y_0}\right) \quad (2.9)$$

where $|\langle \mathbf{u} \rangle|$ is the norm of the averaged or locally-averaged horizontal velocity field at height y , κ is the *von Kàrmàn* constant ($\kappa = 0.38$ [102]) and u_τ is the friction velocity. y_0 is the roughness length: a length-scale representation of the roughness of the surface. $\langle . \rangle$ denotes global or local spatial averaging performed in the plane parallel to the wall. Global averaging is considered along the homogeneous directions of the problem, while local averaging is performed over non-homogeneous directions. For example, for simulations with wind turbines, there is no homogeneous directions, and local averages will thus be privileged in that case. Once a mean friction velocity is obtained, the local shear stress τ_w is computed as [131]:

$$\tau_w = \rho \frac{\mathbf{u}|\mathbf{u}|}{|\langle \mathbf{u}|\mathbf{u} \rangle|} u_\tau^2 \quad (2.10)$$

The velocity information $\mathbf{u}$ is taken at the third grid point above the wall, $y = y_3$. Thiry [131] showed that the first few points adapt to the wall shear stress model and to the coarse resolution, thus making the use of one of the first

two points unsuitable for the estimation of the wall shear stress. As a staggered arrangement is considered, the horizontal velocities are located at the center of the faces whose normal is aligned with the respective velocity component; y_3 is therefore equal to $2.5 \Delta y$, where Δy is the cell size in the vertical direction. In this work, all simulations use a uniform grid from the ground and up to the top of the wind turbine; Δy is thus constant in the interest zone of the wall modeling procedure.

This wall modeling will be used when simulations of wind farms are performed, with the use of a rough ground.

2.2 Generation of the atmospheric turbulence

Inflows with turbulent fluctuations will be implemented in several fashions in this work. These are described in the next two subsections.

2.2.1 Mann's algorithm

This method consists in imposing a pre-generated turbulent field over a mean inflow velocity. In that case, the introduced turbulence is generated by the so-called Mann's algorithm [88, 89], widely used in the wind energy field. This method consists in generating all the three velocity components of a three-dimensional incompressible turbulence field, that has the particularity to be homogeneous, stationary, Gaussian, anisotropic and to have the same second order statistics as atmospheric turbulence. The algorithm models the *spectral tensor* characterising the neutral atmospheric surface layer turbulence, using rapid distortion theory; this implies a linearization of the Navier-Stokes equations, combined with an assumption of linear shear velocity and a model for eddy lifetime.

The output of Mann's method is a spatial box characterized by a uniform grid, with turbulent fluctuations characteristic of a certain height. In our case, the x-axis is in the direction of the mean wind speed and is inferred as a time axis by invoking Taylor's frozen turbulence hypothesis. This assumes that the advection of the velocity fluctuations can be taken to be entirely due to the mean inflow if these fluctuations remain small. Then the substitution $t = x/U$ where U is the mean inflow velocity, can be seen as a good estimation.

However, as soon as the fluctuation field is interpolated at the inlet of the simulation domain and handled by the numerical code, it gets distorted and

there is a redistribution between the three velocity components. The initial fluctuations are thus not perfectly conserved and evolve before impacting the wind turbine (that is located at several rotor diameters from the inlet).

In [55], it is mentioned that the Mann’s algorithm does not ensure a divergence-free field. Once the velocity fluctuations are imposed at the inlet of the simulation domain, the pressure Poisson solver alters the velocity field to ensure a zero divergence. This also contributes to an evolution of the initial fluctuations.

This technique is used when one machine is considered. Indeed, these pre-computed fluctuations have the characteristics of those at a fixed height and for a given mean inflow velocity. This is thus not the most appropriate technique when a shear flow is considered, as in wind farm simulations. If only one box is generated and added to an imposed shear inflow, the fluctuations will be strongly distorted above and below the reference height. An adaptation length is thus required for the structures to develop inside the domain. This length appears to be important for developing the larger scale structures, as demonstrated in [101], where the evolution of the velocity fluctuations has been investigated for different techniques of turbulent inflow generation. The computational domain thus sensibly increases, as well as the computational cost. As a consequence, another technique is used for the wind farm cases to ensure appropriate turbulent fluctuations.

2.2.2 Precursor and concurrent simulations

Precursor approaches rely on an auxiliary wind flow simulation, performed on an independent domain, for generating turbulent inflow conditions. Here, the precursor simulates a canonical flow case, a half-channel flow driven by an imposed pressure gradient, where periodic conditions are invoked in the horizontal directions. This allows a relatively rapid generation of turbulent structures, as they are recycled through the boundary conditions, and the flow reaches statistically steady conditions after a number of flow-through times.

The main disadvantage of precursor approaches is the increase in computational cost associated to this auxiliary simulation. However, as the turbulent inflow conditions are directly computed by the Navier-Stokes equations (of course, here also with SGS modeling and rough wall modeling), this sensibly improves the accuracy over other approaches, as shown in earlier comparative studies [129]. The use of precursor techniques has increased in wind energy in recent years [37, 101, 143], and is strongly recommended for wind farm

simulations, in order to obtain faithful flow structures impacting the wind turbines [105].

In the present work, the precursor is performed on a domain size equal to that of the main simulation, which comprises the wind turbines. Periodic conditions are enforced in the horizontal directions, while the wall model is used at the bottom and a no-through flow is imposed at the top. The flow is driven by the imposed pressure gradient, determined so as to reach the desired hub velocity and level of turbulence (detailed in Chapter 6). Once the flow reaches a statistically-steady state, either yz -planes of instantaneous velocities are saved to generate an independent data base of inflow data, or this auxiliary simulation runs concurrently with the main simulation, which allows inflow conditions to be directly transferred to the inlet of the main domain. The first technique avoids the need of two concurrent simulations that roughly doubles the computational cost, but it requires a large amount of storage. In the present work, we use both techniques, according to the considered domain size and simulation time.

In the present thesis, thermal and stratification effects are not included; the resulting simulations are thus equivalent to neutral boundary layer (NBL) cases. The NBL is the simplest case of an ABL, with no heat flux at the surface and a potential temperature constant with height. Vertical motions are neither enhanced nor suppressed and buoyancy forces are completely absent. This thermal stratification appears with very strong wind conditions, or during a short transition period after the sunset. Over the sea, this appears more often as the surface heat flux tends to be smaller. As stratification effects are neglected, the height of the ABL is constrained by the vertical length of the domain, as in [101, 143]; and not by a capping inversion, as it could be observed in some numerical simulations [14, 37].

Coriolis forces are also neglected, and so, the wind-direction variation along the height (“wind-direction veer”) is not represented here.

2.3 Wind turbine modeling

The wind turbine is added in the code through the addition of body forces, f_i , in Eq. (2.2). The wind turbine model is described below.

2.3.1 Actuator Disk approach

The geometry of the blades is not fully represented, and the effect of the rotor is accounted for through a disk of equivalent forces, i.e. an Actuator Disk approach. The concept of an AD consists in considering an average effect of the N_b blades over the surface swept by the blades through one rotation, i.e. a disk. In our methodology, the AD is discretized into a certain number of polar/cylindrical elements (see Fig 2.1): at each time step, at a certain radial location r , each azimuthal position bears the effects of N_b blades, thus weighted by a ratio between the surface element and the total annular surface at the given r . The effect of the blades is thus felt on the entire disk surface (we recall that this effect can be non uniform over the disk area, as the local flow velocities are used for computing the forces).

As in several AD methodologies, our approach implies exchanging information between the rotor mesh, which is polar and the LES mesh (see Fig. 2.1). The radial and azimuthal resolutions of the polar mesh are fixed such that the largest polar cells are roughly the size of a cartesian cell. The two meshes give flexibility in the placement of the disk in the computational domain and in its orientation around the y -axis, i.e. for the yaw degree of freedom. They exchange information using high-order interpolation kernels typically used in particle-mesh methods: we here rely on the third-order accurate M'_4 scheme [99] to interpolate the velocities from the LES grid to the polar grid.

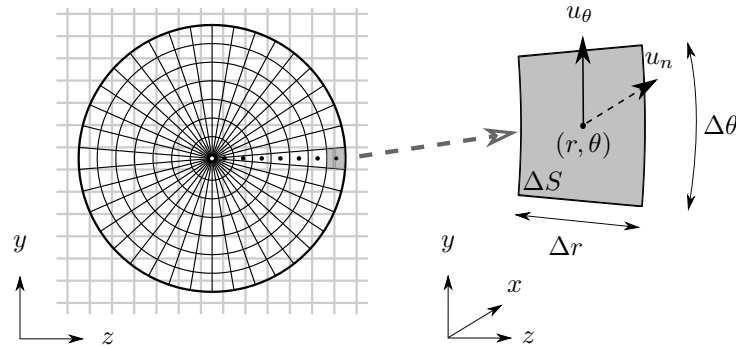


Figure 2.1 – Cartesian and polar meshes used for AD methodology and polar cell in the disk

The disk is thus discretized into a certain number of elements in both radial and azimuthal direction (see Fig 2.1), as in the methodology of [93, 143]. On each small disk surface, we thus account for the N_b blades aerodynamic forces.

Those are determined using the Blade Element theory [124]. This consists in splitting the blade into several small parts of length Δr then determining the forces on each of these small blade elements. At each blade section, the aerodynamic forces acting on the rotor are governed by the local velocities, i.e. the normal and tangential velocities to the rotor plane, u_n and u_θ respectively, and determined with the use of the 2-D airfoil characteristics, i.e. the lift and drag coefficients, C_l and C_d , the chord length, c , the blade twist, β , and the possible pitch angle of the blade, γ . Figure 2.2 presents the velocities and forces at a location r on the blade: u_θ is the velocity induced by the wake in the tangential direction, while u_n represents the difference between the upstream velocity and the velocity induced by the wake in the normal direction.

The lift and drag aerodynamic forces on one blade section of length Δr are thus computed as:

$$(L, D) = 0.5\rho V_{rel}^2 c\Delta r (C_l, C_d) . \quad (2.11)$$

V_{rel} is the relative velocity, defined as

$$V_{rel} = \sqrt{u_n^2 + (\Omega r - u_\theta)^2} \quad (2.12)$$

with Ω the rotor speed. (C_l, C_d) are obtained from the effective angle of attack α and tabulated 2D airfoil data; α is computed from the difference between the flow angle, $\phi = \arctan\left(\frac{u_n}{\Omega r - u_\theta}\right)$, and the sum of the twist and pitch angles, β and γ .

L and D can be expressed in the normal and tangential directions of the disk plane (see Fig. 2.2),

$$F_N = L \cos \phi + D \sin \phi \quad \text{and} \quad F_\theta = L \sin \phi - D \cos \phi . \quad (2.13)$$

To obtain the loads on ΔS , F_N and F_θ are multiplied by the number of blades N_b and by a weight that corresponds to the ratio between the surface element and the total annular surface at the given radius r ,

$$F_{N,AD} = F_N N_b \frac{\Delta S}{2\pi r \Delta r}, \quad (2.14)$$

$$F_{\theta,AD} = F_\theta N_b \frac{\Delta S}{2\pi r \Delta r}. \quad (2.15)$$

These forces are finally translated into bulk forces for the LES equations

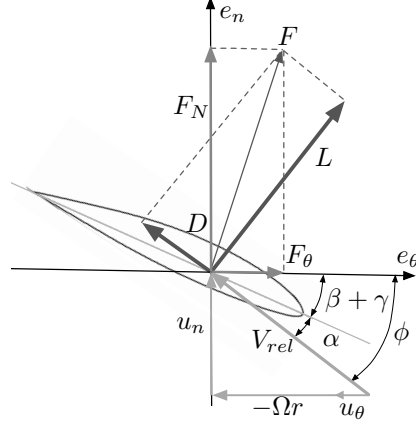


Figure 2.2 – Schematics of a blade airfoil; definition of the vector quantities and notations

(f_i in Eq. 2.2) by using a second-order mollifier, the M_4 kernel [99]; this ensures a non-oscillatory projection of these forces onto the mesh. The use of the distribution scheme avoids the computation of exponential terms, unlike a Gaussian distribution [93, 143]. However, This distribution procedure does not give flexibility in the mollification size. Note that the present work does not investigate the effect of mollification.

By summing the force elements, diagnostics can be computed, such as the thrust, T , the torque, Q , and the power, $P = \Omega Q$. This version of the AD does not account for a tip-loss correction and we will refer to it as AD-NC (for not corrected). This model constitutes the most general approach: it will be modified for studies where we consider only constant normal loading or constant circulation (in Chapter 3).

2.3.2 Additional features

Tip-loss correction Compared to a classical Blade Element Momentum (BEM) theory [57], the implemented AD approach provides more physics to the rotor model and gets rid of some strong assumptions: it naturally accounts for wake expansion and yaw misalignment, the wake is explicitly resolved by a Navier-Stokes solver, which couples the responses of the disk elements, and the disk can react to unsteady flows, with a resulting non-uniform forces distribution. However, the flow experienced by a disk still differs from that felt

by actual individual blades: a disk implies that each azimuthal position bears the effect of the blades, and the wake just behind an AD is a continuous vortex sheet. The contribution of the wake to the induced velocities at the rotor level thus differs from that due to a finite number of tip and root vortices as obtained with an AL or a LL approach (see Fig. 1.1 and 1.3). The flow velocities, and thus the aerodynamic forces, vary according to the rotor representation, and a disk approach often overestimates the power production of the machine, compared to discrete blade type approach. One solution consists in adding a tip-loss correction [96, 118], commonly used for the BEM method. This tip-loss factor aims at correcting the streamwise and tangential velocities induced by the wake at a certain azimuthal position on the disk, in order to recover an induction that could be felt by a discrete blade at the same location. This is discussed in Chapter 3.

Wind turbines and control schemes Most of the computations presented in this thesis were conducted using airfoil data and geometry from “NREL offshore 5-MW baseline wind turbine” [75]. This model was created from broad design information from the published documents of turbine manufacturers for different existing rotors. This is a variable speed machine, with a rotor diameter of 126 m and a hub height of 90 m. A simple version of the Vestas v80 wind turbine has also been implemented for verification purposes and is described in Chapter 7. The two wind turbine models are here improved with realistic control schemes. Those are mainly required when wind farm simulations are performed, and they are described in Chapters 6 and 7.

2.4 The Vortex-Particle Mesh method

In order to verify the AD approach in terms of aerodynamics and wake statistics, the AD simulations are compared to higher fidelity calculations of a discrete blade rotor. The computations are based on a Vortex Particle-Mesh method coupled to immersed lifting lines for the LES of wind turbine wakes [32, 33, 34, 49]. It builds upon the Vortex-in-Cell [43] technique, which combines both particles and a mesh: the particles are used solely for the advection of vorticity while the mesh handles all the other computationally intensive tasks. The method thus exhibits little dispersion and dissipation, and achieves high accuracy in computations of turbulent flows such as a wind turbine wake. The

method is briefly described in the next subsections.

2.4.1 The vortex particle mesh method

The three-dimensional incompressible flows and the filtered Navier-Stokes equations in their velocity-vorticity form are considered here, $\mathbf{u}$ and $\boldsymbol{\omega} = \nabla \times \mathbf{u}$:

$$\frac{D\boldsymbol{\omega}}{Dt} = (\boldsymbol{\omega} \cdot \nabla) \mathbf{u} + \nu \nabla^2 \boldsymbol{\omega} + \nabla \cdot \mathbf{T}^M \quad (2.16)$$

$$\nabla \cdot \mathbf{u} = 0 \quad (2.17)$$

where $\frac{D}{Dt} = \frac{\partial}{\partial t} + \mathbf{u} \cdot \nabla$ denotes the Lagrangian derivative, ν is the kinematic viscosity and $\mathbf{T}^M$ is the Sub-Grid Scale (SGS) model, here a Regularized Variational Multiscale (RVM) type model developed in vorticity form [41]. A Helmholtz decomposition of the velocity field $\mathbf{u} = \mathbf{U}_\infty + \mathbf{u}_\omega$ allows to recover it from the vorticity through the Poisson equation

$$\nabla^2 \mathbf{u}_\omega = -\nabla \times \boldsymbol{\omega} . \quad (2.18)$$

A Vortex Particle (VP) method discretizes the vorticity field by means of particles, characterized by a position $\mathbf{x}_p$, a volume V_p and a strength $\boldsymbol{\alpha}_p = \int_{V_p} \boldsymbol{\omega} d\mathbf{x}$. Particles are convected by the flow field, thereby waiving classical advection stability constraints and also reducing dispersion errors (there are still some, due to the remeshing of particle discussed later). The strength of the particle is modified to account for vortex stretching, diffusion and also subgrid-scale modelling. One can integrate Eq. (2.16) over a particle volume through a mid-point quadrature with the assumption of the particle discretization (see [43, 141] for a detailed analysis of particle discretizations). One then finds a set of Ordinary Differential Equations for the particle positions and strengths

$$\frac{d\mathbf{x}_p}{dt} = \mathbf{u}(\mathbf{x}_p) , \quad (2.19)$$

$$\frac{d\boldsymbol{\alpha}_p}{dt} = ((\boldsymbol{\omega} \cdot \nabla) \mathbf{u} + \nu \nabla^2 \boldsymbol{\omega} + \nabla \cdot \mathbf{T}^M)_p V_p , \quad (2.20)$$

where we identify the role of the velocity field in the advection, and of the vortex stretching, and diffusion and SGS terms for the evolution of vorticity.

The present method combines the use of particles with a mesh. This allows the treatment of the Lagrangian distortion through a periodic regularization of

the particle set onto the mesh. The presence of the mesh is also exploited to improve computational efficiency. Once the particle quantities have been interpolated onto the mesh, standard Finite Differences can be used to evaluate the differential operators on the RHS of Eq. (2.18) and the Poisson equation can be solved using a grid-based solver. The results are then interpolated back at the particle locations. The remeshing operation and the communication between particles and mesh use high order interpolation kernels, here M'_4 [99]. The grid-based solver in this work is a Fourier-based solver, which handles a combination of periodic and unbounded directions through a Hockey-Eastwood [64] approach. The solver has been extended to handle the unbounded transversal directions together with the inflow, $u_n = U_\infty$, $\partial u_t / \partial n = 0$ and outflow $u_t = 0$, $\partial u_n / \partial n = 0$, conditions in the wind direction. The time integration of the ODEs for the positions and strengths is carried out by means of a third order low-storage Runge-Kutta scheme.

2.4.2 Immersed lifting lines

The generation of the vorticity along the blades is accounted for through a lifting line model. Under the assumption of slender blades, the blades can be approximated as a concentrated bound vortex going through the quarter-chord line, and the airfoil in a section is then treated as essentially two-dimensional. Under the assumption of steady flow around the airfoil, the lift, $\mathbf{L}$, the relative velocity, $\mathbf{V}_{\text{rel}}$ and the circulation, $\mathbf{\Gamma}$ can be related through the Kutta-Joukowski theorem [58]

$$\mathbf{L} = \rho \mathbf{V}_{\text{rel}} \times \mathbf{\Gamma} , \quad (2.21)$$

where we note that $\mathbf{L}$, $\mathbf{V}_{\text{rel}}$, and $\mathbf{\Gamma}$ are vector quantities. $\mathbf{\Gamma}$ corresponds to the circulation multiplying by the vector tangent to the lifting line $\hat{\mathbf{e}}_{\mathbf{\Gamma}}$. The expression for lift can then be compared to the one obtained from aerodynamic information:

$$L_{2D} = \frac{1}{2} \rho |\mathbf{V}_{\text{rel}} - (\mathbf{V}_{\text{rel}} \cdot \hat{\mathbf{e}}_{\mathbf{\Gamma}}) \hat{\mathbf{e}}_{\mathbf{\Gamma}}|^2 c C_L(\alpha) \quad (2.22)$$

where c is the chord of the local airfoil. This expression has to agree with the norm of Eq. (2.21), thus leading to

$$\Gamma = \frac{1}{2} |\mathbf{V}_{\text{rel}} - (\mathbf{V}_{\text{rel}} \cdot \hat{\mathbf{e}}_{\mathbf{\Gamma}}) \hat{\mathbf{e}}_{\mathbf{\Gamma}}| c C_L(\alpha) . \quad (2.23)$$

V_{rel} is interpolated from the velocity field onto the blade.

One limitation of the approach is the lack of any shed structure due to the drag. This however leads to a minor impact, as high Reynolds number are considered, and the induced velocities at the blades together with the wake dynamics are mostly governed by the lift-induced component of the shed structures. The drag force acting on the elements is computed nevertheless and accounted for in the force, torque and power statistics of the rotors.

The bound vorticity along the blade is thus known and the shed vorticity is constructed through Lagrangian flow tracers, shed and advected by the flow over the course of a time step. This allows to enforce the solenoidal condition of the combined shed structure (see Fig. 2.3). These tubes and sheets are then discretized by means of particles, and smoothed in the thickness direction (normal to both the streakline and the lifting line). Thanks to the use of these tracers, this construction can be carried out at any step of a multistep time integration scheme and interpolated onto the mesh as needed. At the end of the time step, the shed vorticity has to be added to the bulk vorticity through particle-to-particle interpolation (implemented through two successive particle-to-mesh and mesh-to-particle interpolation). As a consequence, this approach allows large time steps unconstrained by the blade tip speed (the time step is rather adapted according to, among other conditions, Lagrangian stability constraints [141]).

2.4.3 Turbulent inflow boundary condition

The turbulence in the incoming flow is achieved through a perturbation vorticity field ω' injected at the inflow as particles. We start from a precomputed or synthetic turbulent flow field $\mathbf{u}'$; in this work, only a turbulent wind box generated by Mann's algorithm [88, 89]. As unbounded directions are used in the transversal directions (y and z), the initially periodic velocity field is made compact through a smooth clipping along its edges. The curl of this clipped velocity field is thus computed by means of finite differences in order to obtain the vorticity fluctuations. The introduction of this ω' into the computational domain now relies on a smooth blending of ω' with the domain vorticity, rather than on the direct insertion. The turbulent inflow particles reside in a buffer domain, which overlaps the computational domain. The vortex particles within this buffer are progressively released: their velocities and their stretching terms transition from a uniform flow at velocity $\mathbf{U}_\infty$ into the full three-dimensional

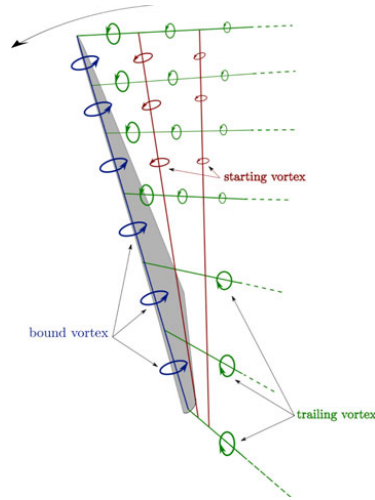


Figure 2.3 – Immersed lifting line: sketch of the particle generation pattern for the immersed lifting line and its near wake (from [32])

flow at the end of the buffer region.

Chapter 3

An Actuator Disk method with tip-loss correction based on effective upstream velocities

This chapter aims at assessing the performance of the advanced actuator disk (AD) method implemented in the FD4 code. The AD method is here corrected with a tip-loss factor; one of the objectives of this work consists in making this correction possible in a wind farm configuration. The classical Glauert tip-loss factor, commonly used in the blade element momentum method, is added here to correct the induced velocities at the rotor in the tip and root regions of the rotor. However, it requires a reference upstream velocity, which is problematic to define in complex flows, such as in wind farms. A methodology is proposed here to infer an effective upstream velocity local to each disk element, based on the one-dimensional momentum theory and using only the local data at the rotor. This estimation is verified through a first set of simulations. This AD supplemented with the tip-loss correction is compared with a high fidelity Vortex Particle-Mesh method, through the simulations in a uniform wind of a constant circulation wind turbine and of a more realistic machine, the NREL-5MW rotor.

A part of these results was presented at the 68th Annual Meeting of the

American Physical Society, Division of Fluid Dynamics (APS-DFD68). *This chapter has been published in Wind Energy* [98].

3.1 Introduction

Large wind farms are challenging to model; this is mostly due to the complex interactions of wakes with the turbines and the environment, and to their inherent unsteadiness. However, an accurate representation of the wind farm and its rotors is of critical importance for the wind industry, in its efforts towards better understanding wakes and reducing their negative impacts by, for instance, developing farm-level control schemes.

Currently, one of the most appropriate approaches consists in using Large Eddy Simulations (LES) coupled to a wind turbine model, allowing unsteady simulations with reasonable computational costs. However, in order for LES to be affordable for large wind farm simulations, coarse mesh resolutions and large time steps are required, leading to the modelling of the rotors through an Actuator Disk (AD) method. With this model, the actual geometry of rotor blades is replaced by a regularized body force term acting over the surface swept by the wind turbine. The most advanced models include torque effects and force computations based on the prevailing velocities at the disk location [15, 92, 96, 143].

Compared to a classical Blade Element Momentum (BEM) theory [57], those improved AD approaches provide more physics to the rotor model and get rid of some strong assumptions: they naturally account for wake expansion and yaw misalignment, the wake is explicitly resolved by a Navier-Stokes solver, which couples the responses of the disk elements, and the disks can react to unsteady flows, with a resulting non uniform forces distribution. However, they only account for averaged forces effects and the flow experienced by a disk differs from that felt by actual individual blades: it implies an azimuthal uniformity of the flow field at a certain radius r (except variations due to turbulent or sheared winds), and the wake just behind an AD is a continuous vortex sheet. The contribution of the wake to the induced velocities at the rotor level thus differs from that due to a finite number of tip vortices. The impact is relatively small for rotors operating at high loading and high tip-speed ratios [26], because the tip and root vortices shed by actual blades are very close to each other, leading to a flow field at the rotor level and a wake topology similar to those of

an AD. Advanced AD methods are thus claimed by some authors to capture the induced velocities in a satisfactory manner [135]. Although, the discrepancies between a disk approach and an actual rotor increase for lower values of tip-speed ratios and loading. The tip region being responsible for most of the torque production, this is thus critical in the resulting power.

One solution consists in using the typical Glauert tip-loss factor, commonly added in the BEM methods (a review of this factor and its variants was made by Branlard [25]) and in adapting its implementation for AD in Navier-Stokes equations [96, 118]. This correction, and its alternative versions [26, 116] are well adapted for the BEM implementation and often applied on the induction factors that are naturally available in this theory. Still, their use within an advanced AD model requires to compute these factors at the very disk from the local velocity field [96]. For the axial induction factor, this implies an estimation of an unperturbed velocity, which appears to be trivial for studies in uniform winds. However it becomes problematic to define and recover a reference upstream velocity when the rotor is impacted by more complex flows such as turbulent winds, wind shear, wakes of upstream machines,... The tip-loss corrections are thus often neglected in wind farm simulations.

The goals of this chapter are twofold: (i) to enable the use of tip-loss corrections on an AD model aimed at LES of wind farms and (ii) to verify the beneficial effect of this correction. Our long-term objective is the large-scale and accurate simulation of wind farms in unsteady scenarios; this motivates the choice of an advanced version of the AD. This specific variant involves the discretization of the disk into a certain number of elements where the normal and tangential aerodynamic forces are evaluated from the local disk velocities [92, 143], hence allowing a realistic response of the disk to incoming flow structures. The added tip-loss correction is the basic version of the Glauert tip-loss factor [57], which then requires an estimation of the upstream velocity for isolating the axial induction factor. The present work addresses this through an estimation of the effective upstream velocity local to each disk element. This velocity estimate relies on the one-dimensional momentum (M1D) theory [30] and the local axial velocities and forces at the rotor. This approach is thus based on very crude assumptions but, as will be shown, it gives very satisfactory results.

This method is described in Section 3.2. We base our assessment of the effective upstream velocity estimation and of the behavior of the resulting AD

method on simulations of idealized wind scenarios and thus only consider steady and uniform winds. Calculations with drag-only disks are first performed; this allows to highlight the differences existing between a disk in LES and in M1D theory and to assess the errors in the computation of the upstream velocity (Section 3.3). We then perform highly resolved simulations of wind turbines with an imposed constant blade circulation in order to emphasize the differences between the AD and the VPM approaches in terms of tip and root behaviors (Section 3.4). The performances of the chosen Glauert tip-loss factor and its effect on the induced velocities are also verified. Simulations of the “NREL offshore 5-MW baseline wind turbine” [75] are finally performed to verify the disk, the effect of the correction and the estimation of the upstream velocity for a more realistic machine (Section 3.5). Two resolutions are used for the disk method, including a relatively coarse one that is representative of that of a wind farm simulation. Finally, conclusions are drawn in Section 3.6.

3.2 Method

3.2.1 Wind turbine modelling

The AD approach was described in Section 2.3.1 and this work will thus refer to some equations presented in Chapter 2. This first version of the AD does not account for a tip-loss correction and we will refer to it as AD-NC (for not corrected). This model constitutes the most general approach: it will be modified for studies where we consider only constant normal loading or constant circulation (see Sections 3.3 and 3.4).

Tip-loss factor An actuator disk approach accounts for an azimuthal averaging of the loading due to the N_B blades. This means that all fluid particles passing through the disk at the same radius r will experience the same change of momentum. This is not the case for an actual rotor for which the local induction will be larger close to the blades than between the blades. The resulting flow, and thus the aerodynamic forces, vary according to the rotor representation. One solution consists in adding tip-loss corrections, commonly used for the BEM method, to recover the actual induction at a blade location and to perform a meaningful aerodynamic look-up [96, 118].

We use here the basic version of the Glauert tip-loss factor (a detailed description of this factor and of its several formulations is available in the work

of Branlard [25]), which is the most common correction added in the BEM method. Its elaboration originates from Prandtl's model, which corrects Betz's optimal circulation in order to account for a finite number of blades [108]. This model is highly simplified as the wake is considered as a series of semi-infinite rigid lines (there is thus no effect of the wake expansion or distortion) and it is developed under the assumption of optimal operating condition [124, 25]. Investigations have been performed to improve this model or to create alternatives to this classical correction, based on higher fidelity methods. A data base of tip-loss corrections was elaborated by Branlard *et al.* [26], using a lifting-line code and aimed at replacing the Glauert tip-loss factor in an adapted BEM theory. Schmitz and Maniaci [116] added a simple modification to the classical tip-loss correction that led to improved BEM prediction at the blade tip. Although this modification is analytical, it requires the flow angle computed by a higher fidelity approach. These works provide corrections accounting for wake expansion, distortion and roll-up but do not lead to a general and universal formulation. An additional correction was also suggested by Shen *et al.* [117, 119]; applied on the normal and tangential forces coefficients, it aims at accounting for three-dimensional flow effects near the tip. This last correction aims to handle a phenomenon that differs from that of the non-uniformity of the flow at the rotor level, and is also applied in actuator lines approaches in order to improve the tip behavior [118, 140]. Since our reference results are produced by lifting line-type approaches, which do not account for three-dimensional effects, the ambition of the present work is restricted to capturing the effects of a finite number of blades.

The Glauert correction impacts the axial and tangential induction factors, respectively a and a' . These factors are defined on each surface element and computed from u_n and u_θ

$$a = 1 - \frac{u_n}{U_\infty}, \quad (3.1)$$

$$a' = -\frac{u_\theta}{\Omega r}, \quad (3.2)$$

where U_∞ is the yet undetermined upstream velocity. The Glauert correction f_{Gl} is then applied on a and a' to compute the factors at the blade, a_B and a'_B

$$a_B = \frac{a}{f_{Gl}}, \quad (3.3)$$

$$a'_B = \frac{a'}{f_{Gl}}, \quad (3.4)$$

where f_{Gl} is

$$f_{Gl} = \frac{2}{\pi} \arccos(\exp(-f_{tip})) \frac{2}{\pi} \arccos(\exp(-f_{root})), \quad (3.5)$$

$$f_{tip} = \frac{B}{2} \frac{(R_{tip} - r)}{r \sin(\phi)}, \quad (3.6)$$

$$f_{root} = \frac{B}{2} \frac{(r - R_{hub})}{R_{hub} \sin(\phi)}. \quad (3.7)$$

with R_{hub} and R_{tip} the radii at the hub and at the tip. The blade velocities are expressed in terms of the corrected induction factors

$$u_{n,B} = U_\infty (1 - a_B), \quad (3.8)$$

$$u_{\theta,B} = -a'_B \Omega r, \quad (3.9)$$

and are then used to compute the blade aerodynamic forces. Because the Glauert tip-loss factor depends on the flow angle ϕ , itself involving the velocities at the blade, an iterative procedure is required.

As mentioned earlier, the correction is applied to the induction factors; this makes it necessary to estimate the upstream velocity. For complex flows, such as sheared or turbulent winds, it becomes very challenging to provide an unambiguous U_∞ seen by the rotor. From these observations we elect to discard this notion and rather use the concept of an effective velocity, U_∞^{eff} , defined as the one that would be hypothetically measured at a location far upstream from this actuator surface. This velocity is inferred based on the local disk velocities and the aerodynamic loads using the one-dimensional momentum (M1D) theory [30], applied to each element (see Fig. 3.1). It yields the following relations

$$u_n = \frac{(U_\infty^{\text{eff}} + u_{n,w})}{2}, \quad (3.10)$$

$$\Delta T = \rho (U_\infty^{\text{eff}} - u_{n,w}) u_n \Delta S, \quad (3.11)$$

where $u_{n,w}$ is the axial velocity far downstream in the wake (see Fig. 3.1). ΔT is the thrust element, equal to the previous time step value of $F_{N,AD}$. With

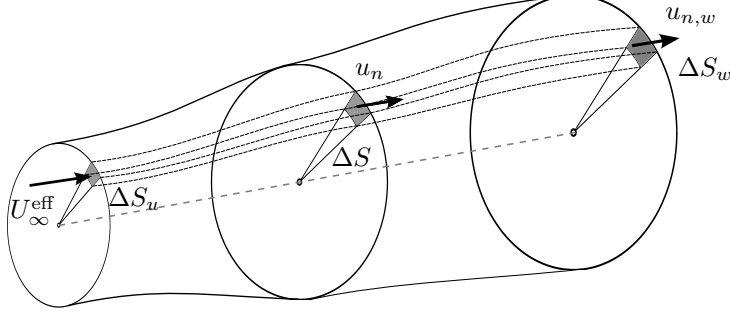


Figure 3.1 – Streamtube intercepted by a disk element

Eqs. (3.10) and (3.11), we find an estimation of U_∞^{eff} local to each element

$$U_\infty^{\text{eff}} = u_n + \frac{1}{2} \frac{\Delta T}{\rho \Delta S} \frac{1}{u_n}. \quad (3.12)$$

The M1D theory relies on strong assumptions [124] and the described methodology bluntly transposes an essentially one-dimensional, low-loading-only approach to an unevenly loaded rotor, in potentially heavily loaded scenarios. The next sections will discuss this approximation and the results will show that this method, in spite of its crudeness, produces consistent estimations, and that the resulting errors have a small impact on the global correction.

Once U_∞^{eff} is known, it is easy to estimate an axial induction factor. Eq. (3.1) is then re-written as

$$a = 1 - \frac{u_n}{U_\infty^{\text{eff}}}, \quad (3.13)$$

and we apply the correction of Eq. (3.3) on this a . The corrected blade velocity becomes

$$u_{n,B} = U_\infty^{\text{eff}} (1 - a_B). \quad (3.14)$$

This AD with tip-loss correction based on an effective upstream velocity is denoted AD-C-EV (for corrected using the effective velocity) in the remainder of this chapter.

The resolutions used by the AD approach in the present chapter call for a comment; we indeed use high resolution cases (about 64 points per D for the NREL-5MW rotor) for verification purposes. Such grid resolutions are comparable to those used in Actuator Line (AL) simulations where one aims to accurately capture the near wake and resolve the interactions of tip vortices

with the larger-scale eddies of the ambient flow [73]. Production simulations with AD methods typically (and in the present work as well) use much coarser resolutions. The only advantage that an AD method could bring at a fine spatial resolution lies in a relaxed time step constraint. Indeed, an explicit method cannot allow an AL to travel more than one mesh cell over a time step whereas an AD method is free of that constraint. This means that an AD method could take a time step approximately λ times larger than an AL technique working at the same resolution (λ being the Tip Speed Ratio). Incidentally, the time step restriction for the AL approach can be somewhat relaxed by distributing the forces on a larger number of points around the line [93]. In such a case, the discrete tip vortices are less resolved and corrections may have to be used again in an attempt to recover some accuracy.

Relaxed time step conditions and coarse spatial resolutions are crucial in making large-scale wind farm simulations computationally affordable. Attempts [45] to use the AL technique at a disk-like coarse resolution have highlighted the need for corrections, identified a sensitivity to the mollification parameter and showed that AD and AL methods then produce similar results (other works have introduced the so-called Actuator Sector method [128, 138] that bridges the gap between AL and AD methods). These considerations strongly support the use of an AD method for farm level production simulations. Therefore, in addition to the verification cases, our approach is also assessed at coarse resolutions against high resolution results produced by a high-fidelity lifting line approach in Section 3.5.

3.3 Estimation of the upstream velocity

The U_∞^{eff} estimation is first verified using simulations with drag-only disks, on which a constant loading is imposed. The upstream velocity is uniform and the thrust coefficient C_T ($=T/(\frac{1}{2}\rho U_\infty^2 \pi R_{tip}^2)$) is chosen to be close to that produced by the “NREL offshore 5-MW baseline wind turbine” [75], for the imposed wind condition.

The wind conditions are chosen to correspond to three typical rotor behaviors: just after the start-up of the rotor ($U_\infty = 5$ m/s and a tip-speed ratio, λ , equal to 10.24), during the optimum operating condition ($U_\infty = 9$ m/s and $\lambda = 7.55$) where the power is maximised, and during the rated operation ($U_\infty = 14$ m/s and $\lambda = 5.7$) where the rotor speed and the torque are kept constant to

avoid extreme loading on the generator. λ is defined as $\Omega R_{tip}/U_\infty$. The resulting C_T for the NREL 5MW at these conditions is computed in a preliminary step using a BEM approach; these values, as well as the parameters used for the BEM simulations, are reported in Table 3.1.

In the methodology described in Section 2.3.1, the tangential component $F_{\theta,AD}$ is forced to be equal to 0 and $F_{N,AD}$ is imposed with the previously computed C_T

$$F_{N,AD} = \frac{1}{2} \rho U_\infty^2 C_T \Delta S. \quad (3.15)$$

The disk radius is the same as that of the NREL-5MW, i.e. $R_{tip} = 63$ m ($D=126$ m), and the hub radius, R_{hub} , is imposed to 0.

U_∞ (m/s)	λ	RPM	γ (°)	C_T
14	5.7	12.10	8.7	0.3
9	7.55	10.30	0	0.8
5	10.24	7.76	0	0.95

Table 3.1 – NREL-5MW: Operating points.

The simulation domain has to be large enough to limit the effect of the boundaries on the resulting velocities: it is set to $8D \times 9D \times 9D$, and the center of the wind turbine is located at $(3D, 4.5D, 4.5D)$. Three different resolutions are used to highlight any effect of this parameter on the U_∞^{eff} computation: 16 points per D (which is the typical resolution used in our wind farm simulations), 32 points per D and 64 points per D.

The inlet boundary condition is a laminar flow and a natural convective boundary condition is applied at the outflow. Periodic boundary conditions are used in the lateral and vertical directions. All the meshes used in the present work are uniform and do not involve local refinement in the rotor region or in its wake. These boundary conditions are used for the AD simulations performed in this chapter.

In Fig. 3.2, U_∞^{eff} , made dimensionless with respect to the imposed wind velocity U_∞ , is presented as a function of the radius for the different wind conditions and resolutions. The results are globally close to the unity, with a maximum error of about 4 %. The increase of the error value at the last point is due to higher axial velocities at the disk extremities (see Fig. 3.3) and explained by the regularization of the disk. The three different resolutions give

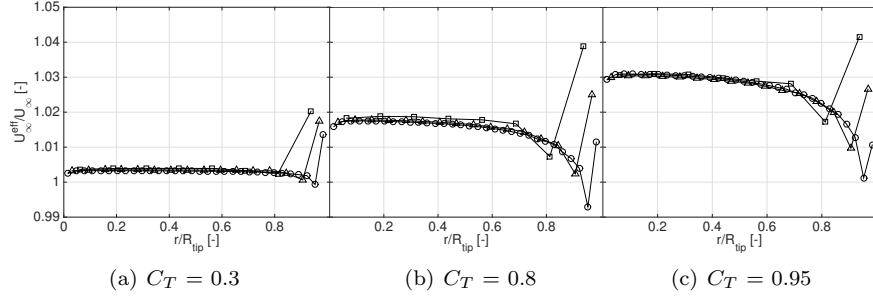


Figure 3.2 – Uniformly loaded wind turbine: U_{∞}^{eff} normalized by the imposed inflow velocity for the three C_T and three resolutions, 16 points/D (solid line with squares), 32 points/D (solid line with triangles) and 64 points/D (solid line with circles).

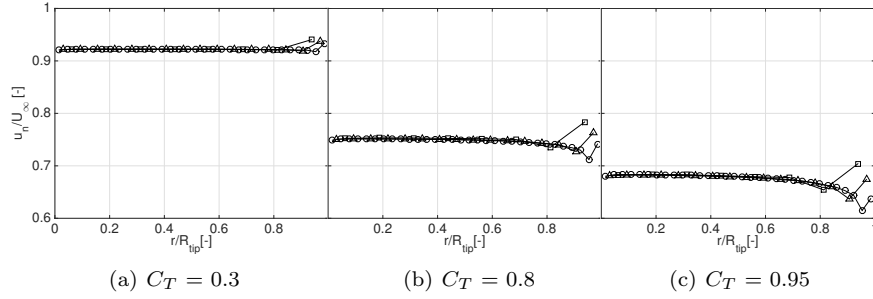


Figure 3.3 – Uniformly loaded wind turbine: Axial velocities for the three C_T and the three resolutions (16 points/D, solid line with squares, 32 points/D, solid line with triangles, 64 points/D, solid line with circles). The velocities are normalized by the imposed inflow velocity U_{∞} .

results close to each other: this indicates that the method to estimate U_{∞}^{eff} is robust with respect to the mesh resolution. As the loads are arbitrarily imposed and not computed based on the local flow velocities, the dependence with the mesh is expected to be small, and the possible variations are only due to the velocities induced by the wake. However, the results of Section 3.5 will confirm that the methodology is robust with respect to the mesh.

Results show that the higher the C_T , the higher the discrepancies. The M1D theory logically assumes a constant induction along the radius when a constant loading is imposed, while Fig. 3.3 shows that the drag disk simulations give non uniform axial velocities. $\Delta T/\Delta S$ remaining constant, this non-uniformity of u_n impacts the U_{∞}^{eff} estimation. This can be traced to the contribution of the pressure component acting on the lateral boundaries of the annular streamtubes to their momentum balance [96, 134]. This pressure term is usually

neglected when the whole disk is considered, because the control volume is entirely surrounded by a uniform pressure, but this contribution does not vanish in a discrete analysis. This increases the induction on the outboard part, and decreases it on the inboard part; Eqs. (3.10) and (3.11) should then be modified accordingly to account for this pressure term. However, it becomes negligible when the expansion is small and the simulation with $C_T = 0.3$ gives an accurate estimation of U_∞^{eff} (Figs. 3.2(a) and 3.3(a)).

Modifications have been proposed to account for the physical mechanisms causing the deviation mentioned above. In the work of van Kuik *et al.* [134], an annulus-correction model was derived based on a potential flow model for the wake. This correction is a function of the radial position and the global C_T and is applied on the axial velocity. In order to account for the wake expansion (and rotation), sub-models were also developed by Madsen *et al.* [87], to be added in the BEM model. The mentioned corrections could be included in our approach for estimating U_∞^{eff} when the loading increases. However, we recall that U_∞^{eff} is used for isolating an induction factor on which the tip-loss correction is applied. When C_T and λ increase, the effect of the tip-loss factor decreases and the errors of U_∞^{eff} and a are mitigated. The beneficial effect of such corrections should be small in terms of accuracy on the resulting power.

Additional effects can reinforce the deviations between the M1D theory and an actuator disk in a flow solver, such as a wake that becomes rapidly turbulent for heavily loaded cases [56]. Classical corrections exist for the BEM approach that modify the axial momentum equation for high C_T [29, 56, 139] and are active when the induction factor exceeds a certain value (between 0.33 and 0.4). For most practical designs, optimal conditions imply induction factors around 0.3 and the corrections would thus have a negligible impact. Near the start-up conditions ($\lambda = 10.24$ in our simulations), the induction factors can reach larger values, up to 0.5-0.6, and these corrections could improve the U_∞^{eff} estimation. However, as mentioned above, this case implies a small tip-loss factor and the addition of such corrections will thus not bring sensitive differences in the results.

Based on these considerations, we decide, as a first approach, to keep the basic M1D theory for the computation of U_∞^{eff} .

3.4 The Joukowski rotor

The disk and the lifting line approaches are used to simulate a wind turbine with a constant circulation along the blade. This concentrates vortices of high intensity at the very ends of the blades, where the circulation sharply decreases to zero; this emphasizes the influence of the vortices on the induced velocities. These working hypotheses strengthen the differences between the disk and the discrete blade descriptions, hence facilitating their comparison. They also allow to highlight the need for a tip-loss correction and its performance.

The imposed wind speeds are the same as those of the previous simulations and the rotor speed is fixed to obtain the tip speed ratio of the NREL-5MW for the considered wind speed (see Table 3.1). The tip radius is the same as that of the NREL-5MW and the hub radius is fixed to be 10% of R_{tip} ($R_{hub}=6.3$ m) to help reveal the root behavior. For each wind speed, the value of the circulation is adjusted within AD simulations at a coarse resolution in order to reach the predetermined values of C_T that correspond to those of the NREL-5MW (see Tables 3.1 and 3.2).

The imposed circulation is the same in the disk and VPM methods. For the lifting lines, the circulation Γ is directly imposed while, in the AD simulations, the lift distribution is selected in such a way that $L = \rho V_{rel} \Gamma$. The velocity V_{rel} is computed from the normal and tangential flow velocities (see Eq. (2.12)): these velocities are either corrected or not, according to the disk version. Indeed, for each wind condition, three AD simulations are performed: without tip-loss correction (AD-NC), with a Glauert tip-loss correction based on U_∞ (the a factor is computed from Eq. (3.1)) and with a correction based on U_∞^{eff} (the a factor is computed from Eq. (3.13)). These last two disk approaches are denoted AD-C-FV and AD-C-EV for corrected using fixed and effective velocities, respectively. For these simulations, the blades are assumed to have zero drag, and $(F_N, F_\theta) = (L \cos \phi, L \sin \phi)$. F_N can also be expressed as $\rho \Gamma (\Omega r - u_{\theta,B}) \Delta r$.

The initial size of the VPM domain is $8D \times 2D \times 2D$; the method indeed handles exactly the unbounded spanwise directions using a compact domain solely containing the vorticity. The wind turbine is located at $(3D, 1D, 1D)$; the resolution is set at 64 points per radius (128 points per D) to accurately capture the tip and root vortices. The AD simulation domain remains the same as the one used for the drag disk studies and it has a resolution as fine as possible in order to verify the differences between the two approaches and

highlight the need of a tip-loss correction: 48 points per radius (96 points per D) is a compromise between computational costs and enforcing similar resolutions in the AD and VPM calculations.

Table 3.2 presents the resulting values of the thrust coefficients and, as expected, all simulations produce thrust coefficients close to the expected one.

case			$C_T = \frac{T}{1/2\rho U_\infty^2 \pi R_{\text{tip}}^2}$			
λ	Γ (m^2/s)	C_T	VPM	AD-NC	AD-C-FV	AD-C-EV
5.70	48.65	0.3	0.3006	0.3005	0.3010	0.3005
7.55	62.7	0.8	0.8024	0.8020	0.8027	0.8025
10.24	30.65	0.95	0.9515	0.9514	0.9517	0.9516

Table 3.2 – Constant circulation wind turbine: Operating points and resulting thrust coefficients

Figure 3.4 presents $u_{n,B}$ and $u_{\theta,B}$, normalized by the inflow velocity U_∞ : for the VPM method, the velocities are a direct measure at blade level while, for the disk, the velocities are corrected using the tip-loss factor (except for AD-NC, where $u_{n,B}=u_n$ and $u_{\theta,B}=u_\theta$). Figure 3.5 shows the estimation of U_∞^{eff} for AD-C-EV.

Again, the regularization of the disk or blades has an impact at the extremities of the rotor, as its stencil in both methods will use points past the blade tips and roots. The addition of the tip-loss factor masks this effects for the AD-C-FV and AD-C-EV, but it is well present for the uncorrected u_n .

The remainder of this section is devoted to the discussion of these figures. For the sake of clarity, we have structured it around four specific aspects.

Need for a tip-loss correction Results show that the AD-NC underestimates the induction near the tip and the root, with a higher axial velocity and a lower tangential velocity, compared to the VPM approach. For the highest λ , the discrepancies are lower. Indeed, in that case, the structure of the very near wake, which strongly influences the rotor induction, is similar in the disk and in the lifting-lines simulations (see Fig. 3.7(c)): the tip and root vortices shed by the VPM blades are very close to each other, due to the high λ and C_T , leading to a wake topology similar to that of the disk. The induction at the AD-NC is close to that at the VPM blade and the tip-loss correction does

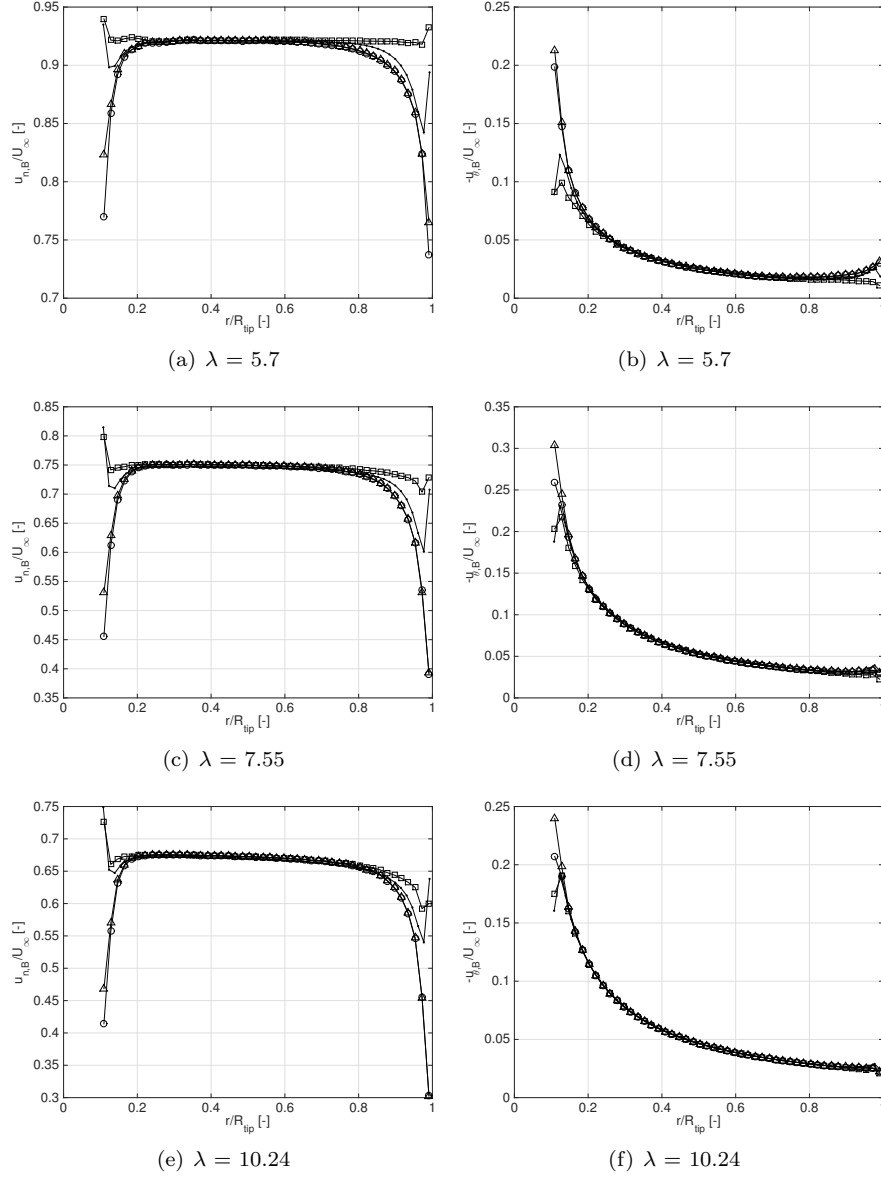


Figure 3.4 – Constant circulation wind turbine: Blade velocities for VPM (solid line with dots), AD-NC (solid line with squares), AD-C-FV (solid line with triangles) and AD-C-EV (solid line with circles). The velocities are normalized by the imposed flow velocity U_{∞} .

not bring sensitive improvements. This partially agrees with the conclusions of van Kuik *et al* [135]: in this work, the authors found that a disk with a potential flow correction does not require additional correction and capture

sufficiently well the induced velocities. However, by decreasing λ and C_T , the shed vortices get further apart, increasing the differences with the disk results (see Figs. 3.7(a) and 3.7(b)) and making the tip-loss correction useful. Low λ and low C_T do require a supplementary correction.

The use of a tip-loss factor is more or less essential according to the operating condition but it globally improves the tip and the root behaviors of the disk for all cases. Glauert's correction appears slightly too strong however.

Estimation of U_∞^{eff} Before discussing the performances of the correction in more details, we first study the estimation of U_∞^{eff} . The two corrected disk models, AD-C-FV and AD-C-EV, produce results very close to each other, except for the axial velocity in the root region (see Fig. 3.4). The estimate of U_∞^{eff} (Fig. 3.5) presents systematically higher errors near the hub due to a local high loading. This behavior can be revealed through the local thrust coefficient C_t , which can be expressed in terms of the induction and the radial position

$$C_t = \frac{\Delta T}{\frac{1}{2}\rho U_\infty^2 \Delta S} = \frac{N_b \Gamma (\Omega r - u_{\theta,B})}{\pi r U_\infty^2} = 2q \left(\lambda - \frac{u_{\theta,B}}{U_\infty} \frac{R_{tip}}{r} \right), \quad (3.16)$$

where q is the dimensionless circulation and is defined as $N_b \Gamma / 2\pi R U_\infty$. Near the tip, $-\frac{u_{\theta,B}}{U_\infty} \frac{R_{tip}}{r}$ is negligible compared to λ but its contribution increases when the radius decreases, due to $\frac{R_{tip}}{r}$ term but also $-\frac{u_{\theta,B}}{U_\infty}$ that reaches 0.2 - 0.3 near the blade root. The loading can thus have a very high value near the hub, accentuating the departures from the assumptions of the M1D theory, as previously explained in Section 3.3. The root behavior will be discussed later, but the present configuration is quite artificial and constitutes a very severe case. In an actual turbine, the loading decreases in the hub region; this will prevent high U_∞^{eff} estimation errors, as shown in Section 3.5.

Tip-losses In each case, the correction appears to be excessive: the velocity drops it introduces in the tip regions are below those of VPM (see Figs. 3.4(a), 3.4(c) and 3.4(e)). This is less noticeable for the tangential components, but the same trend is observed, with an induction higher than that of VPM. It is better highlighted by comparing the computed tip-loss factor in AD-C-EV to ones extracted from the VPM simulations (Fig. 3.6). The methodology of Branlard *et al.* [26] is used to express the normal and the tangential tip-loss factors in the VPM approach

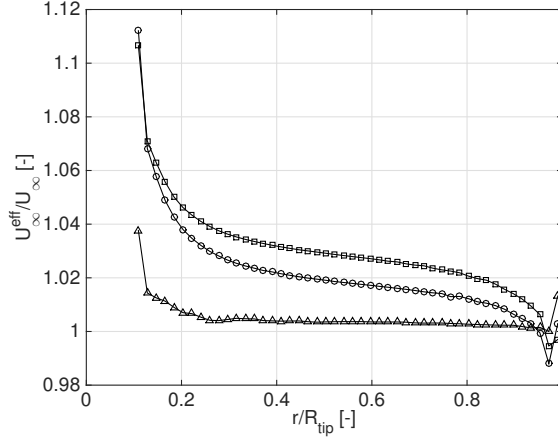


Figure 3.5 – Constant circulation wind turbine: U_{∞}^{eff} normalized by the imposed inflow velocity U_{∞} for λ equal to 5.7 (solid line with triangles), 7.55 (solid line with circles) and 10.24 (solid line with squares).

$$f_a = \frac{\langle a \rangle_{\theta}}{a_B} \quad \text{and} \quad f_{a'} = \frac{\langle a' \rangle_{\theta}}{a'_B} \quad (3.17)$$

where $\langle \cdot \rangle_{\theta}$ represents the azimuthal average. a_B and a'_B are extracted from the VPM blade velocities, $a_B = 1 - u_{n,B}/U_{\infty}$ and $a'_B = -u_{\theta,B}/\Omega r$, while averaged induction factors are obtained in the same way but by using the azimuthally averaged velocity field at the rotor position. We first notice that f_a and $f_{a'}$ are close to each other, it is thus reasonable to use the same tip-loss factor for correcting u_n and u_{θ} in the AD approach. The analytical tip-loss factor appears to overestimate the losses, mainly on the outboard part of the blade. This agrees with observations made in the work of Branlard *et al.* [26], where the Glauert tip-loss correction computed in a BEM approach over predicted the losses extracted from a lifting line method. This most probably comes from the Glauert tip-loss correction itself, which relies on strong assumptions. Results of the VPM approach could be used for improving the correction, in a fashion similar to that of Schmitz and Maniaci [116]. We leave this for future investigations.

Hub losses Near the root, the nature of these losses is somewhat different and the vortex sheets are shed at a high angle. They possess a strong streamwise vorticity component, making their induced velocities mostly tangential, and they quickly interact downstream of the rotor (Figs. 3.7(b) and 3.7(c)). In fact,

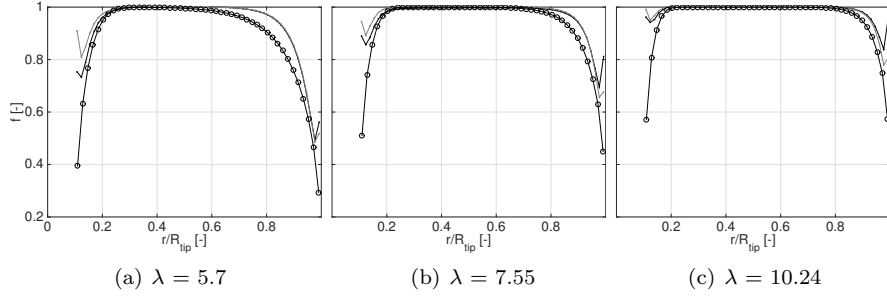


Figure 3.6 – Constant circulation wind turbine: analytical Glauert tip-loss factor used in AD-C-EV (solid line with circles) and normal and tangential tip-loss factors extracted from the VPM rotor velocity field (black and grey solid lines with dots)

the hub-loss factor is expressed in a form similar to that of the tip correction (see Eqs. (3.6) and (3.7)) for modelling convenience but has not been validated [25]. For that reason, many BEM implementations often neglect the hub losses. This approximation does not really affect the wind turbine production, because the hub radius often represents a small fraction of the rotor and the tangential force near the root does not significantly contribute to the total power. Nevertheless, we see that the corrected disk presents a better root behavior, with velocities that follow the VPM curve (see Figs. 3.4 and 3.6), even if the correction appears too high. We thus decide to add the hub-loss effect for the second set of simulations.

Globally, these simulations clearly highlight the need of a tip-loss correction, and the addition of the Glauert correction, although often criticized because of some physical inconsistencies [117], helps to improve the disk behavior. The estimation errors for effective upstream velocity are relatively small and do not significantly impact the tip-loss correction, except near the hub where the discrepancies are higher. We do recall however that the present case was very severe and it is not representative of an actual rotor. We will verify the AD-C-EV for a more realistic wind turbine case in the next section.

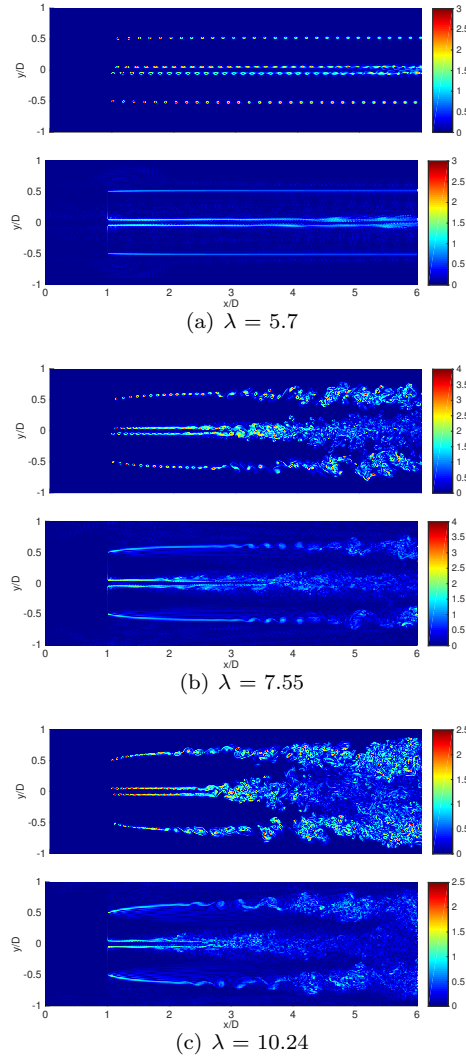


Figure 3.7 – Constant circulation wind turbine: Vorticity magnitude in the horizontal mid plane for the VPM and the AD-C-EV approaches (top and bottom for each subfigure, respectively). The units are in $1/s$.

3.5 Simulations of the NREL 5MW wind turbine

This last set of simulations aims at verifying our disk approach and the correction for a realistic machine: it has been performed using the “NREL offshore 5-MW baseline wind turbine” [75]. The produced lift and drag forces or the circulation (for the VPM method) are thus obtained from the local flow velocities, tabulated airfoil and geometrical data. The parameters used for these simulations, i.e. the wind speed, the rotor speed and the blade pitch angle are those given in Table 3.1.

The NREL-5MW has a hub radius of 1.5 m. However, this value is not used for the correction factor computation in Eq. (3.7). Indeed, the NREL blade has a cylindrical cross-section over its first meters, so only drag forces are present. R_{hub} in Eq. (3.7) is thus imposed to the position of the first airfoil element, i.e. around 8 meters.

For the VPM approach, the domain and the resolution remain the same as those of the previous simulations. For the AD method, the domain lengths are conserved but the resolution is coarser and imposed to 16 points per D, which is the typical resolution for our wind farm simulations, which aim at large, accurate yet affordable farm calculations. Simulations with 64 points per D are also performed to highlight possible resolution effects. Again, the three different versions of the AD are used (AD-NC, AD-C-FV and AD-C-EV).

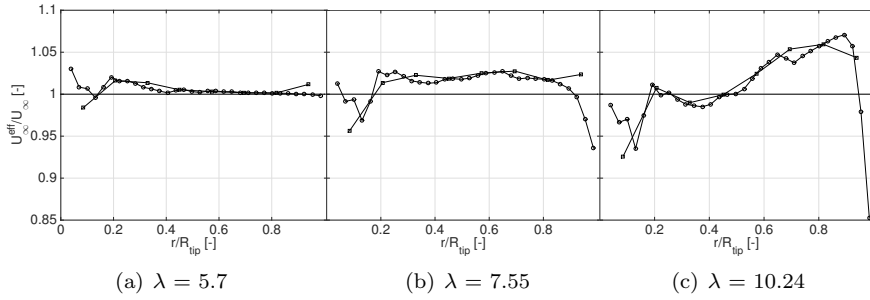


Figure 3.8 – NREL-5MW: U_{∞}^{eff} normalized by the imposed inflow velocity U_{∞} for the three λ and the two resolutions, 16 pts/D (solid lines with squares) and 64 pts/D (solid lines with circles).

Figure 3.8 presents $U_{\infty}^{\text{eff}}/U_{\infty}$ computed along the disk for the different operating conditions. Figure 3.9 presents the axial and tangential blade velocities for the three different λ and normalized using U_{∞} . Again, it is a direct mea-

sure at the blade in the VPM approach while flow velocities are corrected with the tip-loss factor for the AD-C-FV and AD-C-EV (for AD-NC $u_{n,B} = u_n$). The normal and tangential force distributions on the blade are presented in Fig. 3.10.

Estimation of U_∞^{eff} Again, the methodology for computing U_∞^{eff} provides a very good estimation for the smallest λ and C_T values (see Fig. 3.8(a)). When λ is higher (7.55 and 10.24), the method deviates and the errors increase; the global remarks given for the drag disk remain valid in the NREL case. Some geometrical and aerodynamic effects are also noticeable, according to the operating point. For example, the deceleration is important near the tip for the $\lambda = 10.24$ (see Fig. 3.9(e)), which means that the loading is significant in this region, and there is thus more deviation with the M1D theory. On the contrary, higher errors are observed around $r/R_{tip} = 0.2$ for the $\lambda = 5.7$, where u_n is the lowest.

The two resolutions give results close to each other. This indicates that the methodology for computing U_∞^{eff} is robust with respect to the mesh resolution, and thus appropriate for large wind farm simulations. The errors are globally below 4% but, near the tip for the largest λ , the error reaches more than 15%. This has however a very limited impact on the resulting velocities and forces, as it will be discussed in the next paragraph.

Blade velocities and loads Globally, there is a good agreement between all results, with the exception of coarse resolution effects in strong gradient regions (mainly near the root). The impact of U_∞^{eff} errors is small: the AD-C-FV and AD-C-EV lead to results very close to each other, for both resolutions. The largest errors observed for the $\lambda = 10.24$ are attenuated due to the weak correction for that case (high C_T and high λ), and the large contribution of the rotor speed near the tip.

The simulations with 64 points per D lead to results very close to those produced by the VPM approach, and the addition of a tip-loss factor helps to further reduce the differences, except for the highest λ , where the correction appears to be excessive (see Figs. 3.9(e) and 3.9(f)). The resulting difference observed near the tip for the tangential forces (see Fig. 3.10(f)) can strongly impact the global power, as F_θ is multiplied by the radius to obtain the torque, itself used for the power computation. The addition of a tip-loss correction for this highest λ increases the differences with the VPM method, in terms of

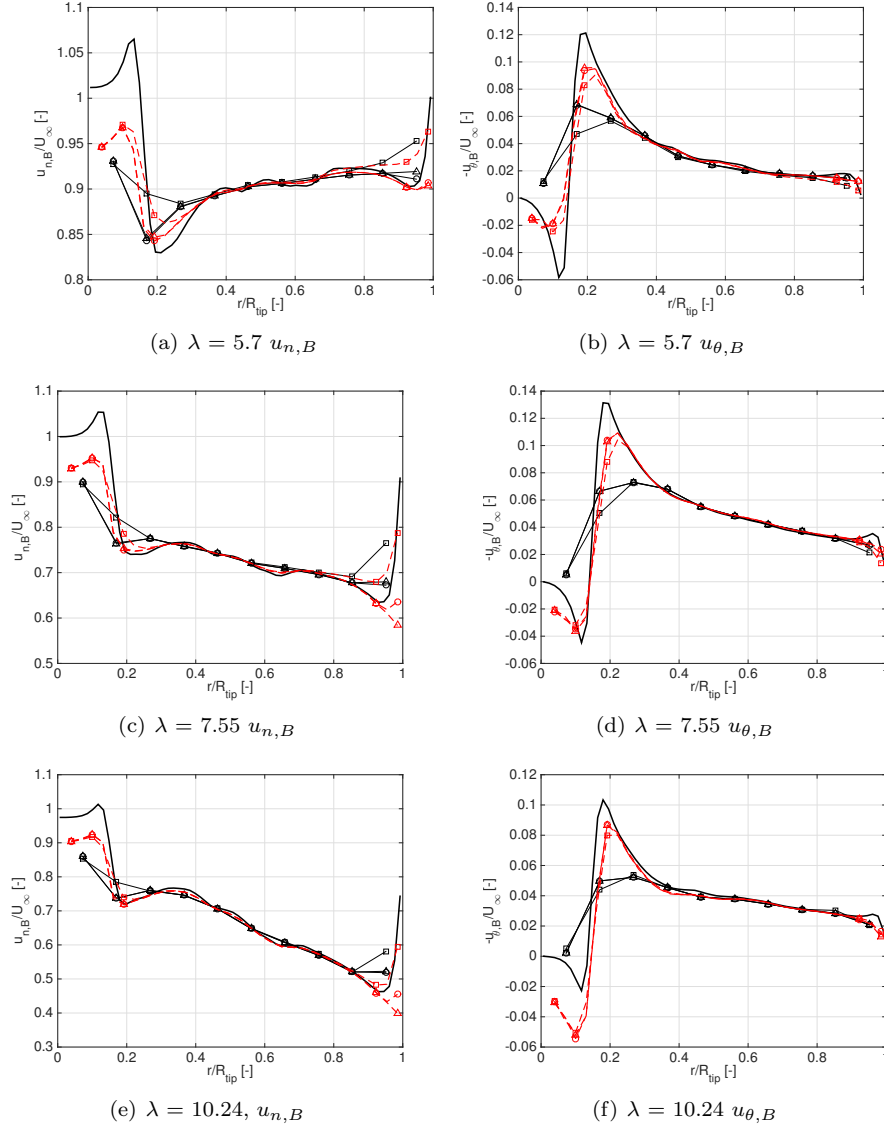


Figure 3.9 – NREL-5MW: Blade velocities for VPM (solid line), AD-NC (line with squares), AD-C-EV (line with circles) and AD-C-FV (line with triangles). Two resolutions are presented: 16 points per D (black solid lines) and 64 points per D (red dashed lines). The velocities are normalized by the imposed flow velocity U_∞ .

power, as discussed further below.

In the simulations with 16 points per D, the addition of the correction helps to reduce the discrepancies between the VPM and the disk approaches for all the cases. Indeed, the too excessive correction observed for the largest λ and 64

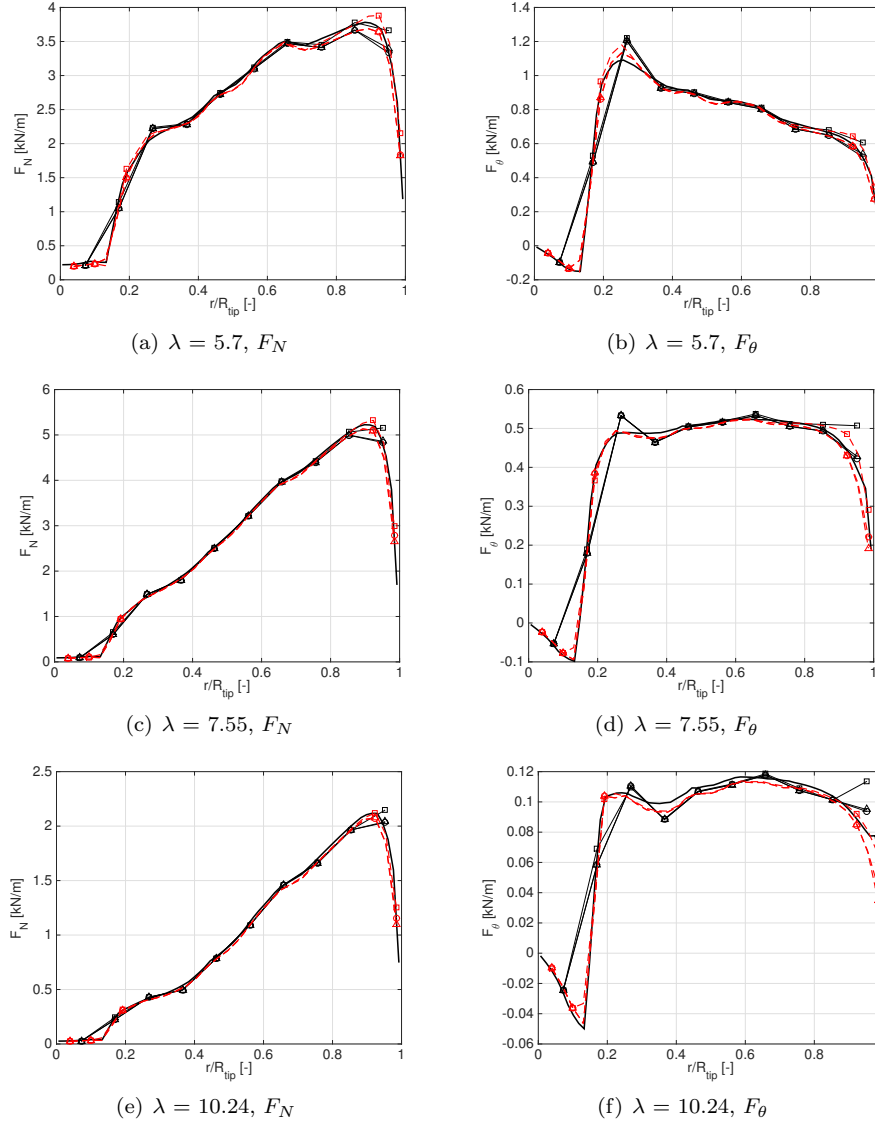


Figure 3.10 – NREL-5MW: Forces for VPM (solid line with dots), AD-NC (solid line with squares), AD-C-EV (solid line with circles) and AD-C-FV (solid line with triangles). Two resolutions are presented: 16 points per D (black solid lines) and 64 points per D (red dashed lines).

points per D seems to be attenuated by an effect of resolution. The beneficial impact is less noticeable for the forces, while it was evident for most of the cases with the finest resolution. However, the AD-C-EV appears to have a better tip behavior (compared to the AD-NC), with curves decreasing and tending

towards null forces. This will be highlighted when power and thrust will be considered below.

Power and thrust Table 3.3 shows the percentage difference between the AD-NC, AD-C-FV, AD-C-EV and VPM approaches in terms of power and thrust productions. We consider that the VPM method produces our reference results, this approach being the most faithful representation of the rotor.

(a) Power							
case	Power (kW)	Power difference (%)					
λ	VPM	AD-NC ₆₄	AD-C-FV ₆₄	AD-C-EV ₆₄	AD-NC	AD-C-FV	AD-C-EV
5.70	5368.8	2.9	-0.4	-0.4	6.0	2.6	2.1
7.55	2930.8	3.2	-1.1	-0.7	8.0	3.7	3.4
10.24	472.8	0.1	-3.8	-3.3	6.4	2.4	2.1

(b) Thrust							
case	Thrust (kN)	Thrust difference (%)					
λ	VPM	AD-NC ₆₄	AD-C-FV ₆₄	AD-C-EV ₆₄	AD-NC	AD-C-FV	AD-C-EV
5.70	461.6	1.6	-0.6	-0.6	4.4	2.0	1.8
7.55	500.6	1.4	-0.3	-0.2	4.6	2.8	2.7
10.24	182.4	0.1	-1.2	-1.0	3.9	2.6	2.5

Table 3.3 – Power and thrust for VPM simulations and percentage difference between the AD-NC, AD-C-FV, AD-C-EV and VPM approaches. The subscript 64 indicates the results for the finest resolution

The power produced by the VPM code can exceed the 5.3 MW of mechanical power of the reference machine (in Jonkman *et al* [75]). Indeed, the rotational speed and the pitch angle are imposed as prescribed in the report for the NREL-5MW; however those values are obtained using a BEM method, and the VPM approach produces slightly different results. It can result in the power exceeding the rated power.

As was already noticed in velocities and forces, AD-C-EV and AD-C-FV lead

to results very close to each other, for both resolutions and the tip speed ratios, with a maximum of 0.5 % of differences. This provides confidence in our method for estimating U_∞^{eff} .

As expected after observing the velocities and the loads on the blades, for the two lowest λ , we see that the AD-C-EV simulation with the finest resolution yields results in close agreement with those of VPM (the power and thrust differ by a maximum of 1 %), while we observe larger differences for the AD-NC (between 1.4 % and 3.2 %). At a higher λ , the addition of a tip-loss increases the differences. We thus observe about 3 % of differences for the power and 1% for the thrust, while, in that condition, the AD-NC agrees better with the VPM method.

For the disk simulations at a coarser resolution, the correction helps to reduce the differences in all cases. The level of errors decreases from 7-8% to 2-3.5% for the power, and from about 4.5 % to 2-3 % for the thrust. In spite of its coarser resolution, the disk corrected with a tip-loss factor produces results close to the higher fidelity method, which involves a finer resolution and a finite number of blades.

Computational aspects For the sake of completeness, we provide some information about the computational costs involved in the simulations above. Tables 3.4 and 3.5 provides the domain size, the cost in terms of processors-hours (CPUs-hours) for one convective time (defined as D/U_∞) and the same cost, normalized by the total number of grid points.

As mentioned at the end of Section 3.2, the time step used in AD simulations is not restricted by the condition that the blade tip sweep across less than a mesh cell; it is only constrained by the conventional Courant-Friedrichs-Lewy (CFL) number. The fourth-order finite differences code used for the present simulations cannot exceed a CFL number of 0.2 – 0.25 in order to avoid numerical oscillations. Consequently, the computational cost is not much influenced by the operating condition and the values presented in Table 3.5 are representative of the three cases.

As the lifting lines and the shed vorticity are both handled by Lagrangian tracers [32], the VPM approach allows large time steps unconstrained by the blade tip speed, unlike an Actuator Line technique. The time step is rather adapted according to, among others, Lagrangian stability constraints [141]; this leads to a blade tip CFL number, $CFL_{tip} = \Omega R_t/h$, of approximately 4.4 for

$\lambda = 5.7, 2.75$ for $\lambda = 7.55$ and 2.7 for $\lambda = 10.24$. The domain size is adapted over the course of the simulations to encompass all the vorticity; it particularly grows for the two operating conditions involving a turbulent wake state ($\lambda = 7.55$ and 10.24). The computational costs for the VPM simulations are thus given for each operating condition and the domain size is to be understood as an average over the whole simulation time.

	VPM		
	128 pts/D		
Case : λ	5.7	7.55	10.24
$[L_x \times L_y \times L_z]/D$	$8 \times 2 \times 2$	$\simeq 8 \times 2.6 \times 2.6$	$\simeq 8 \times 2.7 \times 2.7$
CPUs-hours/ (D/U_∞)	$\simeq 118$	$\simeq 391$	$\simeq 555$
CPUs-hours/ $(D/U_\infty)/\text{grid point}$	$\simeq 1 \times 10^{-6}$	$\simeq 3.5 \times 10^{-6}$	$\simeq 4.6 \times 10^{-6}$

Table 3.4 – Computational costs of the simulations for the VPM approach

	AD	
	64 pts/D	16 pts/D
$[L_x \times L_y \times L_z]/D$	$8 \times 9 \times 9$	$8 \times 9 \times 9$
CPUs-hours/ (D/U_∞)	$\simeq 110$	$\simeq 0.4$
CPUs-hours/ $(D/U_\infty)/\text{grid point}$	$\simeq 6.5 e^{-7}$	$\simeq 1.6 e^{-7}$

Table 3.5 – Computational costs of the simulations for the AD approach

3.6 Conclusions

This work aimed at verifying the need of a tip-loss correction for advanced Actuator Disk (AD) methods coupled to a flow solver and at enabling the use of this correction in wind farm simulations. The chosen AD model accounts for torque effects and the aerodynamic loads are computed based on the prevailing velocities at each point of the disk. We have supplemented it with the Glauert tip-loss correction, typically used in Blade Element Momentum theory. This factor requires a value of the upstream velocity, which could become problematic for rotors impacted by complex flows (a turbulent wind, a wind shear,...).

A methodology has thus been developed to estimate an “effective upstream velocity”, local to each disk element, based on the one-dimensional momentum (M1D) theory and the local axial velocities and forces.

This approach for estimating the effective upstream velocity was first verified through a set of simulations with drag disks at several resolutions. The resulting errors, due to the difference between the M1D theory and the disk, were very small and not sensitive to the resolution.

The AD results were then compared to higher fidelity results provided by a VPM method coupled to immersed lifting lines. A wind turbine with a constant circulation was first simulated in different uniform wind conditions to establish the need for a tip-loss correction and assess the impacts of the effective upstream velocity computation errors. The comparison between both methods clearly emphasized the added value of a correction, and the chosen Glauert correction, despite its strong underlying assumptions, helped in improving the tip and root behaviors of the disk. The errors on effective upstream velocity estimations were small and do not impact the tip-loss correction, except near the hub where the discrepancies were higher. However, such a case was a very severe case and was not representative of a realistic rotor.

The last set of simulations concerned the NREL-5MW wind turbine; it aimed at verifying the AD on a more physically relevant configuration and at comparing its behavior to the VPM method. Two resolutions were used, including a coarser one, representative of that used in our wind farm simulations. Again, the addition of the tip-loss correction helped to reduce the discrepancies, and the AD in coarse resolution led to a maximum power difference of about 3.5% relatively to VPM results. The largest effective upstream velocity errors that appeared for the largest loading and the largest tip-speed ratio, were mitigated due to the weak tip-loss correction for that case.

These first investigations have shown that we can trust the disk model and the method for estimating the effective upstream velocity. This first version of the corrected AD model exhibits a lot of potential for strongly non uniform inflows: Chapter 4 will investigate this by testing this AD model in more complex scenarios (e.g., a turbulent wind or wind shear and veer) and verifying the local effective upstream velocity computation.

Finally, we mention that the present correction is based on the combination of a routinely used tip-loss correction and simple analysis. This combination appears to over-predict the losses and indicates that the present comparisons

between actuator disk and lifting line could be further exploited to improve the tip-loss correction, as already suggested by others [26, 116]. The actuator disk approach could also be further improved with additional factors accounting for three-dimensional effects at the blade tip [118, 119] or the increase of the lift coefficient and stall delay due to the rotation of the blade [86]. These corrections will bring additional physics to the approach and will allow a quantitative comparison between AD simulations and measurements, such as those available in the NREL phase IV [54, 60] or MEXICO [121] projects.

Chapter 4

Assessment of the Actuator Disk method in turbulent winds

In the previous chapter, the Actuator Disk method and its tip-loss correction have been verified in uniform flows. We here perform simulations in which the AD is impacted by turbulent fluctuations. This will allow to verify the tip-loss procedure and its beneficial impact in more complex flows. Again, the AD results are compared to those obtained with the VPM method.

A part of these results was presented at the 68th Annual Meeting of the American Physical Society, Division of Fluid Dynamics (APS-DFD68).

4.1 Introduction

In actual operating conditions, wind turbines are affected by the turbulence of the atmospheric boundary layer or of wakes of upstream rotors and they are thus exposed to a variable velocity field. According to its level of complexity, an Actuator Disk (AD) method may more or less accurately respond to such conditions. A simple drag disk, despite its simplicity, reacts at the moment an incoming fluctuation impinges the rotor if the thrust is computed based on the local disk averaged velocity [31]. However, the normal forces are usually distributed uniformly over the rotor disk and torque effects are omitted. This strongly degrades the accuracy in the resulting power production and wake flow.

More advanced AD methods, with aerodynamic loads computed at each point of the disk [143] can respond to incoming temporal and spatial flow variations, with a resulting non uniform flow distribution. The Actuator Disk developed in this work presents the characteristics of this last variant and is expected to produce relevant results in turbulent flows.

Furthermore, the developed AD is improved with the tip-loss factor. As already detailed in the previous chapter, the tip-loss correction procedure requires an estimation of an unperturbed velocity, which is very challenging in turbulent flows. We thus use the local disk data, and the concept of the one-dimensional momentum (M1D) theory to define local effective upstream velocities, corresponding to those that would be hypothetically measured at a location far upstream. This procedure should prove very appropriate for turbulent flows. Indeed, it removes any ambiguity in the choice of an upstream velocity measurement in the timing of the response of the rotor (and its tip loss correction) to incoming flow structures.

This procedure entails two rather crude assumptions, however: it is meant to be applied to steady and uniform flows. These constraints are here waived in order to produce a simple and fast approach that includes tip loss corrections. The objectives of this chapter are then two-fold: (i) to verify the tip-loss correction procedure and its performances in turbulent flows, and (ii) verify the AD method by comparing its power and thrust histories and its loads statistics against those produced by a higher fidelity approach working at a finer resolution in turbulent flows. We here use the AD method only in coarse resolutions, i.e. 16 points per D. We first verify the estimation of the upstream velocity in a turbulent flow. We then compare the velocities, loads and instantaneous power and thrust production against fine resolution results produced by the VPM method coupled to immersed lifting-lines. In the last section, the root bending moments are investigated and compared to the VPM ones. The objective consists in verifying if finer information relevant to fatigue loads can be recovered from a disk approach at a coarse resolution.

4.2 Configuration

We still consider the NREL-5MW wind turbine and two operating conditions of this machine are tested; the parameters are presented in Table 4.1. The controller schemes are inactive, so the rotor speed and the pitch angle remain the

same for the entire simulations. In Table 4.1, $\bar{U}$ represents the time-averaged inflow velocity and λ is the tip-speed ratio computed with that wind speed, i.e. $\lambda = \Omega R_{tip} / \bar{U}$.

$\bar{U}$ (m/s)	λ	RPM	γ (°)
20	4.0	12.10	17.5
9	7.55	10.30	0

Table 4.1 – NREL-5MW: Operating points.

Again, the simulation domain for the AD approach has to be large enough to limit the effect of the boundaries on the resulting velocities: it is set to $8D \times 9D \times 9D$, and the center of the wind turbine is located at $(3D, 4.5D, 4.5D)$. Only one resolution is tested here, i.e. 16 points per D , leading to a grid of $N_x \times N_y \times N_z = 128 \times 144 \times 144$. The initial VPM domain is set to $8D \times 3D \times 3D$, with the wind turbine located at $3D$ from the inlet. The VPM simulations are performed at a fine resolution, i.e. 128 points per D , leading to an initial grid of $N_x \times N_y \times N_z = 1024 \times 384 \times 384$.

Two versions of the disk are tested: without correction (denoted AD-NC) and with tip-loss correction (denoted AD-C-EV), in order to highlight the effect of the tip-loss correction and to verify the effective upstream velocity estimation.

The turbulence field is generated by the Mann algorithm and added to the mean inflow velocity (the methodology is explained in Chapter 2 for both approaches) and one turbulence intensity (about 8%) is considered for the two operating conditions. In order to compare instantaneous powers and thrusts, it is important that the same turbulent structures impact the wind turbine at the same times in the two codes. Only one turbulence box is thus generated and the resolution of the resulting turbulence field has thus to be adapted according to the approach. The procedure is explained in the next section.

4.3 Generation of the inflow turbulence fluctuations

The turbulence field is generated with the following parameters: a ground roughness length of $y_0 = 2 \times 10^{-2}$ m, a mean wind speed of 9 m/s and a

height above the ground of $y = 90$ m (corresponding to the hub height of the rotor [75]). This last value does not correspond to the height of the resulting turbulence box but, with y_0 and the wind velocity, it determines the integral length scales and the turbulence intensity (TI) of the velocity fluctuations (see [88, 89] for details); the expected TI is here about 8%. The box size is set to $16D \times 3D \times 3D$. We recall that the statistics of the resulting velocity fluctuations do not vary with the height of the box, and, on the whole vertical length, they are typical of those observed in a neutral atmospheric flow with a wind speed of 9 m/s and a height of 90 m, fitted to all three components of the Kaimal spectrum [89].

The Mann algorithm requires a resolution of 2^n points per direction, the initial box grid thus has 256 points in the vertical and spanwise directions; the box is further interpolated or spectrally truncated to adapt to the considered mesh. The resolution in the streamwise direction is imposed to 512 points. As mentioned, this initial box is modified for AD and VPM simulations; the different steps are shown in Fig. 4.1.

For the VPM approach, the turbulent fluctuations field is interpolated on a box with 192 points in the two unbounded directions and the resolution is maintained to 512 points in the streamwise direction. The VPM turbulence box thus has a streamwise resolution that is four times coarser than the one used for the simulations (described in the previous section), while the box resolution is twice coarser in the two lateral directions (compared to the VPM domain); this aims at limiting the memory footprint of the turbulence box. Finally, the box is smoothly clipped along its boundaries (see Fig. 4.1) and the curl of this clipped turbulence velocity field is taken in order to add vorticity fluctuations at the inflow. The resulting velocities in the flow are shown in Fig. 4.1.

For the AD simulations, as the resolution decreases substantially between the initial turbulence box and the simulation, the turbulent field is not interpolated but truncated in the y and z directions in the spectral domain (using a sharp low-pass filter), to reach 48 points (see Fig. 4.1). This represents the same grid spacings as the ones used in the AD simulations (see previous Section). The mesh used in the AD simulations is already coarse, there is thus no need to use an even coarser resolution for the turbulence fluctuations and the storage remains limited. In order to limit the discrepancies between the VPM and the FD4 turbulence boxes, we keep 512 points in the streamwise direction, which is twice finer than the one used in the AD simulations. We

recall that the turbulence fluctuations are imposed at the inlet based on the frozen turbulence hypothesis, and the x-axis is inferred as a time axis with the substitution $\Delta t_{box} = \Delta x_{box} / \overline{U}$, with Δt_{box} and Δx_{box} the time step and the streamwise grid spacing of the turbulence box, respectively. As the CFL of the FD4 simulations has to remain small (less than 0.25, see Chapter 3), Δt_{box} remains larger than the time step used in the AD simulations (even with $\Delta x_{box} = \Delta_x / 2$), which avoids aliasing. As this initial box is smaller than the simulation domain, the turbulent field is only added on a square of $3D \times 3D$ at the inflow, centered around the wind turbine position (thereby reproducing as much as possible the turbulent field effectively entering the VPM simulations). The turbulent box is thus also smoothly clipped at its extremities to avoid sharp transitions between fluctuations and the mean inflow velocity (see Fig. 4.1 for the resulting flow in the AD simulations). A correction is finally added to the streamwise component in each plane to ensure the conservation of the mass flow.

After those steps, even if the main structures are similar in the AD and VPM boxes (vertical slices in the middle of Fig. 4.1) and in the AD and VPM simulations (vertical slices at the bottom of Fig. 4.1), the initial box statistics are slightly changed. The resulting turbulence intensities for the AD and the VPM boxes are presented in Table 4.2 and compared to that of the initial box. The turbulence intensity, TI_u , is here defined as:

$$TI_u = \frac{\langle u'u' \rangle_{xyz}^{1/2}}{\overline{U}} \quad (4.1)$$

where u' denotes the streamwise fluctuations, and $\langle . \rangle_{xyz}$, the spatial average in the 3 directions.

	Mann box	VPM box	FD4 box
$TI_u(\%)$	8.22	8.17	7.96

Table 4.2 – Turbulence intensity due to the streamwise fluctuations, TI_u , for the different turbulence boxes. The values are computed including the clipped points for the VPM and AD simulations.

The turbulence intensities slightly decrease, compared to the initial box. It

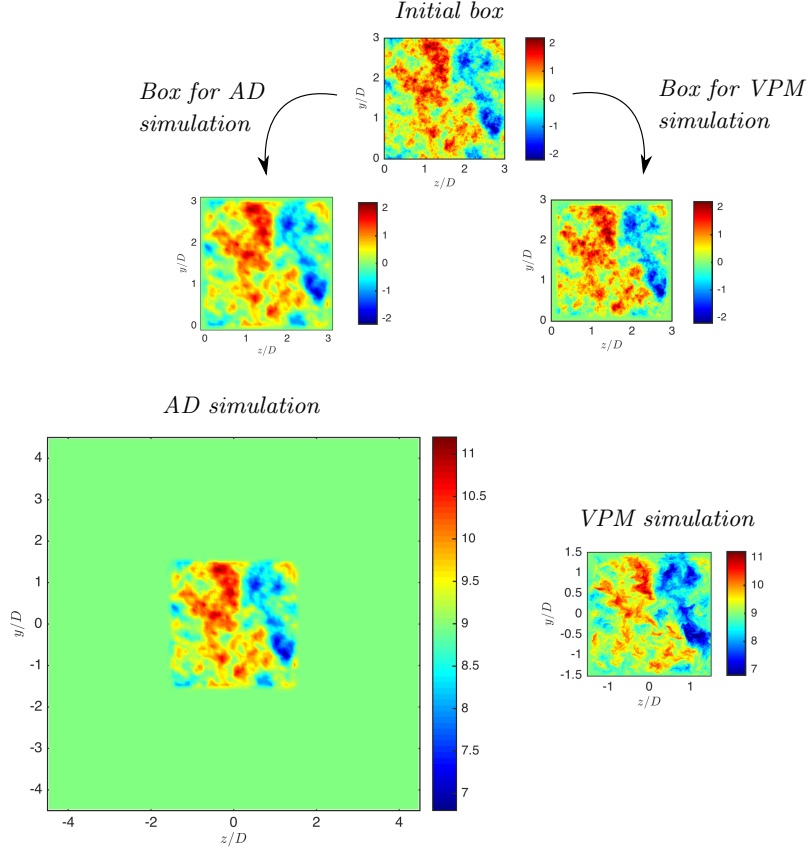


Figure 4.1 – Turbulence box: vertical slice of instantaneous streamwise velocity for the initial box (top), the box for AD and VPM simulations (middle), and the resulting velocity field in the AD and VPM simulations.

is more noticeable for the box aimed at AD simulations, for which the signal is truncated, and that loses some energy slightly.

The codes also handle the turbulent fluctuations in a different manner because of a significant difference in their numerical inflow boundary condition: the FD4 code uses a Dirichlet condition on the velocity components whereas the VPM approach sets the streamwise velocity to U_∞ (and the transverse components are free) and feeds in the vorticity field that corresponds to the desired turbulent fluctuations. Differences are thus to be expected in a region close to the inflow plane. This can be highlighted by comparing the resulting

TI at different locations in simulations without wind turbine. The turbulence intensity is here defined as:

$$TI_u(x) = \frac{\langle \overline{u'u'} \rangle_{yz}^{1/2}}{U} \quad (4.2)$$

where overline denotes the temporal average, and $\overline{u'u'} = \overline{uu} - \bar{u}\bar{u}$. Here, the values are averaged over one Mann box period, T_{Mann} ($T_{\text{Mann}} = 16D/\bar{U} = 224.0$ s).

	VPM			FD4		
	1D	2D	3D	1D	2D	3D
$TI(\%)$	7.60	7.71	7.54	7.83	7.73	7.59

Table 4.3 – TI_u , for the two methods, at different locations from the inlet. The values are computed including the clipped points for the VPM and AD simulations. TI for the FD4 simulation is computed only at the location of the turbulence fluctuations.

We clearly notice an evolution of the statistics. This can be due to different reasons. For both methods, the fluctuations are fed in the inlet according to the Taylor’s frozen turbulence hypothesis (albeit in different manners, see above); this assumption states that the turbulent structures move as frozen entities transported by the mean wind. However, as soon as the fluctuation field is handled by the numerical codes, it gets distorted and there is a redistribution between the three velocity components. For both methods, the box is also clipped, which removes the low wavenumbers and so impacts the statistics. Moreover, for both approaches, the transversal directions are not periodic, while it was initially the case for the Mann box: the VPM approach has unbounded directions while the turbulence is only added at the center of the AD simulations. As a result for the FD4 code, the turbulent field is clipped and modified to preserve the mass flow rate. This in turn causes the pressure Poisson solver to alter the velocity field and ensure a zero divergence in the clipping regions.

Another set of simulations are performed, imposing in that case a mean inflow velocity $\bar{U} = 20$ m/s. A new turbulence box is generated and the resulting TI is again about 8%.

As we are interested in the instantaneous flow field impacting the wind turbine, we also compare the disk-averaged velocity at the rotor position for a

flow without a wind turbine in the two codes (see Figs. 4.2 and 4.3 for $\bar{U} = 9$ and 20 m/s, respectively); the size of the disk corresponds to the size of the NREL-5MW. Globally, the fluctuations are the same, with relatively small discrepancies between the two fields. The rotor in both codes will thus be impacted by a very similar wind; this should allow a meaningful comparison. We also compare the available thrust and power in the wind at the rotor location. Those are defined as:

$$P_{wind} = \frac{1}{2} \rho \int_{S_{disk}} u (\mathbf{u} \cdot \mathbf{u}) dS \quad (4.3)$$

$$T_{wind} = \frac{1}{2} \rho \int_{S_{disk}} u u dS . \quad (4.4)$$

Those values will be useful when power and thrust histories will be studied in Section 4.7. Their behavior is similar to the disk-averaged velocity.

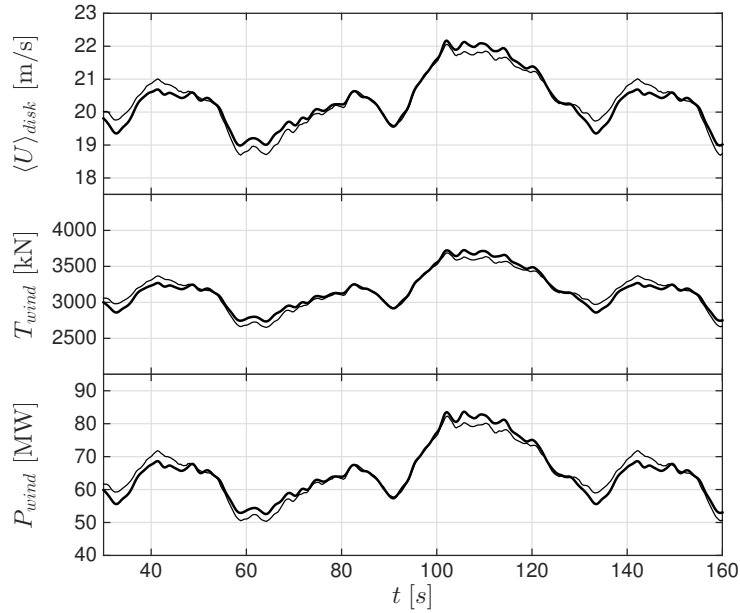


Figure 4.2 – Disk averaged axial velocities, available thrust and power at the rotor location for simulations without wind turbine for $\bar{U} = 20$ m/s in the FD4 code (thick solid line) and the VPM code (thin solid line).

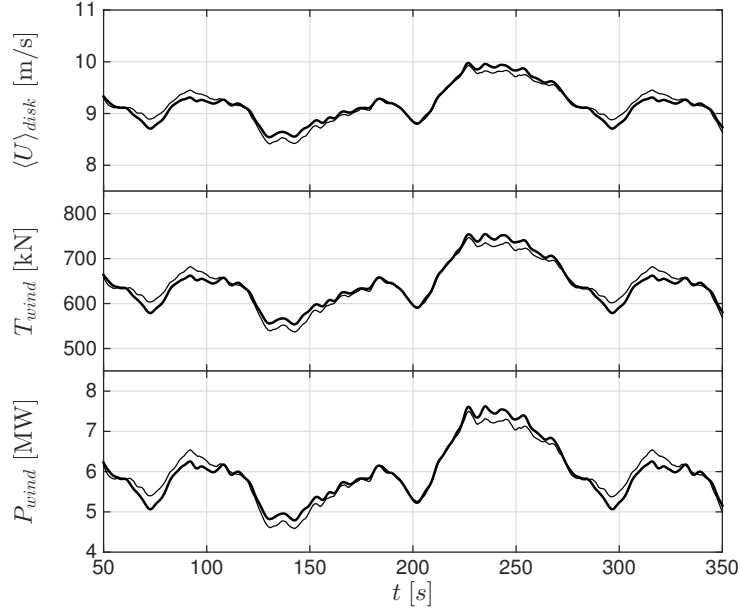


Figure 4.3 – Disk averaged axial velocities, available thrust and power at the rotor location for simulations without wind turbine for $\bar{U} = 9$ m/s in the FD4 code (thick solid line) and the VPM code (thin solid line).

4.4 Instantaneous velocity fields in the rotor vicinity

Figure 4.4 shows slices of the streamwise velocity taken at the same instants in the vertical mid-plane $z = 0$ for the AD-C-EV and VPM results ($z = 0$ corresponding to the axis of the wind turbine). The first remark concerns the difference of resolution: the VPM simulation is able to capture very fine structures, while the AD simulations lose this information. However, the largest structures are globally the same in both codes, allowing us to compare the resulting instantaneous rotor powers and thrusts. In both cases, the two codes produce similar results in terms of the global shape of the wakes and wake meandering. We also notice that the wake presents a different aspect according to the case: for $\lambda = 7.55$ (Fig. 4.4(b)), the velocity deficit is important (compared to the ambient velocity) and it is present along the whole blade, while the blade is only loaded at its inboard part for $\lambda = 4.0$ (Fig. 4.4(a)), with a noticeable velocity deficit only present in the region close to the root. In that case, the wake features are comparable to the ambient turbulence ones. The comparison of the wake statistics between the two methods or cases will be investigated in

detail in Chapter 5.

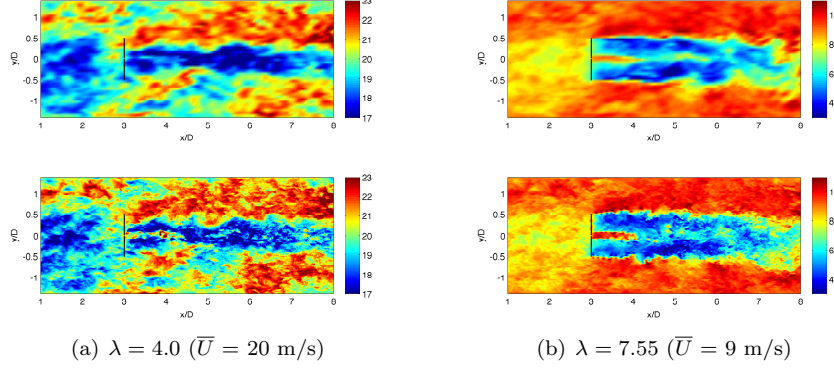


Figure 4.4 – Instantaneous streamwise velocities in the vertical mid-plane $z = 0$ for the AD-C-EV (top) and VPM (bottom) methods. The units are in m/s.

4.5 Estimation of the effective upstream velocity

We here want to verify the computation of the local U_{∞}^{eff} in turbulent flow, used for the tip-loss correction. We have to recall that the 1D momentum theory, on which the estimation of U_{∞}^{eff} is based, is normally valid for a steady flow; this makes the concept of an “upstream velocity” difficult to transpose to turbulent flows. Indeed, the velocity field still evolves between a location far upstream and the wind turbine position. This leaves some ambiguity in the choice of the reference U_{∞} seen by the disk, which we should then compare to the AD-C-EV estimation. A solution consists in comparing the instantaneous estimation of U_{∞}^{eff} with the velocity field at the same location provided by the simulation without wind turbine.

Figures 4.5 and 4.6 show U_{∞}^{eff} for three specific locations on the disk: near the root (after the cylindrical cross-section), in the middle and near the tip of the disk (see Fig. 4.7). The U_{∞}^{eff} value is compared to the streamwise velocity at the same location, provided by the simulations without wind turbine. This reference velocity is denoted U_{∞}^{ref} .

For the case $\lambda = 4.0$, U_{∞}^{eff} is very close to U_{∞}^{ref} . Indeed, that case corresponds to a very low loading case (C_T is about 0.1), and the deviation with the M1D

theory is thus small (as analyzed in details in Chapter 3). However, we observe larger errors in the inboard part of the blade, where the loading is higher.

For the case $\lambda = 7.55$, we notice small discrepancies between the estimation and the velocity field. There is a small time offset between the two curves, noticeable for each probe. Indeed, the considered rotor operating condition corresponds to a relatively high loading. The blocking effect upstream the rotor is thus non-negligible, which results in a slowdown of the flow and a small delay in U_∞^{eff} , compared to U_∞^{ref} . The discrepancies seem also higher for *probe 1*, near the center; this indeed corresponds to an area of potential strong gradients and therefore leads to distortion of the incoming turbulent structures.

An actual objective measurement of the U_∞^{eff} errors is challenging. For example, if we consider the instantaneous U_∞^{eff} and U_∞^{ref} , the time delay observed for the case $\bar{U} = 9$ m/s would bring a constant component to any error measure, e.g. L^2 , L^1 . Evidently this would not produce a fair assessment.

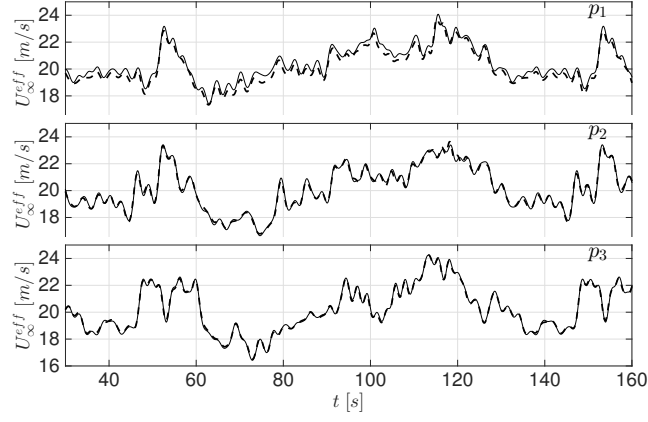


Figure 4.5 – $\lambda = 4.0$ ($\bar{U} = 20$ m/s): U_{∞}^{eff} estimation (solid line), compared to U_{∞}^{ref} (thick dashed-line) for 3 probes.

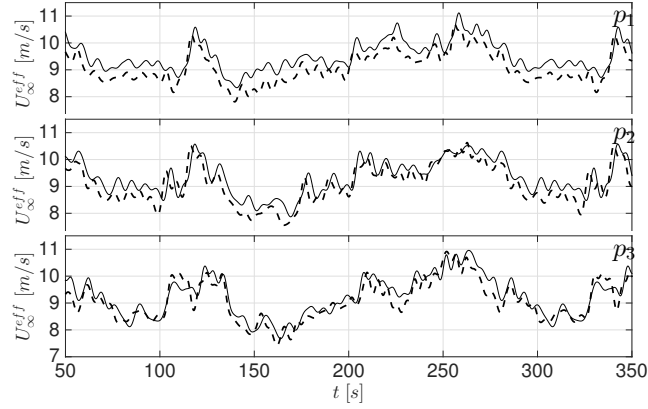


Figure 4.6 – $\lambda = 7.55$ ($\bar{U} = 9$ m/s): U_{∞}^{eff} estimation (solid line), compared to U_{∞}^{ref} (thick dashed-line) for 3 probes.

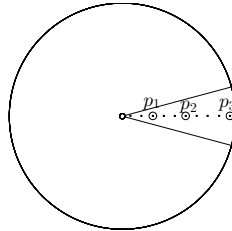


Figure 4.7 – Position of the probes

4.6 Time-averaged blade velocities and forces

Figures 4.8 and 4.9 show the resulting velocities and the aerodynamic forces produced by the two versions of the AD. Those are compared to results obtained from VPM simulations. We here consider the values averaged over a time equal to T_{Mann} . For the disk approach, the velocities and the forces are averaged in the azimuthal direction, while we consider the three-blade average values for the VPM approach. The velocities are corrected with the tip-loss factor for AD-C-EV, while $u_{n,B} = u_n$ and $u_{\theta,B} = u_\theta$ for AD-NC.

As expected, AD-C-EV presents better tip and root behaviors, closer to those produced by a blade-type approach. For $\lambda = 4.0$, the correction appears stronger and impacts a larger part of the blade. This is an expected effect, after the investigations of tip-loss corrections in uniform flows (see Chapter 3).

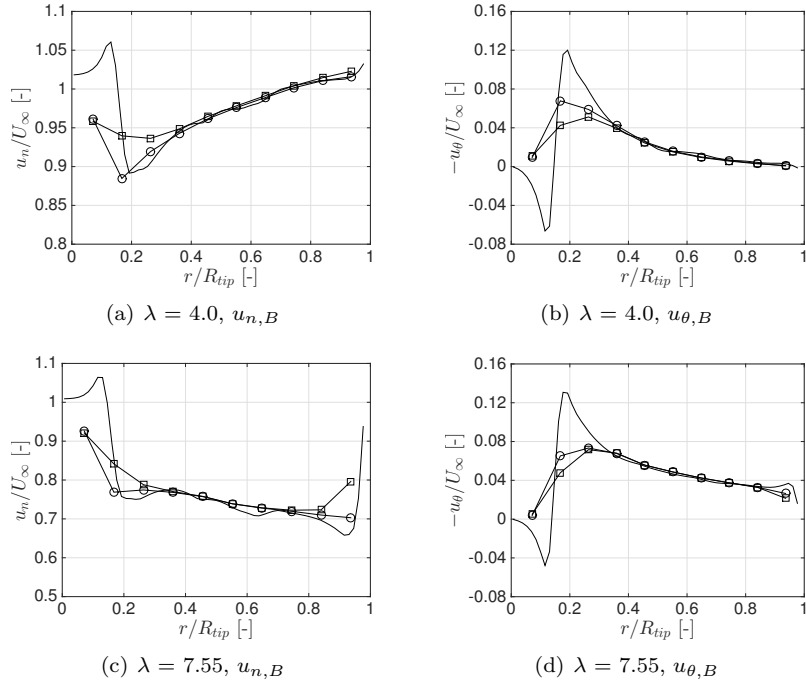


Figure 4.8 – Time averaged blade velocities for VPM (solid line), AD-NC (solid line with squares), AD-C-EV (line with circles). The velocities are normalized by the mean inflow velocity. The VPM results correspond to a three-blade average.

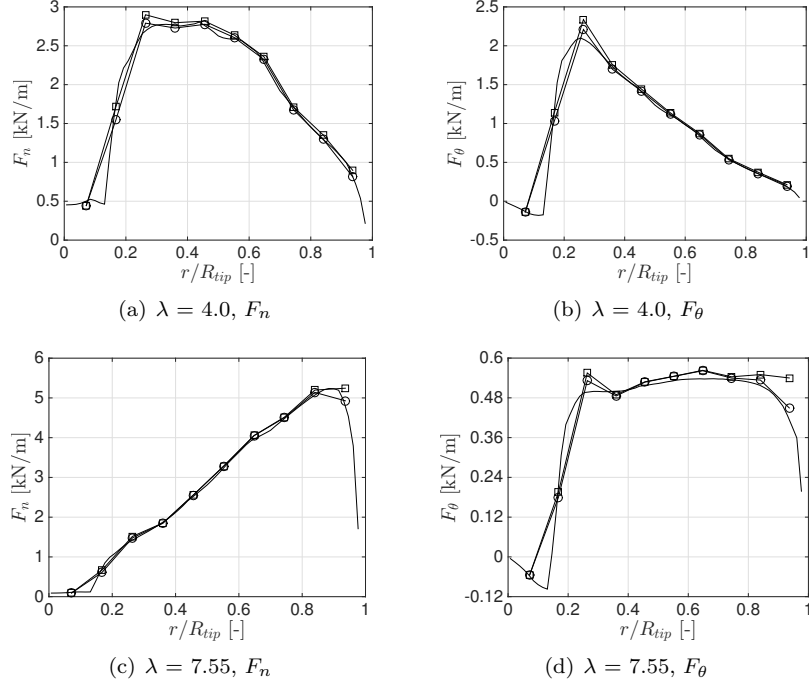


Figure 4.9 – Time averaged forces for VPM (solid line), AD-NC (solid line with squares), and AD-C-EV (line with circles). The VPM results correspond to a three-blade average.

4.7 Power and Thrust

Figures 4.10 and 4.11 present the power P and the thrust T developed by the rotor as a function of time. The power and thrust histories of the VPM results are obtained by filtering the instantaneous values by means of a moving average. The filter width is chosen as $1/3$ of the period of rotation in order to attenuate the oscillations due to the blades rotation and to reproduce the expected AD behavior. The initial transient, different in both methods, is not shown in the figure. We also note that the signals exhibit the much longer periodicity of the turbulent inflow: the Mann box is finite in length ($T_{\text{Mann}} = 16D/\bar{U} = 100.8$ s or $= 224$ s, according to the case) and thus re-fed with that periodicity. A last general remark concerns the power for the lowest TSR; the power signal can, at times, widely exceed the 5.3 MW of mechanical power of the reference machine [75]. Indeed, there is here no control and the rotor speed, as well as the pitch angle, are maintained constant for the whole simulation time. The wind turbine power is thus not rated, as it should be the case in an actual

operation for this averaged wind speed.

The power and thrust still remain higher; that behavior had already been observed in the uniform flow simulations at the same operating conditions.

For both cases, we can see that the AD-C-EV produces lower powers and thrusts than the AD-NC: it is an expected effect after the investigations of the loading in Section 4.6 and the analyses performed in uniform flows (see Chapter 3).

For $\lambda = 4.0$, the AD-C-EV produces values very close to those of VPM. We however observe short episodes where the power and the thrust of VPM are smaller (around $t = 60$ s and $t = 110$ s) or higher (around $t = 140$ s) than those of the AD-C-EV. This seems to correspond to the discrepancies observed between the FD4 and VPM available wind power and thrust (see Fig. 4.2).

In the high TSR case, the AD-C-EV seems to maintain a constant overestimation for both the power and thrust, at the exception of a short episode inside a lull ($t \simeq 70$ s or $t \simeq 300$ s) where it matches the reference. This is due to the lower wind speed, power and thrust observed around the same times in the FD4 code (see Figs. 4.3). The constant overestimation, or the larger errors observed in the case $\lambda = 7.55$ are probably due to the loading distribution on the blade. The loading is high near the blade tip, where the effect of the coarse resolution in the AD simulation has a strong impact: the AD method misses the decrease in F_n and F_t . The last element of the AD, accounting for the blade tips, thus produces torque and thrust contributions which are too large compared to the VPM simulation on a same tip element. Conversely, for $\lambda = 4.0$, the blade loading is concentrated on the inboard part and the impact of the coarse resolution of the AD method is less marked.

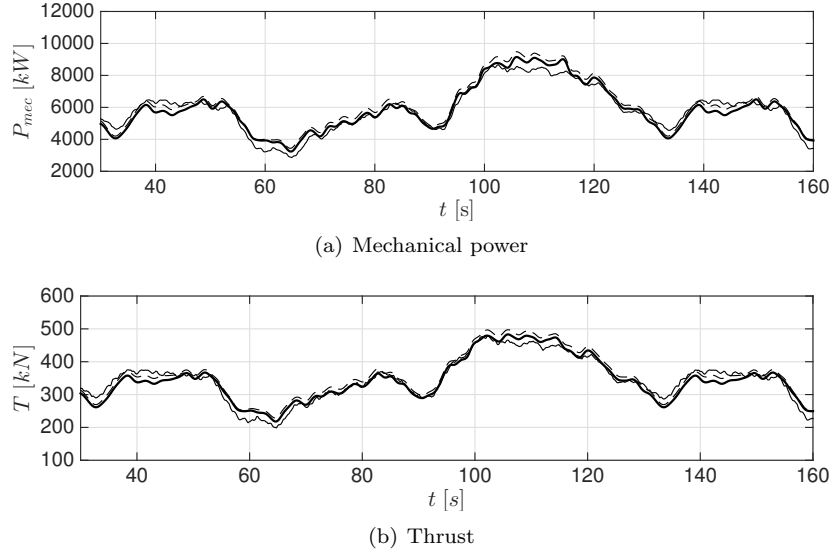


Figure 4.10 – $\lambda = 4.0$ ($\bar{U} = 20$ m/s): Mechanical power and thrust for VPM (fine solid line), AD-C-EV (thick solid line) and AD-NC (thick dashed line).

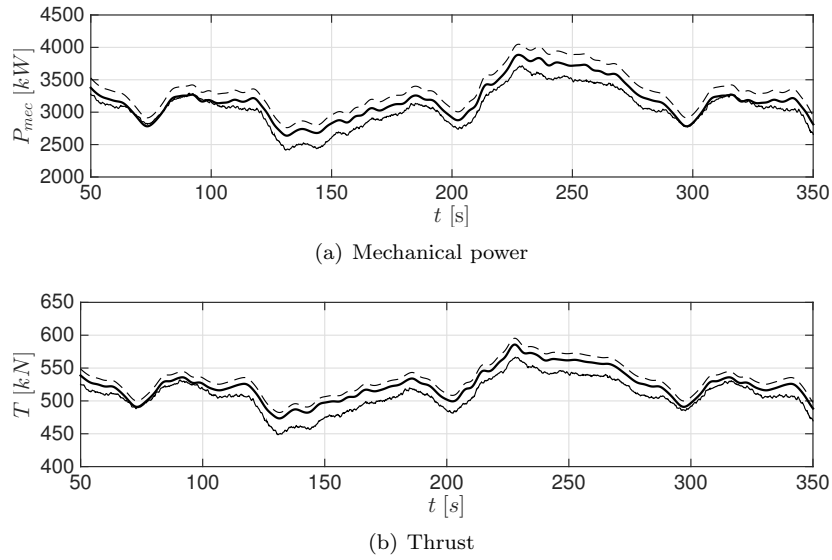


Figure 4.11 – $\lambda = 7.55$ ($\bar{U} = 9$ m/s): Mechanical power and thrust for VPM (fine solid line), AD-C-EV (thick solid line) and AD-NC (thick dashed line).

Table 4.4 shows the percentage difference between the AD-NC, AD-C-EV and VPM approaches in terms of time-averaged power and thrust productions, $\bar{P}$ and $\bar{T}$ (again, we consider that the VPM method produces our reference results). The values are averaged over one period T_{Mann} of the turbulent inflow box (224s or 100.8s). The AD-C-EV produces power and thrust closer to VPM results than the AD-NC: from 0.5 to 5 % of difference for the AD-C-EV and between 4 and 9 % for the AD-NC. It confirms what we observed in Figs. 4.11 and 4.10.

(a) Power			
case	Power (kW)	Power difference (%)	
λ	VPM	AD-NC	AD-C-EV
4.0	5946.8	5.63	1.71
7.55	3062.8	9.37	4.67

(b) Thrust			
case	Thrust (kN)	Thrust difference (%)	
λ	VPM	AD-NC	AD-C-EV
4.0	349.1	3.76	0.45
7.55	509.0	5.08	3.25

Table 4.4 – Time-averaged power and thrust for the VPM simulations and percentage difference between the two considered methods (AD-NC and AD-C-EV) and the VPM approach.

4.8 Unsteady loads

In this section, we investigate the unsteady loads in the AD model and compare them to those captured by the VPM method. We here consider the blade root bending moments due to the aerodynamic forces and excluding gravity loading, i.e., the flapwise and edgewise moments. The flapwise moment, M_f , is caused by the force component aligned with the rotor axis and tends to bend the blades out of the rotor plane; the edgewise moment, M_e , is due to the tangential force component and contributes to the torque generation. They are evaluated for

each blade as

$$M_f = \int_{R_{hub}}^{R_{tip}} (r - R_{hub}) \mathbf{F} \cdot \mathbf{e}_n dr \quad (4.5)$$

$$M_e = \int_{R_{hub}}^{R_{tip}} r \mathbf{F} \cdot \mathbf{e}_\theta dr \quad (4.6)$$

where $\mathbf{F}$ is the aerodynamic blade force per length. For the VPM method, it is trivial to know the instantaneous blade loading, and integrate it along the blade span. For the disk approach, the blades are localised everywhere on the disk surface; a straightforward computation of these moments, averaged over annular regions of the disk, will however produce filtered quantities only remotely connected to the sought information. In order to reproduce pertinent information, we rather propose to replicate the blade trajectories through the disk and recompute blade-attached quantities. We thus let the blades go through the trajectories integrated from the rotor angular velocity history and interpolate the aerodynamic forces on the disk onto these "virtual blades" at each time step. This procedure can be carried out as a post-processing step if the disk aerodynamic forces have been stored. As the larger scale turbulent fluctuations impacting the rotor are very similar in both methods, the blades in the disk approach and in the VPM method should normally be subjected to similar large scales variations in terms of loading.

Only the AD corrected with the tip-loss factor is considered here. In Figs. 4.12 and 4.13, the probability density function (denoted "pdf") of the flapwise and edgewise moments are shown for one blade and for the two operating conditions ($\lambda = 7.55$ and $\lambda = 4.0$). The behavior of the two other blades are assumed to be similar. The results are accumulated over one period T_{Mann} .

We clearly notice that the disk approach leads to results very similar to the VPM approach. However, for the case $\lambda = 7.55$, the flapwise moment presents a mean value higher for the AD approach. It is consistent with the already observed power and thrust which exhibit an offset with those obtained with the VPM method.

With the AD approach in coarser resolutions, we cannot expect to capture the load fluctuations at frequencies as high as those captured by the high resolution VPM simulations. This is highlighted with the power spectra densities (PSD) of M_f and M_e for one blade (see Figures 4.14 and 4.15). The spectra for the VPM and AD-C-EV loads can be seen to agree very well over a broad band

at low frequencies. As expected, the AD-C-EV cannot capture the fluctuations beyond a cutoff frequency in each case.

The good agreement over low frequencies hints at a potential extrapolation of the AD-C-EV spectral behavior to high frequencies: this would indeed allow to recover fatigue-relevant information such as equivalent loads, etc.

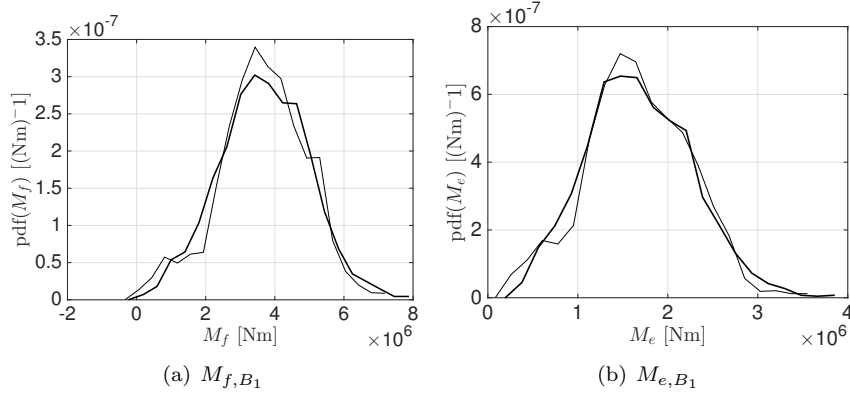


Figure 4.12 – $\lambda = 4.0$ ($\bar{U} = 20$ m/s): PDF of the flapwise and edgewise moments for AD-C-EV (thick solid line) and VPM (thin solid line).

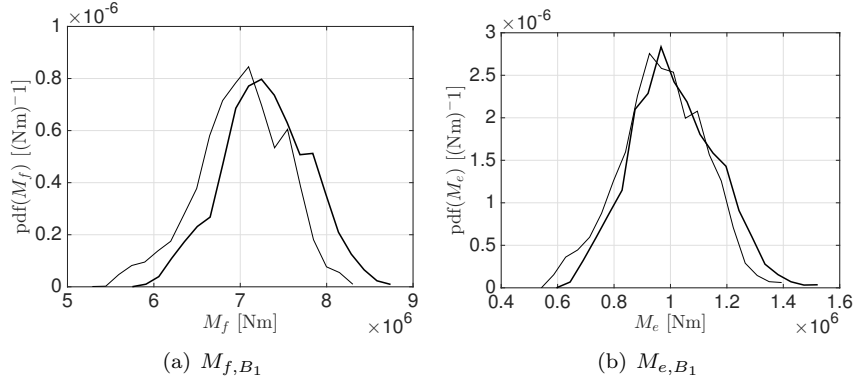


Figure 4.13 – $\lambda = 7.55$ ($\bar{U} = 9$ m/s): PDF of the flapwise and edgewise moments for AD-C-EV (thick solid line) and VPM (thin solid line).

4.9 Conclusions

Simulations performed for turbulent inflows have been used to verify the AD behavior in more complex conditions, which entail unsteady and non-uniform

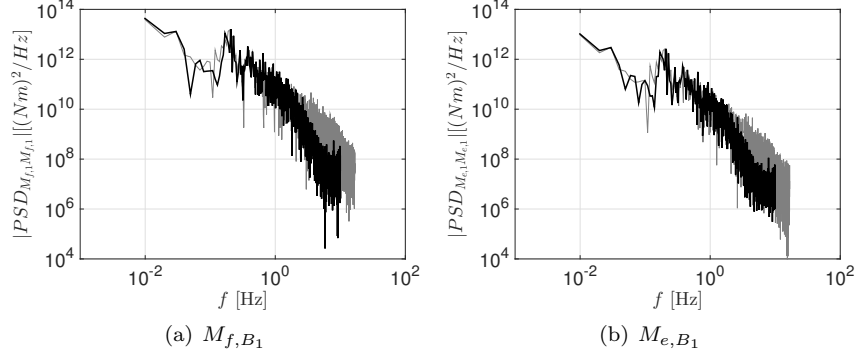


Figure 4.14 – $\lambda = 4.0$ ($\bar{U} = 20$ m/s): PSD of the flapwise and edgewise moments for AD-C-EV (black solid line) and VPM (gray solid line).

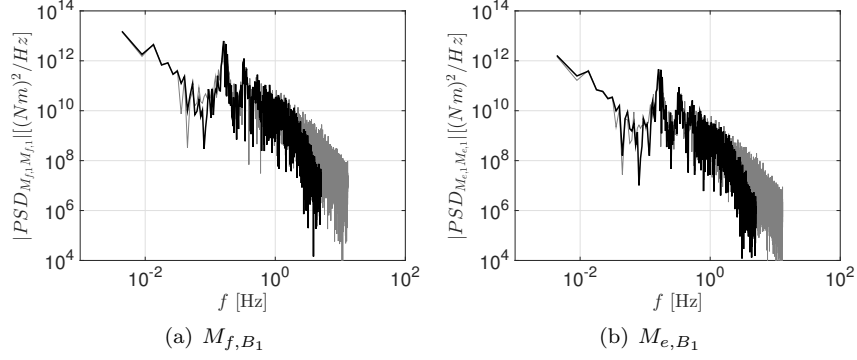


Figure 4.15 – $\lambda = 7.55$ ($\bar{U} = 9$ m/s): PSD of the flapwise and edgewise moments for AD-C-EV (black solid line) and VPM (gray solid line).

velocities across the AD. The tip-loss procedure, developed in Chapter 3, was first verified by comparing the velocity estimated based on the One-Dimensional Momentum (M1D) theory and that recovered at the same location in a simulation without the wind turbine. The errors appeared small; their quantification was challenging because of the modification of the velocity fluctuations inherent to the presence of the wind turbine (blockage). The resulting velocities and aerodynamic forces were compared to those obtained with the VPM approach, highlighting the improvement obtained with the tip-loss factor. The power and thrust histories, as well as the power and thrust coefficients, were also compared to VPM results: the resulting time-averaged errors did not exceed 5%. First investigations of the root bending moments were made; artificial blades were included in the disk, that replicated the blade trajectories of the

VPM approach, and blade-attached quantities were recomputed. This clearly highlighted a good agreement over low frequencies and showed a potential extrapolation of the AD-C-EV spectral behavior to high frequencies, which could allow to recover fatigue-relevant information.

Chapter 5

Investigation of wake statistics

In the previous two chapters, the developed Actuator Disk model has been verified in terms of aerodynamic data for uniform flow velocities and more complex flows including turbulent fluctuations. The AD approach improved with a tip-loss correction has also been shown to produce an accurate behavior, even at a coarse resolution. However, as this wind turbine model is aimed at large wind farm simulations, it is important that this approach also produces accurate wake statistics at a certain downstream location, where another machine can potentially be located. This chapter aims at comparing the velocity deficit and the level of turbulence in the AD wake with those obtained with the Vortex particle-mesh method coupled to immersed lifting lines. This last approach is used on fine resolution. The first investigations consider simulations in uniform flows, highlighting the contrast between the wake behavior behind an AD and behind an approach with discrete blades; this difference is particularly important for AD simulations in coarse resolutions. In the second step, turbulent fluctuations are added to the mean inflow; several turbulence intensities are investigated for three main operating conditions of the NREL-5MW wind turbine.

5.1 Introduction

Modern wind turbines are often located and clustered in wind farms; the rotors are thus influenced by the wakes shed by the upstream wind turbines. As a consequence, the power production often decreases for the wind turbines located at the second and following rows. The downstream rotors are also subjected to a higher level of turbulence, which may increase the dynamic loading and intensify the fatigue loads on the structural parts. It is thus primordial that the wind turbine models used in wind farm simulations (i.e, the Actuator Disk (AD) approaches, for most cases) are able to produce wake statistics close to those produced by real wind turbines.

The physics of the wake and its destabilization are different between an AD method and a more accurate representation of the rotor, with models that account for the blade rotating in the flow, such as Actuator Line (AL) or Lifting Line (LL) approaches. For an actual rotor, distinct vortex sheets are shed by the blades and roll up to form concentrated tip and hub vortices and their interactions contribute to destabilize the wake. The location at which the onset of the instabilities occurs depends on the operating conditions of the rotor and the ambient turbulence. Numerical and experimental studies about the wake stability can be found in [24, 68, 83, 84, 85, 113, 123]. An AD method is unable to capture the discrete vortices shed by the blades, as its wake is a continuous cylindrical vortex sheet. The wake instabilities are thus those observed in a shear flow, due to the velocity difference between the wake and the outer flow. Again, the onset of the instabilities will depend on the operating conditions of the rotor, which influence the thickness of the shear layer, and on the ambient turbulence that may contribute to destabilize the flow (accounting for swirl can also influence the wake physics [107]).

The AD approach is thus unable to capture the near-wake and its turbulence level, and this incorrect estimation has repercussions on the mixing process across the wake and on the rate at which the wake recovers momentum from the ambient flow [83, 84, 115]. In turbulent flows, however, the AD model appears to produce acceptable wake statistics from five rotor diameters downstream of the wind turbine; this has been shown in several works. Wu and Porté-Agel [144] and Porté-Agel *et al.* [106] compared the wake statistics behind a drag disk, a rotating disk and actuator lines in coarse resolution with wind tunnel measurements. The comparison showed a good agreement between the rotating disk and the AL approach at several diameters downstream of the

rotor. In the work of Stevens *et al.* [127], the same wind turbine and wind farm measurements were used to study the influence of the nacelle and tower representation on the resulting wakes. Again, an AD method (here, only a drag disk) was shown to agree relatively well with an AL approach for the far-wake. The simulations mentioned above are performed with relatively coarse resolutions, where an AL method cannot hold significant advantages over the AD approach (an accurate near wake representation,...); the AL method even presents some unexpected behavior, such as a lower turbulence kinetic energy production in the near wake than that of the drag disk method [127]. Conversely, comparisons have been performed at finer resolutions, for simulations of the Lillgrund wind farm [93]; the wakes were not analyzed in details, but the power production was compared for the two methods and the measurements, showing a relatively good agreement, while the considered separation between the machines was relatively small (about five rotor diameters).

The comparisons have often been made at an equivalent resolution for both AD and AL approaches, even though the methods are normally aimed at different applications. The AD model is expected to be used for wind farm studies at a coarse resolution, while simulations with higher fidelity models, such AL or LL approaches, are often performed at finer resolutions for an accurate near-wake representation. This chapter aims at verifying if the developed disk approach, working at a coarse resolution, presents wake statistics close to those produced with the Vortex Particle-Mesh method coupled to immersed lifting lines, working at fine resolution. This last approach accounts for a finite number of blades, and the code exhibits little dispersion and dissipation; it is thus well-suited to produce high accuracy results of this turbulent flow that we will treat as our reference. This work does not have the ambition to investigate how the two approaches recover momentum from the outside flow, but to verify the velocity deficit and the level of turbulence in the wake at downstream locations where another rotor may be located.

The first section (Section 5.2) handles the difference of the wake physics of an AD and a discrete blade type approach. Simulations with uniform flows are considered and both coarse and fine resolutions are used for the AD approach. A new set of simulations is performed, for three operating conditions of one rotor, the NREL-5MW wind turbine. For each operating condition, three turbulence intensities (TI) are investigated. The resulting streamwise velocities and turbulent kinetic energy (TKE) are thus investigated at several locations

downstream of the rotor. The set-up, as well as the results, are presented in Section 5.3.

5.2 Wake topology

5.2.1 Discrete blade type approach

For the Lifting line (LL) approach (or an equivalent method, such as an Actuator Line (AL) method), the wake behind each blade consists in a continuous sheet of trailing vorticity due to the spanwise variation in the bound circulation. These vortex sheets roll up and form concentrated vortices at the root and tip of the blade. The instantaneous flow structures in the wake are clearly visualized in Fig. 5.1 through a volume rendering of the vorticity magnitude (this result was obtained with a VPM simulation [35] similar to those performed in this thesis. In most situations, this vortex system becomes unstable and breaks down at a certain distance downstream of the machine, losing its initial coherence and its periodicity: this is the transition from the near wake to the far wake. In Fig. 5.1, the ambient turbulence of the wind contributes to trigger vortical instabilities and reconnections. In purely uniform flows, the wake behavior and its destabilisation solely depends on the operating condition. This is highlighted in Figure 5.2, where instantaneous vertical slices of the transverse component of the vorticity (ω_z) are shown for three operating conditions of the NREL-5MW wind turbine (see Chapter 3 for details of these simulations). Vertical slices of the time-averaged streamwise velocity and the turbulent kinetic energy (TKE) are also presented and are shown in Fig. 5.3. The TKE is defined as:

$$TKE = \frac{1}{2} (\overline{u'u'} + \overline{v'v'} + \overline{w'w'}) \quad (5.1)$$

where overline denotes the time-averaging operator. The velocity and the TKE are normalized by the upstream velocity U_∞ and U_∞^2 , respectively. These values will translate the presence of the vortical structures and their destabilisation and reconnections on the resulting velocity deficit and level of turbulence in the wake. These results are briefly described in the next paragraphs.

$\lambda = 7.55$ and $C_T \simeq 0.8$ This depicts very well the vortex pairing phenomenon. Small perturbations of the initially evenly distributed vortices are

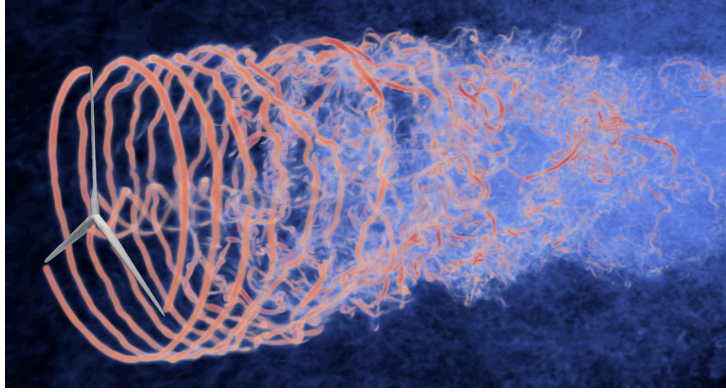


Figure 5.1 – Volume rendering of the vorticity magnitude in the wake [35]. The lifting lines are visualized as three-dimensional blades, taking into account the airfoil, the chord, and twist distributions.

amplified in a way that the neighbouring vortices get closer and start to group in pairs [24, 68, 82]. Initial perturbations can here be due to the non-negligible wake expansion, that contributes to slow down the tip vortices and to decrease the distance between them and the following ones. In the considered simulations, the vortices do not completely rotate around each other, as has been observed in [85] or in [68]; it is due to the relatively short distance between the vortices, and they thus rapidly merge together. Near the root, the vortices are shed in a more streamwise direction (this is clearly visible in the three-dimensional view presented in Fig. 5.1) and they thus appear more spread out in the vertical slice. The vortex cores are located closer together and they coalesce quickly downstream of the rotor. In an actual wind turbine, they potentially interact with the nacelle and its wake, which may promote a faster break-down of the vortex system at this location [66, 114].

The concentrated tip vortices in the near wake contribute to an increase of turbulence in the flow but do not take part in the mixing and the mean-flow kinetic energy entrainment, as demonstrated by Lignarolo *et al.* [85]: they investigated experimentally the evolution of vortex structures behind a small rotor and showed that the tip vortices in their regular configuration inhibited the mixing of the wake during its expansion. This is highlighted by observing the velocity deficit and the turbulent kinetic energy in this region (see region between $x/D = 0$ and $x/D = 1$ in Figs. 5.3(c) and 5.3(d)): there is a net increase of turbulence in the near wake, but the velocity deficit remains more or less constant. The onset of pairing instabilities around $x/D = 1$ (Fig. 5.2(b)) causes

a sudden increase of turbulence and generates an efficient wake re-energising by the action of random flow motions [85]: the production of turbulence increases and the velocity deficit becomes lower. Near the root, the behavior is slightly different, as the vortices are rapidly coalescing together. The net increase of turbulence and the wake mixing appear when the nearly continuous sheet starts to oscillate (around $x/D = 2$).

$\lambda = 5.7$ **and** $C_T \simeq 0.3$ For low tip speed ratio and low loading (Fig. 5.2(a)), the distance between two vortex sheets is larger, and the vortices circulations are weaker. The wake expansion is also negligible. This leads to a much decreased growth rate for the instabilities, and vortex pairing does not appear in the considered domain length. Near the root, again, as the vortices are shed in a more streamwise direction, the distance between each other are shorter; small perturbations thus lead to vortex pairing instabilities. As a consequence, a net increase of turbulence is observed from $x/D = 4$ in the root region (see Fig. 5.2(a)), with an increase in velocity at this location (see Fig. 5.3(b)).

$\lambda = 10.24$ **and** $C_T \simeq 0.95$ For high tip speed ratios (see Fig. 5.2(c)), the shed tip vortices are very close to each other, and interact at a shorter distance downstream of the machine. This effect is reinforced due to the high loading: the subsequent wake expansion slows down the vortex sheets markedly and those finally coalesce together. Vortex pairing is here not observed; it is difficult to affirm that this will never appear at this operating condition as finer resolution might help in better resolving the vortices and change the interactions between them. Still, at the considered resolution, slightly downstream of the rotor, the wake is similar to a continuous vortex, and the observed instabilities are more similar to those produced by an AD approach. The net increase of turbulence appears when the vortex sheet starts to oscillate, with a consequent re-energising of the wake.

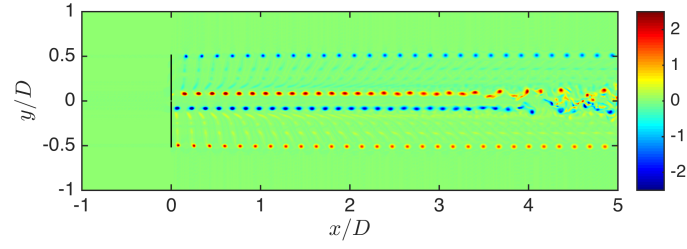
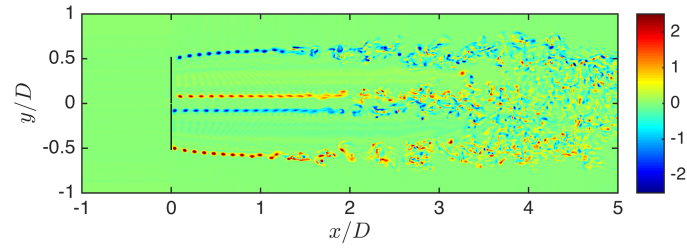
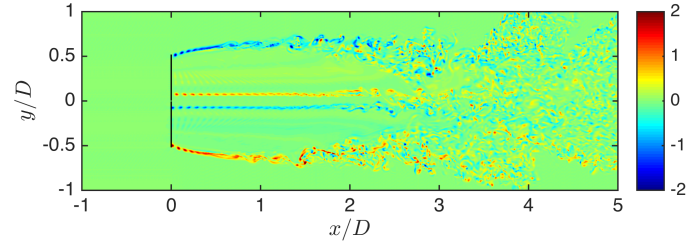
(a) $\lambda = 5.7$ and $C_T \simeq 0.3$ (b) $\lambda = 7.55$ and $C_T \simeq 0.8$ (c) $\lambda = 10.24$ and $C_T \simeq 0.95$

Figure 5.2 – Vertical slices of the transverse vorticity component, ω_z for different operating conditions of the NREL-5MW wind turbine for VPM simulations with 128 points per diameter. The units are in $1/s$.

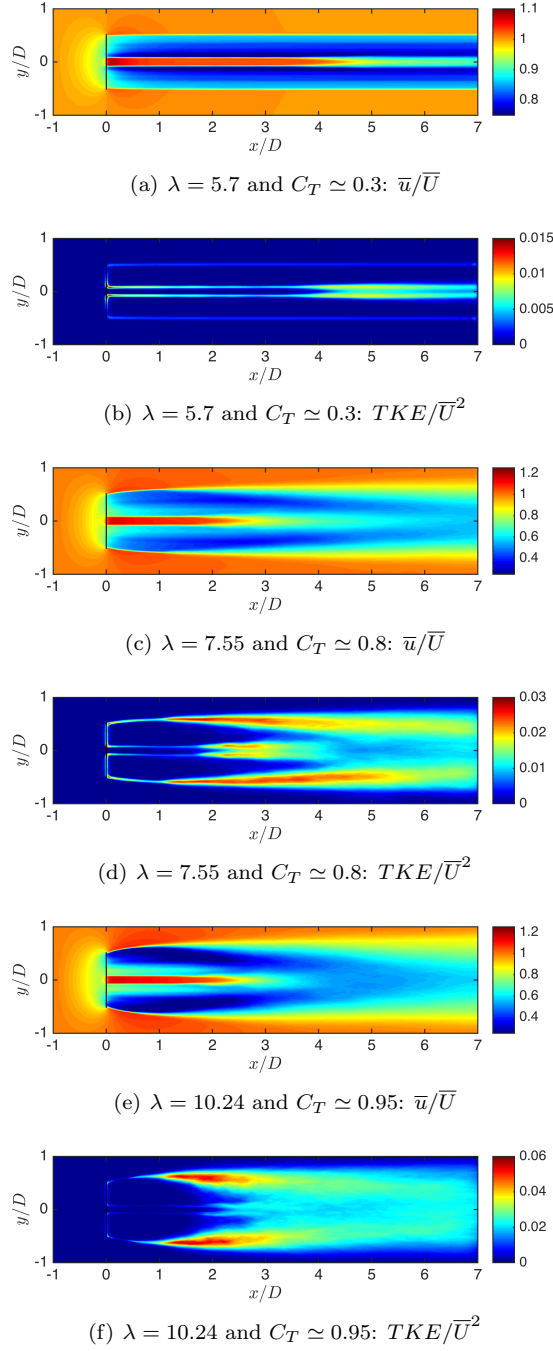


Figure 5.3 – Vertical slices of the time-averaged velocity and TKE, normalized by $\bar{U}$ and $\bar{U}^2$ respectively, for VPM simulations (128 points per D). Three different operating conditions are presented

5.2.2 AD approach

The disk approach is unable to capture all these phenomena, as its wake is a continuous cylindrical vortex sheet. This is highlighted in Figure 5.4 that shows instantaneous vertical slices of the transverse component of the vorticity (ω_z) for the same three operating conditions of the NREL-5MW wind turbine. There is here no turbulence at the inflow and the AD has 64 points per D (see Chapter 3 for details of these simulations).

The wake destabilization is mainly due to the strong velocity gradients at the wake edges and the growth of Kelvin-Helmholtz like instabilities. The shear layer becomes unstable and starts to oscillate (see Fig. 5.4(b), from $x/D = 3$). For the highest loading (Fig. 5.4(c)), this roll-up leads to the development of distinct vortices that interact between each others, which may lead to vortex pairing instabilities. For a low loading, as in Fig. 5.4(a), as the shear is relatively weak, the wake remains stable through the domain length. Instabilities can be here reinforced by swirl within the wake (our disk approach accounts for tangential forces, and thus produces swirl); comparison between wakes downstream drag-only disks and rotating disks have not been investigated in this study, but research works have shown that the wake statistics behind disks with rotation are closer to those produced by more accurate approaches or measurements [106, 144].

Globally, for the AD simulations, the wake destabilisation requires a longer development region as the Kelvin-Helmholtz instabilities of a wake sheet grow more slowly than the pairing of already distinct vortices. The differences in resolution and code can also contribute to that effect. As a consequence, the wakes differ significantly between the two methods and the wake re-energising process thus appears at a very far downstream distance.

5.2.3 Effect of the resolution

The AD approach is usually used at coarse resolutions; this further degrades the performances of the method to capture accurate far wake statistics. Figure 5.5 shows the transverse vorticity behind a disk at a resolution of 16 points per D: the velocity transition between the wake and the outer flow is smeared significantly and instabilities are not noticeable, on the contrary of the results of Fig. 5.4(b). As a consequence, the resulting time-averaged velocity deficit and turbulence kinetic energy for a disk at such a resolution differ from the VPM results considerably. Figure 5.6 shows these wake statistics for the AD in coarse

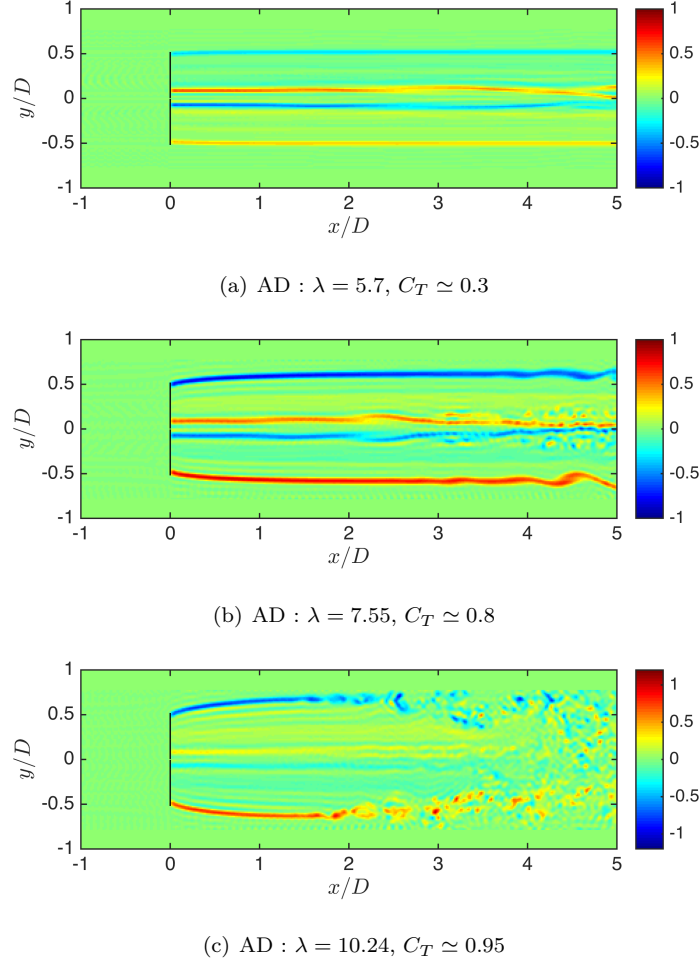


Figure 5.4 – Vertical slices of the spanwise vorticity component, ω_z for different operating conditions of the NREL-5MW wind turbine (λ is the tip-speed), for AD simulations with 64 points per diameter. The units are in 1/s.

resolution (16 points per D) for the three operating conditions (again, details of these simulations can be found in Chapter 3). The wake remains stable over large distances, and the level of turbulence does not sensibly increase within the considered domain. Only the highest loading leads to a small increase of turbulence and re-energising of the flow, the velocity deficit becomes lower beyond four rotor diameters behind the wind turbine.

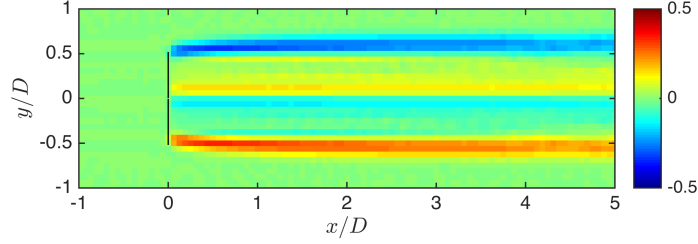


Figure 5.5 – Vertical slice of ω_z for one operating condition of the NREL-5MW wind turbine ($\lambda = 7.55$ and $C_T \simeq 0.8$), for an AD simulation with 16 points per diameter (see Chapter 3, for details of this simulation).

5.2.4 Effect of the ambient turbulence

A perfectly uniform wind never occurs however, and there is always turbulence in the flow impacting the rotor. In a discrete blade type approach, this contributes to destabilize the vortex system [68, 113]: according to the level of perturbations, the tip vortices start to oscillate and pair at a shorter distance, and there are additional interactions between the wake and the ambient turbulence eddies. Fig. 5.7(a) highlights this for one operating condition of the NREL-5MW wind turbine and with a turbulent inflow of about 7% (see Chapter 4 for details of the simulation). The ambient turbulence also impacts the wake destabilisation of the AD at a coarse resolution (see Fig. 5.7(b), see Chapter 4 for details of the simulation) and helps the AD method in capturing more accurately the wake statistics. This is highlighted with a vertical slice of the resulting time-averaged streamwise velocity and TKE in the wake, for both methods, shown in Fig. 5.8. While, in a uniform flow, the wake statistics obtained with the AD method in coarse resolution strongly differ from those computed with the VPM approach, the addition of turbulence fluctuations in the wind clearly helps to reduce the discrepancies between the two methods. The velocity deficit of the two methods are comparable, and, although the TKE increase starts at a further downstream location for the AD method, the TKE levels are similar for the two methods. We also note that the wake is not symmetrical for the two methods; this is due to the Mann turbulent field and will be discussed in Section 5.3.2.

Finally, the snapshots of Fig. 5.7 show that the AD vorticity can be seen as a smoother version of the VPM simulation: the large scale perturbations are thus globally preserved in the wake.

These results are only available for a relatively short distance downstream

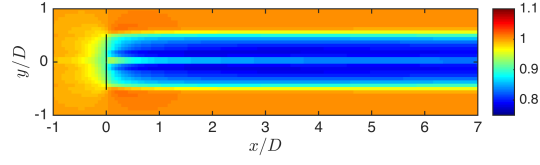
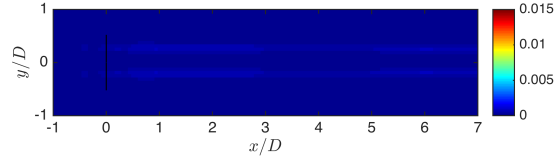
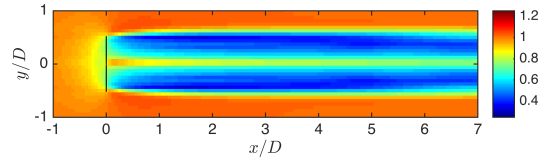
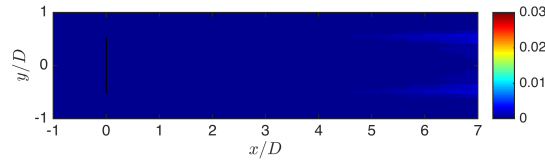
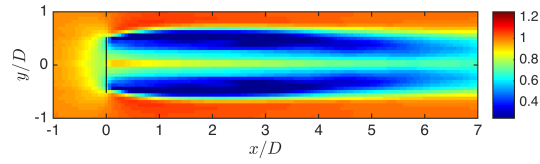
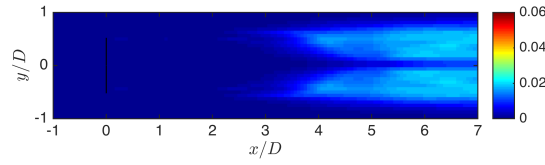
(a) $\lambda = 5.7$ and $C_T \simeq 0.3$: $\bar{u}/\bar{U}$ (b) $\lambda = 5.7$ and $C_T \simeq 0.3$: $TKE/\bar{U}^2$ (c) $\lambda = 7.55$ and $C_T \simeq 0.8$: $\bar{u}/\bar{U}$ (d) $\lambda = 7.55$ and $C_T \simeq 0.8$: $TKE/\bar{U}^2$ (e) $\lambda = 10.24$ and $C_T \simeq 0.95$: $\bar{u}/\bar{U}$ (f) $\lambda = 10.24$ and $C_T \simeq 0.95$: $TKE/\bar{U}^2$

Figure 5.6 – Vertical slices of the time-averaged velocity and TKE, normalized by $\bar{U}$ and $\bar{U}^2$ respectively, for an AD (16 points per D).

of the machine, and concerns only one rotor operation point and one turbulence intensity in the wind. The next section aims at investigating the wake behavior for a longer downstream length and more wind cases.

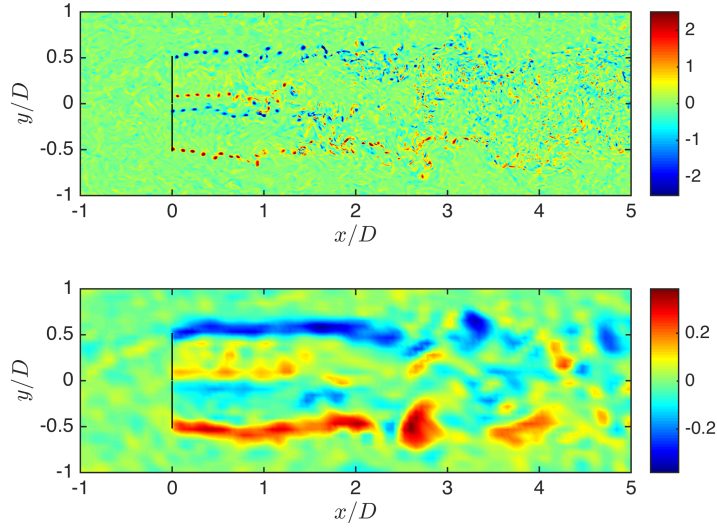


Figure 5.7 – Vertical slices of ω_z in the wake of the NREL-5MW wind turbine for the VPM method with 128 points per D (top) and an AD with 16 points per D (bottom). The mean inflow velocity is 9 m/s ($\lambda = 7.55$ and $C_T \simeq 0.8$) and the ambient turbulence intensity is about 7 %. The units are in $1/s$.

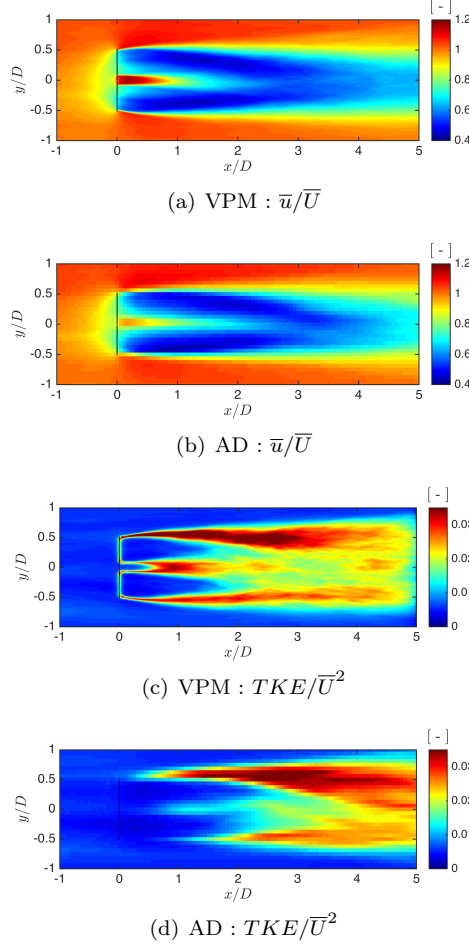


Figure 5.8 – Vertical slices of time-averaged streamwise velocity and TKE production, normalized by $\bar{U}$ and $\bar{U}^2$ respectively. The results are shown for the VPM (top of the subfigure) and AD (bottom of the subfigure) approaches. The mean inflow velocity is 9 m/s ($\lambda = 7.55$ and $C_T \simeq 0.8$) and the ambient turbulence intensity is about 7 %.

5.3 Numerical simulations in turbulent winds

We here investigate the wake statistics behind the AD model for several wind conditions and turbulence intensities and compare them to those produced by the VPM method coupled to immersed lifting-lines. Again, we consider the NREL 5MW and the operating points and time-averaged wind conditions are those previously determined for simulations in uniform flows used in Chapter 3. We recall the parameters in Table 5.1 (here, $\bar{U}$ is the time-averaged velocity).

The disk controller is inactive, so the rotor speed and the pitch angle remain constant for the whole simulation. The AD method is corrected with the tip-loss factor based on effective upstream velocity. The SGS model for AD simulations is the multiscale variant of the Smagorinsky model, the RVMs-Smagorinsky, with $C_s = 0.066$.

$\bar{U}$ (m/s)	λ	RPM	γ (°)
14	5.7	12.10	8.7
9	7.55	10.30	0
5	10.24	7.76	0

Table 5.1 – *NREL-5MW: Operating points.*

We only perform AD simulations at a coarse resolution, i.e. 16 points per D. The domain is set to $14D \times 9D \times 9D$, in order to have a sufficient distance for investigating wake statistics downstream of the machine. The VPM initial size is set to $14D \times 4D \times 4D$. In order to save computational costs, the resolution is coarsened to 64 points per D. The impacts of such a resolution change in terms of wake statistics for the VPM simulations are investigated in Section 5.3.1. The rotor is located at $3D$ from the inlet for both methods, and at the center of the domain for the other two directions.

For each wind condition, three turbulence intensities are tested: 6 %, 10 % and 15 %. The turbulent field is generated with the Mann tool, as described in Chapter 2, and added to the mean inflow velocity. The initial size of the turbulence box, the parameters used for the Mann algorithm, as well as the resulting turbulence intensities are presented in Table 5.2. All the parameters are expressed in the FD4 coordinate system.

The box is spectrally truncated for the AD simulations, as the resolution is decreased from 256 points to 64 points in the y and z directions; the streamwise resolution is decreased to 768 points, the same as used in the VPM simulations. Again, the turbulent fluctuations are only added on a square of $4D \times 4D$ at the inflow, centred at the (y, z) position of the wind turbine, and the box is clipped. For the VPM code, the spanwise and vertical resolutions remain the same, while the streamwise direction is interpolated to 768 points, and the box is smoothly clipped along its edges. These operations change the parameters of the boxes, and can slightly impact the fluctuations and the resulting turbulence

	Mann's algorithm parameters					
	$\simeq 6\%$		$\simeq 10\%$		$\simeq 15\%$	
	y_0 [m]	TI_u [%]	y_0 [m]	TI_u [%]	y_0 [m]	TI_u [%]
14 m/s	0.0005	6.2	0.0873	10.8	0.7063	15.5
9 m/s	0.0005	6.0	0.0873	10.5	0.7063	15.0
5 m/s	0.0012	6.0	0.1085	10.0	1.0197	15.0

Table 5.2 – Parameters of the Mann turbulent boxes for the different wind conditions and turbulence intensities. The domain is equal to $24D \times 4D \times 4D$ with $1024 \times 256 \times 256$ points. The boxes are generated at hub height (90 m for the NREL-5WM)

intensities.

5.3.1 Resolution effects on the wake statistics for the VPM method

The impact of the above-mentioned coarsening of the VPM simulations resolution is verified in terms of the resulting wake behavior and statistics. The wind speed is here equal to 9 m/s (see Table 5.1 for the wind turbine parameters at this wind speed) and the turbulence intensity is about 7 %. The results in fine resolutions are those recovered from the simulations performed for tip-loss investigations (see Chapter 4 for details of the simulation, as the well the parameters of the turbulence field), the wake statistics are thus only available up to five diameters downstream of the rotor. The simulation at a coarser resolution uses exactly the same generated Mann turbulence field.

Figure 5.9 shows a vertical slice of the transverse component of the vorticity for the coarser resolution. We clearly notice that this simulation presents less resolved vortices and those quickly coalesce behind the rotor to form a nearly continuous vortex sheet, which starts to oscillate from $x/D \simeq 1$ downstream of the rotor. For the finest resolution, as previously observed, the vortices pair together from $x/D \simeq 0.5$ (see Fig. 5.7(a)), with a resulting increase of mean kinetic flux entrainment and turbulence production that contributes to mixing the wake. The wake physics and its re-mixing process seem different, but the resulting time-averaged axial velocity profiles are very close to each other (see Fig. 5.10(a)). Meanwhile, the turbulent kinetic energy profiles exhibit differences albeit small ones. Using the VPM method with 32 points per blade, should therefore lead to a still meaningful comparison.

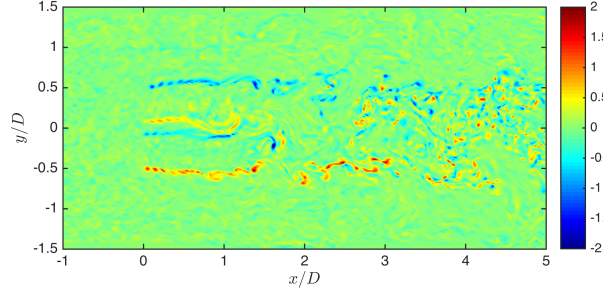


Figure 5.9 – Vertical slice of ω_z in the wake of the NREL-5MW wind turbine for the VPM method with 64 points per D . The units are in $1/s$.

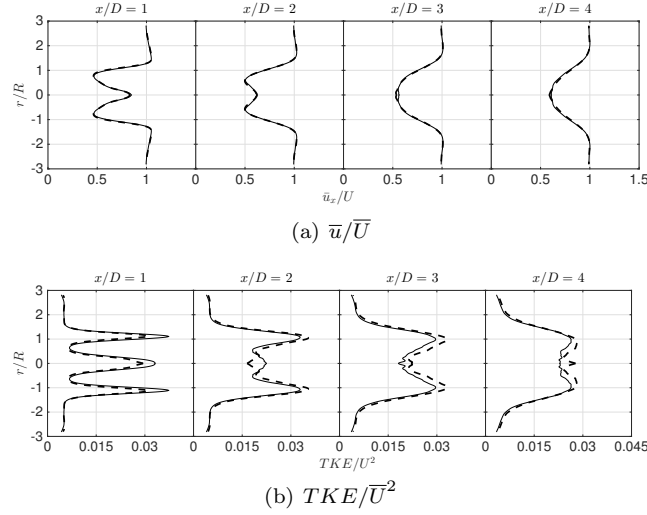


Figure 5.10 – Azimuthal averages of the normal and tangential velocities and TKE, for the VPM method at the two resolutions: 64 points per D (dashed lines) and 128 points per D (solid line)

5.3.2 Velocity and TKE contours

Figures 5.12 to 5.14 show the time-averaged velocities and TKE over one Mann box period ($T_{mann} = 24D/\bar{U} = 216$ s, 336 s and 604.8 s for $\bar{U} = 14$, 9 and 5 m/s, respectively), normalised by $\bar{U}$ and $\bar{U}^2$. The discussion about these results is structured in several points, presented below. Some points discussed below will be better highlighted by considering azimuthal averages profiles in the next Section 5.3.3.

We precise that the figures show the resolved TKE. As the mesh used for the AD simulations is relatively coarse, the subgrid scale TKE may represent

a non-negligible part of the total TKE. A short study has been performed in Appendix A to quantify the TKE part contained in the subgrid scales at several pointwise locations downstream of the machine. It shows that about 9% of the total TKE may be contained in the subgrid scales.

Asymmetry in the flow We first notice the asymmetry in the flow: there are areas of larger velocities and TKE, particularly marked for simulations at high TI (see Figs. 5.12(e) and 5.12(f)). It is due to the Mann turbulent field, which is itself non-uniform when time averaging is considered. This impacts the resulting wake, which is not symmetric. The use of a longer box of turbulence fluctuations may help in reducing the non-uniformity in the flow, but has the disadvantage of increase the amount of storage. The use of a box with a length of 64D (not shown in this work) still leads to noticeable asymmetry in the resulting wake. An other solution consists in performing several simulations, and in each simulation, adding turbulence fluctuations generated with a different seed (for the random number generator). The wake statistics can thus be computed based on an average of all simulations. This solution considerably increases the cost of the simulations, and is not appropriate for the VPM simulations that require a fine mesh. In our work, a third solution is used: azimuthal averages are performed, in order to attenuate this non-uniformity and to compare the time-averaged velocity and TKE profiles at different downstream locations (those are discussed in Section 5.3.3).

Inflow turbulence decay Another general remark is the streamwise decay of the ambient turbulence fluctuations: it is very noticeable for the highest turbulence intensity levels (Figs. 5.12(e) and 5.12(f)). Indeed, there is no production to balance the dissipation (except the enhancement of turbulence due to the presence of the wake, only localized in the wake region) and this potentially impacts the resulting wake behavior and its recovery with the streamwise distance.

Figure 5.11 shows the streamwise evolution of the standard deviations (normalized by $\bar{U}$) over one Mann period for the three velocity components. The results are obtained from simulations without wind turbines (in the FD4 code), for one wind speed, 9 m/s, and the three levels of inflow turbulence. The statistics are based on averages in the transverse direction (limited to the surface with the presence of turbulence fluctuations). The standard deviations of the axial component continuously decreases with the downstream location (Fig. 5.11(a)),

and, at the end of the domain, the final value reaches a level of approximately 80% for the lowest TI. The decay increases with the initial level of turbulence, with a final value reaching 60% of the initial one for the highest level of inflow turbulence. TI_v remains nearly constant (we even notice very small increases, see Fig. 5.11(b)). TI_w slightly decreases with the streamwise position; again, the drop increases with the initial level of turbulence.

The turbulent fluctuations decay seems comparable in the two different codes (see Fig. 5.13(f), for example): this will allow a still valuable comparison between the two approaches. The used method for inflow turbulence generation may be problematic for simulations with two or three aligned wind turbines, separated with a classical length of seven rotor diameters, as the decrease in TI may be very important at far downstream locations.

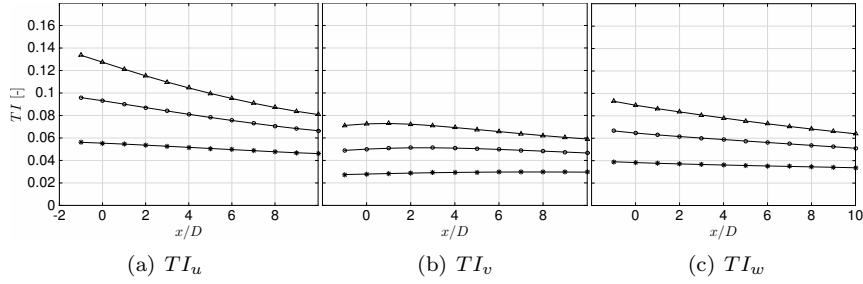


Figure 5.11 – $\bar{U}_\infty = 9$ m/s: Spatially averaged TI for the three different components and three inflow turbulence intensities: 6% (solid line with stars), 10% (solid line with circles) and 15% (solid line with stars).

Differences in the mean flow between the two codes The resulting mean flow seems different between the codes. This is well marked in simulations at high turbulence levels and low loading cases (see Figs. 5.12(e) and 5.12(f)). There are several explanations: as already mentioned, the turbulent field is clipped for AD simulations and only added at the center of the domain, leading to strong non zero divergence at these locations. Once advected by the code, the initial fluctuations are altered by the Poisson solver, which enforces a solenoidal velocity field; this may impact the fluctuations and the ambient flow. Simulations without clipping should be performed; in that case, the initial box would be centered around the wind turbine, and the outside domain would be filled with duplicated boxes (as the Mann algorithm produces periodic boxes).

Although it handles the fluctuations in a different manner, the VPM code

can also modify the properties of these fluctuations.

Impact of operating conditions and turbulence

$\lambda = 5.7$ **and** $C_T \simeq 0.3$ The weakly loaded cases lead to small velocity deficits: $\bar{u}/\bar{U}$ reaches a minimum of 0.8 inside the wake in Fig. 5.12(a). When the ambient turbulence is small, the wake (and its velocity deficit) remains stable on a large downstream distance and the turbulence production increases from $x/D = 4$ (Fig. 5.12(b)). For the VPM approach, the root vortices quickly interact behind the machine, which leads to a net increase of turbulence in this area. The velocity deficit captured in the AD simulations is similar to that obtained with the PM approach while the level of turbulence seems slightly lower however. When the TI increases, the wake destabilises on a shorter distance for both methods and the turbulence production increases in the wake regions. For the highest TI, the TKE increase due to the wake is the same order of magnitude as that in the initial ambient flow (Fig. 5.12(f)).

$\lambda = 7.55$ **and** $C_T \simeq 0.8$ When the tip-speed ratios and C_T increase (Fig. 5.13), the velocity deficit is more important. The wake then becomes unstable and re-energises over a shorter distance downstream of the rotor, as observed in simulations with uniform flows. The instabilities mainly appear at the blade tip and the disk edges for VPM and the AD, respectively. The increase of the level of turbulence helps the AD to reproduce wake statistics closer to those obtained with the VPM approach: the lowest TI leads to a more important velocity deficit in the AD approach (noticeable up to $x/D \simeq 6$ in Fig. 5.13(a)) and the increase in TKE appears at a larger distance (see Fig. 5.13(b)), while the case with TI = 15% leads to similar behaviors in the two approaches.

$\lambda = 10.24$ **and** $C_T \simeq 0.95$ For the very highly loaded case, presented in Fig. 5.14, results with TI = 6% already lead to very similar behaviors for the AD and the VPM approaches. The wake destabilisation just appears at a location slightly further downstream for the AD, with a resulting TKE that remains important over a larger distance (up to $x/D \simeq 7$, against $x/D \simeq 6$ in the VPM approach (Fig. 5.14(b))).

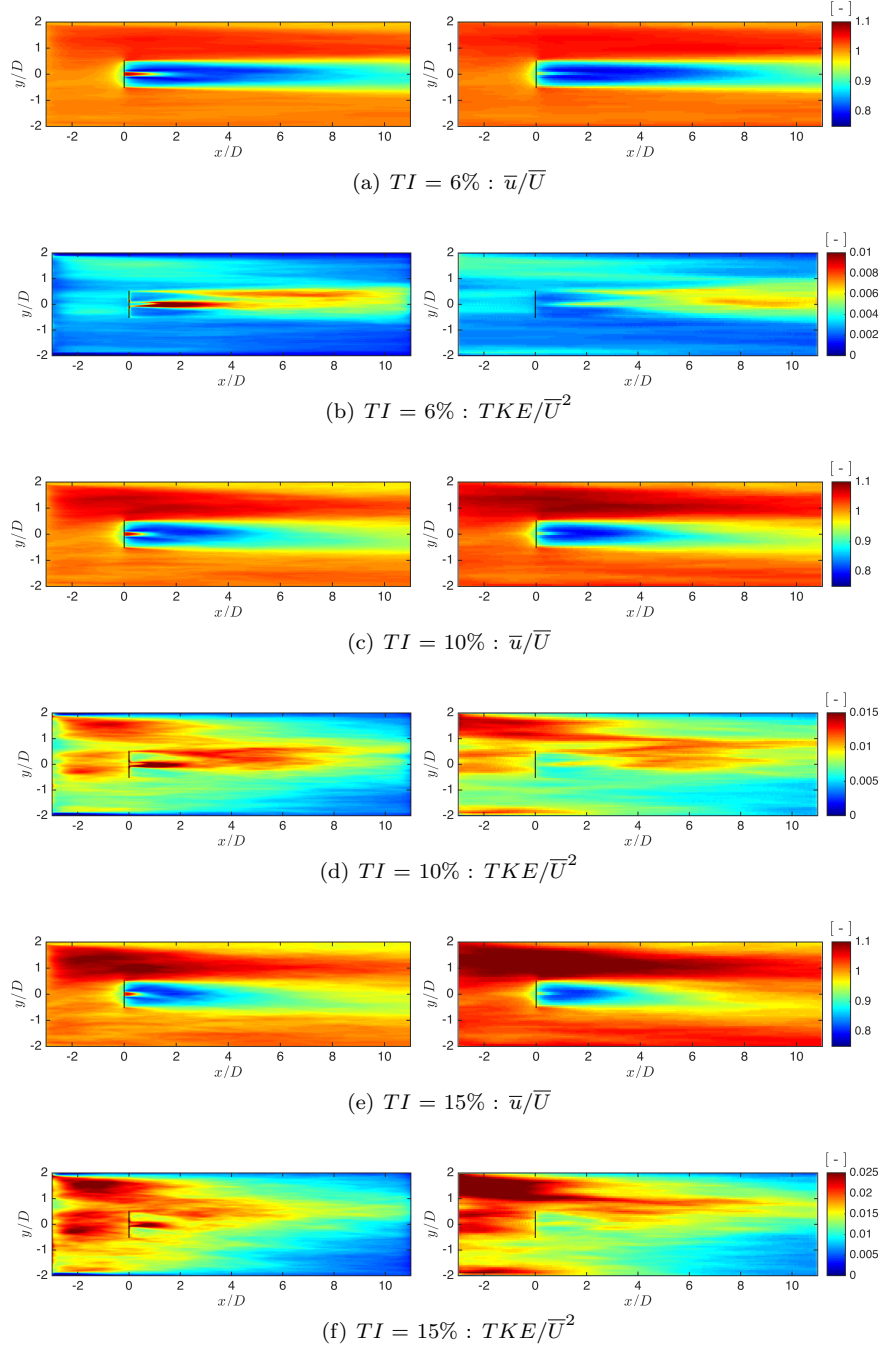


Figure 5.12 – $\lambda = 5.7$ ($\bar{U} = 14\text{m/s}$): Time-averaged streamwise velocity and TKE production for VPM (left) and AD (right) approaches

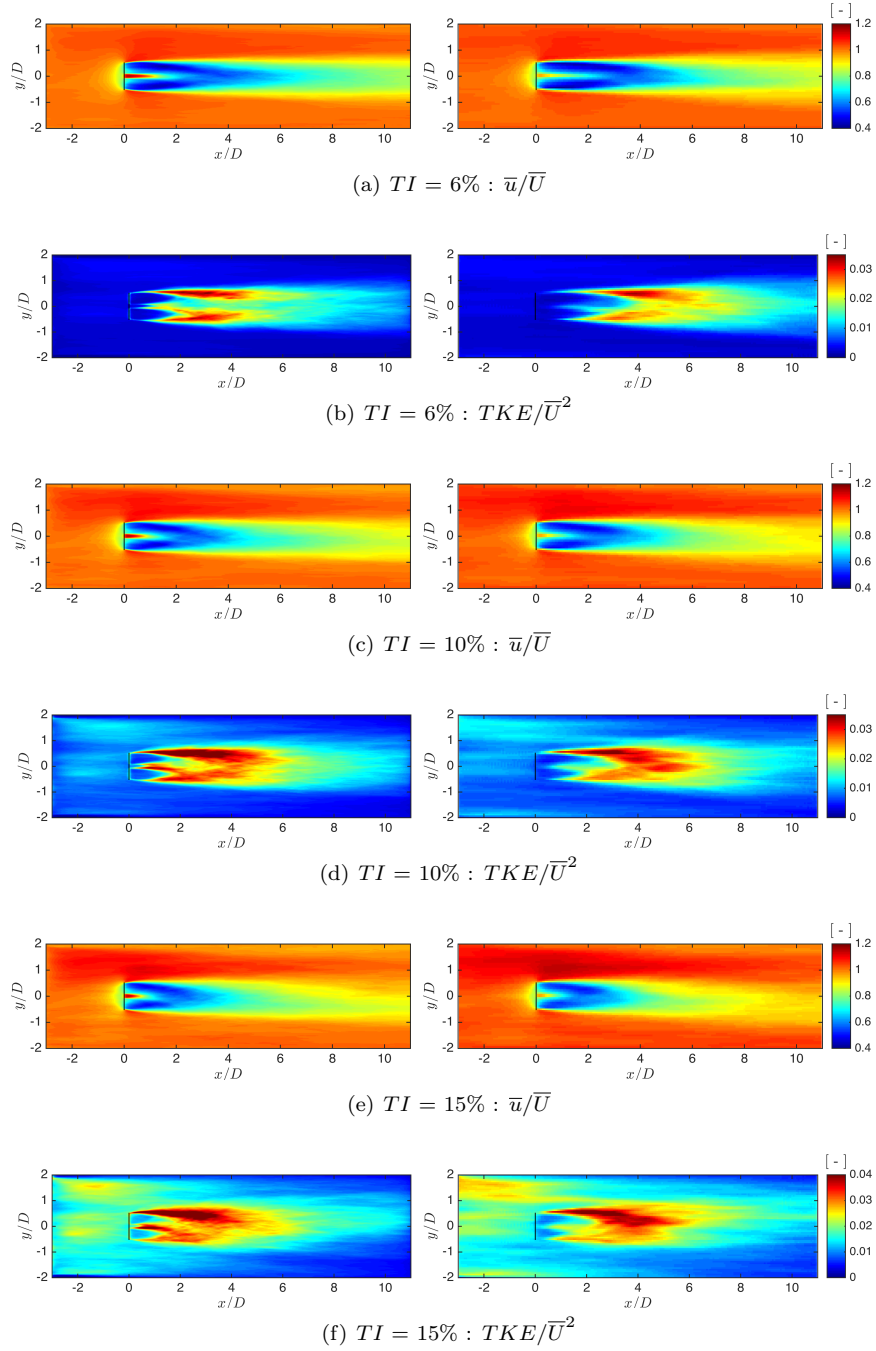


Figure 5.13 – $\lambda = 7.55$ ($\bar{U} = 9\text{m/s}$): Time-averaged streamwise velocity and TKE production for VPM (left) and AD (right) approaches

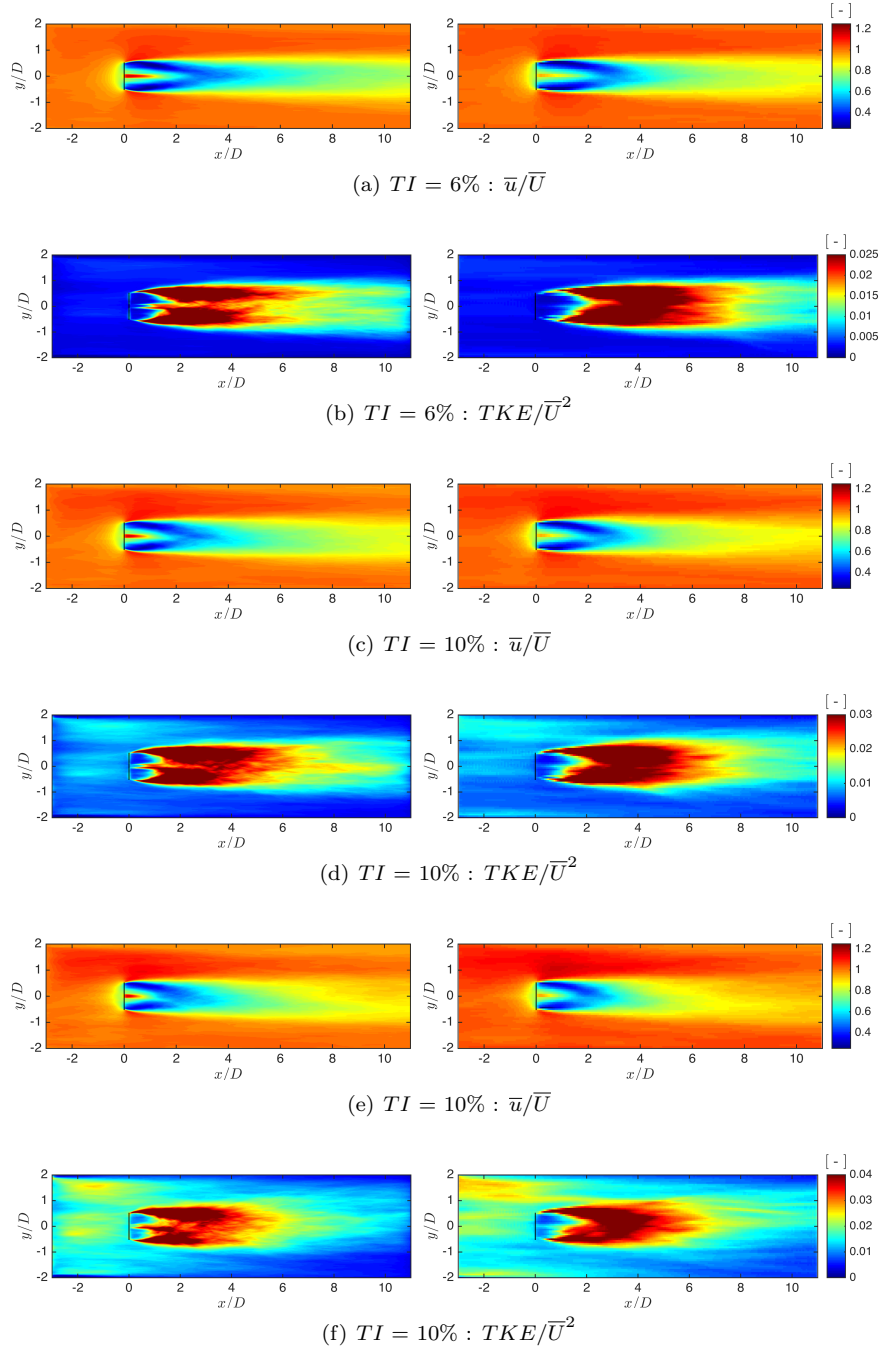


Figure 5.14 – $\lambda = 10.24$ ($\bar{U} = 5\text{m/s}$): Time-averaged streamwise velocity and TKE production for VPM (left) and AD (right) approaches

5.3.3 Radial profiles

For the sake of completeness, we provide the velocity and TKE profiles at different downstream locations of the wind turbine (Figs 5.15 to 5.20). Azimuthal averages are here performed, as the azimuthal direction is normally an homogeneous direction for these simulations. Again, the AD results are compared to those obtained with VPM. The red lines in Figs. 5.17 and 5.18 correspond to AD simulations performed with a Smagorinsky model and $C_s = 0.02$; these results will be discussed at the end of this section.

Impact of operating conditions and turbulence Globally, for all considered cases, the velocity deficits are in good agreement from the downstream location $x/D \simeq 5$ on (see Figs. 5.15, 5.17 and 5.19). The increase in the inflow turbulence tends to bring this agreement closer to the rotor. The AD approach, at a coarse resolution, is thus able to produce velocities in the far wake similar to those obtained with the VPM approach.

Further downstream, the velocity deficit appears to be slightly higher in the VPM method: for the highest loading, this is already observable from $x/D \simeq 5$ (see Figure 5.19). A potential explanation could be that the fourth-order finite difference code works at a coarser resolution and exhibits a behavior that is more dissipative than that of the VPM code. This leads to an increased wake dissipation in the highly turbulent regions.

The difference between the mean inflow in the two methods mentioned earlier is noticeable in Figure 5.15(c): this leads to an offset between the profiles. The wake evolution and its recovery are not too impacted by this difference.

The AD approach is unable to capture the near-wake. It is rather going to develop its instabilities further into the wake (see Figs. 5.16, 5.18 and 5.20) (the coarse resolution contributes to accentuate this). The TKE thus increases further downstream: for the two highest loadings at a low turbulence level (Figs. 5.18(a) and 5.20(a)), the TKE is thus higher than that obtained by the VPM method at $x/D = 5-7$. The discrepancies decrease at further downstream locations, helped by the increased dissipation of the flow. For low loading cases (Fig 5.16), the turbulence productions are very close to each other.

Impact of the SGS model As mentioned in Chapter 2, the RVMs-Smagorinsky model is used for simulations with one wind turbine, but it is no longer appropriate for wind farm simulations, when a wall modeling procedure is required;

the classical Smagorinsky model is thus used. We here investigate the impact of a change of the SGS model, and verify the wake statistics obtained with the Smagorinsky model, which will be used in the next two chapters.

Globally, for all the considered turbulence intensities, the velocity profiles are very similar for the two SGS models. In terms of TKE, the behavior is slightly different; in each case, the simulation with a Smagorinsky model leads to a slightly lower level of turbulence. However, the difference is small and from $x/D \simeq 6 - 7$, the level of TKE is similar for the two SGS models.

Globally, despite its coarse resolution and its approximation, the AD approach produces far wake results that are in good agreement with those obtained with the VPM approach. The impact of the small observed discrepancies in the turbulence production could be quantified by performing simulations with two wind turbines. The second rotor would be located between $x/D = 5$ and $x/D = 7$, and investigations about its power production and its resulting wake behavior would highlight the repercussions of the upstream wake differences.

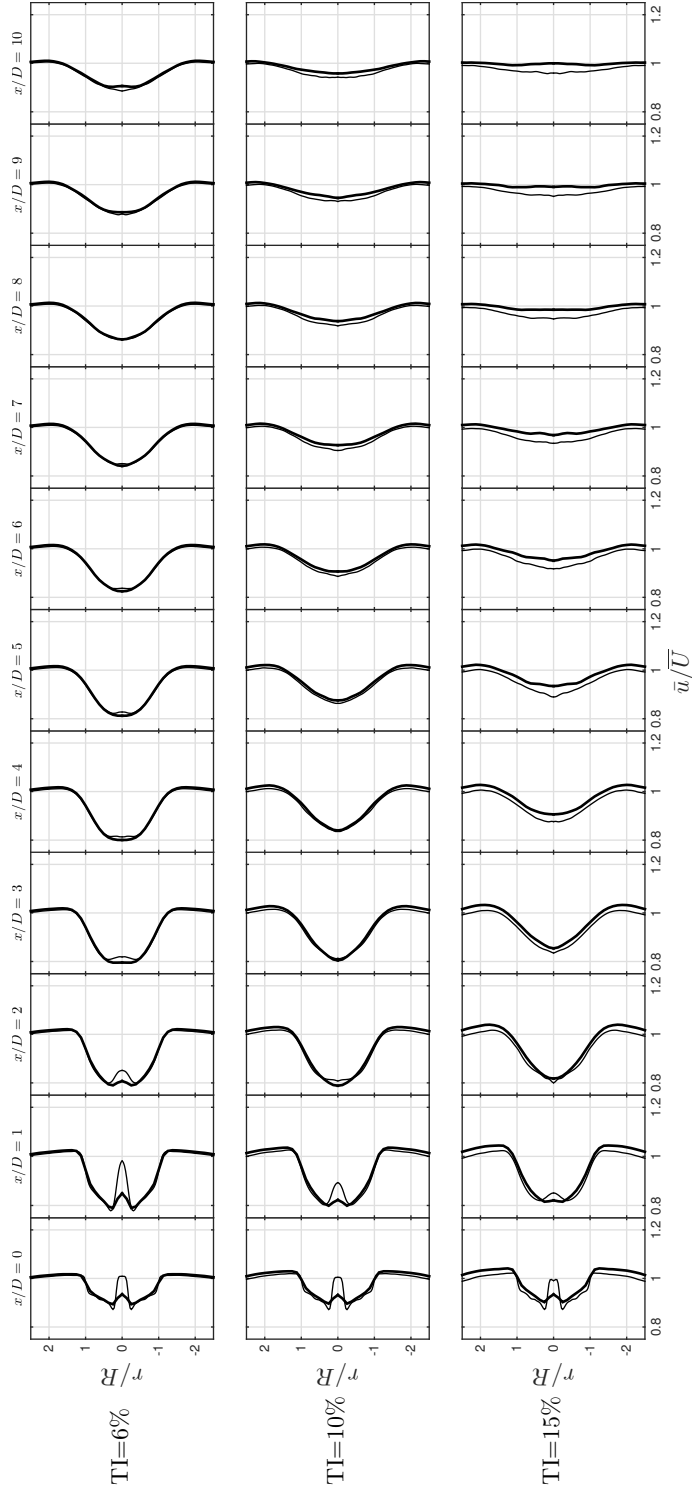


Figure 5.15 – $\lambda = 5.7$: Azimuthal averages of the time-averaged streamwise velocity at several downstream locations, for the VPM approach (thin solid line) and the AD method (thick solid line).

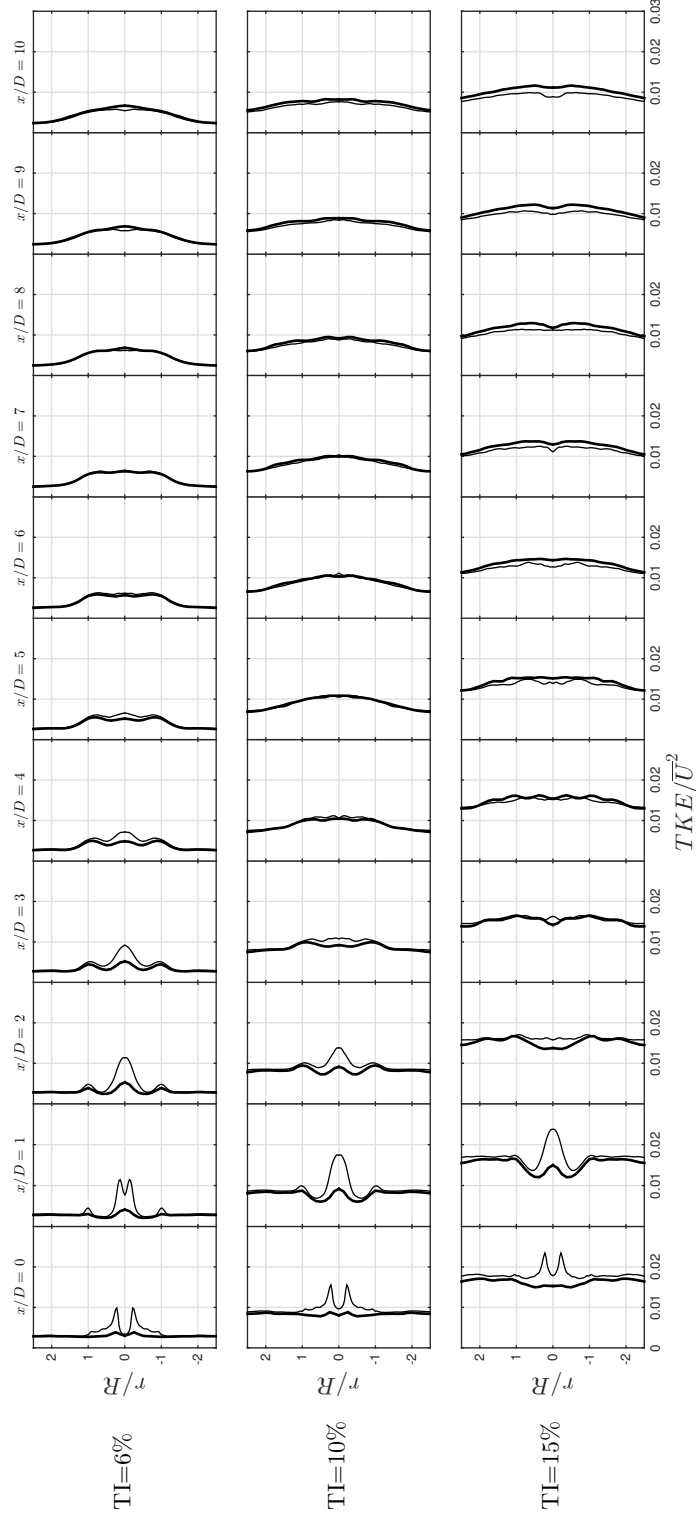


Figure 5.16 – $\lambda = 5.7$: Azimuthal averages of the TKE at several downstream locations, for the VPM approach (thin solid line) and the AD method (thick solid line).

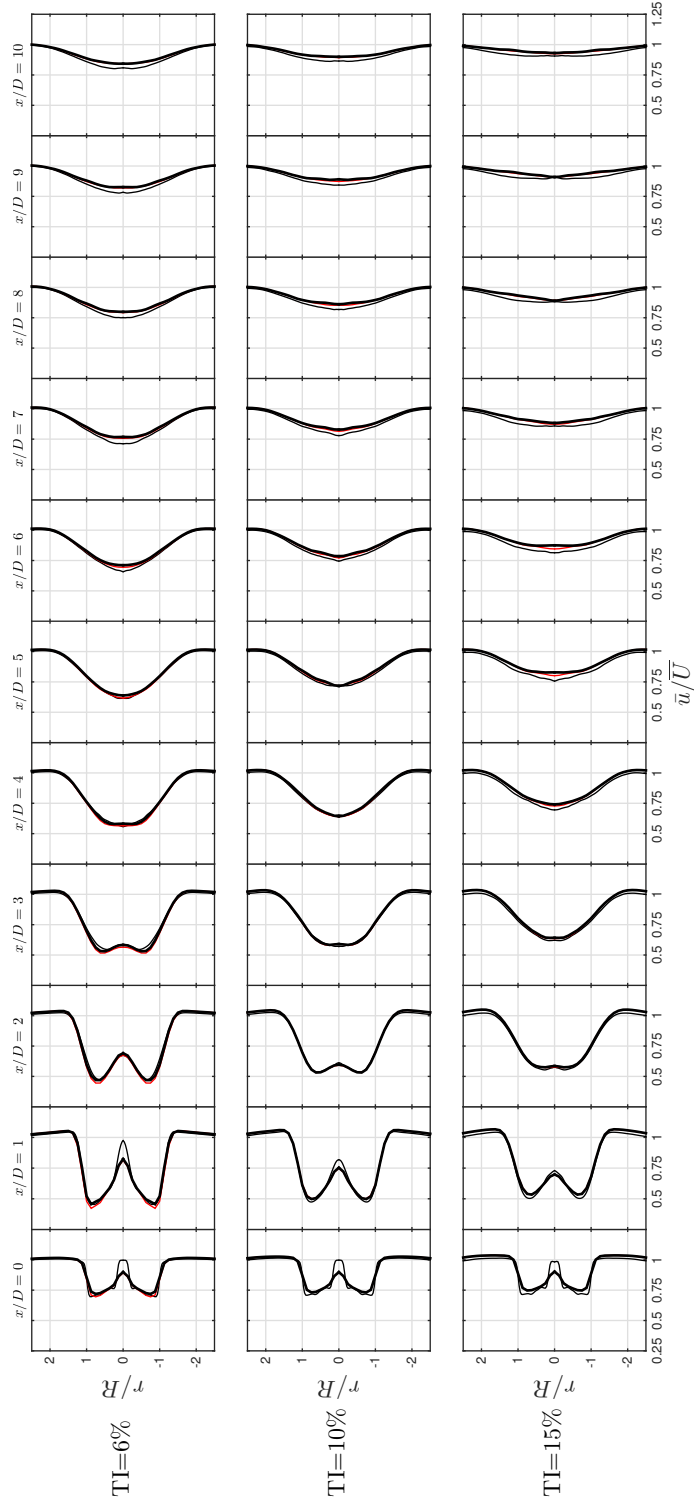


Figure 5.17 – $\lambda = 7.55$: Azimuthal averages of the time-averaged streamwise velocity at several downstream locations, for the VPM approach (thin solid line) and the AD method with the two SGS models, RVMs-Smag (thick solid line) and Smagorinsky (red thin line) models.

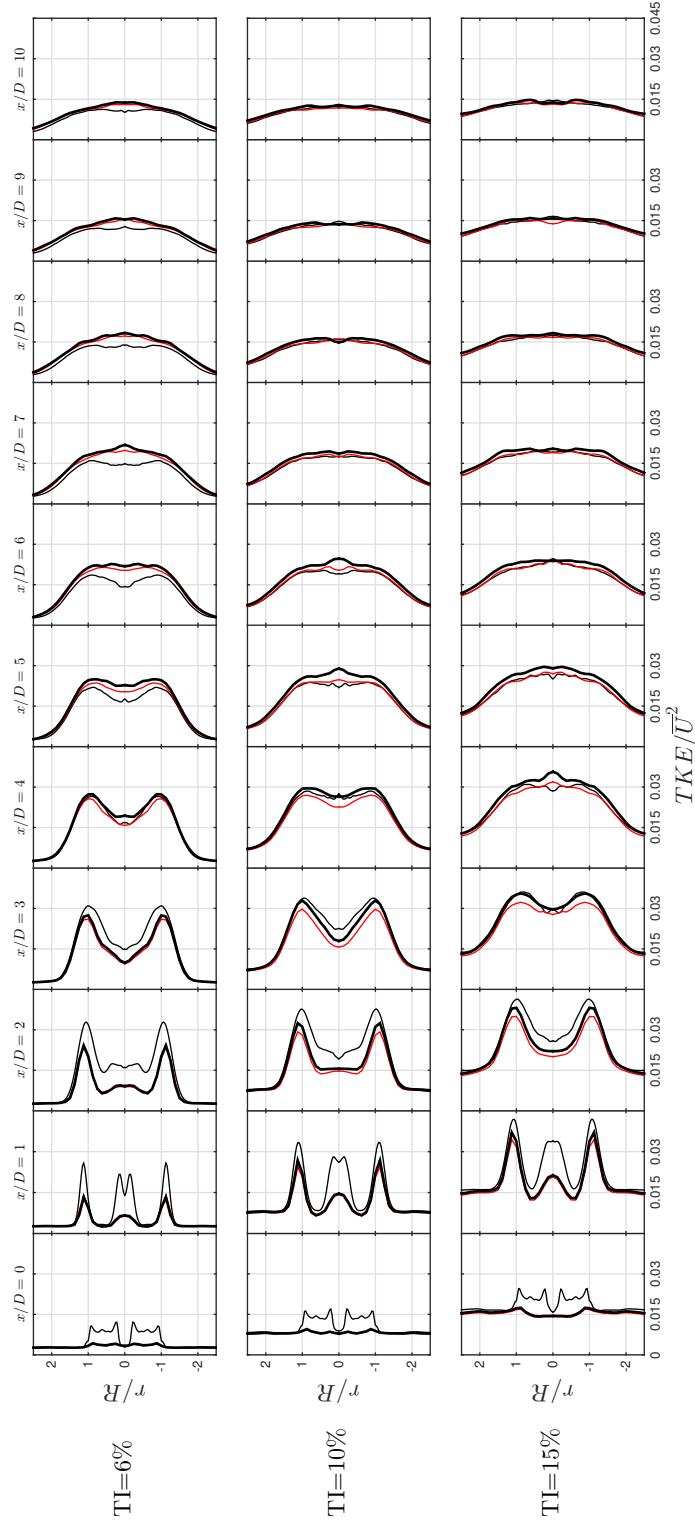


Figure 5.18 – $\lambda = 7.55$: Azimuthal averages of the TKE at several downstream locations, for the VPM approach (thin solid line) and the AD method with the two SGS models, RVMs-Smag (thick solid line) and Smagorinsky (red thin line) models.

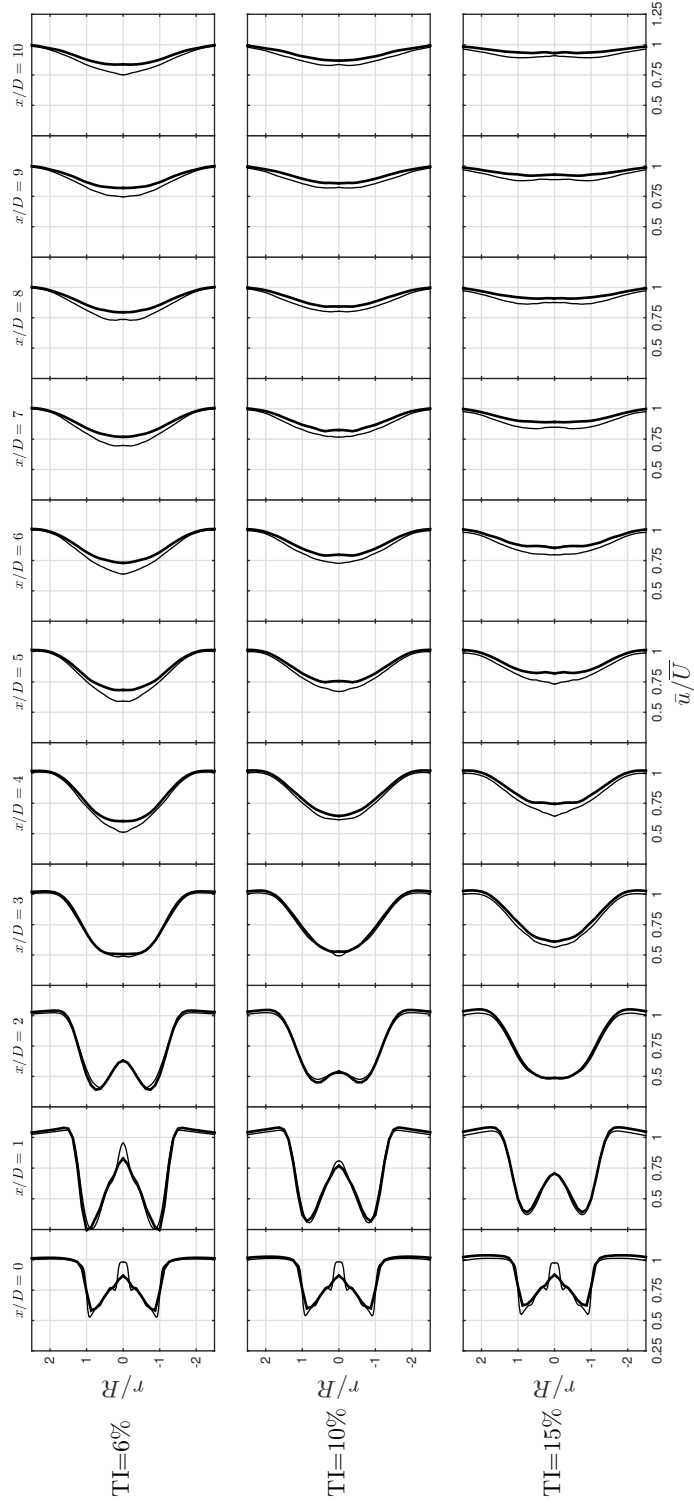


Figure 5.19 – $\lambda = 10.24$: Azimuthal averages of the time-averaged streamwise velocity at several downstream locations, for the VPM approach (thin solid line) and the AD method (thick solid line).

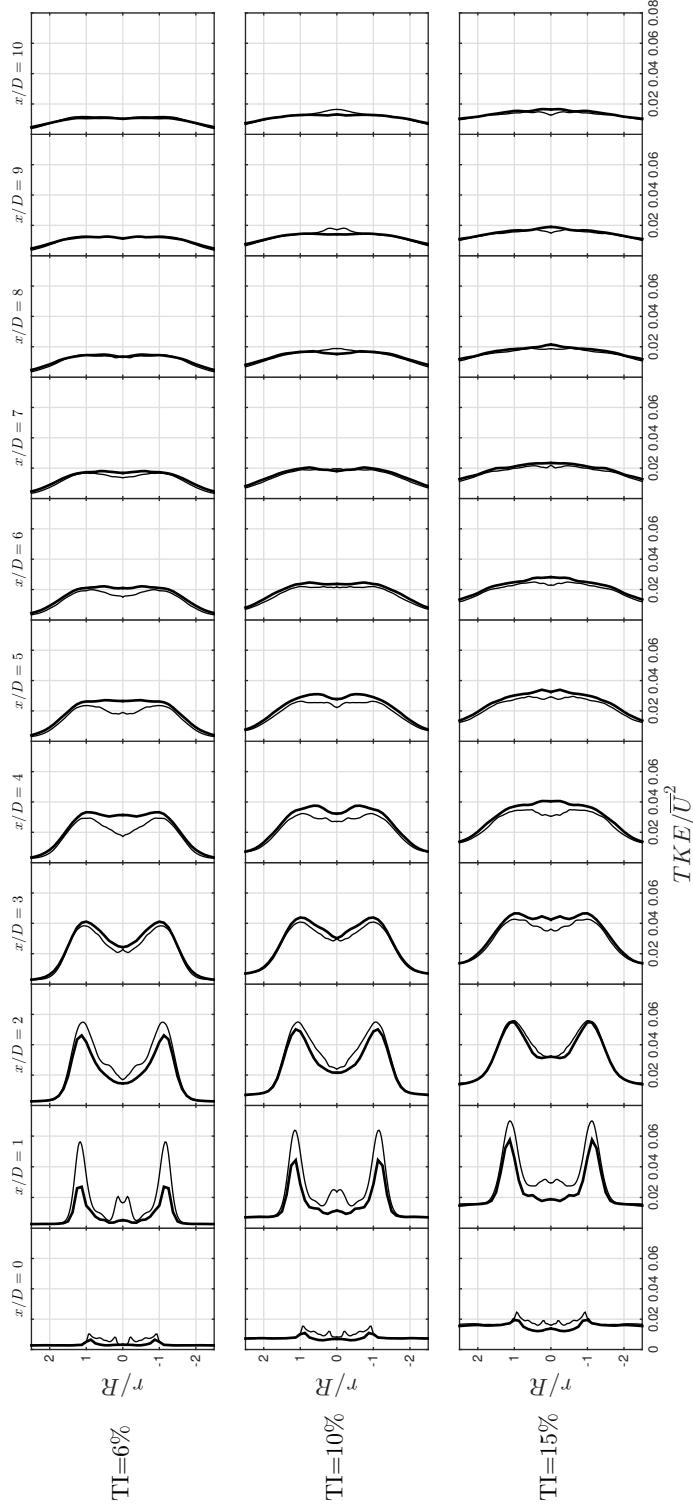


Figure 5.20 – $\lambda = 10.24$: Azimuthal averages of the TKE at several downstream locations, for the VPM approach (thin solid line) and the AD method (thick solid line).

5.4 Conclusions

In this chapter, wake statistics obtained in AD simulations were investigated and compared to those obtained with a more accurate approach, accounting for a finite number of blades (a Vortex Particle-Mesh method coupled to immerse lifting lines (LL)). The difference in their capture of the flow physics was first highlighted in simulations in uniform flows, with similar resolutions for both approaches. The impact of coarser resolution was then investigated and this clearly emphasized the differences in the velocity deficit and the level of turbulence obtained behind an AD. The addition of turbulence clearly helped the AD method in destabilizing the wake and in producing more accurate wake statistics.

Several simulations were then performed for three operating conditions of one rotor, the NREL-5MW wind turbine. For each operating condition, three turbulence intensities (TI) were investigated. The objective was here to analyse the wake statistics captured behind an AD at a coarse resolution through a comparison with a discrete-blade type approach at a fine resolution. For these simulations, the turbulent fluctuations at the inflow had been computed beforehand with Mann's algorithm. This method implies a decay of the turbulent fluctuations with the streamwise direction, as there is no production to balance the dissipation; this surely impacted the wake behavior and its re-mixing process. However, as the decaying appeared similar in the two codes, the comparison remains relevant.

Globally, the velocity deficits in the far wake were very similar in both approaches, even though the level of TKE slightly differed, mainly for simulations at low TI and high loadings. Still, the current comparison has some limitations and could be further improved.

First, there is here no ground or mean shear in the inflow velocity. Nevertheless, the present test campaign subjected the AD to shear in an unsteady fashion, and in that case, the approach was capable of reproducing loads and accurate wake statistics. This leads us to believe that the level of fidelity of our AD approach should not suffer from the addition of the ground. Moreover, a ground and the subsequent forced shear would maintain a level of turbulence through the computational domain, and thus may even help the AD method in reproducing more accurate statistics.

A similar issue is that of the absence of nacelle and tower effects. In the actual flow, these structures indeed contribute to perturb the otherwise cleanly

shed vortical structures (the hub vortices for the nacelle, and both tip and hub vortices for the tower). Accounting for these structures would however be challenging as their sizes put them well below the targeted mesh resolutions for the AD method. One could perhaps envisage an additional perturbations fitted from experiments or geometry-resolving CFD.

Only the time-averaged statistics were investigated in this work, which hides the wake kinematics, i.e. its meandering. We recall that the present AD method was run at coarse resolutions and for large time steps: our code then effectively captures the large scales of the flow. This hints at the possibility that the AD method is actually reproducing, at a reasonable fidelity, the large scale motions of the wake. Nevertheless, one should assess more carefully the reproduced wake meandering.

Finally, one could also evaluate the present method in scenarios with multiple machines. Indeed, such a test case would permit the study of the disk behavior in a waked condition, i.e., the disk would be subjected to a more heterogeneous turbulence level. Moreover, such a scenario would allow to analyze the accuracy in a wake merging scenario and check whether the observed small errors in the TKE will accumulate through the wake merging and worsen in the wake of the next machine.

Chapter 6

A transient scenario inside a wind farm

This chapter proposes first investigations in a wind farm configuration. We here consider a wind farm of 15 machines, among which an emergency shut-down occurs. Again, we consider the “NREL offshore 5 MW baseline wind turbine” and the model is improved with the addition of control schemes and an emergency shut-down procedure. The generation of the turbulent flow and the representation of the Atmospheric Boundary Layer are obtained by performing a first simulation without wind turbines, in a domain of the same size as the main simulation but with a coarser resolution. This chapter focuses on an operational scenario, with investigations on the power histories, and we do not make thorough studies about wake statistics through the wind farm. This work is a slightly modified version of the results presented in *The Science of Making Torque from Wind (Torque 2016)* conference and published in *Journal of Physics : Conferences Series, 2016* [97].

6.1 Introduction

Numerical simulations offer a convenient framework to investigate transient events occurring in a wind farm and the resulting response in terms of wake dynamics, loads and power variation. This last signature is important for grid integration, in particular, the management of intermittency and the quality of electricity service (frequency). It will be useful for the elaboration of wind farm

control schemes allowing to regulate the global power production. In order to make such a study possible, the simulations need to be sufficiently accurate, with realistic responses by the wind turbines, while remaining affordable at the scale of a wind farm.

Such a transient wind scenario is clearly one of the target applications for the tool developed in this thesis. We here perform a first assessment of the well suitability of our tool on a wind farm with 15 machines where an emergency shutdown occurs. Different studies exist about the consequences of such a stop on the structural loads of the rotor itself [16, 17, 65], but there is currently no investigation of the effect on other wind turbines. This chapter thus also offers a study of the impacts of such a stop on the defective machine but also on the wakes and power production at wind farm scale. The Actuator Disk model is here equipped with realistic control schemes, based on those of the “NREL offshore 5 MW baseline wind turbine” [75] and described in Section 6.2. As a wind farm simulation is considered, more advanced methods must be envisaged for generating the turbulence. The inflow is thus obtained using a precursor simulation of the same size as that of the main domain, and instantaneous velocity fields are saved to be later used as inflow for the main simulation with rotors. The wind farm configuration, as well as the precursor simulation, are described in Section 6.3. The results are presented in Section 6.4. Finally, our conclusions are drawn in Section 6.6.

6.2 Wind turbine model and control schemes

The wind turbine is accounted for through the AD corrected with tip-loss factors (as described in Chapter 3). The AD model is here improved with the use of control schemes, allowing a realistic rotor response to incoming flow variations. The implemented controller schemes are those of the NREL 5MW [75]: a torque controller for the region II of the power curve (corresponding in a maximization of the power production), supplemented with a pitch control scheme, when the rotor operates in the third region. This last controller degrades the performances of the machine to maintain the rated rotational speed and torque. The different regions are highlighted on the NREL power curve reported in Fig. 6.1. A yaw controller has also been added. Those are briefly described below.

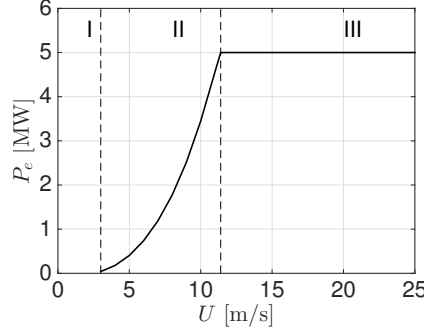


Figure 6.1 – NREL 5MW : Power curve relation, with the different control zones. The values are reproduced from the NREL-5MW reference paper [75]

6.2.1 Generator-torque and collective blade pitch angle controllers

Drivetrain dynamics

The wind turbine is controlled by imposing the generator torque; this value is imposed based on a measurement of the generator speed and tabulated data (explained below). The imbalance between the imposed generator torque and the aerodynamic torque (here produced by the AD model coupled to the LES) thus leads to variations in the rotor speed. The whole drivetrain dynamics is represented by a first order system:

$$Q_{aero}^n - N_{gear}Q_g^n = I_{drivetrain} \frac{(\Omega_r^{n+1} - \Omega_r^n)}{\Delta t} . \quad (6.1)$$

In this equation of the drivetrain motion, N_{gear} is the high-speed (generator) to low-speed (rotor) gearbox ratio, $I_{drivetrain}$ is the drivetrain inertia and $\frac{(\Omega_r^{n+1} - \Omega_r^n)}{\Delta t}$ is the rotor shaft rotational acceleration. The rotor shaft (low speed shaft) is here assumed to be rigidly connected to the gearbox, itself rigidly connected to the generator; there is thus no torsion effects. $N_{gear} = 97$ and $I_{drivetrain} \simeq 43 \times 10^6 \text{ kg m}^2$ for the NREL-5MW. At the next time step, $n + 1$, aerodynamic forces are thus computed with the new rotor speed Ω_r^{n+1} , given a new mechanical torque and the control procedure is applied again. For the third region, the rotor speed is held constant, as well as the generator torque and the power: this is only possible by pitching the blades. A Proportional-Integral (PI) controller is thus used; it is described at the end of this section.

Generator speed filtering

The generator-torque and blade-pitch controllers are both based on the sole measurement of the generator speed, Ω_g , equal to $N_{gear}\Omega_r$. This value is thus classically filtered before its use for controlling, to mitigate high-frequency excitation of the control systems [75]. The used filter is a low-pass filter with exponential smoothing:

$$\Omega_g^{f,n} = (1 - \alpha) \Omega_g^n + \alpha \Omega_g^{f,n-1} \text{ with } \alpha = e^{-2\pi\Delta t f_c} \quad (6.2)$$

where $\Omega_g^{f,n-1}$ and $\Omega_g^{f,n}$ are the filtered rotor speeds (coming from the measurement) of the previous and current time steps, Ω_g^n is the unfiltered generator speed (input), α is the low-pass filter coefficient, Δt is the discrete time-step, and f_c is the cutoff frequency. This last value has been set to 0.25 Hz (corresponding to roughly one-quarter of the blade's first edgewise natural frequency (see [75])).

Generator-Torque Controller

In the NREL-5MW control schemes, there are five control regions: I, I^{1/2}, II, II^{1/2} and III. The curve “generator speed-generator torque” for these several regions are presented in Fig. 6.2(a); this relation is used in the torque control scheme, in order to evaluate the required Q_g , based on the generator speed measurement.

Region I is here not modeled, and it corresponds to wind velocities below the cut-in wind speed. The generator torque is set to 0 and so the wind is used to accelerate the rotor up to 6.9 RPM ($\Omega_g \simeq 670$ RPM) for the start-up. In Fig. 6.1, the region I corresponds to a zone where there is no electrical power production.

Region II is the region for which the power is maximised, with a constant tip-speed ratio and a maximal power coefficient, $C_{p,max}$. The generator torque is thus proportional to the square of the filtered generator speed, $K^* (\Omega_g^f)^2$ (where K^* is a function of the rotor characteristics, $C_{p,max}$ and the optimal λ). In the case of the NREL-5MW, the computed $C_{p,max} = 0.482$ and occurs at $\lambda_{opt} = 7.55$, leading to $K^* \simeq 0.026$ Nm/rpm².

Region III corresponds to the rated power region, and the generator torque should be inversely proportional to the generator speed. However, in this region, the rotor speed (and so, the generator speed) is maintained constant with

the pitch controller, for noise considerations. The generator torque is thus also kept constant this region. In order to limit the tip-speed to 80 m/s, the rotor speed is rated to 12.1 RPM. The rated generator speed is thus equal to about 1174 RPM and the rated torque is about 43 kN m. This last value is obtained based on the rated generator speed and the rated mechanical power (about 5.3 MW for an efficiency of 94.4 %).

Region I^{1/2} is a linear transition between Region I and Region II, to place a lower limit to the rotor's operational speed range; it is defined between the minimum generator speed and 30% above this value, i.e. 871 RPM. Region II^{1/2}, also a linear transition, is required to limit the rotor speed at rated power. This region begins at 99% of the rated generator speed.

The controller bases the determination of the region on the filtered generator speed measurement and the pitch angle. The torque is computed as if the rotor operated in Region III whenever the previous blade-tip pitch angle command is 1° or higher, regardless of the generator speed value; this improves the output power quality.

Pitch angle controller

In Region III, the rotor speed, as well as the power, must be held constant. The only way to reach this state consists in varying the pitch angle of the blades, to degrade the performances of the rotor, and thus limit Q_{aero} when the wind speed increases. In this model, only a collective pitch control is considered, i.e. the three blade pitch angles have the same value and move according to the same command. A Proportional-Integral (PI) control is thus added, based on the error between the rated rotor speed and the actual rotor speed, $\Delta\Omega$:

$$\Delta\gamma = K_P N_{gear} \Delta\Omega + K_I \int_0^t N_{gear} \Delta\Omega dt \quad (6.3)$$

where K_P and K_I are the blade-pitch controller proportional and integral gains, respectively. As the sensitivity of aerodynamic power to the pitch angle increases over the Region III, K_P and K_I are adapted according to the considered pitch angle (see details in [75] and see Fig. 6.2(b)). Limitation blocks have been added in order to impose minimal and maximal values for the pitch angle (here 0 and 90°, respectively). The blade-pitch rate is limited, here at 8°/s in absolute value. This controller does not include any blade-pitch actuator dynamic effects. The time scale of these dynamics is often relatively small, and

does not impact the whole operation of the wind turbine. However, this could be an improved feature for future works (examples are widely spread in the literature).

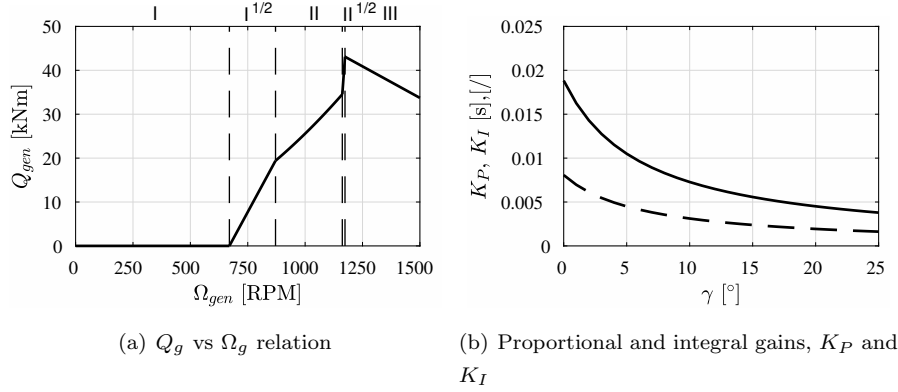


Figure 6.2 – NREL 5MW : (left) Torque response to variable generator speed and (right) blade-pitch controller proportional and integral gains (solid line and dashed line, respectively)

The controller has been calibrated with a Blade Element Momentum method in its initial version [75] and our first implementation of this controller has also been verified and recalibrated with that method. The controller has however not been re-calibrated for its coupling with the Actuator Disk model, and some characteristics thus change. For example, $C_{p,max}$ for the AD model is not 0.48, but higher, as it has been highlighted in Chapter 2. This can slightly change the equilibrium reached by the rotor, but not its transient behavior.

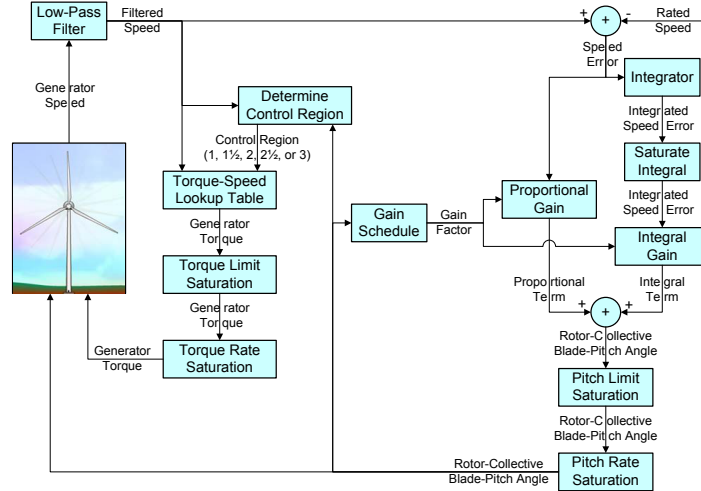


Figure 6.3 – NREL 5MW : Global control system (from [75])

6.2.2 Yaw controller

A yaw controller has also been implemented based on that from the pre-design phase of the DOWEC 6MW turbine [77]. It uses averages of the wind direction errors to monitor the yaw misalignment: the controller activates yaw actuators if a two-seconds averaged yaw error is higher than 30° or if a 30-seconds averaged yaw error is higher than 5° . The nacelle thus rotates at $0.3^\circ/s$ until the two-seconds average of the error is less than 0.5° . The wind direction error is based on the difference between the normal to the rotor plane and the actual wind direction; this value is computed based on the horizontal velocity components at the center of the disk. Again, the dynamics of the actuation system are not accounted for.

6.2.3 Emergency shutdown procedure

An emergency shutdown procedure has also been added. Based on the aerodynamic braking principle only, this method is typically used for variable blade-pitch wind turbines [30]. This is done by turning off the generator torque and pitching the blades to feather (90°) at the maximum pitch rate ($8^\circ/s$ for the NREL 5MW [75]). The wind turbine behavior during such a shutdown is studied in Section 6.4.

6.3 Numerical set-up

6.3.1 Wind farm configuration

LES coupled to AD are used to investigate transient scenarios occurring at a wind farm level and the impacts in terms of resulting power and wake dynamics. The chosen scenario is an emergency stop of a wind turbine within a wind farm of 15 machines (3 lines of 5 wind turbines); the rotor for which a shutdown procedure is required is located in the middle of the farm, in the second row and on the center line. There are 7D between the wind turbines, in both horizontal directions and the first wind turbines are located at 14D from the inlet. The global domain size is $52D \times 8D \times 28D$ with a grid of $N_x \times N_y \times N_z = 832 \times 192 \times 448$. The mesh is uniform in the horizontal directions, with a resolution of 16 points per D and $\Delta x = \Delta z = 7.875$ m. The grid is partially stretched in the vertical direction: the grid is uniform below 200 m with $\Delta y = 3.9375$ m; above that, Δy increases to reach $\Delta y_{max} \simeq 10$ m at the top of the domain.

This stretching and the used resolution prevent an AD force mollification at the lower points from affecting a sample location of the wall model. The stretching is here relatively weak to prevent cells at the top to be larger than those of the precursor simulation.

Instantaneous velocities are imposed at the inflow boundary at each time step. We recall that the code can handle turbulent inflow conditions in two fashions: using precursor LES results or using turbulent fluctuations pre-generated with the Mann algorithm [89] and added on a chosen velocity profile at the inlet. For wind farm simulations, thus with wind shear and ground effects, the first approach is the most appropriate. The best method involves running a prior simulation of a turbulent boundary layer flow without wind turbines and, once the flow reaches a statistical equilibrium, yz -planes of instantaneous velocities are saved for a certain number of time steps and later used as inflow for simulations with wind turbines.

A convective boundary condition is applied at the outflow, periodic boundary conditions are imposed in the z direction and no-through flow is enforced at the top. At the bottom, a wall stress model for a rough wall is applied in order to compute the surface shear stress as a function of the LES velocity field at the third vertical grid point. We recall the main equations for the wall modelling procedure:

$$u_\tau = \frac{\kappa |\langle \mathbf{u} \rangle|}{\log \left(\frac{y}{y_0} \right)}, \quad (6.4)$$

$$\tau_{\mathbf{w}} = \rho \frac{\mathbf{u} |\mathbf{u}|}{|\langle \mathbf{u} | \mathbf{u} \rangle|} u_\tau^2, \quad (6.5)$$

where the roughness length is here taken as $y_0 = 0.1$ mm (corresponding to a calm sea surface) and $\kappa = 0.38$. $\langle . \rangle$ denotes here a local averaging using the $n_x \times n_z = 5 \times 5$ closest points. Indeed, this simulation comprises the rotors and do not present homogeneous directions, contrary to the precursor simulation.

The SGS model is here a classical Smagorinsky model, with $C_s = 0.02$.

The time step is fixed to $\Delta t = 0.125$ s. The total simulation time reaches 3000 s and the shutdown occurs at $t = 1500$ s ($= T_{SD}$). A simulation without the rotor stop is also performed: the results will provide a reference for power productions and allow to clearly identify the emergency stop effects on the power through the wind farm.

6.3.2 Precursor simulation

The inflow condition is obtained from a prior simulation without wind turbines: it is performed on a domain of the same size as the main simulation but with a coarser resolution, 12 points per D in the vertical direction and 6 points per D in the x and z directions, leading to a grid of $N_x \times N_y \times N_z = 312 \times 96 \times 168$. The use of a coarser resolution here allows shorter computational times for reaching the statistically-steady conditions and smaller storage of instantaneous velocity planes.

For this pre-simulation, periodic boundary conditions are applied in both the x and z directions, while the conditions for the vertical direction remain the same. The flow is driven by a constant forcing pressure gradient $\left(\frac{dP}{dx} \right)_F$, where $P = \frac{p}{\rho}$. The wall friction balances the pressure gradient, such that the momentum budget is equal to

$$u_\tau^2 = - \left(\frac{dP}{dx} \right)_F H \quad (6.6)$$

where H is the height of the domain. It is thus possible to determine the required pressure gradient for the considered characteristic $u_\tau = 0.25$ m/s. This value has been chosen in order to reach 9 m/s at hub height with the considered $y_0 = 0.1$ mm. The wall model is the same as described in the previous section,

but $\langle \cdot \rangle$ of Eqs. (6.4) and (6.5) denotes a global average over the whole x and z extents of the domain.

Once the flow reaches a statistically-steady state, yz -planes of velocities are saved to finally obtain about 3000 s of wind, which are interpolated on the inlet grid of the wind farm simulation.

The results in terms of mean velocity profile and turbulent kinetic energy for this precursor simulation are presented in Fig. 6.4. The resulting TI_u and TI are also presented, computed as:

$$TI_u(y) = \frac{\langle u'u' \rangle_{xz}^{1/2}}{U_{hub}} \quad (6.7)$$

$$TI(y) = \frac{\sqrt{\frac{2}{3}k}}{U_{hub}} \text{ where } k = \frac{1}{2} (\langle u'u' \rangle_{xz} + \langle v'v' \rangle_{xz} + \langle w'w' \rangle_{xz}) \quad (6.8)$$

U_{hub} is here the time-averaged velocity at hub height. We first notice that this precursor simulation captures the logarithmic law well. The two first points correspond to an adaptation of the LES to the wall shear stress boundary condition [131]. The mean streamwise velocity is effectively 9 m/s at the hub height (see horizontal dashed-line in Fig. 6.4(b)). The wind turbines will thus operate in the region II of their controllers [75], in which the maximum energy is extracted from the wind. In this region, only the torque-controller is active and the pitch angle of the blades is set to 0° . The turbulence intensity, depending on its definition, varies from 4.5 to 6.5 % (see Fig. 6.4(c)).

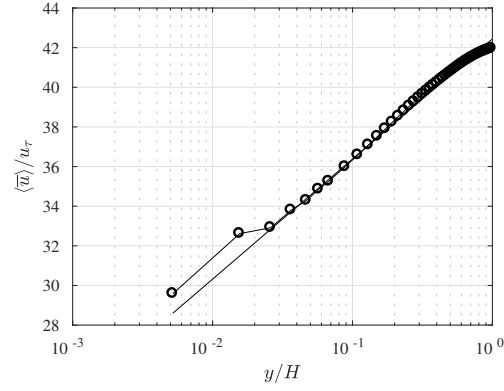
Impact of the resolution jump between the two simulations

The difference of resolution is quite important between the precursor and the main simulations, and the adaptation length before the first wind turbines is relatively small, only $14D$. We thus first perform a fullsimulation without wind turbines in order to investigate the evolution of the flow statistics at different streamwise locations. Figure 6.5 shows the mean streamwise velocity and the turbulence intensities, TI and TI_u , for the precursor results and mean vertical profiles at three different streamwise locations, inside the main simulation ($x/D = 7$, $x/D = 14$ (where the first wind turbines are located) and far downstream, at $x/D = 50$).

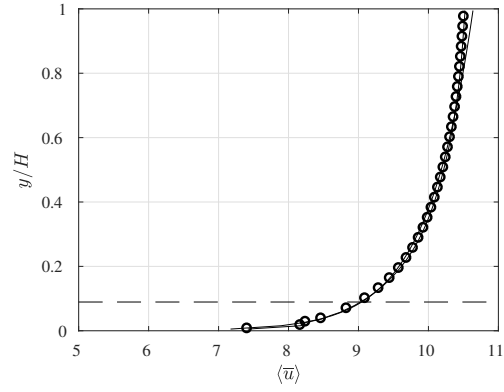
The mean velocity evolves between the precursor and the main simulations, but the adaptation inside the main domain is quite fast and the mean velocity does not vary sensibly with the streamwise direction. The turbulence inten-

sities also present some discrepancies with respect to those of the precursor simulations and variations are observed along the streamwise direction in the main simulation. In the lower part, the TI increases between $x/D = 7$ and $x/D = 14$ then decreases again at $x/D = 50$. Small changes are also noticeable in the upper part, where the turbulence intensity increases with the x direction. It could be relevant to compute the spectra of the turbulence fluctuations, and investigate the development of both small and large scales, in order to verify the flow convergence at the location of the first wind turbines.

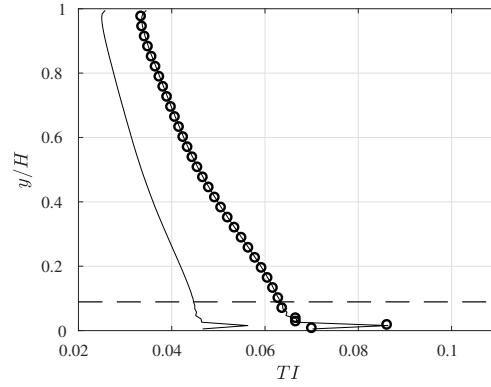
We chose here a coarser resolution for the precursor simulation in order to allow a faster computational time and avoid a large amount of storage when the velocities were saved. For the future wind farm simulations, we will rather use concurrent simulations, with equivalent resolutions. This will avoid so such marked differences between the ABL and the main simulation, and the subsequent large adaptation distances.



(a) $\langle \bar{u} \rangle$ (solid line with dots), compared with the logarithmic law (solid line)



(b) $\langle \bar{u} \rangle$ (solid line with dots), compared with the logarithmic law (solid line)



(c) TI_u (solid line with dots) and TI (solid line)

Figure 6.4 – ABL : Time-averaged and horizontally-averaged streamwise velocity and turbulence statistics for the ABL precursor simulation. The horizontal dashed line represents the hub height. The markers do not show all grid points for the sake of readability.

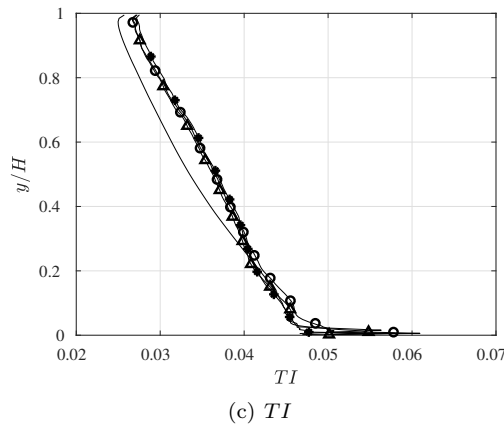
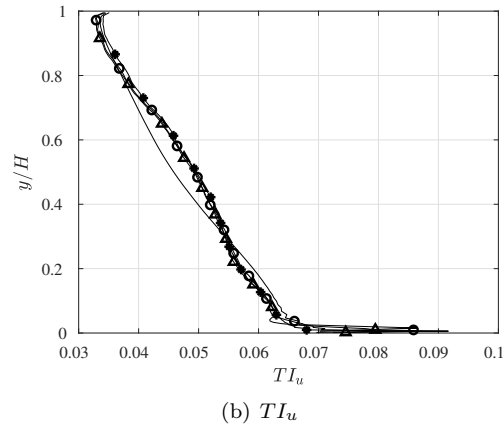
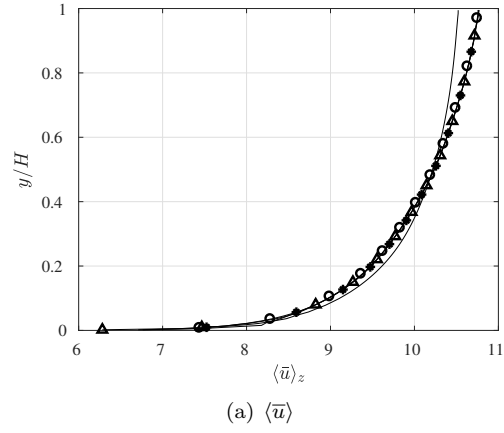


Figure 6.5 – Time-averaged and horizontally averaged streamwise velocity, TI , TI_u and k for the ABL simulation (solid line) and three different streamwise locations inside the main domain: $x/D = 7$ (solid line with triangles), $x/D = 14$ (solid line with circles) and $x/D = 50$ (solid lines with stars). The markers do not show all grid points for the sake of readability.

6.4 Wind farm simulation and emergency shut down

We first study the transient behavior of the faulty wind turbine: Fig. 6.6 shows the blade pitch angle, the rotational speed, the aerodynamic and generator torques as functions of time (graphs are only displayed for a short time interval). When the emergency stop occurs, the blades are pitched to their feather position (Fig. 6.6(a)), and the generator torque is turned off (Fig. 6.6(c)). This results in the rotor speed decreasing (Fig. 6.6(b)) and in the aerodynamic torque becoming negative (Fig. 6.6(c)), which successfully brings the rotor to a stop within a little less than 30 seconds. Before decreasing, the rotor speed presents a positive spike, which is due to the disconnection of the generator and the sudden removal of resistive torque. Afterwards, the negative aerodynamic torque builds up and slows the rotor down. These transient states are similar to results of other studies [17]. Such a shutdown has consequences on structural loads (tower top and flapwise blade tip displacements, bending moments,...); those are not investigated in this work.

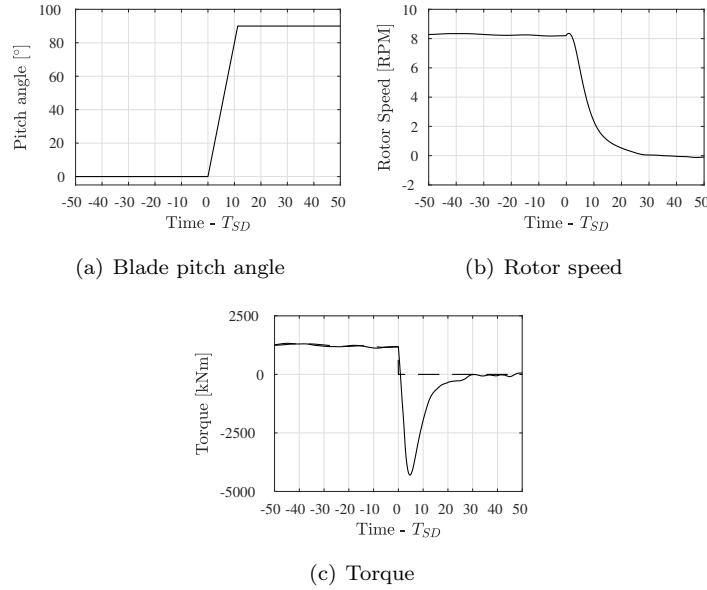


Figure 6.6 – Faulty wind turbine data : blade pitch angle, rotor speed, generator and aerodynamic torques (dashed line and solid line) as functions of time. (T_{SD} is the time of the shutdown).

The shutdown of the wind turbine impacts the productions of the downstream wind turbines. These data can be linked to the evolution of the flow and wakes, presented in Figs. 6.7 and 6.8: Fig. 6.7 shows horizontal slices of axial velocities at hub height, and Fig. 6.8, xy -planes taken at the middle of the farm, at $z/D=14$, cutting through the center line of turbines. These slices present instantaneous data at various relevant times. Figures 6.9 and 6.10 present the rotor speed and power of the machines of the center line.

When the second wind turbine stops producing power at $t = T_{SD}$, its wake still impacts the wind turbine located downstream, WT3. Therefore, a certain delay after the failure is necessary before observing a rise of the rotor speed of WT3 (Fig. 6.9), at about $t = T_{SD} + 150$ s. This delay is equal to the time for the wake of WT2 to cross the distance between WT2 and WT3. Figures 6.7(b) and 6.8(b) highlight this: 100 s after the emergency stop, we still see some remnants of the WT2 wake just before WT3. From 150-200 s after the WT2 shutdown, WT3 produces more power (Fig. 6.10) but does not reach the production of the first wind turbine WT1: indeed, the WT1 wake effect is still felt 14D downstream, at the WT3 location (Figs. 6.7(c) and 6.8(c)). The change in WT3 operating conditions does not affect much the fourth (WT4) and fifth (WT5) wind turbines of the line: it will be better highlighted when time-averaged power productions will be studied.

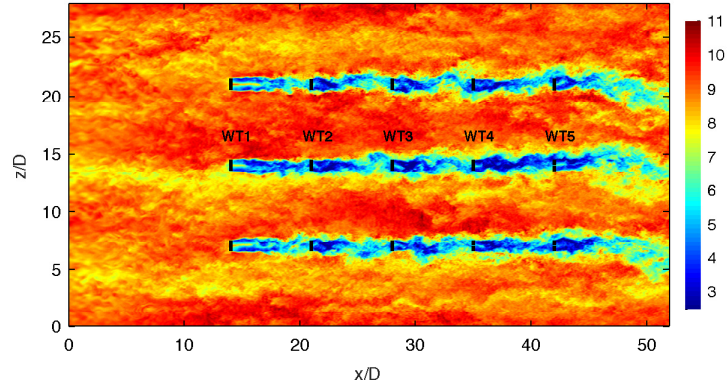
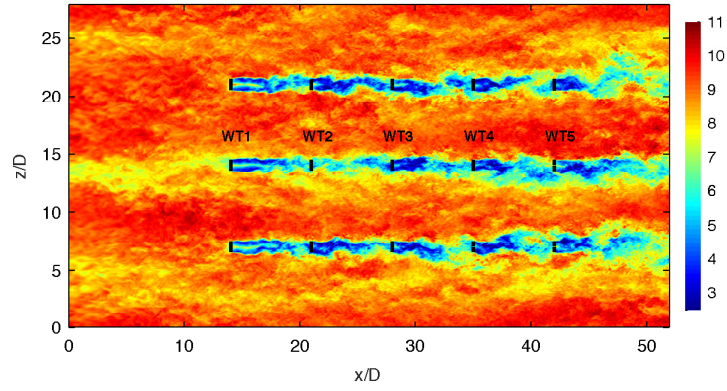
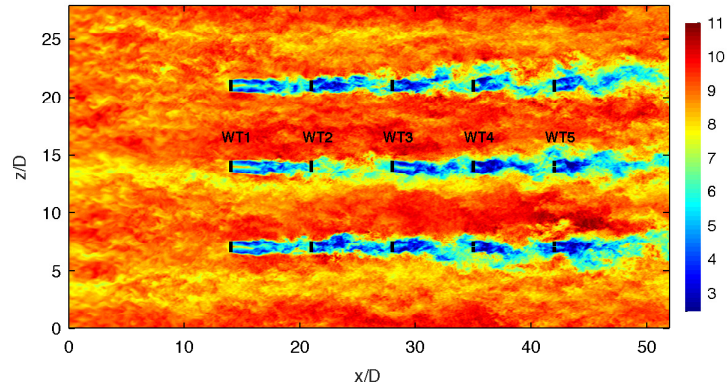
(a) $t = T_{SD} - 300s$ (b) $t = T_{SD} + 100s$ (c) $t = T_{SD} + 500s$

Figure 6.7 – Horizontal slices of axial velocities taken at the hub height for three times: long before the shutdown ($T_{SD} - 300s$), during the transient behavior ($T_{SD} + 100s$) and long after the shutdown ($T_{SD} + 500s$). The data are in m/s.

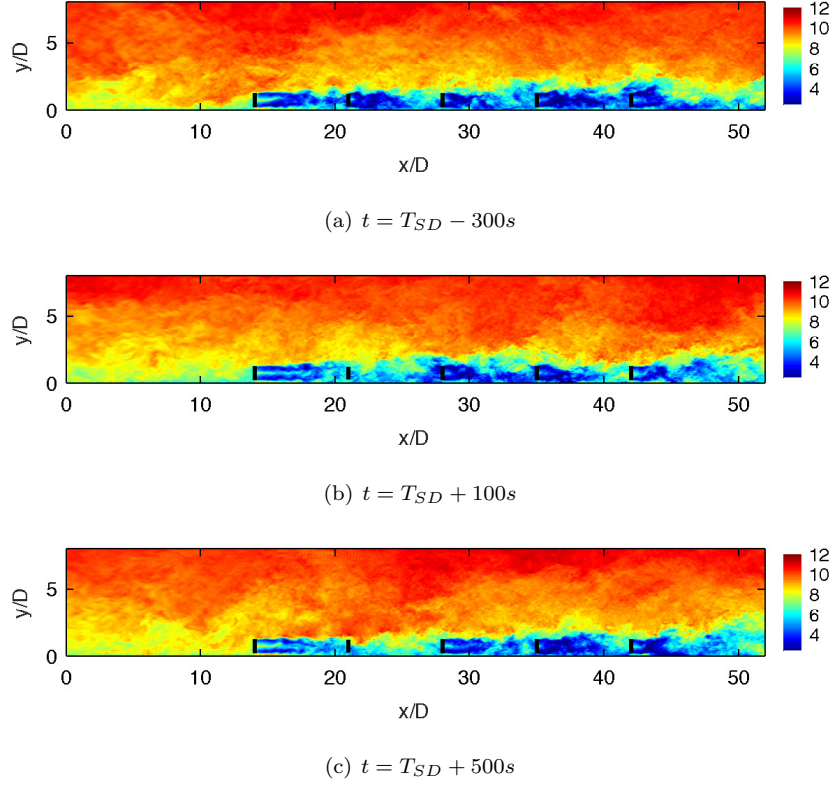


Figure 6.8 – Vertical slices of axial velocities taken at $z/D=14$ (center line) for three times: long before the shutdown ($T_{SD} - 300s$), during the transient behavior ($T_{SD} + 100s$) and long after the shutdown ($T_{SD} + 500s$). The data are in m/s.

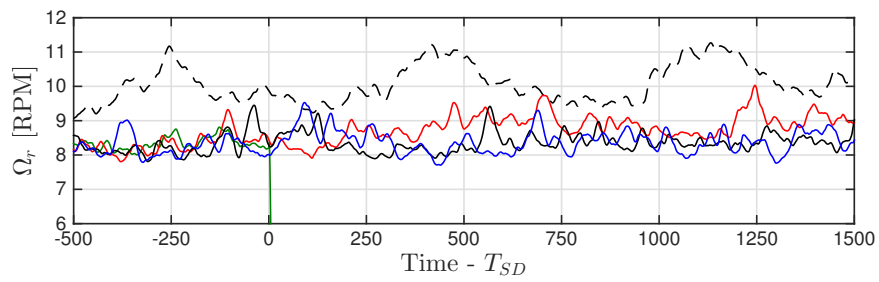


Figure 6.9 – Rotor speeds as functions of time for the wind turbines of the center line: WT1 (dashed black line), WT2 (green solid line), WT3 (red solid line), WT4 (black solid line) and WT5 (blue solid line).

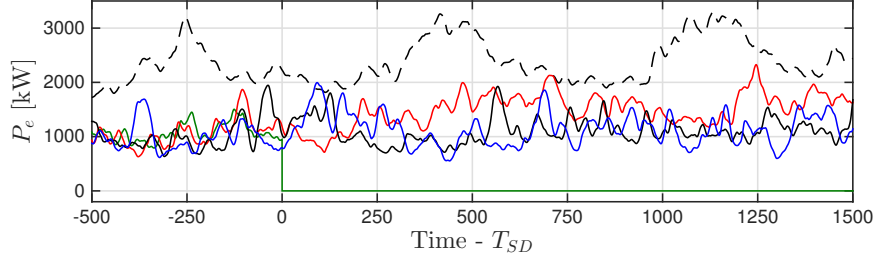


Figure 6.10 – Electrical powers as functions of time for the wind turbines of the center line: WT1 (dashed black line), WT2 (green solid line), WT3 (red solid line), WT4 (black solid line) and WT5 (blue solid line).

Figure 6.11 shows the time-averaged power production of the center line for the two main simulations (with and without the emergency shutdown), normalized by the power production of WT1. These values are averaged over a period of about 728 s (corresponding to $52 D/\bar{u}_{hub}$), starting from a time of 600 s after T_{SD} . When all the wind turbines are active, we see that the WT2 power is about 46 % of the first one, while the power production of the downstream machines stabilizes and is close to the WT2 power production. In the case with the emergency shutdown, the WT2 power is zero and WT3 produces more power. WT4 then adopts the behavior of the second wind turbine in a line, with an important power reduction. However, the WT3 wake is more turbulent, less structured than the WT1 wake, the power loss between WT3 and WT4 is thus less important than that observed between WT1 and WT2 before the shutdown: we obtain $\bar{P}_{WT4}/\bar{P}_{WT3} = 0.68$ while $\bar{P}_{WT2}/\bar{P}_{WT1} = 0.46$. As already noticed, $\bar{P}_{WT4}$ and $\bar{P}_{WT5}$ do not differ sensibly.

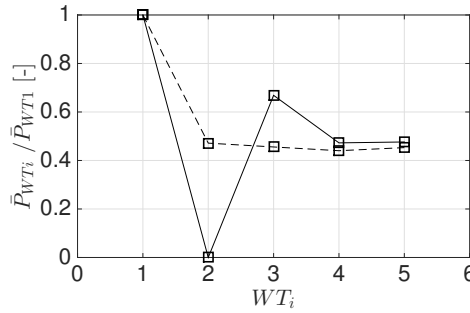


Figure 6.11 – Center line: time-averaged power produced by each wind turbine, $\bar{P}_{WTi}$, for the simulation without (dashed line) and with (solid line) the shutdown, normalized by the time-averaged power of the first rotor in the line, $\bar{P}_{WT1}$.

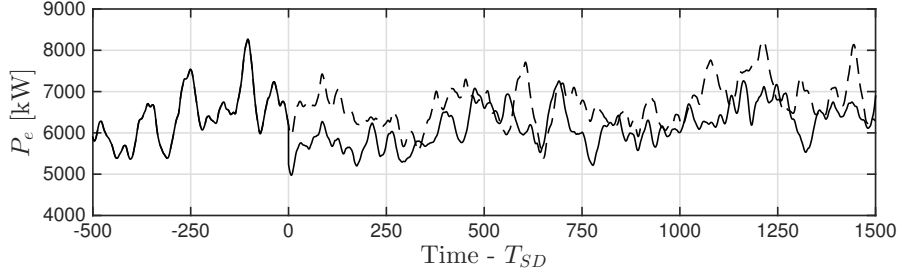


Figure 6.12 – Global power as function of time for the second line, for the simulation without (dashed line) and with the shutdown (solid line).

Figure. 6.12 shows the power history of the center line; it is also compared to that produced by the center line without an emergency shutdown. We observe the sudden decrease in power at the shutdown time T_{SD} . The global power then progressively increases, when WT3 begins to produce more power, but it still remains below the power production without the shutdown. Globally, this event leads to a power loss smaller than $\bar{P}_{WT2}$ computed without shutdown: the increase of the WT3 production attenuates the stop of WT2, leading to a global deficit of about 46 % of $\bar{P}_{WT2}$.

6.5 Computational aspects

The main simulation required roughly 2,600 processors-hours for a domain size of about 72 million points and 3000 s of simulation time (with a time step of 0.125 s).

6.6 Conclusions

We use LES coupled to Actuator Disks to perform accurate, yet still affordable, unsteady wind farm simulations. Higher fidelity is injected into the rotor representation by including realistic controllers. The code is used to study unsteady phenomena while keeping a reasonable CPU cost, to allow fast-time studies. A transient event has been investigated: the failure of a wind turbine inside a wind farm, with a required emergency shutdown. The impact of such an event has been studied in terms of wake dynamics and resulting rotor speed and power variations.

Such studies allow a better understanding of the operating condition variations during complex scenarios, and are useful for the elaboration of future

global control schemes. Indeed, it could be envisaged to leverage this tool for a full-chain simulation in order to improve the management of wind and grid events through farm-scale control.

In these first wind farm investigations, we used a precursor approach for the generation of the turbulent fluctuations, with a coarser grid than that of the wind farm simulation. However, the difference of resolution between the two simulations requires a large adaptation length, and the storage may become problematic for larger wind farms. As a consequence, we will rather use a concurrent inflow generation method for the simulations of Chapter 7.

Chapter 7

A validation case : the Horns Rev wind farm

This chapter aims at using and validating the Actuator Disk model in large wind farm simulations. In the previous chapter, wind farm simulations have also been performed but the resulting wake behavior, as well as its impact on the production of downstream machines have however not been investigated in details. This chapter has several objectives: (i) to study the performances of the tool and the AD model implementation in very large wind farms in terms of computational costs, (ii) to investigate the wake statistics, loads and power production of the different machines inside the wind farm and (iii) to verify the power production against measurements and observations of an actual wind farm. We here consider the Horns Rev wind farm, for which measurements are available and treated in several research works and papers. Several wind sectors around a main direction are investigated: increasing the size of the sector allows to account for a larger number of observations, and to decrease the uncertainties of the measurements. This chapter presents nine directions: one main direction and eight additional ones around this main direction, thereby covering an angular sector of about 18° . This chapter is to be submitted for publication; for this final version, it is expected to run additional simulations to cover a wind sector of 30° .

7.1 Introduction

The Horns Rev wind is an operational large offshore wind farm with 80 machines, located 14 km away from the west coast of Denmark, for which measurements and observations are available. This wind farm has been treated and investigated in many works (see [18, 19, 21, 20] for several references) and the observed power data has been used for verifying and validating different simulation tools. Barthelmie *et al.* [20] compared the measurements with several wind farm simulations tools, from the simplest wake models to more complex CFD models. Ivanell [67] simulated two lines of the Horns Rev wind farm, for different wind directions. We note that Advanced Actuator Disk methods were used, and the Mann’s model was used to generate turbulent fluctuations [89]. Wu and Porté-Agel [143] performed LES of the entire wind farm for different wind sectors around three main directions. Again, advanced Actuator Disk models were used, and simple control schemes were added to allow the machines to respond to the incoming fluctuations. Munters *et al.* [101] performed LES, with simple drag disks for modeling the rotor, for which normal forces were estimated from the local disk velocities.

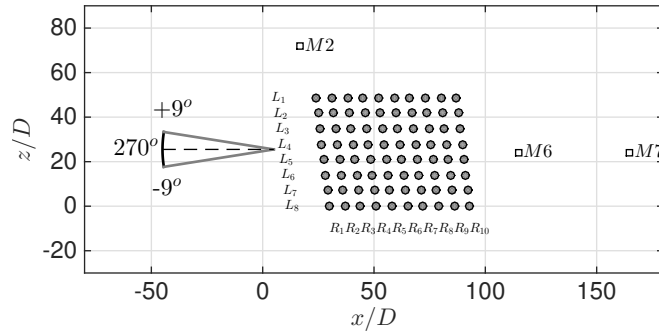
We here perform simulations for a wind sector of about 18° around the main direction corresponding to full waked conditions. In that case, each line contains rotors perfectly aligned with each others. The objectives are multiple: the validation of our simulation tool, the investigation of the wake behaviors and loads inside the wind farm, and the verification of the performances of the tool in terms of computation costs.

The Horns Rev wind farm is described in Section 7.2. A simple version of the actual rotor of the Horns Rev wind farm is implemented and described in Section 7.3. As we want to faithfully reproduce the rotor response, control schemes are also implemented. As large simulations are considered, the turbulent fluctuations are here obtained with a concurrent method; simulating one direction thus implies two simulations. This is described in Sections 7.4. The results are finally presented in Section 7.5, including the investigations of wakes and flow statistics, power productions and unsteady loads.

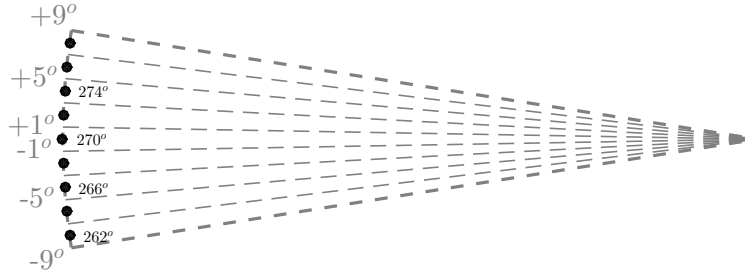
7.2 The Horns Rev wind farm

In this study, we validate the developed tool by simulating the Horns Rev wind farm, an operational large offshore wind farm, located 14 km away from the

west coast of Denmark. The wind farm has a rated capacity of 160 MW; it comprises 80 wind turbines arranged in a regular array of 8 by 10 turbines, as shown in Fig. 7.1(a). The SCADA (Supervisory Control And Data Acquisition) data, as well as the wind measurements (from three masts located inside the wind farm, see M2, M6 and M7 in Fig. 7.1(a)) recorded during three years (from 2005 to 2007) were accessible in the context of European Union-funded research projects. They were sorted and analysed in many reports and research papers (examples are available in [20, 143]).



(a) Rotor organisation, including location of the three nearby off-shore masts (M2, M6 and M7)



(b) Wind directions and wind sectors. The black dots represent the all simulated wind directions (there are 2° between the dots), and the grey dashed lines show several wind sectors, as defined in this work.

Figure 7.1 – Horns Rev wind farm and definition of the several wind directions and wind sectors

We base our comparison on measurements and results available in [20]. In that research paper, power losses inside the Horns Rev wind farm were investigated and compared to results obtained with wake models. Electrical power, nacelle position and wind turbine status signals were extracted from the SCADA system with a reference period of 10 minutes and merged with meteorological measurements from the masts. In order to maximize the number

of observations, the analyses were concentrated on prevailing wind directions (from NW to SW, 221, 270 and 312°) and near-neutral stability for the atmospheric boundary layer conditions. The presented cases include data for wind speeds equal to 8 m/s (derived from the power output of a freestream turbine and the power curve of the machine). The ambient turbulence intensity, estimated from the M2 mast for a westerly inflow to the wind farm, is below 8 % for the considered wind speed [20].

Several wind sectors were investigated ($\pm 1^\circ$, $\pm 5^\circ$, $\pm 10^\circ$, $\pm 15^\circ$ around the main direction). Increasing the size of the sector allows to include a larger number of samples and to decrease the uncertainties. To some extent, the uncertainties also come from the wind direction estimation: many periods for wind direction measurements made at the mast M2 were erroneous from 2005 to 2007, which resulted in a lack of reliable data for the westerly inflow sector. The wind direction was thus estimated from the yaw position of a freestream wind turbine [21]. However, all the wind turbine yaw position signals lacked calibration and this hampered the estimation of the potential yaw position offset of the chosen reference wind turbine. Moreover, the uncertainties for the wind vane measurements were estimated to be larger than 5° [20]. The discrepancies between measurements and wake models or full-blown CFD are thus the highest for the narrowest wind sector [143].

We here decide to perform simulations for a westerly wind, with the main wind direction equal to 270° (see Fig. 7.1(a)). As a consequence, the turbine spacing is at its minimum (about 7D). In this chapter, we perform several LES with wind directions around 270° in order to compare the results with the observations of [20]. The wind direction step is set to 2° , from 262° to 278° , covering a wind sector of $\pm 9^\circ$ around 270° (see Figs. 7.1(a) and 7.1(b)). This step is chosen to save computational costs. As already mentioned, the final objective will consist in covering a wind sector of 30° ($\pm 15^\circ$) and the totality of the results will be submitted as a paper.

7.3 The Vestas wind turbine modelization

7.3.1 Aerodynamics/rotor

The wind farm comprises Vestas v80 wind turbines, characterized by a rated power of 2MW. The rotor diameter is equal to 80 m and the hub is located at a height of 70 m. It is a variable speed machine, regulated with pitch and

torque control schemes. The electrical power, P_e , and the thrust coefficient, C_T , are available in [62] and are reproduced in Fig. 7.2(a). The electrical power curve was determined from measurements on five machines, during unstable conditions and for two distinct, free sectors, and from their combination with wind speeds from the masts [19]. The variations between these power curves were very small, and so were the deviations of these data compared from the official power curves (2 – 3%). The derived mean curve was assumed to be representative for each of the 80 rotors in the wind farm, and typical of the on-site behavior of the machines.

The rotor speed and the pitch angle as a function of the wind speed are also available in many documents [62] and are presented in Fig. 7.2(b).

Although the detailed geometry of the blade is unknown, some geometrical and aerodynamic data have been disclosed in the literature. These parameters have been used in [143] in a BEM approach for creating a radial distribution of the twist angle and the chord length based on the aerodynamic of a single airfoil, the NACA 63-415 airfoil; this allows the creation of a wind turbine that produces power and thrust close to the expected ones. The chord and twist distributions along the blade, as well as the lift and drag coefficients of the airfoil are presented in Figs. 7.3. In [61], other parameters have been given for the Vestas wind turbine, with another specific radial distribution for the chord and the twist, and different airfoils according to the radial position. However, the aerodynamic data of these airfoils are not all available. We recall that the objective of this present study does not consist in representing all the aerodynamic features of the wind turbine but rather having a model that produces power and thrust close to the specifications. We thus decide to use the data available in [143].

For this work, we decide to decrease the resolution from 16 points per rotor diameter (the resolution used up to now) to about 12 points per rotor diameter. This results from a compromise between accuracy and efficiency. Indeed, the representation of the whole wind sector ($\pm 15^\circ$) will imply 15 simulations (we recall that we only present the results for wind sectors up to $\pm 9^\circ$ in this chapter). Again the resolution is chosen finer in the y direction, from the ground to a height slightly above the highest point of the disk, in order to have a few points between the lowest disk position and the height where the velocity is sampled for the wall modelling procedure.

AD simulations are then performed with a resolution of about 12 points per

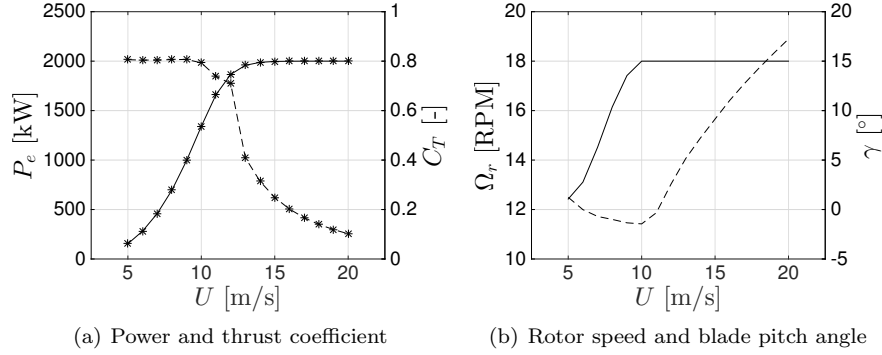


Figure 7.2 – VESTAS 2MW : left, the electrical power (solid line) and C_T (dashed line) curves for the Vestas v80 rotor. Right, the averaged rotor speed (solid line) and blade pitch angle (dashed line) as function of wind speed at hub height (data from [62]).

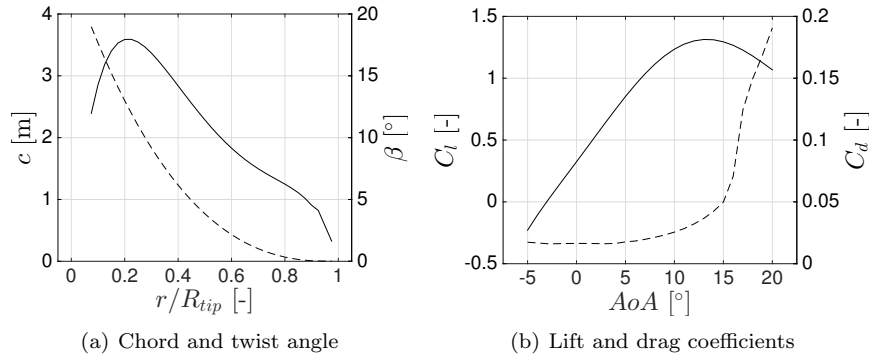


Figure 7.3 – VESTAS 2MW : chord (solid line) and twist angle (dashed line) data and lift (solid line) and drag (dashed line) coefficients (AoA is the angle of attack) (data from [143])

D in the z direction and about 24 points per D in the y direction ($\Delta x = \Delta z = 6.5$ m and $\Delta y = 3.75$ m) in order to verify the resulting rotor production. The pitch angles are adapted to match the P_e and C_T curves at that mesh resolution. The aerodynamic power produced by the AD (dashed line with circles in Fig. 7.4(a)) is multiplied by a constant mechanical efficiency of 0.939 in order for the AD electric power to agree with the reference electrical power curve. The thrust curve agrees with the specifications for a wind speed below 10 m/s, while there are small discrepancies above this wind speed. Indeed, with the used aerodynamic blade data, it was not possible to obtain a perfect fit with the manufacturer specifications for both electrical power and thrust. However, for the present investigations, the wind speed will not exceed 10 m/s,

and the geometry and pitch angles are thus assumed sufficiently accurate.

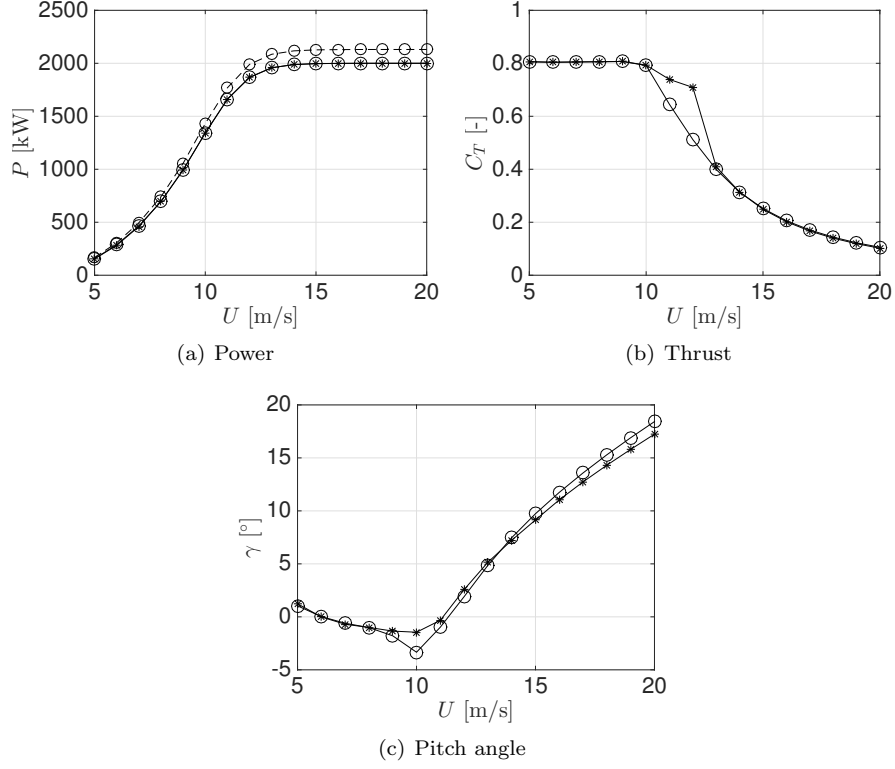


Figure 7.4 – VESTAS 2MW : AD results (lines with circles) and SCADA results (solid lines with stars). In the power curves, both aerodynamic (dashed line with circles), and electrical (solid line with circles) powers are shown for the AD method.

7.3.2 Controller

For most wind turbines in operation, the parameters of the controller constitute sensitive data and are not available for the Vestas turbine. We will here assume a classical control approach. As for most machines, there are three main regions of operation: the first region, in which the wind is too weak and the wind turbine is turned off, the second region for which the power is maximised, and the third region, where the power is regulated to the maximum value supported by the generator. Beyond the third region, the wind turbine shuts down to prevent loads and generator power from reaching damaging levels. In the second region, the machine is controlled via the generator torque, as usual for variable speed rotors. In the Vestas rotor, a blade pitch angle controller is

added in order to regulate the rotor speed above 10 m/s. Indeed, the machine speed reaches its rated rotor speed at 10 m/s while the power still increases (see Fig. 7.2): it is thus no longer possible to control the rotor only with the generator torque. Beyond 15 m/s, the generator torque and the rotor speed are maintained constant, by controlling the blade pitch angles, in a way to degrade the aerodynamic performances of the rotor.

It is planned to perform simulations characterized by a hub height velocity of 8 m/s and a TI lower than 8 %. The velocity will thus not exceed the rated wind speed and the controller will focus only on the second region of the power curve. We also limit the controller to the region for which the rotor speed varies (up to 10 m/s), making the implementation of the controller easier. We see that the pitch angle slightly varies between 5 and 10 m/s (see Fig. 7.2(b)); we will thus develop a simple pitch control in addition to the torque controller.

The global controller is presented in Fig. 7.5 and the systems involved in the wind turbine dynamics are described in the next subsections. The variables are functions of time; in the remainder of this section, the time dependencies of variables $x(t)$ are omitted for the sake of clarity.

Drivetrain dynamics

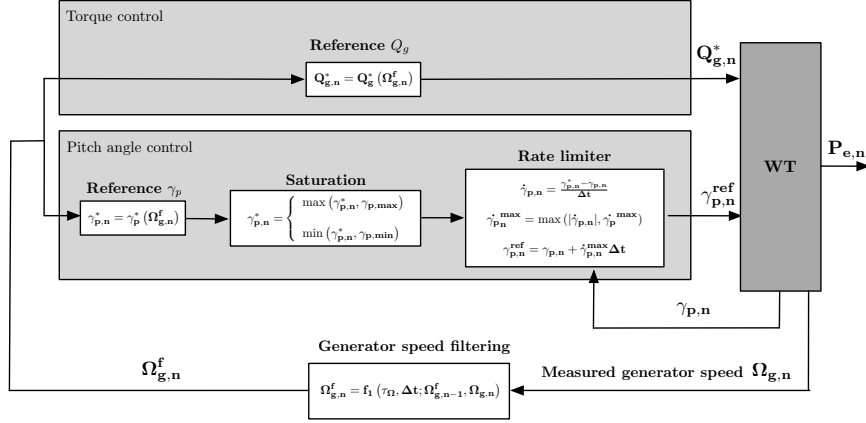
The rotor is assumed rigidly connected to the low-speed main shaft and this shaft is also rigidly coupled to the high-speed generator side via a gearbox. There is thus no representation of elastic or torsional effects. This can be represented with a first order system:

$$Q_{aero} - Q_g N_{gear} = I \dot{\Omega}_r \quad (7.1)$$

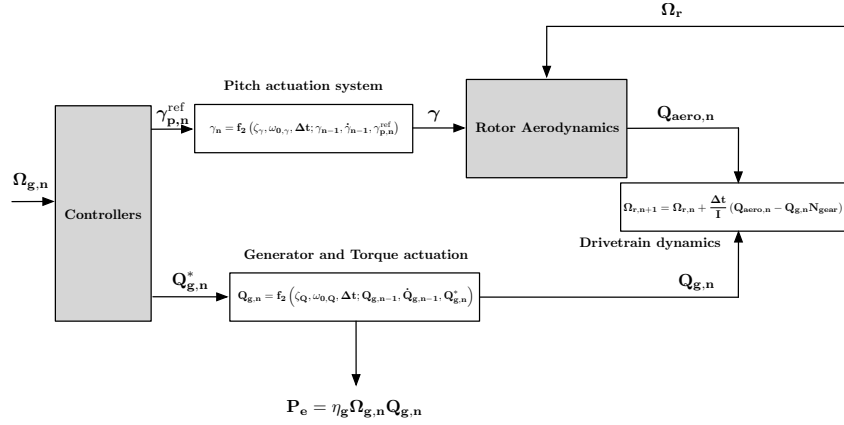
where Q_{aero} is the aerodynamic torque at the rotor, Q_g is the generator torque, N_{gear} is the speed ratio of the gearbox, and I is the total moment of inertia of the drivetrain. $(\dot{})$ corresponds to the time derivative $d()/dt$. The speed ratio N_{gear} is found to be 92.27 in the technical specifications of a gearbox intended for offshore Vestas v80 wind turbines [7]. I is defined as the “slow-speed shaft equivalent” inertia and corresponds to the rotor inertia, I_r , supplemented by the generator inertia, I_g , cast to the low-speed shaft:

$$I = I_r + N_{gear}^2 I_g \quad (7.2)$$

While the mass for a single blade is known for the Vestas v80 rotor ($\simeq 6$



(a) Pitch angle and torque controllers



(b) Sub-systems of the wind turbine model

Figure 7.5 – VESTAS 2MW : Diagram of the wind turbine model and controllers for the partial load operation.

500 kg [5]), its distribution along the blade is not specified. We will thus refer to [110] to obtain an estimation of the rotor inertia. In that work, the authors propose formulas for wind turbine mechanical constants, based on a set of existing wind turbines for which most of the parameters are well known and defined. The derived generic formula of the moment of inertia for one single blade is

$$I_b = k_j m_b L_b^2 \quad (7.3)$$

where m_b and L_b are the blade mass and length and $k_j \simeq 0.212$. The total in-

ertial for the considered rotor is thus about $6 \times 10^6 \text{ kg m}^2$, which is a reasonable value according to references for wind turbines in the same size range.

The specifications of a generator aimed at being used in Vestas v80 2MW wind turbines mention a moment of inertia of about 70 kg m^2 [6]. Including inertia from auxiliary systems, i.e. a mechanical disc brake fixed on the high-speed shaft after the gearbox, the total inertia on the high-speed shaft side can reach 150 kg m^2 for a 2 MW rotor [79]. Since the Vestas is equipped with such a system, we decide to take this last value. The total drivetrain inertia, cast to the low-speed shaft, then reaches a value of about $7.36 \times 10^6 \text{ kg m}^2$.

Generator speed and filtering

The traditional control approaches rely on a sensor measuring the generator speed, Ω_g , and this figure constitutes the sole feedback input. To mitigate possible high-frequency excitation of the control systems, Ω_g is filtered with a first-order low-pass filter. The continuous equation of this system is:

$$\tau_f \dot{\Omega}_g^f + \Omega_g^f = \Omega_g \quad (7.4)$$

where τ_f is the user-defined time constant of the system and Ω_g^f the filtered generator speed. The inertia of the Vestas v80 wind turbine is important and it already mitigates high-frequency fluctuations of the wind in the responses for the rotor and generator speed. The time constant does not have to be very large and we set it to 1 s, a quite arbitrary value which prevents any spurious oscillation.

The state and its time derivatives in Eq. (7.4) are approximated with the values of the current and previous time steps to be expressed and solved in a discrete form.

Torque controller and electric conversion

The torque controller aims at adapting Q_g as a function of the generator speed measurement. In classical controllers, the optimal Q_g is defined as $K^* \Omega_g^{f,2}$, where K^* is a constant value for the second region, depending on the wind turbine model and the $C_{p,max}$ and λ_{opt} of the machine. In our case, in order to exactly follow the power of Fig. 7.2(a), we will not use this relation and we will refer to a torque look-up table, previously established from the AD simulations (see Fig. 7.6):

$$Q_g^* = f(\Omega_g^f) \quad (7.5)$$

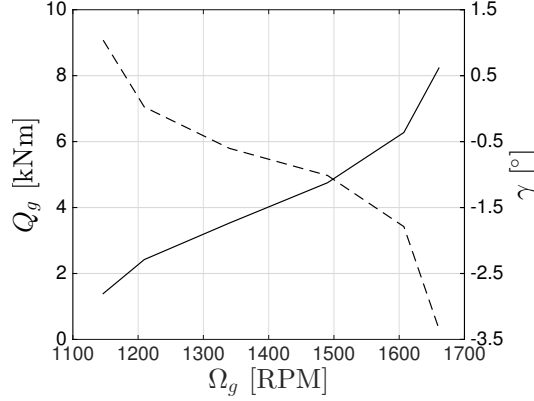


Figure 7.6 – VESTAS 2MW : Generator torque (solid line) and pitch angle (dashed line) as a function of the generator speed.

A dynamic system is here added to account for the electronic conversion. We represent the overall dynamic behavior from the set-point to the actual electric generator torque by a second order system:

$$\frac{1}{\omega_{0,Q}^2} \ddot{Q}_g + \frac{2\zeta_Q}{\omega_{0,Q}} \dot{Q}_g + Q_g = Q_g^* \quad (7.6)$$

where ζ_Q is the damping rate, and $\omega_{0,Q}$ ($= 2\pi f_{0,Q}$) is the natural system frequency. ζ_Q is fixed 0.7 and $f_{0,Q}$ is imposed to 0.6 Hz, according to [133]. Again, this equation is solved in a discrete form. Q_g is thus the final value imposed in the generator side. The electrical power is thus obtained:

$$P_e = \eta_g \Omega_g Q_g \quad (7.7)$$

where η_g is the efficiency of the generator, and, as already mentioned, is taken as 0.939 for fitting the AD values with the electrical power of the Vestas v80.

Pitch controller and pitch actuation systems

A simple controller is added to impose the required pitch angle value for the considered filtered generator speed. Again, we refer to a look-up table, obtained from AD simulations (see Fig. 7.6):

$$\gamma^* = f(\Omega_g^f) \quad (7.8)$$

We add saturation blocks to avoid exceeding the limits of the pitch angle positions and the maximal pitch angle speed; in partial load, the pitch angle speed does not exceed $0.8^\circ/\text{s}$, the minimal and maximal pitch angle positions are -4° and 35° , respectively. After those blocks, γ^* is denoted γ^{ref} . The Vestas wind turbine uses hydraulic pitch actuation systems [10]. The entire system comprises a comparator (the difference between the current pitch position and the pitch angle set-point), an actuator (here a hydraulic system) and a sensor allowing a feedback of the actual position after actuation. This procedure is here modelled by a second-order system:

$$\frac{1}{\omega_{0,\gamma}^2} \ddot{\gamma} + \frac{2\zeta_\gamma}{\omega_{0,\gamma}} \dot{\gamma} + \gamma = \gamma^{\text{ref}} \quad (7.9)$$

where $\omega_{0,\gamma} = 2\pi f_{0,\gamma}$ and γ is the resulting pitch angle. We use the parameters $f_{0,\gamma} = 0.8$ Hz and $\zeta_\gamma = 0.8$, as given in [63] for a 2MW wind turbine with a diameter of 80 m.

The variations of pitch angles for the considered wind speeds are very small. For actual rotor and pitch controller schemes, small pitch actions of the pitch actuation system are undesired, because they lead to considerable mechanical loads for the system [133]. There is thus often an inactivity zone, which ignores very small pitching actions. In the described controller, this has been omitted, as the objective consists in having a simple controller that leads to power production close to those given the Vestas specifications. However, adding this inactivity zone could be an improvement for accurate control modeling.

The controller is tested with a BEM method; indeed, this method offers a wide flexibility in the wind conditions and allows sharp changes in wind intensities. Figure 7.7(a) presents the turbine response for a two-step change in the wind. Figure 7.7(b) presents data for a turbulent inflow (the flow variations are imposed in the upstream velocity).

We clearly notice the effect of the rotor inertia, that smoothes the response of the rotor to the step changes, and the effect of the highest frequency wind fluctuations. As the controller works on a filtered version of the generator speed, the response in terms of generator torque and pitch angle are also smooth commands. For the aerodynamic values, however, there are sharp transitions as the rotor and its aerodynamics react quickly to the incoming wind.

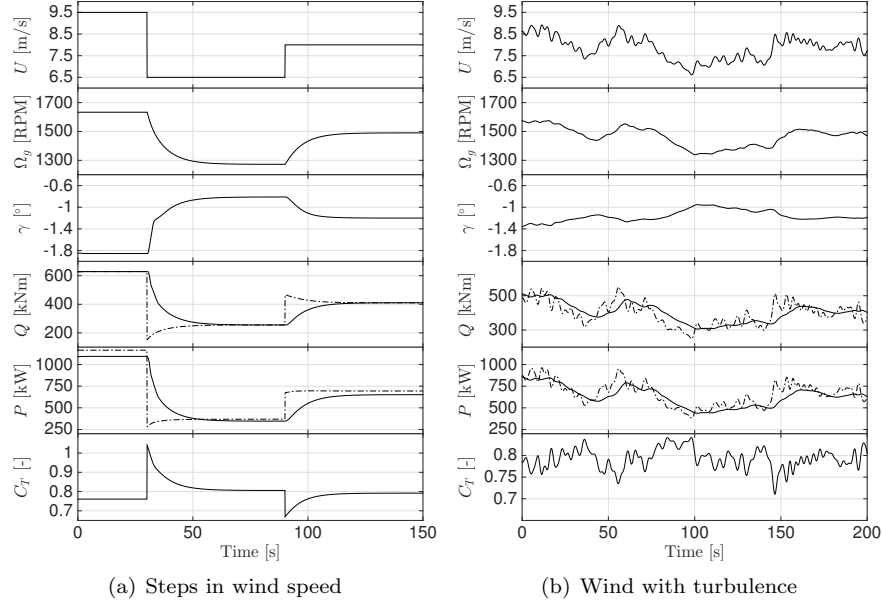


Figure 7.7 – VESTAS 2MW : response of the rotor to step changes in wind speed (left) and to turbulence fluctuations (right). From top to bottom : wind speed, generator speed, pitch angle, aerodynamic (dashed-line) and generator (solid line) torques, mechanical (dashed-line) and electrical (solid line) powers, thrust coefficient. These results are obtained with a BEM method.

The controllers are calibrated for the AD approach, the BEM method leads to slightly different results, which explains a thrust coefficient that is not exactly 0.8 for 9.5 m/s in Fig. 7.7(a).

7.4 Wind farm flow: two concurrent simulations

As mentioned, one global simulation implies two concurrent simulations. There is the main one that actually involves the wind turbines, while the auxiliary one is used for generating the inflow conditions. The methodology has been explained in Chapter 2.

7.4.1 Main simulation

The main simulation extends over to $L_x \times L_y \times L_z = 124.8D \times 12D \times 124.8D$, leading to horizontal lengths of 9984 m and a height of 960 m. This height

is close to that of similar simulations performed for this wind farm [143, 101]. The horizontal lengths are set to be large enough to avoid an effect of the boundaries on the flow velocities at the wind farm level (this has been verified for simulations in coarse resolutions, not shown here). The mesh is a $N_x \times N_y \times N_z = 1536 \times 128 \times 1536$ grid that is uniform in the wall-parallel directions ($\Delta x = \Delta z = 6.5\text{m}$) and that is partially stretched in the vertical direction. This stretching leads to uniform vertical grid below 150 m, with $\Delta y_{min} = 3.75$ m. Above that, Δy increases, to reach $\Delta y_{max} \simeq 20$ m at the top of the domain.

Instantaneous velocities are provided by a concurrent simulation (discussed in the next section, Section 7.4.2) and are imposed at each time step at the inlet boundary. A convective boundary condition is applied at the outflow, periodic boundary conditions are imposed in the z direction and no-through flow is enforced at the top. At the bottom, a wall stress model for a rough wall is applied, with a roughness y_0 set to 2 mm and $\kappa = 0.38$ (the equations were recalled in Chapter 6, see Eqs. (6.4) and (6.5)). The averaging procedure $\langle . \rangle$ in Eqs. (6.4) and (6.5)) is here a local averaging using the $n_x \times n_z = 5 \times 5$ closest points, as the simulations comprise the rotors and do not present homogeneous directions.

The SGS model is here a classical Smagorinsky model, with $C_s = 0.027$. The time step is fixed to $\Delta t = 0.125$ s. The total simulation time reaches 4056 s for each direction, i.e. more than hour, and the flow statistics are computed during the last 40 minutes. For all directions, the first wind turbine is located 30 rotor diameters from the inlet and the wind farm is globally centered in the middle of the domain.

7.4.2 Atmospheric Boundary layer

The domain size of this auxiliary simulation is equal to that used for the main simulation, but the resolution is slightly lower in the streamwise direction in order to save computational costs. Indeed, this simulation requires an important computational time to reach a statistically-converged state before it can be used concurrently with the main simulation. However, to avoid the need of an important adaptation length upstream of the wind farm, the difference of resolution between the two simulations has to remain small; the number of points is only decreased from 1536 points to 1280 points in the streamwise direction. This leads to a domain size of $L_x \times L_y \times L_z = 124.8D \times 12D \times 124.8D$ with $N_x \times N_y \times N_z = 1280 \times 128 \times 1536$. Again, the grid is uniform in the

wall parallel directions (Δx and Δz are constant, and equal to 6.5 m and 7.8 m, respectively) while the vertical direction is stretched according to the same procedure described in the previous section.

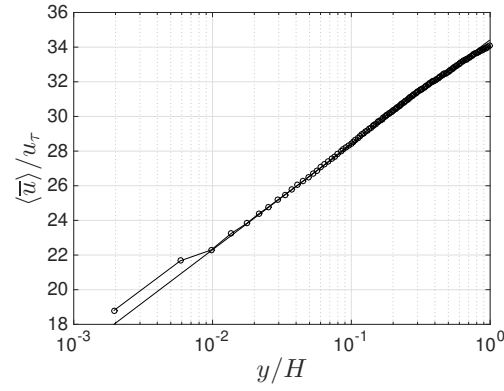
In that case, periodic boundary conditions are imposed in both x and z directions and the flow is driven by a variable pressure gradient. The forcing is adapted in order to conserve the mass flow, which allows to reach more rapidly the statistically-steady state. The wall model is the same as that described in the previous section, but $\langle \cdot \rangle$ of Eqs. (6.4) and (6.5) denotes a global average over the whole x and z directions. The SGS model is the same as that used for the main simulation, and the time step is also fixed to $\Delta t = 0.125$ s.

This simulation runs alone in a preliminary step, in order to reach a statistically-steady state. The resulting velocity profile is presented in Fig. 7.8(a), and compared to the logarithmic law. u_τ is here the time- and space-averaged value of the simulation, and is equal to 0.292 m/s. The resulting TI_u and TI are also presented (as defined in Chapter 6, see Eqs. (6.7) and (6.8)).

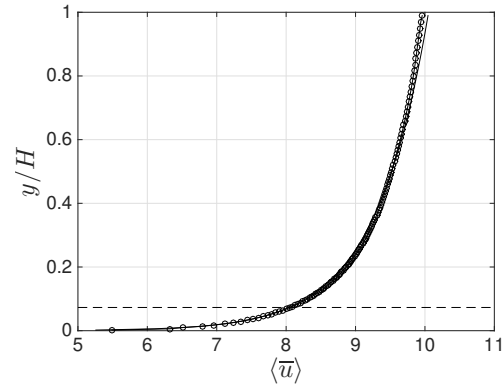
The precursor simulation captures the logarithmic law well, and leads to a mean streamwise velocity of 8 m/s at the hub height (see horizontal dashed-line in Fig. 7.9(a)). The turbulence intensity, depending on its definition, varies from 6.5 to 8.5 % (see Fig. 7.8(c)), which is close to the expected conditions for the Horns Rev wind farm.

7.4.3 Evolution of the flow statistics between the two domains

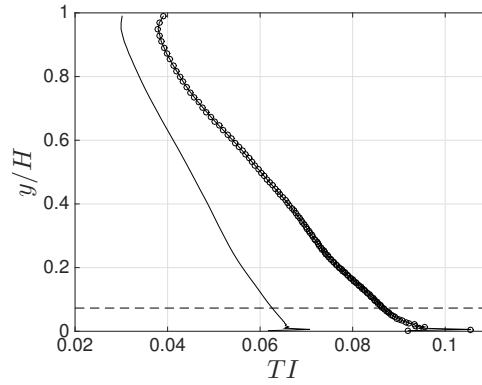
We first perform a complete simulation without wind turbines to investigate the evolution of the flow statistics at the different locations in the main simulation. Figure 7.9 shows the mean streamwise velocity, the turbulence intensities TI and TI_u for the initial profiles and at three streamwise locations: at $x/D = 30$ (where the first wind turbine is located), $x/D = 60$ and far downstream, $x/D = 120$. The profiles remain very close to the initial one although we notice a slight evolution of the statistics.



(a) $\langle \bar{u} \rangle$ (solid line with dots), compared with the logarithmic law (solid line)



(b) $\langle \bar{u} \rangle$ (solid line with dots), compared with the logarithmic law (solid line)



(c) TI_u (solid line with dots) and TI (solid line)

Figure 7.8 – ABL : Time-and horizontally-averaged streamwise velocity and turbulence statistics for the ABL precursor simulation. The horizontal dashed line represents the hub height, H is the height of the domain.

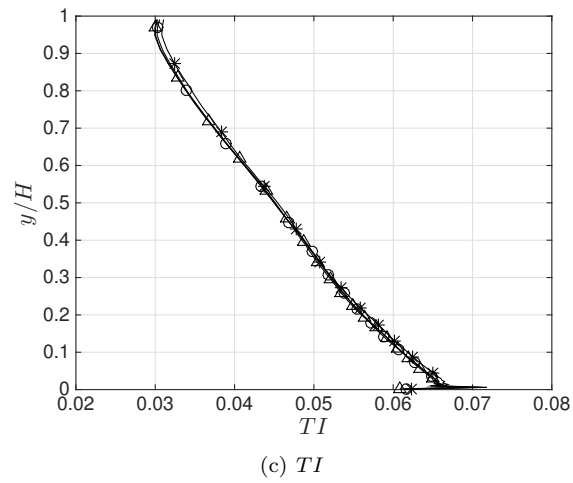
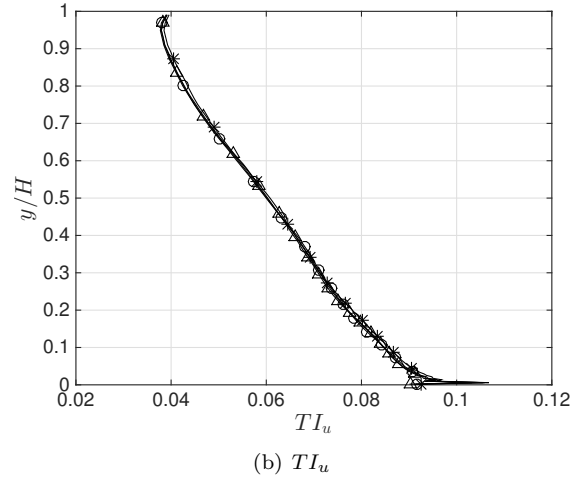
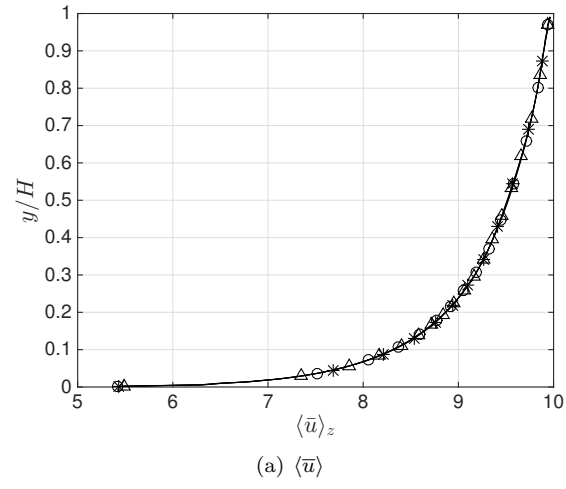


Figure 7.9 – Horns Rev simulations: Time- and horizontally-averaged streamwise velocity, TI_u , TI for the ABL simulation (solid line) and three streamwise locations inside the main domain: $x/D = 30$ (solid line with triangles), $x/D = 60$ (solid line with circles) and $x/D = 120$ (solid lines stars). H is the height of the domain.

7.5 Results

7.5.1 Wake topology and features

Instantaneous velocities

Horizontal planes of instantaneous streamwise velocities at hub height are presented in Fig. 7.10 for the four wind directions: 262° , 266° , 270° and 274° (see Fig. 7.1(b) for the visualization of these wind directions). Those are chosen to explain the main phenomena and differences existing between a wind farm with rotors perfectly aligned or in a somewhat staggered arrangement.

We first clearly notice an evolution in the wakes with the streamwise location for all four directions. The first row produces structured wakes, that remain stable on a relatively large distance downstream of the machine. This is highlighted by zooming on the first wind turbines of the line L4 (see Fig. 7.11(a)). The hub jet is distinguishable and the wakes appear to start to oscillate from about one to two rotor diameters downstream of the machines. The amplitude and the frequency of the wake meandering are relatively moderate. The wavelength can be estimated from Fig. 7.11(a), and is about three to four rotor diameters, while the amplitude does not exceed one diameter around the centerline of the rotor. For machines located downstream in the wind farm, the wakes are less structured, more turbulent, and the hub jet is noticeable only for a very short distance behind the rotor. The wake physics of local machines become more complex, as there are interactions and merging events between the wakes. The amplitude of oscillations is higher, and the wavelength also seems more important, about five to six rotor diameters (see Fig. 7.11(b) for a zoom around the last wind turbines of line L4). This however should be confirmed by more accurate wake meandering investigations, such as those performed for the 15 machines wind farm and presented in [44]. In that work, wake meandering was investigated by tracking the minimal available power in the wake regions, and the first results clearly highlighted an increase of the amplitude of oscillations for downstream wakes.

For $\alpha_w = 270^\circ$, the wind farm wakes remain concentrated in lines. When the rotors are no longer perfectly aligned, the wake shed by each machine partially immerses the downstream machine and its wake, leading to noticeably wider wakes after a few rows. This will be better highlighted when the time-averaged streamwise velocities will be studied.

In the flow, we notice the presence of significant elongated low- and high

speed structures aligned with the flow. Those structures have been observed in other LES of wind farm [37] and are characteristic of a neutral ABL, as there are no buoyancy-induced vertical motions to break these streamwise structures. We will see that these structures are long-lived, and their trace is still noticeable even when the flow is temporally averaged.

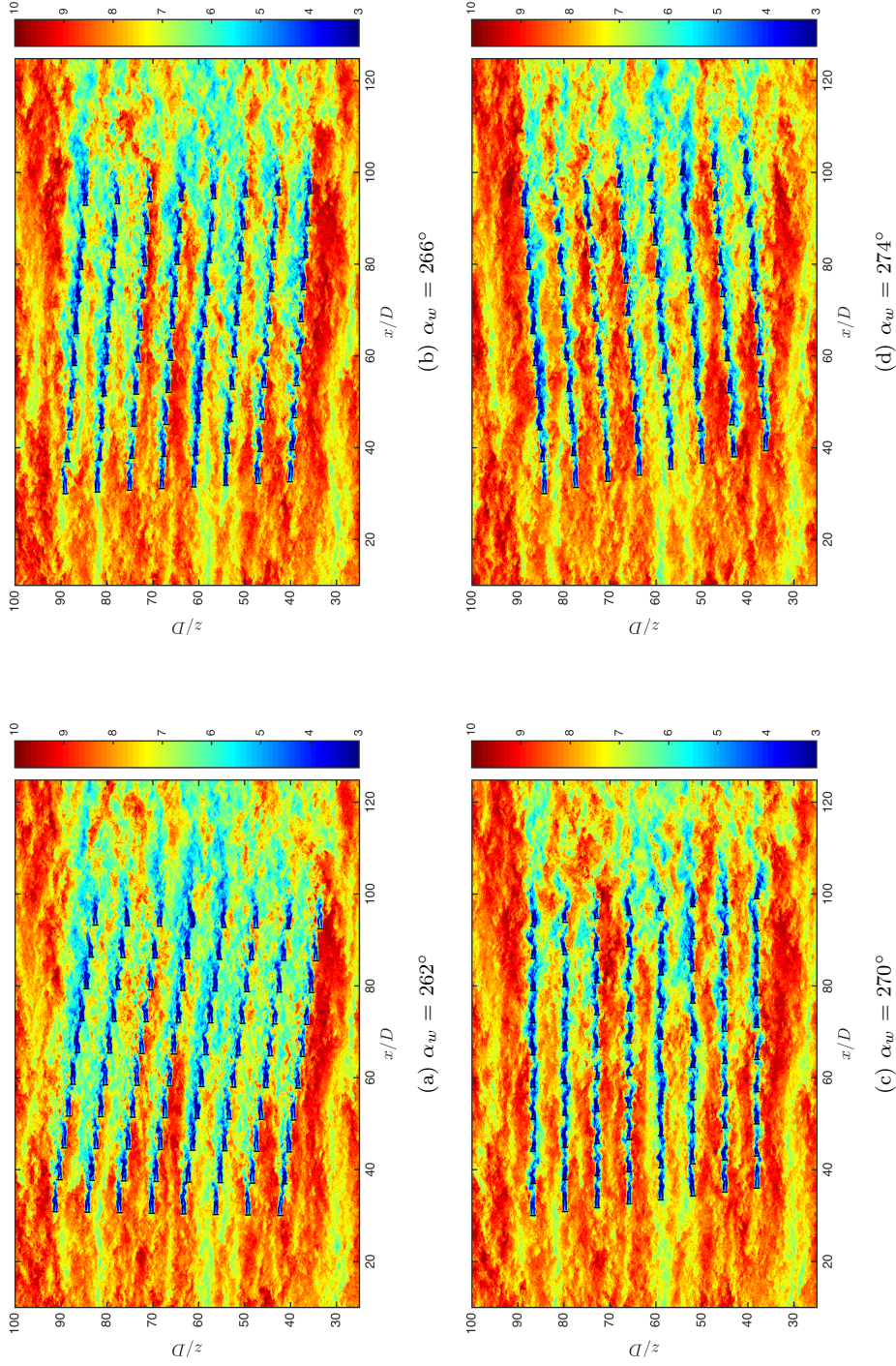


Figure 7.10 – Horns Rev simulations: Instantaneous streamwise velocity on a horizontal plane at hub height for different incoming wind directions. The units are in m/s.

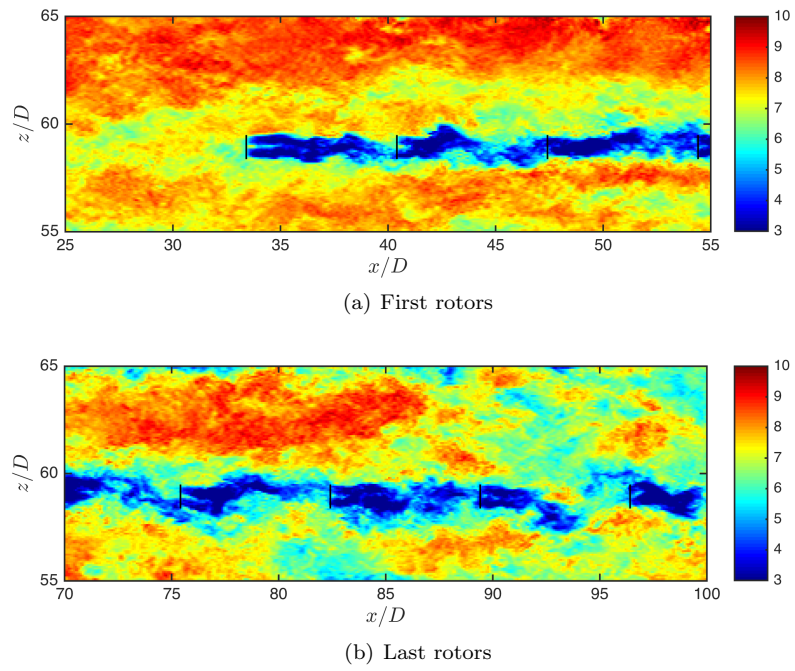


Figure 7.11 – Horns Rev simulations, $\alpha_w = 270^\circ$: Zoom of instantaneous streamwise velocity on a horizontal plane at hub height for the first wind turbines in a line, and for the last wind turbines in a line. The units are in m/s.

Time-averaged velocities and turbulence intensities

Hub height slices of time-averaged streamwise velocities and turbulence intensities are presented in Figs. 7.12 and 7.13 for the same wind directions. The turbulence intensity is defined based on the streamwise fluctuations, and normalized by the unperturbed time-averaged velocity at hub height.

We first study the wind direction $= 270^\circ$. As observed in the instantaneous snap-shot, the first row of wind turbines produces very stable wakes, which remain structured on relatively large distances downstream of the machines (see Fig. 7.12(c)). The second row is thus impacted by a flow with low velocities and is expected to produce less power. In terms of turbulence intensity, in agreement with wake investigations performed in Chapter 5, the wakes of the first row of turbines increase the turbulence intensity compared to the incoming wind. The highest levels of turbulence are observed on the sides of the wake, a region characterized by its important shear. There is also a small increase of turbulence at the hub location. The second row of turbines is impacted by a higher levels of turbulence, due to the upstream wakes. The wakes destabilise on a much shorter distance, allowing an improved mixing between the wake and the outside flow. This improved wake recovery leads to a lower velocity deficit upstream of the third row. The level of turbulence also decreases before impacting the third row. As a consequence, the machines in the third row produce slightly more. Beyond the third row, it is more challenging to distinguish differences between the wakes; this will be better highlighted when the velocity profiles in vertical planes will be studied in the next section. However, we clearly notice that the global wake width of each line increases along the streamwise direction; this is noticeable for the velocities but also for the level of turbulence.

For the wind directions of 266° and 274° , a similar behavior is observed. However, as the downstream rotors are not fully impacted by the upstream wake, the second row is expected to produce more. Due to the misalignment of the rotors, the global wake width of each line is larger. In terms of the turbulence intensities, the wakes present some asymmetry from the second row. Indeed, the shear is less important on the side where the rotor is immersed in the upstream wake, leading to a lower level of turbulence. The level of turbulence in the wakes of the downstream machines is relatively lower compared to the one obtained for $\alpha_w = 270^\circ$.

For $\alpha_w = 262^\circ$, the rotors in the first few rows within the farm sit nearly

outside of the wakes of upstream turbines, the power loss will be thus considerably reduced for these machines. From the rows R_5 - R_6 on, the rotors in a line begin to be impacted by the wakes of the adjacent line; only the line L_8 is not affected by the wakes of the other lines.

A final global remark than can be made for the four directions is that there are some variations between lines due to ambient incoming wind: as mentioned in the previous section, some regions of the wind farm are impacted by durable lull or gusts. For example, the line L_5 is impacted by a slower wind and the wakes present lower turbulence intensities (see Fig. 7.1(a) for references to lines and rows)). The mean production is thus expected to vary according to the line.

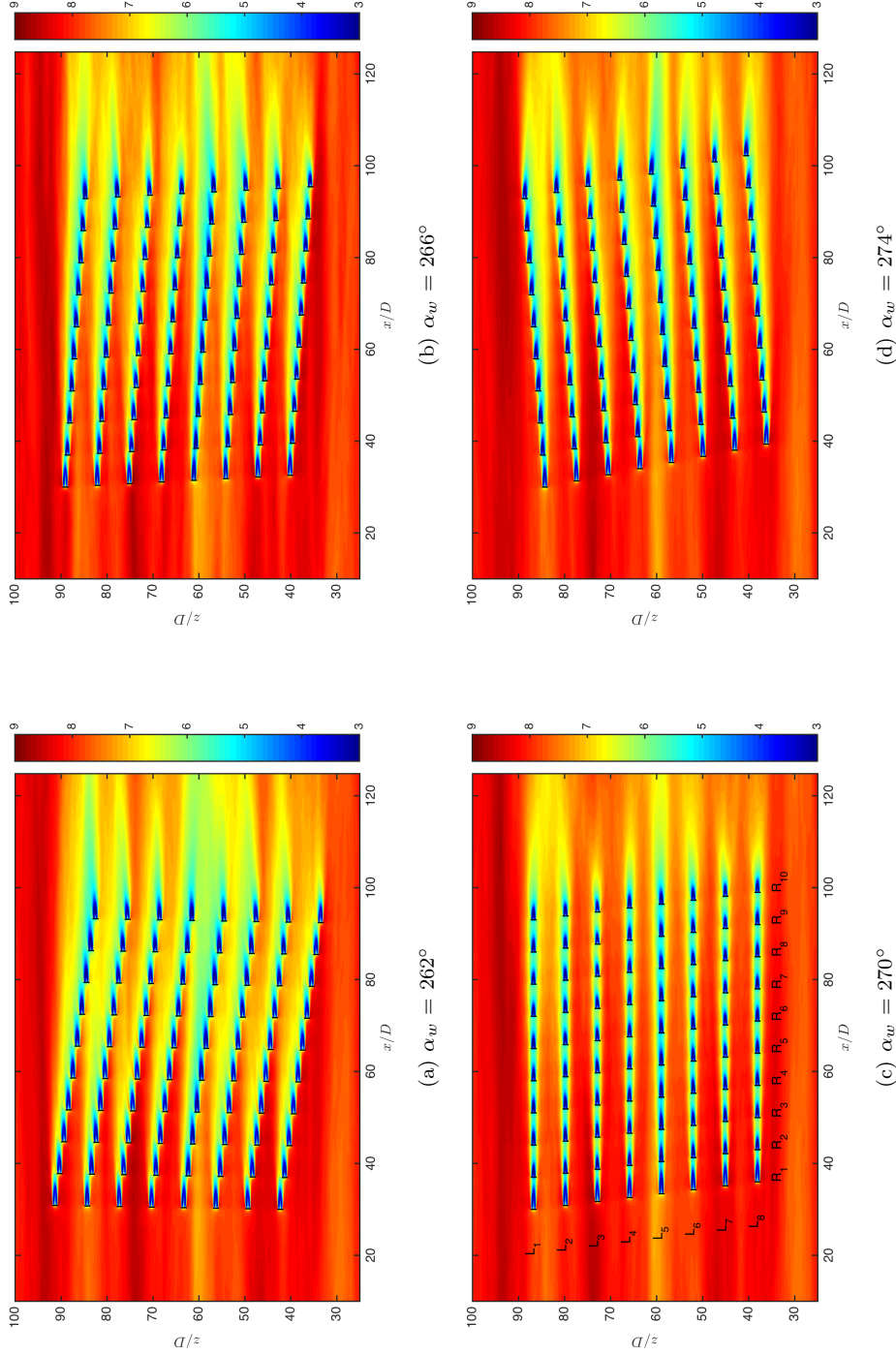


Figure 7.12 – Horns Rev simulations: Time-averaged streamwise velocity on a horizontal plane at hub height for different incoming wind directions. The units are in m/s.

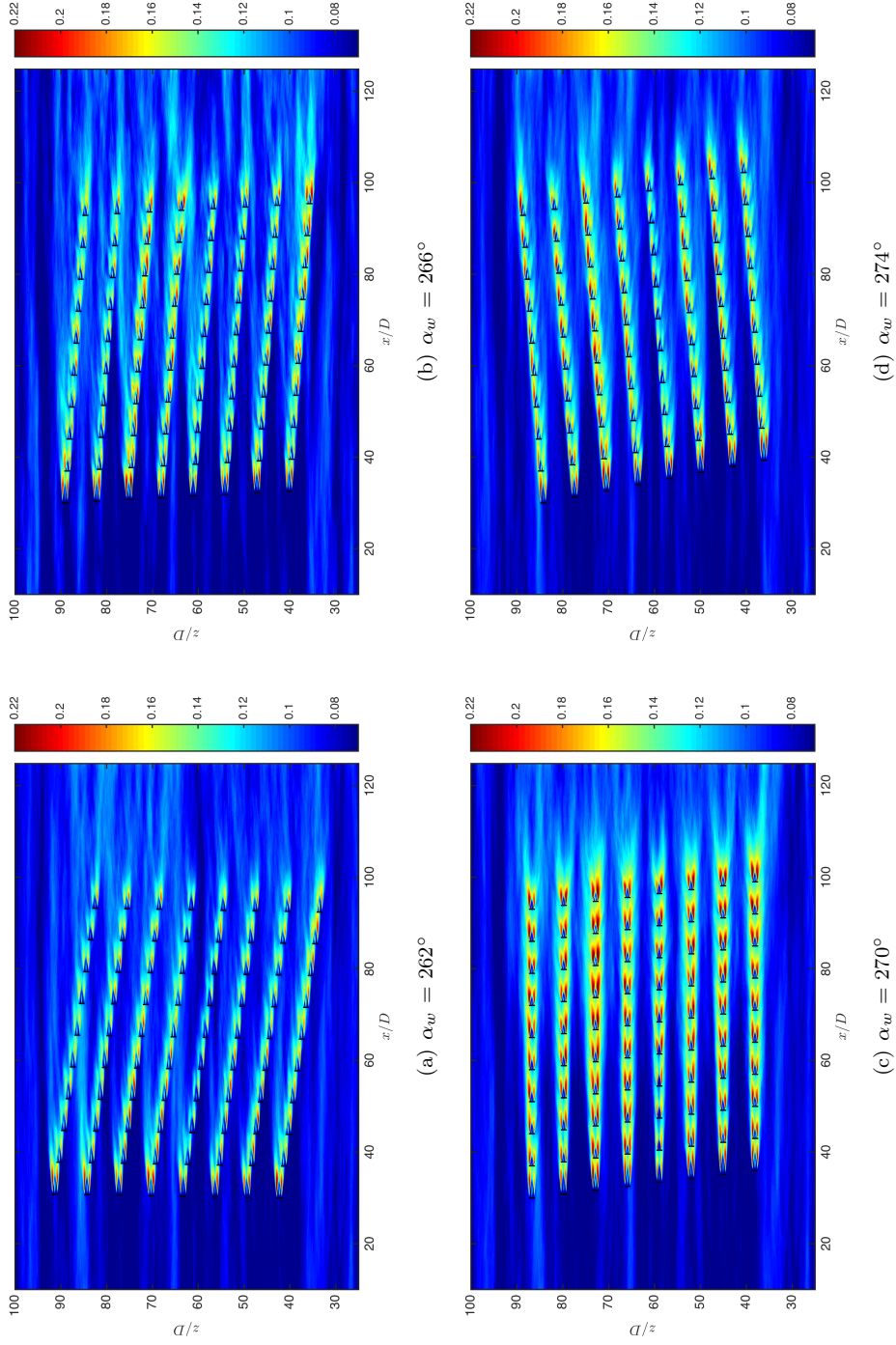


Figure 7.13 – Horns Rev simulations: streamwise turbulence intensity on a horizontal plane at hub height for different incoming wind directions.

7.5.2 Flow statistics

The Figures 7.15 to 7.25 present the vertical profiles of the time-averaged streamwise velocity and turbulence intensity for each direction. The profiles are shown for several streamwise locations, centered in the middle of the line L_4 (assumed to be representative of the global behavior inside the wind farm). The data are presented for seven rotors through the line (rotors 1 to 5, 7 and 10) and the streamwise locations are $x/D = -1, 1, 3$ and 5 , relatively to the position of the considered rotor ($x/D = 0$ is the position of the considered rotor i).

The time-averaged power production for each wind turbine of the line, normalized by that produced by the first rotor is also presented (see Figs. 7.14, 7.17 7.20 and 7.23). In that way, the difference observed in terms of velocity profiles will be highlighted in terms of the production of each rotor.

As noticed during the discussion of Fig. 7.12, the first rotor is impacted by the freestream flow, which presents a relatively low ambient turbulence. The wake remains structured over a larger distance, with a high velocity deficit (compared to that of other machines at the same relative positions). The level of turbulence only increases further downstream at which point the mixing between the wake and the ambient flow is enhanced. A more general remark is that the turbulent intensity is the highest in the upper part of the wake, at the blade tip level. Indeed, in that region, the shear between the wake velocity and the freestream flow is important. Conversely, it is reduced at the lowest part of the wake, as the rotor is impacted by a lower velocity due to the incoming shear flow.

The average velocity profile impacting the second wind turbine exhibits a pronounced reduction at hub height. However, for $\alpha_w = 266$ and 274 in Figs. 7.18(a) and 7.24(a), we observe that the wind speed impacting the second machine of the line is higher; this corresponds to a wind sampling that is not perfectly centered in the wake coming from the upstream machine. The rotor is only partially immersed in the wake of the upstream rotor (on average) and, to some degree, is still exposed to the ambient flow; this leads to a lower power deficit, as it is highlighted in the power production for these two wind directions: $\bar{P}_{4,2}^{270^\circ} / \bar{P}_{4,1}^{270^\circ} \simeq 0.50$ for $\alpha_w = 270^\circ$ against approximately 0.7 and 0.69 for $\alpha_w = 266$ and 274° , respectively ($\bar{P}_{l,r}^\alpha$ denotes the time-averaged power for the line l , the row r and the wind direction α .)

The profiles of these three directions also offer some insights into the asymp-

otic equilibration of the power productions of rows inside the farm. It can indeed be seen as the result of the balance between (1) the velocity deficit due to the power extraction, (2) the ambient and wake turbulence, and (3) the resulting recovery which can take place before the next row. We can indeed see that the second rotor in the line is impacted by a lower velocity and a sensibly more intense turbulence. Hence, its wake is less coherent and it recovers over a shorter distance: unlike the first machine, the hub jet is no longer identifiable at $x/D = 1$. However, the turbulence that hits that third machine is sensibly lower than the previous row and it causes the wake to dissipate more slowly. As a result, the power of the fourth row machine is slightly lower. Further into the farm, a balance can be seen to be then reached, leading to a power production plateau beyond the fifth row. The row misalignment observed for $\alpha_w = 266^\circ$ has a relatively small effect on this asymptotic balancing. There is only an effect on the level of the power plateau: as discussed earlier, the rotors are not fully impacted by the upstream wakes. For $\alpha_w = 274^\circ$, the behavior should be similar to the direction 266° . However, an increase of power production is observed for rotor R_6 . This may be due to the durable gusts observed in the flow, which impacts this machine when the rotors are misaligned.

For the case $\alpha_w = 262^\circ$, the second rotor in the line is nearly outside the wake of the first wind turbine and the average inflow for this machine is just slightly lower than the ambient flow; the power loss between the first and the second rotors is thus relatively small, with $\bar{P}_{4,2}^{262^\circ} / \bar{P}_{4,1}^{262^\circ} \simeq 0.86$. When we consider rotors deeper in the wind farm, they are impacted by rotor wakes of the line 5, with a higher velocity deficit. A plateau is not visible anymore and the power loss increases, with $\bar{P}_{4,10}^{262^\circ} / \bar{P}_{4,1}^{262^\circ} \simeq 0.55$ at the end of the line.

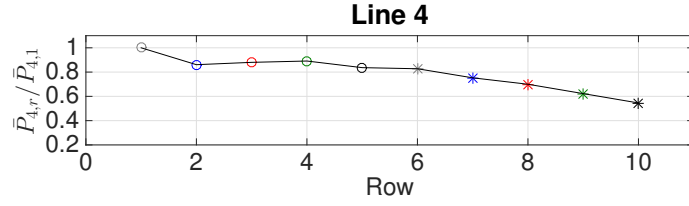


Figure 7.14 – Horns Rev simulations: Normalized mean power output data for the line L4 for the incoming wind direction of 262° .

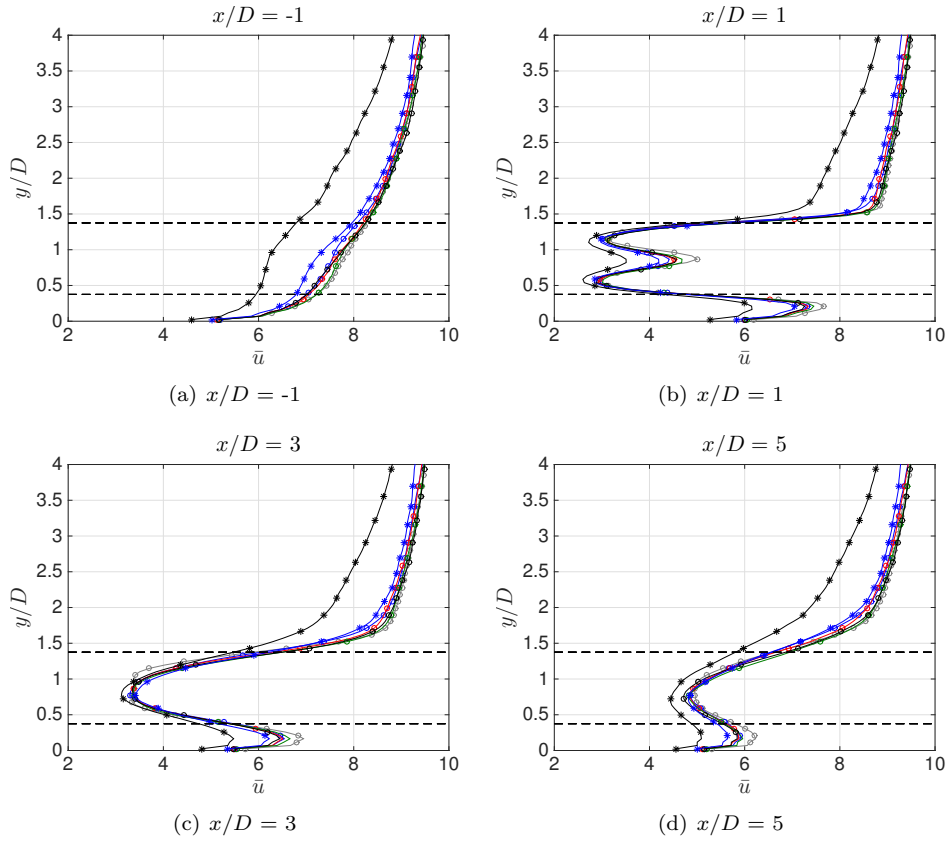


Figure 7.15 – Flow statistics, $\alpha_w = 262^\circ$: Time-averaged streamwise velocity profiles at different upstream and downstream locations for 7 wind turbines in line L4: rotor 1 (grey line with circle), 2 (blue line with circles), 3 (red line with circles), 4 (green line with circles), 5 (black line with circles), 7 (blue line with stars) and 10 (black line with stars). The profiles are taken in the vertical direction, centered in the spanwise rotor position. x/D is relative to the considered wind turbine. The horizontal dashed lines indicate the bottom and the top of the turbine rotor. For sake of clarity, the markers of the curves are spaced and do not correspond to the actual resolution.

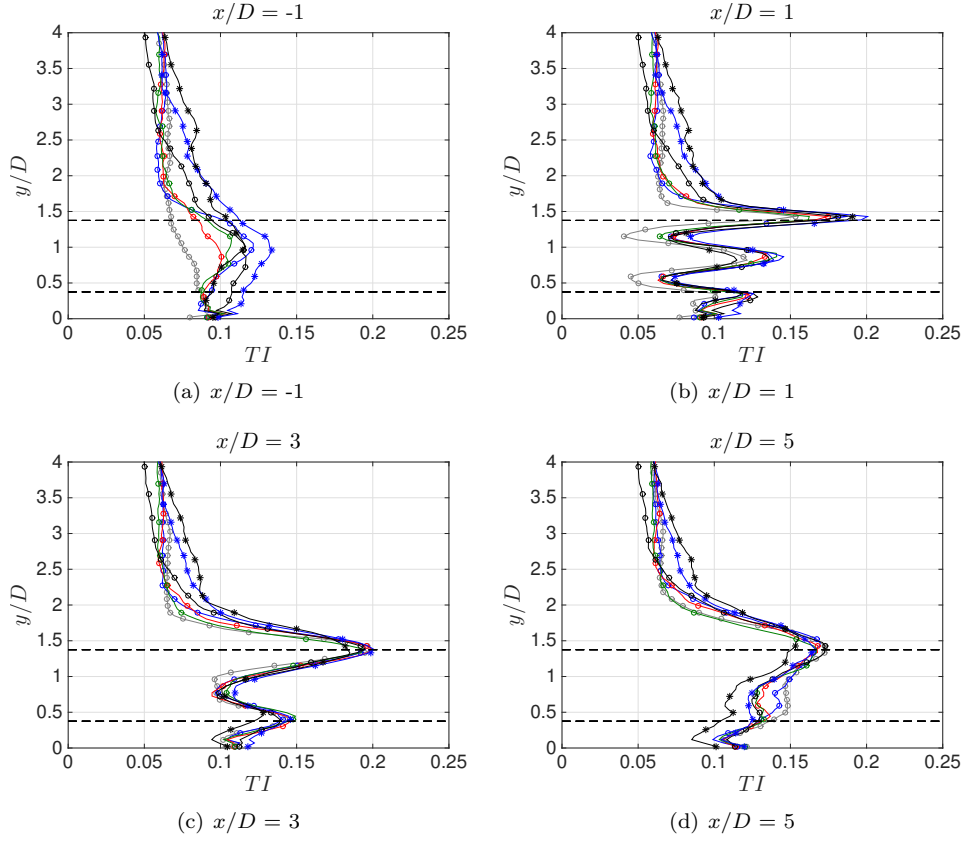


Figure 7.16 – Flow statistics, $\alpha_w = 262^\circ$: Streamwise turbulence intensity profiles (normalized with the mean incoming hub velocity at the first rotor) at different upstream and downstream locations for 7 wind turbines in line L4: rotor 1 (grey line with circle), 2 (blue line with circles), 3 (red line with circles), 4 (green line with circles), 5 (black line with circles), 7 (blue line with stars) and 10 (black line with stars). The profiles are taken in the vertical direction, centered in the spanwise rotor position. x/D is relative to the considered wind turbine. The horizontal dashed lines indicate the bottom and the top of the turbine rotor. For sake of clarity, the markers of the curves are spaced and do not correspond to the actual resolution.

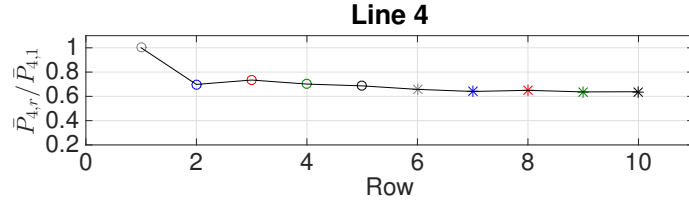


Figure 7.17 – Horns Rev simulations: Normalized mean power output data for the line L4 for the incoming wind direction of 266° .

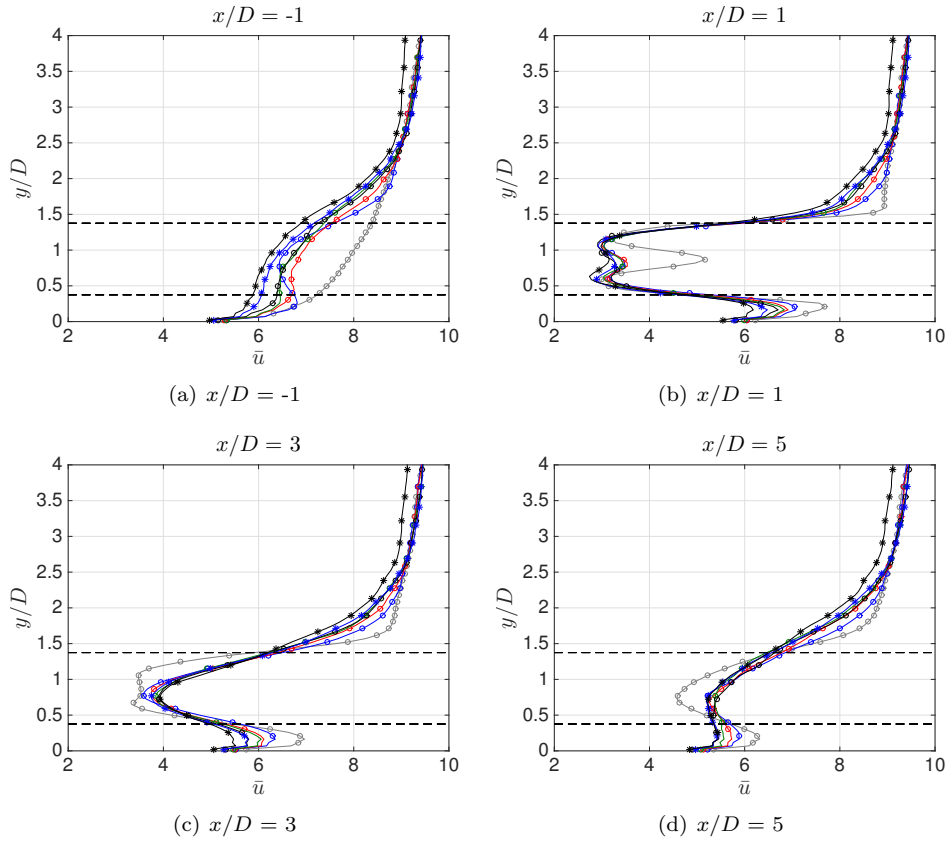


Figure 7.18 – Flow statistics, $\alpha_w = 266^\circ$: Time-averaged streamwise velocity profiles at different upstream and downstream locations for 7 wind turbines in line L4: rotor 1 (grey line with circle), 2 (blue line with circles), 3 (red line with circles), 4 (green line with circles), 5 (black line with circles), 7 (blue line with stars) and 10 (black line with stars). The profiles are taken in the vertical direction, centered in the spanwise rotor position. x/D is relative to the considered wind turbine. The horizontal dashed lines indicate the bottom and the top of the turbine rotor. For sake of clarity, the markers of the curves are spaced and do not correspond to the actual resolution.

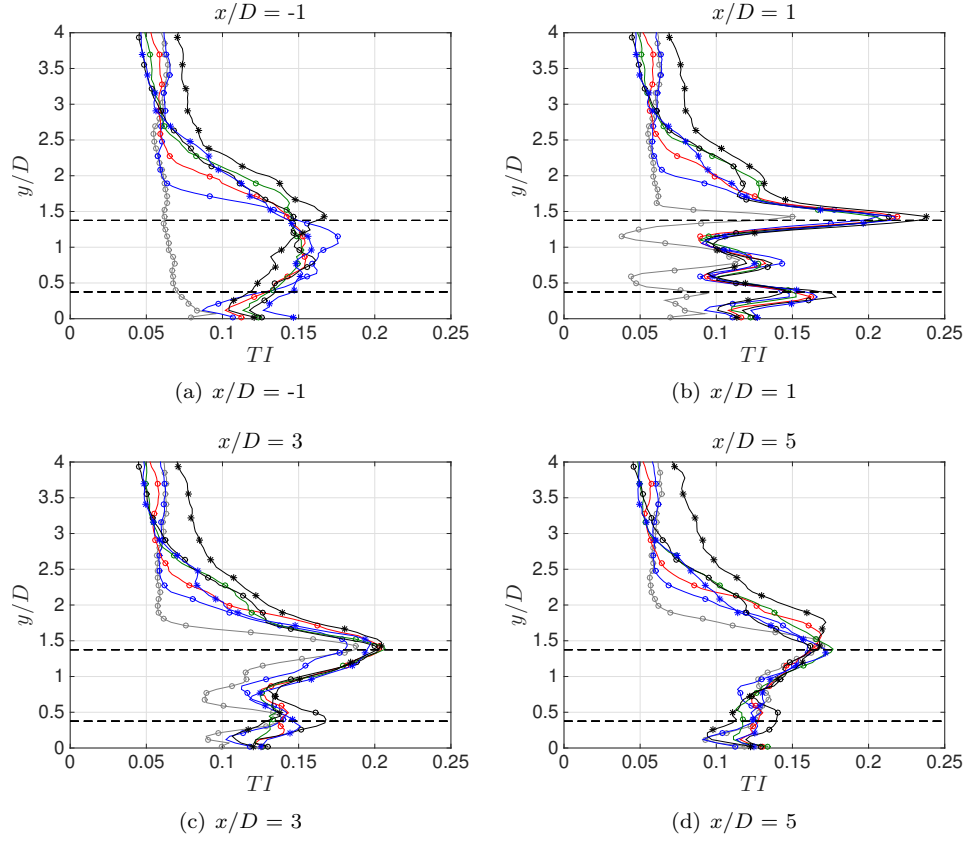


Figure 7.19 – Flow statistics, $\alpha_w = 266^\circ$: Streamwise turbulence intensity profiles (normalized with the mean incoming hub velocity at the first rotor) at different upstream and downstream locations for 7 wind turbines in line L4: rotor 1 (grey line with circle), 2 (blue line with circles), 3 (red line with circles), 4 (green line with circles), 5 (black line with circles), 7 (blue line with stars) and 10 (black line with stars). The profiles are taken in the vertical direction, centered in the spanwise rotor position. x/D is relative to the considered wind turbine. The horizontal dashed lines indicate the bottom and the top of the turbine rotor. For sake of clarity, the markers of the curves are spaced and do not correspond to the actual resolution.

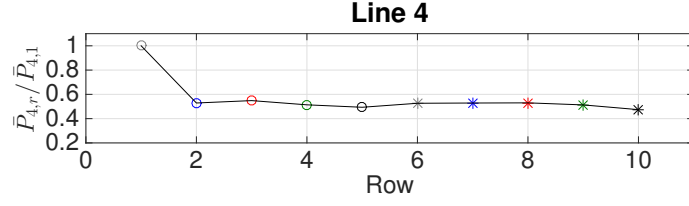


Figure 7.20 – Horns Rev simulations: Normalized mean power output data for the line L4 for the incoming wind direction of 270° .

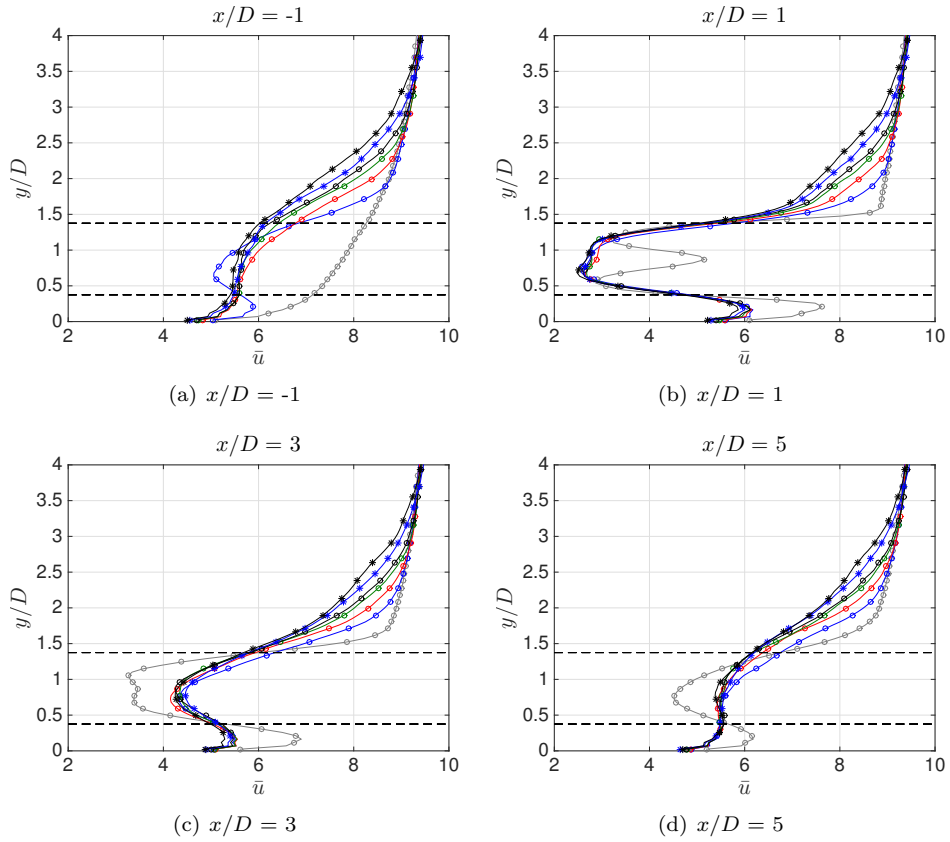


Figure 7.21 – Flow statistics, $\alpha_w = 270^\circ$: Time-averaged streamwise velocity profiles at different upstream and downstream locations for 7 wind turbines in line L4: rotor 1 (grey line with circle), 2 (blue line with circles), 3 (red line with circles), 4 (green line with circles), 5 (black line with circles), 7 (blue line with stars) and 10 (black line with stars). The profiles are taken in the vertical direction, centered in the spanwise rotor position. x/D is relative to the considered wind turbine. The horizontal dashed lines indicate the bottom and the top of the turbine rotor. For sake of clarity, the markers of the curves are spaced and do not correspond to the actual resolution.

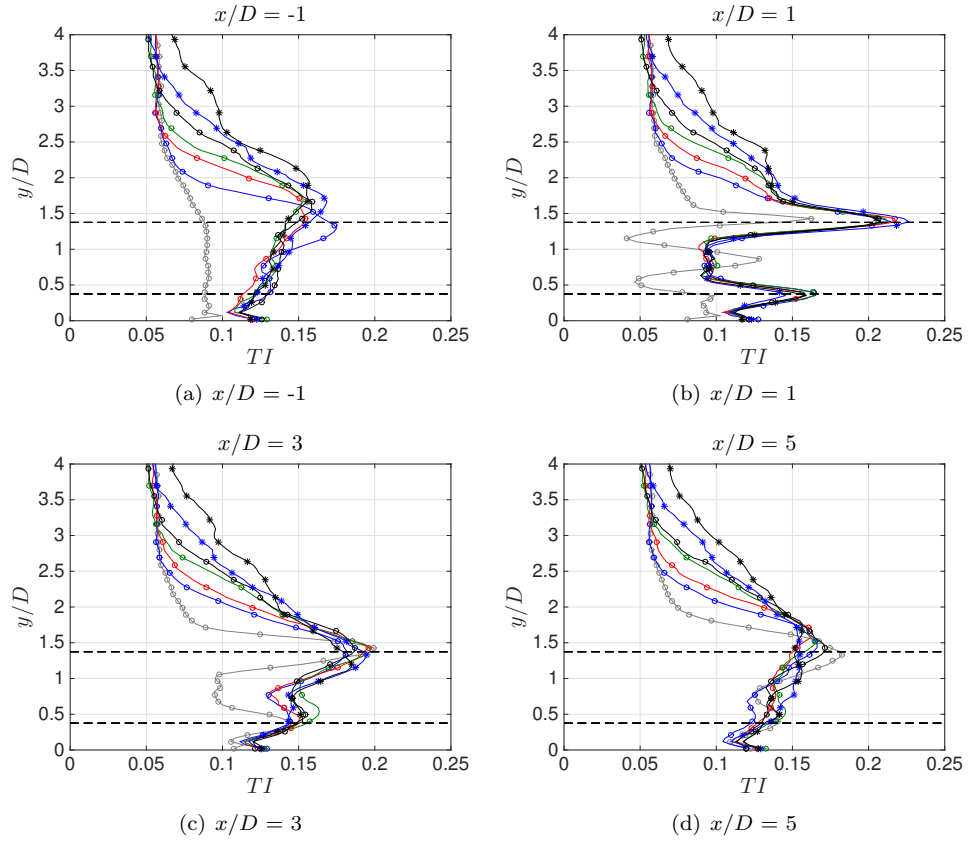


Figure 7.22 – Flow statistics, $\alpha_w = 270^\circ$: Streamwise turbulence intensity profiles (normalized with the mean incoming hub velocity at the first rotor) at different upstream and downstream locations for 7 wind turbines in line L4: rotor 1 (grey line with circle), 2 (blue line with circles), 3 (red line with circles), 4 (green line with circles), 5 (black line with circles), 7 (blue line with stars) and 10 (black line with stars). The profiles are taken in the vertical direction, centered in the spanwise rotor position. x/D is relative to the considered wind turbine. The horizontal dashed lines indicate the bottom and the top of the turbine rotor. For sake of clarity, the markers of the curves are spaced and do not correspond to the actual resolution.

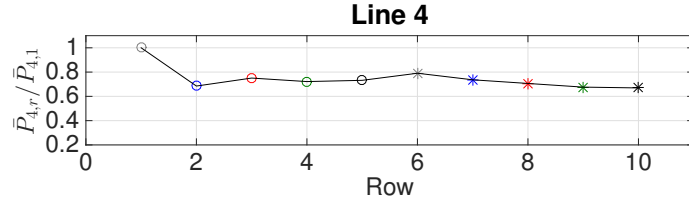


Figure 7.23 – Horns Rev simulations: Normalized mean power output data for the line L4 for the incoming wind direction of 274° .

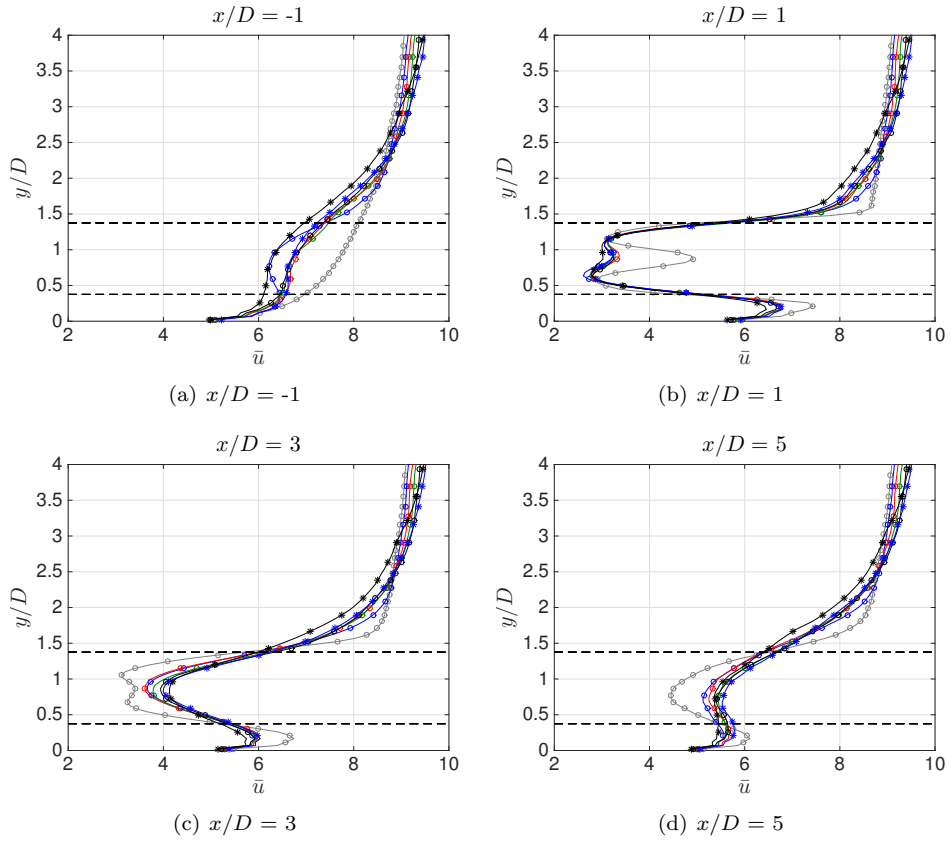


Figure 7.24 – Flow statistics, $\alpha_w = 274^\circ$: Time-averaged streamwise velocity profiles at different upstream and downstream locations for 7 wind turbines in line L4: rotor 1 (grey line with circle), 2 (blue line with circles), 3 (red line with circles), 4 (green line with circles), 5 (black line with circles), 7 (blue line with stars) and 10 (black line with stars). The profiles are taken in the vertical direction, centered in the spanwise rotor position. x/D is relative to the considered wind turbine. The horizontal dashed lines indicate the bottom and the top of the turbine rotor. For sake of clarity, the markers of the curves are spaced and do not correspond to the actual resolution.

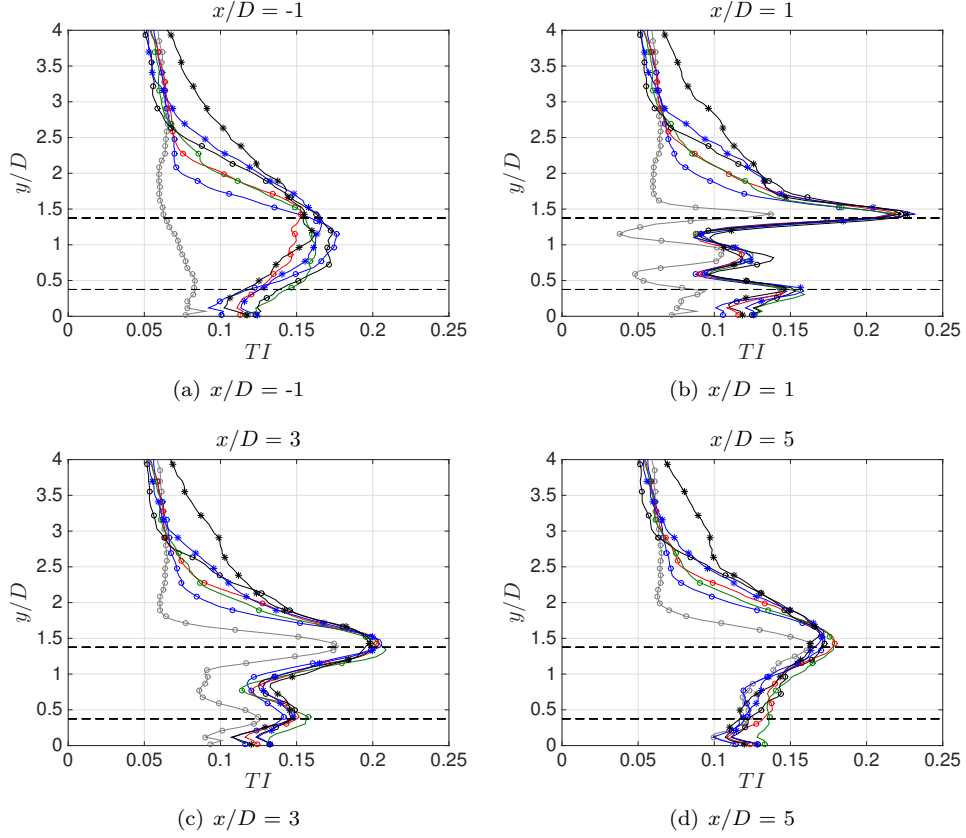


Figure 7.25 – Flow statistics, $\alpha_w = 274^\circ$: Streamwise turbulence intensity profiles (normalized with the mean incoming hub velocity at the first rotor) at different upstream and downstream locations for 7 wind turbines in line L4: rotor 1 (grey line with circle), 2 (blue line with circles), 3 (red line with circles), 4 (green line with circles), 5 (black line with circles), 7 (blue line with stars) and 10 (black line with stars). The profiles are taken in the vertical direction, centered in the spanwise rotor position. x/D is relative to the considered wind turbine. The horizontal dashed lines indicate the bottom and the top of the turbine rotor. For sake of clarity, the markers of the curves are spaced and do not correspond to the actual resolution.

7.5.3 Comparisons with measurements and other LES results

We here compare the time-averaged power output of the present results to available observations in the reference paper of Barthelmie *et al.* [20]. The results are moreover compared to an other set of LES performed by Wu and Porté-Agel [143]. The Horns Rev measurements, the different LES, as well as the considered size of wind sectors, are detailed in the next paragraphs.

Measurements For the Horns Rev measurements investigated in [20], when an observation with a reference time period of 10 minutes had the correct mean wind speed and TI, and a wind direction within the considered wind sector, it was selected for the analysis. It is not well detailed in the reference paper, but we assume that, for one observation, the power output of each rotor in a line is normalized to the power output of the first turbine in the same line. Each rotor power is then averaged on the selected number of observations. The power of each row was then computed as a average of the eight lines. The wind sectors analyzed in the paper were $270 \pm 1^\circ$, $\pm 5^\circ$, $\pm 10^\circ$ and $\pm 15^\circ$. In this work, we only report the results for wind sectors up to $270 \pm 10^\circ$. The amount of observations per wind sector is not mentioned in [20] and values of uncertainties or standard deviations are not available. An other paper of Barthelmie *et al.* [18] investigated a narrow wind sector of $270 \pm 2.5^\circ$, for which uncertainties are available, but it was not especially specified that there was a sorting according to the atmospheric stability. We thus decided to not include this set of observations in the present comparison. Only the power output for the first eight rows were shown in the reference paper.

LES of Wu and Porté-Agel [143] Wu and Porté-Agel [143] performed LES of the Horns Rev wind farm and they used an AD approach similar to that developed in this thesis, with aerodynamic loads computed based on the local flow velocities. Simple control schemes were also added to account for the dynamics of the rotor. Simulations were performed for each degree from 255° to 285° , and several size of wind sectors were investigated, following the same approach as for the Horns Rev measurements. Again, in this work, we only report the results for wind sectors up to $270 \pm 10^\circ$. The mean powers of a wind sector consist of averages of rotor powers from simulations for which the wind direction is within the considered wind sector; a wind sector of $10 \pm^\circ$ thus implies simulations from 260° to 280° , including these two wind directions. The power output was averaged over a time of 40 minutes. Again, details about the averaging procedure in [143] are scarce: it is only mentioned that the power output of each row is obtained by averaging power of the eight lines at the row r . The different averaging procedures as well as their impact of the power output will be slightly discussed in the next paragraph. Only the power output for the first eight rows were shown in the paper.

AD simulations The present results are shown for different wind sectors: $270 \pm 1^\circ$, $270 \pm 3^\circ$, $\pm 5^\circ$, and $\pm 9^\circ$. Again, the mean powers for a wind sector are averages of rotor powers from simulations for which the wind direction is within the considered wind sector. We recall that we use a discretization of 2° , and so, for example, the wind sector $270 \pm 5^\circ$ includes the directions 266° , 268° , 270° , 272° and 274° (see Fig. 7.1(b)). As our simulations are performed from 262° to 278° , the maximal wind sector covers $\pm 9^\circ$. The power output of each machine is averaged over the 40 minutes of the simulation ($\bar{P}_{l,r}^\alpha$ for the considered wind direction α) and then averaged over the N_α simulations for the considered wind sector; the resulting power of each row is then averaged over the eight lines:

$$\bar{P}_{ad,r} = \frac{1}{8} \sum_{l=1}^8 \frac{1}{N_\alpha} \sum_{\alpha=1}^{N_\alpha} \bar{P}_{l,r}^\alpha. \quad (7.10)$$

The power is thus normalized with the power output of the first row: $\bar{P}_{ad,r}/\bar{P}_{ad,1}$. Different averaging procedures have been tested (first average over the lines before averaging on the wind sectors,...). The variations between those procedures were very small, except when the power output $P_{l,r}$ is normalized by that of the first machine on the row before the temporal averaging. This last procedure indeed generates large power ratios simply through the time delays due to the advection of lulls and gusts in the farm; such effects will clearly lead to non-linear biases in the averages, which should be avoided. The results are shown for the ten rows.

For the sake of clarity, the data are presented in two separate figures. Figure 7.26 shows the LES results and observations, while Figure 7.27 shows the LES results compared to those of Wu and Porté-Agel [143].

Comparison with measurements

For narrow wind sectors (up to $\pm 5^\circ$), we notice that the LES results clearly under-predict the power output compared with observations. As explained in the reference paper [18], the challenges involved in comparing simulations and measurements are multiple:

- the establishment of the freestream flow,
- the presence of wind speed gradients across the wind farm, and of the natural fluctuations in the wind speed and direction in any period for the

actual farm,

- the determination of the turbulence intensity and atmospheric stability,
- the estimation of a true freestream wind direction or the nacelle direction and the presence of a possible yaw misalignment.
- the site-specific power curve and thrust coefficients
- the wake transport time through the wind farm
- the time-averaging between models and measurements

For narrow wind sectors, the fourth point may have a strong impact on the power output. Indeed, the power data of the second rotor row and the following ones are very sensitive to the small offset of the incoming wind and a possible yaw misalignment. The number of observations is also smaller for those wind sectors, and the absence of uncertainties makes the discrepancies very high. These imperfections lead us to the conclusion that the idealized conditions of our numerical simulations can only provide an upper bound for the wake deficit. As our discretization is quite coarse (each 2°), the average over narrow wind sectors only account for a small number of simulations and wind cases, and this can also play a role in the discrepancies observed for those wind sectors. For larger wind sectors, the impact of the discretization decreases, as well as the impact of the errors about wind direction measurement and yaw misalignment mentioned hereinabove. The power output averaged over a wind sector of $\pm 9^\circ$ is similar to that of the measurements for a wind sector of $\pm 10^\circ$ around 270° . However, the decrease of power production of the second row is more important for LES results. It is interesting to note that the present LES results produce an equilibration process that spans the first four rows, a behavior that is quite comparable to the measurements.

Comparison with other LES results

Compared to the power data from Wu and Porté-Agel [143], we notice small differences. First of all, as already mentioned, we perform simulation every two degrees, which is coarser than the wind direction discretization of other LES results (one simulation each 1°). For narrow wind sectors (up to $\pm 5^\circ$), our results produce slightly lower levels of power than those reproduced from [143]. A larger number of simulations could help in reducing these discrepancies. However, mainly for narrow wind sectors, the present results lead to a different

behavior for the fourth row and the following ones. Our simulations predict a slightly lower production for these rotors, while a plateau in power production is reached from the third one for Wu and Porté-Agel. It was confirmed by investigating the velocity profiles upstream of the fourth wind turbine in Figs. 7.18, 7.21 and 7.24. This cannot be attributed solely to a difference in the discretization. We propose several potential explanations:

- First of all, the grid used by Wu and Porté-Agel [143] is coarser than the one used in the present simulations (more than twice as coarse); this impacts the rotor production but also the numerical and the subgrid scale dissipations. A simulation on coarser resolution could be performed, to verify the modification in terms of power production.
- Secondly, the level of turbulence in our simulations is about 6.7% while that of simulations of Wu and Porté-Agel [143] is about 7.7%. This can affect the production of the first machines: the second row of rotors may produce less (our simulations lead to a second row that produces slightly less than those of Wu and Porté-Agel [143] simulations for $270\pm 3^\circ$), so the third one produces slightly more and the fourth one thus produces less. The plateau is then reached from the fourth row on. This could also explain the discrepancies observed in the comparison with measurements, as the observations are selected when the turbulence intensity is about 8%, which is slightly higher than that obtained in our simulations.
- Finally, there are also differences in the rotor representation. The rotor dynamics is not represented in Wu and Porté-Agel, and we assume that, at each time step, the rotor speed is adapted instantaneously through an iterative procedure to produce the optimal torque based on the local flow velocities; the optimal behavior is thus reached at each time step of the simulation. In our case, the rotor speed results from the balance between the controlled generator torque, the aerodynamic torque, and the inertia of the rotor. This thus required a certain number of time steps to reach the optimal rotor speed. This clearly affect the power output, and may explain the difference of powers for the first rows.

7.5.4 Power and thrust distributions

We now study the evolution of the power and thrust productions for one line inside the wind farm, and this for the three considered directions. The proba-

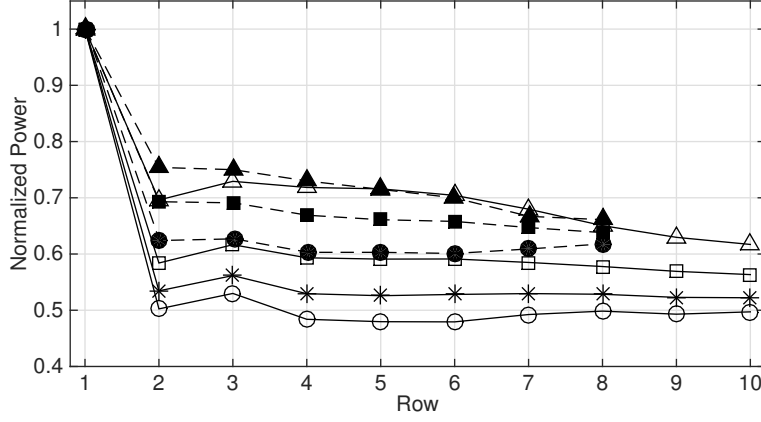


Figure 7.26 – Horns Rev simulations: comparison of the normalized mean power output data obtained from the measurements [20] for $270 \pm 1^\circ$ (dashed line with filled circles), $270 \pm 5^\circ$ (dashed line with filled squares), $270 \pm 10^\circ$ (dashed line with filled triangles) and the present simulations for $270 \pm 1^\circ$ (solid line with circles), $270 \pm 3^\circ$ (solid line with stars), $270 \pm 5^\circ$ (solid line with squares) and $270 \pm 9^\circ$ (solid line with triangles).

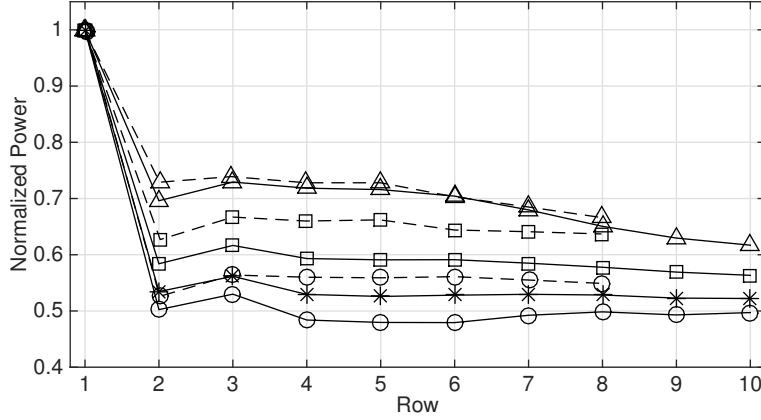


Figure 7.27 – Horns Rev simulations: comparison of the normalized mean power output data obtained from the LES of Wu and Porté-Agel [143] for $270 \pm 1^\circ$ (dashed line with circles), $270 \pm 5^\circ$ (dashed line with squares), $270 \pm 10^\circ$ (dashed line with triangles) and the present simulations for $270 \pm 1^\circ$ (solid line with circles), $270 \pm 3^\circ$ (solid line with stars), $270 \pm 5^\circ$ (solid line with squares) and $270 \pm 9^\circ$ (solid line with triangles).

bility density functions are shown in Fig. 7.28 for seven machines, the same as considered in Section 7.5.2: rotors 1, 2, 3, 4, 5, 7 and 10.

For $\alpha_w = 266^\circ$, 270° or 274° , the mean power of the first machines is clearly higher than those produced by rotors inside the wind farm. There are small variations for this first machine according to the direction: as the position of

the wind farm moves, the line is impacted by a slightly different wind.

For these three directions, the width of the distribution for the downstream machines is similar to that of the first machine; relative to the mean production, the standard deviation is thus higher. It is due to the large scale oscillations of the upstream wakes, which increase the velocity variations suffered by the rotor. The downstream machines also present asymmetry in their distributions, mainly noticeable in the power data. If we assumed that the velocity seen by the rotor has a Gaussian distribution with a high standard deviation, a simple approximation of the power distribution tendency (taken as the cube of the velocity) will lead to a distribution with a non-zero skewness.. For the two unaligned directions, the skewness of the curves of the downstream machines is more marked, with a right tail longer than the one observed for $\alpha_w = 270^\circ$. As the rotors are not perfectly aligned with the first ones, they are more often impacted by higher velocities.

For $\alpha_w = 262^\circ$, the mean power of the first rotor is similar to those of the five first machines in the line. Only the pdf of the last rotors in the line present a lower mean power production.

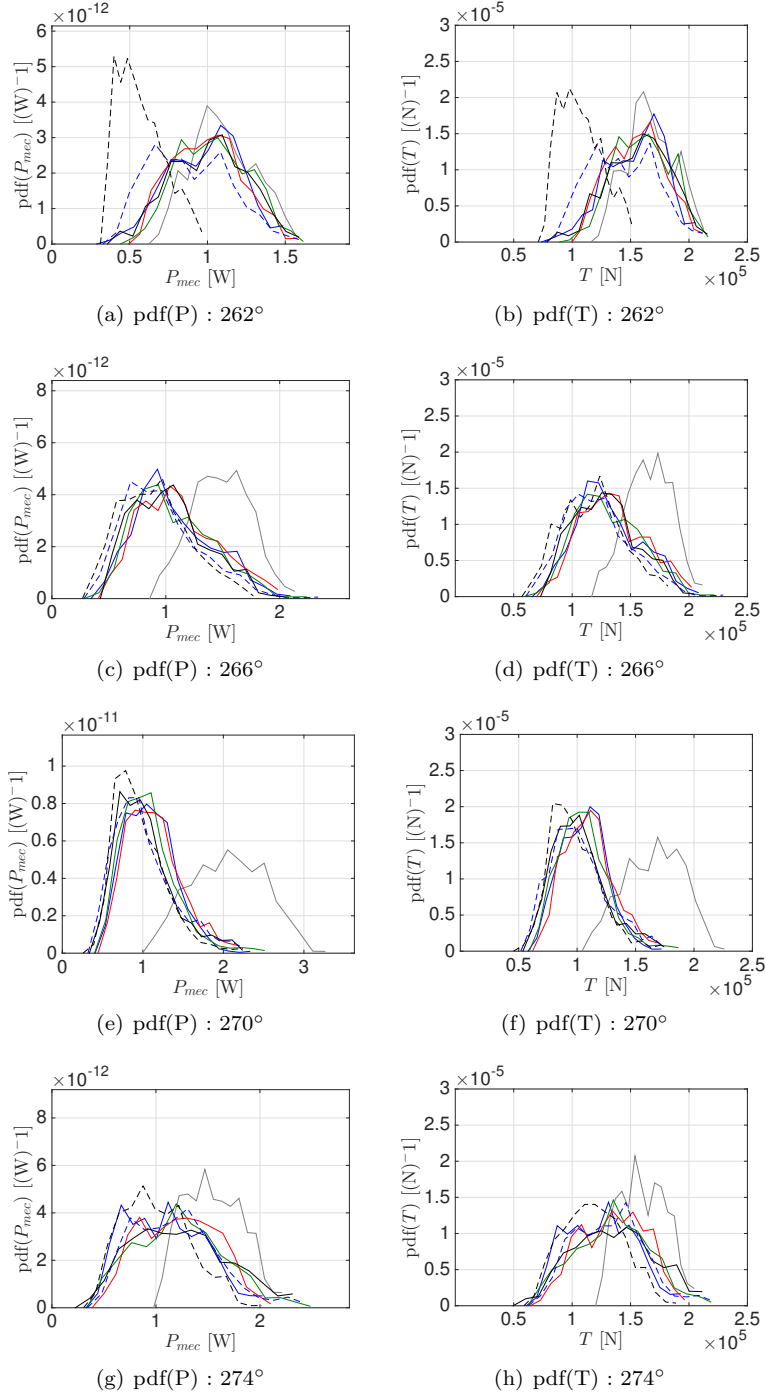


Figure 7.28 – Probability density function of the power and thrust for the three directions for seven wind turbines in line L4: rotor 1 (grey solid line), 2 (blue solid line), 3 (red solid line), 4 (green solid line), 5 (black solid line), 7 (blue dashed line) and 10 (black dashed line). The results are presented for the three wind directions.

7.5.5 Yawing moments

The yawing moment is here investigated; it is denoted M_y . This is caused by an imbalance in the forces with respect to the yaw axis, here the vertical axis at the hub location, e_y . This can be expressed as:

$$M_y = \mathbf{e}_y \cdot \int_S \mathbf{x} \times \mathbf{f} dS \quad (7.11)$$

where $\mathbf{f}$ is the force density on the disk element of a surface dS , $\mathbf{x}$ is the vector position between the yaw axis and the considered force point, and S is the whole disk surface. In this case, only the normal force component plays a role in the yaw moment, because the nacelle length is not taken into account, and the distance between the rotor plane and the yaw axis is zero. Its contribution may be added, by using an artificial distance between the rotor and the y-axis. One can quickly realize however that the contribution of f_θ should be small compared to contribution of f_n for a well balanced rotor.

The yawing moments are presented in Fig. 7.29, for the same seven rotors in the line L6. The first wind turbine in the line presents the same behavior for each direction, with a yawing moment centered at 0. For the following wind turbines, there are noticeable differences. While the rest of the line still presents yawing moments centered at 0 for the direction 270° , the two directions $\alpha_w = 266^\circ$ and $\alpha_w = 274^\circ$ capture an offset in the mean value. The rotors are indeed partially immersed in the upstream wakes, leading to a non-zero mean value. For $\alpha_w = 262^\circ$, the offset is less pronounced, as the rotors are nearly out of the wake of the upstream machines. For the same direction, for rotors deeper in the farm, such as rotors 7 or 10, they begin to be impacted by wakes of rotors of the line L_7 , the yawing moment is more centered around 0.

For all directions, the yawing moment of machines inside the farm presents a more smeared shape, as the rotors are impacted by an upstream wake that horizontally oscillates, leading to larger normal force variations than those felt by the first rotor in the line. Again, it is less marked for $\alpha_w = 262^\circ$, as the rotors are less impacted by the wake meandering of the upstream rotors.

7.5.6 Power and thrust densities

The power density over the rotor surface is presented in Fig. 7.30 for the first and second rotors of line L6, and for the four considered wind directions. We here consider the local $\Delta P / \Delta S$, i.e. the local time-averaged power on a disk

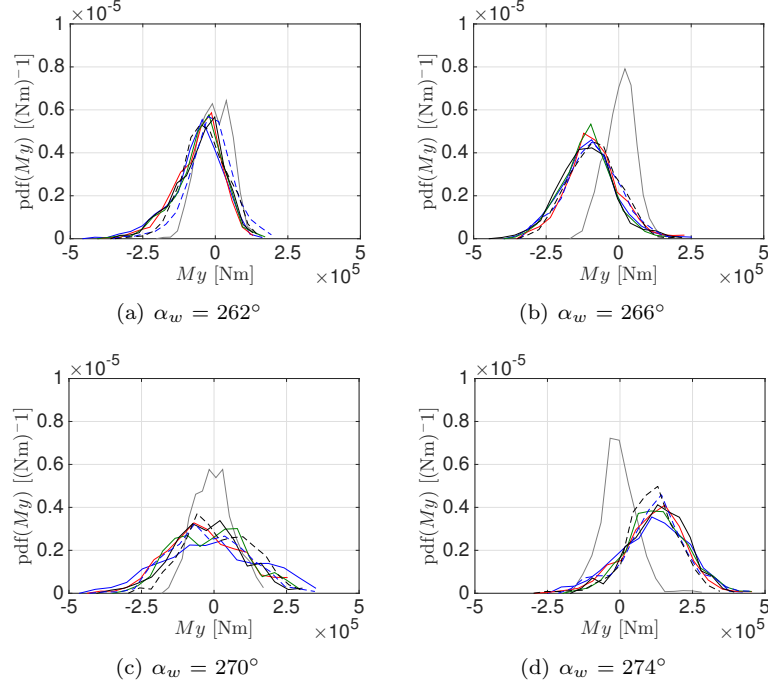


Figure 7.29 – Horns Rev simulations: Probability Density Function (PDF) of the total disk yaw moment for seven rotors in line L6: rotor 1 (grey solid line), 2 (blue solid line), 3 (red solid line), 4 (green solid line), 5 (black solid line), 7 (blue dashed line) and 10 (black dashed line). The results are presented for four wind directions.

element divided by ΔS , the surface of this disk element ΔS . They are normalized by the global power density, defined as the global power produced divided by the disk surface.

As expected, the power production for the first rotors (see Fig. 7.30(a), 7.30(b), 7.30(c) and 7.30(d)) is not uniform over the vertical direction, due to the incoming shear flow. For the second rotor, the shear is attenuated, due to the upstream wake (see the flow velocities impacting the second rotor in Fig. 7.21(a)); we would expect a more uniform power repartition over the disk. It is not really noticeable in Fig. 7.30(g), but the power appears slightly more uniformly distributed for the second rotor. For the unaligned directions $\alpha_w = 266^\circ$ and 274° , the distribution is asymmetric, with higher power densities on the left side and the right side of the disk, respectively. As expected, this asymmetry decreases for $\alpha_w = 262^\circ$.

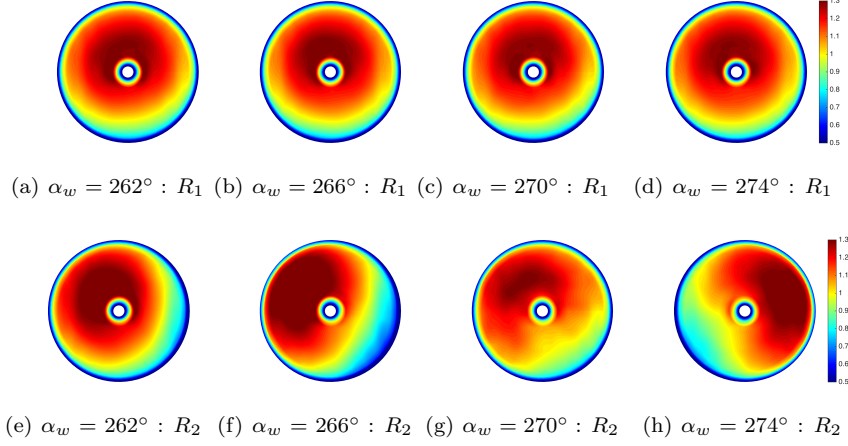


Figure 7.30 – *Horns Rev simulations: Time-averaged power densities on the disk, normalized by the total power divided by the disk surface, for the first and second rotor of Line L6. The results are presented for the four directions.*

7.6 Computational aspects

The precursor simulation required 1024 processors during about 80 hours (i.e. roughly 154 000 hours-processors) in order to simulate about 100 000 seconds. This corresponds to about 10 flow-through times (one flow-through time is here equal to L_x/U_{hub}).

One complete simulation, including the main and the auxiliary domains, required 1536 processors during about 55 hours for simulating 67 minutes of wind. This leads to roughly 85 000 hours-processors. The auxiliary simulations used 512 processors, while the main simulation was performed on 1024 processors.

7.7 Conclusions

Simulations of a very large wind farm have been performed in order to verify and validate the tool. We chose the Horns Rev wind farm as a case study since data were available in many research works. As full geometrical and aerodynamic data were not available, we based our rotor representation on the one used in [143]. The pitch angle was slightly adapted to follow the curves of power and thrust coefficient available in specifications. A simple control scheme, including both torque and pitch angle controllers, was implemented

in order for the machine to respond to the incoming flow fluctuations. The inflow turbulence generation was obtained with a precursor simulation; once a statistically state was reached, this simulation concurrently ran with the main simulations with rotors, allowing the velocities to be directly transferred from the auxiliary domain to the inlet of the main simulation. Simulations were performed and analysed for a wind sector of about 18° around one main direction, and power data were compared to observations [20] and to other available LES computations [143]. Discrepancies were found between the measurements and simulation results, that could be attributed to uncertainties in the measured wind direction and possible yaw misalignment of the rotors for the actual wind farm, as already observed in [109]. Small discrepancies were also observed between the numerical results available in [143] and the present work. These could be due to a difference of resolution and to the small differences in the turbulence intensity of the incoming flow. Wake topology, flow statistics, power and thrust distributions, as well as the yaw moments were investigated for the different directions. It was shown that the developed disk approaches could bring valuable results in the comprehension in wake phenomena, and the resulting loads. These simulations demonstrated the ability of the developed disk approach and the numerical tool to simulate a large wind farm, and reproduce accurate wake behaviors. The present chapter only investigated four directions, and thus represents only the beginning of a more thorough validation process. This will be performed in a study over larger wind sectors.

Chapter 8

Conclusions and perspectives

This thesis has been dedicated to the implementation of an advanced AD model in a fourth-order finite differences code, aiming at efficient and still accurate simulations of large wind farms. An effort was made in order to keep an accurate response of the rotor model, despite its approximations and the coarse resolutions used. The model was thus improved with a tip-loss correction, also appropriate for wind farm flows, and with realistic control schemes. An important part of this work was devoted to the verification of the developed disk approach in terms of aerodynamics and wake statistics through comparisons against results obtained with a higher fidelity approach. Wind farm simulations were finally performed, to validate the tool and to investigate wake statistics and the responses.

8.1 Conclusions

Verification of the AD approach

The first part of this thesis was mainly dedicated to the verification of the AD model at a coarse resolution.

Rotor behavior and tip-loss correction

As the AD model was known to suffer of some weaknesses in its rotor representation, a tip-loss factor was added, here a correction typically used in the classical Blade Element Momentum method. The procedure of the tip-loss correction required to isolate “induction factors”, which itself asked for an estimation of the upstream velocity.

In this work, a method was proposed for estimating a reference upstream velocity, based on the simple One-dimensional Momentum (M1D) theory and the local disk data. The procedure appears well suited for complex flows as it removes any ambiguity in the choice of an upstream velocity measurement. The simulations of Chapter 3 showed that the methodology, despite the crude assumptions that implied the M1D theory, led to a correct estimation of reference upstream velocity and the resulting errors did not affect so much the tip-loss correction itself. The AD model, supplemented with this tip-loss factor, produced results close to those obtained with the VPM approach. Simulations of Chapter 4 verified this procedure, as well as the rotor power and thrust production, for one machine impacted by turbulence fluctuations.

The present correction is based on the combination of a routinely used tip-loss correction and simple analysis. This combination appeared to over-predict the losses in some cases and indicated that the present comparisons between actuator disk and lifting line could be further exploited to improve the tip-loss correction, as already suggested by others [26, 116]. The considered correction only implied the differences existing between an disk-averaged approach and a discrete blade type approach, and did not account for three-dimensional effects at the blade tip [118, 119] or the impact of rotational blade movement on the lift and drag coefficients [86]. Accounting for these additional corrections could further improve the approach (we recall that the VPM approach also misses the mentioned effects, and required the corrections). Interestingly, the inclusion of such corrections might waive the need for tip loss corrections. Nevertheless our extensive testing showed that the AD approach was clearly improved with the tip-loss factor. At the same time, the procedure remains simple and fast, and does not require a tuning of coefficients according to the considered wind turbine model.

Flow statistics

At the beginning of this thesis, first attempts to perturb the AD wake were made, in order to reproduce flow statistics close to those obtained with the VPM approach. These first investigations concerned simulations in uniform flows and the additional perturbation clearly improved the resulting deficit and level of turbulence. Once turbulent inflows were considered, it did not bring sensible improvements in the wake behavior, as the AD already produced wake statistics very close to those obtained with the higher fidelity approach, and we decided to leave this as a topic of future. The simulations of Chapter 5 confirmed that, in turbulent flows, the AD produces correct velocity deficit from five rotor diameters downstream of the rotor, for all considered operating conditions and levels of inflow turbulence. We observed more discrepancies in the TKE production, mainly when the inflow turbulence was relatively low; the discrepancies remained small however.

A simulation with two aligned wind turbines could highlight the impact of such discrepancies, in terms of power production and wake statistics for the second wind turbine (another approach for generating turbulence would be required, to avoid the important turbulence decay observed with Mann's algorithm).

As the observed discrepancies remain small, before envisaging additional perturbations to improve the AD wake behavior, as suggested in [115], a short-term and quite valuable investigation could be carried out: one should verify whether the AD approach is able to capture the large-scale oscillations of the wake. Indeed, the wake meandering is crucial at a wind farm scale, as it impacts the production and the fatigue loads of the downstream machines.

Wind farm simulations

Several wind farm simulations have been performed in this work. A first involved a relatively small farm of 15 machines, while the second one considered a larger operational wind farm with 80 machines and was initially meant for validation purposes.

The simulation of the 15-rotor wind farm was performed for investigating the impact of a transient scenario, here the emergency stop of one machine. Such a scenario was already investigated in other works, but only in terms of structural loads, and not in the resulting wakes. The AD approach was improved with realistic control schemes, and an emergency shutdown procedure

was added. This resulted in an accurate response of the wind turbines during the transient.

In order to validate the code, simulations of a large operational wind farm were performed in Chapter 7. These simulations demonstrated the ability of the developed disk approach and the numerical tool to simulate a large wind farm, and reproduce accurate wake behaviors. The results were presented only for a narrow wind sector; additional simulations are currently running in order to cover a larger wind sector. This would further substantiate our validation of the tool.

This first set of simulations highlighted some computational aspects too: for the Horns Rev wind farm, the total grid involved more than 550×10^6 points and required 85 000 processor-hours in order to reproduce 67 minutes of wind (with a time step of 0.125 s). By comparison, Martinez *et al.* performed a simulation of the Lillgrund wind farm, with its 48 rotors modeled with advanced AD approaches and using the well-known Simulator for Wind Farm Applications (SOWFA): this required 500 000 processor-hours for 10 minutes of wind for a grid of 300×10^6 points and a time step equal to 0.06 s. The tool developed in this thesis appears to be more efficient, as it uses structured meshes. On the one hand, these computational cost estimations are quite rough: the SOWFA simulation also accounted for temperature effects and several aspects of the computational resources were not provided; this urges us to use caution in our comparisons. On the other, several aspects of the present numerical tool are far from optimized. The wall-modeling procedure, for instance, entails some important communication bottlenecks between the processors, which hint at a potential three-fold optimization of the FD4 speed.

In a more personal opinion, I think that this thesis work brought some innovations and valuable contributions both at the scale of our group (at UCL) and at a larger scale. Before this work, our group could only simulate wind turbines by using the VPM code, which limited the simulations to one or two machines. The addition of AD models in a fourth-order finite differences code significantly increased the scale of the simulations, allowing the representation of large wind farms. The intensive comparison between the AD and the VPM methods also allowed a cross verification of the two approaches. At a more global scale, the investigations of the tip-loss corrections highlighted the actual need of a correction, and even if it was demonstrated in other works for AD

method in uniform flows, it was not clear how the classical tip-loss corrections had to be applied when the wind turbine model was used at a wind farm scale. This work brought an answer, with a simple approach for applying the tip-loss factor on the AD velocities. The developed tool is now ready for production simulations and investigations of global control, as the rotor dynamics is represented, and as it has been highlighted that fatigue-relevant information could be recovered from this AD method.

8.2 Perspectives

The thesis contributed in the development of a numerical tool aimed at wind farm simulations. A great effort of verification has been made here, mainly in the rotor representation, and it has been proved that the tool was well suited for efficiently simulating unsteady wind farm flows. This also constitutes a starting point for further improvements and applications.

Validation of the AD approach

The AD approach has been verified for a baseline wind turbine and compared with results obtained with VPM approach. An actual validation of the AD model should be performed, through quantitative comparison with measurements, such as those available in the NREL phase IV [54, 60] or MEXICO [121] projects. This step should required additional corrections, such as those mentioned previously, i.e. factors accounting for three-dimensional effects at the blade tip [118, 119] and the impact of rotational blade movement on the lift and drag coefficients [86]. This would allow a more accurate comparison between the AD and actual data.

Wake meandering investigation

As mentioned, the wake meandering, characterized by its large-scale oscillations, impacts the power histories and increases fatigue loads on the downstream machines. It would be quite valuable to verify whether the AD approach at a coarse resolution is able to reproduce the same wake oscillation characteristics as those of the VPM approach. The tool has been used for preliminary investigations about wake meandering tracking downstream of a disk and inside a wind farm (paper accepted for The Science of Making Torque from

Wind (Torque 2018) [44]), highlighting the complex physics of the wake and the challenges its tracking and affordable modeling.

Atmospheric Boundary Layer

In the present thesis, thermal and stratification effects were not included; only neutral boundary layer (NBL) cases were thus simulated. However, over land, NBLs occur for very short transition times and other stability classes of ABL, i.e. convective boundary layer (CBL) and stable boundary layer (SBL), have impacts on the resulting behavior of wakes. In particular, the SBLs can lead to wind farm underperformance and are thus relevant to study in wind farm flows. Efforts are currently made to include stratification effects in the FD4 code (as a part of a master thesis, Lejeune [81]), and this could improve the accuracy of the wind farm simulation tool in a very short term. A further improvement could envisage a coupling with mesoscale codes to transfer realistic wind fluctuations into the simulation that contains the wind farm. This is a quite challenging problem, especially in terms of the resolution gap between the codes, which implies the filling of flow scales once inside the local simulation.

Vertical Axis wind turbine

There is an acceleration in research efforts about Vertical Axis Wind turbines (VAWT) [50, 23, 33, 34] due to promises of higher power extraction densities in wind farm configurations and potential operational gains. The developed tool is well suited for investigating the gains in the global production of a wind farm composed of VAWTs over that of classical horizontal axis wind turbine. As a part of two master theses (Taverne [130] and Collin and Möens [42]), an Actuator Cylinder approach was implemented in the code. This model currently presents a loose-coupling with the flow solver; indeed, only a plane of velocities is recovered in the flow, at an upstream distance of the rotor, and a Double Multiple Stream Tubes method (DMST) [104] uses these velocities for estimating the forces on the cylinder. A definite improvement should consist in recovering the velocities at the cylinder position, and computing the aerodynamic forces, in the same manner as the disk approach.

Regulation and global control schemes

Investigations of strategies for optimising the production according to the wind condition could also be performed. It consists in forcing some machines to degrade their power extraction (by pitching the blades) or in yawing some rotors to weaken or deviate the wakes to finally increase the production of the other ones and the final global power. The impacts of such actions, mainly for the forced yaw angle, can be also studied to avoid an unacceptable increase of loads [11].

Wind events

The tool enables studies of the wind farm response to transients in terms of power, wakes and loads. As transient scenarios, we consider here the unexpected loss of one machine inside the wind farm or a sudden change in the wind condition (the generation of the gust is an ongoing work). The definition and the representation of these events are also useful for verifying the robustness of the global control schemes (optimisation and regulation).

Appendices

Appendix A

Subgrid scale turbulent kinetic energy

The Actuator Disk (AD) results presented in Chapter 5 only account for the resolved part of the turbulent kinetic energy (TKE). As the mesh used for these simulations is relatively coarse, the subgrid scale TKE may represent a non-negligible part. We here make an estimation of the amount TKE part contained in the resolved and in the subgrid scales.

The simulation is here the same as one performed in Chapter 5. The main parameters are recalled in Table A.1. $\bar{U}$ is the time-averaged velocity, and λ , the tip-speed ratio computed with that wind speed ($\lambda = \Omega R_{tip}/\bar{U}$). The domain

$\bar{U}$ (m/s)	λ	Rotor Speed (RPM)	Blade pitch angle (°)
9	7.55	10.30	0

Table A.1 – NREL-5MW: Operating point.

size is $14D \times 9D \times 9D$, with the wind turbine located at $(3D, 4.5D, 4.5D)$, and there 16 points per D, leading to $\Delta x = \Delta y = \Delta z \simeq 7.88$ m. The turbulences fluctuations are obtained with the Mann’s algorithm and lead to a turbulence intensity (TI) of about 6%(see Chapter 5 for the used parameters). One Mann box period, T_{Mann} , is equivalent to 336.0 s of wind.

Probes are inserted at several downstream locations (see Fig. A.1), where the wake is fully developed and turbulent, and record the histories of the three

velocity components over a time equivalent to T_{Mann} .

The energy spectrum of the resulting fluctuations is computed in the frequency domain, and expressed as a function of the wavenumber with the relation:

$$k = \frac{2\pi f}{\bar{u}} \quad (\text{A.1})$$

where $\bar{u}$ is the time-averaged velocity at the considered probe position. The resulting energy spectra are shown in Fig. A.2(a). As the CFL is smaller than one, the temporal resolution is finer than the spacial resolution, and there are results beyond the cut-off wavenumber tied to the mesh resolution, k_c ($k_c = \frac{\pi}{\Delta x}$); these are not reported in Fig. A.2.

The resolved TKE is obtained by integrating the energy spectrum E_s of the sensors:

$$E_{res} = \int_0^{k_c} E_s(k) dk \quad (\text{A.2})$$

In order to have an idea about the amount of energy contained in the subgrid scales, we consider the Kolmogorov energy spectrum for the inertial subrange:

$$E(k) = C_k \bar{\epsilon}^{\frac{2}{3}} k^{-5/3} \quad (\text{A.3})$$

with $C_k \simeq 1.6$. As the considered Re is high, the characteristic length scale of the dissipative structures is very small (corresponding to a very high wave number) and the model of Eq (A.3) gives a good estimation of the energy spectrum behavior for all the scales. In order to have an estimation of the dissipation of the turbulence kinetic energy, $\bar{\epsilon}$, we fit the model to the results of Fig. A.2(a) (see Fig. A.2(a)). The subgrid scale part is thus obtained by integrating the model from the cut-off wavenumber k_c up to infinity:

$$E_{sgs} = \int_{k_c}^{\infty} C_k \bar{\epsilon}^{\frac{2}{3}} k^{-5/3} dk = \frac{3}{2} C_k \left(\frac{\bar{\epsilon}}{k_c} \right)^{2/3} \quad (\text{A.4})$$

We can also have an estimation of the TKE part that is not captured by the LES, due to the drop of the energy spectrum before the cut-off frequency. This is computed as the difference between the model and the resolved TKE between k_s and k_c :

$$E_{sfs} = \int_{k_s}^{k_c} C_k \bar{\epsilon}^{\frac{2}{3}} k^{-5/3} dk - \int_{k_s}^{k_c} E_s(k) dk \quad (\text{A.5})$$

k_s is estimated from the results (see Fig. A.2(b)), as the frequency from which

the spectrum drops and does not longer follow the model. The total kinetic energy is obtained as the sum of these three parts:

$$E_{tot} = E_{res} + E_{sgs} + E_{sfs} \quad (\text{A.6})$$

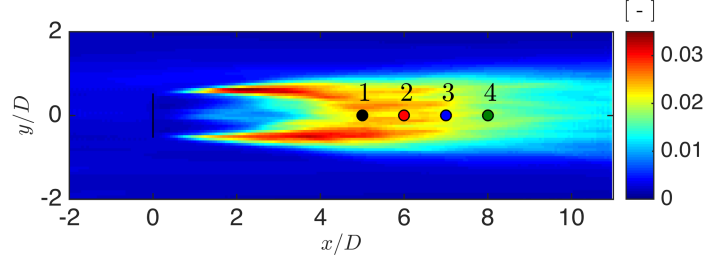


Figure A.1 – Vertical slice of the TKE production for the AD approach ($\bar{U} = 9$ m/s, $\lambda = 7.55$, $TI = 6\%$): the coloured circles represent the position of the sensors.

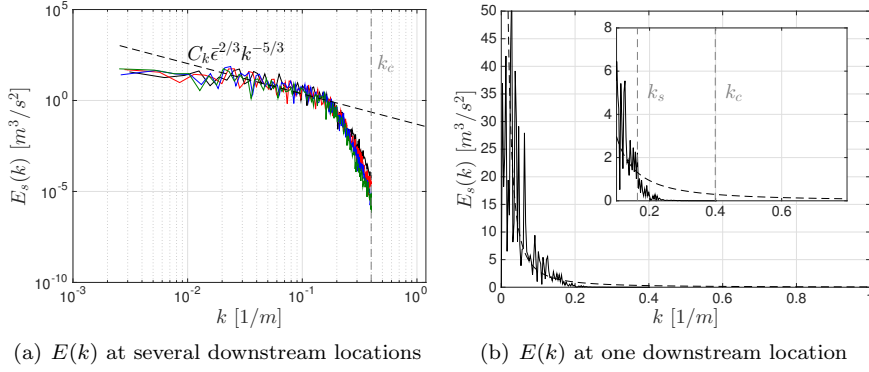


Figure A.2 – Energy spectra of the turbulence fluctuations at several locations downstream of the machine (top) in a log-log diagram: the colour of the curves is linked to the sensors presented in Fig. A.1 and the dashed black line represents the model with $\bar{\epsilon}$ imposed to fit the results. Energy spectrum of the turbulence fluctuations for one sensor (black solid line) and the model (dashed line). The small figure is a zoom where are reported k_s and k_c .

The amount of energy contained in each part, compared to the total energy, is reported in Table A.2. We see that about 85% of the total TKE is captured by the AD simulation, while about 8% is not captured because of the coarse mesh. About 5% is missed by the LES due to the drops in the energy from a certain wavenumber k_s , smaller than k_c . We recall that the translation of a temporal spectrum into a spatial one is performed by assuming that the flow structures

are advected at the time-averaged velocity at the sensor position under the hypothesis of frozen turbulence. This states that the turbulent structures move as frozen entities transported by the mean wind. However, this may not be valid anymore for the smallest eddies of the flow. The use of Taylor's frozen turbulence hypothesis thus may shift or stretch the wavenumbers at those scales and change the slope of the spectrum or shift the spectrum to larger scales. This could explain the important drop from k_s . This assumption may also slightly modify the repartition of the TKE into the resolved and subgrid scales.

	% from E_{tot}		
Sensors	E_{res}	E_{sgs}	E_{sfs}
1	85.9%	8.6%	5.5%
2	86%	8.6%	5.4%
3	86.6%	8.1%	5.3%
4	86.2%	8.3%	5.5%

Table A.2 – % of the turbulent kinetic energy contained in E_{res} , E_{sgs} and E_{sfs} , compared to the total turbulent kinetic energy E_{tot} (see Fig. A.1 for the reference to the sensor numbers)

Bibliography

- [1] Baromètre éolien. <https://www.eurobserv-er.org/barometre-eolien-2018/>. Accessed: 2018-05-12.
- [2] Baromètre éolien. <http://www.belgianoffshoreplatform.be/fr/projets/>. Accessed: 2018-05-12.
- [3] Farmflow. <http://www.ecn.nl/nl/units/wind/rd-programma/aerodynamica/windfarmaerodynamics/farmflow/>. Accessed: 2018-05-12.
- [4] Horns rev 1. <https://powerplants.vattenfall.com/en/horns-rev>. Accessed: 2018-03-09.
- [5] <https://en.wind-turbine-models.com/turbines/668-vestas-v80-offshore>.
- [6] <https://www.sparesinmotion.com/fr/pièces/generateurs/abb-amk-500l-4a>.
- [7] <https://www.sparesinmotion.com/wind-turbine-parts/gearboxes/zf-hansen-zf-hansen-eh-802-cn21-bn-i9227>.
- [8] openwind. <http://software.awstruepower.com/openwind/>. Accessed: 2018-05-12.
- [9] Sowfa. <http://wind.nrel.gov/designcodes/simulators/sowfa/>.
- [10] V80-2.0 mw. <http://www.windmolensite.be/other/V80{ }UK.pdf>. Accessed: 2018-19-08.
- [11] Wakeopcoll. <sites.uclouvain.be/wakeopcoll>. Accessed: 2018-05-16.
- [12] Windfarm from resoft. <http://www.resoft.co.uk/English/index.htm>. Accessed: 2018-05-12.

- [13] J. Ainslie. Calculating the flow field in the wake of wind turbines. *Journal of Wind Engineering and Industrial Aerodynamics*, 27:213–224, 1988.
- [14] D. Allaerts and J. Meyers. Boundary-layer development and gravity waves in conventionally neutral wind farms. *Journal of Fluid Mechanics*, 814:95–130, 2017.
- [15] I. Ammara, C. Leclerc, and C. Masson. A viscous three-dimensional differential/actuator-disk method for the aerodynamic analysis of wind farms. *Transactions-American Society of Mechanical Engineers Journal of Solar Energy Engineering*, 124(4):345–356, 2002.
- [16] M.-A. Asareh, W. Schonberg, and J. Volz. Effects of seismic and aerodynamic load interaction on structural dynamic response of multi-megawatt utility scale horizontal axis wind turbines. *Renewable Energy*, 86:49 – 58, 2016.
- [17] E. E. Bachynski, M. Etemaddar, M. I. Kvittem, C. Luan, and T. Moan. Deepwind 2013, selected papers from 10th deep sea offshore wind r&d conference, trondheim, norway, 24 â 25 january 2013 dynamic analysis of floating wind turbines during pitch actuator fault, grid loss, and shut-down. *Energy Procedia*, 35:210 – 222, 2013.
- [18] R. J. Barthelmie, S. C. Pryor, S. T. Frandsen, K. S. Hansen, J. G. Schepers, K. Rados, W. Schlez, A. Neubert, L. E. Jensen, and S. Neckelmann. Quantifying the impact of wind turbine wakes on power output at offshore wind farms. *Journal of Atmospheric and Oceanic Technology*, 27(8):1302–1317, 2010.
- [19] R. J. Barthelmie, S. T. Frandsen, O. Rathmann, K. S. Hansen, E. Politis, J. Prospathopoulos, J. Schepers, K. Rados, D. Cabezón, W. Schlez, et al. Flow and wakes in large wind farms: Final report for upwind wp8. Technical report, Danmarks Tekniske Universitet, Risø Nationallaboratoriet for Bæredygtig Energi, 2011.
- [20] R. J. Barthelmie, K. Hansen, S. T. Frandsen, O. Rathmann, J. Schepers, W. Schlez, J. Phillips, K. Rados, A. Zervos, E. Politis, et al. Modelling and measuring flow and wind turbine wakes in large wind farms offshore. *Wind Energy*, 12(5):431–444, 2009.

- [21] R. J. Barthelmie, O. Rathmann, S. T. Frandsen, K. Hansen, E. Politis, J. Prospathopoulos, K. Rados, D. Cabezón, W. Schlez, J. Phillips, et al. Modelling and measurements of wakes in large wind farms. In *Journal of Physics: Conference Series*, volume 75, page 012049. IOP Publishing, 2007.
- [22] P. Beacage, N. Robinson, M. Brower, and C. Alonge. Overview of six commercial and research wake models for large offshore wind farms. In *Proceedings EWEA 2012*, Copenhagen, 2012.
- [23] L. Beaudet. *Etude expérimentale et numérique du décrochage dynamique sur une éolienne à axe vertical de forte solidité*. PhD thesis, Université de Poitiers, 2014.
- [24] H. Bolnot, S. Le Dizès, and T. Leweke. Pairing instability in helical vortices. In *Wind Energy-Impact of Turbulence*, pages 23–28. Springer, 2014.
- [25] E. Branlard. *Wind Turbine Aerodynamics and Vorticity-Based Methods: Fundamentals and Recent Applications*, volume 7. Springer, 2017.
- [26] E. Branlard, K. Dixon, and M. Gaunaa. Vortex methods to answer the need for improved understanding and modelling of tip-loss factors. *IET Renewable Power Generation*, 7(4):311–320, 2013.
- [27] L. Bricteux. *Simulation of turbulent aircraft wake vortex flows and their impact on the signals returned by a coherent Doppler Lidar system*. PhD thesis, Faculté des Sciences Appliquées-Université catholique de Louvain, March 2008.
- [28] C. Brower and N. Robinson. The openWind deep-array wake model : Development and validation. Technical report, AWS Truepower, 2012.
- [29] M. Buhl. A new empirical relationship between thrust coefficient and induction factor for the turbulent windmill state. Technical Report TP-500-36834, NREL, August 2005.
- [30] T. Burton, D. Sharpe, N. Jenkins, and E. Bossanyi. *Wind energy handbook (Second Edition)*. John Wiley & Sons, 2011.

- [31] M. Calaf, C. Meneveau, and J. Meyers. Large eddy simulation study of fully developed wind-turbine array boundary layers. *Physics of Fluids*, 22(1):–, 2010.
- [32] P. Chatelain, S. Backaert, G. Winckelmans, and S. Kern. Large eddy simulation of wind turbine wakes. In W. Rodi, editor, *Proceedings of The 9th International Symposium on Engineering Turbulence Modelling and Measurements (ETMM-9), June 6–8, 2012, Thessaloniki, Greece*, volume 91 of *Flow, Turbulence and Combustion*, pages 587–605. ERCOFTAC, Springer, Jun 2013.
- [33] P. Chatelain, M. Duponcheel, D. Caprace, Y. Marichal, and G. Winckelmans. Vortex particle-mesh simulations of vertical axis wind turbine flows: from the airfoil performance to the very far wake. *Wind Energy Science*, 2(1):317–328, 2017.
- [34] P. Chatelain, M. Duponcheel, S. Zeoli, S. Buffin, D. Caprace, G. Winckelmans, and L. Bricteux. Investigation of the effect of inflow turbulence on vertical axis wind turbine wakes. *Journal of Physics: Conference Series*, 854(1):012011, 2017.
- [35] P. Chatelain, Y. Marichal, M. Duponcheel, and G. Winckelmans. Vortex methods applied to the large eddy simulation of wind turbine flows. In *2nd Frontiers in Computational Physics Conference: Energy Sciences, 3-5 June, Zurich, Switzerland*, 2015.
- [36] M. J. Churchfield, S. J. Schreck, L. A. Martinez, C. Meneveau, and P. R. Spalart. An advanced actuator line method for wind energy applications and beyond. In *35th Wind Energy Symposium*, page 1998, 2017.
- [37] M. Churchfield, S. Lee, J. L.A. Martinez, S. Leonardi, G. Vijayakumar, and J. Brasseur. A Large-Eddy Simulation of Wind-Plant Aerodynamics. *AIAA Aerospace Science Meeting paper*, ((preprint)), January 9-12 2012.
- [38] M. Churchfield, P. Moriarty, Y. Hao, M. Lackner, R. Barthelmie, J. Lundquist, and G. Oxley. A comparison of the dynamic wake meandering model, large-eddy simulation, and field data at the egmond ann zee offshore wind plant. Technical Report NREL/CP-5000-63321, NREL.
- [39] R. Cocle, L. Dufresne, and G. Winckelmans. Investigation of multiscale subgrid models for les of instabilities and turbulence in wake vortex sys-

- tems. In S. C. Kassinos, C. A. Langer, G. Iaccarino, and P. Moin, editors, *Complex Effects in Large Eddy Simulations*, pages 141–159, Berlin, Heidelberg, 2007. Springer Berlin Heidelberg.
- [40] R. Cocle, L. Bricteux, and G. Winckelmans. Scale dependence and asymptotic very high Reynolds number spectral behavior of multiscale subgrid models. *Physics of Fluids*, 21(8), 2009.
- [41] R. Cocle, G. Winckelmans, and G. Daeninck. Combining the vortex-in-cell and parallel fast multipole methods for efficient domain decomposition simulations. *Journal of Computational Physics*, 227(21):9091 – 9120, 2008.
- [42] C. Collin and V. Moëns. Numerical investigation of an offshore vertical axis wind turbine farm, 2017.
- [43] G.-H. Cottet and P. D. Koumoutsakos. *Vortex methods: theory and practice*. Cambridge university press, 2000.
- [44] N. Coudou, M. Moens, Y. Marichal, J. Van Beeck, L. Bricteux, and P. Chatelain. Development of wake meandering detection algorithms and their application to large eddy simulations of an isolated wind turbine and a wind farm. In *Journal of Physics: Conference Series (proceedings of Torque 2018)*. IOP Publishing, accepted.
- [45] M. Draper, A. Guggeri, and G. Usera. Validation of the actuator line model with coarse resolution in atmospheric sheared and turbulent inflow. *Journal of Physics: Conference Series*, 753(8):082007, 2016.
- [46] M. Duponcheel, L. Bricteux, M. Manconi, G. Winckelmans, and Y. Bartosiewicz. Assessment of RANS and improved near-wall modeling for forced convection at low Prandtl numbers based on LES up to $Re_\tau = 2000$. *International Journal of Heat and Mass Transfer*, 75:470–482, 2014.
- [47] M. Duponcheel, P. Orlandi, and G. Winckelmans. Time-reversibility of the Euler equations as a benchmark for energy conserving schemes. *Journal of Computational Physics*, 227(19):8736 – 8752, 2008.
- [48] M. Duponcheel. *Direct and large-eddy simulation of turbulent wall-bounded flows - Further development of a parallel solver, improvement of multi scale sub grid models and investigation of vortex pairs in ground*

- effects*. PhD thesis, Faculté des Sciences Appliquées-Université catholique de Louvain, October 2009.
- [49] M. Duponcheel, C. Leroi, S. Zeoli, G. Winckelmans, L. Bricteux, E. De Jaeger, and P. Chatelain. Investigation of the complete power conversion chain for small vertical-and horizontal-axis wind turbines in turbulent winds. 1037(7):072046, 2018.
 - [50] C. S. Ferreira, H. A. Madsen, M. Barone, B. Roscher, P. Deglaire, and I. Arduin. Comparison of aerodynamic models for vertical axis wind turbines. In *Journal of Physics: Conference Series*, volume 524, page 012125. IOP Publishing, 2014.
 - [51] S. T. Frandsen. Turbulence and turbulence-generated structural loading in wind turbine clusters. Technical Report Risø-R-1188, Risø National Laboratory, 2007.
 - [52] P. M. Gebraad and J. Van Wingerden. A control-oriented dynamic model for wakes in wind plants. In *Journal of Physics: Conference Series*, volume 524, page 012186. IOP Publishing, 2014.
 - [53] L. Georges. *Development and validation of a LES methodology for complex wall-bounded flows - Application to high-order structured and industrial unstructured solvers*. PhD thesis, Faculté des Sciences Appliquées-Université catholique de Louvain, June 2007.
 - [54] P. Giguere and M. S. Selig. Design of a tapered and twisted blade for the nrel combined experiment rotor. Technical Report SR-500-26173, NREL, April 1999.
 - [55] L. Gilling. Tugen synthetic turbulence generator, manual and user's guide. Technical report, Aalborg University - Department of Civil Engineering, Division of Structural Mechanics, 2009.
 - [56] H. Glauert. *The analysis of experimental results in the windmill brake and vortex ring states of an airscrew*. HM Stationery Office, 1926.
 - [57] H. Glauert. Airplane propellers. In *Aerodynamic Theory*, pages 169–360. Springer Berlin Heidelberg, 1935.
 - [58] H. Glauert. *The Elements of Aerofoil and Airscrew Theory*. The University Press, 1926.

- [59] T. Göçmen, P. van der Laan, P.-E. Réthoré, A. P. Diaz, G. C. Larsen, and S. Ott. Wind turbine wake models developed at the technical university of denmark: A review. *Renewable and Sustainable Energy Reviews*, 60:752–769, 2016.
- [60] M. Hand, D. Simms, L. Fingersh, D. Jager, J. Cotrell, S. Schreck, and S. Larwood. Unsteady aerodynamics experiment phase vi: wind tunnel test configurations and available data campaigns. Technical Report TP-500-29955, NREL, December 2001.
- [61] K. S. Hanse. Presentation of horns rev offshore wind farm and vestas v80 wind turbine. Technical report, IEA Wind Task 31 Wake Bench, 2012.
- [62] K. S. Hansen, R. J. Barthelmie, L. E. Jensen, and A. Sommer. The impact of turbulence intensity and atmospheric stability on power deficits due to wind turbine wakes at horns rev wind farm. *Wind Energy*, 15(1):183–196, 2012.
- [63] M. H. Hansen, A. D. Hansen, T. J. Larsen, S. Øye, P. Sørensen, and P. Fuglsang. Control design for a pitch-regulated, variable speed wind turbine. Technical report, 2005.
- [64] R. Hockney and J. Eastwood. *Computer Simulation using Particles*. Taylor & Francis, Inc. Bristol, PA, USA, 1988.
- [65] Z. Q. Hu, L. Li, J. Wang, Q. H. Hu, and M. C. Shen. Dynamic responses of a semi-type offshore floating wind turbine during normal state and emergency shutdown. *China Ocean Engineering*, 30(1):97–112, 2016.
- [66] G. V. Iungo, F. Viola, S. Camarri, F. Porté-Agel, and F. Gallaire. Linear stability analysis of wind turbine wakes performed on wind tunnel measurements. *Journal of Fluid Mechanics*, 737:499–526, 2013.
- [67] S. Ivanell. *Numerical computations of wind turbine wakes*. PhD thesis, Royal Institute of Technology, Linne Flow Centre, Department of Mechanics, 2009.
- [68] S. Ivanell, R. Mikkelsen, J. N. Sørensen, and D. Henningson. Stability analysis of the tip vortices of a wind turbine. *Wind Energy*, 13(8):705–715, 2010.

- [69] H. Jeanmart and G. Winckelmans. Comparison of recent dynamic subgrid-scale models in turbulent channel flow. In *Proceedings of the summer program*, pages 105–116, 2002.
- [70] H. Jeanmart and G. Winckelmans. Investigation of eddy-viscosity models modified using discrete filters: a simplified regularized variational multi-scale model and an enhanced field model. *Physics of fluids*, 19(5):055110, 2007.
- [71] N. O. Jensen. A note on wind generator interaction. Technical Report Risø-M-2411, Risø National Laboratory, 1984.
- [72] P. K. Jha and S. Schmitz. Actuator curve embedding—an advanced actuator line model. *Journal of Fluid Mechanics*, 834, 2018.
- [73] P. Jha, M. Churchfield, P. Moriarty, and S. Schmitz. Guidelines for volume force distributions within actuator line modeling of wind turbines on large-eddy simulation-type grids. *Journal of Solar Energy Engineering*, 136(3):031003, 2014.
- [74] A. Jimenez, A. Crespo, E. Migoya, and J. Garcia. Advances in large-eddy simulation of a wind turbine wake. *Journal of Physics: Conference Series. The Science of Making Torque from Wind. Technical University of Denmark*, August 2007.
- [75] J. Jonkman, S. Butterfield, W. Musial, and G. Scott. Definition of a 5-MW Reference Wind Turbine for Offshore System Development. Technical Report TP-500-38060, NREL, February 2009.
- [76] I. Katic, J. Højstrup, and N. Jensen. A simple model for cluster efficiency. In *European wind energy association conference and exhibition*, Italy, 7-9 October 1986.
- [77] H. J. T. Kooijman, C. Lindeburg, D. Winkelaar, and E. van der Hooft. Dowec 6 MW Pre-design: Aero-elastic modeling of the DOWEC 6 MW pre-design in PHATAS. Technical report, DOWEC Dutch Offshore Wind Energy Converter 1997-2003 Public Reports, Petten, the Netherlands, September 2003.
- [78] G. C. Larsen, H. A. Madsen, K. Thomsen, and T. J. Larsen. Wake meandering: a pragmatic approach. *Wind energy*, 11(4):377–395, 2008.

- [79] T. J. Larsen, M. H. Hansen, and F. Iov. *Generator dynamics in aeroelastic analysis and simulations*. 2003.
- [80] M. Lee, B. Oh, and Y. Kim. Canonical fractional-step methods and consistent boundary conditions for the incompressible navier-stokes equations. *Journal of Computational Physics*, 168(1):73–100, March 2001.
- [81] M. Lejeune. Large eddy simulation of a convective atmospheric boundary layer, 2018.
- [82] T. Leweke, S. Le Dizès, and C. H. Williamson. Dynamics and instabilities of vortex pairs. *Annual Review of Fluid Mechanics*, 48:507–541, 2016.
- [83] L. Lignarolo, D. Ragni, C. Ferreira, and G. Van Bussel. Experimental comparison of a wind-turbine and of an actuator-disc near wake. *Journal of Renewable and Sustainable Energy*, 8(2):023301, 2016.
- [84] L. Lignarolo, D. Ragni, C. S. Ferreira, and G. Van Bussel. Kinetic energy entrainment in wind turbine and actuator disc wakes: an experimental analysis. In *Journal of Physics: Conference Series*, volume 524, page 012163. IOP Publishing, 2014.
- [85] L. Lignarolo, D. Ragni, F. Scarano, C. S. Ferreira, and G. van Bussel. Tip-vortex instability and turbulent mixing in wind-turbine wakes. *Journal of Fluid Mechanics*, 781:467–493, 2015.
- [86] C. Lindenburg. Investigation into rotor blade aerodynamics. Technical Report ECN-C-03-025, ECN, July 2003.
- [87] H. Madsen, C. Bak, M. Døssing, R. Mikkelsen, and S. Øye. Validation and modification of the blade element momentum theory based on comparisons with actuator disc simulations. *Wind Energy*, 13(4):373–389, 2010.
- [88] J. Mann. The spatial structure of neutral atmospheric surface-layer turbulence. *Journal of Fluid Mechanics*, 273:141–168, 8 1994.
- [89] J. Mann. Wind field simulation. *Probabilistic Engineering Mechanics*, 13(4):269 – 282, 1998.
- [90] Y. Marichal, I. De Visscher, P. Chatelain, and G. Winckelmans. Towards physics-based operational modeling of the unsteady wind turbine

- response to atmospheric and wake-induced turbulence. In *Journal of Physics: Conference Series*, volume 854, page 012030. IOP Publishing, 2017.
- [91] Y. Marichal, I. De Visscher, P. Chatelain, and G. Winckelmans. Identification and quantification of vortical structures in wind turbine wakes for operational wake modeling. In *Journal of Physics: Conference Series*, volume 753, page 032050. IOP Publishing, 2016.
- [92] L. Martínez-Tossas, M. Churchfield, and S. Leonardi. Large eddy simulations of the flow past wind turbines: actuator line and disk modeling. *Wind Energy*, 18(6):1047–1060, 2015.
- [93] L. Martínez-Tossas and S. Leonardi. Wind turbine modeling for computational fluid dynamics. Technical Report SR-5000-55054, NREL, July 2013.
- [94] D. Mehta, A. van Zuijlen, B. Koren, J. Holierhoek, and H. Bijl. Large Eddy Simulation of wind farm aerodynamics: A review. *Journal of Wind Engineering and Industrial Aerodynamics*, 133:1 – 17, 2014.
- [95] J. Meyers and C. Meneveau. Optimal turbine spacing in fully developed wind farm boundary layers. *Wind Energy*, 15(2).
- [96] R. Mikkelsen. *Actuator Disc Methods Applied to Wind turbines*. PhD thesis, Fluid Mechanics, Departement of Mechanical Engineering, Technical University of Denmark., June 2003.
- [97] M. Moens, M. Duponcheel, G. Winckelmans, and P. Chatelain. Les of wind farm response to transient scenarios using a high fidelity actuator disk model. In *Journal of Physics: Conference Series*, volume 753, page 032053. IOP Publishing, 2016.
- [98] M. Moens, M. Duponcheel, G. Winckelmans, and P. Chatelain. An actuator disk method with tip-loss correction based on local effective upstream velocities. *Wind Energy*, 2018.
- [99] J. Monaghan. Extrapolating B splines for interpolation. *Journal of Computational Physics*, 60(2):253–262, 1985.

- [100] N. Mortensen, D. Heathfield, and L. Myllerup. Wind atlas analysis and application program: Wasp 8 help facility. Technical report, Risø National Laboratory, Roskilde, Denmark, 2005.
- [101] W. Munters, C. Meneveau, and J. Meyers. Turbulent inflow precursor method with time-varying direction for large-eddy simulations and applications to wind farms. *Boundary-Layer Meteorology*, pages 1–24, 2016.
- [102] J. M. Österlund, A. V. Johansson, H. M. Nagib, and M. H. Hites. A note on the overlap region in turbulent boundary layers. *Physics of Fluids*, 12(1):1–4, 2000.
- [103] H. Özedmir, M. Versteeg, and A. Brand. Improvements in ecn wake model.
- [104] I. Paraschivoiu. *Wind turbine design: with emphasis on Darrieus concept*. Presses inter Polytechnique, 2002.
- [105] J. Park, S. Basu, and L. Manuel. Large-eddy simulation of stable boundary layer turbulence and estimation of associated wind turbine loads. *Wind Energy*, 17(3):359–384, 2014.
- [106] F. Porté-Agel, Y.-T. Wu, H. Lu, and R. J. Conzemius. Large-eddy simulation of atmospheric boundary layer flow through wind turbines and wind farms. *Journal of Wind Engineering and Industrial Aerodynamics*, 99(4):154–168, 2011. The Fifth International Symposium on Computational Wind Engineering.
- [107] F. Porté-Agel, Y.-T. Wu, H. Lu, and R. Conzemius. Large-eddy simulation of atmospheric boundary layer flow through wind turbines and wind farms. *Journal of Wind Engineering and Industrial Aerodynamics*, 99:154–168, 2011.
- [108] L. Prandtl. Appendix to betz’s article: Schraubenpropeller mit geringstem energieverlust - mit einem zusatz von l. prandtl. *Göttinger Klassiker der Strömungsmechanik Bd*, 3:89–92, 1919.
- [109] P.-E. Réthoré, K. S. Hansen, R. Barthelmie, S. Pryor, G. Sieros, J. Prospathopoulos, J. Palma, V. Gomes, G. Schepers, P. Stuart, et al. Benchmarking of wind farm scale wake models in the eera-dtoc project. In *International Conference on aerodynamics of Offshore Wind Energy*

- Systems and wakes (ICOWES 2013)*. Technical University of Denmark (DTU), 2013.
- [110] A. G. Rodriguez, A. G. Rodríguez, and M. B. Payán. Estimating wind turbines mechanical constants. In *Proc. Int. Conf. Renewable Energies and Power Quality (ICREPQ07)*, pages 27–30, 2007.
 - [111] T. M. P. C. S. Backaert, S. Kern. Gallery of fluid motion, aps-dfd 2012.
 - [112] B. Sanderse. Aerodynamics of wind turbine wakes. *Energy research Centre of the Netherlands. ECN-E-09-016*, 2009.
 - [113] S. Sarmast, R. Dadfar, R. F. Mikkelsen, P. Schlatter, S. Ivanell, J. N. Sørensen, and D. S. Henningson. Mutual inductance instability of the tip vortices behind a wind turbine. *Journal of Fluid Mechanics*, 755:705–731, 2014.
 - [114] S. Sarmast, W. Z. Shen, W. J. Zhu, R. F. Mikkelsen, S. Breton, and S. Ivanell. Validation of the actuator line and disc techniques using the new mexico measurements. *Journal of Physics: Conference Series*, 753(3):032026, 2016.
 - [115] J. G. Schepers. Engineering models in wind energy aerodynamics. *Aerospace Engineering. Delft University of Technology*, 2012.
 - [116] S. Schmitz and D. Maniaci. Methodology to determine a tip-loss factor for highly loaded wind turbines. *AIAA Journal*, 55(2):341–351, 2016.
 - [117] W. Shen, R. Mikkelsen, J. Sørensen, and C. Bak. Tip loss corrections for wind turbine computations. *Wind Energy*, 8(4):457–475, 2005.
 - [118] W. Shen, J. Sørensen, and R. Mikkelsen. Tip Loss Correction for Actuator/Navier–Stokes Computations. *Journal of Solar Energy Engineering*, 127(2):209–213, 04 2005.
 - [119] W. Shen, W. Zhu, and J. Sørensen. Study of tip loss corrections using cfd rotor computations. *Journal of Physics: Conference Series*, 555(1):012094, 2014.
 - [120] J. Smagorinsky. General circulation experiments with the primitive equations. *Monthly Weather Review*, 91(3):99–164.

- [121] H. Snel, J. Schepers, and B. Montgomerie. The mexico project (model experiments in controlled conditions): The database and first results of data processing and interpretation. *Journal of Physics: Conference Series*, 75(1):012014, 2007.
- [122] O. Søren, J. Berg, and N. Morten. Linearised cfd models for wakes. Technical Report Risø-R-1772, Technical University of Denmark, 2011.
- [123] J. N. Sørensen, R. F. Mikkelsen, D. S. Henningson, S. Ivanell, S. Sarmast, and S. J. Andersen. Simulation of wind turbine wakes using the actuator line technique. *Phil. Trans. R. Soc. A*, 373(2035):20140071, 2015.
- [124] J. Sørensen. *General momentum theory for horizontal axis wind turbines*, volume 4. Springer, 2016.
- [125] J. Sorensen and W. Z. Shen. Numerical modeling of wind turbine wakes. *Journal of Fluids Engineering*, 124(393), 2002.
- [126] R. J. Stevens, J. Graham, and C. Meneveau. A concurrent precursor inflow method for large eddy simulations and applications to finite length wind farms. *Renewable energy*, 68:46–50, 2014.
- [127] R. J. Stevens, L. A. Martínez-Tossas, and C. Meneveau. Comparison of wind farm large eddy simulations using actuator disk and actuator line models with wind tunnel experiments. *Renewable Energy*, 116:470–478, 2018.
- [128] R. Storey, S. Norris, and J. Cater. An actuator sector method for efficient transient wind turbine simulation. *Wind Energy*, 18(4):699–711, 2015.
- [129] G. Tabor and M. Baba-Ahmadi. Inlet conditions for large eddy simulation: A review. *Computers and Fluids*, 39(4):553 – 567, 2010.
- [130] Q. Tarverne. Modélisation par forces surfaciques d’éoliennes à axe vertical, 2015.
- [131] O. Thiry. *Direct and large-eddy simulation of turbulent wall-bounded flows - Further development of a parallel solver, improvement of multi scale sub grid models and investigation of vortex pairs in ground effects*. PhD thesis, École polytechnique de Louvain-Université catholique de Louvain, January 2017.

- [132] N. Trolborg. *Actuator Line Modeling of Wind Turbine Wakes*. PhD thesis, Fluid Mechanics, Departement of Mechanical Engineering, Technical University of Denmark, June 2008.
- [133] E. Van der Hooft, P. Schaak, T. Van Engelen, et al. Wind turbine control algorithms. *DOWEC project-DOWEC-F1W1-EH-03-094/0, Task-3 report*, 2003.
- [134] G. van Kuik and L. Lignarolo. Potential flow solutions for energy extracting actuator disc flows. *Wind Energy*, 19(8):1391–1406, 2016.
- [135] G. van Kuik, W. Yu, S. Sarmast, and S. Ivanell. Comparison of actuator disc and joukowsky rotor flows, to explore the need for a tip correction. *Journal of Physics: Conference Series*, 625(1):012013, 2015.
- [136] O. Vasilyev. High order finite difference schemes on non-uniform meshes with good conservation properties. *Journal of Computational Physics*, 157(2):746–761, 2000.
- [137] L. Vermeer, J. Sørensen, and A. Crespo. Wind turbine wake aerodynamics. *Progress in Aerospace Sciences*, 39(6):467 – 510, 2003.
- [138] A. Vitsas and J. Meyers. Multiscale aeroelastic simulations of large wind farms in the atmospheric boundary layer. *Journal of Physics: Conference Series*, 753(8):082020, 2016.
- [139] R. Wilson. Aerodynamic behavior of wind turbines. In D. Spera, editor, *Wind Turbine Technology: Fundamental Concepts of Wind Turbine Engineering*, pages 231–232. ASME Press, New York, 1994.
- [140] A. Wimshurst and R. Willden. Analysis of a tip correction factor for horizontal axis turbines. *Wind Energy*, 20(9):1515–1528, 2017.
- [141] G. Winckelmans. Vortex methods. In E. Stein, R. De Borst, and T. Hugues, editors, *Encyclopedia of Computational Mechanics*, volume 3. Wiley, 2004.
- [142] G. S. Winckelmans and H. Jeanmart. *Assessment of Some Models for LES without/with Explicit Filtering*, pages 55–66. Springer Netherlands, Dordrecht, 2001.

- [143] Y.-T. Wu and F. Porté-Agel. Modeling turbine wakes and power losses within a wind farm using LES: An application to the Horns Rev offshore wind farm. *Renewable Energy*, 75:945–955, 2015.
- [144] Y.-T. Wu and F. Porté-Agel. Large-eddy simulation of wind-turbine wakes: evaluation of turbine parametrisations. *Boundary-layer meteorology*, 138(3):345–366, 2011.
- [145] X. Yang, S. Kang, and F. Sotiropoulos. Computational study and modeling of turbine spacing effects in infinite aligned wind farms. *Physics of Fluids*, 24(11), 2012.