

POLITECNICO DI MILANO DIPARTIMENTO DI SCIENZE E TECNOLOGIE AEROSPAZIALI DOCTORAL PROGRAMME IN ROTARY WING AIRCRAFT

WIND TUNNEL TESTING OF SCALED WIND TURBINE MODELS: AERODYNAMICS AND BEYOND

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Year 2013 – XXIV

Acknowledgments

This work has been totally funded by Vestas Wind Systems A/S, to whom I extend my warmest acknowledgments for giving me the opportunity to dedicate these last few years to research.

The success of this work is undoubtedly and largely due to the valuable cooperation with my supervisor, prof. Carlo L. Bottasso, to whom I extend my admiration for the competence and high availability expressed during the whole period of the thesis.

A strong support came from my colleagues S. Cacciola and C.E.D. Riboldi of the Poli-Wind research group, who in the last years shared with the author a small office, many hours of work, and also all those happy moments, but also troubles, that make our life unique and that is great to share with true friends.

Also many master's thesis students have contributed in the technical development of the experimental facility presented in this work; to you, S. Calovi, M. Capponi, L. Maffenini, S. Rota, C. Simeone, A. Simeone and S. Spinelli I extend my most heartfelt thanks for sharing so many moments and experiences related to work, but also moments of real fun and sincere friendship.

An invaluable support came also from M. Bassetti, A. Bezzolato, P. Bettini, M. Biava, F. Cadei, G. Campanardi, S. Ferragina, G. Galetto, prof. G. Gibertini, D. Grassi, M. Mauri, A. Maggiolini, R. Perini, L. Ronchi, P. Rubini, prof G. Sala, P. Schito and prof. A. Zasso of the Politecnico di Milano for numerous technical contributions, as well as a special thanks go to V. Petrović from University of Zagreb, whose collaboration has been essential in order to achieve the results reported in this thesis.

Finally, I wish to warmly thank my family, all my longtime friends and specially Laura: without their help and support, this endeavor would hardly have come to a conclusion.

"Theory guides. Experiment decides." An old saying in science.

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CHAPTER 1

INTRODUCTION

Wind energy, as other type of renewable sources of energy like solar and biomass, is showing a moment of great evolution and expansion. This is substantially due to an increasing cost of all energy products based on fossil fuels, which has forced the governments of the major industrial countries to diversify, first of all, sources for energy supply, as well as to an increasing and more widespread sensitivity from modern societies and political world to environmental issues, followed by ambitious objectives of gaseous pollutants reduction in the atmosphere. In this regard, the European Union (EU) has set the goal to achieve 20% share of renewable energies in the overall energy consumption by 2020 and it is expected that wind energy will have a major role in achieving this goal.

Until now a huge effort has been made by companies operating in the wind sector and by national governments in terms of financial support to the research, whose contribution to innovation has allowed the development of even more efficient and high-tech

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wind turbines. Furthermore, it is also quite clear as the final goal research and innovation should focus on is to lower the cost of wind energy, which has to become competitive when compared to the price of energy produced by traditional sources, like fossil fuels. However, this latter has not been reached yet and constant research and development is therefore needed.

If one looks at what has been done so far in the field of wind energy, he gets how the increase in innovations and technological content has been possible introducing advanced computing methods that allow, among the other things, the modeling of the wind field, of the wind turbine aerodynamics, dynamics and elasticity as well as their interaction. Indeed, the understanding and simulation of the wind energy conversion process for single wind turbines, as well as for wind farms, requires the ability to model multiple complex interacting physical processes that take place at diverse spatial and temporal scales. However, it is clear as the ability to effectively design wind energy systems ultimately relies on the fidelity to reality of the mathematical models used in simulations. Consequently, there is a need to validate such models and to calibrate their parameters so as to maximize their accuracy.

In all areas of science, validation and calibration have always been driven by experimentation, whose final goal is to simulate natural events under controlled conditions. When looking at wind energy, both in terms of single wind turbines or wind farm system, testing and measurements conducted in the field, although invaluable, present some critical issues. First, it is usually difficult to have complete and accurate knowledge of the environmental testing conditions, which by the way cannot in general be controlled, and, secondly, costs and testing time are often quite relevant.

To complement, support and, when possible, replace field testing, one can resort to the use of scaled models. In such testing conditions it is usually impossible to exactly match all relevant physics due to limitations of the scaling conditions and because of the frequent impossibility of assuring the same values of all non-dimensional parameters in the full scale and scaled environment. Nonetheless, a better control and knowledge of the testing conditions, errors and disturbances can be achieved. Furthermore, it may be possible to perform measurements which might not be feasible at full scale, and, even more interesting, the testing activity typically incurs in much lower costs. Given the above considerations, is clear as scaled testing does not replace simulation nor field testing, but works in synergy with both towards the goal of delivering validated and calibrated numerical simulation tools, as well as improving the knowledge of the problem at hand and evaluating new concepts and ideas.

In wind energy, several research studies related to wind tunnel testing of wind turbine models can be found in the literature, but the vast majority of these latter is related to the area of aerodynamics. Many tests have been conducted more than 20-30 years ago and their goals and results are reviewed in Vermeer et al.^[5]. However, many studies are much more recent; in particular, invaluable results were gained from wind tunnel experiments in the NASA-Ames wind tunnel on a 10 [m] diameter rotor (see Hand et al.^[6] and Simms et al.^[7]), where most emphasis was put on collecting a considerable amount of data which allowed to deepen the knowledge of many aerodynamic phenomena like dynamic stall and 3D rotational effect. Within the European Union, a similar project to the NREL experiment has been carried out under the acronym "MEXICO" (see Snel et al.^[8] and Schepers and Snel^[9]) on a three blades rotor model of 4.5 [m] diameter tested in the largest wind tunnel in Europe; a large amount of detailed pressure data, blade and total loads and quantitative flow field information have been recorded and shared with the project participants for the comparison with numerical results. A scaled model of the "MEXICO" rotor was also tested at the Korea Aerospace Research Institute low speed wind tunnel^[10], highlighting the influence of Reynolds number on the aerodynamic performance.

Many other wind tunnel experiments concerning the aerodynamics of wind turbine have been conducted on smaller models. The performance of a 0.9 [m] diameter model wind turbine were measured experimentally by Krogstad and Lund^[11] and the results, once compared to fully three-dimensional CFD simulations and computations based on blade element method, highlighted that the power coefficients are over-predicted at high tip speed ratios as well as the thrust force computed with BEM are consistently lower than measured values. The latter discrepancy suggested that the experiment were affected by wind tunnel wall effects, which must then be considered when analyzing the results from wind turbine testing. Adaramola and Krogstad^{[12][13]} experimentally investigated the wake interference effect on the performance of a downstream wind turbine using two similar model turbines with the same rotor diameter equal to 0.9 [m], with particular emphasis on the understanding the effects of turbines separation distance and upstream model yaw angle on the downstream turbine performance.

Wind-tunnel experiments have also been widely used to study wind turbine far and near wakes evolution under uniform flow conditions, like those reported in Oku et al.^[14], Medici and Alfredsson^[15] and Bartl et al.^[16], as well as wind-tunnel experiments have been designed and carried out with the purpose of understanding the effects of the atmospheric boundary layer and its stability on the wake structure, considering both stand-alone and multiple wind turbine wakes. About this topic several results can be found in Chamorro and Porté-Agel^[17], Cal et al.^[18], Zhang et al.^[19] and reference therein.

All the above mentioned studies have produced valuable information and measurements regarding the performance of rotors and the behavior of airfoils, blades and wakes, helping not only with the understanding of the involved aerodynamical physical processes, but also with the validation and calibration of suitable mathematical models.

Nonetheless, aerodynamics is only one of the phenomena that takes place in the wind energy conversion process and whose understanding is crucial for the most effective design and operation of wind turbines. In fact, design loads on wind turbines are mainly dictated by transient phenomena, where the effects of inertial and elastic loads, as well as of the closed-loop control laws used for a variety of tasks onboard the machine, play a very major role.

Looking at the literature, we get that few wind tunnel testing have been conducted for purposes other than purely aerodynamic. Among these, some had the goal of experimentally verifing the effectiveness of innovative concept suitable for reducing fatigue loads on wind turbine blades, like the implementation of actuated devices distributed along the blade spanwise and used to control the aerodynamic loading, with possible advantages in terms of mitigation of loads fluctuations and addiction of aerodynamic damping. Andersen et al.^[20] investigated the potential offered by variable trailing edge geometry on a fixed blade tested in the wind tunnel and, under prescribed pitch motion of the profile, remarkable reduction of the lift force oscillation were achieved by

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conveniently actuating the flap. Also van Wingerden et al.^[21] performed test on a two blades 1.8 [m] diameter rotor equipped with trailing-edge flaps based on piezo electric actuators. SISO and MIMO controllers were designed using piezo electric strain sensor readings as feedback signals and the test, conducted in an open jet wind tunnel, showed a significant reduction of the dynamics amplitude. Furthermore, both experiments show the potential benefits of testing in a controlled environment and highlight that the aeroelastic scaling, i.e. the ability to represent the mutual interactions of aerodynamic, elastic and inertial forces, is a key factor for directly upscaling the results obtained by wind tunnel testing.

Given the above considerations, we understand that there is the possibility to further exploit the advantages of testing in the wind tunnel, that, in fact, can support either predominantly aerodynamic activities or experimental investigations on the aeroservoelasticity of wind turbines. In this thesis, expanded scaled wind tunnel testing beyond the sole domain of aerodynamics is then proposed. To this end, aeroelastically scaled models of a multi-MW wind turbine, highly instrumented and featuring active individual pitch and torque control, have been designed so as to deliver realistic aerodynamic performance. The models can be used for several purposes regarding, for example, the investigation of wakes, their characteristics and modeling, or for studying the machine response in extreme operating conditions (e.g., high speed high yawed flow, emergency shut-down in high winds, response following failures of onboard sub-systems, etc.), something that is difficult to do in the field, or in support of research on advanced pitch-torque control laws, on load and wind observers, as well as a variety of other aeroelastic investigations such as the study of the effects of loads induced within a wind farm by wake impingement caused by upstream wind turbines. The models can also provide valuable benchmarks for comparison and validation of numerical codes (e.g., LES, aero-elastic codes, etc.) and for testing the effectiveness of system identification techniques. Moreover, among the unique characteristics of the present experimental facility there are the testing of wind farm control algorithms, that can improve the wind farm efficiency by strategical control of the power extraction of the individual turbines, as well as a better understanding of the potential synergistic effect of active and passive load reduction techniques for wind turbines, thanks to the correct modeling of the aero-servo-elastic interaction.

1.1 Innovative content of the thesis

One of the main innovative content of this thesis has been the design from scratch of the wind turbine model, whose purpose is to support experimental observations not only in the field of aerodynamics but also in the areas of aeroelasticity and control, for single and interacting wind turbines. The need to support these diverse applications dictated a number of specific design requirements which include a realistic energy conversion process enabled by good aerodynamic performance at the airfoil and blade level, translating into reasonable aerodynamic loads and damping, as well as wakes of realistic geometry, velocity deficit and turbulence intensity. This requirements have been met despite the test section dimensions constrained the model turbine size leading to severe mismatches in Reynolds number, to which it was possible to overcome thanks to the use of airfoils developed specifically for low Reynolds numbers and equipped with devices that improve the profiles efficiency, and thanks to the support of suitable identifica-

tion techniques used to improve the aerodynamic performance. Much effort has also been done in equipping the model with a comprehensive onboard sensorization, giving the ability to measure loads, accelerations and positions with good accuracy and bandwidth, as well as to provide the model with individual blade pitch and torque control capability, so as to enable the testing of modern control strategies with also a reasonable rendering of the principal dynamic effects due to the servo actuators. The design has been followed by the production of the various sub-components, whose proper operation has been then checked, and by the final assembly and verification of the entire system functionality.

Another innovative contribution introduced by this thesis is related to the development of a technological process and of a design method suitable for the production of aeroelastic blades for wind turbine models characterized, among the other things, by the proper placement of their natural frequencies with respect to the harmonic per rev excitations and by intrinsic capacity of alleviating the loads on the various wind turbine sub-components. The integration of the aero-elastic blades with the wind turbine model control capabilities becomes then a powerful tool in support of the exploration of possible synergistic effects between active and passive load reduction techniques for wind turbines rotor.

Furthermore, many of the applications shown in this thesis can be considered innovative, or because similar experiments have never been carried out in a wind tunnel, such as the testing of extreme operating conditions or advanced pitch-torque control laws, or because the processing of the information obtained through experimentation has contributed to the validation of advanced simulation codes or innovative wind turbine control support technologies, as occurred with the measurements of the wake evolution and its impingement on downwind wind turbines, and with the blade loads acquired with the model operating with high wind misalignment.

1.2 Thesis outline

The thesis is organized according to the following plan.

Chapter 2 describes the wind tunnel environment where the models are typically operated. The chapter continues with the presentation of the scaling laws which were applied to the reference multi-MW wind turbine data and that were used to define the design requirements for the model, whose general configuration is then shown. The description of the model is complemented by the presentation of the onboard sensors, the pitch and torque systems, as well as the real-time control, the model management system and the support tools that were designed for the testing, calibration and maintenance of the models and of their principal sub-components. Finally the comprehensive aeroservoelastic simulation environment of the experimental facility is presented.

Chapter 3 describes the design of the wind tunnel model rotor, whose aerodynamic performance have to be representative of those of a multi-MW wind turbine in terms of power, thrust and optimal TSR. The method used for adjusting the measured performance by the wall blockage effect is shown, as well as the mathematical tool developed for the identification of the aerodynamic properties of the model airfoils. In the end,

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the improvement of the rotor design, in terms of optimization of its performance, is explained and the comparison between the measured and numerical data is finally reported.

Chapter 4 begins with a brief description of well known active and passive load reduction techniques for wind turbines, focusing on the potential offered by the Individual blade Pitch Control (IPC) and the implications that its use poses on the design of the various wind turbine sub-components, as well as on the main advantages related to the exploitation of the anisotropic mechanical properties of composite materials in terms of loads alleviation. The chapter then discusses about the technology adopted for the manufacturing of the model aero-elastic blades. The tool developed in support of the blades design is then explained, as well as the heuristic approach that led to the fine tuning of the production technology. The designed of the aero-elastic blades is finally presented, together with the description of the first manufactured blade with passive loads alleviation.

In chapter 5 a number of non-aerodynamic and non-standard applications are presented. In particular, a wind misalignment estimator, used in support of active yaw control, is validated and the optimization of emergency shutdown maneuvers, including the calibration of a suitable mathematical model, is presented. Successively, the active control applications, mainly focused on regulation in wake interference conditions and higher harmonic individual blade pitch control, are presented. Finally, the results related to a purely aerodynamic application, related to the measurement of the wakes evolution and its interaction with a downstream model, are shown.

Conclusions, recommendations and future activities end the thesis at chapter 6.

CHAPTER 2

THE WIND TUNNEL ENVIRONMENT: A TOOL FOR WIND ENERGY RESEARCH APPLICATION

As mentioned in the introduction, the aim of this thesis is to demonstrate how testing in the wind tunnel can be an extremely powerful and versatile tool in support to the research in the field of wind energy, and this not only for strictly aerodynamic applications, but also for applications where the aero-servo-elastic interaction plays a fundamental role.

To support research in this area, it is necessary to set up cutting-edge tools, both experimental and numerical. The wind tunnel is therefore not seen as an isolated object, but as an environment where several tools interact with the sole purpose of improving the understanding of the physics that governs the operation of wind turbines.

In the continuation of the chapter the tools that have been designed and used for this purpose will then be described, starting from the description of the wind tunnel and re-

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lated instrumentation and supporting equipment. Afterwards, the wind turbine models used in the wind tunnel are described, while the chapter ends with the presentation of the sophisticated simulation tool extensively used in the course of the thesis.

2.1 The wind tunnel of Politecnico di Milano.

The **GVPM**^[22] is the close circuit wind tunnel which has been used for all the test carried out during this project (Fig. 2.1). Two test section are available: a low turbulence level test section, suitable for aerospace engineering applications, and a boundary layer large dimensions test section, built in the return tube, for civil engineering applications. The civil (boundary layer) test section (Fig. 2.2), where it is possible to achieve a max-



Figure 2.1: Plant configuration of the GVPM wind tunnel.

imum velocity of 14 [m/s], is situated on the second floor of the building, in the return tube, and has an overall dimension of $3.84 \times 13.84 \times 36$ [m] - respectively the height, width and length - that permits to perform tests on very large models with low blockage effects and to reproduce atmospheric boundary conditions.

The aeronautical test section (Fig. 2.2) has a section area of 4×3.84 m, it is located almost at ground height and it lies between the contraction cone and the diffuser; the maximum flow velocity is 55 [m/s].

2.1.1 Wind tunnel instrumentation

Many research activities that will be discussed in this thesis required accurate information about the wind tunnel flow, with special attention on wake evolution. For this regard, the sophisticated and automated traversing system shown in Fig. (2.3) has been design to allow local measurements of the wake using two tri-axial fiber-film probes Dantec 55R91. The traversing system has been conceived for scanning a cylindrical volume with a diameter of 3 [m] and a height equal to 4 [m], or for scanning a semi-elliptical plan 6.5 [m] wide and 3 [m] high.

All the probes are regularly and accurately calibrated, while the effect of the traversing system on the probes measurements, both in terms of flow deviation and module

2.1. The wind tunnel of Politecnico di Milano.



Figure 2.2: *Civil (left) and aeronautical (right) test section details.*





Figure 2.3: Traversing system specifically designed for rotor wake measurements.

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variation, has been estimated placing the traversing system in the empty aeronautical test section and acquiring the probes signals across the entire scanning area. In this way, it is possible to adjust, in post-processing, the probes measured data by the effect produced by the traversing system, so as to allow an extremely accurate measurement of the flow field.

2.1.2 Atmospheric boundary layer reproduction in the wind tunnel

The reproduction of the atmospheric boundary layer in the wind tunnel is essential in order to experimentally study the behavior of wind turbines in conditions that are representative of the real environment. The turbulence can be reproduced using active devices^[23,24] or passive devices^[25,26]. The active generation requires the use of complex and high-priced devices that, once conveniently installed and tuned, provide the enormous advantage of being able to reproduce a wide range of flow conditions with the desired mean wind velocity, turbulence intensity, turbulence scale, power spectral density and boundary layer profiles. All this can be achieved without changing in any way the wind tunnel configuration, with considerable save of time during the test set up phase. Unfortunately, the installation of these devices in wind tunnels of considerable size, as the **GVPM** is, is extremely expensive and complex, to the point that there are rarely other alternatives than using passive devices.

It is pretty obvious that suitable passive devices should be designed for each specific application and, to this purpose, well-established and widely used methods can be find in the literature^[27–29]. Such devices can be grouped into the following two "macro-classes" (see Fig. 2.4) according to their size and function:

- *spires*, i.e. structures of considerable size placed at the inlet; they define, in large part, the characteristics of the wind profile generated in the test section;
- *floor roughness*, i.e. smaller elements placed downstream with respect to the spires and whose main task is to continuously energize the boundary layer. The effect produce by these devices is however negligible at distances greater than 0.35 [m] from the floor^[30].



Figure 2.4: Passive devices used for reproducing the atmospheric boundary layer.

Given that the rotor of the 1:45 scaled wind turbine models (described further in §2.2.2) used for testing extends from about 0.9 up to 2.9 [m], it is clear that the use of floor

roughness would not produce significant effects on the flow felt by the models rotor, thereby making sufficient using only the spires. The only data required in order to definitively determine the spires shape is then the desired boundary layer profiles, which can be expressed as:

$$\frac{U}{U_e} = \left(\frac{z}{\delta}\right)^{\alpha_{bl}} \tag{2.1}$$

where

=mean wind speed

U

- U_e =wind speed external to the boundary layer
- z =distance from the floor
- δ =boundary layer thickness
- α_{bl} =power law coefficient

It is self-evident that the sole use of spires does not allow to reproduce an atmospheric boundary layer with all the desired characteristics, specially in terms of turbulence intensity, turbulence scale and power spectral density. Unfortunately, these limitations are intrinsically related to the wind tunnel testing combined with the use of passive devices, and it is not possible to overcome these drawbacks. However, there is the great advantage of being able to know with great accuracy the flow inside the wind tunnel with the certainty, also, that it is possible to reproduce the same identical environmental conditions at different times, which is hardly achievable in the field testing.



Figure 2.5: View of the in-house manufactured spires ($\alpha_{bl} = 0.4$) located at inlet.

Two set of spires have been then designed and in-house manufactured. The first set was designed to reproduce a wind profile characterized by $\alpha_{bl} = 0.2$, which is the reference value prescribed by the rules for on-shore wind turbines, while the second set (see Fig. 2.5) was designed to get $\alpha_{bl} = 0.4$, which is a fairly high value, and that has been conceived to stress the vertical transport of momentum and kinetic energy across the boundary layer flow due to the wake rotation. Both sets were also designed with the goal of having the entire models rotor immersed in the boundary layer; more details about the spires design can be found in Simeone^[31].

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A measurement campaign has been then performed using the apparatus described in §2.1.1, with the goal of mapping the flow in a wide area of the civil test section equipped with the realized spires. The data measured by the hot-wire probes with spires designed to get $\alpha_{bl} = 0.4$ are shown in Fig. (2.6).



Figure 2.6: *Measured longitudinal wind speed U (up-left); wind profile extracted from the flow data and power law which best fit the measures (up-right); longitudinal turbulence intensity (down-left) and integral scale (down-right) vs. distance from the wind tunnel floor.*

From the graphs we see that the wind profile obtained is well approximated by a power law with $\alpha_{bl} \approx 0.33$ and that the entire rotor disk is immersed in the boundary layer. The turbulence generated is moderate but still significant, especially in the lower part of the rotor disk, while the integral scale is much lower than that would be needed if we take as reference the ratio between the real atmospheric integral scale and the rotor diameter of a multi-MW wind turbine. Unfortunately, this discrepancy is one of the limitations associated with the use of the civil test section that has been conceived for the testing of 1:150–1:200 scaled models. This implies that it is not possible to reproduce, using passive devices, a boundary layer that correctly reproduces the 1:45 scaled atmospheric boundary layer.

2.2 Wind turbine models

The experimental study in the wind tunnel of the physics that governs a wind turbine, seen both as a single object or as part of a complex system such as a wind farm, requires the development of experimental models. During the project were therefore designed, manufactured and tested two identical models. Indeed, despite only two models can not reproduce an entire wind farm, they are sufficient to capture the main effects of wake-

wind turbine interaction, thereby being an extremely useful tool for the understanding of the phenomenon.

For the present project, models were designed with the goal of supporting experimental observations not only in the field of aerodynamics but also in the areas of aeroelasticity and control, in order to expand knowledge or physical insights that can help to validate or develop new theories or computer models. The need to support these diverse applications dictated a number of specific design requirements, which include:

- realistic energy conversion process enabled by good aerodynamic performance at the airfoil and blade level, translating into reasonable aerodynamic loads and damping, as well as wakes of realistic geometry, velocity deficit and turbulence intensity;
- aeroelastic scaling, i.e. the ability to represent the mutual interactions of aerodynamic, elastic and inertial forces, which implies the realization of a flexible machine;
- individual blade pitch and torque control, so as to enable the testing of modern control strategies;
- a comprehensive onboard sensorization of the machine, giving the ability to measure physical quantities to be used for feed-backing and codes validation.

Looking at all the above requirements, it is evident that the models should essentially reproduce a modern multi-MW wind turbine on a small scale. In the light of this consideration, it was therefore considered convenient to take a real wind turbine on the market as reference for the model design; it was therefore decided to use as a reference the V90 of Vestas Wind System A/S, that, as sponsor of the project, has kindly made available the technical data of the machine.

In the next pages, the model design will then be described, starting with the exposition of the scaling laws used to define its design requirements and ending with the presentation of the tools created in support of the models.

2.2.1 Scaling laws and requirements on the model design

The study which has led to the formulation of a specific set of scaling laws is presented here. These scaling laws have been fundamental to understand how the inertial, structural and aerodynamic proprieties of a size Megawatt wind turbine must be scaled.

2.2.1.1 Buckingham theorem: application on a simplified wind turbine model

When approaching a problem of non-dimensional analysis, the starting point is always the Buckingham's theorem^[32], otherwise called Π theorem. The theorem loosely states that if we have a physically meaningful equation involving a certain number, n, of physical variables, and these variables are expressible in terms of k independent fundamental physical quantities, then the original equation is equivalent to another once that involves a set of p = n - k dimensionless variables derived from the original variables. This provides a method for computing sets of dimensionless parameters from the given variables, even if the form of the equation is still unknown. However, the choice of dimensionless parameters is not unique: Buckingham's theorem only provides a way of

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generating sets of dimensionless parameters, and does not choose the most "physically meaningful".

What just explained has been applied to a simple wind turbine mathematical model based on three degrees of freedom: the tower tip fore-aft displacement x_T , the rotor azimuth ψ and the blade flapping angle φ , as we can see in Fig. (2.7), with the blades and the tower treated as rigid bodies, but taking into account their elasticity introducing springs of equivalent stiffness. The three equations of motion which govern the system



Figure 2.7: Scheme of the simplified wind turbine model.

dynamic are reported in (2.2), where T_e is the electrical torque provided by the generator, so is a function of the rotor speed, while T_a , M_a and F_a are, respectively, the torque generated by all the aerodynamic forces around the rotating axis of the turbine, the moment of the blade aerodynamic forces around the flapping hinge and the horizontal components of all the aerodynamic forces. For our purposes, these quantities can be treated as functions of the model degrees of freedom $\boldsymbol{x} = \{x_T, \psi, \varphi\}^T$ and their derivatives, the blade pitch β , the wind velocity U_{∞} , the blade geometry B_g – i.e. the rotor radius R, the chord c, the twist θ and airfoil shape – the air density ρ , the air viscosity μ and last the speed of sound a.

$$J_{b}(\dot{\Omega} - 2\dot{\varphi}\varphi\Omega) + T_{e}(\Omega) = T_{a}(\boldsymbol{x}, \dot{\boldsymbol{x}}, \beta, U_{\infty}, B_{g}, \rho, \mu, a)$$
$$J_{b}(\ddot{\varphi} + \Omega^{2}\varphi) + \frac{1}{2}\ddot{x}_{T}S_{b} + K_{\varphi}\varphi + C_{\varphi}\dot{\varphi} = M_{a}(\boldsymbol{x}, \dot{\boldsymbol{x}}, \beta, U_{\infty}, B_{g}, \rho, \mu, a)$$
$$(M_{T} + M_{b})\ddot{x}_{T} + (\ddot{\varphi} - \dot{\varphi}^{2}\varphi)S_{b} + C_{T}\dot{x}_{T} + K_{T}x_{T} = F_{a}(\boldsymbol{x}, \dot{\boldsymbol{x}}, \beta, U_{\infty}, B_{g}, \rho, \mu, a)$$
(2.2)

Therefore we can state that the dynamic of the simple wind turbine model is controlled

by the following equation:

 $F(\boldsymbol{x}, \dot{\boldsymbol{x}}, \ddot{\boldsymbol{x}}, M_b, M_T, J_b, S_b, K_T, C_T, K_{\varphi}, C_{\varphi}, U_{\infty}, \beta, B_g, t, \rho, \mu, a, g, T_e) = 0; \quad (2.3)$

where it has been added the gravity field, which obviously affects the dynamic, but whose contribution has been omitted for simplicity.

Considering that the dimensions of all the physical quantities of equation (2.3) can be expressed as a combination of only three fundamental dimensions, i.e. mass **M**, length **L** and time **T**, it is possible to derive the non-dimensional parameters which govern the system dynamic, and, for extension, the wind turbines dynamic. In particular, the non-dimensional parameters are represented by the tip-speed-ratio $\left(\lambda = \frac{\Omega R}{U_{\infty}}\right)$, the non-dimensional natural frequencies $\left(\widetilde{\omega}_i = \frac{\omega_i}{\Omega}\right)$, the non-dimensional time $(\tau = \Omega t)$ and the Reynolds $\left(Re = \frac{U_{\infty}c}{\nu}\right)$, Froude $\left(Fr = \frac{U_{\infty}^2}{gR}\right)$, Mach $\left(Ma = \frac{U_{\infty}}{a}\right)$ and Lock $\left(Lo = \frac{C_{L,\alpha}\rho cR^4}{J_b}\right)$ numbers. It is known that it is not possible to design a scaled model whose dimensionless parameters are exactly equal to the full scale ones. In particular, this limitation is strictly related to the environment where the models are tested, i.e. the wind tunnel.

2.2.1.2 Scaling laws formulation

Taking into consideration what aforementioned, we can observe that it is just necessary to fix the scale ratio between the geometry n_l and time n_t in order to obtain the target ratio between all the other physical quantities, like the inertia or the stiffness. Indeed, there is no way to scale arbitrarily the mass ratio, i.e the remaining fundamental dimension, being this last related to the cube of the geometry ratio and to the density ratio, that is equal to one. However, it is self-evident that all the dimensionless numbers can not have the same values in the model and in the real system; it is therefore important to understand which parameters must be kept constant, and which, indeed, can have a different values.

In this regard, what we can state is that:

- It is fundamental that the values of τ are held to be the same, otherwise the scaled model will be subject to periodical forces, due to the rotation of the blades, whose frequencies will not be correctly scaled, thus distorting the system dynamic response. Therefore, once fixed n_t, the rotor speed must be scaled as n_Ω = ¹/_{n_t};
- It is also very important that the values of λ are held to be the same, otherwise the scaled model kinematics will be substantially different from the unscaled one. Therefore, once fixed n_t , we obtain that the wind speed is scaled as $n_v = \frac{n_l}{n_v}$.
- It is necessary as well that the values of the Lock numbers are held to be the same, otherwise the ratio of aerodynamic to inertial forces will be different. Therefore, once fixed n_l , the inertia must be scaled as $n_j = \frac{1}{n_t}$;
- It is also necessary to scale correctly the non-dimensional natural frequencies which means having the same Campbell diagram, i.e. the same relative placement of harmonic excitations and natural frequencies.

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The remaining dimensionless parameters, therefore the Re, Fr and Ma numbers, play an important and complex role in determining the aerodynamics characteristics of a system and the influence of the gravity field on this one. What we can state is that it is impossible to design a model which operate at $Re_M = Re_{FS}$, where $(\cdot)_M$ indicates quantities of the (scaled) model and $(\cdot)_{FS}$ quantities of the physical (full scale) system. Furthermore, being a wind turbine normally operating at Mach lower than 0.3, we can have $Ma_M \leq Ma_{FS}$ and being sure that there will be no difference between the model and full system aerodynamics due to flow compressibility.

Taking into account what aforementioned, the values of the similarity parameters were here derived finding the best compromise between the Reynolds mismatch $\frac{Re_M}{Re_{FS}}$, which is related to the quality of aerodynamics of the scaled model, and the speed-up of the time scale $\frac{t_M}{t_{FS}}$, in order to avoid an excessive increase in the control bandwidth. In fact, very high control frequencies would make it difficult to test advanced control laws, which is one of the goals of the project, since such laws might imply non-negligible computational loads but still need to be operated in real-time on the model.

Given that the rotor dimension should not cause excessive blockage effects due to the interference with the wind tunnel walls, and should allow for the testing of at least two wind turbines in wake-interference conditions, the length scale factor $n_l = \frac{R_M}{R_{FS}}$ has been fixed equal to $\frac{1}{45}$, which means that the model diameter is 2 [m]. By letting $\frac{Re_M}{R_{FS}} = \frac{n_l^2}{n_t}$, the design of best compromise can then be expressed as the following minimization:

$$\min_{n_t} \left(\frac{k^2 n_l^2}{n_t} + n_t \right) \tag{2.4}$$

which yields $n_t = kn_l$, where k is a weight factor in the objective function.

This new definition of the scaling laws does not match the Froude number on the model, which is acceptable unless very large machines are considered, and corresponds to Mach scaling for the choice k = 1. Considering that a real wind turbine operates at wind speeds up to 25 [m/s] and that the maximum velocity achievable in the civil test section is 14 [m/s], what we obtain is a minimum value of k equal to 1.79. An higher value of k = 1.97 was therefore used and the scaling factors of the principal physical quantities resulting from the scaling laws defined above are reported in table (2.1).

 Table 2.1: Scaling factors used for mapping the V90 into the model characteristics.

Quantity	Scaling factor
Length	1:45
Time	1:22.84
Speed	1:1.97
Power	1:15477
Rotor speed	22.84:1
Torque	1:353574
Reynolds	1:88.64
Froude	11.6:1
Mach	1:1.97

2.2.1.3 Requirements on model design

The obtained scaling laws were used to define the requirements that guided the design of the model, here named V2, which will be briefly described in the next paragraphs. In particular, it is possible to view the model as a set of subsystems and/or sub-models, such as the aerodynamics, the system dynamics, the actuation subsystem, the sensors and the control, and for each one is possible to define specific requirements. However, it is important to keep in mind that the complexity of the model does not allow the compartmentalized design of each subsystem, that must be rather guided by a strong and comprehensive overview of the system and of the requirements that the model must fulfill.

Model geometry

Among the requirements that decisively affected the model design there are the overall model dimensions summarized in table (2.2), which were derived by the direct application of the scaling laws on the V90 data^[33].

Table 2.2: Main dimensions of the scaled model.

Quantity	Requirement
Rotor diameter [mm]	2000
Rotor tilt [deg]	6
Rotor cone [deg]	4
Rotor over-hang [mm]	$\approx \! 80$
Nacelle height [mm]	≈ 90
Nacelle width [mm]	≈ 90
Nacelle length [mm]	≈ 215
Spinner diameter [mm]	≈ 90
Tower height [mm]	≈ 1800

It is important to note that the design of the model for purposes related solely to the testing of control laws and related supporting technologies, would enable to be more flexible in terms of constraints on the size of the various sub-components. However, one of the main objectives is the study of the wake evolution and its interaction with the dynamics and aerodynamics of downstream wind turbines. It was thus decided to faithfully respect the geometrical proportions between rotor-tower-hub-nacelle sizes, so as to ensure the scalability of the results related to the wake characterization, being aware that the enforcing of these requirements significantly influenced the model layout.

Aerodynamic performance

The design of the model must take into account that the aerodynamic performance has to be qualitatively representative and comparable with those of multi-MW wind turbine, bearing in mind, however, the limits due to the Reynolds mismatch. Having available the V90 data, it was thus decided to use the same as reference. In particular, the aerodynamic design of the model must guarantee:

• a power curve as similar as possible to that of the reference machine, whose rated rotor speed, wind speed and power are around, respectively, 16 [rpm], 15 [m/s] and 3 [MW]. In fact, make a model characterized by a power curve representative

of a multi-MW machine is of fundamental importance in order to immediately scale the results obtained in the wind tunnel and related to the control of wind farms, as will be shown later.

• an optimal tip speed ratio closed to the V90 one. The fulfillment of this requirement is extremely important in order to scale immediately the results related to the near and far wake evolution, since the TSR determine the vortex brake-down position and, consequently, the interaction between the wake and the downstream wind turbines^[34].

Given the importance and complexity of the aerodynamic design and performance, an entire chapter of this thesis is dedicated to the treatment of this topic.

Sensor, actuation and control

The requirements on actuators characteristics were derived by the direct application of the scaling laws on the V90 technical data related to the actuation subsystem, while the sensors installed on the model are those necessary for the control and the supervisory of a real multi-MW wind turbine. In detail, the scaled model must to be equipped with:

- sensors for measuring the pitch angle of every blade with accuracy in the order of ± 0.2 [deg].
- sensors for measuring the azimuth angle, in order to allow testing of individual pitch control algorithms. The azimuth data is also used to get the rotor speed through numerical derivation;
- sensors for measuring the tower fore-aft and side-side accelerations. These measures are used for monitoring purpose and could also be used as feed-back signals by control algorithms that actively dampen the tower oscillations.
- sensors for measuring the loads acting on the tower, the main shaft and the blades; these are the measures most commonly used for control purpose.

At this stage, we should note that the V2 structure is subjected to minor mechanical stresses compared to the real system, being the stress, in first approximation, scaled with $n_{\sigma} = \frac{n_t^2}{n_t^2} \approx \frac{1}{3.88}$. It will be successively highlighted how the model design has been guided by the need to find suitable solutions for reliably measuring the loads acting on the model without compromising its structural stiffness.

As stated before, the scaling laws were used to get the pitch and torque actuator requirements reported in tables (2.3) and (2.4).

 Table 2.3: Pitch actuators requirement.

Quantity	Requirement
Max pitch rate [deg/s]	230
Max torque required [Nm]	0.25
Chord @ blade root [mm]	42
Bandwidth [Hz]	≈ 30

Given that one of the project objectives is the individual blade pitch control, it is deduced that the requirement on pitch actuator bandwidth is less than the desired one, i.e. about 10 times the nominal frequency of rotation. However, such value is too high and hardly achievable in real wind turbines, since the blades inertia is considerably high and the space available for housing the actuation system is very low and strictly related to the blade root size. It was therefore decided to limit the requirement on the bandwidth to about half the optimum value, being aware that it will be necessary to take into account this limitation in the implementation of the individual pitch control algorithms.

Table 2.4	:	Toraue	generator	speci	fications
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Quantity	Requirement
Max rotor speed [rpm]	420
Max torque [Nm]	≈ 6
Generator inertia [-]	$pprox 8\%$ of J_R
Bandwidth [Hz]	\geq 500

Among the requirements for the torque actuator there is also its inertia, that has to be approximately the 8% of the global rotor inertia J_R (considering also the eventual gear-head between the actuator and the rotor), while the actuator has to be fast enough so as to neglect its dynamics.

About the control and supervision of the system, it is necessary to equip the model with a real-time system capable of acquiring the readings of all the model sensors, as well as the data related to the environmental parameters such as the wind tunnel wind speed, density, temperature, etc. In addition, the real-time system must use this information as input to sophisticated control algorithms and communicate the output to the various actuators. All this must be executed in a fixed time not exceeding 4 [ms].

Model dynamic

As mentioned above, the correct scaling of the dynamics of a wind turbine requires that the Lock number, as well as the placement of the main natural modes of the system with respect to the rotor speed, are the same for both the model and the physical system. The constraint on the rotor Lock number results in a requirement on the value of the rotor inertia and, consequently, on the mass of the aero-elastic blades, as well as the constraint on an equivalent tower Lock number results in a requirement on the masses of the tower and of the nacelle-rotor group, as reported in table (2.5).

 Table 2.5: Components masses of the scaled system.

Quantity	Requirement
Blade mass [Kg]	≈ 0.070
Rotor mass [Kg]	≈ 0.440
Nacelle mass [Kg]	pprox 0.75
Tower mass [Kg]	≈1.85

About the frequencies placement, it is possible to limit the set of those natural modes which have to be correctly placed, and this can be done without jeopardizing the proper modeling of the main aero-elastic interaction phenomena. In particular, it must be guarantee the correct placement of:

- the first and second out-of-plane modes of the blades;
- the first in-plane mode of the blades;
- the first fore-aft and side-side modes of the tower;
- the first in-plane collective rotor mode.

Afterwards, it will be shown that it has not been possible to design a model characterized by a mass of the nacelle equal to slightly more than 700 [g], thus making it impossible to simultaneously satisfy the constraints on the tower mass and frequencies. It was then decided to retain only the constraint on the tower frequencies with the aim to reproduce the main phenomena of tower-rotor dynamic coupling, being aware that this means a greater stiffness of the tower itself, and then minor displacements.

Looking at the requirements for a correct reproduction of the rotor dynamics, it is clear that the same are difficult to meet, thus making a challenge the design and construction of the aero-elastic blades; this topic will be therefore discussed in a dedicated chapter. It should be also pointed out that, among the objectives for which the model was conceived, there is the study of the wake evolution in the wind tunnel, with the aim to validate and tune dedicate simulation codes; in this case, the use of aeroelastic blades would further complicate the achievement of the research scope, as it would require modeling the aero-structural interaction phenomena; this last can be instead avoided by using rigid blades, that is blades whose flexibility negligibly affects the rotor aerodynamics. In light of these conflicting requirements, the model has been equipped not only with aero-elastic blades but also with rigid blades, which have been widely used throughout the project and whose design will be discussed in the following pages.

2.2.2 Model design and characterization

If one considers the foregoing, it is clear that the objective of the model design was to combine the small size of the model with the need to lodge all the actuators, sensors, mechanical and electronic components necessary to guarantee the desired model functionality. In particular, the model has been designed, manufactured and assembled in the same way as a multi-MW wind turbine is conceived, that is viewing the system as a set of:

- mechanical components, i.e. the nacelle and its sub-components, the rotor and its sub-components, the tower and the foundation;
- onboard sensors and actuation system;
- control and supervisory system.

2.2.2.1 Model mechanical design

As with a real aero-generator, each mechanical sub-systems has been designed with the goal of being assembled and tested prior its use in the wind tunnel. This approach makes it possible to quickly assemble the model for testing, sure of the correct operation of each sub-component and thus with consistent save of time, and to use rigid and limited size packaging to store the entire model between successive tests, avoiding
eventual damages. Moreover, the whole system has been thought to be assembled in a modular manner and to enable a rapid substitution of the various sub-components in case of failure, limiting the loss of time otherwise dedicated to test activities. In this perspective shall also figured out the wide use, in the whole system, of miniaturized connectors instead of the classic welds.

The mechanical strength of the various sub-components was verified using loads numerically calculated with the tool described in §2.3 and opportunely increased by a large safety factor, so as to ensure the survivability of the model even in operating conditions totally beyond the expected range.

Nacelle design

Fig. (2.8) shows the model nacelle without spinner and nacelle cover, for clarity. The main structural member of the nacelle is constituted by a rectangular carrying box, that provides the right stiffness to the entire nacelle group; behind it, three electronic control boards, one for each pitch actuator, are welded on a flexible circuit (Fig. 2.9(a)) fixed to a triangular prism that rotates with the shaft. A 36-channels slip ring Moog AC6355–36A occupies the aft part of the nacelle, held in place by a plate connected to the main carrying box by four rods. Specifically designed cables and connectors (Fig. 2.9(b)) travel along the hollow shaft and allow the transmission of the power signals, as well as the measures from the various sensors and the pitch actuator control signals, to and from the slip ring.



Figure 2.8: General arrangement of the model nacelle.

The main shaft is mounted on two bearings and has been stiffness-designed so that the model 1^{st} in-plane collective rotor mode matches the scaled one of the full scale system. The shaft was also machined to increase the load-induced strains (Fig. 2.9(d))

and hosts 4 full strain gauge bridges (Fig. 2.9(c)) that measure the torsional and bending loads. An electronic board placed among the rotor and the nacelle provides the power supply, conditioning and A/D conversion of the shaft strain gauges signals.



(a) Pitch actuator control boards

(b) Nacelle slip ring, cables and connector



(c) Shaft strain gauges

(d) FEM local increased shaft stresses

Figure 2.9: Nacelle sub-components: flexible circuit used for transmitting the power and the PC-control signals to the pitch actuator control boards (a); cables and connectors used to transfer signals to and from the slip ring (b); main shaft strain gauges (c) and FEM local stresses (d)

The quadrature signal provided by an optical encoder disk placed on the shaft allows to measure the rotor azimuthal position, while a tri-axial accelerometer provides for measurements of the accelerations at the tower top. A pair of conical spiral gears (reduction ratio equal to 2) connect the shaft with a motor, housed in the top of the tower, that provides for the torque (and optionally speed) control of the rotor. The torque motor shaft is connected to the pinion of the conical spiral gears by means of a flexible joint so as to ensure the transmission of torque only (Fig. 2.10); the motorpinion group is than fixed to the nacelle in a precise position by means of 4 screws.

The functionality of the entire nacelle can be tested before entering in the wind tunnel making use of the test bench described in §2.2.3, while the main shaft and the carrying box are univocally joined, respectively, to the rotor and the tower top by means of small screws. Finally, a V90-similar nacelle cover (Fig. 2.11) is mechanically fixed above the carrying box to ensure a satisfactory quality of the flow in the central rotor area.



Figure 2.10: Scheme of the torque transmission components.

Rotor and blade design

Fig. (2.11) shows the rotor layout with rigid blades made of unidirectional-carbon-fiber. The model rotor has been designed to allow, in a first phase, the fastening of each blade to the hub by means of a passing pin and, subsequently, the fastening of the entire rotor to the main shaft. This constructive solution ensures a rapid assembly of the rotor and permits to use both rigid or aero-elastic blades keeping unchanged the configuration of the entire nacelle.

Unlike the real turbine, the model blade root (Fig 2.12(a)) is extremely complex and made up of various components that allow the pitch handling from -5 up to 90 [deg], with mechanical stops that avoid getting out of this range. In detail, each blade houses in its hollow root a pitch motor with built-in relative encoder. Contrary to the usual practice, the motor shaft remains stationary while the motor frame is fixed to the blade root and rotates with it, with consequent exploitation of a greater amount of space for the motor housing. The gear head backlash (± 1 [deg]) of the selected pitch motor (see §2.2.2.3) is removed using a torsional spring, which joints the blade root and the fixed-to-the-hub component. The spring constant and its assembly position were specifically conceived for having a spring restoring torque, with the blade pitch equal to 0 [deg], almost equal to the half of the maximum torque supplied by the actuator. Two ball bearings allow the pitch variation with reduced friction and simultaneously ensure the suppression of all the other degrees of freedom. It is also important to remark that this solution enables to test the operation of the pitch mechanism before entering in the wind tunnel and without the need to assemble blade on the hub.

The blade root layout is the same for both the aero-elastic and rigid blades. In particular, both are manufactured by accurately placing a machined steel component with four small bridges at the blade root and before starting the cure process. The four bridges serve to increase the load-induced strains and to improve the accuracy of the reading coming from the half strain gauge bridges (Fig. 2.12(b)) fixed over them. In the steel-made root there is also a through hole, whose axis is orthogonal to the zero-twist blade line, used to zero setting the pitch motors encoder. Another electronic board, placed in front of the rotor hub, provides the power supply, conditioning and A/D conversion of up to 6 strain gauges bridges while a V90-similar spinner is mechanically



Figure 2.11: General arrangement of the model rotor and tower.



(a) Blade assembly

(b) Blade strain gauges and stresses

Figure 2.12: Model blade assembly (a) and applied strain gauges with FEM local stresses(b).

fixed to the hub in order to ensure a satisfactory quality of the flow in the central rotor area.

The rigid blades were designed with the goal of being extremely stiff and, in the meanwhile, not too heavy if compared to the the mass target required for the aeroelastic blade. These requirements have been satisfied using unidirectional high modulus carbon fiber all over the blade and the same tools described in §4.3.1 were used for the design. Two sets of identical rigid blades were manufactured, one for each V2 model; we then proceeded to the identification of the blade properties using the approach described in Bottasso et al.^[35] and starting from the structural and inertial data estimated in the design phase. The physical parameters estimated by the identification procedure reasonably match:

- the measured blade mass and center of gravity;
- several measurements of the tip deflections under static and gravity-aligned loads applied at different locations along the blade span;
- the first two out-of-plane and first in-plane measured natural frequencies;
- the dynamic response related to the instantaneous release of a gravity-aligned load applied to the blade, that allowed to reasonably estimate the structural damping.

With regard to the inertial properties, the identified mass, as well as the inertia about the rotor axis of rotation, is little more than twice the aero-elastic blade target, highlighting that the rigid blades can be used in applications where the exact modeling of the blade dynamics and flexibility is not critical to catch the main physical phenomena involved.

Tower and ground fixing

The tower is composed by a structural alloy-made tube whose thickness distribution was designed so that the first fore-aft and side-side natural frequencies of the model nacelle-tower-balance group match the scaled ones of the full scale system, with the constraint of having a distribution of the external diameter as closed as possible to the V90 scaled one. The latter allows to capture the effects of the aerodynamic interaction between tower and rotor, as well as the tower effect on the wake evolution. As previously mentioned, it has not been possible to achieve the target nacelle mass prescribed by the scaling laws reported in §2.2.1.3, thus the tower stiffness is higher than the scaled one of the real tower.

Compressed air, blown in at the tower foot, travels along the hollow tower and cools the torque motor before escaping from a small hole in the back part of the tower top. The cables that connect the torque motor to its control and its power source unit travel along the hollow tower, while several cables travel along the outer wall of the tower. These latter transmit all the signals passing through the slip ring channels to the various electronic devices that supply and control the pitch actuators, as well as to the analog modules that read the output of the sensors housed in the nacelle.

At the foot of the tower, the model is mounted on a balance that provides measurements of the three force and three moment components at the tower base. The foundation of the model is composed of two parts: a thick rectangular plate rigidly anchored to the wind tunnel floor around whose center a second thick plate, fixed to the

lower side of the balance, can rotate; in this way it is possible to set as desired, and with great accuracy, the entire model yaw angle.

In support of the above description, Fig. (2.13) shows the model tested in the civil test section.



Figure 2.13: Model overview inside the civil test section.

2.2.2.2 Onboard sensors

The data coming from the sensors available on board are used by closed loop control algorithms that drive the actuators and by the supervisory system for switching between possible machine states and handling emergencies.

A fundamental parameter used by the control and the supervisory systems is the rotor speed, derived from the rotor azimuth. This latter is measured with an optical incremental encoder ($N_e = 1800$ count per revolution) provided with index track, so as to get absolute azimuth readings. The quadrature signal provided by the transmissive module is read in **4X** counting mode and the rotor speed is function of the number of the observed pulses inside a given time window, with a quantization error, at rated rotor speed, equal to $E_{\Omega} = \frac{60}{\Omega * N_e * T_{sc}} \simeq 0.57$ [%], with $T_{sc} = 4$ [ms] the observation window.

Other important inputs for the control algorithms are the shaft loads, derived from the signals provided by the strain gauges, which were applied very close to the rotor, so as to avoid the sensitivity of the measured loads, in particular the torque, to the frictional losses produced by the bearings, the gear head and the brushes of the slip ring. The sensitivity of the shaft loads transducer was calibrated using well known weights conveniently applied to stress the shaft simultaneously with torque (M_{XR} , positive when concordant to the direction of rotation), yawing bending moment (M_{ZR} , parallel to the pitch axis of the 1st blade, positive toward the tip) and nodding bending moment (M_{YR} , directed as to compose a right-handed triad with the two other shaft loads), with notation in agreement with Fig. (2.14(a)).

A full 3-by-3 sensitivity matrix were obtained by linear regression. Fig. (2.15(a)) highlights that relatively small errors have been found between the known applied



Figure 2.14: Rotating hub coordinate system and blade coordinate system (Source: Germanischer Lloyd^[1]).

loads and the corresponding ones reconstructed multiplying the sensitivity matrix by the strain gauges output.

Among the information most commonly used to test individual pitch control algorithms there are the blades root loads computed in the pitchable reference frame and expressed in terms of flap-wise $M_{YB,P}$ and edge-wise bending moments $M_{XB,P}$, with the former aligned with the zero-twist blade line, positive toward the trailing edge, and the latter positive if pointing versus the blade suction side, in agreement with the fixed reference frame of Fig. (2.14(a)) in case of zero pitch angle. The relation between the blade loads and the output voltages of the blade root strain gauges was calibrated using an approach similar to the one explained previously for the shaft loads transducer. After having verified the insensitivity of the blade loads transducer to axial and torsional loads, the full 2-by-2 sensitivity matrix were obtained by linear regression. Higher errors, expressed in per cent of the reference loads of typical operating conditions, are founded on the edge-wise loads reconstruction (see Fig. 2.15(b)). The reason lies in the lower magnitude of the edge-wise reference load, substantially affected by gravity, with respect to the flap-wise reference load, mostly related to the model aerodynamics, as can be expected considering that the model Froude number is about an order of magnitude greater than the full system one.

Other measures used by the control-supervisory system are the ones provided by the RUAG SG-Balance 192-6I placed at the tower root, with an accuracy on loads measurements equal to ± 0.3 [%] of the balance full scale loads, and the accelerations given by the PCB 356A17 tri-axial accelerometer. Finally, two Pt100 probes are used for monitoring the winding and gear head temperature of the torque actuator, with the supervisory system programmed to change the model state from POWER PRODUCTION to IDLING condition in case of generator overheating.

2.2.2.3 Actuation system design and characterization

In the following pages will be described in detail the model actuators and the results related to the verification of their performance.



Figure 2.15: Errors between the known applied loads and the corresponding reconstructed ones, expressed in per cent of the reference loads, related to shaft (a) and blade (b) loads transducers.

Torque control

The Maxon^[36] brushless motor EC-4-Pole-30BL-200W, equipped with the gear head GP32HP-14/1 and the magnetic encoder MR-500IMP, was selected as the torque actuator for the model on the basis of its characteristics, i.e. rotational speed up to 400 [rpm], dimension compatible with the housing space available at the top of the tower and maximum power slightly higher than the scaled one of the full scale system.

The torque actuator is driven by the 4-Q-EC-DES-50/5 control electronic which allows to make the motor operate also as a generator. The produced electrical power is dissipated by using the chopper 12-75V/5.0 [Ω] Shunt Regulator connected to an external 6.8 [Ω] resistance, which allows to dissipate a continuous power up to 300 [W].

The actuator can be controlled both in speed or in torque mode. Speed control is performed by appropriately sending the desired reference speed to the control electronic using Can-open communication protocol at a data transfer speed of 500 [Kb/s]. The gains of the 4-Q-EC-DES-50/5 internal PI speed controller –which uses the motor encoder measure as feed-back signal– were appropriately set to achieve a good speed reference tracking with low level of speed and current oscillations.

Usually, for small size wind tunnel models, the torque control is implemented in open loop, by forcing the actuating system to supply a desired current value i, related to the generator/motor torque via the known relationship $T_e = K_Q \cdot i$, with the consequent assumption of an invariable value of the torque constant K_Q . However, the latter condition is true when the electrical device operates in a restricted range of speed-temperature and when there are no mechanical organs interposed between the actuator and the system driven by it. This is exactly the opposite of what occurs in the model, where the rotor speed varies depending on the system operating region, the temperature is subject to considerable variations during operation, being the motor used at

the limit of its capacity, and the nacelle bearings and gear-head provide variable and difficult-to-predict friction torques. We therefore decide to develop a close-loop controller implemented on the Bachmann M1 real time system (see §2.2.2.4), which uses the torque measurement as feed-back signal and whose output is a current reference sent to the motor control electronic via its analogue input every 400 [μ s], i.e. ten time faster than the model control/supervisory frequency.

The main objectives of such control are the accurate tracking of the reference torque and an extremely fast response of the actuator against a variation of the reference. Indeed, a real multi-megawatt wind turbines torque control is typically very fast to the point that high level control algorithms typically assume that the generator torque tracks its reference almost instantaneously. However, the requirement on the fast response was found to be hampered by the not entirely negligible level of the oscillations observed on the model shaft torque measurements, specially with high rotor speed and torque levels. The source of the oscillations was traced back to the not perfectly homogenous mesh of the bevel gear teeth, that results in the excitation of the shaft natural modes and, cosequently, on a level of the high frequency oscillations in the order of around 10% of the rated value, as shown in Fig. (2.16).



Figure 2.16: Spectrum of the measured torque M_{XR} expressed in [%] of the rated torque T_r .

The necessary adequate suppression of these oscillations could put limitations on the controller speed, unless using the feed-forward shown in Fig. (2.17), which allows to design system response to reference change and to disturbance rejection independently^[37].

The implemented feed-forward is based on the plane:

$$T_g = \mathbf{a}\Omega + \mathbf{b}u + \mathbf{c},\tag{2.5}$$

where u is the current reference sent to the generator control electronic (i.e. controller output). The plane parameters **a**, **b** and **c** are determined from the steady state measurements on the test bench (see §2.2.3) where another motor is connected to the nacelle shaft, having the possibility to independently set the current of the model wind turbine generator and the speed of the second motor. The generator torque T_g , rotor speed Ω and current reference u were recorded over the whole generator operating region and the plane parameters were calculated using least squares method.



Figure 2.17: Torque control loop.

Because of the integral behavior of the PI controller, the offset C is not necessary for control, so actual feed-forward term is calculated as:

$$u_{FF} = \frac{1}{\mathbf{b}} T_{g,ref} - \frac{\mathbf{a}}{\mathbf{b}} \Omega, \qquad (2.6)$$

where $T_{g,ref}$ is the torque reference. Assuming well determined plane parameters (2.5), the feed-forward guarantees a fast torque response when the torque reference changes. Therefore the PI controller, whose main purpose is to compensate the disturbances mainly caused by slow temperature drift and ensure offset-free reference tracking, does not have to be very fast. This enables to low pass filtering the feed-back signals and, thus, to eliminate the oscillations in the torque measurements, ensuring an accurate and fast torque control.

Pitch control

The Faulhaber^[38] brushed motor 1724T018SR, equipped with the precision gear head 16/7-134:1 and the encoder IE2-512, was selected as the pitch actuator for the model on the basis of requirements concern accuracy and repeatability of pitch positioning, dimension compatible with the housing space available at the blade root, maximum pitch rate high enough to allow emergency shut down operations, and a bandwidth sufficiently wide to ensure individual pitch control capability.

Each pitch actuator is driven by its own MCDC3003C control board and all the three control boards are nodes of the same Controller Area Network (CAN). The boards receive-transmit data at 500 [Kb/s] from-to the real-time control unit using the Can-open communication protocol, with the main advantage, with respect to the standard serial communication, of having reduced the required number of slip ring channels. The pitch can be set individually for each blade appropriately sending the desired pitch value to each control board; this data is the reference input of two board internal PID controllers for speed and position that use the motor built-in encoder measure as feedback signal, and whose gains have been set using the Ziegler-Nichols open-loop method^[39].

The pitch actuator performance were checked using a cylindrical dummy blade, shorter than the real one, but with the same inertia around the pitch axis. The dummy blade effective pitch angle has been measured using a flexible linear strip potentiometer fixed over the dummy blade surface (see Fig. 2.25(b)). The linear relation between the

potentiometer voltage output and the pitch angle has been calibrated using the inclinometer WYLER CLINOTRONIC PLUS (accuracy ± 1 [arcmin]).

The static tests have confirmed a good repeatability and an acceptable accuracy, as shown in Fig. (2.18), where β_{LS} and β_{MCDC} are, respectively, the pitch angle measured by the linear strip and the motor encoder. In particular, the error between the two measures is always lower than ± 0.1 [deg] even if the test is carried out by increasing or decreasing the pitch; this confirm that the torsional spring used for removing the motor gear-head backlash is working as expected and that the motor encoder output can be used for measuring the blade pitch.



Figure 2.18: Static pitch actuator accuracy.

The dynamic tests were performed by imposing to the real-time control system to generate a periodical pitch reference β_{ref} as input to the actuator control board, with the frequency of the periodic signal varying from 1 to 30 [Hz] and amplitude $|\beta_{ref}|$ in the range 1–5 [deg], but not all combination were tested due to limit on maximum achievable pitch rate. The frequency content of the measured β_{LS} related to $|\beta_{ref}| = 1$ [deg] has been used to identify the transfer function $H(s) = \frac{\beta_{LS}(s)}{\beta_{ref}(s)}$ which relates the measured pitch and the reference input, as shows in Fig. (2.19).

The module diagram shows that the quasi-static error at frequency equal to 1 [Hz] is in the order of ± 0.1 [deg], hence confirming the static test results, and that it is possible to periodically change the blade pitch up to 5 [deg], at frequency equal to the nominal rotor speed, and up to 2 [deg], at frequency equal to the nominal twice-perrev. Moreover, the phase trend shows non negligible phase delay between the reference input and the measured pitch at frequency equal to 1P and 2P and these delays will be considered in the IPC formulation (see §5.4.4).

The pitch actuator transfer function, whose poles pairs are reported in table (2.6), was identified using a Matlab® algorithm based on Levi^[40]; the numerator order was fixed equal to 0 and the denominator order was gradually increased up to achieve a good match. Based on the identified transfer function, the bandwidth of the pitch actuator is 29.90 [Hz], totally in agreement with the requirement specified in §2.2.1.3.



Figure 2.19: Experimental and identified pitch actuator transfer function.

Table 2.6: Pair poles of the identified pitch actuator transfer function.

	Freq. [Hz]	Damping [-]
1^{st} pair	32.5	0.25
2^{nd} pair	19.6	0.85

2.2.2.4 Model real-time control-supervision

The architecture of the data acquisition, control and management system is shown in Fig. (2.20). The experimental model is controlled by a hard-real-time module implementing a supervisor of the machine states and pitch-torque control laws, similarly to what is done on a real wind turbine. Two implementations of this system have been developed, one using a real-time patch of the Linux operating system^[41] running on a standard PC, used in the first experimental tests, and the other using the industrial wind turbine Bachmann M1 system^[42], which has been used in all the tests whose results are reported in this thesis and which will be therefore described in the following paragraphs.

In particular, three analog acquisition modules and one counter module acquire all the on-board sensors readings, as well as the wind tunnel sensor readings (including wind speed, air temperature and humidity), at a sampling frequency equal to 250 [Hz], with exclusion of the shaft loads and tower top accelerations signals, which are filtered with 8^{th} order analog Butter filters and than sampled at 2.5 [KHz].

All the sensors readings are inputs of the supervisor-control algorithms, real-time executed by the M1-CPU unit every 4 [ms], whose outputs are pitch and torque demands sent to the actuators control boards via the M1-CAN module or by analog output.

The software package supplied with the Bachmann system contains a suite designed to manage every aspect of the development of the real-time controller, from the configuration of the hardware modules to the creation of applications using the **C** programming language. This latter aspect is of fundamental importance, since **C** is also used by the wind energy research group of Politecnico di Milano for the implementation of advanced control laws^[43,44] that interface with the simulation tool described in §2.3, thus enabling the immediate implementation in the Bachmann system of such control laws, with minimal modifications.



Figure 2.20: Data acquisition, control and model management system.

Model state machine

The operation of a real wind turbine is run through a states machine that manages the system differently depending on the situation, thus providing start-up phases, energy production, emergencies management, etc. The operating logic implemented in the scaled model is an augmented version of the one implemented in many real machines and is shown in Fig. (2.21).

At the beginning of each test, the machine changes from IDLING, with all actuators disabled, to the state of FUNCTIONALITY TESTING, where the correct functioning of the model is verified and followed by the zero setting of the sensors readings and pitch actuators encoders. If the model check is successful, the state is changed to PARKING and the wind turbine operates in standing still condition. Successively, the user can decide to operate the model in TRIMMING MODE, usually used for aerodynamic focused test since it is possible to regulate the machine at user-defined values of rotor speed and blade pitch, or in CONTROL-SUPERVISORY MODE. Here the supervisor switches between the machine states of START UP, POWER PRODUCTION, SHUT DOWN and EMERGENCY MANAGEMENT in an autonomous way, e.g. for managing start up and shut down procedures on the basis of the measured rotor speed and in case of real emergencies (like an excessive rotor speed or overheating of the torque actuator), or by the intervention of the user, e.g. for reproducing an extreme condition like an emergency shut down with grid loss. In POWER PRODUCTION state it is possible to switch among different collective pitch-torque control laws, whose gains can also be changed by the users during the wind tunnel testing, and to enable/disable multi-frequency individual pitch control capabilities, with the possibility, also in this case, to tune as desired the controller gains.

An operator control station implements software for the management of the experiments, for the data logging and visualization of all measurements through the support



Figure 2.21: Implemented model state machine.



Figure 2.22: Graphical interface for the model management.

of dedicated graphical interfaces (as that reported in Fig 2.22), which allowed to switch between the machine states, enable and disable the desired controller and tune the controller gains.

Model cabinet

In order to facilitate and speed up the assembly of the whole model in the wind tunnel it has been designed, manufactured and assembled the cabinet of Fig. (2.23) that contains:

- the real-time hardware and its power supply;
- the power supply of the conditioning electronic boards;
- the pitch actuators power supply;
- the torque actuator control electronic with its power supplies and the shunt regulator, which is connected to an heat exchanger cooled by a fan.

All the power, control and measure signals from the nacelle, the torque motor, the balance and the wind tunnel data arrive to the cabinet where specific connectors allow a quick and error-unaffected assembly of the whole model control system.



Figure 2.23: Model cabinet.

2.2.3 Model support tools

The wind turbine model is complemented by a number of support tools for its testing, calibration and maintenance. A back-to-back test bench, with the blades replaced with dummy ones but with the same real rotor inertia (Fig. 2.25(a)), was used for the hardware-in-the-loop test of the control-supervisory algorithms, with the aerodynamic torque T_a provided by the brushless motor Maxon EC-45 with gear-head GP-42C and encoder EHDL. The relation between the gear-head output shaft torque and the motor current-rotor speed was experimentally estimated, while the required aerodynamic

torque $T_{a,r}$ is real-time computed using, as input, the pitch and rotor speed read with the nacelle sensors, the rotor aerodynamic torque coefficients $C_T(\beta, \Omega)$ and an userdefined time history of the wind speed, as shown in Fig. (2.24).



Figure 2.24: Aerodynamic torque computation.

The test bench has been extremely useful, since it allowed to ensure the proper functioning of all the implemented control-supervisory algorithms and to set initial reasonable values to the controllers gains before entering in the wind tunnel, with great save of time.



Figure 2.25: Back-to-back test bench used for the hardware-in-the-loop test of the control-supervisory algorithms (a) and the set up used for determining the pitch actuator performance (b).

In addition, specific support tools were designed to guarantee an accurate calibration of the shaft and blade root strain gauges, with laser emitters used to align the dead loads with the desired direction. Moreover, specific tools were produced to mechanically zero setting the pitch encoders with the model operating in FUNCTIONALITY TESTING state and with an estimated accuracy of ± 0.2 [deg]. Finally, a small laser emitter, placed at the hub center, guarantees a very good alignment of the rotor axis with the wind tunnel wind flow.

2.3 Simulation tool

The comprehensive aero-elastic simulation environment (Fig. 2.26) Cp-Lambda (<u>Code</u> for <u>Performance</u>, <u>Loads</u> and <u>Aeroelasticity</u> by <u>Multi-Body</u> <u>Dynamic</u> <u>Analysis^[45,46]</u>), based on a finite-element multibody formulation (see Bauchau et al.^[47] and references therein) has supported all the phases of the wind turbine model design, including the determination of loads, of the aero-elastic response of the machine, and the testing of control laws.



Figure 2.26: Multi-body aero-servo-elastic simulation tool.

The multibody approach is based on the full finite-element method, i.e. no modalbased reduction is performed on the deformable components of the structure. Cartesian coordinates are used for the description of all entities in the model, and all degrees of freedom are referred to a single inertial frame; the formulation handles arbitrarily large three-dimensional rotations.

The turbine blades and tower are modeled using geometrically exact, compositeready beams. The formulation models beams of arbitrary geometry, including curved and twisted reference lines, and accounts for axial, shear, bending, and torsional stiffness. Joints are modeled through holonomic or nonholonomic constraints, as appropriate, that are enforced by means of Lagrange multipliers using the scaled augmented Lagrangian method^[48]. All joints can be equipped with internal springs, dampers, backlash, and friction models.

Lifting lines can be associated with beam elements and their geometric description is given in terms of three-dimensional twisted curves; for generality of the implementation, these aerodynamic reference curves are distinct from the structural reference ones they are associated with. The lifting lines are based on classical two-dimensional blade element theory, and account for the aerodynamic center offset, twist, sweep, and unsteady corrections. At a number of span-wise stations along each lifting line, the aerodynamic characteristics of the airfoil used at that location are given using look-up tables, which store for a given number of angles of attack and Reynolds numbers the values of the sectional lift, drag and moment coefficients. Through the appropriate definition of these coefficients one can also model those aerodynamic phenomena related to

3D flow bahaviour, such as the stall delay in the inner part of the blade due to Coriolis accelerations. Lifting lines are used here to model the aerodynamic characteristics of the blades, but also of the tower and of the nacelle. An inflow element can be associated with the blade lifting lines so as to model the rotor inflow effects; the code implements the Peters-He dynamic inflow wake model^[49] and a classical blade-element momentum (BEM) model based on the annular stream-tube theory with wake swirl. Tip and hub loss models are also considered.

Wind is modeled as the sum of a steady state mean wind and a perturbation wind, accounting for turbulence and/or gusts. The deterministic component of the wind field implements the transients specified by IEC 61400^[4], the exponential and logarithmic wind shear models, and the tower shadow effects, which include the potential flow model for a conical tower, the downwind empirical model based on Powles^[50], or an interpolation of these two models. The stochastic component of the wind field is precomputed before the beginning of the simulation for an assigned duration of time and for a user-specified two-dimensional grid of points. During the simulation, the current position of each air-station is mapped to this grid, and the current value of the wind is interpolated in space and time from the saved data.

The multibody formulation used in this effort leads to a set of non-linear partial differential algebraic equations. Spatial discretization of the flexible elements of the model using the finite-element method yields a system of differential algebraic equations in time, that are solved using an implicit integration procedure that is nonlinearly unconditionally stable^[51,52]. The implicit nature of the scheme allows for the use of large time steps and is more appropriate than explicit schemes for the typical dynamics of rotor systems. At each time step, the resulting nonlinear system of equations is solved using a quasi-Newton scheme. The time-step length is adjusted based on an error indicator.

The user can specify a number of sensors on the virtual prototype of the turbine, which provide output information for the analysis. The same sensor outputs can also be fed as inputs to the onboard controllers, which, in turn, operate the system actuators. Controllers, which can include supervision logics and feedback controllers, are implemented as user-defined routines that are linked with the rest of the code. The code supports static and transient analysis, and the computation of eigenfrequencies and eigenmodes about deformed equilibrium configurations. Finally, the model preparation and data interpretation phases are supported by various graphic procedures, including animations and time history plots, while automated procedures support a number of standard operations, such the computation of Campbell diagrams, the generation of C_P vs. tip-speed-ratio curves, the tracing of power curves, the determination of ultimate loads and fatigue equivalent loads using rain-flow analysis, etc.

The full scale mathematical model is based on data provided by the sponsor, while the mathematical model of the scaled wind turbine is based on its measured geometric, structural and aerodynamic properties. In particular, the model structural properties were computed using:

- well known analytical formulas for tower properties calculation;
- composite blade analysis code ANBA (Anisotropic Beam Analysis^[53]) for computing the cross sectional characteristics of the blade, verified with detailed three-

dimensional FEM models and corrected with the aid of identification techniques^[35] when necessary;

• detailed three-dimensional FEM computation for the main shaft and the nacelle properties calculation.

The masses of the model were updated according to data obtained by precise weighing, allowing the modeling of all non-structural masses, such as wiring and aerodynamic covers.

The model aerodynamic data are based on identification results described in §3.4.3, while the actuators dynamic and range, as well as the sensors and control, faithfully reproduce what is available on the real model.

CHAPTER 3

ROTOR AERODYNAMIC DESIGN

The objective of this research work is the comprehension of the wind turbine aeroservo-elasticity through wind tunnel testing. For this purpose, designing a wind tunnel model whose aerodynamic performance are representative of a multi-MW wind turbine is an essential requirement for getting reliable results. In the continuation of the chapter is then described the design of the wind tunnel model rotor, whose aerodynamic performance have to be realistic when compared to those of a multi-MW wind turbine in terms of power, thrust and optimal TSR. The method used for adjusting the measured performance by the wall blockage effect will then be explained, as well as the mathematical tool developed for the identification of the aerodynamic properties of the model airfoils. In the end, it will be also shown how this tool have been used to fine tuning the rotor design in order to optimize its performance, and will be highlighted as the same tool is essential for tuning simulation codes that model the rotor aerodynamics with the BEM.

3.1 Blade aerodynamic design

The aerodynamic design of the model blade has been driven by the requirement of having a good match between the model and the reference machine aerodynamic performance. In particular, the constraint on the optimal TSR implies that the model rotor should have the same solidity (approximately 0.04) of the reference rotor; it was than decided to maintain the same chord distribution of the V90, with marginal modifications in the blade root region to take into account that the distance between the model hub center and the blade root is slightly greater than the V90 scaled one.

The choice of the airfoils was, instead, driven by the significant mismatch between the average Reynolds number, which on the V90 is in the range $4 \div 5 \cdot 10^6$, while on the scaled model is only in the range $5 \div 6 \cdot 10^4$. It is important to remark that aerodynamics at Reynolds number lower than 10^5 has been object of several research studies in the past years^[54,55], with a positive impact on the design of small wind turbines, small unmanned aerial vehicles (UAVs), micro-air vehicles (MAVs), as well as on the understanding of bird/insect flying aerodynamics. In particular, a lot of effort has been done to develop low Re airfoils specifically suitable for wind energy applications^[56–59]. Indeed, an airfoil developed for high Re application, but operating at low Re, produces much lower lift and higher drag with respect to design values, with consequent drop of the aerodynamic efficiency and, for wind turbine application, decrease of the achievable rotor performance. The use of profiles developed for low Re applications can therefore lead to a great improvement of the wind turbine performance, as well as the introduction of "turbulators" could improve the airfoils efficiency^[56]. Indeed, these devices induce the transition from laminar to turbulent flow and are used for eliminating the laminar separation bubbles and delay the suction side separation at higher angles of attack.

All the above considerations emphasize the importance of the choice of profiles to be adopted in the model blade. Much literature search was then carried out to identify the profiles to be used, and the choice cast on the profiles AH79-100C^[60] and WM006^[61], whose shape is reported in Fig (3.1).



Figure 3.1: Model airfoils shape.

The former airfoil was used in the inboard section for $\eta = r/R \in [0.137, 0.423]$, while the latter in the outboard one for $\eta \in [0.654, 1]$. Not to alter the aerodynamic characteristics of the airfoils, interpolations of the cross sectional shapes were limited to a relatively small transition region between the inboard and outboard sections, i.e. for $\eta \in [0.423, 0.654]$, and at the root region to smoothly deform the inboard airfoil into the blade root cylinder. The choice of these two profiles, as well as their distribution along the blade span, has been driven by the need to get:

- a distribution of the maximum lift coefficient along the blade span similar to that of the real blade;
- high efficiency at Re around $5 \div 6 \cdot 10^4$ achievable, however, only by equipping the airfoils with turbulators;
- airfoils thickness in the order of 9-10% of the chord, with the thicker profile placed in the inner region of the blade. It appears immediate as these thicknesses are extremely low if compared with those of the reference blade airfoils, but this configuration has been found to be the best compromise between aerodynamic performance and structural stiffness, and has made it possible the manufacturing of aero-elastic blades, i.e. blades with a correctly scaled distribution of structural properties, as will be shown in chapter 4.

The airfoils polars at $Re = 6 \cdot 10^4$ are reported in Fig. (3.2). The plotted AH79-100C experimental data were measured by Althaus^[60] applying, at the 23% of the chord on the suction side, the turbulator described in table (3.1: Conf. 2). All the other plots show the numerical polars computed with X-foil^[62] even forcing or not the transition.



Figure 3.2: Model airfoils polar at Re 60000, with free and forced transition at 0.05c and 0.23c on suction side.

The numerical analysis showed that equipping the entire blade with turbulators placed at 0.23c guarantee the highest aerodynamic efficiency, although there are significant differences between the available experimental data and the numerical estimations.

Once fixed the blade airfoils and its chord distribution, the blade twist was derived by the reference one and slightly modified to yield an uniform span-wise distribution

Chapter 3. ROTOR AERODYNAMIC DESIGN

of the axial induction factor, so as to account for the change of airfoils between full scale and scaled blades, but without substantially affecting the direction of structural vibrations. Finally, the span-wise distribution of the relative position between the pitch axis and the airfoils aerodynamic center has been set equal to that of the reference blade.

The numerical/experimental polars with forced transition at 0.23c were used to compute, using the simulation tool described in §2.2.3, the model rotor performance shown in Fig. (3.3). The maximum power coefficient turns out to be about 0.45 at values of TSR around 7.5, confirming that the chosen profiles can ensure, at least nominally, excellent performance even at low Re.



Figure 3.3: Rotor performance computed with the simulation tool described in §2.3.

It is also very interesting to observe the trend of the Reynolds number along the blade span with the model wind turbine operating in region II^[63], with rotor speed changing from 60% to 100% of rated rotor speed Ω_r . Based on what reported in Fig. (3.4), it is expected that the model performance will substantially drop with decreasing wind speed, specially in terms of power coefficients. In fact, in the literature^[56,57,59] there are many examples that show as $Re \approx 5 \cdot 10^4$ is the limit below which do not fall to get acceptable aerodynamic efficiency.

Once fixed the blade shape, the following step should be the tunnel testing, with the aim of experimentally verify the goodness of the design in terms of rotor performance. However, given that:

- the ability of X-foil to predict accurately the airfoils polar at *Re* lower than $1 \cdot 10^5$, by imposing or not the transition, is poor, as reported in Somers and Maughmer^[64] and as highlighted in Fig. (3.2);
- only for the inner profile is known the turbulator shape, thus leaving to define the turbulators to be used for forcing the WM006 transition;

it was decided not to limit the testing to the experimental verification of the estimated



Figure 3.4: Reynolds number at maximum C_P , plotted as function of non-dimensional blade span η and rotor speed.

performance, but to characterized the rotor performance with different turbulator configurations, which were designed as explained in the next section.

3.1.1 Turbulator design

As stated before, very often it is not enough to just use low Re airfoils to achieve good aerodynamic performance, but it is necessary to add devices that prevent, or at least delay, the laminar separation of the boundary layer. Some of these devices can be:

- boundary layer suction;
- high speed air injection in the boundary layer;
- transition strips or vortex generators, i.e turbulator;
- heat generation, electromagnetic interference or noise etc.

For obvious reasons, many of the mentioned solutions can not be used in wind turbine model blades: the search for simple solutions and specially the small size of the model almost mandatory impose the use of transition strips or vortex generators. However, there are several critical aspects regarding the design of a turbulator that have to be face with, as its typology, position and thickness.

3.1.1.1 Turbulator type

Several type of turbulators can be used, and they can be classified as three-dimensional or two-dimensional devices. Those of the first type can be obtained by gluing spheres, cylinders, flaps or little wedges on the body surface, with the goal of triggering the instability of the coherent structures that generate the transition, or can be vortex generators whose induced speed has to be high enough to avoid laminar separation. In any case, these devices induce three-dimensional effects extremely difficult to predict through analytical formulas. Alternatively, it is possible to use 2D devices, such as transition strips made of adhesive tape or simple wires, that are useful in those applications where very small object are involved to. Furthermore, the latter have a better ability to

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force the transition thanks to the greater amount of surface exposed to the air flow^[65]. However, based on literature, it is still difficult to deduce general considerations which can guide the experimenter in the choice of the type of turbulators that are best suited to the application by the hand. We therefore decided to rely on the long experience of the Politecnico di Milano wind tunnel technicians, which recommended to use transition strips made of adhesive aluminum tape, with the possibility to obtain the desired height of the strip through the superimposition of several tape layers.

3.1.1.2 Turbulator position

The role of the turbulator is avoiding the laminar separation by forcing the boundary layer transition. It is self-apparent that place the turbulator too close to the leading edge cause a premature transition, with increment of the extension of the turbulent boundary layer region and, thus, of the frictional resistance. On the other side, a too far back position may not be sufficient to prevent the laminar separation. Given that the optimal placement of the turbulator depends on the airfoil *Re* and angle of attack, several analysis with X–foil have been performed with the aim of determine the relation between the overall friction coefficient and the position where transition is forced. The analysis results showed that:

- there is no need of transition strips on the pressure side;
- the increase of the AoA makes the separation point going even forward until it practically reaches the leading edge;
- the optimal position of the transition strip, i.e. the position that minimizes the overall friction, goes forward as the angle of attack increase.

Given that the airfoil operating Re and angle of attack depend, largely, on the model operating condition $(\Omega, \lambda \text{ and } \beta)$ rather than the airfoil chord and twist, it is self-apparent that the best solution would be to vary the strip position as function of the model operating condition, which is, of course, impracticable. Considering the fact that, based on suggestions found in the literature^[66], the strip should be located 0.05c upstream the desired transition position and, generally, between the 5% and 10% of the chord, placing the turbulator at 5% of the chord appears reasonable.

3.1.1.3 Turbulator size

Correctly choose the transition strip thickness is crucial. As a general rule, it can be stated that the turbulator height must not exceed the local displacement thickness of the boundary layer, but, at the same time, must be sufficiently high to create a disturbance capable of triggering the transition. Indeed, if the strip height exceeds the displacement thickness of the boundary layer there are unwanted changes of the "potential" flow field; the strip must therefore have a small local effect confined within the boundary layer and must be able to energize the same. About the strip width, on the contrary, it was found^[66] that its sizing does not substantially affect the transition mechanism.

Taking into account what has just been pointed out, the turbulator sizing was based on two methods found in the literature^[65,67]. In detail, looking at the transition phenomena for flat plate, is well known that transition begins when the local $Re_x = \frac{U_x x}{\nu}$ reaches the critical value $Re_{x_{crit}}$ of about $1.12 \cdot 10^5$. However, surface irregularities can decrease the value of $Re_{x_{crit}}$ and anticipate the transition; h_{crit} is than the critical height that a particle must have in order to be capable of moving the transition from its natural location (i.e. when $Re_{x_{crit}} \approx 1.12 \cdot 10^5$) to the point where the same particle is located. Once defined:

$$Re_h = \frac{v_h h}{\nu_h} \tag{3.1}$$

with h, v_h and ν_h respectively the particle height, the flow speed and kinematic viscosity evaluated at distance h from the airfoil surface, and using the approach reported in Braslow and Knox^[67], with assumption like $u_h = U_x$ and $\nu_h = \nu_\infty$, we get to:

$$h_{crit} = \frac{Re_{h,crit}x}{Re_x} \tag{3.2}$$

which relates the turbulators height h_{crit} required for triggering the transition to the parameter $Re_{h,crit}$. Cox^[65] suggested a value of $Re_{h,crit}$ equal to $600 \div 800$, if threedimensional turbulators are used, or equal to $100 \div 200$, in case of 2D-type devices. The empirical formulas of eq. (3.3) reported in Cox^[65] were used, together with eq. (3.2), to size the strip thickness vs. strip location, as shown in Fig. (3.5).

$$h_{crit} = \begin{cases} 52x Re_x^{-0.765}, & Re_x > 2 \cdot 10^5 \\ 63200x Re_x^{-1.43} + 1.4x Re_x^{-0.5}, & 3.4 \cdot 10^4 < Re_x < 2 \cdot 10^5 \end{cases}$$
(3.3)



Figure 3.5: Transition strip thickness sized with different methods and plotted vs. strip location, compared with the displacement thickness of the boundary layer δ^* computed with X–foil.

From the graphs we can see that the thickness of the strip applied on the AH79-100C profile in Althaus is close to the displacement thickness of the boundary layer at 0.23c. Moreover, the sizing obtained with Cox appears excessive, while the method proposed by Braslow and Knox with $Re_{h,crit} = 100$ estimates h = 0.001c, thus a value lower than δ^* at 5% of the chord.

3.1.1.4 Tested turbulator devices

The performance of the rotor have been experimentally measured using the turbulators shown in table (3.1); we decide to try, in addition to the configurations proposed by Al-

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thaus and sized with Braslow and Knox, also the clean configuration, i.e. without any turbulators.

Table 3.1: Turbulator configurations tried in the wind tunnel, with X_{tr}/c , w and h respectively the non-dimensional chord position and the dimensional width and height.

	Туре	X_{tr}/c	W	h
Conf. 1	NO turbulator			
Conf. 2	Transition strip	0.23	0.03c	max(0.003 <i>c</i> ,0.07[mm])
Conf. 3	Transition strip	0.05	$\approx 20h$	max(0.001 <i>c</i> ,0.07[mm])



Figure 3.6: Cnc cutting plotter (on left) and template (on right) used to precisely cut and place the transition strip.

The strips were produced overlapping different layers of tape (thickness equal to 0.07 [mm]), shaped using an accurate CNC cutting plotter (Fig. 3.6, on left), so as to discretely approximate the desired height, while the accurate positioning of the strips has been possible thanks to the use of a paper made template (Fig. 3.6, on right).

3.2 Blockage effect correction

The characterization of the aerodynamic performance requires that the the wind tunnel flow is uniform and non-turbulent throughout the test section. It makes, thus, perfect sense to carry out the tests in the aeronautical test section (see §2.1). Considering that the ratio between the model rotor area and the test section is equal to $A_r = \frac{\pi R^2}{C} \approx$ 0.2, it has been necessary, before proceeding with the experimental verification of the effectiveness of the designed turbulators, to identify a method suitable for correcting the measured performance from the effects due to wind tunnel blockage. In fact, it is well known^[68] that the thrust and power measured during wind tunnel experimentation are higher than those that would be measured with the model tested in free stream air, with larger differences with increasing ratio between model size and wind tunnel cross sectional area. In particular, for propellers as well as for wind turbines, the solid blockage is usually negligible, while the wake blockage is related to the downstream wake evolution and largely depends on the system operating condition, i.e. on how much energy is injected or extracted from the wind flow.

3.2.1 Blockage correction methods for wind tunnel testing

A bibliographic research that leads to the identification of several blockage correction methods relevant to wind tunnel experimentation on wind turbine model has been then carried out. All identified correction formulas are based on the equivalent free airspeed U' that, together with the wind tunnel speed U_{∞} , leads to correct the rotor performance as follows:

$$C_F = C_F^{\text{meas}} \left(\frac{U_\infty}{U'}\right)^2 \tag{3.4a}$$

$$C_P = C_P^{\text{meas}} \left(\frac{U_\infty}{U'}\right)^3 \tag{3.4b}$$

$$\lambda = \lambda^{\text{meas}} \frac{U_{\infty}}{U'} \tag{3.4c}$$

where $(\cdot)^{\text{meas}}$ indicates measured quantities not corrected by wake blockage effect.

3.2.1.1 Glauert method

The first identified correction method is the one developed by Glauert^[69] for propeller wind tunnel testing. The method, which is based on the calculation of a "[...] equivalent free airspeed U', corresponding to the tunnel datum velocity U_{∞} , at which the airscrew, rotating with the same angular velocity as in the tunnel, would produce the same thrust and torque.", is synthesized in the following equation:

$$\frac{U'}{U_{\infty}} = 1 - \frac{C_F^{\text{meas}} A_r}{4\sqrt{1 + C_F^{\text{meas}}}}$$
(3.5)

Although dated, the method is still today the most used in propellers wind tunnel testing and can be adapted to wind turbines, taking into account, according to the convention used by Glauert, of a negative thrust coefficient.

3.2.1.2 Dynamic pressure method

Another simple method consists in placing a Pitot tube in the same plane where the rotor disk is located but laterally shifted. The dynamic pressure q_{Pitot} measured by the Pitot can be therefore used to estimate U' as:

$$U' = \sqrt{2\frac{q_{Pitot}}{\rho}} \tag{3.6}$$

On the basis of previous experience in the helicopter rotor testing, this method was not taken into account because it abundantly overestimates the wall blockage.

3.2.1.3 Sørensen & Mikkelsen method

A more recent method is the one proposed by Mikkelsen and Sørensen^[70], that has been specifically conceived for modeling the wind tunnel blockage related to wind turbines



Figure 3.7: Disk actuator model in the wind tunnel

testing. This method is based on the axial momentum theory and employs a set of five equations to obtain a closed-form solution of the axial momentum theory equations.

In particular, with the notation of Fig. (3.7) and defining

$$w_r = \frac{S_1}{S}, \quad A_r = \frac{S}{C}, \quad \tilde{u} = \frac{u}{U_{\infty}}, \quad \tilde{u}_1 = \frac{u_1}{U_{\infty}}, \quad \tilde{u}_2 = \frac{u_2}{U_{\infty}},$$
 (3.7)

it is possible to combine the mass and momentum balance equations, evaluated inside and outside the slipstream, into the following equations system:

$$\tilde{u}_1 \mathbf{w}_r = \tilde{u} \tag{3.8a}$$

$$\tilde{u}_2(1 - A_r \mathbf{w}_r) = 1 - A_r \tilde{u} \tag{3.8b}$$

$$C_F^{\text{meas}} = \tilde{u}_1^2 - \tilde{u}_2^2 \tag{3.8c}$$

$$C_P^{\text{meas}} = \frac{\Delta P}{\frac{1}{2}\rho V_{\infty}^2} = 1 - \tilde{u}_2^2$$
 (3.8d)

$$A_r C_F^{\text{meas}} - C_P^{\text{meas}} = 2\tilde{u}_1 w_r A_r (\tilde{u}_1 - 1) - 2\tilde{u}_2 (1 - w_r A_r) (1 - \tilde{u}_2)$$
(3.8e)

These equations are rearranged in a closed-form solution for \tilde{u} in terms of w_r and A_r

$$\tilde{u} = \frac{w_{\rm r} \left(A_r w_{\rm r}^2 - 1\right)}{A_r w_{\rm r} \left(3 w_{\rm r} - 2\right) - 2 w_{\rm r} + 1},\tag{3.9}$$

with $A_r \leq 1$, $w_r \in \left[1, \frac{1}{A_r}\right]$. Once measured C_F^{meas} and C_P^{meas} , w_r can be easily get solving one of the following non-linear equations

$$C_F^{\text{meas}} - f(\mathbf{w}_r, A_r) = 0 \tag{3.10a}$$

$$C_P^{\text{meas}} - g(\mathbf{w}_{\mathbf{r}}, A_r) = 0, \qquad (3.10b)$$

with $C_P^{\text{meas}} = \tilde{u} (\tilde{u}_1^2 - \tilde{u}_2^2)$, and substituted in eq. (3.9) to compute the free airspeed U' with:

$$\frac{U'}{U_{\infty}} = \tilde{u} - \frac{1}{4} \frac{C_F^{\text{meas}}}{\tilde{u}}$$
(3.11)

It is clear as, based on the method formulation, it is possible to compute U' starting both from thrust or power measurements.

3.2.1.4 Bahaj et al. method

Another recent method is the one proposed by Bahaj et al.^[71], who developed, for wake blockage correction, an iterative procedure based on Mikkelsen and Sørensen model. In particular, the method consists in the following iterative steps:

- 1. fix a value for the u_2/u_1 ratio;
- 2. compute:

$$\frac{u}{u_1} = \frac{-1 + \sqrt{1 + A_r \left(\left(u_2/u_1 \right)^2 - 1 \right)}}{A_r \left(u_2/u_1 - 1 \right)}$$
(3.12)

3. iterate until the values of U_{∞}/u_1 , computed separately from equations (3.13), are equal

$$U_{\infty}/u_1 = u_2/u_1 - A_r u/u_1 \left(u_2/u_1 - 1 \right)$$
(3.13a)

$$C_F^{\text{meas}} = (u_1/U_\infty)^2 \left[(u_2/u_1) - 1 \right]$$
 (3.13b)

The correction formula is then:

$$\frac{U'}{U_{\infty}} = \frac{(u/U_{\infty})^2 + C_F^{\text{meas}}/4}{u/U_{\infty}}$$
(3.14)

3.2.2 Blockage correction applied on CFD based performance

To the author knowledge there are not experimental results proving which method, among those above mentioned, provides the exact correction of the wall blockage effect. It was therefore decided to performed CFD analysis, based on model blade geometry, using the code *ROSITA*^[72], which solves RANS equation on Chimera grid with Spallart-Allmaras turbulence model. The computational domain consists of a circular sector of 120 [deg] with periodic boundary conditions; in the axial direction the domain extends for 5D and 10D respectively upstream and downstream the model rotor disk, while in the radial direction the domain extend up to 5R or $\sqrt{\frac{C}{\pi}}$ in order to numerically reproduce, respectively, a free-air or wall boundary conditions. The purpose of these analysis is not the comparison between the numerical and experimental performance, considering also that the Spallart-Allmaras turbulence model is poorly suited for low *Re* applications, but to compare the numerical performance computed in free-air boundary condition with those calculated in wall boundary condition, with these last adjusted by wall blockage effect using the methods previously proposed and summarized in the following table (3.2).

 Table 3.2: Summary of blockage correction methods applied on CFD computed performance.

ID	Blockage Correction Method
m1	Mikkelsen and Sørensen, eq. (3.10a)
m2	Mikkelsen and Sørensen, eq. (3.10b)
m3	Bahaj et al.
m4	Glauert

The comparison of Fig. (3.8) shows that:

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- all methods overestimate the effect due to wake blockage when compared to CFD simulations;
- the Mikkelsen and Sørensen method solved with (3.10a) and the Bahaj et al. method provide the same correction and are the ones that best match the CFD computation;
- the Glauert method overestimates the wake blockage at high C_F^{meas} s, due to the singularity of the same method for $C_F^{\text{meas}} = 1$;
- the Mikkelsen and Sørensen method solved with eq. (3.10b) largely overestimate the wake blockage effect.



Figure 3.8: Performance data obtained from CFD analysis to evaluate blockage correction methods effectiveness.

It was therefore decided, based on the analysis just shown, to use the Bahaj et al. method to correct all the performance obtained by testing in the aeronautical test section.

3.3 Polar airfoils identification tool

Once designed the model blade and identified the method to be used to correct the aerodynamic performance from the effects of wall blockage, it was decided to develop another tool that would enable to identify the blade airfoils polars directly from the experimental tests. Indeed, not negligible deviations are expected between the estimated and measured rotor performance, and it is also expected that these deviations are mainly related to uncertainties on the real aerodynamic characteristics of the airfoils, in terms of lift and drag coefficients. In particular, real airfoils polar could be different from those reported in Fig. (3.2) due to:

- uncertainties on real blade shape: indeed, if we consider that the chord, in the outer region of the blade, is extremely small, it is easy to see that it is difficult to manufacture a composite-made blade with the correct desired shape, due to molds machining tolerances as well as to the unavoidable manual edging;
- X-foil poor capability to predict, with good accuracy, the polar data at low *Re*, specially with forced transition;

• effectiveness of the designed strips in inducing the transition, and their effect on the aerodynamic characteristics.

The procedure now illustrated has allowed to identify, with good accuracy, the real airfoils polars. In this way, it is possible to bypass all the uncertainties related to the shape of the blade or to the reliability of the polars estimated with the available computational tools.

3.3.1 Formulation: maximum Likelihood estimation

When it comes to identification of mathematical models, one enters in a world that embraces a very wide variety of disciplines; the identification procedure here implemented partly follows the approach described by Bottasso et al.^[35], where it is proposed an innovative procedure for the identification of the structural properties of HAWT blades.

In the present work, a mathematical model \mathcal{M} implemented in the simulation code described in §2.3 is considered. Given a parametric version of the model, $\mathcal{M}(\mathbf{p})$, where $\mathbf{p} \in \Re^n$ is a vector of parameters, and define a set of output quantities $\mathbf{y} = \mathbf{h}(\mathbf{p})$, where $\mathbf{y} \in \Re^m$, an experimental observation of the outputs \mathbf{y} can be expressed as

$$\mathbf{z} = \mathbf{y} + \mathbf{r} \tag{3.15}$$

where the error **r** is due to measurement and/or modeling errors, i.e. modeling approximations and unmodeled or unresolved physical processes in \mathcal{M} with respect to the reality. If we take a sample of observations $S_z = \{z_1; z_2; ...; z_N\}$, the Likelihood function is defined as

$$f(\mathbf{S}_z, \mathbf{p}) = \prod_{i=1}^{N} p(\mathbf{z}_i | \mathbf{p})$$
(3.16)

where $p(\mathbf{z}_i|\mathbf{p})$ is the probability of the observed variable \mathbf{z}_i given \mathbf{p} . Maximum Likelihood estimation identifies the \mathbf{p} that gives the maximum probability to the measurements by maximizing the function $f(S_z, \mathbf{p})$. Given a Gaussian distribution with zero mean for the residuals \mathbf{r}_i , i = 1, N and assuming that the residuals are statistically independent from one to another, the Likelihood function can be expressed as

$$f(\mathbf{S}_z, \mathbf{p}) = (2\pi^m |\mathbf{R}|)^{-\frac{N}{2}} \exp\left(-\frac{1}{2} \sum_{i=1}^N \mathbf{r}_i^T \mathbf{R}^{-1} \mathbf{r}_i\right)$$
(3.17)

where $E[\mathbf{r}_i \mathbf{r}_j^T] = \mathbf{R} \delta_{ij}$, with $E[\cdot]$ the expected value operator, \mathbf{R} the error covariance and δ_{ij} the Kronecker delta symbol. Given the exponential nature of probability densities, the problem is reformulated as the minimization of the negative logarithm of the Likelihood function:

$$\mathbf{p}^* = \arg\min_{\mathbf{p}} J \tag{3.18}$$

with J the following cost function:

$$J = -\ln f(\mathbf{S}_z, \mathbf{p}) = \frac{mN}{2}\ln 2\pi + \frac{N}{2}\ln |\mathbf{R}| + \frac{1}{2}\sum_{i=1}^{N}\mathbf{r}_i^T \mathbf{R}^{-1}\mathbf{r}_i$$
(3.19)

In Klein and Morelli^[73] (and references therein), several procedures are presented to minimize, through an iterative two steps process, the functional of eq. (3.19). Vice

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versa, if we consider, for simplicity, the matrix \mathbf{R} known and constant, (3.19) simplifies and leads to the method of weighted least squares.

The identification problem (3.18) is a non-linear programming problem, which can be solved effectively with a number of methods, most notably SQP^[74] and Interior Point^[75]. In this work, the first of the two methods was used, with gradients of cost function computed by finite differences.

A consideration must also be made on the reliability of the identified model. Indeed, if we borrow the definition of identification proposed by L.A. Zadeh^[73]: "System identification is the determination, on the basis of observation of input and output, of a system within a specified class of systems to which the system under test is equivalent", we get that the possible discrepancies between the final identified model observations and the physical system measured observations are due not only to measurement errors but, in large part, to the intrinsic limitations of the mathematical model to accurately reproduce the physical processes involved in the real system.

3.3.2 Definition of model parameters

The unknown parameters p of problem (3.18) are the polars of the model blade airfoils. In particular, once defined $\Pi_i^k(\alpha)$ the k^{th} aerodynamic coefficients of the i^{th} airfoil as function of the angle of attack α , we define;

$$\Pi_i^k(\alpha) = \Pi_i^{k^0}(\alpha) + \Delta_i^k(\alpha)$$
(3.20)

as the sum of the nominal aerodynamic coefficients $\Pi_i^{k^0}(\alpha)$ and of the additive corrective function $\Delta_i^k(\alpha)$ defined as:

$$\Delta_i^k(\alpha) = \sum_{j=1}^{N_i^k} \chi_j(\alpha) \pi_{i_j}^k$$
(3.21)

where $\chi_j(\alpha)$ are shape functions expressed in terms of the angle of attack, while $\pi_{i_j}^k$ are the associated N_i^k nodal values for the k^{th} aerodynamic coefficients of the i^{th} airfoil. There is ample freedom in the choice of the shape functions; for this case it was used the piece-wise cubic spline interpolation method provided by Matlab. The optimization parameters are therefore defined as the nodal values of the shape function, i.e:

$$\mathbf{p} = \left(\dots, \pi_{i_j}^k, \dots\right), i = 1: N_{air}, k = 1: N_{prop}^i, j = 1: N_i^k$$
(3.22)

The developed procedure is generic and allows to arbitrarily define the number of airfoils N_{air} for each of which is required the identification of N_{prop}^i aerodynamic properties, as well as the number and the position of the N_i^k nodes. For this case, it was decided to identify the lift and drag coefficients of the two airfoils AH79-100C and WM006, considering constant their polars all long their span-wise extension (see Fig. 3.9).

This approach does not allow to identify localized variations of the aerodynamic properties, which are then averaged over the entire airfoil extension, but guarantees the observability of the problem, thus benefiting the well-posedness of the problem itself, as well as reducing the computational costs. A new and interesting approach to the



Figure 3.9: Unknown parameters p of problem (3.18).

estimation of the problem is the one proposed by Cacciola^[76] and based on singular value decomposition. This approach is very common especially in robotics^[77–80] and allows to capture local variations without increasing the problem dimensionality and ensuring optimum observability. However, this last approach has not been adopted in this thesis, but will undoubtedly be used in the future.

3.4 Rotor performance characterization and optimization

The measures of greatest interest for the characterization of the rotor aerodynamics are the shaft torque and the rotor thrust. As mentioned in §2.2.2.2, these metrics are obtained reading, respectively, the shaft strain gauges and the tower root balance outputs. However, in order to get the rotor thrust it is important to remove, from the balance measurements, the amount of drag due to the tower-nacelle group. It was therefore measured, for different values of the wind speed, the coefficient SC_D (see Fig. 3.10) of the entire model equipped without blades, resulting in the following equations:

$$C_P^{\text{meas}} = \frac{M_{XR}\Omega}{q\pi R^2 U_{\infty}} \tag{3.23a}$$

$$C_F^{\text{meas}} = \frac{F_{axial}}{q\pi R^2} - \frac{SC_D(U_\infty)}{\pi R^2}$$
(3.23b)

used to compute the rotor aerodynamic power and thrust (force) coefficients, being $q = \frac{1}{2}\rho U_{\infty}^2$ the dynamic pressure and F_{axial} the scalar product between the balance forces vector F and the rotor axis unit vector.

3.4.1 Rotor performance characterization

As stated before, the rotor aerodynamic performance were measured by testing the model, equipped with rigid blades, in the low-turbulence Aeronautical Test Section, with wind speeds from 4 to 7 [m/s], rotor speeds from 330 to 400 [rpm], TSRs from 5



Figure 3.10: Coefficient SC_D as function of the wind speed.

to 11 and blade pitch from -5 to 5 [deg]. In particular, given λ and β of each measuring point, the test wind speed was defined having the objective of keeping the airfoils Re close to the nominal value and taking into account the constraints related to the maximum rotor speed and power that can be delivered by the torque actuator.



Figure 3.11: Power coefficients as function of λ and β , measured using the turbulators of table (3.1).

In Figs. (3.11) and (3.12) are reported the experimental rotor performance coefficients adjusted by the wall blockage effect. Looking at the experimental results what emerges is that:

- as expected, the few performance measurements carried out for the configuration without transition strips (Conf. 1) confirm the need to equip the blades of the model with turbulators, given the low maximum power coefficient achievable with "clean" blade surface;
- as already pointed out in §3.1, the transition strips placed at 23% of the blade


Figure 3.12: Thrust coefficients as function of λ and β , measured using the turbulators of table (3.1).

chord (Conf. 2) allow to produce the highest maximum power coefficient, which indicates that, for values of λ around 8-9, the airfoils are operating at high efficiency conditions. The trend of these curves is, however, very irregular for lower λ and pitch angles β : this suggests a probable flow separation in this operating conditions, resulting in performance degradation;

• the turbulators placed at 5% of the blade chord (Conf. 3) provide a maximum power coefficient slightly lower (see table 3.3), but the $C_P - \lambda - \beta$ curves look regular throughout the range of tested λ and β ; given that a model power curve comparable to that of a multi-MW HAWT is an essential requirement, it is believed that the best solution is to locate the strips at 0.05c, in order to avoid the sharp drop of C_P for $\lambda \leq 8$, so as to ensure proper operation of the model for a wide range of TSR and pitch angle.

Table 3.3: Region II optimal C_P and λ measured using designed turbulators.

	C_P^{II}	λ^{II}
Conf. 1	0.316	7.75
Conf. 2 Conf. 3	0.354 0.342	8.64 7.74

Fig. (3.13) shows the comparison between the numerical and experimental performance obtained using Conf. 2 turbulator. The measured power coefficients, as well as the thrust coefficients, are generally lower than the design values, except for high values of the pitch angle, where it is expected that the model airfoils are operating at low angles of attack.

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One wonders, at this point, what these differences are due to. It has been therefore conducted a preliminary sensitivity analysis using the simulation code described in §2.3 which let to the following qualitative considerations:

- the increase of both airfoils drag coefficients has little impact on the values of C_F , specially at high TSRs and pitch angles; this means that the differences observed on the thrust coefficients are mainly due to incorrect modeling of the airfoils C_L s;
- the increase of the WM006 drag has greater effect on the rotor performance than increasing the AH79-100C drag, since the aerodynamic forces generated by the outer part of the blades are higher than those produced by the inner part;
- changing the lift coefficients influences both power and thrust coefficients;
- the match between the numerical and experimental performance significantly improves by substantially increasing the C_D s at high AoAs, which suggests the possibility that the model airfoils suffer a premature stall than expected.

After this preliminary analysis, it has been possible to obtain quantitative information using the identification tool described in §3.3.



Figure 3.13: Comparison between the numerical and experimental rotor performance related to Conf. 2 turbulators.

3.4.2 Identification of airfoils polar

Considering that, as previously stated, the drag coefficients has little impact on the values of C_F , the optimization problem (3.18) can be solved adopting a classical divide & conquer approach and performing two consecutive optimizations, with considerable benefits in terms of reduction in computation time and convergence of the solution to a global minimum. The first of the two optimizations minimizes the error vector related to solely observations of thrust coefficients, with nodal values $\pi_{i_i}^{C_L}$ related to lift

coefficients as unknown parameters p, and can be written as:

$$\mathbf{p}_{(1)}^{*} = \arg\min_{\mathbf{p}_{(1)}} J_{(1)}$$
(3.24)
with:
$$\begin{cases} \mathbf{p}_{(1)} = \left\{ \dots, \pi_{i_{j}}^{C_{L}}, \dots \right\} \\ \mathbf{p}_{(1)}^{0} = \mathbf{0} \\ J_{(1)} = \frac{1}{2} \sum_{i=1}^{N} r_{i} R^{-1} r_{i} \\ r_{i} = \frac{C_{F_{i}}^{\mathcal{M}}}{C_{F_{i}}} - 1 \end{cases}$$

where $(\cdot)^{\mathcal{M}}$ indicates model observations and the error vector has been scaled by dividing each observation by its corresponding measure.

The second optimization (3.25) minimizes the error vector related to observations of both thrust and power coefficients with initial values for $\pi_{i_j}^{C_L}$ the solution of problem (3.24).

$$\mathbf{p}_{(2)}^{*} = \arg\min_{\mathbf{p}_{(2)}} J_{(2)}$$
(3.25)
with:
$$\begin{cases}
\mathbf{p}_{(2)} = \left\{ \dots, \pi_{i_{j}}^{C_{L}}, \dots, \dots, \pi_{i_{j}}^{C_{D}}, \dots \right\} \\
\mathbf{p}_{(2)}^{0} = \left\{ \mathbf{p}_{(1)}^{*}, \mathbf{0} \right\} \\
J_{(2)} = \frac{1}{2} \sum_{i=1}^{N} \mathbf{r}_{i}^{T} \mathbf{R}^{-1} \mathbf{r}_{i} \\
\mathbf{r}_{i} = \left\{ \frac{C_{F_{i}}^{\mathcal{M}}}{C_{F_{i}}}, \frac{C_{P_{i}}^{\mathcal{M}}}{C_{P_{i}}} \right\}^{T} - \mathbf{1}
\end{cases}$$

The nodes location has been then defined according to the numerical estimates, based on model nominal properties, of the angles of attack at which operate the two airfoils along their whole extension, as shown in figure 3.14

3.4.2.1 Conf. 2 turbulator

Looking at Fig. (3.14) and based on TSRs and pitch angles of the measurement points, we get that the inner airfoil AoAs vary between approximately 1 and 18 [deg], while the outer airfoil AoAs vary between about -1 and 10 [deg]. The nodes of the shape functions have been therefore placed within these ranges, except for the airfoil WM006. Indeed, a node is located at $\alpha = 13$ [deg], forasmuch as the aerodynamic properties of the transition airfoil ($\eta \in [0.423, 0.654]$), that operates at AoAs between 0 and 14 [deg], depend also upon those of the outer airfoil. In the end, the shape functions were imposed null for $\alpha \notin [-8, 35]$, so as to smoothly joining up the identified properties with the nominal ones outside the range of identifiability.

Fig. (3.15) shows the measured performance (black lines with triangular symbols), the performance computed with the model prior to identification (blue lines with square symbols) and the ones after identification (red line), plotted vs. TSR and for several pitch angles. The agreement between the experimental and model performance after identification is extremely good, for both power and thrust coefficients, which highlights the effectiveness of the developed identification tool.



Figure 3.14: Numerical AH79-100C and WM006 AoAs estimated at the ends of their respective regions of extension along the blade span.

The identified (solid lines) airfoils properties, as well as the additive corrective functions, whose nodes are marked with small squares, are shown in Fig. (3.16). It is immediate to note that the stall, for both profiles, takes place well in advance with respect to what expected, so as the presence of a laminar separation bubble can be noted if one looks at the aerodynamic coefficients of the AH79-100C. This latter consideration, together with the evidence that the C_D is much higher than expected for all AoAs, leads to say that the transition strip placed at 23% of the AH79-100C chord do not avoid the laminar separation, even at low angles of attack. The aerodynamic properties of airfoil WM006 do not show, instead, a behavior which might be attributed to the presence of a laminar separation bubble, but there is an extremely premature stall resulting in dramatic increase of the drag coefficients.

3.4.2.2 Conf. 3 turbulator

The performance with the strips placed at the 5% of the chord were measured for TSRs between 5 and 10.5 and pitch angles between -1 and 3 [deg], which means that the inner airfoil AoAs vary between about 0 and 20 [deg], while the outer airfoil AoAs vary between about -1 and 10 [deg]. The nodes of the shape functions have been therefore placed within these ranges, except for the airfoil WM006, since a node has been located at $\alpha = 12$ [deg] for the same reasons previously explained. Also in this case it was imposed that the shape functions were null for $\alpha \notin [-8, 35]$.

Fig. (3.17) shows the measured performance and the performance computed with the model before and after identification. Also in this case, the agreement between the experimental and model performance after identification is extremely good, for both power and thrust coefficients.

The identified airfoils properties with transition strips placed at 0.05c are shown in Fig. (3.18). The aerodynamic coefficients of the airfoil AH79-100C once again



Figure 3.15: *Rotor performance: blade equipped with Conf. 2 turbulator.* $\Box = Nominal model$

 $\times = Identified model$

 $\triangleright = Experimental data$



Figure 3.16: Conf. 2 turbulator: initial (dashed lines) and identified (solid lines) AH79-100C (on left) and WM006 (on right) aerodynamic properties together with respective distributions of the additive corrective functions (red line, \Box = nodes location).

highlight the possible presence of a laminar separation bubble at $\alpha \approx 3$ [deg], which indicates that the strip is probably not correctly sized for energizing the boundary layer at moderate angles of attack; the same strip works, instead, very well at high AoAs, where it is important, above all, the turbulator position instead of its thickness. The aerodynamic properties of airfoil WM006 do not show, instead, a considerable improvement due to the different position and thickness of the turbulator, if not a slight increase of the maximum lift coefficient.

3.4.3 Improvement to rotor design

The identification procedure has allowed to better understand the behavior of the model blade airfoils equipped with different turbulator devices. The presented results are not definitive: there is, in fact, a certain dependence on the nodes number and position and it has not been computed the Cramér-Rao standard deviations of the estimated properties, so as to have a perception of the identification reliability. However, the presented results certainly give a qualitative indication of the phenomenon at the hand.

In particular, given that the properties of the WM006 are substantially independent of the type of turbulator and extremely different from what is expected, the question arises whether the shape of the blade, specially in the outer region, is exactly equal to the nominal one or not. However, since the blade is composite made, its shape matches to the mold one. In this regard, we proceeded by analyzing the CAD (Computer Aided Design) model of the mold used to manufacture the blades. The analysis showed a shape incongruity of the mold leading edge line that, instead of being rounded, presents a net edge (see Fig. 3.19), more pronounced in the outer region of the blade, where the





 $\times = Identified model$

 $\triangleright = Experimental data$



Figure 3.18: Conf. 3 turbulator: initial (dashed lines) and identified (solid lines) AH79-100C (on left) and WM006 (on right) aerodynamic properties together with respective distributions of the additive corrective functions (red line, \Box = nodes location).

WM006 is located, and accentuated by the fact that the mold parting plane lies on the leading edge line in order to get a positive draft.

Once identified what is believed to be the cause of the performance below expectations, it was decided to change the mold, reproducing, by CNC machining, part of the leading edge on a new piece of metal. In this way, it has been possible to replace the leading edge area of the existing mold – which then results being composed by three parts instead of two, as shown in Fig. (3.19) – with the further advantage that none of the mold parting plane lies on the leading edge line.

Once changed the mold, the attention has been focused on the transition strip design. As explained above, the strip at 5 % of the blade was found to be under sized for the airfoil AH79-100C. It was therefore decided to increase its thickness, thus defining a new turbulator configuration, whose height h is equal to $\max(0.002c, 0.07[mm])$ in the inboard section, and equal to $\max(0.001c, 0.07[mm])$ in the outer blade area.

The rotor aerodynamic performance were again measured by testing the model equipped with new rigid blades in the low-turbulence Aeronautical Test Section, with wind speeds from 4 to 8 [m/s], rotor speeds from 310 to 400 [rpm], TSRs from 4 to 11 and blade pitch settings from -5 to 15 [deg].

If one looks at the performance shown in Fig. (3.20), in particular the maximum power coefficient, he realizes that the new turbulator and mold have not yielded the desired effects, being the optimal C_P^{II} equal to about 0.34. However, the $C_P - \lambda - \beta$ curves are extremely regular throughout a wide range of λ and β ; this condition is by itself sufficient to ensure that the model power curve will be representative of a multi-MW wind turbine.

A small test campaign was also carried out to quantify the effect of airfoils Re on



Figure 3.19: Blade mold with highlighted the shape incongruity at leading edge and the amendment introduced in the mold design.



Figure 3.20: *Rotor performance as function of* λ *and* β *, measured using new blades and turbulators.*

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the rotor performance. For this purpose, the power coefficients were measured keeping constant, and equal to the optimal values, the blade pitch and the TSR, but changing the rotor speed from 75% to 100% of its rated value Ω_r . In this way, in fact, it is possible to capture only the effect of the airfoils Re on the rotor performance by excluding all other variables, since the structural flexibility can be neglected, as well as the model kinematics is kept constant.



Figure 3.21: Region II optimal C_P vs. rotor speed.

The test results, reported in Fig.(3.21), clearly highlight that the maximum power coefficient decreases substantially with decreasing rotor speed due to the lower airfoils Re (as shown in Fig. 3.4). The following relationship between the rotor speed and the optimal power coefficient C_P^{II} has been then obtained by best fitting the measured data:

$$C_P^{II}\big|_{\Omega} = C_P^{II}\big|_{\Omega=\Omega_r} \left[-2.08 \left(\frac{\Omega}{\Omega_r} - 1 \right)^2 + 1 \right]$$
(3.26)

The following step was the updating of the airfoils polar based on latest observations. In this regard, a subset of all available observations, characterized by TSRs between 5 and 9.5 and pitch angles between -1 and 3 [deg], has been used. With this subset, the identifiable inner airfoil AoAs vary between about 0 and 20 [deg], while the identifiable outer airfoil AoAs vary between about 1 and 10 [deg]. The nodes of the shape functions have been therefore placed within these ranges, while it has been imposed that the shape functions were null for $\alpha \notin [-10, 40]$ and $\alpha \notin [-20, 25]$, respectively for the AH79-100C and WM006 airfoils.

The identified airfoils properties are shown in Fig. (3.22). Looking at the aerodynamic coefficients of AH79-100C, no evidence of a laminar separation bubble is noted, which indicates that the new transition strips are correctly located and sized. The identified drag coefficients of the airfoil WM006 are generally lower than those identified using the old blades, index that the angularities of the leading edge has been removed with the new mold. However, it is self-evident that the identified $C_L(\alpha)$ curve is significantly different then the one computed with X-foil; indeed, the value of $C_L(0)$ is remarkably lower as to suggest that the camber of the manufactured blade airfoil is substantially different from the nominal one. Very similar results have been obtained using a new approach based on singular value decomposition described in Cacciola^[76], that, compared to the used method, allows to:

• capture local variations of the aerodynamic properties;





Figure 3.22: New blade: initial (dashed lines) and identified (solid lines) AH79-100C (on left) and WM006 (on right) aerodynamic properties together with respective distributions of the additive corrective functions (red line, \Box = nodes location)

- free the solution of problem (3.18) by the choice of the nodes number and location;
- identify only those parameters for which it is possible to ensure a reliable identification.

This implies that the identified polar are effectively the right ones, as confirmed by the comparison shown in Figs. (3.23) and (3.24) between the measured performance and the performance computed with the model before and after identification.

The agreement is extremely good for β s and TSRs included in the subset used for the identification, while not negligible discrepancies are observed for lower β s, both in terms of thrust and power coefficients, as well as for high tip speed ratios, as already observed by Krogstad and Lund^[11]. It is very probable that the state-of-the-art BEM engineering model implemented in the simulation code is not adequate to capture, with good accuracy, the physics that governs the rotor aerodynamics at low Re across the overall range of the rotor operating conditions. Once noted that the comparison shown in Figs. (3.15(a)) and (3.15(b)) reports a good agreement even at low pitch angles, it is reasonable to assume that the BEM is not able to accurately model the low Re rotor aerodynamics when the wind turbine model is providing, at the same time, high thrust and power at low β . However, although the phenomenon should be studied in depth in the future to understand the reason for such discrepancy, it is possible to conclude that the match between the measured and the numerically computed aerodynamic performance is extremely good for values of TSR and β close to those defined by the control regulation trajectory. This latter is shown in Fig. (3.25) and is computed, on the basis of the identified polars, using the approach reported in Bottasso et al.^[43] and briefly summarized in §5.3.

This latter feature is of paramount importance, given that the model will be mainly





 $\Box = Nominal model$

 $\times = Identified model$

 $\triangleright = Experimental data$





 $\times = Identified model$

 $\triangleright = Experimental data$



Figure 3.25: Control regulation trajectory plotted over measured $C_P - \lambda - \beta$ curves.

used for control applications. It is therefore possible to state that the aerodynamics of the rotor, even with all the limitations due to low Re, is representative of that of a multi-MW wind turbine.

Given the above, it was decided to do not modify further the mold or to investigate possible improvements equipping the blades with other transition strips, being aware, on the one hand, that the reason of the remarkable difference between the identified and X-foil computed $C_L(\alpha)$ curves of the WM006 should be analyzed and that, on the other hand, it is very difficult to manufacturing the model blades, whose shape is complex and whose outboard airfoil thickness is in the range of 1-4 [mm], using composite materials and, at the same time, to ensure that the final shape exactly match the design one.

It should be pointed out, finally, how the identified properties can be used as parameter data in those simulation codes that model the aerodynamics of the rotor with the BEM, i.e.:

- the majority of the aero-servo-elastic simulation codes used in the industry and university for the calculation of performance and loads;
- those 3D Navier Stokes solvers that, once combined with actuator line/disk techniques, are particularly suitable for the numerical study of the wind turbine wake evolution under various flow conditions, i.e. including also the interaction between wakes and downstream rotor aerodynamics.



AERO-ELASTIC BLADE DESIGN, MANUFACTURING AND TESTING

Load reduction techniques for wind turbines can be broadly categorized into two families: active and passive. Within the active category, Individual blade Pitch Control (IPC) has the potential of reducing fatigue and possibly ultimate loads on crucial structural components of modern wind turbines, such as blades, drive-train, bearings and tower. The current literature describes a variety of architectures and of approaches used for the synthesis of IPC laws^[44,81–87], and some have also been successfully tested in the field^[88]. However, the benefits that IPC brings, especially to fatigue loads, come at the cost of increased duty cycle (ADC). The increase in pitch activity can be substantial when compared to a classical collective-pitch control strategy, to the point that the use of IPC needs to be accounted for at the time of sizing of the pitch system.

Passive load reduction techniques are based on the idea of designing a structure

that, when loaded, deforms so as to induce a load reduction. The classical solution to achieve this structural behavior is to design blades with some degree of bend-twist coupling (BTC). This implies that, when the blade bends because of increased loads, the ensuing change of twist affects the aerodynamic loading through a change of the angle of attack. Passive load mitigation by BTC can be obtained by exploiting the anisotropic mechanical properties of composite materials, like using off-axis fiber angles, and the literature^[89,90] clearly shows the potential benefits of BTC. This form of load alleviation is very attractive because of its passive nature: there are no actuators which may fail, no moving parts which may wear out, and no need for sensors, all characteristics that are very interesting for wind turbines where simplicity, low maintenance and high availability are key to reducing the cost of energy.

Recently, it has been also shown in Bottasso et al.^[91] that, by properly designing a partially coupled BTC blade, one not only can achieve significant load alleviation on the wind turbine sub-structures, but also the pitch actuator duty cycle (ADC) is much reduced. In fact, since the blade self-reacts to wind fluctuations, the control system has to pitch the blades less in response to wind disturbances. This result is of potential interest, because it opens the way to a synergistic combination of passive and active load control technologies. In fact, individual pitch control (IPC) has been shown to reduce loads at the expense of an increase in pitch activity, and this often requires suitably designed pitch systems which can withstand the wear induced by IPC, with effect on cost, complexity and maintenance. However, since BTC and IPC can both mitigate loads, but BTC reduces ADC while IPC increases it, there is a potential synergistic effect of the two technologies. Therefore, it appears possible to have a combined load reduction effect, larger than one would get by adopting one single technology, while limiting the effects on ADC.

Considering that this synergy has never been tested on the field, the wind tunnel aero-servo-elastic models described in this thesis are thus perfectly suitable for this purpose. The models are, in fact, actively controlled and allows the implementation of IPC controls for reducing not only the 1-rev loads harmonic but also higher order harmonics (see 5.4).

This chapter will then discuss about the design and manufacturing of the model aero-elastic blades with and without bending-twist coupling. First of all the technology solution identified for the realization of the blades will be described. Subsequently, the tool developed in support of the blades design will be explained, as well as the heuristic approach that led to the fine tuning of the production technology. Unfortunately, due to time constraints it has not been possible to complete the outlined path by producing two complete sets of aero-elastic blades, with and without coupling, and completing the tunnel testing. Therefore, the designed of the aero-elastic blades will only be presented, togheter with the description of the first manufactured blade with bending-twist coupling.

4.1 Design requirements and state of the art on aero-elastic blade manufacturing

On the basis of what is stated in §2.2.1.3, it is deduced as the aero-elastic blades design should meet the following requirements:

4.1. Design requirements and state of the art on aero-elastic blade manufacturing

- to have an external shape consistent with that designed in §3.1 with good surface finishing, in order to guarantee a realistic energy conversion process;
- to have a mass-stiffness distribution consistent to the ones specified by the scaling laws, which implies also requirements on the placement of the blade natural frequencies.
- to house the pitch motor with related cables, as well as the sensors used for measuring blade loads along the blade span.

Moreover, it is of fundamental importance to set up a manufacturing process which has to be controllable and reproducible, in order to guarantee the production of identical blades that must, all, fulfill the requirements listed above.

4.1.1 Overview on aero-elastic blade manufacturing

Most of the wind turbines blades currently installed on large dimension HAWT present an aeronautic type structure: the key element is a carrying-box structure made up of two shear webs connected to the spar-caps located on both pressure and suction sides of the blade, as shown in Fig. (4.1). This structural component, made of composite material like epoxy resin reinforced with glass fiber and/or carbon fiber, is designed with the primary function of withstanding the aerodynamic and inertial loads acting on the blade. Finally, the external shell, usually made of epoxy resin reinforced with glass fiber, provides the necessary torsional stiffness and gives the desired aerodynamic shape to the blade.



Figure 4.1: Typical cross-section of a modern multi-MW wind turbine.

Having the goal of producing a scaled version of such a blade, the most obvious solution would be to directly scale the structural elements just described using the ratios of table (2.1). This would involve scaling the blade geometry, as well as the structural thicknesses, with a ratio equal to $n_l = \frac{1}{45}$ and using materials whose Young's modulus are scaled with a ratio equal to $\frac{n_l^2}{n_t^2} \approx \frac{1}{3.88}$. However, this solution is unfeasible. In fact, the external shape of the V2 blade is significantly different from that of the V90, having equipped the model blade with much thinner airfoils. In addition, the scaling of the original structural elements would require the manufacturing of components whose thicknesses would be in the order of a few hundredths of millimeters, resulting in huge

technological complications, as well as high fragility and difficult handleability of the manufactured components.

It can be then figure out how the design and production of the aeroelastic blades is a complex challenge. Furthermore, few documentation about wind tunnel testing of aero-elastic wind turbines blades has been found in the literature^[21,92], but, based on author knowledge, nothing has never been done about the manufacturing of aeroelastic blades of wind turbine model that satisfy all the requirements previously listed; this means that has not been developed, with success, any technology that allows to produce wind turbine model blades even with bend-twist coupling. Therefore, the first step has been to understand how the problem is being addressed in other fields, like the aeronautics and helicopters ones.

If one looks at applications in the field of aeronautics, we get that it is normal practice to perform wind tunnel test on aero-elastic model of new airplane before producing the first prototype. These tests are extremely useful to understand the goodness of the design, as well as to tune the numerical models used by engineers during the subsequent development phases. If we look at a typical wing of an aero-elastic aeronautical model (see Fig. 4.2) we can see that, usually, it is made of two separated parts: an internal spar of appropriate shape and size, which gives to the wing the desired stiffness, and a set of large ribs, usually made of balsa wood or other lightweight materials, which give the aerodynamic shape and, contemporaneously, permit to get the desired inertial proprieties. With this solution, the original wing shape is usually not correctly reproduced, since the model wing does not have a uniform and continuous shape, but rather a segmented shape. However, this fact does not affect substantially the test results, given that the test objective is to quantify the rate of change of the aerodynamic loads due to the structural elasticity; thus, it is important to faithfully reproduce the aerodynamic derivatives rather than the aerodynamic coefficients. However, the issue is different in our case, since it is required that the aerodynamic performance are comparable with those of a real wind turbine and, therefore, it is not possible to use this solution.



(a) Fixed-wing model global overview

(b) Wing longeron

Figure 4.2: Overview of a typical fixed-wing aero-elastic model

Looking at the rotary wing sector, we get that carring out experimental activities in the wind tunnel using model rotor equipped with aero-elastic blades is a normal practice. However it is rare to find in the literature information about the experimental set-up and results, since, usually, the tests are conducted by the same companies that produce helicopters and the results are often covered by secret. Among the results of

4.1. Design requirements and state of the art on aero-elastic blade manufacturing

public domains, there is the research study report in Bernhard and Chopra^[2]; the four blades rotor, as well as the cross-section outline of the aeroelastic blade, are shown in Fig. (4.3): the blade is constituted by a foam core made of Rohacell®^[93] covered by a fiberglass reinforced skin; within the airfoil there are two small carbon-made spars and a balancing weight located at the leading edge, as well as an actuation device used to actively change the blade twist distribution.



Figure 4.3: Constructive solution for aeroelastic blades of wind tunnel helicopter rotor model^[2]

It has been found that this manufacturing technology is fairly common. Jinsong et al.^[3] tested five different set of Mach scaled tailored rotor blades made of a composite D-spar, an aft foam core, a composite weave skin and a composite or alloy made blade root insert. Also in this case, the composite spar is the primary structural element which supports the blade loads and provide the desired elastic couplings. The leading edge weights are used to ensure the correct chord-wise mass balance for aeroelastic stability, while the aft foam core and the blade skin are used to maintain the blade airfoil profile.



Figure 4.4: Model aero-elastic tailored blade^[3]

The use of composite materials seems to be the optimal solution in the field of wind tunnel testing of aero-elastic helicopter rotors, at least based on the information found in the open literature. The realization of aeroelastic blades for wind turbines model

presents, however, major complications with respect to the realization of helicopter model blades. In fact, the shape is much more complex: it passes from the cylinder root to airfoils whose thickness is about 0.10*c*, with chord variable between about 80 and 10 [mm], thickness variable between about 40 and 1 [mm], as well as twist variable between approximately 2 and 27 [deg]. Furthermore, the required mass is equal to slightly less than 70 [g], while the first flap-wise frequency has to be greater than the 3-per-rev. There is, however, the advantage of having a less stressed structure (stress are approximately scaled as $\frac{n_t^2}{n_t^2} \approx \frac{1}{3.88}$) and therefore it is not fundamental to take into account the limits on allowable stresses and any life-fatigue issues in the design phase.

The design requirements, the indications found in the literature as well as the above consideration were the factors that led to the development of the blade production technology described in the next section.

4.2 Technological solution for aero-elastic blade manufacturing

The development of the manufacturing technological process results in:

- choice of sensors to be used to get the strains along the blade span and reconstruct the loads acting on the blade;
- definition of the structural layout of the model blade and choice of the materials;
- definition of the manufacturing process, in terms of:
 - realization of the various sub-components and related molds;
 - assembly of all the blade sub-components.

4.2.1 Sensors for blade monitoring and loads measure

As stated in §2.2.2.2, the information most commonly used to test individual pitch control algorithms are the blades root loads that, usually, are reconstructed from the reading of strain gauge bridges. Strain sensors are also placed in different locations of the blade in order to permit its health monitoring throughout the operational lifetime of the wind turbine^[94]. On the model there is not this need, given that any issue about fatigue life has been neglected for obvious reasons, but it is undoubtedly advantageous to be able to reconstruct the loads along the blade; indeed, these can be used, for example, for improving the tuning of mathematical models. However, this requirement comes up against the model reduced size; actually, there is not enough space for housing other electronics boards like those shown in Fig. (2.11)), which are of paramount importance in order to have good quality measures from strain gauges readings. A valid alternative solution has been found in the use of optical fibers equipped with FBG (Fibre Bragg Grating) sensors: a solution already being applied in the wind energy field^[94-96]. As for the strain gauge signals, it is necessary to transmit the output of the FBGs from the rotating system to the acquisition system, i.e. the optical interrogator. The sole solution is, then, the use of an electro-optic slip-ring equipped with both electrical rings and an optic rotary joint; a product having the required characteristics and compatible with the space available in the aft part of the nacelle has been therefore identified on the market (see Fig. 4.5).

4.2. Technological solution for aero-elastic blade manufacturing



Figure 4.5: Purchased optical-electrical slip ring equipped with 24 electrical rings and 1 optic rotary joint.

The maximum number of sensors that can be installed on the rotor is strictly related to the type of optical interrogator which is used for acquiring the sensors output, in particular to its bandwidth. Considerin that:

- the optical interrogator Micron Optic sm 130-170^[97] used within this project has a bandwidth of 80 [nm];
- the measured strains are estimated to remain within the range $\pm 1000 \ [\mu\varepsilon]$; thus, the wavelength range used by one FBG sensor (sensitivity: 1.2 [pm/ $\mu\varepsilon$]) is around 2.4 [nm];
- it is necessary to keep approximately a gap of 1 [nm] between the wavelength range used by each sensor, so as to ensure uniquely identification of the wavelength drift due to the applied strain;

a total of 21 strain sensors can be installed on the rotor, i.e. seven sensors for each bladen to be applied both on the suction and pressure side for this purpose, it is convenient to embed, in each blade, two optical fibers equipped with four and three FBGs. Given that all the optical signals shall converge in the single optic rotary joint of the slip ring, and that each optical connection means loss of light intensity, the following optimal solution has been found:

- both optical fibers are inserted into the blade from its root and travel along the blade span close to its suction or pressure side;
- both fibers are made turn back immediately after the position of the farthest-fromthe-root FBG sensor and, finally, the fibers are made going out again from the blade root.

Having to validate the proposed solution, it was decided to embed four sensors at the blade root, two on the suction and two on the pressure side, through which reconstruct the loads during the model testing in the wind tunnel; these latter can be then compared with the loads reconstructed with the strain gauge bridges (installed as shown in Fig. 2.12(b)). If the comparison is satisfactory, the proposed technology is applicable not only to real wind turbine but also to the scaled model and it is, therefore, possible to embed the optical sensors in other parts of the blade being sure of their proper

functioning. In so doing, one obtains a blade instrumented with strain gauges, used to reconstruct the root loads, and FBG sensors, used to reconstruct the loads along the blade span. The layout of the optical fiber and FBG sensors embedded into the model blade is shown in Fig. (4.6).



Figure 4.6: Layout of the optical fibers and FBG sensors embedded into the model blade.

4.2.2 Aero-elastic model blade layout

The blade layout is very similar to that used to produce aero-elastic blades for helicopter rotor models and is shown in Fig. (4.7). The bending stiffness is mainly provided by two properly sized and located spars made of epoxy resin reinforced with unidirectional carbon fibers; this solution allows to:

- ensure the proper distribution of the flap-wise stiffness using a very small fraction of the blade target mass;
- have smaller impact on the distribution of the edge-wise stiffness than the D-spar configuration; this aspect is crucial given the reduced thickness of the model blade airfoils compared to the original blade profiles, which implies, fixed the spars width and thickness, a ratio between the model blade edge-wise and flap-wise stiffness greater than that would be obtained by using original blade airfoils;
- include, between the spar carbon layers, the optical fiber sensors during the lamination process.

As in Bernhard and Chopra^[2] and Jinsong et al.^[3], a Rohacell core is used to avoid any deformation of the blade sections shape due to the forces exerted by the



4.2. Technological solution for aero-elastic blade manufacturing

Figure 4.7: Aero-elastic model blade layout

aerodynamic pressure, as well as it binds the two carbon spars together and it greatly increases the buckling stability of the overall blade. All these advantages are achieved using a very small and predictable fraction of the blade target mass and with little increase of the cross-section stiffness; furthermore, the Rohacell exhibits good deformability properties if subjected to conveniently high level of pressure, like that developed by the mold action (see §4.2.3.2), and resists at high cure temperature. On the other hand, it is necessary to pre-form the Rohacell before its inclusion in the model blade, as well as conveniently sized grooves must be realized for housing the carbon spars. In this regard, it is problematic the high fragility of the material, which requires the use of appropriate techniques for its machining and handling during the lamination phase. The latter handicaps could be avoided using low density balsa wood, but its high Young's modulus would result in an excessive cross-section stiffness.

Finally, an uncured polymeric layer is used to cover the blade surface and to provide good and smooth finishing by filling the Rohacell pore. Moreover, it contributes to increase the torsional stiffness with small effect on the bending stiffnesses. This solution allows to precisely know the modest amount of mass added to the blade, which is not possible in the case, for example, of manual application of epoxy resin or similar; furthermore, during the cure process the adhesive film becomes very fluid allowing its homogeneous distribution all over the blade surface.

The blade root layout is the same of the rigid blades one. In particular, a machined steel component with four small bridges is univocally positioned at the blade root before starting the cure process, allowing the subsequent assembly of the blade on the hub and the application of strain gauges.

4.2.2.1 Materials selection

Composite materials are made up of two or more distinct materials: commonly the reinforcing fiber and the matrix. The first provides most of the stiffness and strength, while the second binds the fibers together providing load transfer between fibers and between the composite and the external supports. The simple and common composite manufacturing method is to place the uncured composite material manually into a mold so that the material can be shaped into the final part. However, to reduce the handling

difficulty of resin and fibers, composite prepregs are usually used. A prepreg consists of resin preimpregnated fibers and its produced by a manufacturing company through a careful control of the resin and fiber ratio, which means that the prepreg properties are stable up to its shelf-life.

During the project two different materials have been used for the production of the carbon spars: the uni-directional medium-modulus graphite/epoxy prepreg Toray T800H (layer thickness equal to 0.15 [mm]), and the HM M50J 100 EU334 32.5%^[98] uni-directional high-modulus graphite/epoxy prepreg (layer thickness equal to 0.0903 [mm]).

With regard to the filler, it has been chosen, among the variants available in the market and shown in table (4.1), the Rohacell WF71, which is a good compromise between low density and not excessive cells size.

Mechanical properties	WF51	WF71	WF110
Density [kg/ m^3]	52	75	110
Young modulus [MPa]	75	105	180
Shear modulus [MPa]	24	42	70
Max curing temperature [○C]	180	180	180

 Table 4.1: Main Rohacell nominal mechanical properties

For the external skin a film of Scotch Weld®AF 163-2K^[99], nominal thickness equal to 0.241 [mm], has been used; this epoxy structural adhesive has a curing temperature equal to that of the UD carbon and its supporting carrier provides good drapability, which is fundamental given the complex and doubly curved blade surface, avoiding, at the same time, excessive deformation of the resin film during its application on the blade surface.

The characterization of the material properties, which proved crucial as we will see later, is shown in Appendix A.

4.2.3 Manufacturing process

The entire manufacturing process has been set up with the goal of manufacturing, with a high level of reproducibility, good quality blades conform with the design requirements. The manufacturing process explained briefly in the following pages should be then consider in this context.

4.2.3.1 Blade components manufacturing

A fundamental part of the blade is the Rohacell core. Its shape has to be quite close to that of the blade and, then, can only be produced by CNC machining. This process allows to generate, with good accuracy and reproducibility, many Rohacell fillers with the desired shape starting directly from the CAD model shown in Fig.(4.8-up).

It is important to remark that the thickness distribution of the machined core is slightly greater than the blade one. With this stratagem, once closed the mold, the core is forced to occupy a lesser volume with consequent increase of its density, that results equal to the nominal density multiplied by the compression ratio; as a reaction, the core exerts a considerable pressure on the inner mold surfaces. Once the curing

4.2. Technological solution for aero-elastic blade manufacturing



Figure 4.8: Pictures of the CAD model (up) and the machined (down) Rohacell core.

process reached the glass transition temperature, this pressure favors the mobility of the skin resin which has already become fluid, ensuring the homogenous distribution of the resin over the blade and, thus, a good surface finishing. Furthermore, the core is conveniently machined in order to create, over its external surface, the grooves which house the carbon spars and that guide the placement of the same spars during the assembly process. In the end, the filler root is worked up to allow the connection with the root steel part, as shown in Fig. (4.8-down), while a large enough hole is machined for housing the pitch motor.

The carbon-made plies are produced starting directly from the material roll and using the automatic cutting tool of Fig. (3.6, on left); in this way, highly precised and reproducible plies can be obtained starting from the mathematics of the plies flat pattern.

The spars lamination is carried out before assembly the blade sub-components and the plies are correctly stacked in accordance to the ply-book. During the lamination process the optical fibers are embedded between the two outer plies while the FBG sensors are placed at the desired location by means of a printout of the spar layout. The first prototypes of the aero-elastic blade have been manufactured lying directly the spar laminate within the groves machined on the Rohacell core surfaces; in this way, the cure process is carried out all at once and after assembly the blade sub-components. In a second time, another solution has been adopted, making it possible to produce aeroelastic blade even more strictly in accordance to the design. Indeed, the spars curing process is carried out prior to assembly the entire blade and through the aid of an adhoc mold specifically conceived for letting the optical fibers get into and out the spars without causing damage, as shown in Fig. (4.9).

This last solution is particularly suitable when high modulus materials are used, as the HM M50J is. Indeed, this material is characterized by a low content of resin and, therefore, within the ply there is not enough resin to avoid the mobility of the fibers along the direction perpendicular to their extension. In case the spar are placed over the core surface uncured, there is the concrete risk that the carbon fibers are shifted from the desired position during the assembly phase, and even more during the closure of the mold due to the strong pressures involved. All this can be avoided by pre-curing



Figure 4.9: Several pictures about the mold used for the spar curing process that is carried out in the autoclave and by vacuum bagging.

the spars with the advantage, at the same time, to manufacture highly accurate and reproducible carbon reinforcement strictly in accordance to the design output.

4.2.3.2 Blade assembly and curing process

Once available all the blade sub-components, it is possible to proceed with the final assembly and, immediately after, the final curing process. For this purpose, an alloy female mold, made up of two CNC machined and polished halves, has been designed taking into account the difference between the carbon fiber and the alloy coefficients of thermal expansion. The shape of the blade has been imprinted on the mold as if it had a pitch angle equal to 22 [deg], so as to have everywhere positive draft. The two halves of the mold perfectly match at the leading edge parting plane, while there is a small gap of 0.1 mm at the trailing edge parting plane, so as to allow excess resin to squeeze out. Finally, some support blocks allow the fixing of the blade root to the mold.

Once spread a thin layer of release agent on the mold, the assembly is characterized by the phases shown in Fig. (4.10) and listed below:

- a thin film of glue is used for sticking together the Rohacell core and the metal part on which are assembled the bearings and the pitch motor;
- both spars laminates are laid down on the Rohacell core surfaces, whether they have been cured or less; in the first case a thin layer of glue is used to ensure the

bonding between the two parts, while in the second case the resin contained in the laminate acts as sticker and prevents the spars laminates to move during the subsequent stages of assembly;

- one single layer of AF 163-2K is laid up over the core using the mold as support base, so as to avoid breaking the fragile core; the resin film is laid on one side of the blade, wrapped around the leading edge and then laid on the other side, so as to form a continuous and homogeneous coating;
- finally, such an artifact is placed over one half of the mold, with the second half successively fixed to the first one using several uniformly spaced screws: in this way high and uniform pressure is generated thanks to the Rohacell compression. The metal root part is fixed, by means of small screws, at the mold support blocks, where small holes allow the optical fibers to going out from the mold without causing damage.



Figure 4.10: Pictures and description of the blade sub-components assembly.

Once finished the assembly procedure the polymerization process takes place in the oven with monitored temperature. At the end of the production process modest finishes are necessary to obtain a high-quality aero-elastic blade.

4.3 Blade design tool

The structural design of the model aero-elastic blade is a highly complex and challenging task, since the design should identify optimal structural layout, choice of materials and sizing of the structural members to ensure the fulfillment of the requirements stated in §4.1. Given layout and materials, the sizing problem should be performed in such a way that the structural properties of the scaled model blade are as close as possible to the appropriately scaled span-wise distribution of mass and stiffness of the full scale

blade. Moreover, the correct aero-elastic scaling of the model requires the matching of the lowest N_{ω} non-dimensional frequencies of the system, i.e. $(\omega_i/\Omega)_{\mathbb{M}} = (\omega_i/\Omega)_{\mathbb{FS}}$ for $i = 1 : N_{\omega}$, where N_{ω} is an appropriate number that selects all modes up to a certain frequency band of interest, while $(\cdot)_M$ indicates quantities of the model and $(\cdot)_{FS}$ quantities of the physical (full scale) system. To simplify the problem, only the lowest three blade frequencies are considered, i.e. two flap and one lag bending modes; the first torsional mode of the blade is high enough that can be neglected in the design problem.

The main challenge of this design problem comes from the need to have a fast design tool that accounts for all the requirements listed above and, at the same time, to capture the local effects in complex 3D structures made with anisotropic composite materials, as the model blade is. The approach here proposed is based on the works published in^[100,101] and consists of a multi-level design procedure that conducts the design with a high level of integration and automation. Fig. (4.11) illustrates the proposed multi-level constrained structural design optimization of model blade, which is briefly described here below and that will be described in detail later.



Figure 4.11: Multi-level structural blade design tool

As a starting point for the optimization, an initial definition of the blade structural configuration and associated material properties is required. Next, the primary design variables are defined at selected span-wise sections while intermediate values along the blade span are interpolated using shape functions. Based on this, a beam-like multibody model of the blade is developed and the automatic computation of the model beam eigenvalues is performed, whereby constraint conditions ensuring the right placement

of the model natural frequencies are included in the optimization process. Furthermore, additional constraints on the unknown design parameters are included, while the cost function of the optimization problem is a measure of the difference between the spanwise distributions of the edgewise stiffness of the model blade and of the physical system blade. The reason associated with the use of this cost function is related to the fact that the model blade airfoils are much thinner than those used in the target blade, which implies that the achievable ratio $\frac{\hat{K}_{egde}}{\hat{K}_{flap}}$, i.e the ratio between the model blade edge-wise and flap-wise stiffness, will be greater than target blade one. With this cost function the solution which minimizes this unavoidable discrepancy is sought.

A 3D CAD model is than directly generated from the optimal blade geometry which precisely accounts for all components of the blade (spar caps, external skin, Rohacell core and blade root insert) as well as their associated material properties and laminate characteristics. The meshing of the blade is performed with the commercial pre-processing software HyperMesh^[102], which provides macro-based facilities for automatic mesh generation, using either shell and solid elements, and the subsequent export of the model data in the form of input files compatible with various commercial FE solvers.

The 3D FE model provides the framework for a fine-level verification of the design constraints, as the detailed model reveals effects that may have been overlooked by the coarse quasi-3D model composed of 1D spatial beam and 2D cross sectional models. For example, local effects at regions with rapidly changing geometry in the span-wise direction cannot be correctly represented by beam models, since in these cases the very hypotheses underlying beam theories are violated. In case constraint violations are detected at the fine-level, the coarse optimization loop is repeated with constraint bounds that are tightened proportionally to the violation amount; coarse and fine level iterations are repeated until an optimal design that satisfies the constraint conditions at the finest description level is obtained.

4.3.1 Coarse-level optimization design tool

The problem of finding the configuration that yields a model aero-elastic blade that meets the requirements specified above is formulated as the following constrained op-timization:

$$p_s^* = \arg\min_{p_s} \left\| \frac{\hat{K}_{edge}(p_s, D) - K_{edge}}{K_{edge}} \right\|,$$
 (4.1a)

s.t.:
$$\boldsymbol{g}_s(\boldsymbol{p}_s) \leq \mathbf{0},$$
 (4.1b)

$$\left|\frac{\hat{\omega}_i(\boldsymbol{p}_s, D) - \omega_i}{\omega_i}\right| \le \varepsilon_\omega \quad i = 1: N_\omega, \tag{4.1c}$$

$$\left\|\frac{\hat{K}_{flap}(\boldsymbol{p}_{s}, D) - \boldsymbol{K}_{flap}}{\boldsymbol{K}_{flap}}\right\| \leq \varepsilon_{K_{flap}}, \tag{4.1d}$$

$$\frac{\hat{W}_b(\boldsymbol{p}_s, D) - W_b}{W_b} \le \varepsilon_W.$$
(4.1e)

In problem (4.1), p_s are structural unknown parameters to be optimized, while D:

$$D = \{R, AF, c(\eta), \theta(\eta), \ldots\}$$
(4.2)

is a list of given data as the rotor radius R, the list $AF = \{\dots, AF_i, \dots\}$ containing the airfoil types used along the blade span, the twist $\theta(\eta)$ and chord $c(\eta)$ distributions along the blade span η , and many others like the mechanical properties of the materials or the direction of the fiber within the laminates.

Problem (4.1) seeks a minimum of the differences between model and target edgewise bending stiffness and is subjected to several constraints; the notation highlights the fact that model quantities (\cdot) depend on the structural configuration p_s and given data D. The first constraint is given by eqs. (4.1b), that in general are used to expressed lower and upper bounds on the unknown structural parameters, for example to limit the maximum and minimum spars thickness, so as to account of the minimum ply thickness, or to limit the minimum spars width in order to succeed in cutting the carbon plies and to have enough gap between the optical sensors, so as to be able to measure the edge-wise bending moment at the blade root. Inequality (4.1c) constraints the lowest three natural frequencies $\hat{\omega}_i(p_s, D)$ of the beam blade model to match, with tolerance ε_{ω} , the corresponding scaled ones of the target blade. Inequality (4.1d) constraints the differences between model and target flap-wise bending stiffness to lay below a predetermined tolerance $\varepsilon_{K_{flap}}$, while inequality (4.1e) constraints the difference between the target weight W_b and the model weight $\hat{W}_b(p_s, D)$ to be lower than a small tolerance ε_W .

The constrained optimization procedures just described requires the ability to define parametric beam models of the aero-elastic blade, as well as to numerically evaluate the blade natural frequencies in the non-rotating blade configuration frame without inclusion of inertial effects, i.e. without adding a geometric stiffness term proportional to the angular velocity squared. The beam models are based on the multibody formulation that is reviewed in §2.3. The structural parametrization used by the optimization problem is based on detailed structural models of the blade cross sections at a number of span-wise locations; from the parametric detailed sectional models, equivalent cross sectional stiffness and inertial data are generated using the approach of Giavotto et al.^[53], which leads to the characterization of the sectional beam data necessary for the definition of the multibody model. In particular, the structural model of the blade comprises the following elements:

- a description of the external shape of the blade, which is obtained by providing the airfoil data coordinates at each span-wise location. This information, together with the curved and twisted aerodynamic reference line and its associated chord length data, fully defines the external blade geometry;
- a description of the blade cross sectional and span-wise internal geometry. The cross sectional definition requires, at a number of locations along the span, to define the chord-wise location, the chord extension and thickness of the spars, as well as the thickness of the external blade skin. The cross section is modeled using:

- panels made of equivalent materials for modeling the spars and the external

skin, which involves a discretization of the mid-thickness line with 1D elements;

- 2D quad elements for modeling the Rohacell core;

while mesh density parameters are associated to the structural elements to support the computation of beam-like equivalent structural blade properties;

 for each cross section, a description of the lay-up of composite laminates of skin and spars, together with the definition of all the necessary material properties. In this regard, the carbon and skin-glue properties are constant for each cross section, while the Rohacell properties may vary from section to section due to the fact that the amount of core compression (see §4.2.3.1) changes along the blade span.

The structural optimization parameters $p_s = \{p_{s,c}; p_{s,w}; p_{s,t}\}$ are defined as the chord-wise location $p_{s,c}$, the width $p_{s,w}$ and thickness $p_{s,t}$ of the spars at a number of user-defined locations along the blade span. The span-wise distribution of the chord-wise location $s_c(\eta)$, width $s_w(\eta)$ and thickness $s_t(\eta)$ of the spars is then defined as:

$$s_c(\eta) = \boldsymbol{\chi}_c(\eta) \boldsymbol{p}_{s,c}, \tag{4.3a}$$

$$s_w(\eta) = \boldsymbol{\chi}_w(\eta) \boldsymbol{p}_{s,w}, \tag{4.3b}$$

$$s_t(\eta) = \boldsymbol{\chi}_t(\eta) \boldsymbol{p}_{s,t} \tag{4.3c}$$

where $\chi_c(\eta)$, $\chi_w(\eta)$ and $\chi_t(\eta)$ are linear shape functions.

From the detailed structural model of the blade, the sectional code ANBA^[53] is used for defining the structural and inertial characteristics of the cross sections along the blade span, that are used as inputs for the definition of the beam model. The computation of a possibly fully populated sectional stiffness matrix, which hence accounts for all possible couplings (flap-torsion, flap-lag, extension-torsion, etc.), is performed starting from a detailed finite element mesh of the cross section using the anisotropic beam theory of Giavotto et al.^[53]. The analysis also yields all other data of interest, including location of centroid and elastic center, orientation of principal axes, sectional inertia and mass. The analysis is conducted for each section along the blade span, whose number and location can be selected by the user to provide for an accurate representation of the blade characteristics; the number of such sections is usually significantly larger than the number of sections where structural optimization parameters p_s are defined.

Problem (4.1) is a mixed continuous-discrete constrained optimization problem, since some of the the unknown variables p_s can assume real values, as the chord-wise location and the width of the spars, while other variables, as the spars thicknesses, are restricted to assume only discrete values, i.e multiple of the thickness t_{ply} of the single ply. Instead of solving the problem using discrete optimization algorithms, usually slower and more complex to manage than continuous optimization algorithms, it has been decided to split problem (4.1) in the 2 sub-problems (4.4) and (4.5).

The unknown variables $p_{s_1} = \{p_{s_1,c}; p_{s_1,w}; p_{s_1,t}\}$ of problem (4.4) are treated as continuous variables. Once solved problem (4.4), another optimization problem is run, with unknowns $p_{s_2} = \{p_{s_2,c}, p_{s_2,w}\}$ the chord-wise location $p_{s_2,c}$ and the width $p_{s_2,w}$ of the spars, and initial values the optimum of problem (4.4), i.e. $p_{s_2}^{(0)} = \{p_{s_1,c}^*, p_{s_1,w}^*\}$.

$$\boldsymbol{p}_{s_1}^* = \arg\min_{\boldsymbol{p}_{s_1}} \left\| \frac{\hat{\boldsymbol{K}}_{edge}(\boldsymbol{p}_{s_1}, D) - \boldsymbol{K}_{edge}}{\boldsymbol{K}_{edge}} \right\|$$
(4.4a)

s.t.:
$$\boldsymbol{g}_s(\boldsymbol{p}_{s_1}) \leq \boldsymbol{0},$$
 (4.4b)

$$\left|\frac{\hat{\omega}_{i}(\boldsymbol{p}_{s_{1}}, D) - \omega_{i}}{\omega_{i}}\right| \leq \varepsilon_{\omega} \quad i = 1: N_{\omega},$$
(4.4c)

$$\left\|\frac{\hat{K}_{flap}(\boldsymbol{p}_{s_1}, D) - \boldsymbol{K}_{flap}}{\boldsymbol{K}_{flap}}\right\| \le \varepsilon_{K_{flap}},\tag{4.4d}$$

$$\frac{\hat{W}_b(\boldsymbol{p}_{s_1}, D) - W_b}{W_b} \le \varepsilon_W.$$
(4.4e)

$$\boldsymbol{p}_{s_2}^* = \arg\min_{\boldsymbol{p}_{s_2}} \left\| \frac{\hat{\boldsymbol{K}}_{edge}(\boldsymbol{p}_{s_2}, D_2) - \boldsymbol{K}_{edge}}{\boldsymbol{K}_{edge}} \right\|$$
(4.5a)

s.t.:
$$\boldsymbol{g}_s(\boldsymbol{p}_{s_2}) \leq \mathbf{0},$$
 (4.5b)

$$\left|\frac{\hat{\omega}_i(\boldsymbol{p}_{s_2}, D_2) - \omega_i}{\omega_i}\right| \le \varepsilon_{\omega} \quad i = 1: N_{\omega}, \tag{4.5c}$$

$$\left\|\frac{\hat{\boldsymbol{K}}_{flap}(\boldsymbol{p}_{s_2}, D_2) - \boldsymbol{K}_{flap}}{\boldsymbol{K}_{flap}}\right\| \leq \varepsilon_{K_{flap}},\tag{4.5d}$$

$$\frac{\hat{W}_b(\boldsymbol{p}_{s_2}, D_2) - W_b}{W_b} \le \varepsilon_W.$$
(4.5e)

The span-wise distribution of the spars thickness is, then, not more related to optimization variables, but is included in the list D_2 of given data, where it appears as discrete value multiple of the ply thickness t_{ply} .

$$D_2 = \left\{ R, \operatorname{AF}, c(\eta), \theta(\eta), \left\lceil \frac{s_t(\eta)}{t_{ply}} \right\rceil * t_{ply} \right\}$$
(4.6)

The constrained optimizations problems (4.4) and (4.5) can than be solved with continuous optimization algorithms. In particular, when refining an already good design solution, which hence provides an initial guess close to the optimal one, gradient-based methods can be used effectively; for the problems solution it has been then used the implementation of the sequential quadratic programming (SQP) method available in the fmincon routine of Matlab.

4.3.2 Fine-level check of the requirements conformity

Once designed the aero-elastic blade with the coarse level design tool, it is fundamental to check the fulfillment of the design constraints, in this case natural frequency constraints, by performing structural analysis on a detailed 3D FE blade model. Indeed, the effect, on blade structural properties, produced by regions with rapidly changing

geometry in the span-wise direction can not be correctly represented by beam models, since in these cases the very hypotheses underlying beam theories are violated.

The generation of the complete CAD solid model with boundary mid-thickness surfaces is generated as follows. A number of cross sections, typically of the order of one hundred, are obtained by thickness-interpolation of the generating airfoil data points, using their span-wise chord and twist distributions. From the chord-wise interpolations of the airfoils mid-thickness projections, collocations data points are obtained with sufficient sampling resolution (typically of the order of one thousand points per cross section) to allow for an accurate surface parametrization. This last is obtained by using NURBS^[103] on each surface describing the two carbon fiber spars and the four leading-trailing edge and suction-pressure side glue-made shell components (see Fig. 4.12(a)). In the present implementation only non-rational surfaces are utilized, i.e. with weights equal to unity, and control points are obtained by least squares from collocation points. Collocation parameters for chord-wise interpolation are obtained using the centripetal method^[104], while span-wise collocation parameters follow by averaging of the parameters determined at two consecutive sections. Once all collocation parameters are obtained, knot vectors are computed by using the average method suggested by de Boor^[103]. Finally, the information associated with the parametric NURBS representation of the resulting surface model is exported in IGES (Initial Graphics Exchange Specification) format towards suitable Matlab tool, therefore imported in HyperMesh. Its geometry modeling environment provides special commands which allow to create blended surfaces between near wireframe elements, to join all the surfaces in an unique one, and fill the resulting surface to make it become a solid. Successively, the solid is sliced, with cutting plane orthogonal to the blade root, into several smaller sub-solids (Fig. 4.12(b)), as to associate more easily the tetra and shell elements to the related structural properties, since these change along the blade span, as well as to facilitate the meshing process.



Figure 4.12: Selected steps in the generation of the CAD model of the aero-elastic blade

For the FE modeling of the blade, mid-thickness layered shell elements are used for modeling the glue-made blade skin and the carbon fiber reinforcements, while solid tetra elements are used for modeling the Rohacell core and the metal part placed at the root of the blade during the lamination process.

The generation of an unstructured shell and tetra mesh is then obtained by using HyperMesh, whose meshing algorithm ensures the conformity of the resulting grid across edges-surfaces bounding the various sub-solids of the model. The meshing starts

from the root sub-solid and continues, gradually decreasing the size of the elements, up to the sub-solid located at the blade tip. At first the surface mesh, characterized by only triangular shell elements, is created; successively, the meshing algorithm creates the internal solid elements, ensuring the conformity between the resulting tetra grid and its bounding shell grid.

Layer-by-layer laminates properties are associated to the shell mesh, while different core materials properties are associated to the tetra elements, in agreement to what explained in §4.2.3.1. Appropriate boundary conditions representing a fully fixed constraint have been imposed by restraining all nodal translational degrees of freedom for the nodes located on the pitch bearings surface. The dynamic behavior of the blade subjected to vibrational excitation is therefore investigated for the detailed 3D model by performing a linear modal analysis in MSC Nastran, without inclusion of inertial effects, in the same way as described for the coarse optimizer

If the verification of the constraint conditions on the fine level model reveals that design inequalities on frequency placement are not satisfied, a heuristic approach is applied in which the constraints are modified proportionally to the violation amount. In particular, assume that the beam-model first natural frequency $\hat{\omega}_1^{(2D)}$ is correctly placed at the end of the i^{th} coarse level optimization, i.e $|\hat{\omega}_1^{(2D)} - \omega_1| / \omega_1 \leq \varepsilon_{\omega}$, but it is not when the fine level 3D analysis is performed, i.e. $|\hat{\omega}_1^{(3D)} - \omega_1| / \omega_1 \geq \varepsilon_{\omega}$. Then the target natural frequency for the next iteration is modified as

$$\omega_1^{(i+1)} = s_\omega \, \omega_1^{(i)},\tag{4.7}$$

where $s_{\omega} = \hat{\omega}_1^{(2D)} / \hat{\omega}_1^{(3D)}$. This way, a constraint condition which reflects the results obtained with the fine-level verification is imposed at the next coarse-level iteration. It may be expected that the ratio between the natural frequencies computed with the quasi-3D and the 3D analysis is almost constant for moderate variations of the structural element sizes. Therefore, the present approach may be used for refining the coarse level analysis according to the results from the fine level solution.

4.4 Fine tuning of the technological solution

The technological solution for the realization of the model aero-elastic blade described in §4.2 is the result of a quite long heuristic tuning process characterized by different steps. In fact, the manufacturing and the design of the blade has been an extremely challenging task, since the shape of the blade is very complex, as well as the technology was totally new and possible critical aspects were unknown. Furthermore, it was not possible to know in advance whether the manufacturing process affects or less the final structural properties of the blade and, in the event, if the developed design tools would be capable or not to predict these effects.

For this reason, it was decided to progress step by step with the objective of having the confidence necessary to the production of the aero-elastic blade, both in terms of manufacturing technology and design tool, as will be explained in the next sections.

4.4.1 Manufacturing and testing of constant geometry specimens

Since one of the major complexity related to the manufacturing of the aero-elastic blade is its highly complex shape, it was decided to start with the production of test specimens of simple, but representative of blade aero-elastic shape, geometry. For this purpose, several untwisted and untapered specimens have been manufactured; in particular, the specimen shape has been generated by lofting the AH79-100C profile with chord equal to 50 [mm], as shown in Fig. (4.13(a)).



Figure 4.13: Constant geometry sample (a) and the mold used for its manufacturing (b).

The width and the total thickness of the two spars made of Toray T800H were set equal to about 30% and 20% of, respectively, the specimen chord and thickness, while the Rohacell core has been pre-shaped using an hot wire foam cutter so that its size was greater than the specimen final volume. Based on what stated previously, it is pretty obvious as an excessive over-sizing of the core can lead to an undesirable increase of the specimen mass, while under-sizing the core could mean compromising the surface quality. Several specimens, characterized by different Rohacell dimensions, have been then fabricated, allowing the experimental identification of the optimal core oversizing.

Experimental modal analysis has been then carried out to characterize the dynamic properties of two specimens with excellent surface finishing. Impact hammer tests were performed in free-free condition, i.e. with the specimen suspended with extremely flexible spring, using one miniaturized and lightweight (0.5 [g]) PCB 352C22 accelerometer and exciting the structure at 6 different locations using an hammer instrumented with a load cell. The first 6 natural frequencies and related eigenvectors have been identified on the basis of the measured FRFs (Frequency Response Function) and using the LSCE method^[105].

The measured modes have been compared to the corresponding ones numerically computed using, as an analysis tool, the optimization design tool described in §4.3.1. The mass of the accelerometer has been included in the beam model in order to take into account its influence on the specimen dynamic properties. Initially, the nominal mechanical properties of the Rohacell and of the adhesive film (see table 4.2) were used, together with the characterized properties of the Toray T800H (see Appendix A), in

the calculation of the cross sectional properties of the beam model. However, the comparison has highlighted not-negligible discrepancies between the numerical and experimental data.

	RohacellWF71	AF163-2K
E [MPa]	105	1110
ν [-]	0.33	0.34
G [MPa]	42	414

 Table 4.2: Nominal properties of the specimens core and skin materials.

It has been then carried out a sensitivity analysis, whose results are reported in table (4.3), to evaluate the influence of the mechanical properties of the two abovementioned materials on the specimens natural frequencies. In particular, the variations $\Delta_{\omega,AF}$ and $\Delta_{\omega,R}$ of the natural frequencies of the specimen beam-model have been computed by changing, respectively, the AF163-2K and the RohacellWF71 Young's and shear modulus.

 Table 4.3:
 Sensitivity analysis related to the influence of the mechanical properties of the

 RohacellWF71 and of the AF163-2K on the beam-model natural frequencies.

	1^{st} flag	1^{st} flap-wise 1^{st} torsional		2^{nd} flap-wise		1 st edge-wise		
$\Delta E, \Delta G [\%]$	$\Delta_{\omega,\mathrm{AF}}$ [%]	$\Delta_{\omega,R} [\%]$	$\Delta_{\omega,\mathrm{AF}}$ [%]	$\Delta_{\omega, \mathbf{R}} [\%]$	$\Delta_{\omega,\mathrm{AF}} [\%]$	$\Delta_{\omega,R} [\%]$	$\Delta_{\omega,\mathrm{AF}}$ [%]	$\Delta_{\omega, \mathbf{R}} [\%]$
-10	-0.32	-1.63	-2.19	-2.47	-0.47	-2.28	-2.52	-3.51
-20	-0.67	-3.53	-4.48	-5.08	-1.16	-5.73	-4.53	-4.01
-30	-1.03	-5.75	-6.88	-7.84	-1.78	-9.46	-6.43	-7.82
+10	0.31	1.44	2.11	2.36	1.28	2.95	1.93	1.1
+20	0.61	2.72	4.19	4.62	1.76	5.52	3.79	2.6
+30	0.89	3.86	6.16	6.79	2.24	7.89	5.61	3.98

Looking at the results, we got that the core properties actively affect all frequencies, while the adhesive properties, as expected, affect mainly the edge-wise and torsional modes. It has been then proceeded to the experimental measurement of both materials mechanical properties, as shown in Appendix A. The experimental tests showed that the mechanical properties are very different from those declared by manufacturers; particularly, it was found that the AF163-2K Young's modulus is nearly three times the nominal value.

The comparison shown in Fig. (4.14) highlights that there is a good match between the experimental and numerical data once the beam-model properties are based on measured materials properties. The differences are lower than 6%, as well as the measured natural frequencies of the two specimens are very similar. Furthermore, the comparison between the measured and the numerically estimated mode shapes is satisfactory, as highlighted by the MAC(Φ_i, z_{Φ_i}) matrix plotted in Fig. (4.15), where Φ_i is the *i*th eigenvector computed on the beam-model and z_{Φ_i} the corresponding measured eigenvector, while the operator MAC(\cdot, \cdot) is defined as:

$$MAC(\boldsymbol{a}, \boldsymbol{b}) = \frac{\left(\boldsymbol{a}^{T}\boldsymbol{b}\right)^{2}}{\left(\boldsymbol{a}^{T}\boldsymbol{a}\right)\left(\boldsymbol{b}^{T}\boldsymbol{b}\right)}$$
(4.8)


Figure 4.14: Comparison between the experimental and numerical natural frequencies of two specimens with $\pm 6\%$ deviation lines.

The production and testing of the constant geometry specimens has allowed to tuning some fundamental aspects related to the manufacturing technology, such as the identification of the optimal over-dimensioning of the core, as well as it has highlighted that it is necessary to characterize the properties of all the materials in order to obtain a good match between the experimental and numerical data. Furthermore, it has been confirmed that the developed technology allows the realization of objects characterized by good reproducibility.

4.4.2 Manufacturing and testing of blade with simplified spars geometry

Before manufacturing the aeroelastic blades, an additional intermediate step has been performed: two blades characterized by an external shape equal to the final desired one, but with a simplified and partially user-defined spars geometry, have been manufactured and tested. In particular, $s_c(\eta)$ and $s_w(\eta)$ have been fixing a priori, while the thickness distribution of the spars made of Toray T800H has been obtained solving problem (4.9), with $D_3 = \{R, AF, c(\eta), \theta(\eta), s_w(\eta), s_c(\eta)\}$ the list of given data.

$$p_{s_{3},t}^{*} = \arg\min_{p_{s_{3},t}} \left\| \frac{\hat{K}_{edge}(p_{s_{3},t}, D_{3}) - K_{edge}}{K_{edge}} \right\|$$
 (4.9a)

s.t.:
$$\boldsymbol{g}_s(\boldsymbol{p}_{s_3,t}) \leq \mathbf{0},$$
 (4.9b)

$$\left|\frac{\hat{\omega}_1(\boldsymbol{p}_{s_3,t}, D_3) - \omega_1}{\omega_1}\right| \le \varepsilon_{\omega},\tag{4.9c}$$

$$\left\|\frac{\hat{\boldsymbol{K}}_{flap}(\boldsymbol{p}_{s_{3},t},D_{3})-\boldsymbol{K}_{flap}}{\boldsymbol{K}_{flap}}\right\| \leq \varepsilon_{K_{flap}},$$
(4.9d)

(4.9e)





Figure 4.15: MAC matrix evaluated for one specimens.

Problem (4.9) seeks a minimum of the differences between model and target edgewise bending stiffness, while just the first flap-wise natural frequencies $\hat{\omega}_1(p_{s_3,t}, D_3)$ of the beam blade was constrained to match the corresponding scaled one of the target blade. Once solved problem (4.9), the span-wise distribution of the spars thickness has been discretized according to the carbon ply thickness.

The spars laminates have been placed on the Rohacell core uncured, while two fiber optics were inserted between the two outer carbon plies during the lamination phase of one of the two manufactured blades. Four FBG sensors had been recorded within the core of the optical fiber placed on the blade suction side, while three FBG sensors had been recorded on the fiber placed on the suction side, as explained in §4.2.1.



Figure 4.16: Cross-sectional properties of the beam-model blade with simplified spars geometry: flapwise stiffness \hat{K}_{flap} (up-right), edge-wise stiffness \hat{K}_{edge} (down-left) and mass \hat{m} (down-right), together with the span-wise distribution of the spars width and thickness (up-left). The label **AH** on the abscissa axes indicates the first location of the **AH79-100C** airfoil.

This preliminary task was carried out with the goals of proving the effectiveness of the developed computing methods in the design of the model aero-elastic blade, verifying that the proposed technological solution allows the realization of a blade whose external shape is congruent to the desired one and assessing the presence of any critical issues concerning the inclusion of FBG sensors within the blade.

Fig. (4.16) shows the span-wise distribution of the spars width and thickness related to the design blade – the spars extend symmetrically with respect to the blade pitch axis – as well as the cross-sectional properties of the beam-model computed at several locations along the blade span. Proceeding from the tip toward the blade root, one sees how the flap-wise stiffness increases very rapidly once reached the transition region between the AH79-100C airfoil and the root cylinder, as well as the mass and edge-wise stiffness steeply increase in correspondence of the machined steel component located at the blade root. The mass of the blade, except the root, is approximately 99 [g], then about 80% more then the scaled mass of the V90, again excluding the root.



Figure 4.17: Picture of the two manufactured blades with simplified spars geometry. One of the two blades shows optical fibers coming out from the blade root.

Experimental modal analysis were carried out to characterize the dynamic properties of the two manufactured blades (see Fig. 4.17), whose great finished external shape good matched the mold shape. The blades roots were fixed to a steel-made plate that, in turn, were fixed to the laboratory floor, thus ensuring a perfectly rigid constraint. Different types of modal analysis have been performed: shaker modal testing on one blade, impact hammer modal testing on the other. The same instruments previously described were used in the impact hammer modal testing, while a laser vibrometer was used to measure the modal displacements in specific marked points along the blade span in the shaker modal testing (see Fig. 4.18). The shaker excitation was transmitted to the blade through a stinger while, at the blade root, an accelerometer placed close to the excitation point allowed to check for any constraint flexibility.



Figure 4.18: Shaker modal testing on one blade.

The first 6 natural frequencies and related damping and eigenvectors have been identified on the basis of the measured FRFs (see table 4.4) and compared to the matching ones numerically computed with the beam-model. The comparison, divided into two separate graphs for clarity, is shown in Fig. (4.19) together with the $\pm 5\%$ deviation lines.

Table 4.4: Mode frequencies and damping related to the first manufactured blade and identified on the basis of the measured FRFs.

Mode	f [Hz]	ζ [%]
1^{st} Flap-wise	24.9	0.763
1 st Edge-wise	41.99	0.761
2^{nd} Flap-wise	56.56	0.457
3^{rd} Flap-wise	125.0	0.387
2 nd Edge-wise	146.7	0.38
1^{st} Torsional	193.7	1.175

The graphs highlight the good agreement between the numerical and experimental frequencies related to the flap-wise modes and to the first torsional mode, while there are discrepancies in the order of 15-20% between the edge-wise natural frequencies. The measured frequencies for the two blades are quite similar, indication that the technological process is characterized by a good reproducibility and that both modal testing techniques are substantially equivalent in terms of analysis results. Given the large discrepancy observed between some modes, the 3D FE model of the designed blade has been generated following the procedure described in §4.3.2, and its numerical modal analysis have been performed. The results for the first six blade frequencies are presented in Fig. (4.20) together with the matching measured frequencies.

It is noted that the agreement between the numerical and experimental frequencies related to the flap-wise modes is excellent, as well as the agreement between the edgewise frequencies is significantly improved. This illustrates the short-coming of using the beam model for detailed calculation of the blade natural frequencies, thereby re-



Figure 4.19: Comparison between the two blades experimental frequencies and the matching ones computed with the beam model; $\pm 5\%$ deviation lines are also plotted.

quiring the fine-level check of the blade conformity with the design requirements and the re-iteration of the coarse-level optimization until the complete satisfaction of the design constraints is verified at finest-level.



Figure 4.20: Comparison between the two blades experimental frequencies and the matching ones computed with 3D FE model; $\pm 5\%$ deviation lines are also plotted.

4.4.2.1 Measurement of blade root bending moments with FBG sensors.

The blade instrumented with optical sensors has been tested in order to verify the quality of the measurements and assess if the loads can be reconstruct with good accuracy. The test have been performed in collaboration with a research team of the Renewable Energy Engineering Laboratory of the South Korean Kangwon National University,

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who was interested in using the FBGs strains as feedback signal for IPC algorithms. First of all, the continuity of the optical signals has been checked, as well as it has been verified that the wavelengths reflected by the embedded FBG sensors had not excessively shifted during the cure, in order to exclude their overlapping under load conditions. Afterwards, the experimental apparatus shown in Fig. (4.21) has been used to find out the relationship between the magnitude and direction of the load acting on the blade and the strains read with the embedded sensors.



Figure 4.21: Experimental apparatus with the blade installed.

In order to pitch the blade by a controlled amount of angle during experiments, an automatic rotating system, made up of a step motor whose shaft is fixed to the blade root, has been designed and manufactured. The external loads have been applied at distance r_w from the root by means of a proper loading system which includes a clamp, whose internal shape is matched with the outer shape of the blade, a weight holder and a wire connecting the clamp and the weight holder. This allowed the blade to be exerted by distributed loads along the airfoil, and the loading to be applied vertically while the pitch angle varies, as illustrated in Fig. (4.21). Given that the FBG sensors are sensitive to temperature changes, a T-type thermocouple probe was used to measure the temperature of the test room near the sensors. The temperature was later used for compensating sensor readings so as to avoid any errors associated with a temperature change. An interrogator was used to generate broadband lights, transmit them into the optical fiber lines, and analyze the lights reflected from the FBG sensors. Finally, the strain information received by the interrogator were retransmitted to a PC connected via an Ethernet cable.

The blade was initially installed with a pitch angle of zero degree and successively clockwise rotated by the step motor from 0 to 180 [deg], with step equal to 1 [deg]. At each step, the strain signals were measured, while the experiments were repeated with five different masses \tilde{w} , whose weight changed from 0.05 to 0.25 [Kg]. All the measurements were performed inside a semi-anechoic chamber to minimize any external interference or temperature changes. Fig. (4.22(b)) shows the strains measured by the root FBGs.

4.4. Fine tuning of the technological solution



Figure 4.22: Blade with FBG sensors installed at its root (on left) and the measured strains (on right), as function of the applied mass and load direction.

To reconstruct the root loads, the same approach normally adopted for the calibration of transducers based on strain gauge readings has been applied. In detail, given ε_{p_1} , ε_{p_3} , ε_{s_1} and ε_{s_4} the strains read by the blade root FBGs (see Fig. 4.22(a)), the linear relationship (4.10) is assumed between the measured strains and the flap-wise and edge-wise bending moments expressed in the pitchable reference frame, i.e. the reference frame that rotates with the blade pitch.

$$\boldsymbol{\varepsilon} = \left\{ \begin{array}{c} \varepsilon_{p_1} \\ \varepsilon_{p_3} \\ \varepsilon_{s_1} \\ \varepsilon_{s_4} \end{array} \right\} = \left[\begin{array}{c} c_{\varepsilon_{p_1},y} & c_{\varepsilon_{p_1},x} \\ c_{\varepsilon_{p_3},y} & c_{\varepsilon_{p_3},x} \\ c_{\varepsilon_{s_1},y} & c_{\varepsilon_{s_1},x} \\ c_{\varepsilon_{s_4},y} & c_{\varepsilon_{s_4},x} \end{array} \right] \left\{ \begin{array}{c} M_{YB,P} \\ M_{XB,P} \end{array} \right\} = \boldsymbol{C}_{\varepsilon} \boldsymbol{M}_{P} \,. \tag{4.10}$$

Once defined

$$\tilde{M}_{YB,P}^{(i)} = r_{\rm w} \tilde{\rm w}^{(5)} \sin \tilde{\theta}_{\rm w}^{(i)}$$
(4.11a)

$$\tilde{M}_{XB,P}^{(i)} = -r_{\rm w}\tilde{\rm w}^{(5)}\cos\tilde{\theta}_{\rm w}^{(i)}$$
(4.11b)

respectively the i^{th} flap-wise and edge-wise bending moments exerted by the weights $\tilde{w}^{(5)}=0.25$ [Kg] with a pitch angle $\tilde{\theta}_{w}^{(i)}$, and $\tilde{\varepsilon}$ the vector of measured strains, is fixed

$$\tilde{\varepsilon} = C_{\varepsilon} \begin{bmatrix} \tilde{M}_{YB,P}^T \\ \tilde{M}_{XB,P}^T \end{bmatrix} = C_{\varepsilon} \tilde{M}_P, \qquad (4.12)$$

leading to the computation of the matrix C_{ε} as:

$$\boldsymbol{C}_{\varepsilon} = \tilde{\varepsilon} \tilde{\boldsymbol{M}}_{P}^{T} \left(\tilde{\boldsymbol{M}}_{P} \tilde{\boldsymbol{M}}_{P}^{T} \right)^{-1}$$
(4.13)

It is then possible to reconstruct the i^{th} applied load

$$\tilde{\boldsymbol{M}}_{P_r}^{(i)} = \left\{ \begin{array}{c} \tilde{M}_{YB,P_r}^{(i)} \\ \tilde{M}_{XB,P_r}^{(i)} \end{array} \right\}$$
(4.14)

by means of the following equation

$$\tilde{\boldsymbol{M}}_{P_r} = \left(\boldsymbol{C}_{\varepsilon}^T \boldsymbol{C}_{\varepsilon}\right)^{-1} \boldsymbol{C}_{\varepsilon}^T \tilde{\boldsymbol{\varepsilon}}, \qquad (4.15)$$

and, finally, to reconstruct the applied mass as

$$\tilde{\mathbf{w}}_{r}^{(i)} = \frac{\left|\tilde{M}_{P_{r}}^{(i)}\right|}{r_{\mathbf{w}}}$$
(4.16)

and the load direction as

$$\tilde{\theta}_{\mathbf{w}_{r}}^{(i)} = -\arctan\left(\frac{\tilde{M}_{YB,P_{r}}^{(i)}}{\tilde{M}_{XB,P_{r}}^{(i)}}\right)$$
(4.17)

The errors $\Delta \tilde{\mathbf{w}} = \tilde{\mathbf{w}} - \tilde{\mathbf{w}}_r$ and $\Delta \tilde{\boldsymbol{\theta}}_w = \tilde{\boldsymbol{\theta}}_w - \tilde{\boldsymbol{\theta}}_{w_r}$ between the reconstructed and applied masses and load directions are reported in Fig. (4.23), as function of the applied mass and load direction.



Figure 4.23: Errors between the reconstructed and applied mass (on left) and load direction (on right).

The graphs highlight an unsatisfactory reconstruction of the loads, which means that the embedded FBGs readings are not solely affected by the bending moments, as assumed in a first time, but probably also by the torque. Indeed, a possible misalignment of the optical sensors with respect to the longitudinal direction can not be excluded, which implies that the sensors probably measure shear strain. Unfortunately, this consideration emerged only afterwards the tests, during which the values of the applied torque were not registered. However, what stated can be demonstrated assuming a linear relation between the FBGs measured strains and the applied weights, considering the latter, in a first approximation, linearly related to the applied torque. The coefficients of C_{ε} were then calculated with:

$$\tilde{M}_{P} = \begin{bmatrix} \tilde{M}_{YB,P}^{T} \\ \tilde{M}_{XB,P}^{T} \\ \tilde{\mathbf{w}}^{T} \end{bmatrix}, \qquad (4.18)$$

using the strains measured loading the blade with the weights $\tilde{w}^{(2)}=0.10$, $\tilde{w}^{(3)}=0.15$ and $\tilde{w}^{(5)}=0.25$ [Kg].

The errors between the reconstructed and applied masses and load directions are reported in Fig. (4.24). The agreement between the reconstructed and applied loads is



Figure 4.24: Errors between the reconstructed and applied weight (on left) and load direction (on right) assuming a dependence of the strains by the applied weight.

much improved but still not totally satisfactory; this means that the linear relation masstorque is still an approximation. The calibration tests will have, then, to be repeated in the future, taking care to stress the blade with a linearly independent family of known bending and torsional loads.

4.5 Aero-elastic blades design and manufacturing

Once solved the most critical aspects related to the technological process, the design of the aero-elastic blades, with and without bending-twist coupling, has been carried out using, as material for the spars, the HM M50J carbon pre-preg, whose high modulus is necessary in order to manufacture a blade characterized by a weight slightly lower than 70 [g] and by a correct placement of its natural frequencies.

4.5.1 Aero-elastic blades design without BTC

The blade without coupling has been design first: problems (4.4) and (4.5) have been solved in sequence followed by the fine-level checking of the conformity of the design with the requirements; few iterations of the coarse design and of the fine level check have been repeated until an optimal design, that satisfies the constraint conditions at the finest description level, was obtained.

Fig. (4.25) shows the span-wise distribution of the spars width, chord-wise location and thickness of the design blade, as well as the cross-sectional properties computed at several locations along the blade span and compared to the target, i.e. the scaled cross-sectional properties of the V90 blade.



Figure 4.25: Comparison between the cross-sectional properties of the uncoupled V2 and target blades: flap-wise stiffness \hat{K}_{flap} (up-right), edge-wise stiffness \hat{K}_{edge} (down-left) and mass \hat{m} (down-right), together with the span-wise distribution of the spars thickness, width and chord-wise location (up-left).

The distribution of the structural properties calculated with ANBA well matches the scaled properties of the reference blade, specially if one looks at the mass and flap-wise stiffness. The edge-wise stiffness distribution is instead quite dissimilar to the reference one, both in the region where the blade shape is determined by the two airfoils AH79-100C and WM006, and in the transition region between the inboard section and the root cylinder. Indeed, the model airfoils are much thinner than those used in the V90, which implies that it is not possible to have, simultaneously, the right distribution of both flap-wise and edge-wise stiffness, with the latter always higher than its target. It is possible to note that this effect has been compensated by structurally weakening the blade in its transition region, so as to satisfy the constraint on the 1^{st} edge-wise frequency placement, as shown in table (4.5).

Mode	$rac{\hat{\omega}^{(2D)}-\omega}{\omega}$ [%]	$\frac{\hat{\omega}^{(3D)}-\omega}{\omega}$ [%]
1 st Flap-wise	+1.15	+0.28
1 st Edge-wise	+10.74	+2.15
2^{nd} Flap-wise	-2.58	-0.86

Table 4.5: Difference between the target natural frequencies and the model blade ones, computed using the beam-model and the detail 3D FE model.

It is noted that the out-of-plane natural frequencies of the designed blade are quite close to their target, both using the beam-model and the 3D-FE model for the computation. However, the value predicted by the beam model for the edge-wise natural frequency is approximately 10% higher than its target, while the one predicted with the 3D-FE model is much closer to the desired value. The shapes of the first three vibration modes for the model blade without coupling are shown in Fig. (4.26).

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Figure 4.26: First three vibration modes for the model blade without coupling.

4.5.2 Aero-elastic blades design with BTC

Once designed the uncoupled aero-elastic blade, we tried to figure out how to generate sufficient bending-twist coupling with the adopted blade layout. For this purpose, the best approach is to introduce BTC to twist the blade sections so as to decrease the angle of attack, the so called twist-to-feather concept shown in Fig. (4.27).



Figure 4.27: Approach adopted to introduce BTC in the model aero-elastic blade.

Fully coupled design, where fibers are rotated in the spar of an angle equal to θ_{spar} and for the whole span-wise extension of the blade, has been then consider. Starting from the design of the blade without coupling, the coefficient $\alpha_s = \hat{K}_{ft}/\sqrt{\hat{K}_{flap}\hat{K}_{tors}}$ (defined as in Lobitz et al.^[106], where \hat{K}_{flap} is the local flap-wise bending stiffness, \hat{K}_{tors} the torsional one and \hat{K}_{ft} the coupled bend-twist stiffness) has been computed for several cross-section along the blade span and for different amount of fibers rotation θ_{spar} . Looking at Fig. (4.28) it is possible to note that rotating the carbon fibers with an angle equal to 3 [deg] generates a coupling that is in line with those reported in Bottasso et al.^[91].

As stated previously, the spars have to be made of high-modulus carbon pre-preg, whose low resin content implies the necessity of pre-curing the spars before the final assembly. Given that:

• manufacturing two blades, respectively with and without BTC, characterized by



Figure 4.28: Coupling coefficient α_s as function of the carbon fiber rotation θ_{spar} .

different shapes of the spars, i.e. different distribution of their chord-wise position and width, implies the use of two different molds for pre-curing the spars, with consequent increase of costs;

- if one considers fixed the spars shape, the movement of the fiber directions away from the blade axis leads to a reduction of the bending stiffnesses; this implies that a complete redesign of the spars, both in terms of shape and thickness, is required in order to exactly fulfill the same constraints imposed in the design of the blade without coupling;
- however, according to the design output of the blade without coupling, the spars weigh little more than 8.5 [g], i.e. less than 15% of the total blade weight; this means that, once frozen the spars shape, there will be a very small increase of the overall weight due to the increase of the spars thickness necessary to compensate the changes in bending stiffness and, therefore, the blade with BTC will be very close to that without coupling.

The design of the coupled blade has been then carried out solving the optimization problem (4.19), with $D_4 = \{R, AF, c(\eta), \theta(\eta), s_w(\eta), s_c(\eta), \theta_{spar}\}$ the list of given data and $s_c(\eta)$ and $s_w(\eta)$ the chord-wise location and width of the uncoupled blade spars.

$$p_{s_4,t}^* = \arg\min_{p_{s_4,t}} \frac{\hat{W}_b(p_{s_4,t}, D_4) - W_b}{W_b}$$
 (4.19a)

s.t.:
$$g_s(p_{s_4,t}) \le 0,$$
 (4.19b)

$$\left|\frac{\hat{\omega}_i(\boldsymbol{p}_{s_4,t}, D_4) - \omega_i}{\omega_i}\right| \le \varepsilon_\omega \quad i = 1:3$$
(4.19c)

The design has been carried out constraining just the placement of the blade natural frequencies and with the goal of minimizing the weight raise due to the unavoidable increase of the spars thickness. Once found the optimal solution, the span-wise distribution of the spars thickness has been discretized according to the carbon ply thickness. Fig. (4.29) shows the comparison between the cross-sectional properties of the designed



Figure 4.29: Comparison between the cross-sectional properties of the designed blades with and without *BTC*, respectively plotted with black solid line and red dashed line, together with the span-wise distribution of the spars thickness.

blades with and without BTC, here named respectively $[0]_{s_t}$ and $[+3]_{s_t}$, together with the span-wise distribution of the spars thickness.

It can be noted that the thickness of the spars has been increased to compensate for the rotation of the fibers, while the structural properties of the $[0]_{s_t}$ and $[+3]_{s_t}$ are very close, which justifies having frozen the spars shape, with consequent reduction of manufacturing costs. This choice has also resulted in a slight decrease of the out-of-plane natural frequencies, which are, in any case, very close to the reference value, as can be seen in table (4.6).

Table 4.6: Difference between the target natural frequencies and the ones of the model blades, with and without coupling, computed using detail 3D FE model.

Mode	$\tfrac{\hat{\omega}_{i}^{\left(3D,\left[0\right]_{s_{t}}\right)}-\omega_{i}}{\omega_{i}}\left[\%\right]$	$\frac{\hat{\omega}_{i}^{\left(3D,[+3]s_{t}\right)}-\omega_{i}}{\omega_{i}}\left[\%\right]$
1 st Flap-wise	+0.28	-1.52
1 st Edge-wise	+2.15	+3.25
2^{nd} Flap-wise	-0.86	-3.72

The complete multi-body model of the scaled wind turbine described in §2.3 has been used to synthesized, considering both uncoupled and coupled aero-elastic blades, the model regulation strategy and a collective-pitch/torque controller, with the latter based on a wind speed scheduled linear quadratic regulator (LQR, see Bottasso et al.^[43]).

Fig. (4.30) reports the values of mean blade pitch (at left) and power (at right) vs. mean wind speed, computed from steady wind simulations, for the two designs. The figure shows that both designs have essentially the same power production. In fact, the necessary trim pitch settings in the partial and full power regions were adjusted so as to compensate the different deformations of the different blades. In particular, the figure shows on the left how pitch is lower in the partial region for coupled blades because

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Figure 4.30: Mean blade pitch setting (left) and power (right) in steady wind vs. mean wind speed for both design solutions.

twisting induced by bending tends to decrease the angle of attack, and this requires a lower pitch to achieve the same aerodynamic torque and rotor RPM at each given mean wind speed. This behavior follows, both in qualitative and in quantitative terms, what has been observed in Bottasso et al.^[91], thus emphasizing the possibility of using the wind tunnel model equipped with the designed blades for research purpose related to the passive alleviation of the loads.



Figure 4.31: *Time histories of the rotor speed (right-up), blade pitch (left-down) and tower base fore-aft bending moment (right-down) for the wind gust (left-up) corresponding to a properly scaled extreme operative gust (EOG, DLC 1.5^[4]) at wind speed close to rated one.*

The effects of BTC on the model loads are illustrated in Fig. (4.31), which shows the time histories of some metrics related to the simulation of an extreme operative gust (EOG) at wind speed close to rated one, a condition that usually defines the envelope tower base fore-aft moment of multi-MW wind turbine. In can be seen how the coupled blades positively affect the tower loads, as well as the rotor speed oscillations are reduced with a lower use of the pitch actuators. This effects have also been highlighted in Bottasso et al.^[91], confirming once again that the wind tunnel aero-servo-elastic

model described in this thesis is thus perfectly suitable for experimentally studying the synergistic effect of active and passive load reduction techniques.

4.5.2.1 Manufacturing of blades with BTC and short outlook on future activities

The carbon-made plies with fibers rotated of 3 [deg] have been produced starting from the mathematics of the plies flat pattern and using the automatic cutting tool of Fig. (4.32(a)); by conveniently rotating the plies flat pattern with respect to the unrolling direction of the material roll, it is possible to produce plies characterized by the desired off-axis fiber angles, as shown in Fig. (4.32(b)).



(a) Automatic cutting tool.

(b) Plies flat pattern with rotated fibers.

Figure 4.32: Carbon plies cutting.

Two fiber optics with embedded FBG sensors have been inserted between the two outer carbon plies during the lamination phase of the spars, which were pre-cured before the final assembly. Fig. (4.33) shows the manufactured blade with bend-twist coupling, whose external finishing is excellent.



Figure 4.33: Manufactured blade with BTC.

As mentioned in the introductive paragraph, due to time constraints it has not been possible to complete the task described at the beginning of the chapter. However, given that it has been amply demonstrated that the developed technology allows the manufacturing of several blade prototypes in accordance with the requirements, it is expected that also the aeroelastic blades, with and without coupling, will comply with the design constraints, as predicted by computational tools. Future work will then address:

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- the characterization of the blade with BTC including static tests, useful to quantify the amount of coupling, and dynamic tests, in order to verify the satisfaction of design constraints related to frequencies placement;
- the fabrication of two sets of three aero-elastic blades, with and without coupling;
- the final testing in the wind tunnel in order to evaluate the synergistic effect of active and passive loads reduction techniques.

CHAPTER 5

APPLICATIONS AND RESULTS

As mentioned in the introduction, the objective of this thesis is to demonstrate that the wind tunnel environment can be an exceptional tool available to researchers not only for studies related the aerodynamics of wind turbines, but also in many applications where a good comprehension of the aero-servo-elasticity of wind turbines is crucial. In the course of this chapter, several applications and related results will then be presented with the goal of highlighting the enormous potential offered by the wind tunnel experimentation, while other futures and interesting applications still under development will be introduced in the concluding chapter.

The first application is related to technologies in support of wind turbine control, and describes the validation of an observer that estimates wind direction based on blade loads, used for providing reliable information to a yaw controller. The second deals with the management of shutdown procedures, and the study of the effects that optimized pitch policies can have on design-driving load. The third and fourth deals with

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controls, and illustrates the regulation of wind turbines in wake interference conditions emulating operation within a wind farm, as well as the reduction of blade loads by higher harmonic individual blade pitch control. The last application is purely aerodynamic and is related to wake measurements, with particular attention to the interaction wake-downstream turbine. It is important to remark that all the reported results have been obtained using rigid blades instead of aero-elastic blades, but this does not alter in any way the results obtained given the considerations reported in §2.2.2.1.

5.1 Yaw observer validation

It is extremely important to prevent a wind turbine to operate at high yaw angles, in order to avoid the reduction of the power which could be extracted from the wind and the generation of side-side loads that excite low damped modes of the machine, with potential increment of fatigue damages. On the other side, yaw actuation has a relevant cost, since it usually involves the movement of a consistent tower head mass. It proves, then, to be quite beneficial limiting the yaw actuation system duty-cycle in terms of reduced cost, complexity, size and maintenance of the yaw actuation system.

In practice, best compromise between operation in yawed flow and yaw actuation is a control strategy that realign the machine only when the misalignment error exceeds a predetermined threshold for a sufficiently long period of time. To that aim, an accurate and reliable measurement of the yaw error is fundamental but, unfortunately, difficult to obtain since vane wind sensors are usually affected by various sources of inaccuracy due, by the others, to rotor wake effect and to the presence of the nacelle. Even when the yaw measurements obtained with existing sensors are well compensated for all sources of error, it is possible to get just point information, usually at hub height, which is a limitation for modern wind turbines.

To overcome the limitations of currently available wind vanes, an innovative approach has been developed and reported in Riboldi^[107], with the whole wind turbine used as a large sensor that provides the necessary information for estimating the rotor relative wind direction. This approach consists of using the blade loads, typically obtained from strain gauges applied at the root of the blades, to infer the yaw direction, by exploiting the effect that a lateral wind component has on the amplitude and phasing of the blade response. The same approach can also be used to infer additional wind information, as for example the vertical wind shear. Since the wind state estimates are obtained directly by the rotor response as measured by the rotor loads, the resulting information has a rotor-equivalent (as opposed to local) nature.

Riboldi^[107] shows that wind direction and shear are observable from the blade loads by inverting the 1P response of an analytical blade flapping-lagging model. In that same work, the simplified analytical result is used to suggest the form of a more general observation model that, while maintaining the same structure of the analytically derived one, is defined in terms of unknown coefficients, which are in turn estimated by a system identification approach. In particular, a wind-scheduled linear model between the flap-lag root loads, the wind cross-flow $\bar{V}_c = \frac{V_c}{\Omega R}$ and shear K (see Fig. 5.1) can be expressed as:

$$\left\{\begin{array}{c} \bar{V}_c\\ K\end{array}\right\} = \boldsymbol{T}(U_{\infty}) \left\{\frac{M_F^{1c}}{M_F^{0P}}, \frac{M_F^{1s}}{M_F^{0P}}, \frac{M_E^{1c}}{M_E^{0P}}, \frac{M_E^{1s}}{M_E^{0P}}\right\}^T,$$
(5.1)



Figure 5.1: Definition of cross-flow and vertical shear.

where $T(U_{\infty})$ contains the linear observation model coefficients, scheduled in terms of the wind speed U_{∞} , while the driving input vector is made up of means (0P) and 1P load harmonic amplitudes of the blade root loads, where $(\cdot)^{1c}$ and $(\cdot)^{1s}$ indicate the first cosine and sine harmonic amplitudes, respectively, while $(\cdot)_F$ and $(\cdot)_E$ indicate the flap-wise and edge-wise blade root bending moment components, respectively.

Having a sufficiently complete set of measurements of wind direction Ψ and associated blade root harmonics, one may identify the model coefficients $T(U_{\infty,i})$ around a given wind speed $U_{\infty,i}$, for example using least squares. Next, the models obtained this way at different wind speeds are linearly interpolated at the generic wind speed U_{∞} , to yield a linear parameter varying observation model covering the range of wind speeds of interest. Finally, at each time instant during operation of the machine, model (5.1) is used to estimate the wind direction, based on the current wind speed and blade load harmonic amplitudes. Given the fact that yaw actuation is performed only when the wind misalignment has been above a certain threshold for a sufficiently long period of time, the estimates provided by the observation model are typically filtered with a moving average to avoid responding to fast wind fluctuation and disturbances. More details on the formulation are given in Riboldi^[107].

The wind observer here briefly described has been extensively validated using models of multi-MW wind turbines in a simulated environment^[46], where the aerodynamics of steady yaw, despite being modeled with state-of-the-art engineering method based on BEM, is still a simplification of the reality. In addition to this limitation, the simulation environment provides measurements not affected by noise; all these considerations led to the experimental validation the observer. In this regard, it was decided to take advantage of the unique capabilities related to testing in the wind tunnel. In fact, the latter allows to have a deterministic knowledge of the input conditions, in terms of speed and wind direction, and it is therefore possible to identify with certainty the unknown terms of (5.1). This is far from obvious in the real environment, given the variability of the wind and knowledge merely statistical of the same, both in terms of magnitude and direction. Therefore, the wind tunnel test is used to confirm the truthfulness of the

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assumptions and theory underlying the observer, allowing to separate this issue from the implementation of the observer in the real environment.

The V2 model has been therefore used to prove the effectiveness and reliability of the yaw observer trough a test campaign carried out in the civil test section. All necessary measurements are available onboard the models, including blade loads and rotor azimuth (necessary so as to compute the load harmonics). Furthermore, the wind conditions in terms of speed and direction are easily controllable, the latter parameter being readily changed by simply bolting the model to the wind tunnel floor at the desired angles with respect to the wind tunnel axis. All the data coming from the model sensors have been acquired during 19 trials where the wind tunnel speed, the rotor speed and the blade pitch have been kept constant and equal respectively to around 8 [m/s], 365 [rpm] and 6.5 [deg], so as to make the machine work in region III. The wind misalignment Ψ has been varied between -45 and 45 [deg] with steps of 5 [deg], with yaw fixed positive if the cross flow seen from downwind comes from left. Since in the wind tunnel the characteristics of the incoming wind shear can not be quickly changed and set arbitrarily, the tests have been performed with no shear. Nonetheless, this is not very relevant and shall not affect the validity of the experiments results, because the independence of shear on misalignment, and viceversa, had been previously verified by simulation^[107].

The measured power and thrust coefficients plotted in Fig. (5.2) clearly show the effect produced by the misalignment of the rotor axis on the model performance. As suggested by Dahlberg and Montgomerie^[108], the measured power coefficients have been used to identified the exponent of the cosinusoidal law (5.2) that relates the power coefficient to the yaw angle.

$$C_P(\Psi) = C_P|_{\Psi=0} * \cos^{3.227}(\Psi)$$
(5.2)

The identified exponent is in the range of the exponents observed in the field and also in other wind tunnel testing, thus confirming once more that the model aerodynamics is representative of a full scale wind turbine.



Figure 5.2: Model power and thrust coefficients vs. yaw angle together with the graphs of the cosinusoidal law (5.2) that best fits the experimental data.

For each trial, the flap-wise $M_{YB,P}$ and edge-wise $M_{XB,P}$ bending moments were recorded for 30 sec, successively filtered with a zero-phase 4th-order Butterworth lowpass filter to lower down the noise level and finally observed using a demodulation window of 15 rotor revolutions. In this way, it is possible to determine the moments mean, as well as the amplitude and phase of the 1P component. These metrics, related to the blade whose pitch axis is the reference for the rotor azimuth measurements, are shown in Fig. (5.3), with notation in agreement with the following equation:

$$M_{YB,P}^{0P-1P} = M_F^{0P} + M_F^{1P} \cos(\psi + \phi_F^{1P})$$
(5.3a)

$$M_{XB,P}^{0P-1P} = M_E^{0P} + M_E^{1P} \cos(\psi + \phi_E^{1P})$$
(5.3b)

where $M_{YB,P}^{0P-1P}$ and $M_{XB,P}^{0P-1P}$ are the measured 1P flap-wise and edge-wise loads harmonic with also the mean values included. The graphs underline that the 1P harmonic components of the loads vary, both in terms of amplitude and phase, as a function of the yaw angle, justifying, therefore, their use to infer the yaw angle. One can see also that the quality of the measurements of the flapping loads is very good, while higher fluctuations due to measurements noise are observed in the edge-wise loads, so perfectly in agreement with what stated in §2.2.2.2.



Figure 5.3: Measured bending moments mean, 1-rev amplitude and phase vs. yaw angle.

Once acquired all the necessary data, the identification of the unknown terms of $T(U_{\infty})$ has been performed using the blade loads acquired at 7 different yaw angles, i.e. between -30 [deg] and +30 [deg], with step equal to 10 [deg]. The synthesized observer was then used to infer the yaw angle using the blade loads signals registered during wind tunnel runs characterized by a wind misalignment varying between -35 [deg] and +35 [deg], with step equal to 10 [deg].

The results reported in Fig. (5.4), confirm, on one hand, the potentialities of the yaw observer to work properly in the real environment, given that the errors between



Figure 5.4: Comparison between the real and the averaged observed yaw.

the observed wind misalignment vs. the true one, i.e. the model mounting angle with respect to the wind tunnel axis, as measured with a laser emitter mounted in the hub, are generally within the range of ± 0.8 [deg], and, on the other, the great potential offered by the present aero-servo-elastic experimental facility as a tool for the validation of innovative wind turbine control support technologies.

5.2 Emergency shut-down maneuver: innovative solution and model calibration.

Among the objectives for which the model was developed there is the test of emergency operating conditions that occur rarely during the life of the machine and whose management is given to the supervision system, which handles the system when an abnormal operation is detected.

Looking in the literature, it is possible to find several papers^[109,110] concerning the wind turbine control state of the art and its future development, but a scientific production related to the improvement of the emergency management is totally absent. In particular, the literature research conducted by the author has uncovered just a patent^[111] which illustrates a pitch control strategy, based on rotor acceleration measurements, suitable to optimally brake the wind turbine rotor during emergency conditions.

However, manage the emergencies in a timely manner could mean the reduction of the envelope loads, which can be readily translated into savings by resizing the affected wind turbine components, especially if the next dominating non-emergency-related loads are significantly lower. In particular, this statement is usually true for the tower, whose sizing loads are often due to the effect produce by an extreme gust with contemporary grid loss at wind speed closed to the rated one (DLC $1.5(a)^{[4]}$) as shown in Fig. (5.5), with the generator disconnected and none of the onboard measurements available for feedback.

Unfortunately, it is difficult to exactly replicate in the wind tunnel the typical conditions prescribed by the certification guidelines during emergencies. In fact, the generation, for example, of a typical "Mexican hat" Extreme Operating Gust (EOG)^[4] implies substantial changes in the wind speed within about 10 [s]. Given the time scaling ratio

5.2. Emergency shut-down maneuver: innovative solution and model calibration.



Figure 5.5: Non-dimensional tower fore-aft bending moments as function of several DLCs prescribed by rules.

reported in table (2.1), we get that the exact replication of an EOG would require the ability to substantially change the wind tunnel flow speed in about half a second, something that is hard if not impossible to do for such a large wind tunnel as the one used here.

Notwithstanding such limitations, the wind tunnel testing of shutdown procedures can still provide useful information to the analyst. To illustrate this point, we performed the following tasks. At first, we defined different open-loop control policies for the pitch actuation during shutdowns, and we tested them in the wind tunnel using the aero-servo-elastic model; the tests included the grid loss condition, simulated by abruptly setting the control torque to zero, but were conducted in steady wind for the reason noted above. Next, we used the experimentally measured response to calibrate the mathematical model of the machine, achieving a good match between simulated and measured responses. To accurately capture peak loads, the model tuning phase highlighted the importance of a correct setting of the aerodynamic parameters at unusual angles of attack, achieved during this maneuver but otherwise seldom encountered in other operating conditions. Finally, we used the validated mathematical model to simulate shutdown maneuvers, this time including gusts, verifying in this more complete case the load reduction capability of the modified pitch profiles.

In an industrial design environment, these steps should be followed by up-scaling and the study and optimization of the pitch profile for the full scale machine, something that can be done more effectively and with greater confidence once a study as the one conducted here on the scaled model has been completed. In fact, such a study allows to evaluate the goodness of innovative maneuvers using simulation tools whose ability to capture the transient physical processes that take place during extreme maneuvers have been experimentally validated. Furthermore, it also highlights which are the critical modeling aspects of a given class of problems, which in this case involved the aerodynamics at negative angles of attack. Clearly, similar conclusions would be very hard and expensive to achieve by using full scale field testing mainly for two reasons:

• the lack of sufficient experimental data to be used for the model tuning, given that

extreme conditions rarely occur in the real environment;

• the uncertainty related to the measurement of the wind on the entire rotor disk, with consequent difficulties to correlate model inputs, i.e. wind time series, and outputs, i.e. turbine measurements time series.

5.2.1 Innovative open-loop pitch actuation for the emergency shut-down

A general approach to the problem of reducing emergency-related envelope loads is described in Guerinoni^[112]. In that work, a constrained optimal control formulation is used for automatically computing the best possible open-loop pitch profile during a shutdown. The solution is obtained by minimizing peak loads during the breaking maneuver over a variety of wind conditions and fault time instants, subjected to constraints that ensure an upper limit to the rotor over-speed and avoid to pointlessly push the peak loads too much below the next dominating ones. Although it was not possible to use that approach here for property right issues, the following and simpler approach has been considered.

In the industry, the supervisor usually manages the emergencies by activating a shut down procedure which consists of increasing the collective blade pitch up to the maximum allowable limit and at the maximum allowable pitch rate $\dot{\beta}_{max}$, according to the equation:

$$\beta_{ref}(t) = \beta_{max} \left(t - t_{gl} \right) + \beta \left(t_{gl} \right)$$
(5.4)

with $\beta(t_{gl})$ and t_{gl} respectively the pitch and the instant when the fault occurs. By the way, assuming to have available the measure of the tower root fore-aft bending moment M_{fa} and having the goal to reduce this tower load, an intuitive solution for the emergency management could be the following one: set the blades pitch equal to the sum of a linear time-variant term, which acts for reducing quickly the rotor speed, and a load-mitigation term which is the output of a PID regulator that feeds back the error between the load measures and a reference value $M_{fa,ref}$.

$$\beta_{ref}(t) = \dot{\beta} \left(t - t_{gl} \right) + \beta \left(t_{gl} \right) + \beta_{fa}(t)$$
(5.5a)

$$\beta_{fa}(t) = K_P(M_{fa} - M_{fa,ref})(t) + K_I \int_{t_{gl}}^{t} (M_{fa} - M_{fa,ref})(\tau) d\tau + K_D \dot{M}_{fa}$$
(5.5b)

Equations (5.5) reports the pitch law which was implemented in the simulation environment described in §2.3, with $\dot{\beta}$ constant and lower than the maximum allowable pitch rate, while Fig. (5.6) reports the outputs of several numerical simulations of grid loss conditions with constant wind speed ($U_{\infty} = U_{\infty,r} \approx 8 \text{ [m/s]}$).

The values of the PID gains were suitable set to substantially reduce the peak load during the springing forward of the machine, generated by a rapid decrease and inversion of the rotor thrust caused by the aggressive pitch-to-feather of the blades, without incrementing the maximum rotor speed, as can be observed comparing the simulations outputs of two emergency maneuvers with the same $\dot{\beta}$.

Unfortunately, the approach just explained can not be used in a real supervisor, since no measurement is available after a grid loss event, and it is therefore necessary to find an open-loop pitch law that has to be unique for all emergencies or, at least, be different



Figure 5.6: Comparison between non-dimensional rotor speed (a) and tower fore-aft bending moment (b) obtained with different values of $\dot{\beta}$, with or without addition of the term β_{fa} .

on the basis of the last measures available before the fault. The proposed solution consists on the identification of the sinusoidal term:

$$\widetilde{\beta}_{fa}(t) = \begin{cases} -\beta_a \sin\left[\omega_{\rm OL}\left(t - \tau_s^{II}\right)\right] & t \in \left[\tau_s^{II}, \tau_s^{II} + \frac{\pi}{\omega_{\rm OL}}\right]; U_{\infty} \le U_{\infty,r} \\ -\beta_a \sin\left[\omega_{\rm OL}\left(t - \tau_s^{III}\right)\right] & t \in \left[\tau_s^{III}, \tau_s^{III} + \frac{\pi}{\omega_{\rm OL}}\right]; U_{\infty} > U_{\infty,r} \end{cases}$$
(5.6)

obtained from the solution of the minimization problem:

$$p^* = \arg\min_{p} E, \quad E = \sum_{i=1}^{N} \int_{t_{gl}}^{t_0^{(i)}} \left(\beta_{fa}^{(i)}(t) - \widetilde{\beta}_{fa}(t)\right)^2 \mathrm{d}t$$
 (5.7)

where the unknown to-be-defined parameters are the amplitude β_a , the time shift τ_s and pulsation ω_{OL} here collected in the vector $\boldsymbol{p} = \{\beta_a, \omega_{OL}, \tau_s^{II} \tau_s^{III}\}^T$. Two time shifts are included in the identification parameters since it was found that they were necessary to effectively cover the entire operating range of the machine, i.e if the wind turbine is working above (τ_s^{III}) or below (τ_s^{II}) the rated wind speed.

The identified sinusoidal (see Fig. 5.7) is the one that best approximates the loadmitigation terms $\beta_{fa}^{(i)}$, obtained from numerical simulations with constant wind speed in the range $6 \div 12 \text{ [m/s]}$ and $\dot{\beta} = \frac{3}{4}\dot{\beta}_{max}$. Since the goal of the proposed emergency maneuver is the reduction of the negative peak of the tower fore-aft bending moment, the cost function E of the optimization problem is only computed from the fault time t_{gl} until $t_0^{(i)}$, which is the first instant when $\beta_{fa}^{(i)}$ becomes null.

The open-loop pitch profiles identified in this way, were implemented onboard the supervisory control system of the wind turbine model, and tested in the wind tunnel.

5.2.2 Experimental testing and model calibration

The tests were performed in the civil test section and at four different uniform wind tunnel speeds, varying from 6 to 12 [m/s] (turbulence intensity equal to 1-2%) with steps of 2 [m/s]. For each wind speed the maneuvers reported in table. (5.1) were



Figure 5.7: Comparison between the load-mitigation pitch $\beta_{fa}^{(i)}$, related to simulations of region II (on right) and region III (on left) steady-wind conditions, with the open loop pitch $\tilde{\beta}_{fa}$.

tested, with the goal of comparing three standard emergency maneuvers, which differ in the pitch rate, with two innovative shut down procedures based on (5.6), which also differ in their steady-state pitch rates. The same maneuvers were simulated with the Cp-Lambda code.

 Table 5.1: Maneuvers tested in the wind tunnel.

maneuvers ID	$eta_{ref}(t)$
1	$\dot{\beta}_{max}\left(t-t_{gl}\right)+\beta\left(t_{gl}\right)$
2	$rac{3}{4}\dot{eta}_{max}\left(t-t_{gl} ight)+eta\left(t_{gl} ight)$
3	$\frac{1}{2}\dot{\beta}_{max}\left(t-t_{gl}\right)+\beta\left(t_{gl}\right)$
4	$\frac{3}{4}\dot{\beta}_{max}\left(t-t_{gl}\right)+\beta\left(t_{gl}\right)+\widetilde{\beta}_{fa}(t)$
5	$\frac{1}{2}\dot{\beta}_{max}\left(t-t_{gl}\right)+\beta\left(t_{gl}\right)+\widetilde{\beta}_{fa}(t)$

The comparison between the experimental and numerical outputs highlighted some discrepancies between the trend of the rotor speed and of the tower root fore-aft bending moment immediately after the failure. Appropriate numerical analysis have shown that these discrepancies are mainly related to:

- uncertainties on the $C_L(\alpha)$ curve of the outer blade airfoil at angle of attack lower than -1 [deg]. Indeed, this region of the $C_L(\alpha)$ curve was not identified from the $C_P - \lambda - \beta$ and $C_F - \lambda - \beta$ measured experimentally (see §3.3), but estimated using the method proposed in Moriarty and Hansen^[113], which is the most adopted for extending to ±180 [deg] the polars of airfoils which operate at high Reynolds numbers. The plots reported in Fig. (5.8) confirm what just sentenced, since they evidence that the maximum rotor speed and the negative peak of the load occur when the outer airfoils are working at very low angle of attack, while the inner ones are usually operating at angles of attack whose lift coefficients have already been identified.
- not correct modeling of the effect of the balance stiffness on the first fore-aft and side-side tower natural frequencies;



• not correct estimation of the tower damping.

Figure 5.8: Comparison between the angles of attack felt by the airfoil at $\eta = 0.4$ (a) and $\eta = 0.8$ (b) obtained with simulations at 8 [m/s], and for several maneuvers. The squares represent the instant of maximum over-speed, while the circles the instant of minimum M_{fa} .

The experimental data have been therefore used for tuning the tower damping and the values of the linear spring stiffness used for modeling the tower root balance. Moreover, the identification procedure described in §3.3 was used to estimate the $C_L(\alpha)$ curve of the WM006 airfoil in the range -25÷0 [deg] by minimizing the following cost function:

$$J = \sum_{i=1}^{N_m} \int_{t_g l}^{t_0^{(i)}} \left(\left(\tilde{\Omega}^{(i)}(t) \right)^2 + w_M \left(\tilde{M}_{fa}^{(i)}(t) \right)^2 \right) \, \mathrm{d}t, \tag{5.8}$$

where w_M is a weighing factor chosen so as to make the two terms dimensionally consistent, while $\tilde{\Omega}^{(i)}(t)$ and $\tilde{M}_{fa}^{(i)}(t)$ are the differences between the time trend of the experimental and numerical data related to the i^{th} maneuver used for the identification.



Figure 5.9: Initial (dashed lines) and identified (solid lines) WM006 lift coefficients together with its distributions of the additive corrective functions (red line, $\Box = nodes \ location$).

Only a subset N_m of the 20 available shutdowns was used for the tuning of the

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model, while the remaining ones were used for validating the results; in particular, the model tuning allowed to reduce the cost function, computed for all the experiments, of approximately the 15 %. The identified $C_L(\alpha)$ curve (Fig. 5.9) clearly shows a sharper stall and higher values of the C_L s in the post-stall region with respect to the ones estimated with the method proposed in Moriarty and Hansen^[113]. Evidently, a more precise definition of the airfoil behavior was necessary in the present case.

The plots reported in Fig. (5.10) show the rotor speed and the non-dimensional tower root fore-aft bending moment which were recorded at wind speed equal to 8 [m/s] (i.e close to the rated speed) and with the pitch profile ID 1 from table (5.1), compared to the ones numerically computed before and after the model parameters tuning.



Figure 5.10: Test at wind speed 8 [m/s]: comparison between the experimental and numerical rotor speed (a) and non-dimensional tower root fore-aft bending moment (b) related to emergency maneuver ID 1, before and after the model parameters tuning.

The parameters tuning allowed to improve the agreement between the numerical and experimental fore-aft time trends, while practically negligible effect can be observed on the agreement between the speeds of rotation. There could be several explanations for this last discrepancy:

- Physical phenomena not correctly modeled, like the inflow dynamic and/or the dynamic of the aerodynamics.
- Values of some model parameters that do not match exactly the real ones. One of these parameters could be the rotor inertia that, albeit slightly different, considerably affects the speed of rotation. Indeed this last is, in first approximation, the integral over time of the aerodynamic torque divided by the rotor inertia and the integration of even small errors could produce considerable discrepancies.
- Gear-head backlash do not removed by the torsional spring (see §2.2.2.1). Indeed, looking at Figs. (5.11) and (5.12) it is quite evident that the time trends of the tower loads are really well captured by the model for all the experiments carried out in the wind tunnel, while the gaps between the rotor speeds become even larger as the wind speed increases. Considering that higher wind speed means also higher value of $\beta(t_{gl})$, it could be possible that the torsional spring did not remove totally the gear-head play as the pitch angle increased; as result, the real blade pitch could

be lower than the encoder readings and the braking action produced by the rotor could be delayed, resulting in an higher over-speed.



Figure 5.11: Comparison between the experimental and numerical rotor speed related to all emergency maneuvers. The red lines represent the experiments data, while the black ones the numerical outputs.

5.2.3 Wind gust simulations

In any case, the tuned mathematical model captures the meaningful behavior of the system dynamic even in transient state, and can be therefore used for evaluating the goodness of the proposed innovative emergency maneuvers. Fig. (5.13) shows the numerical output in case of extreme gust contemporary to grid loss. The simulations were performed just at rated wind speed, which is known to be usually the most critical condition, and the fault was simulated at the beginning (G1) and at the end (G3) of the gust raising part, as well as in correspondence of the maximum gradient (G2). It is possible to notice how the reduction of the pitch rate involves, on the one hand, an increase of the speed of rotation, as it slows the braking action produced by the rotor aerodynamics,



Figure 5.12: Comparison between the experimental and numerical fore-aft bending moment related to all emergency maneuvers. The red lines represent the experiments data, while the black ones the numerical outputs.

but on the other hand substantially reduces the negative peaks pointed out in the tower root loads. Moreover, the procedure proposed by the author (ID 4) allows to reduce the tower loads with respect to the standard one (ID 1) with small influence on the rotor over-speed values. These last are also comparable to those measured on real multi-MW wind turbines, thus confirming the ability of the model to reproduce, both qualitatively and quantitatively, the dynamics of real machines.



Figure 5.13: Wind speed \approx 7.5 [m/s]: grid loss and contemporary EOG50 gust (see ^[4]). Comparison between the numerical rotor over-speed and the non-dimensional tower root fore-aft bending moment related to different emergency maneuvers.

A more thorough study should be conducted to prove the real robustness of the optimized policy, by verifying loads and over-speeds over a larger variety of conditions. In this regard, the approach of Guerinoni^[112] explicitly accounts for the entire operating envelope of the machine. Nonetheless, the example presented here has served the purpose of illustrating the process of calibration/validation in the wind tunnel without gusts and to demonstrate that the proposed emergency procedure, once included the presence of gusts, allows to considerably reduce the loads on the tower with minimal effect on the maximum speed of rotation.

5.3 Control in wake interference conditions

One of the project objectives is the study of advanced control algorithms that simultaneously maximize the annual energy production and reduce the active loads on wind turbines subject to turbulent wind conditions. These features not only allow for the basic control of the model, but also for the operation of two wind turbine models, one in the wake of the other. In fact, the testing of two wind turbines in wake effects is one of the unique characteristics of the present experimental facility, since it enables the study of wind turbine interactions and the testing of wind farm control algorithms. Although the dimensions of the model and of the wind tunnel allow only for two wind turbines to be simultaneously tested, this setup still captures the essence of the couplings that take place within a wind farm, i.e. the reduced speed and increased turbulence experienced by the downstream machine, and the dynamic changes of these parameters that follow from a change in the trim set point of the upstream wind turbine. To author knowledge, most of the activity about wind farm control is performed at the level of numerical simulation, where either very simplified wind farm interaction models^[114–116], which might not be very accurate, or very sophisticated CFD-based ones^[117], which are ex-

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tremely demanding from a computational point of view, are used. On the other hand, the present experimental facility, although limited to two wind turbines, allows for a rapid, low cost and high fidelity testing of control algorithms.

Given that the wind tunnel validation of advanced wind farm control methodology will certainly be the subject of future research, it is of fundamental importance providing the model of a collective pitch-torque control that ensures a good tracking of the trim reference values and a low level of the oscillations, even when the wind turbine operates in wake-flow conditions. In detail, the control must have different target depending on the wind speed^[63] and, in this regard, the regulation trajectory of the machine has been computed using the approach reported in Bottasso et al.^[43], which is briefly summarized next keeping the same notation of the cited paper.

During low winds (region II), it is necessary to maximize the power output, which is done by setting the optimal generator torque $T_{g,opt}$ proportional to the square of the rotor speed (5.9) and by keeping the blades pitch equal to β^{II} , while during strong winds (region III) the generator torque is kept at the nominal value and the blades are pitched in order to ensure that the model operates at rated rotor speed and power. Usually, for large rotor machines a blade tip speed constraint has to be enforced; consequently, the rated power can not be reached by regulating the machine using region II policy and it is important to manage the transition region, here called region II¹/₂, between region II and III. In this region the control goal is therefore to keep the rotor speed equal to the rated value through the change of the generator torque, while the blades pitch is kept equal to a λ -depended optimal angle.

Once defined the machine regulation policy, one may readily trace, starting from the experimental or numerical characterization of the rotor performance (in terms of $C_P - \lambda - \beta$ curves), the schedules of the optimal regulation set points for the rotor speed, collective blade pitch and generator torque. Such quantities are of crucial importance, since they will be used as goal regulation values for the control laws described in the next paragraphs.

5.3.1 Rotor speed control with PI

The first collective pitch-torque control implemented is actually the combination of two PI controllers, here called $PI_{\rm T}$ and PI_{β} , whose synergistic effect allows to achieve the desired system behavior for the entire range of wind speeds. In detail, the $PI_{\rm T}$ control prevents the wind turbine to operate in overspeed condition by properly setting the generator torque reference; its output value $T_{g,PI}$ is computed as expressed in (5.9) and compared to the optimal torque value $T_{g,opt}$: the higher of these two values is than used as the torque reference value $T_{g,ref}$

$$T_{g,ref} = \max \begin{cases} T_{g,opt} = \frac{1}{2}\rho\pi R^5 \frac{C_P^{II}}{(\lambda^{II})^2} \Omega^2 = k_{opt}^{II} \Omega^2 \\ T_{g,PI} = K_{P_{\rm T}} \left(\Omega - \Omega_{ref}\right) + K_{I_{\rm T}} \int \left(\Omega - \Omega_{ref}\right) \end{cases}$$
(5.9)

To explain easily such controller, let us observe two cases: if the rotor speed remains lower than its reference value, i.e. the system is operating in region II, the $PI_{\rm T}$ controller reduces the required generator torque with the goal of increasing the rotor speed, obtaining as result that the optimal torque $T_{g,opt}$ is higher than the $PI_{\rm T}$ output and is therefore used as reference. On the other hand, if the rotor speed is higher than its reference value, which is a possible condition for a system operating in region II¹/₂ or III, the $PI_{\rm T}$ controller set the generator torque to even higher value until an equilibrium is reached; in this case the $PI_{\rm T}$ output is higher than the optimal torque $T_{g,opt}$ and is therefore set as the generator torque reference. Anti-windup techniques have been implemented in the $PI_{\rm T}$ controller in order to avoid negative effects due to switching logic, as well as controller output limits are defined in order to ensure that the generator torque is always in the range $T_g \in [0, T_{g,r}]$, with $T_{g,r}$ the rated value of the generator torque. This restriction allows a smooth transition between regions II¹/₂ and III, when the torque reaches its nominal value with consequent saturation of the $PI_{\rm T}$ controller and taking of control by the pitch controller PI_{β} . Furthermore, the restriction has no effect on the performance of the system, since the speed fluctuations in the wind tunnel are not so fast as to require the simultaneous action of the torque and pitch control with the system operating in region III, as happens in the case of real wind turbine gust.

As stated earlier, in region III, the wind turbine is generating its nominal power and all the excess wind power has to be rejected by pitching the rotor blades. For that purpose, a classical PI_{β} controller is designed: the rotor speed measurement is used as the feedback signal and the rotor speed nominal value is used as the reference. Similarly to torque controller, actuation limits are also implemented together with antiwindup techniques; in particular, a lower limit, i.e the minimal allowable pitch angle, is defined and scheduled as function of wind speed.

It is important to remark that the torque and pitch controllers do not have exactly the same reference values. Indeed, the reference for the pitch controller is set equal to the rated rotor speed value, while the reference for the torque controller is set on a slightly lower value, with difference around 1%. This small gap is by itself sufficient to ensure a full separation of region II¹/₂ from region III without adding additional switching logic. Furthermore, a small difference between the references can reduce or even eliminate unnecessary pitch action in region II¹/₂ and unnecessary torque action in region III. The same could be accomplished by using wind speed measurement to define the current operating region and necessary control actions, but the previously described approach is chosen to avoid the need for high-fidelity wind speed measurement.

The implemented collective pitch-torque controls are both active in all operating regions, but, thanks to the limits implemented on each controller, when one is saturated the other is actively controlling the wind turbine, and this occurs in all operative regions, also in the $II^{1/2}$ one, thanks to the different reference values used for the rotor speed.

5.3.2 Rotor speed control with LQR

The second collective pitch-torque control implemented on the model wind turbine is based on LQR (Linear Quadratic Regulator) theory. Such control algorithm is briefly described here, while more detailed description can be found in Bottasso et al.^[43]. LQR is state space controller based on linear mathematical model which minimizes quadratic cost function comprised of model states and inputs. In particular, the adopted mathematical model takes into account the following state variables grouped in the state vector \boldsymbol{x}_{LQR} :

- two states for the tower oscillations;
- two states for the pitch actuator dynamic, assuming that all the blades are pitched

identically;

- one state for the generator dynamics (although it is shown that torque changes almost instantly regarding the sampling time);
- one state for the wind turbine rotor speed;
- one state for the integral of the rotor speed, to eliminate error in steady state;

Since a wind turbine is an highly nonlinear system, the wind turbine mathematical model is linearized at several wind-speed-scheduled regulation set points and a gain-scheduled LQR is obtained. For each operating point, state vector deviation Δx_{LQR} from the steady state value $x_{LQR}^0(U_{\infty})$ is therefore defined as

$$\Delta \boldsymbol{x}_{\text{LQR}} = \boldsymbol{x}_{\text{LQR}} - \boldsymbol{x}_{\text{LQR}}^0(U_{\infty}), \qquad (5.10)$$

and is piecewise affine function of the wind speed U_{∞} . The controller output is defined as:

$$\boldsymbol{u}_{LQR} = \begin{bmatrix} \beta_{ref} \\ T_{g,ref} \end{bmatrix} = \boldsymbol{u}_{LQR}^{0}(U_{\infty}) - \boldsymbol{K}_{g}(U_{\infty})\Delta\boldsymbol{x}_{LQR}, \qquad (5.11)$$

where $\boldsymbol{u}_{LQR}^0(U_{\infty})$ is the steady state model input and $\boldsymbol{K}_g(U_{\infty})$ is the controller gain vector.

It is obvious from (5.11) that the control loop is comprised of two parts: the feedback part $-\mathbf{K}_g(U_\infty)\Delta \mathbf{x}_{LQR}$ and feedforward part $\mathbf{u}_{LQR}^0(U_\infty)$. Similarly to discussion in §2.2.2.3, feedforward part is expected to improve the wind turbine behavior in case of changes in wind speed. In that case feedforward part should bring the wind turbine close to the new operating point while LQR (with integral behavior) should compensate any disturbances or steady state errors introduced by plant-model mismatch.

It is important to note that criterion for LQR is not the same for each operating point, since different wind turbine behavior is desirable for different operating conditions. In particular, on higher wind speeds the pitch angle should mainly used as control variable, while for lower wind speeds just the generator torque is used as the control variable

5.3.3 Full-wake experiment results

The collective pitch-torque control described in §5.3.1 was used for controlling two identical models tested in the civil wind tunnel section (Fig. 5.14), with the second model, here defined **WTM2**, perfectly aligned with the first one, here defined **WTM1**, and placed at a distance of around 4 diameters downstream. The regulation trajectory was traced off-line, starting from the numerical characterization of the rotor performance based on the identified polars data reported in §3.4.3. An initial tuning of the controllers gains had been performed before testing using the test bench described in §2.2.3, so that it has just been necessary a minor tuning of the controllers gains during the initial phase of the wind tunnel testing.

The wind speeds, here named U_{WTM1} and U_{WTM2} , were measured, using pitot transducer MENSOR CPT-6115 and DRUCK LPM9481, at hub height and approximately one diameter in front of both models. The test were performed at several wind speed, with U_{WTM1} varying from ≈ 4.7 [m/s] to ≈ 10 [m/s], and recordings of 30 [sec] were carried out for each wind speed. We did not perform test with **WTM1** operating in



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Figure 5.14: Wind farm layout for full-wake control test.

region II – which is estimated to extend approximately up to 4.7 [m/s], since **WTM2** would experience an extremely low wind speed and, consequently, its airfoils Reynolds would be too much lower than the nominal value.

In this range of wind speed the first model operates in region II¹/₂ from ≈ 4.7 [m/s] to ≈ 7.5 [m/s] and, as expected, the rotor speed is slightly lower than the rated value (see Fig. 5.15), in agreement with the implemented control policy. This operative region is relatively wide, specially if compared to the one of modern multi-MW wind turbines, due to the low value of the maximum region II power – in turn due to the lower model C_P^{II} – and to the fact that the model is a scaled reproduction of a 90 [m] diameter on-shore wind turbine characterized by an high value of the rated power and, at the same time, a kept down value of the maximum allowable tip speed^[33].

The plots reported in Fig. (5.15) also highlight how the trend of the wake deficit agrees with the trend of the **WTM2** power, which is equal to the rated value with a wind tunnel speed of ≈ 9 [m/s], while the **WTM2** rotor speed reaches the rated value at a wind speed of ≈ 6.3 [m/s].

It is also interesting looking at the power ratio plotted in Fig. (5.16). On the basis of what has been shown in Fig. (3.21), the aerodynamic performance of the model are influenced by the model airfoils Reynolds, in turn largely related to the rotor speed. This dependency has a significant effect on the performance of the model in region II, as highlighted by equation (3.26), where full scale turbines operate at constant TSR, pitch and C_P^{II} , but variable rotor speed. The ratio between the powers produced by the upstream and downstream wind tunnel model in conditions of full wake is, therefore, not only related to the wake deficit generated by the upstream model, as in full-scale wind farm, but also by the downstream turbine rotor speed. However, it is possible to correct the experimentally measured power ratio $\frac{P_2}{P_1}$ by the effects due to variation of



Figure 5.15: Wake deficit (on left) and comparison between the non-dimensional rotor speed and power output of WTM1 and WTM2 (on right).

the airfoils Re with the following equation:

$$\left(\frac{P_2}{P_1}\right)^{(Re-corr)} = \frac{P_2}{P_1} \left[-2.08 \left(\frac{\Omega_{WTM2}}{\Omega_r} - 1\right)^2 + 1 \right]^{-1}$$
(5.12)

where $\left(\frac{P_2}{P_1}\right)^{(Re-corr)}$ states for power ratio corrected by Reynolds effect.



Figure 5.16: Measured and corrected-by-Reynolds-effect power ratio vs. U_{WTM1}.

The corrected power ratio reported in Fig. (5.16) emphasizes that the power produced by the downstream model is around the 20% of the power produced by the upstream turbine when this latter operates in region II. This power ratio is comparable with the value measured also in field testing with low turbulence intensity, turbines perfectly aligned and spaced about three diameters^[118].

The left graphs of Fig. (5.17) reports the non-dimensional performance of the two


Figure 5.17: Comparison between the thrust coefficients C_F and power coefficients C_P of **WTM1** and **WTM2** (on left), and distribution of **WTM2** rotor speed standard deviation σ_{Ω} , power standard deviation σ_P and actuator duty cycle ADC (on right).

models, in terms of C_F and C_P , computed using the corresponding pitot sensor readings. It is interesting to note the rapid downgrading of the power coefficients as the wind speed decrease, due to the increment of the drag coefficients of the blade airfoils as the Reynolds falls off, while the thrust coefficients remain more or less unchanged being mainly related to the lift coefficients. In this condition the control also trims the machine at TSRs lower than λ^{II} , since a constant value of k_{opt}^{II} were used for relating the optimal torque to the rotor speed. The same plots highlight the good trim-ability provided by the implemented speed-control system, being the distribution of the two models power and thrust coefficients really close one to the other. The small discrepancies observed in region $II^{1/2}$ and III are probably due to the wind speed values used for the computation; indeed, the pitot transducers provide punctual flow data which probably underestimate the global wake deficit felt by the entire rotor. Moreover, small discrepancies have been noticed between the power and thrust coefficients measured within the civil test section and with the upwind model onboard sensors ($C_P^{II} \approx 0.38$), and the ones measured in the aeronautical chamber, once corrected by blockage effect $(C_P^{II} \approx 0.34)$. The reason probably still lies in the punctual flow data provided by the pitot tube and used for the coefficients computation, since it is known that, in the civil test section, the flow is not perfectly uniform along all the 14 [m] of the chamber width and spatial variations in the order of the 3-5% are expected on the mean wind speed.

The right graph of Fig. (5.17) shows the distribution, as function of the wind speed, of the **WTM2** rotor speed and power standard deviation (σ_{Ω} and σ_{P}) and of the model pitch actuators duty cycle computed as:

ADC
$$(U_{WTM2}) = \frac{1}{T} \int_0^T \frac{|\dot{\beta}(t, U_{WTM2})|}{\dot{\beta}_{\max}} dt,$$
 (5.13)

The PI control guarantees extremely low oscillations of the rotor speed in the entire operative region, while the power quality and the ADC trends are consistent with those measured on real machines. There is just an exception related to the ADC values in region II, where the pitch control is nevertheless a bit active, highlighting how the difference between the Ω_{ref} of pitch and torque controllers is probably not sufficient. In any case, this observation does not affect the goodness of the control implemented that fulfills the desired requirements even in wake conditions. The LQR control has not been tested for reasons of time but we do not expect different performance from the PI control ones, at least in terms of capacity to trim the machine at the desired reference.

In summary, the presented results show that:

- the power deficit observed in conditions of full wake and with turbines spaced 4D is similar to that observed in the field;
- both models are equipped with an excellent torque-collective pitch control that allows trimming both wind turbines at the desired values of power and rotor speed, with oscillations comparable to those measured on a full-scale machine.

Therefore, the models actually reproduce a small wind farm and can therefore become a powerful tool to be used for the development and testing of advanced control laws that aim to maximize the power extracted from the entire wind farm.

5.4 Individual pitch control

With the increase in wind turbine dimensions, the loads that a wind turbine has to withstand increase significantly, resulting in requirement for advanced wind turbine control whose aim is the loads reduction without reducing the quality of the wind turbine power output. One of such control concepts is individual pitch control^[84], which has got a lot of attention in the literature in the recent years.

In detail, IPC is an augmentation of the already described rotor speed (collective pitch) controller and is designed to reduce the wind turbine loads, specially the first harmonic of the blade loads (frequency 1P – once per revolution) by introducing additional 1P blade pitch signal, since each blade is subjected to different loads and has to be pitched individually in order to reduce these last.

Afterward, the reduction of higher loads harmonics, which can be regarded as an extension of the basic individual pitch control, has also been object of research^[44,119]. According to the author knowledge, the studies made so far utilize measures of the blade loads as feed-back signal for the control and some tests have been carried out in the field to prove their effectiveness^[88]. However, also the shaft loads can be used instead of blade loads, with the main advantage that a reduce number of onboard sensors is required and, therefore, also the probability of a sensor failure is lower. In this regard, any experimental activity related to the validation of control algorithms that use the shaft loads for the reduction of higher order harmonics is not known.

The potential offered by the wind tunnel testing return extremely useful, since it is possible to quickly explore the possibilities of reduction of the higher loads harmonics using the shaft loads measurements instead of the blade loads measurements. All this can be done with reduced costs than field testing and ensuring conditions of greater safety for the machine than those that can be ensured in the field experimentation. In fact, one can turn off quickly the wind tunnel to stop the wind in case of undesired operation of the control system, which is unfeasible in full scale condition. On the model wind turbine an augmented IPC has been therefore implemented which, besides the first harmonic, also reduces the second harmonic using, as feedback signals, the shaft loads measurements instead of the blade loads measurements.

5.4.1 Wind turbine loads propagation

In a wind turbine, the main sources of blade loads are aerodynamic effects, gravity and inertial loads, where aerodynamic and gravity loads have very pronounced oscillatory behavior caused by the rotor rotation. These loads do propagate to the rest of wind turbine, and it is an immediate consequence than their reduction involves considerable advantages in terms of lowering the loads acting on all the other wind turbine components.

Looking to the hub-rotor system, the blade loads do propagate into rotating hub loads as follows:

$$M_{YR} = \sum_{i=0}^{2} M_{YB}^{i} \cos \frac{2\pi}{3} i - \sum_{i=0}^{2} M_{ZB}^{1} \sin \frac{2\pi}{3} i,$$

$$M_{ZR} = \sum_{i=0}^{2} M_{ZB}^{i} \cos \frac{2\pi}{3} i + \sum_{i=0}^{2} M_{YB}^{i} \sin \frac{2\pi}{3} i,$$
(5.14)

where M_{YB}^i and M_{ZB}^i are the *i*th blade loads expressed in the coordinate system of Fig. (2.14(b)), while M_{YR} and M_{ZR} are resulting loads in the rotating hub coordinate system (see Fig. 2.14(a)).



Figure 5.18: Fixed hub coordinate system (Source: Germanischer Lloyd^[1]).

Since the fixed hub coordinate system and the rotating hub coordinate system overlap when the first blade is positioned vertically up (rotor azimuth $\psi = 0$), the loads in the fixed hub coordinate system can be expressed as:

$$M_{YN} = M_{YR} \cos \psi - M_{ZR} \sin \psi,$$

$$M_{ZN} = M_{YR} \sin \psi + M_{ZR} \cos \psi.$$
(5.15)

where M_{YN} and M_{ZN} do refer to Fig. (5.18) and the rotor azimuth angle is defined as positive when rotating in the clockwise direction, in agreement with the wind turbine sense of rotation.

Note that (5.14) and (5.15) do not describe the complete loads on the hub, but only the propagation of the blade loads to the hub. Indeed, there are additional load components on the hub, but since they are not of the main interest for the purpose of this work, they are not written here.

5.4.2 Basic individual pitch control

The main objective of the individual pitch control is the reduction of the first harmonic of the blade loads, that involves the reduction of all the loads acting on rotating wind turbine components, together with the alleviation of the mean value of the loads on the fixed wind turbine components (i.e. nacelle or tower).

The individual pitch control (IPC) as described in Bossanyi^[84] is based on d-q transformation (also known as Coleman or Park's transformation in the literature) of the blade loads:

$$M_{d} = \frac{2}{3} \sum_{i=0}^{2} M^{i} \cos \psi^{i},$$

$$M_{q} = \frac{2}{3} \sum_{i=0}^{2} M^{i} \sin \psi^{i},$$
(5.16)

where M^i is a generic load and ψ^i the azimuth position of the i^{th} blade, while M_d and M_q are loads in d-q coordinate system. Such transformation extracts the information about the first blade loads harmonic, i.e. the mean value of the transformed loads represents the amplitude of the first harmonic of the blade loads^[120]. Once extracted the loads in the d-q coordinate system, two controllers are used to reduce the mean value of the transformed loads. Subsequently, references pitch for the three blades are obtained by adding the inverse d-q transformation of the controllers outputs to the collective pitch reference β_{ref} defined by the wind turbine rotor speed controller:

$$\beta^{i} = \beta_{ref} + \beta_d \cos\left(\psi + \frac{2\pi}{3}i\right) + \beta_q \sin\left(\psi + \frac{2\pi}{3}i\right), \qquad (5.17)$$

where β_d and β_q are blade pitch references in the d-q coordinate system and β^i is the blade pitch reference of the *i*th blade.

Note that the d-q transformation (5.16) is very similar to the propagation of the blade loads to the fixed wind turbine components. Therefore, also the loads acting on the fixed wind turbine components can be used for IPC instead of the transformed blade loads, as suggested by Bossanyi^[84]. For instance, loads from fixed hub coordinate system can be used for individual pitch control, where the mean of such loads corresponds to the amplitude of the first harmonic of the blade loads.

Since our purpose is to demonstrate that shaft measurements can be used for reducing loads higher order harmonics, it is quite clear that transformation of the measurements to fixed shaft coordinate system by (5.15) would be convenient for the 1-rev loads suppression but not for the reduction of the higher harmonics, so a different approach has to be used.

5.4.3 Loads transformation suitable for reduction of higher loads harmonics

In this subsection, frequency components of the shaft loads are analyzed and the loads transformation is proposed. Only deterministic blade load sources (such as the gravity, wind shear, tower shadow) are considered, which produce harmonic loads in steady state. Stochastic effects (such as wind turbulence, wind gusts) can be regarded as control disturbances and therefore they are not directly considered in this analysis. Furthermore, it is assumed that all the rotor blades are identical; although this is generally a valid assumption, note that any difference between the blades will only generate additional harmonic loading that the controller will try to compensate.

Since the blades are equally spaced with $\frac{2\pi}{3}$ between each two blades, the deterministic loads are identical up to phase offset of $\frac{2\pi}{3}$ and can be expressed as harmonic signals with the first harmonic on the frequency 1-rev or 1P:

$$M_{YB}^{i} = \sum_{k=0}^{\infty} M_{Y}^{(k)} \cos\left(k\psi + \phi_{Y}^{(k)} + \frac{2\pi}{3}ki\right),$$

$$M_{ZB}^{i} = \sum_{k=0}^{\infty} M_{Z}^{(k)} \cos\left(k\psi + \phi_{Z}^{(k)} + \frac{2\pi}{3}ki\right),$$
(5.18)

where $M_Y^{(k)}$, $M_Z^{(k)}$, $\phi_Y^{(k)}$ and $\phi_Z^{(k)}$ are amplitudes and phase angles of the k-th harmonic of the blade loads.

To examine the frequency components of the rotating hub loads, the blade loads expressed in (5.18) are transformed into rotating hub coordinate system by means of (5.14). For clarity, only propagation of the blade loads M_{YB}^i is written, i.e. as if $M_{ZB}^i = 0$; it is easy to check that propagation of the M_{ZB}^i would have the same characteristics. The propagation of M_{YB}^i into rotating hub coordinate system can be written as:

$$M_{YR} = \frac{3}{2} \sum_{k=0}^{\infty} M_Y^{(3k+1)} \cos\left[(3k+1) \psi + \phi_Y^{(3k+1)} \right] + \frac{3}{2} \sum_{k=0}^{\infty} M_Y^{(3k+2)} \cos\left[(3k+2) \psi + \phi_Y^{(3k+2)} \right],$$
(5.19)
$$M_{ZR} = -\frac{3}{2} \sum_{k=0}^{\infty} M_Y^{(3k+1)} \sin\left[(3k+1) \psi + \phi_Y^{(3k+1)} \right] + \frac{3}{2} \sum_{k=0}^{\infty} M_Y^{(3k+2)} \sin\left[(3k+2) \psi + \phi_Y^{(3k+2)} \right].$$

It is clear from (5.19) that multiples of the third blade loads harmonic are not present in rotating hub loads; therefore these harmonics can not be reduced using the shaft loads. However, if these harmonics were present it would be, in any case, not possible to reduce the same using the approach here described, since the pitch action on multiples of the third harmonic would interfere with the wind turbine power control.

All the other blade loads harmonics can be clearly seen in shaft measurements and it is possible to view each harmonic in the rotating hub coordinate system as a rotating vector. Therefore, the amplitude of each blade loads harmonic can be extracted

by transforming measured shaft loads into a coordinate system that is rotating with the same speed as the observed harmonic with respect to the rotating hub coordinate system. In such coordinate system, the vector of the transformed harmonic is stationary, which enables us to easily use it for individual pitch control, as it will be shown later.

Also note that loads harmonics rotate in different directions. Obviously, the harmonics 1P, 4P, 7P... rotate from ZR axis to YR axis, i.e. harmonic 3k + 1 has rotating speed of $-(3k + 1)\dot{\psi}$ with respect to the rotating hub coordinate system. On the contrary, the harmonics 2P, 5P, 8P... rotate from YR axis to ZR axis, i.e. harmonic 3k + 2 has rotating speed of $(3k + 2)\dot{\psi}$ with respect to the rotating hub coordinate system. Therefore loads transformation can be defined as:

$$\boldsymbol{T}_{L}^{(N_{P})} = \begin{bmatrix} \cos N_{P}\psi & \sin N_{P}\psi \\ -\sin N_{P}\psi & \cos N_{P}\psi \end{bmatrix},$$
(5.20)

where N_P represents the rotation speed of the harmonic with respect to rotating hub coordinate system, expressed as the multiple of the rotor speed $\dot{\psi}$. In other words, for harmonic 3k + 1, transformation matrix $T_L^{-(3k+1)}$ should be used, while for harmonic 3k + 2, transformation $T_L^{(3k+2)}$ should be used.

It is clear that for the first harmonic, the transformation matrix T_L^{-1} actually transforms the loads from rotating hub to fixed hub coordinate system in agreement with (5.15) – note that in fact fixed hub coordinate system is rotating relative to the rotating hub coordinate system with speed $-\dot{\psi}$. Since it is known that loads from the fixed wind turbine parts can be used for reduction of the first harmonic of the blade loads, the proposed transformation (5.20) is obviously valid for the reduction of the first harmonic.

To show that the transformation works for other harmonics as well, we apply it to the rotating hub loads (5.19) and calculate the mean value:

$$\frac{1}{2\pi} \int_{0}^{2\pi} T_{L}^{(N_{P})} \begin{bmatrix} M_{YR} \\ M_{ZR} \end{bmatrix} d\psi = \\
= \begin{cases}
\begin{bmatrix} \frac{3}{2} M_{Y}^{(N_{P})} \cos \phi_{Y}^{(N_{P})} \\ \frac{3}{2} M_{Y}^{(N_{P})} \sin \phi_{Y}^{(N_{P})} \end{bmatrix}, & N_{P} = 2, 5, 8 \dots \\
\begin{bmatrix} \frac{3}{2} M_{Y}^{-(N_{P})} \cos \phi_{Y}^{-(N_{P})} \\ -\frac{3}{2} M_{Y}^{-(N_{P})} \sin \phi_{Y}^{-(N_{P})} \end{bmatrix}, & N_{P} = -1, -4, -7 \dots \\
\begin{bmatrix} 0 \\ 0 \end{bmatrix}, & \text{otherwise}
\end{cases}$$
(5.21)

The mean values of the transformed loads of (5.21) clearly show that transformation (5.20) can extract the amplitude of all harmonics from the shaft measurements except the multiples of the third harmonic. It can also be seen that the right rotating direction has to be used for each harmonic.

5.4.4 Proposed control algorithm

Using transformation (5.20), the control algorithm for the reduction of higher loads harmonics based on shaft measurements can be implemented, and its basic structure is shown in Fig. (5.19). First, the shaft loads are transformed with appropriate transformation (5.20), then filtered and fed to two PI controllers which try to bring the mean



Figure 5.19: Controller for the reduction of N^{th} loads harmonic.

value of the transformed loads (and hence the amplitude of the observed harmonic) to zero.

Since the amplitude of the observed harmonic is converted into mean value in the transformed coordinate system, low-pass filter is used to suppress transformed load oscillations caused by other harmonics. The PI controllers generate the pitch references in the transformed coordinate system, but before they can be transformed into actual blade pitch references, decoupling has to be made. Namely, the d-q components of the pitch actuation and load are typically coupled and the amount of coupling is caused, among the other, by the blade pitch actuator dynamic, the dynamical system response, the delay due to real time acquisition and computation which could be not negligible in case of high sample time compared to rotor speed^[121]. To correct this effect, which could degrade performance, a steady state coupling model was experimentally identified by recording load harmonics caused by prescribed harmonic variations of blade pitch, and then used for decoupling. After the decoupling, the blade pitch references are obtained by using the inverse d-q transformation:

$$\beta^{i(N_P)} = \beta_d^{(N_P)} \cos\left(N_P \psi + \frac{2\pi}{3} N_P i\right) + \beta_q^{(N_P)} \sin\left(N_P \psi + \frac{2\pi}{3} N_P i\right), \quad (5.22)$$

where $\beta^{i(N_P)}$ is the signal to be added to the *i*th blade reference signal in order to reduce the N_P^{th} harmonic. Therefore, the final blade pitch reference is obtained by adding one term for each harmonic to the collective blade pitch reference:

$$\beta^i = \beta_{ref} + \sum \beta^{i(N_P)}.$$
(5.23)

Note that the controller shown in Fig. (5.19) has the goal of reducing only one harmonic and, obviously, the number of such controllers has to be equal to the number of harmonics that is to be reduced. Despite the control has as objective the reduction of its target harmonic, non negligible effects on both higher and lower harmonics

are expected, due to the periodicity of the system^[122]. However, the reactions of the controllers to the effects the same produce at higher harmonic have been considerably limited by low-pass filtering the transformed loads.

It is important to remark that, since the multiples of the third harmonic are not being reduced, the described controllers for load reduction can not change the collective pitch angle and therefore do not not interfere with the wind turbine power control, being:

$$\frac{1}{3}\sum_{i=0}^{2}\beta^{i} = \beta_{ref}.$$
(5.24)

5.4.5 Experimental results

The implemented individual pitch controller was tested in the civil wind tunnel section, and the PI based controller described in §5.3.1 was used for controlling the model collective pitch and torque.

The effectiveness of the implemented controllers was tested with the model operating in region III, at uniform wind speed of ≈ 9 [m/s] and turbulence in the order of $\approx 2\%$, and with the model operating in region II¹/₂, at wind speed ≈ 6.5 [m/s], but in condition of not uniform flow. Indeed, region II¹/₂ test were performed using two models placed as showed in Fig. (5.14), but with the **WTM2** rotor center shifted laterally at a distance of half diameter from the **WTM1** rotor center (this condition is here named half-wake case). In this way it is expected that **WTM2** is subjected to loads with consistent 2P harmonics content, thus making it possible to verify the implemented controller effectiveness.

The maximum pitch amplitudes were fixed based on results report in §2.2.2.3, while the PI IPC controllers gains were independently set during the test in order to guarantee the stability of the system; in particular, small values were used for the proportional gains, given the lack of change of the mean wind flow during the test and in accordance with Bossanyi^[84].



Figure 5.20: Performance of the collective and IPC controllers for the wind turbine operating in region III.

The results presented in Fig. (5.20) clearly show that the implemented individual pitch control is properly working with model operating in region III, being the first loads

harmonic reduced by more than 80%. In particular, the figure shows the harmonics amplitude 0P-3P of the shaft load magnitude, computed as $\sqrt{M_{YR}^2 + M_{ZR}^2}$, the blade pitch and the power for three different cases: without IPC (IPC OFF), with IPC whose target is the reduction of the first harmonic (IPC – 1P), and with IPC whose target is the reduction of both first and second harmonics (IPC – 1P2P). It is self-evident that both IPC algorithms work in the same way, i.e only reducing the first harmonic. Indeed, being mainly the gravity and the rotor conicity the only source of periodic excitation, the amount of higher loads harmonics is not appreciable.

Looking at the results obtained for the half-wake case (Fig. 5.21) it is immediately clear that the shaft is excited by loads whose harmonic content is richer than that observed for the previous test, and, therefore, the condition is extremely useful to assess the ability of the implemented individual pitch control to reduce both the first and second loads harmonic, as widely highlighted in the figure.



Figure 5.21: Performance of the collective and IPC controllers for the downstream wind turbine operating in region II¹/2 and in half-wake interference conditions.

It is also possible to note that both results here presented point out no significant influence of the IPC algorithms on the power (i.e. rotor speed) control. Also, as the loads are reduced, increased pitch activity can be observed on the first and the second harmonic, as observed on real wind turbines^[88], which demonstrates that the designed model can be suitable used for testing individual pitch control algorithms.

Another measure of the increased pitch activity caused by the individual pitch control is also the actuator duty cycle computed with (5.13) and shown in Fig. (5.22). It is important to remark that region III measurements were recorded with the wind turbine operating in uniform flow condition; furthermore, the loads for this condition are lower than the half-wake case ones and the increase of the ADC in region III is obviously lower than the same in region II¹/₂.

To stress even more the IPC effectiveness, shaft loads and blade pitch angles are plotted in Fig. (5.23) for the half-wake case. At the beginning, IPC for the first two harmonics (IPC – 1P2P) is active; then in the middle of the figure, the reduction of the second harmonic is deactivated, while the reduction of the first harmonic is still active (IPC – 1P). Finally, the last part of the figure shows only collective pitch control (IPC OFF). It can be clearly seen how the amplitude of the loads rises as the individual



Figure 5.22: Actuator duty cycle for both IPC experiments.

pitch control is progressively deactivated and how, in the mean while, blade pitch becomes less active as long as, with only collective pitch control active, all the blades are identically pitched.



Figure 5.23: Normalized shaft loads and blade pitch response to different control algorithms.

The results show the potential offered by the wind tunnel testing with the designed wind turbine models, since it has been possible to prove quickly and cost-effective the effectiveness of the proposed approach for the reduction of the higher order loads harmonics.

5.5 Wake measurements

In the design process of a wind farm the aerodynamic interactions between the single turbines have become a field of major interest. Indeed, the upwind wind turbines substantially affect the inflow conditions of downstream turbines, resulting in a considerable reduction on the amount of produced energy as well as in an increased fatigue due to vibrations induced by wake turbulence. A lot of effort has been done in the past years to increase the ability of simulation codes in predicting the wake evolution and much about this topic can be found in Crespo et al.^[123] and Sanderse et al.^[124]. However, the main problem related to the validation of the numerical methods is the difficulty of obtaining reliable experimental data to be used for comparison.

Few field experiments were directed towards wake measurements^[5], but flow conditions are generally not fully known due to wind shear, turbulence, orography, thermal stability, changes in wind direction and many other issues. For this reason, it is clear that validating numerical models through comparison with field measurements may be questionable, given that any observed agreement or discrepancy could be somewhat coincidental.

In this respect, wind tunnel studies are preferable since the flow conditions may be specified at all levels required to set up a proper simulation, even though the models operate at much lower Reynolds numbers than found at full scale. Indeed, the Reynolds number of the atmospheric flow cannot be matched by wind-tunnel experiments, but, although the influence of Reynolds on turbine wakes is not fully understood, previous studies have shown that primary wake characteristics, such as wake deficit, rotation and tip vortices, can be reproduced even at relatively low Reynolds^[15]. This highlights the value of wind turbine wake data acquired in wind tunnels for the purpose of understanding the fluid dynamics and, as long as the aerodynamic characteristics of the rotor blades are known in the modeled Reynolds number range, to provide valuable benchmarks for comparison and validation of numerical models.

Looking at the literature, we get that most wind tunnel experiments involve the use of stationary models (e.g., porous disks), as summarized in Vermeer et al.^[5], while few experiments have been conducted using rotating models. However, even in these latter case^[15,17] the models tested in the wind tunnel only partially represent a full scale multi-MW wind turbine, since the tests are carried out with TSRs much lower than those proper of a real machine^[125]. As a consequence, it is not possible, for example, to faithfully reproduce the phenomena affecting the transition region between the near wake and the far wake, where the vortex system undergoes a transition from organized flow structures to a fully turbulent flow, with resulting uncertainties in the prediction of the loads acting on downstream wind turbines. Indeed, it is clear that if a wind turbine is located in a wake consisting of stable tip vortices it will be more severely loaded than if the vortices break down by instability^[34]. Also in the case where the tests are performed with models operating at high TSRs^[13,16], it has been seen^[126] that the comparison between the experimental wind tunnel data and the predicted wake evolution is rather poor, which means that we are quite far from having reached the reliability that is necessary to faithfully estimates the wake-turbine interaction.

The foregoing highlights the need for further tests in the wind tunnel, which can provide additional data for the fine tuning of simulation codes. As stated in Vermeer

et al.^[5], the experiments has to be performed in controlled conditions, as well as appropriate airfoils have to be chosen and their characteristics have to be well known at low Reynolds; finally, the model to tunnel area ratio has to be taken into account, since the performance of the rotor are influenced by the fact that the wake has or has not the possibility to expand. The wind tunnel environment object of this thesis is therefore congenial to this kind of activity. In fact, a traversing system which allows the accurate, automatic and fast mapping of the flow within the wind tunnel has been developed, as well as the designed wind turbine models blades are equipped with profiles developed for low Re applications, whose aerodynamic characteristics have been conveniently calibrated taking into account the blockage effect due to the wind tunnel walls. Furthermore, the aerodynamic performance of the models are representative of those of a real machine, both in terms of power coefficients, thrust coefficients and optimal tip speed ratio.

An extensive test campaign has been then conducted to measure the evolution of the wake generated by a single model tested in in the aeronautical section of the wind tunnel. In addition, several tests were conducted in the civil section with uniform flow, i.e. without reproduction of the the atmospheric boundary layer, and constant rotor speed and pitch, in order to measure the interaction between the wake generated by the upstream model and the aerodynamics of the twin model positioned downstream. The results were provided to the company that sponsored the project in order to contribute to the validation of their simulation codes based on LES combined with actuator line/disk techniques. Here only a few graphs that describe qualitatively the evolution of the wake are reported, while no comparison between the numerical and experimental data will be reported since all data are covered by a non-disclosure agreement.

5.5.1 Single wake measurements

From a wind engineering perspective, there are two characteristics of wind turbine wakes that are of considerable practical interest: the velocity deficit, which significantly reduces the power extracted by downstream wind turbines in a wind farm arrangement, and the turbulence levels, which may affect flow-induced rotor loads on other turbines located downwind in a wind park. These metrics have been measured at four different downstream positions and with the model operating at optimal condition. The flow field has been traversed in a half-circle including a total of around 300 measuring points.



Figure 5.24: Reference frame adopted for describing the wake measurements.

The reference frame UVW used for describing the wake measurements is shown in Fig. (5.24), with U, V and W respectively the wake speeds measured along the longitudinal, vertical and lateral direction, as well as Y and Z are the vertical and lateral position of the wake measurements points, represented with white dots also in all the following figures.



Figure 5.25: Downstream development of the averaged axial velocity (a) and turbulence intensity (b) in the wake of the wind turbine model operating in uniform flow and at optimal C_P^{II} . White dots represent the measuring points.

(b) Turbulence intensity

1000

Z [mm]

500

1500

0

1000

Z [mm]

500

1500

1000

Z [mm]

1500

500

C

-1500

0

1000

Z [mm]

500

1500

Fig. (5.25(a)) shows that, moving axially downstream, the velocity deficit in the wake gradually recovers and the turbulence intensity slowly decrease. The wake deficit is characterized by evident axial-symmetry, while the tip vortices can be clearly observed looking at the distribution of the turbulence intensity. The presence of the tower

causes a bluff body wake characterized by an additional velocity deficit which is transported toward the center of the rotor by the considerable root vortex, which is also the cause of the turbulent swirl observed in the region around the rotor axis.



Figure 5.26: Measured profiles of the time averaged axial velocity and tangential velocity in a vertical plane going through the rotor center axis. The wind turbine model operates in uniform flow and at optimal C_P^{II} .

Fig. (5.26) shows the measured profiles of the time averaged axial velocity and tangential velocity in a vertical plane going through the rotor center axis. A gentle expansion of the wake can be observed moving downstream, while significant tangential components, that cause the wake to counter-rotate in respect to the clock-wise direction of rotation of the turbine rotor and in reaction to the torque exerted by the flow on the rotor, are clearly visible and show, also, typical axial-symmetric behavior.

The measured wake data have been used to estimate the rotor power and thrust coefficients using the following equations based on the conservation of momentum:

$$C_F^{\text{wake}} = \frac{\int_{S_{\text{wake}}} \left(1 - \frac{U^2}{U_w^2}\right) dS}{\frac{\pi}{4}R^2}$$
(5.25a)

$$C_P^{\text{wake}} = -\frac{\int_{S_{\text{wake}}} \frac{U}{U_w} \left(\frac{V}{U_w}y - \frac{W}{U_w}y\right) dS}{\frac{\pi}{4}R^3} \frac{\Omega R}{U_w}$$
(5.25b)

where S_{wake} is the wake area defined as in Fig. (5.27), while dblquad function of Matlab have been used for the computation of the integrals starting from the punctual measures. It is important to remark that only the available data related to half wake are used in (5.25), that justifies the use of $\frac{1}{4}$ instead of $\frac{1}{2}$ in the denominator. Table. (5.2) shows the comparison between the measured power and thrust coefficients, derived from the torque-meter and the tower root balance measurements and adjusted of wall blockage effect, and the ones reconstructed from the wake measurements. The modest percentage errors clearly highlight the goodness of the measurements carried out with

the hot-wire probes and provide evidence of the gradually recover of the velocity deficit in the wake due to turbulent diffusion from the surrounding flow.



Figure 5.27: Definition of the wake area.

Table 5.2: Comparison between the power and thrust coefficients measured with the torque-meter and the balance and the ones reconstructed from the wake measurements.

Plane	C_P [-]	C_P^{wake} [-]	$\Delta C_P [\%]$	C_F [-]	C_F^{wake} [-]	ΔC_F [%]
1D	0.3404	0.325	-4.52	0.635	0.621	-2.20
1.5D	-	0.346	1.65	-	0.616	-2.99
2	-	0.345	1.35	-	0.587	-7.56
2.5D	-	0.304	-10.69	-	0.572	-9.92

5.5.2 Double wake measurements

The main focus of this experimental study has been to investigate the local velocity deficit and the turbulence intensities in the wake between and behind an array of two identical model wind turbines. The wake is scanned at three different downstream positions and for three different turbine layouts, i.e with the downstream model feeling conditions of full-wake, half-wake and out-of-wake.

5.5.2.1 Full-wake turbines layout

The full-wake test has been carried out placing the second model perfectly aligned with the first one and 4D downstream, as shown in Fig.(5.28). The flow field has been traversed in a circle including a total of 192 measuring points, while the measures have been acquired at three axial positions, i.e. 1D and 2D upstream and 2D downstream of the second turbine. The wind tunnel inflow speed was approximately 6.5 [m/s], which means that both turbines were operating in region II¹/₂ and at rated rotor speed. The collective blade pitch of both models have been defined in order to have a power ratio equal to approximately 0.5, in agreement to the results reported in Fig. (5.15), in order to experimentally reproduce a typical situation observed in full scale wind farm. Is is important to remark that it has not been possible to reproduce a condition in which the upstream machine operates at optimal C_P , since this would have led to operate

the downstream model at too low Re (see §5.3). In this case, in fact, the numericalexperimental comparison would be falsified, as it is believed that the identified polar are correct only if the airfoils operate at their nominal Re.



Figure 5.28: *Experimental setup and axial measurement stations for full-wake turbines layout. The in-plane measured wake velocities are also shown.*

In Fig. (5.29(a)) the axial development of the wake deficit is reported. Looking at the wake generated by the upstream model, we can observe that it is shifting upward and that the horizontal expansion is more pronounced in comparison to the vertical expansion, probably due to the effect produced by ceiling and floor boundary layer, while the deficit velocity 2D downstream the second model is found to be influenced by the wake rotation, whit this latter highlighted by Fig. (5.28). The turbulence intensity behind the second wind turbine is found to be significantly higher than that observed behind the upstream turbine, as shown in Fig. (5.30), and one core of very high turbulence intensity level can be found slightly to the right and below the rotor center. It is very likely that this uneven distribution is due to the wake rotation. This aspect stresses, therefore, the importance of using rotating models instead of porous disks in order to be able to fully capture the physics governing the evolution of the wake and the interaction wake-turbine.

5.5.2.2 Out-of-wake turbines layout

The out-of-wake test has been performed using two models positioned as showed in Fig. (5.30), i.e. with the downstream rotor shifted laterally with respect to the upstream turbine in order to have an offset equal to one diameter. The wind tunnel inflow speed was approximately 6.5 [m/s] and both models have been trimmed at the same rotor speed and collective pitch on the basis of the regulation trajectory reported in §5.3.3.

The measured power coefficient developed by the downwind rotor that is offset in the crosswind direction by one rotor diameter has been found to be around 15% higher than the measured power coefficient that is developed by the upstream rotor, although the two models are identically trimmed.



Figure 5.29: *Full-wake interaction: downstream development of the averaged axial velocity (a) and turbulence intensity (b) in the wake 1D and 2D upstream and 2D downstream of the second turbine.*



Figure 5.30: *Experimental setup and axial measurement stations for out-of-wake turbines layout. The in-plane measured wake velocities are also shown.*



(b) Turbulence intensity

Figure 5.31: *Out-of-wake interaction: downstream development of the averaged axial velocity (a) and turbulence intensity (b) in the wake 1D and 2D upstream and 2D downstream of the second turbine.*

In Fig. (5.31(a)) the axial development of the wake deficit is reported. It can be seen as the impingement of the upstream rotor wake on the downwind turbine is largely, but not entirely, eliminated when the offset between the rotor axes of rotation is around one diameter. Indeed, the wake of the upwind rotor induces a change in the local wind velocity at the downstream turbine, yielding the 15% of increase in its power coefficient. It is clear also that there is a distortion of the wake generated by the upstream model, which is shifted laterally and upward, due to the presence of the downstream rotor. It can also be observed that the upstream rotor wake is visibly broader at a distance of 6D from the rotor, as well as the area of high velocity deficit shrinks since fluid of higher kinetic energy is slowly transported into the center of the wake due to turbulent diffusion. The latter is also the cause of the smoother gradients in turbulence intensity observed in Fig. (5.31(b)) at 6D downstream the upwind model.

5.5.2.3 Half-wake turbine layout

Interactions where approximately half of the downwind rotor disc is affected by impingement from a wake generated upwind are referred to as half-wake interaction. This condition has been reproduced in the wind tunnel by laterally shifting the downstream rotor with respect to the upstream turbine in order to have an offset equal to half diameter. The wind tunnel inflow speed was approximately 6.5 [m/s]. The two turbines have been trimmed as in IPC test of §5.4.5: the upstream model has been trimmed as in the previous tests performed to evaluate the wake evolution, while the torquecollective pitch control described in §5.3.1 has been used to identify the optimal trim of the downwind model.

High oscillations have been measured in the downwind loads because the wake developed by the upwind rotor impinges on approximately only half of the downwind rotor disc, in agreement to what reported in Fig. (5.23) with IPC control turned off.

When the two turbines are laterally displaced, Figs. (5.32(a)) and (5.32(b)) reveal a complicated interaction of the two wakes. Since both wakes rotates in the counterclockwise direction, when seen from upwind, the maximum deficit region related to the downstream wake is turned upwards and to the right by the rotation of the upstream wake. It is also noted a strong interaction between the eddies of the root, as highlighted by the distribution of the turbulence intensity over both rotor disks.



(a) Wake deficit



(b) Turbulence intensity

Figure 5.32: *Half-wake interaction: downstream development of the averaged axial velocity (a) and turbulence intensity (b) in the wake 1D and 2D upstream and 2D downstream of the second turbine.*

CHAPTER 6

CONCLUSIONS

The thesis has described a novel experimental facility designed to expand the scope of wind tunnel testing of wind turbines beyond the domain of aerodynamics. This has been motivated by the fact that simulation is the key enabler of the design and optimization of wind energy systems, while model validation and calibration is the key enabler for ensuring that the predictions generated by simulation models are reliable. Although wind tunnel testing cannot exactly reproduce full-scale conditions nor can it substitute field-testing, it may nonetheless play an important role in the validation/tuning of models and in the evaluation of new concepts and ideas. By this work, a step towards building a better understanding of the full potential of wind tunnel testing in these new application areas has been made.

A novel aeroelastically scaled model has been then described, featuring a comprehensive onboard sensorization and active individual pitch and torque control. The latter are controlled by a hard-real-time module implementing a supervisor of the machine states and pitch-torque control laws, similarly to what is done on a real wind turbine. Several laboratory tests have shown the accuracy and reliability of the measurements provided by the onboard sensors as well as the model ability to render the principal dynamic effects due to the servo actuators.

The model shows a realistic energy conversion process in terms of reasonable power and thrust coefficients. This have been achieved despite the severe mismatches in Reynolds number and thanks to the use of suitable airfoils equipped with devices that improved their efficiency. An identification tool based on reverse-BEM has been used both for improving the rotor aerodynamic performance and to calibrate the aerodynamic parameters of aero-servo-elastic simulation codes and 3D Navier Stokes solvers combined with actuator line/disk techniques.

The thesis also presented a technological process suitable for the production of

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aeroelastic blades that, by featuring bend-twist coupling, are intrinsically capable to alleviate the loads on the various wind turbine sub-components. Due to time constraints, it has not been possible to complete the task, i.e. to experimentally ascertain the synergistic effect of active (IPC) and passive (BTC) control techniques in terms of loads mitigation with modest increase of actuator duty cycle. However, in the thesis it has been amply demonstrated that the developed manufacturing technology permits to produce aero-elastic blades with a sufficient amount of bend-twist coupling, while numerical simulations highlighted that a loads reduction similar to the one observed with full scale simulations can be achieved.

The model has been used for conducting a number of non-aerodynamic and nonstandard experiments, which have included open and closed-loop control, and the validation of control-support technologies. In particular, the accurate blade root loads measurements have been used to confirm the good quality of the estimates provided by a novel wind direction observer, in support to the results obtained with an extensive simulation campaign. Furthermore, the model have been used to study the effects that optimized shutdown procedures can have on design-driving loads; in this regard, the presented results serves the purpose of illustrating the process of validation/calibration of the numerical model parameters from wind tunnel test data, so as to ensure an appropriate level of fidelity for the verification of load reduction capability of the proposed innovative shut-down maneuvers.

Also the capability of actively controlling the model, in the same way a real wind turbine is, have been widely demonstrated by testing in the civil wind tunnel. It has been shown, first, that the collective pitch and torque control implemented within the model real-time hardware is capable of trimming the machine throughout its entire operating envelope, with good power output and power quality also when the model operates in wake interference conditions. This ability, coupled with the experimental evidence that the wake produced by the model is realistic, both in terms of geometry, velocity deficit and turbulence intensity, is one of the unique characteristics of the presented experimental facility, since it enables the study of wind turbine interactions and the testing of wind farm control algorithms. Indeed, even only two turbines are enough to captures the essence of the couplings that take place within a wind farm, i.e. the reduced speed and increased turbulence experienced by the downstream machine, as well as the dynamic changes of these parameters that follow from a change in the trim set point of the upstream wind turbine. All this allows for a rapid, low cost and high fidelity testing of control algorithms, a capability that will be exploited in the near future.

The alleviation of loads by the use of individual pitch control have also been considered. The reduction of the higher loads harmonics using the shaft loads measurements instead of the blade loads measurements has been quickly explored with dedicated testing activities in the civil test section. Individual pitch control targeting 1P and 2P loads have been implemented within the model real-time hardware and successfully tested using two models in half-wake interference conditions, obtained by laterally displacing of about one rotor diameter the downstream wind turbine with respect to the upstream one. The highly non-uniform flow conditions experienced by the downstream machine induced loads with a wide spectrum, which made it possible to verify the performance of the IPC algorithms. The reported results clearly highlight the potential offered by the wind tunnel testing with the designed model, since the effectiveness of the higher harmonic IPC featuring shaft load feed-back has been quickly and cost-effective proved on a physical system, rather than only in a simulation environment.

The models were also tested for strictly aerodynamic purposes. Indeed, an extensive test campaign has been conducted with the aim of measuring the wake evolution and its interaction with the aerodynamics of the twin model positioned downstream. The test results, provided to the company that sponsored the project, have contributed to the validation of LES simulation codes combined with actuator line/disk techniques. It is also important to emphasize that the aerodynamic properties of the actuator line/disk have been suitably calibrated on the basis of the rotor performance measured in the aeronautical test section and corrected by tunnel wall effects. The comparison between the numerical and experimental data has not been shown in this thesis since the results are covered by secrecy, but it is possible to state that the obtained data proved to be extremely valuable.

All the results reported in this thesis clearly prove that the designed experimental facility is capable of effectively supporting the applications areas it was designed for, exhibiting a good robustness and availability throughout many hours and days of testing. However, it should be highlighted that the models have the potential for further expansions in other application areas. In addition to the studies mentioned above about the wind turbine interactions and wind farm control, another direction of future development is in the area of off-shore wind, where important aeroelastic, stability and control problems come from the coupling of the machine with a floating support structure. To enable such applications, one can mount the present model on an actuated moving platform, whose motion is either prescribed or co-simulated on the basis of a suitable model of the "wet" part of the machine, or directly mount the model on a floating platform for testing in a wave tank.

In the first case, the test can be carried out in the civil test section and the tower base can be controlled according to a mathematical model of the floating platform. In this way, "dry" tests of floating wind turbines can be carried out with realistic energy conversion process, while the mathematical model of the floating platform could be calibrated and tuned on the basis of experimental data obtained by testing, in a tank, scaled model of the sole platform. It is clear that the correct reproduction of all the aeroservo-hydro-elastic interaction phenomena would require the use of 6 d.o.f. moving platforms featuring adequate tower base displacements, velocities and accelerations. However, valuable results can be obtained with lower costs and using 2 d.o.f. platforms, so as to provide the tower base with surge and pitch movements (as shown in Fig. 6.1) or sway and roll movements. In the first case, the stability problems that occur when a turbine with very low natural frequencies is combined with a traditional pitch controller can be investigated by exploiting the model pitch-torque control capability. In fact, it is well known^[127] that possible controller-induced instabilities can arise due to the changes in wind speed induced by large nacelle tilting; thus, the effectiveness of several adaptations of the wind turbine control system that aim to avoid instability could be quickly and cost-effective tested with the described experimental facility. Also the use of 2 d.o.f. platforms featuring sway and roll movements could provide better insight of the side-to-side instability phenomena highlighted in Jonkman and Matha^[128].

The author is fully convinced that, in addition to the future activities here briefly described, the unique capabilities offered by the experimental facility object of this

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thesis will be precious in many other exciting applications in the near future.

Figure 6.1: Subsequent frames of the model mounted, within the wind tunnel, on an actuated 2 d.o.f. moving platform featuring surge and pitch displacements.

APPENDIX \mathcal{A}

Characterization of materials mechanical properties

Composite material properties depend upon the resin-to-fiber volume ratio, fiber type and resin type. Prediction of the material properties of a composite laminate from the elastic properties of its constituents (fiber and resin) is not very accurate by micromechanics formulation. For design studies, the normal practise is then the characterization of the composite material properties by performing standard tensile tests on sample coupons of the material.

Just few parameters are needed to completely define the mechanical properties of orthotropic materials. They are E_{11} : the longitudinal tensile modulus in the direction of the fiber orientation (axis 1 in Figure A.1); E_{22} : the tensile modulus transverse to the fiber direction; G_{12} : in plane shear modulus and ν_{12} : major Poisson ratio. From these four parameters, the mechanical properties of composite can be obtained along arbitrary lamina axes (axes x and y in A.1) using the equations provided by the Classical Lamination Theory.



Figure A.1: Fiber axis and lamina axis of a composite lamina

Appendix A. Characterization of materials mechanical properties

Experimental measurement of the main mechanical properties of the Toray T800H graphite/epoxy prepreg was carried out by the tensile testing of several sample coupons, using an MTS793 Materials Test machine and in agreement with rules^[129]. The coupons were fabricated from the Toray T800H graphite/epoxy prepreg tape with the layups of $[0]_{10}$, $[90]_{16}$ and $[\pm 45]_{8s}$ and instrumented by strain gauges along the longitudinal and lateral directions. During the testing the coupons were loaded in tension up to their breaking points and strains were recorded as function of stresses, as shown in Fig. (A.2).



Figure A.2: Measured Toray T800H mechanical properties.

Four mechanical properties of the Toray T800H graphite/epoxy prepreg can be derived from these measurements. The longitudinal module E_{11} is calculated using the data of Fig. (A.2(a)); as prescribed by rules, E_{11} is the slope of the straight line joining the (σ , ϵ) points at 2000 and 6000 [$\mu\epsilon$], which are marked with gray dots in the figure. The longitudinal module E_{22} is calculated using the data of Fig. (A.2(b)) and is the slope of the straight line joining the (σ , ϵ) points at 1000 and 3000 [$\mu\epsilon$], while the major Poisson ratio ν_{12} is the negative steady value of the ratio $\frac{\epsilon_1}{\epsilon_2}$ reported in Fig. (A.2(d)). Finally, the shear modulus G_{12} is calculated as the slope of the straight line joining the points $\frac{\epsilon_1}{\epsilon_2}$ at 2000 and 6000 [$\mu\epsilon$] reported in Fig. (A.2(c)).

Table A.1: Measured mechanical properties of the Toray T800H.

	Coupon 1	Coupon 2	Coupon 3	Mean value
E_{11} [MPa]	146812	148154	140392	145120
E_{22} [MPa]	9725	9449	-	9587
G [MPa]	12547	12661	13196	12801
$ u_{21}$ [-]	0.28	0.28	0.29	0.28



The computed mechanical properties are reported in table (A.1).

Figure A.3: Sample coupons tested up to their breaking points

Experimental measurement of the main mechanical properties of the HM M50J was carried out by the tensile testing of several sample coupons with layups $[0]_{12}$, $[90]_{20}$ and $[\pm 45]_{10s}$. The coupons were loaded in tension up to their breaking points, as shown in Fig. (A.3), and the mechanical properties shown in table (A.2) were computed as explained above.

 Table A.2: Measured mechanical properties of the HM M50J.

	Coupon 1	Coupon 2	Coupon 3	Coupon 4	Coupon 5	Mean value
E_{11} [MPa]	244311	239849	242471	247976	250487	245019
E_{22} [MPa]	5955	6003	5955	5828	5941	5936
G [MPa]	4313	5052	4783	3582	3025	4151
ν_{21} [-]	0.415	0.294	0.267	0.368	0.276	0.324

The AF163-2K is not used as an adhesive, but as a polymeric film which makes the blade surfaces smooth; thus appears to be of particular importance the correct evaluation of its mechanical characteristics. Following the requirements imposed by^[129], 3 sample coupons with layup $[0]_{10}$ were fabricated and loaded in tension. The measured mechanical properties are reported in table (A.3), where G has been computed using the well known relation $G = \frac{E}{2(1+\nu)}$ applies to isotropic materials.

Table A.3: Measured mechanical properties of the AF163-2K.

	Coupon 1	Coupon 2	Coupon 3	Mean value
E [MPa]	2906	2881	2875	2887
ν [-]	0.40	0.41	0.42	0.41
G [MPa]	1038	1022	1012	1024

As explained in §4.2.3.1, the Rohacell core must be oversized in order to provide a sufficient level of pressure during the curing process; consequently, the material density can be much greater than the nominal one and variable along the blade span. It has been therefore decided to characterize the Rohacell WF71 mechanical properties of several specimens characterized by different density but identical dimensions. Specimens with measured density equal to 72, 97 and 110 $[kg/m^3]$ were produced with ad-hoc molds used to compress small blocks of different thickness into the desired

Appendix A. Characterization of materials mechanical properties

shape. The pictures reported in Fig. (A.4) highlight the effect produced by the compression on the material cells size, that become smaller with increasing compression level, i.e. with increasing density.



Figure A.4: Specimens of Rohacell core with different density.

The specimen Young's and shear modulus has been characterized, as function of the temperature, using the DMA 2980 Dynamic Mechanical Analyzer. The measured modulus are shown in Fig. (A.5).



Figure A.5: Rohacell WF71 modulus as function of the specimen density and temperature.

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