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Development of a 3 kW Wind Energy Conversion System Emulator Using a Grid-Connected Doubly-Fed Induction Generator

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Abstract: This paper presents the design and performance evaluation of an experimental platform that emulates the static and dynamic behavior of a 3 kW Wind Energy Conversion System (WECS). The platform includes a wind turbine emulator (WTE) using a separately excited DC motor (SEDCM) as the prime mover, coupled with a grid-connected doubly-fed induction generator (DFIG). This setup enables comprehensive laboratory studies of a WECS without the need for large-scale field installations. A novel inertia compensation strategy is implemented to ensure the SEDCM accurately replicates the power and torque characteristics of a real wind turbine across various wind profiles. The DFIG was chosen for its high efficiency at variable wind speeds and its reduced power converter requirements compared to other generators. The control strategy for the DFIG is detailed, highlighting its performance and seamless integration within the system. Unlike most studies focusing on generators connected to simple loads, this research considers a grid-connected system, which introduces additional challenges and requirements. This study thoroughly investigates the grid-connected converter, addressing specific demands for grid connection and ensuring compliance with grid standards. Experimental results validate the effectiveness of the emulator, demonstrating its potential as a key tool for optimizing wind turbine control strategies and real-time algorithm validation, and enhancing the performance and reliability of renewable energy systems.

Keywords: wind energy conversion system (WECS); wind turbine emulator (WTE); grid-connected power systems; doubly-fed induction generator (DFIG); direct power control (DPC)



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1. Introduction

Renewable energy is critical to the global energy transition, with wind power emerging as one of the most promising sources for electricity generation. Among green energy alternatives, wind power stands out due to its zero emissions and widespread availability [1,2]. Recent advancements in wind energy conversion systems (WECSs) have driven a rapid expansion of this clean and cost-effective energy source worldwide over the past few decades [3–5].

Maximizing WECS efficiency requires extensive research into turbine design, generator integration, and grid connection. However, real-time testing of actual wind turbines is often infeasible in research environments due to their large physical footprint, high costs, and reliance on intermittent wind conditions. Wind turbine emulators address these challenges by enabling both simulation and hardware-based emulation of WECSs. While simulation is a fundamental tool for WECS analysis and design [6], hardware emulation provides a more realistic testing environment, allowing for the evaluation of control strategies, power

converter topologies, and the effects of wind variability on grid integration. Consequently, research laboratories are increasingly focused on developing comprehensive emulators that replicate the aerodynamic, mechanical, and electrical behaviors of real wind turbines [7].

WECS emulators can be constructed using only power electronic converters, known as electronic emulators [8,9]. However, to better replicate real WECS behavior, most emulators consist of two mechanically coupled machines: one motor simulating wind torque, commonly referred to as a wind turbine emulator (WTE), and the other acting as an electrical generator. The choice of motor and generator is a critical design consideration, and numerous studies have been conducted on this topic. For the prime mover, some studies use permanent magnet synchronous motors (PMSMs) [10,11] or switched reluctance motors (SRMs) [12], which offer high energy efficiency and precise torque control. However, induction motors (IMs) and DC motors are the most commonly used options.

IMs are widely used due to their robustness, moderate cost, and ability to operate in diverse environments with minimal maintenance. They can handle overloads and provide a wide range of speeds and torques, making them suitable for wind turbine emulation under variable wind conditions. IM-based WTEs are discussed in [13–21]. For instance, [13] uses a 7.5 kW IM to replicate the static characteristics of a wind turbine, while [14] employs a 1.5 kW squirrel-cage motor to emulate the dynamic behavior of a wind turbine. Other works focus on maximum power point tracking (MPPT) algorithms [15], tower shadow effects [16], and wind shear [17]. Authors in [18,19] present systems that emulate the behavior of small-scale wind turbines. Study [20] explores power and voltage fluctuations as well as flicker emissions, while [21] examines the impact of short-circuit faults on the electrical and mechanical components of a fixed-speed wind turbine.

On the other hand, DC motors are widely appreciated for their simplicity of control and ability to provide consistent torque over a broad speed range. Their straightforward modeling and implementation make them a popular choice for test platforms in research and rapid prototyping applications. Additionally, they are cost-effective, requiring fewer power electronics components than IMs. Separately excited DC motors (SEDCMs) and permanent magnet DC motors have been widely used in the literature [22–33]. They are often preferred for WTEs because of the direct relationship between armature current and torque. Two control strategies—speed control and torque control—were proposed by [23]. In [24], a refined torque control strategy was introduced to account for harmonic torques caused by wind gradients and tower shadow effects, while [25] employed a three-phase thyristor rectifier instead of a chopper for DC motor control. Advanced techniques for optimizing the PI controller parameters were presented in [26–28], and ref. [29] replaced the PI controller with a fuzzy logic controller. An open-loop control method that eliminates the need for a processor or complex control systems was proposed in [30]. In [31], a WTE using a DC motor controlled by an FPGA-based chopper was developed, and a Hardware-in-the-Loop (HIL) implementation of a DC motor-based WTE was presented in [32]. A 300W DC motor was used to emulate the dynamic characteristics of a 5 MW wind turbine in [33].

Studies on WECS emulators have employed various generators, including permanent magnet synchronous generators (PMSGs), induction generators (IGs), and doubly-fed induction generators (DFIGs). PMSGs are favored for their high efficiency and superior reliability, owing to the absence of mechanical components such as the brush-collector system and gearboxes in high-pole machines. These generators do not require separate excitation, making them suitable for small-scale designs. Emulators using PMSGs are described in [19,26,29,33–37]. However, the high cost of permanent magnets is a significant concern, especially in large wind turbines. Additionally, a full-scale power converter is required to connect the generator to the grid. IGs, on the other hand, offer advantages such as lower unit cost and high robustness. However, they require an external excitation circuit and reactive power from the grid. Furthermore, they need a synchronization relay and a full-scale power converter for grid connection. Studies using IGs are detailed in [14–17,20,21,38]. By contrast, DFIGs are the most commonly used generators in WECSs, primarily because

they only require a converter rated at approximately 30% of the nominal power, while allowing a speed variation of 60% around the synchronous speed [1]. This leads to reduced costs at higher overall efficiency. DFIGs are also valued for their ability to control reactive power, making them more suitable for high-power applications. Despite this, few studies on WTEs have employed DFIGs [36].

The design of a WTE must overcome the challenge of replicating the slow dynamic behavior of a real wind turbine on a test platform using low-inertia machines. This challenge is further complicated by the fact that the inertia of the motor-generator system (WTE) is significantly lower than that of an actual wind turbine. To address this, two primary approaches are proposed in the literature. The first is rigid inertia compensation using a mechanical flywheel [37,39]. The second, more flexible approach, dynamically adjusts the motor torque based on the difference between the inertia of the WTE and that of the wind turbine. This method, which employs a control algorithm to adapt the drive torque [36,40–42], offers several advantages, including no need for hardware modifications, ease of implementation, and precise tuning. This flexible approach is one of the key improvements discussed in this paper.

Research on grid-connected wind turbine emulators using DFIGs are crucial due to the growing demand for efficient integration of intermittent energy sources. A grid-connected emulator not only replicates the real operating conditions of a wind turbine but also facilitates the testing of various control strategies. These experiments are vital for assessing grid stability in response to sudden and often unpredictable fluctuations in wind power, while minimizing the risk of instability or imbalance. Additionally, they offer deep insights into DFIG performance during grid disturbances, such as voltage dips or load variations, to enhance system resilience. There is limited research on grid-connected emulators [35,43]. For instance, [35] employs a PMSG generator, while [43] explores DFIG grid connection with unrealistic wind profiles and a unidirectional converter. In this work, however, a back-to-back bidirectional converter and a realistic wind profile model are used for grid connection experiments, representing a significant improvement.

This work provides a comprehensive overview of the development of a complete WECS emulator test bench. The system uses an SEDCM as the prime mover, controlled via a DC-DC buck converter to simulate the aerodynamic behavior of a wind turbine, and a 3 kW grid-connected DFIG as the generator. The DFIG allows for independent regulation of active and reactive power exchanged with the grid, while a synchronous reference frame phase-locked loop (SRF-PLL) ensures precise grid synchronization. The system implementation was carried out using dSPACE DS1104 hardware system, operated through ControlDesk software (version 3.7.1).

Key features of the developed test bench include the following:

- The capability to simulate and test the system under various wind profiles.
- Real-time validation of advanced control strategies for managing active and reactive power.
- Online implementation and evaluation of different MPPT algorithms.
- Testing of different grid synchronization techniques.
- A highly valuable tool for the WECS industry, research laboratories, and as an educational platform for teaching wind turbine operation, control, and dynamic behavior.

2. Overall Scheme of the WECS

The studied WECS, shown in Figure 1, consists of a wind turbine coupled with a DFIG connected to the grid. The DFIG, is a widely used generator in wind energy systems due to its ability to operate efficiently over a range of wind speeds. The DFIG is interfaced with the grid via two converters: the Rotor-Side Converter (RSC) and the Grid-Side Converter (GSC). The RSC controls the rotor voltages, ensuring optimal power extraction from the wind, while the GSC regulates the DC bus voltage and ensures that power is injected into the grid with a unity power factor. The control strategy employed in the DFIG is based on direct power control (DPC), which decouples the active and reactive power components. In the synchronous reference frame (dq -frame), the active power is controlled through the

d -axis by adjusting the rotor speed to follow the maximum power point tracking (MPPT) algorithm. The q -axis controls the reactive power, ensuring that the system operates at a unity power factor by minimizing reactive power exchange with the grid. The system also incorporates phase-locked loops (PLLs) to ensure precise synchronization with the grid by continuously tracking the phase angle of the grid voltage. This guarantees smooth integration and stable operation under varying wind conditions.

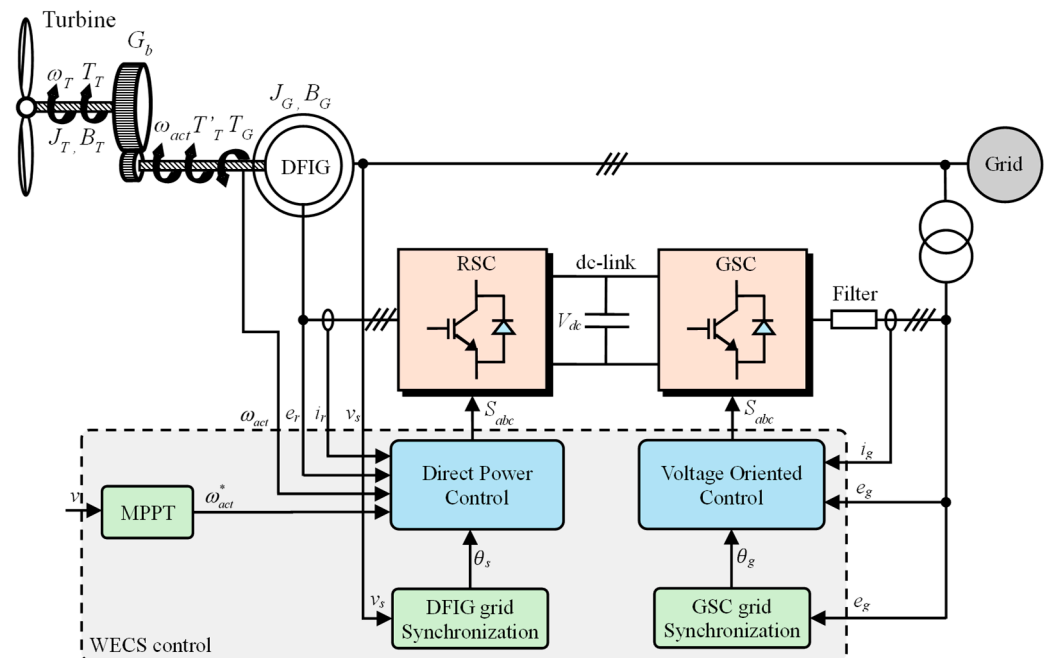


Figure 1. DFIG control strategy diagram for variable-speed wind turbines.

Overall, the WECS is designed to maximize energy capture from the wind, ensure efficient power transfer to the grid, and maintain stable operation, even during dynamic wind conditions.

2.1. Extracted Wind Power

Energy production is achieved by extracting the wind kinetic energy as it passes through the wind turbine. The mechanical power P_T extracted from the wind and available on the turbine shaft is then expressed as follows:

$$P_T = \frac{1}{2} \rho S v^3 C_p(\lambda) \quad (1)$$

where ρ is the air density, v is the wind speed at the blades, S is the swept area by the blades of length R such that $S = \pi R^2$, and $C_p(\lambda)$ is the power coefficient which depends only on λ for small power wind turbines with a fixed pitch angle.

λ represents the tip-speed ratio, defined as the ratio of the wind turbine's peripheral speed $v_T = \omega_T R$ to the wind speed v , given by

$$\lambda = \frac{\omega_T R}{v}. \quad (2)$$

The λ value that yields a maximum power coefficient C_{pmax} is λ_{opt} , which corresponds to the Betz limit, formulated by the German physicist Albert Betz in 1919 [44]. According to this limit, a wind turbine can extract a maximum of 59.3% of the available wind power. Although wind turbine manufacturers try to approach this limit, the current C_{pmax} is around 40%, and the Betz limit is never reached in practice. Each wind turbine is characterized by a $C_p = f(\lambda)$ curve that describes its efficiency. This characteristic is either provided by

the manufacturer or determined experimentally. In this study, the $C_p(\lambda)$ curve shown in Figure 2 corresponds to experimental measurements of an actual wind turbine obtained from [45]. Due to its non-linear nature, Matlab's 'Basic Fitting' tool is used to interpolate this characteristic. The obtained expression is given by

$$C_p(\lambda) = -9.727 \times 10^{-7} \lambda^9 + 0.00004554 \lambda^8 - 0.00088816 \lambda^7 + 0.009302 \lambda^6 - 0.056085 \lambda^5 + 0.19293 \lambda^4 - 0.35191 \lambda^3 + 0.30148 \lambda^2 - 0.070868 \lambda + 0.0039702. \quad (3)$$

The $C_p = f(\lambda)$ curve has an optimal point ($\lambda_{opt} = 6.71$, $C_{pmax} = 0.41$), which corresponds to the maximum efficiency of the turbine. At this point, the maximum mechanical power can be extracted from the available wind power. Table 1 presents the studied wind turbine parameters.

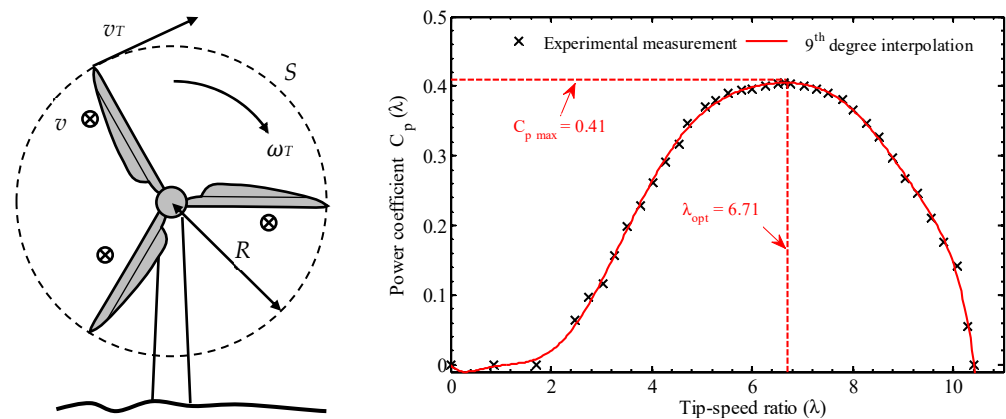


Figure 2. Power coefficient $C_p(\lambda)$ curve of real and simulated wind turbine.

Table 1. Wind turbine parameters [45].

Parameters	Values
Rated Power P_n (kW)	3
Rotor radius R (m)	4.5
Gearbox ratio G_b	24
Moment of inertia J_T (Kgm ²)	7
Performance Coefficient C_{pmax}	0.41
Tip-speed ratio λ_{opt}	6.71
Wind speed max v_{max} (m/s)	5.71
Wind speed min v_{min} (m/s)	3

Knowing the rotational speed ω_T of the turbine, the mechanical torque T_T available on the turbine's slow shaft can be expressed as follows [15]:

$$T_T = \frac{P_T}{\omega_T} = \frac{1}{2} \rho S \frac{v^3}{\omega_T} C_p(\lambda). \quad (4)$$

By substituting ω_T from (2) into (4), it is shown that torque T_T depends on λ .

$$T_T = \frac{P_T}{\omega_T} = \frac{1}{2} \rho \pi R^3 \frac{v^2}{\lambda} C_p(\lambda). \quad (5)$$

2.2. Wind Turbine Mechanical Model

The physical system consists of a wind turbine on a slow shaft, coupled to the generator's fast shaft via a gearbox with gain G_b , which adjusts the primary shaft's speed to match the generator's speed. Indeed, the low rotational speed of the wind turbine does not allow for efficient electrical power generation with conventional generator. To derive the mathematical model linking the turbine's rotational speed ω_T and torque T_T to the

generator's speed ω_{act} and torque T'_T , let J_T and B_T represent the turbine's moment of inertia and friction coefficient, and $J_G = 0.037 \text{ kg}\cdot\text{m}^2$ and $B_G = 0.012 \text{ Nm}\cdot\text{s}/\text{rad}$ represent the generator's inertia and friction coefficient. Assuming negligible mechanical losses in the gearbox, the following is obtained:

$$T_T = G_b T'_T, \quad \omega_T = \omega_{act} / G_b. \quad (6)$$

Based on these assumptions, a mechanical model consisting of two masses is obtained. The total equivalent inertia J_{TG} and the total friction coefficient B_{TG} of the wind turbine system, viewed from the generator rotor, are calculated as follows:

$$\begin{cases} J_{TG} = J_G + J_T / G_b^2 \\ B_{TG} = B_G + B_T / G_b^2 \end{cases}. \quad (7)$$

The equation of motion that determines the mechanical speed ω_{act} from the driving torque T'_T at the generator rotor and the electromagnetic torque T_G developed by the generator is given by [15,45]

$$J_{TG} \frac{d}{dt} \omega_{act} + B_{TG} \omega_{act} = T'_T - T_G. \quad (8)$$

The corresponding transfer function $G_{TG}(s)$ is as follows:

$$G_{TG}(s) = \frac{\omega_{act}(s)}{T'_T(s) - T_G(s)} = \frac{1}{J_{TG}s + B_{TG}}. \quad (9)$$

In this model, the friction coefficient B_T is neglected due to the very low ratio of B_T / G_b^2 . This assumption simplifies the system dynamics.

2.3. MPPT-Based Wind Turbine Control

This article focuses on MPPT operation. The wind turbine starts operating at a minimum wind speed v_{min} to extract maximum power until the wind reaches the maximum speed v_{max} , corresponding to the generator's nominal mechanical power P_{Gn} . To maximize extracted power, the turbine must operate at the point corresponding to C_{pmax} and λ_{opt} as shown in Figure 3a. The optimal torque is obtained by substituting C_p with C_{pmax} and λ with λ_{opt} in (5), resulting in

$$T_{T-opt} = \frac{P_{Tmax}}{\omega_T} = \frac{1}{2} \rho \pi R^3 \frac{v^2}{\lambda_{opt}} C_{pmax}. \quad (10)$$

For wind speeds between $v_{min} = 3 \text{ m/s}$ and $v_{max} = 5.71 \text{ m/s}$, corresponding to 109.9 and 204.2 rad/s ($\pm 30\% \omega_{Gn}$), respectively, the extracted power can be maximized by applying the corresponding optimal torque. For wind speeds exceeding $v_{max} = 5.71 \text{ m/s}$, these values exceed the nominal ratings of the turbine and generator, making MPPT infeasible and necessitating limiting devices. Figure 3b illustrates that to extract maximum power, the mechanical torque must be optimal, which does not always coincide with the turbine's maximum torque across different wind speeds.

Various MPPT strategies exist with and without rotational speed control. This work adopts an MPPT approach that adjusts the generator's electromagnetic torque to set the rotational speed to a reference, enabling maximum power extraction. The method's scheme is shown in Figure 4. Mechanical power is maximized when the coefficient C_p reaches its maximum value at the optimal tip-speed λ_{opt} . Thus, the reference rotational speed of the generator is calculated as follows [15]:

$$\omega_{act}^* = \frac{\lambda_{opt} v}{R} G_b. \quad (11)$$

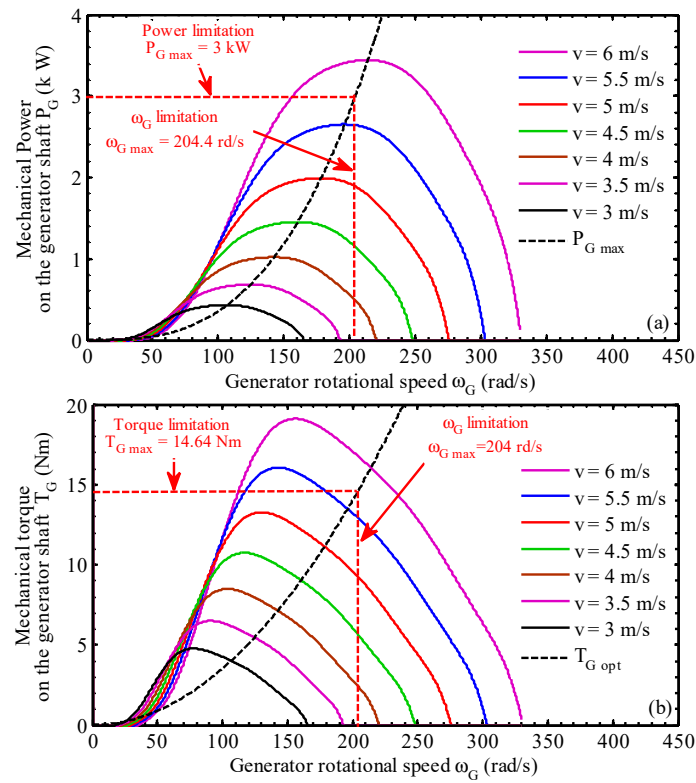


Figure 3. Wind turbine characteristics for different wind speed: (a) mechanical power on DFIG shaft and maximal power; (b) mechanical torque and optimal torque.

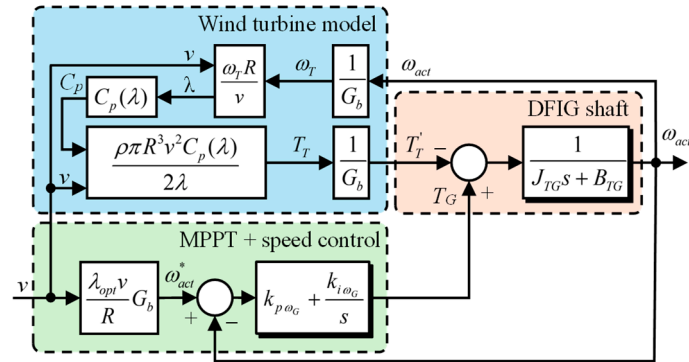


Figure 4. MPPT control with rotational speed feedback.

To regulate the rotational speed to the setpoint determined by the MPPT, a proportional-integral (PI) controller is employed, characterized by proportional gain $k_{p\omega_G}$ and integral gain $k_{i\omega_G}$ [46]. The mechanical torque T'_T is considered a compensable disturbance since its value can be estimated using (5) and (6). The design of the controller is based on the mechanical model (9) and the corresponding closed-loop transfer function is given by

$$G_{\omega_G}(s) = \frac{\omega_{act}(s)}{\omega_{act}^*(s)} = \frac{(k_{p\omega_G}/k_{i\omega_G})s + 1}{(J_{TG}/k_{i\omega_G})s^2 + ((k_{p\omega_G} + B_{TG})/k_{i\omega_G})s + 1}. \quad (12)$$

By identifying with a 2nd-order system having a damping ratio $\zeta = 1$ and a cutoff frequency $\omega_n = 4.8/\text{tr}$ [46], where t_r is the desired response time, the following results are obtained:

$$\begin{cases} k_{i\omega_G} = \omega_n^2 J_{TG} \\ k_{p\omega_G} = 2\zeta\omega_n J_{TG} - B_{TG} \end{cases}. \quad (13)$$

For a wind energy system, the response time t_r should ideally be at least ten times faster than the period of the wind speed variations to ensure that the controller can adjust swiftly to dynamic changes. In the wind profile studied, variations occur in a period of approximately 1 s. Thus, selecting $t_r = 100$ ms allows the controller to respond quickly and accurately to these fluctuations. Accordingly, the values of $k_{i\omega_G} = 113.24$ and $k_{p\omega_G} = 4.70$ are derived to optimize the system's performance. This setup effectively balances the need for rapid adaptation with stable control.

3. SEDCM-Based WTE

The WTE allows for detailed laboratory studies of WECSs without the need for large-scale field installations. The objective is to replicate the mechanical characteristics of a real wind turbine as illustrated in Figure 5. The proposed WTE uses a torque-controlled SEDCM to mimic the mechanical torque of the wind turbine. However, since the mechanical parameters of the wind turbine and the SEDCM are different, the torque produced by the SEDCM differs from that of the actual turbine. To address this, a new compensation method is presented in this section to ensure accurate emulation.

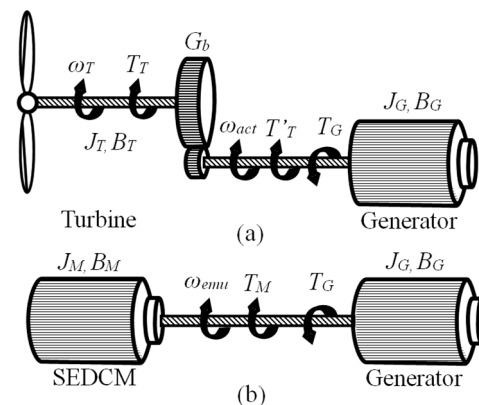


Figure 5. Wind turbine mechanical model: (a) wind energy conversion system and (b) WTE.

3.1. Electromagnetic Torque Controller Design for the SEDCM

The electromagnetic torque $T_M = K_M I_a$ of the SEDCM is regulated by adjusting the armature current I_a , with K_M being the torque constant. To accurately emulate the wind turbine torque, the SEDCM torque setpoint T_M^* must be continuously calculated. The influence of the electromotive force E is compensated for in the control system, resulting in the 1st-order open-loop transfer function $G_{M-OL}(s)$ as follows:

$$G_{M-OL}(s) = \frac{T_M}{U_a} = \frac{G_a}{\tau_a s + 1}. \quad (14)$$

where $G_a = K_M/R_a$ is the static gain, $\tau_a = L_a/R_a$ is the armature time constant, and L_a and R_a are the armature inductance and resistance of the SEDCM, respectively.

By using a PI controller with proportional parameter k_{pM} and integral parameter k_{iM} , and compensating for the pole L_a/R_a with the zero k_{pM}/k_{iM} of the controller [46], the closed-loop transfer function $G_{M-CL}(s)$ remains 1st order and is expressed as follows:

$$G_{M-CL} = \frac{T_M(s)}{T_M^*(s)} = \frac{1}{(R_a/k_{iM}K_M)s + 1} = \frac{1}{\tau_{cl}s + 1}. \quad (15)$$

where $\tau_{cl} = R_a/(k_{iM}K_M)$ is the closed-loop time constant.

The gain values of the controller are finally obtained as

$$\begin{cases} k_{iM} = R_a/(\tau_{cl}K_M) \\ k_{pM} = L_a/(\tau_{cl}K_M) \end{cases}. \quad (16)$$

The PI controller computes the reference voltage U_a^* to be applied to the SEDCM's armature to achieve the desired reference torque. The control loop requires a faster response time than the wind variations and the mechanical time constants of the actual system. To match the open-loop system dynamics, the time constant τ_{cl} is set equal to τ_a , giving $\tau_{cl} = 6.5$ ms, corresponding to a response time of $t_{rcl} = 19.5$ ms. This value provides the necessary dynamic response for the torque to reach its setpoint efficiently. Furthermore, t_{rcl} is sufficiently smaller than the speed control loop response time ($t_r = 100$ ms), ensuring adequate separation between the control loops. The resulting PI controller parameters are $k_{iM} = 75.18$ and $k_{pM} = 0.488$.

Since the DC motor's mechanical parameters differ from those of the wind turbine, T_M will also differ. Therefore, the reference torque T_M^* must be adjusted so that T_M equals T_T' . This is achieved by introducing a compensation term to T_M^* , which is detailed in the following section.

3.2. Electromagnetic Torque Compensation Loop for the SEDCM

The mechanical model of the SEDCM directly coupled to the generator [45] is given by

$$(J_M + J_G) \frac{d\omega_{emu}}{dt} + (B_M + B_G)\omega_{emu} = T_M - T_G. \quad (17)$$

where ω_{emu} is the rotational speed of the SEDCM, identical to the DFIG's speed, and J_M and B_M represent the SEDCM's inertia and friction coefficient, respectively.

The model described in (17) differs from the actual system outlined in (8), necessitating compensation for friction and inertia to achieve accurate emulation. This compensation involves adjusting the SEDCM torque to replicate the effects of the turbine's inertia and friction coefficient. By comparing the mechanical models in (8) and (17), it can be deduced that both systems will be equivalent if, under the same resisting torque T_G developed by the DFIG, the acceleration of both systems is identical. This leads to

$$T_M - J_{MG} \frac{d\omega_{emu}}{dt} - B_{MG}\omega_{emu} = T_T' - J_{TG} \frac{d}{dt}\omega_{act} - B_{TG}\omega_{act}. \quad (18)$$

where $J_{MG} = J_M + J_G$ and $B_{MG} = B_M + B_G$ represent the total inertia and friction coefficient of the SEDCM coupled to the DFIG, respectively.

From this equality, the compensation torque T_{com} , representing the difference between the turbine torque T_T' and the SEDCM torque T_M , is expressed as

$$T_{com} = T_T' - T_M = (J_{TG} - J_{MG}) \frac{d\omega_{emu}}{dt} + (B_{TG} - B_{MG})\omega_{emu}. \quad (19)$$

The resulting 1st-order transfer function is given by

$$\frac{\omega_{emu}}{T_{com}} = \frac{\omega_{emu}}{T_T' - T_M} = \frac{1}{(J_{TG} - J_{MG})s + (B_{TG} - B_{MG})}. \quad (20)$$

To ensure accurate emulation of the real system, T_{com} must be rapidly established, with a response time shorter than other mechanical time constants. Various techniques have been proposed in the literature to address this issue [15,36,45]. In [45], the authors directly exploit (19) for compensation torque estimation. However, the inclusion of a derivative in this equation presents practical implementation challenges. To avoid this issue, [15] replaced the derivative with a high-pass filter (HPF) followed by a low-pass filter (LPF). This solution, while effective, requires precise parameter tuning and complicates the implementation. Moreover, these approaches do not account for turbine and generator friction, resulting in static errors. Works in [36,42] introduced methods that consider friction effects. Authors in [42] proposed a fourth-order filter to mitigate the derivative issue. However, this technique introduces a delay that must be compensated for. To estimate T_{com} , [36] proposed two methodologies: 1-DOF and 2-DOF. These methods estimate the rotational speed based

on the difference between turbine and generator torques. The resulting error between the measured and estimated speeds is then utilized to determine the WTE torque reference. Although the 1-DOF technique employs a PI controller, it remains sensitive to mechanical parameter inaccuracies. In contrast, the 2-DOF technique incorporates a pre-filter for speed estimation and a lead-lag controller. Despite its stability, this approach introduces an additional speed estimation loop, increasing complexity. In this study, a method exploiting a straightforward PI controller is proposed to estimate T_{com} directly from the error between the measured speed and the estimated speed by the MPPT. This eliminates the need for a separate speed and fast shaft torque estimation. The conceptual framework of this method is depicted in Figure 6.

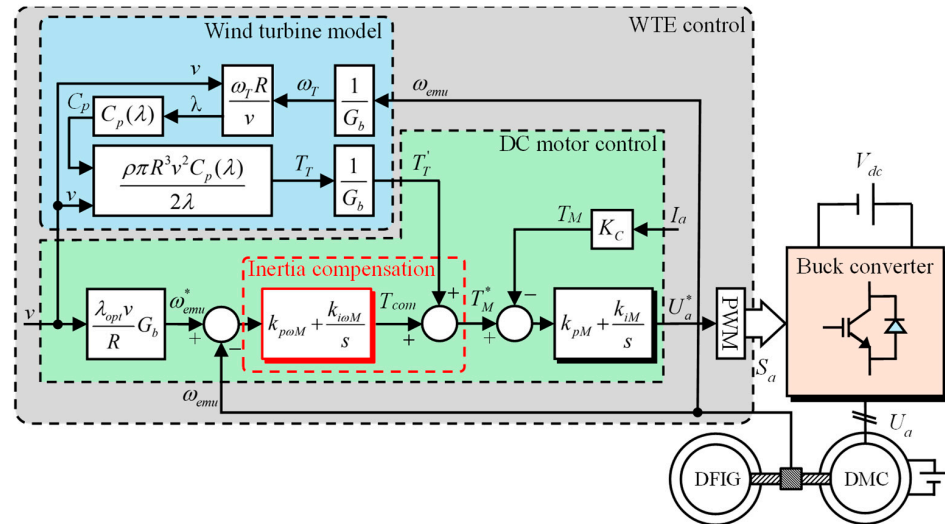


Figure 6. WTE control scheme.

The compensation torque T_{com} is derived using a PI controller with proportional $k_{p\omega M}$ and integral $k_{i\omega M}$ gains calculated from the transfer function in (20) as

$$\frac{\omega_{emu}(s)}{\omega_{emu}^*(s)} = \frac{(k_{p\omega M}/k_{i\omega M})s + 1}{((J_{TG} - J_{MG})/k_{i\omega M})s^2 + ((B_{TG} - B_{MG}) + k_{p\omega M}/k_{i\omega M})s + 1}. \quad (21)$$

The system is identified as a 2nd-order system with a damping ratio $\zeta = 1$ and a natural frequency $\omega_n = 4.8/t_r$ [46], where t_r is the desired response time, leading to the following:

$$\begin{cases} k_{i\omega M} = (J_{TG} - J_{MG})\omega_n^2 \\ k_{p\omega M} = 2\zeta(J_{TG} - J_{MG})\omega_n - (B_{TG} - B_{MG}) \end{cases}. \quad (22)$$

To achieve precise compensation, the response time of the function must be equal to or faster than that of the speed control loop, while remaining slower than the response times of the DFIG power and SEDCM torque control loops. Additionally, for robust stability, the zero must be placed sufficiently far from the origin and decoupled from the poles of function (21). A response time of $t_r = 100$ ms is therefore sufficient to ensure both stability and optimal dynamic performance.

For a response time of $t_r = 100$ ms, the controller gains are $k_{i\omega M} = 100.56$ and $k_{p\omega M} = 4.17$. Finally, the estimated compensation torque T_{com} is added to the mechanical torque T'_T to obtain the reference torque T^*_M of the SEDCM, as illustrated in Figure 6.

3.3. Dynamic Behavior of SEDCM-Based WTE

To demonstrate the effectiveness of the proposed WTE, a simulation comparison in the Matlab/Simulink environment is performed for various quantities of the actual and emulated wind turbine using the parameters from Tables 1 and 2. This test will verify if the

emulator's performance is equivalent to that of a wind turbine. The system is controlled by the MPPT strategy with rotational speed feedback control.

Table 2. SEDCM parameters.

Parameters	Values
Rated power P_{Mn} (kW)	3
Rated speed N_{Mn} (rpm)	1500
Rated armature voltage U_a (V)	220
Rated field voltage U_{ext} (V)	120
Rated armature current I_a (A)	16
Maximum field current $I_{ext-max}$ (A)	1.7
Moment of inertia J_M (Kg.m ²)	0.0558
Viscous friction coefficient B_M (Nm.s/rad)	0.018
Armature resistance R_a (Ω)	1
Armature inductance L_a (mH)	6.447
Electromagnetic torque constant K_M (Nm/A)	1.33

Figure 7 shows the simulation results obtained using a random wind profile with an average speed of 4 m/s, as illustrated in Figure 7a. Figure 7b–f show that the results from the WTE precisely match those of the actual system. The error between the two systems is minimal, validating the emulator's effective operation. Figure 7e shows that the difference between the electromagnetic torque developed by the SEDCM T_M and that produced by the turbine T'_T equals the compensation torque T_{com} calculated by the proposed method. Consequently, as Figure 7d,f show, the tip-speed ratio λ and the power coefficient C_p are perfectly controlled at their optimal values. This indicates that the SEDCM dynamics accurately replicate the behaviour of the wind turbine, with the mechanical torque on the DFIG shaft being precisely compensated for, as depicted in Figure 7c and detailed in Figure 7e.

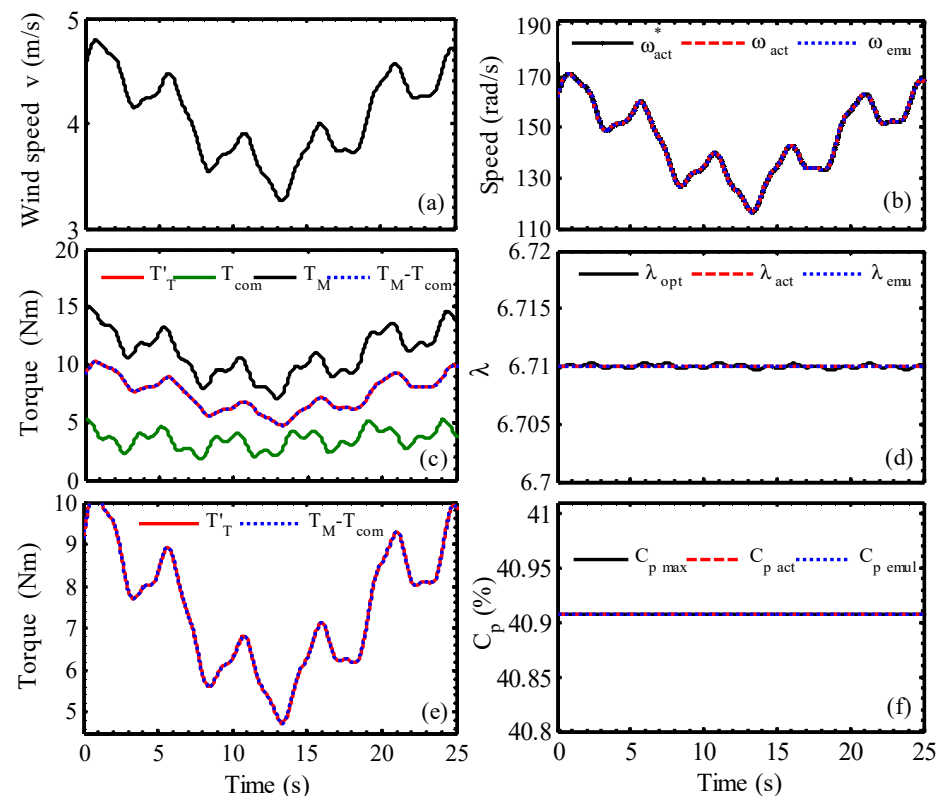


Figure 7. Simulation results of actual and emulated wind turbine under MPPT control with rotational speed feedback: (a) wind profile, (b) rotational speed, (c) torque, (d) tip-speed ratio λ , (e) zoom of emulated torque $T_M - T_{com}$ and turbine torque T'_T , and (f) power coefficient C_p .

4. DFIG Direct Power Control

To simplify the control of the DFIG, the three-phase system is transformed into a two-phase dq reference frame using Park's transformation. This allows the direct and quadrature components to be separated, enabling independent control of active and reactive power. The DFIG equations in the dq frame, aligned with the stator flux rotating at ω_s , and for rotor electrical angular speed ω_r , are expressed as

$$\begin{aligned} V_{sdq} &= R_s I_{sdq} + \frac{d\phi_{sdq}}{dt} + j\omega_s \phi_{sdq} \\ V_{rdq} &= R_r I_{rdq} + \frac{d\phi_{rdq}}{dt} + j\omega_r \phi_{rdq} \end{aligned} \quad (23)$$

where $V_{sdq} = [V_{sd} \ V_{sq}]^T$ and $V_{rdq} = [V_{rd} \ V_{rq}]^T$ are the stator and rotor voltages in the dq frame, and $I_{sdq} = [I_{sd} \ I_{sq}]^T$ and $I_{rdq} = [I_{rd} \ I_{rq}]^T$ are the corresponding currents. R_s and R_r are the stator and rotor resistances, respectively.

The stator and rotor fluxes ϕ_{sdq} and ϕ_{rdq} are related to the currents by

$$\begin{aligned} \phi_{sdq} &= L_s I_{sdq} + L_m I_{rdq} \\ \phi_{rdq} &= L_r I_{rdq} + L_m I_{sdq} \end{aligned} \quad (24)$$

where L_s , L_r , and L_m are the stator, rotor, and mutual inductances, respectively.

The electromagnetic torque T_{em} , as a function of the stator flux and rotor currents, is given by

$$T_{em} = \frac{pL_m}{L_s} (\phi_{sdq} \times I_{sdq}) \quad (25)$$

where p is the number of pole pairs, and the symbol $\times$ denotes the cross-product of the vectors ϕ_{sdq} and I_{sdq} .

The stator active power P_s and reactive power Q_s are given by

$$\begin{aligned} P_s &= V_{sd} I_{sd} + V_{sq} I_{sq} \\ Q_s &= V_{sq} I_{sd} - V_{sd} I_{sq}. \end{aligned} \quad (26)$$

4.1. DFIG Inner Control Loop

To control the power generated by the DFIG coupled with a wind turbine, independent control of active and reactive power is used. This is achieved by deriving equations that link the rotor voltage generated by the RSC to the stator's active and reactive power output. Assuming a balanced sinusoidal voltage supply and neglecting the stator resistance R_s , the chosen dq reference frame is oriented such that the stator voltage space vector aligns along the d -axis ($V_{sd} = -\omega_s \phi_{qs}$ and $V_{sq} = 0$), while the stator flux space vector aligns along the q -axis ($\phi_{sq} = \phi_s$ and $\phi_{sd} = 0$). Under these conditions, the relationships between stator and rotor currents and fluxes can be derived from (24) as follows:

$$\begin{aligned} I_{sdq} &= -\frac{L_m}{L_s} I_{rdq} + \frac{1}{L_s} \phi_{sdq} \\ \phi_{rdq} &= \sigma L_r I_{rdq} + \frac{L_m}{L_s} \phi_{sdq} \end{aligned} \quad (27)$$

where $\sigma = 1 - (L_m^2 / (L_s L_r))$ is the leakage coefficient.

Substituting (27) into (23), the rotor voltage model in terms of the rotor currents is derived:

$$V_{rdq} = \sigma L_r \frac{dI_{rdq}}{dt} + (R_r + j\omega_r \sigma L_r) I_{rdq} - \frac{\omega_r L_m}{L_s} \phi_{sdq}. \quad (28)$$

By replacing the stator currents from (27) into (26), the following can be written:

$$\begin{aligned} P_s &= -\frac{L_m}{L_s} V_{sd} I_{rd} \\ Q_s &= \frac{L_m}{L_s} V_{sd} I_{rq} - \frac{V_{sd}}{L_s} \phi_{sq}. \end{aligned} \quad (29)$$

Similarly, the expression for the electromagnetic torque T_G is expanded as follows:

$$T_G = -\frac{pL_m}{L_s} \frac{P_s}{\omega_s}. \quad (30)$$

Substituting the rotor currents from (29) into (28) yields the rotor voltage model as a function of stator power:

$$\begin{bmatrix} V_{rd} \\ V_{rq} \end{bmatrix} = \frac{\sigma L_s L_r}{L_m V_{sd}} \frac{d}{dt} \begin{bmatrix} -P_s \\ Q_s \end{bmatrix} + \left(\frac{R_r L_s}{L_m V_{sd}} + j \frac{\omega_r \sigma L_r L_s}{L_m V_{sd}} \right) \begin{bmatrix} -P_s \\ Q_s \end{bmatrix} - \frac{\phi_{sq}}{L_m} \begin{bmatrix} \omega_r L_r \\ -R_r \end{bmatrix}. \quad (31)$$

The DFIG model given by (31) shows that independent control of the stator active and reactive powers can be obtained. The coupling terms $\omega_r \sigma L_s L_r Q_s / (L_m V_{sd})$ and $\omega_r \sigma L_s L_r P_s / (L_m V_{sd})$, as well as the perturbation terms $\omega_r \sigma L_s L_r / (L_m V_{sd})$ and $R_r \phi_{sq} / L_m$, are compensated to independently control both active and reactive powers using two PI controllers. Therefore, the same open-loop transfer function is obtained for both active and reactive powers.

$$G_{OL-DFIG} = \frac{k_{im}}{s} \left(\frac{k_{pm}}{k_{im}} s + 1 \right) \frac{A}{1 + \sigma \tau_r s} \quad (32)$$

where $A = L_m V_{ds} / (L_s R_r)$ and $\tau_r = L_r / R_r$.

For tuning the PI controller parameters k_{pm} and k_{im} , the pole-zero compensation method is used, where the controller zero is set equal to the system pole, i.e., $k_{pm} / k_{im} = \sigma \tau_r$. This method is efficient for a first-order transfer function. The controller gains are calculated based on the DFIG parameters and the closed-loop time constant τ_{cl} as follows:

$$G_{CL-DFIG} = 1 / (\tau_{cl} s + 1). \quad (33)$$

where the time constant $\tau_{cl} = 1 / (A k_{im})$.

The closed-loop transfer function is a first-order system with a unity gain, resulting in zero steady-state error. Thus, the controller gains are

$$k_{im} = \frac{1}{A \tau_{cl}}, \quad k_{pm} = k_{im} \tau_{cl}. \quad (34)$$

To maintain the same closed-loop dynamics as the open-loop system, τ_{cl} is set equal to the open-loop time constant $\tau_{ol} = \sigma \tau_r$. Thus, $\tau_{cl} = 6.7$ ms and the obtained regulator parameters are $k_{im} = 0.787$ and $k_{pm} = 0.0039$.

To achieve a unit power factor operation of the DFIG, the stator reactive power is set to zero ($Q_s^* = 0$). This is accomplished using a PI controller that computes the required rotor voltage V_{rq}^* .

4.2. DFIG Speed Control Loop

The outer control loop regulates the DFIG's rotational speed using the MPPT algorithm presented in Section 2.3 (Figure 4). The transfer function and controller gains are given by (12) and (13). The stator active power reference P_s^* is calculated using (30), with the reference electromagnetic torque T_G^* provided by the MPPT speed controller.

The overall DFIG control strategy is illustrated in Figure 8. The parameters of the DFIG are provided in Table 3, with the rotor parameters referenced to the stator side.

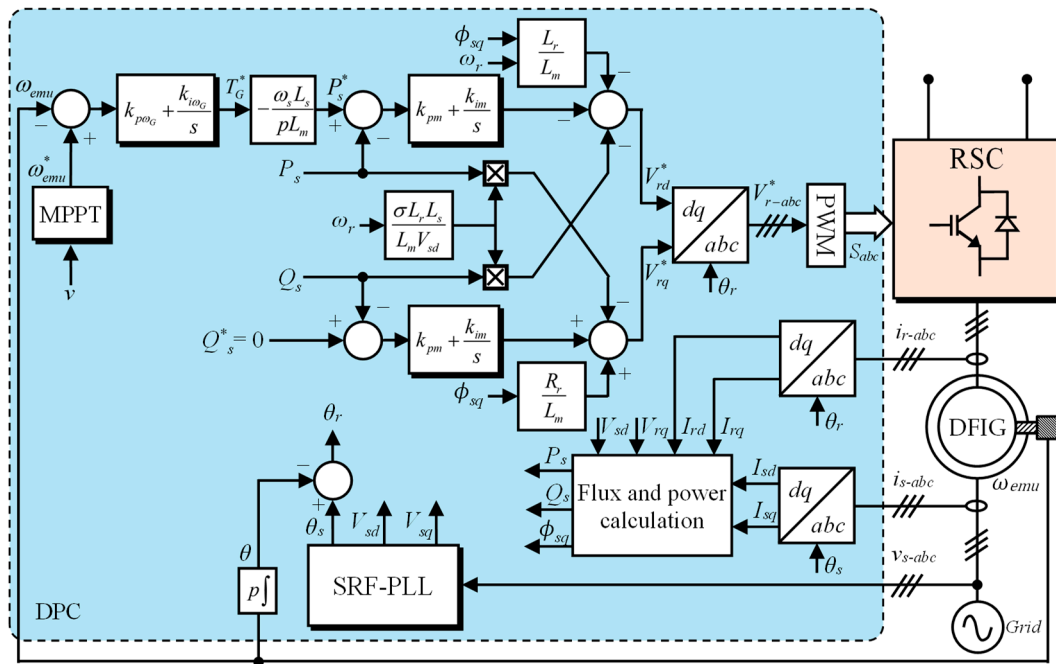


Figure 8. Overall DPC scheme for the DFIG.

Table 3. DFIG parameters.

Parameters	Values
Rated power P_{Gn} (kW)	3
Rated speed ω_{Gn} (rd/s)	148.17
Rated stator frequency f_{sn} (Hz)	50
Rated stator voltage U_{sn} (V)	400
Rated rotor voltage U_{rn} (V)	180
Rated stator current I_{sn} (A)	6.3
Rated rotor current I_{rn} (A)	10.8
Moment of inertia J_G (kg·m ²)	0.037
Viscous friction coefficient B_G (Nm·s/rad)	0.012
Stator resistance R_s (Ω)	1.5
Rotor resistance R_r (Ω)	0.6
Stator inductance L_s (mH)	220
Rotor inductance L_r (mH)	59
Mutual inductance L_m (mH)	110

4.3. DFIG Grid Synchronization

To maintain DFIG control performance during grid frequency ω_s variations, a synchronous reference frame PLL (SRF-PLL) is used, as shown in Figure 9. The linearized model of the SRF-PLL with normalized input voltages can be derived as in [47].

$$G_{CL-PLL} = \frac{\theta_{s-est}}{\theta_s} = \frac{k_{i-pll} + k_{p-pll}s}{s^2 + k_{p-pll}s + k_{i-pll}}. \quad (35)$$

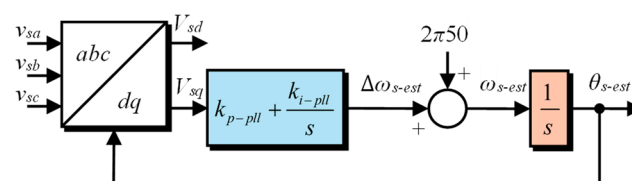


Figure 9. Block diagram of SRF-PLL.

The Voltage Oriented Control (VOC) strategy is selected for the GSC based on the criteria of simplicity, robustness, and signal quality. This method, developed by analogy with the vector control of electrical machines, is widely used in industrial applications. Its main objectives are to keep the DC bus voltage constant, regardless of power flow direction, and to maintain a unit power factor at the grid connection point [49,50]. To meet these objectives, the dq -axis currents are controlled by an internal loop, while the DC bus voltage V_{dc} is controlled by an external loop. Additionally, a PLL is used to estimate the grid voltage angle θ_g for Park's transformation. The next section provides details on the VOC strategy blocks.

5.1. Current Control Loop

The dq -frame converter model allows independent control of each axis using two separate PI controllers. Compensating for the coupling terms $\omega_g L I_{gq}$ and $\omega_g L I_{gd}$, as well as disturbances E_{gd} and E_{gq} on the dq axes, results in similar open-loop transfer functions G_{OL-c} , including the PI controller for both axes.

$$G_{OL-c} = \frac{k_{pc}s + k_{ic}}{s} \cdot \frac{1}{Ls + R} \quad (40)$$

where k_{pc} and k_{ic} are the proportional and integral gains of the current controller.

To cancel the open-loop pole (L/R), the controller zero is set as $k_{pc}/k_{ic} = L/R$. The closed-loop transfer function G_{CL-c} simplifies to

$$G_{CL-c} = \frac{I_{gd}}{I_{gd}^*} = \frac{G_{iOL}}{1 + G_{iOL}} = \frac{1}{(R/k_{ic})s + 1} \quad (41)$$

The pole compensation method is used to calculate k_{pc} and k_{ic} , which is straightforward to apply to a first-order system. The system's cutoff frequency ω_{cc} is defined as $\omega_{cc} = k_{ic}/R$, and the response time ensuring that the currents (I_{gd} , I_{gq}) reach 95% of their reference values is set to $t_{rc} = 3/\omega_{cc}$. The parameters k_{pc} and k_{ic} are then calculated as follows:

$$\begin{cases} k_{ic} = 3R/t_{rc} \\ k_{pc} = 3L/t_{rc} \end{cases} \quad (42)$$

To ensure that the current quickly converges to its reference value, the closed-loop dynamics are accelerated by a factor of 10, resulting in $t_{rc} = 3L/10R = 3$ ms. Consequently, the PI controller parameters are set to $k_{ic} = 1000$ and $k_{pc} = 10$.

5.2. DC Voltage Control Loop

The external loop keeps the DC bus voltage V_{dc} constant despite the current I_L variations. This is done by adjusting the reference current amplitude to control the active power flow between the grid and the DC bus. For high accuracy, a PI regulator is often used to minimize the error between V_{dc}^* and V_{dc} . Since the current (internal) loop is faster than the voltage (external) loop, the current loop's transfer function does not affect voltage loop stability and can be considered as a unit gain in steady state. Assuming that the disturbance caused by I_L is fully compensated for by the PI controller, the DC voltage control scheme can be simplified.

From the simplified diagram in Figure 11, the open-loop transfer function is

$$G_{OL-v}(s) = \frac{k_{pv}s + k_{iv}}{s} \cdot \frac{1}{Cs} \quad (43)$$

where k_{pv} and k_{iv} are the proportional and integral gains of the DC voltage controller.

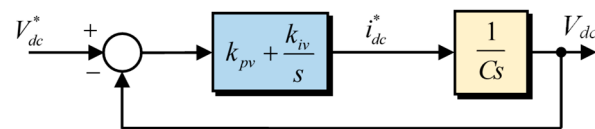


Figure 11. Simplified DC bus voltage regulation.

The closed-loop transfer function of the system is given by

$$G_{CL-v} = \frac{V_{dc}}{V_{dc}^*} = \frac{(k_{iv}/C)((k_{pv}/k_{iv})s + 1)}{s^2 + (k_{pv}/C)s + (k_{iv}/C)} = \frac{k(\tau s + 1)}{s^2 + 2\xi\omega_{cv}s + \omega_{cv}^2}. \quad (44)$$

The PI controller parameters are determined using pole placement to balance voltage loop dynamics and minimize current overshoot. For the closed-loop transfer function in (44), with poles placed for a damping factor $\xi = 0.7$ and cutoff frequency $\omega_{cv} = 3/t_{rv}$, the controller gains k_{iv} and k_{pv} are given by

$$\begin{cases} k_{iv} = C\omega_{cv}^2 \\ k_{pv} = 2\xi C\omega_{cv} \end{cases} \quad (45)$$

To ensure effective decoupling between the dynamics of the inner and outer control loops, the response time of the DC voltage control loop is set to be ten times that of the current loop, yielding $t_{rv} = 30$ ms. Using the GSC parameters provided in Table 4, the resulting PI controller parameters are $k_{iv} = 33$ and $k_{pv} = 0.46$.

Table 4. GSC parameters.

Parameters	Values
Phase grid voltage (Vrms)	55
Grid voltages frequency f (Hz)	50
DC-link capacitor C (mF)	3.3
Nominal ac filter resistance R (Ω)	1
Nominal ac filter inductance L (mH)	10

6. Experimental Results

To validate the grid-connected WECS with the WTE shown in Figure 12, a test bench was set up, as detailed in Figure 13. Its main components are as follows:

- A 3 kW DFIG, with parameters listed in Table 3, is controlled via a PWM converter connected to its rotor winding (RSC). The DPC algorithm is implemented through the RSC using a dSPACE DS1104 board.
- A 3 kW SEDCM, with parameters listed in Table 2, is used to emulate a wind turbine of the same power, as detailed in Table 1. The motor's torque is controlled by regulating the armature current through a DC-DC buck converter. The emulator's control is implemented using the same dSPACE DS1104 board.
- The GSC is a PWM rectifier connected to the grid through an inductive filter and an isolation transformer. This GSC is controlled to provide a 220 V DC voltage at a unity power factor to the DC bus, which manages the rotor voltages of the DFIG. Additionally, it ensures the connection of the DFIG's rotor side to the grid. The GSC is controlled using the VOC method, implemented through a second dSPACE DS1104 board. The GSC parameters are listed in Table 4.

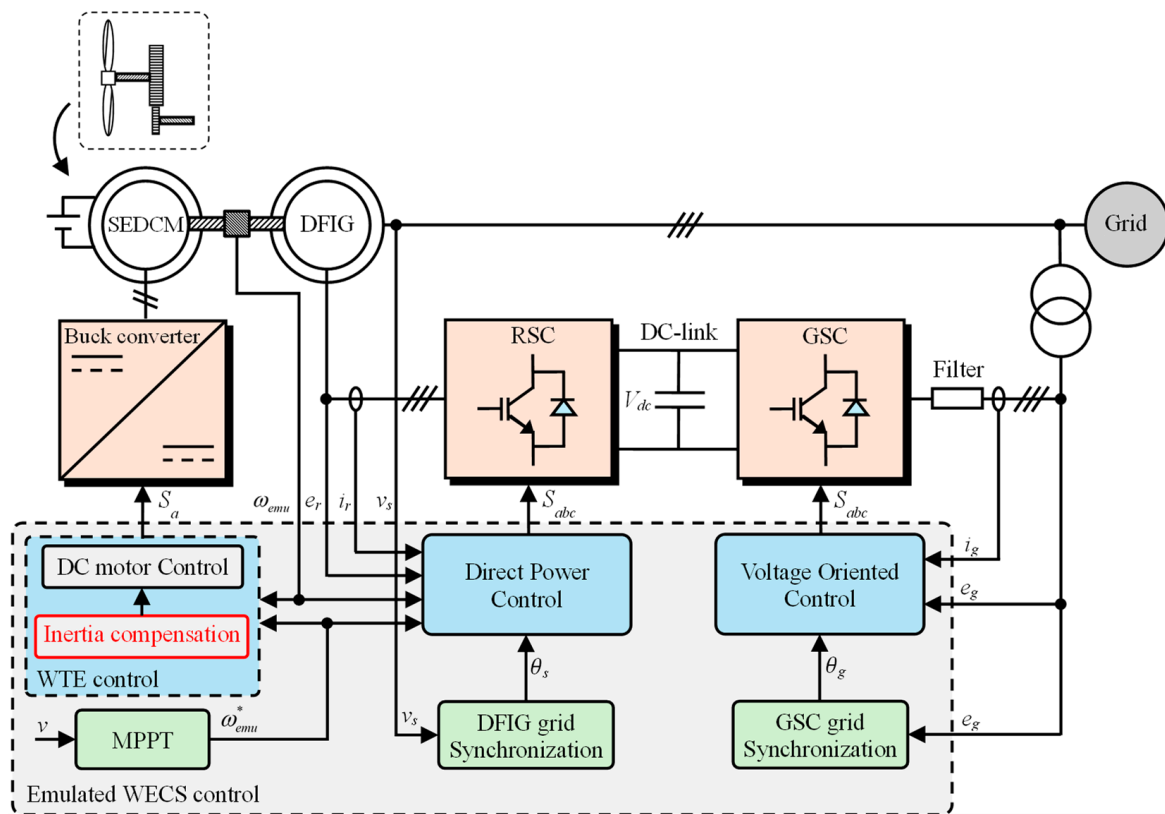


Figure 12. Overall control scheme of the proposed WTE for a DFIG-based WECS.

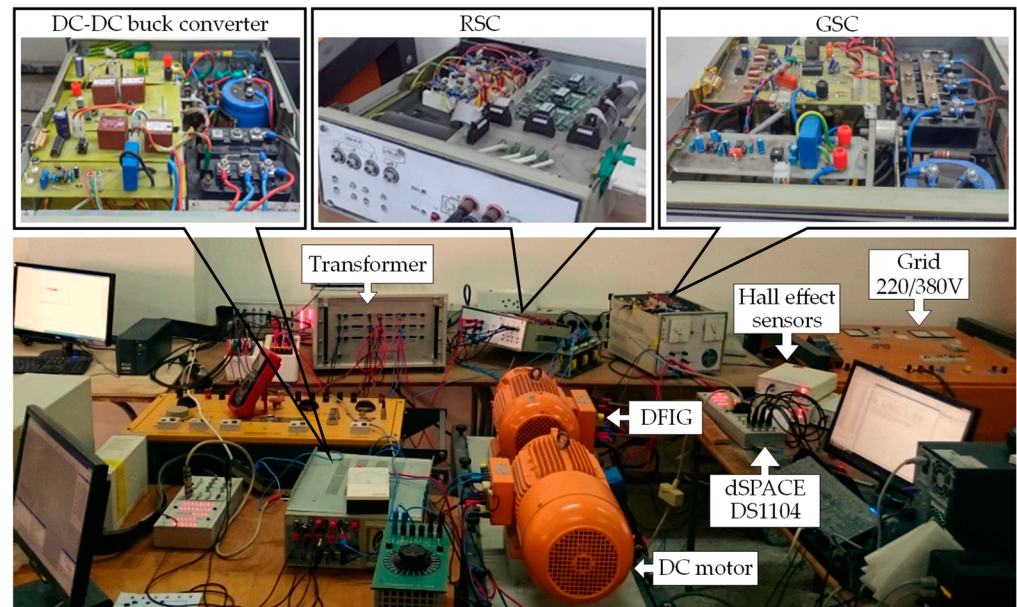


Figure 13. Overview of the test bench.

Current and voltage measurements are performed using Hall effect sensors from LEM (LA 50P and LV 25P). Finally, speed measurements are taken via a 5000-point incremental encoder installed on the DFIG. The control algorithms of the DC-DC buck converter, the RSC, and the GSC are implemented with a switching frequency f_w of 7.5 kHz and a sampling period T_s of 100 μ s.

6.1. WTE Results

Figure 14 presents the experimental results of the WTE, obtained for the same wind profile used in the simulation, which is illustrated in Figure 14a. The experimental results show a very strong similarity to those of the simulation in Figure 7. In Figure 14b, the overlap between the reference speed signal and the measured speed is evident. The results for the torque delivered by the turbine model T'_T , the torque of the SEDCM T_M , and the compensation torque T_{com} , estimated using the proposed technique, are shown in Figure 14c. For comparison, the difference $T_M - T_{com}$ is also added to the figure, demonstrating a perfect overlap with T'_T , confirming the high accuracy of the T_{com} estimation method.

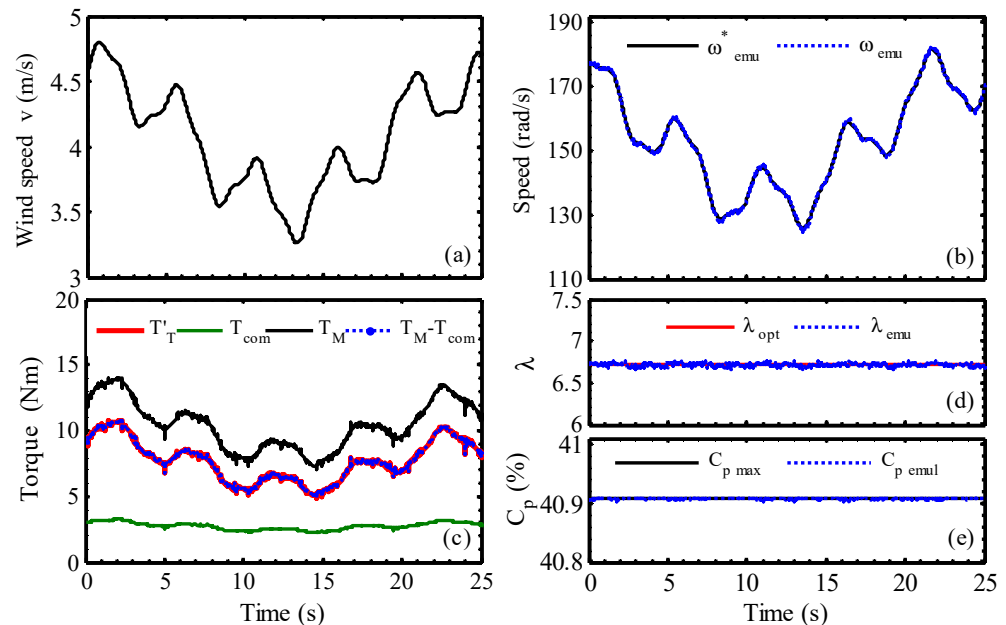


Figure 14. Experimental results for the wind turbine emulator: (a) wind speed profile, (b) rotational speed, (c) mechanical torque provided by the SEDCM, reference torque, and compensation torque, (d) tip-speed ratio λ , and (e) theoretical and practical power coefficient C_p .

Similarly, the effectiveness of the compensation and control is validated by the results in Figure 14d,e, where the instantaneous values of λ_{opt} and $C_{p,max}$ are overlaid on their theoretical values. The similarity between the experimental and simulation results confirms the effectiveness of the proposed torque compensation and WTE control.

6.2. DFIG Results

Figures 15 and 16 illustrate the experimental results obtained after the DFIG was started and synchronized with the grid, under the previously defined conditions. The results are presented within the framework of rotational speed control.

Figure 15 shows the results for the stator quantities. The voltages applied to the DFIG stator are depicted in Figure 15a. It can be observed that V_{sd} is 380 V, while V_{sq} is zero, which aligns with the expectations of the stator's PLL, where the stator voltage is oriented along the direct axis. Figure 15b demonstrates good synchronization between the stator voltage and the phase current "a" with the current amplitude being proportional to the active power injected into the grid. Figure 15c presents the active and reactive powers at the stator, overlaid with their setpoints, where the chosen reference for reactive power is zero. This confirms that the reactive power control loop is functioning correctly. Likewise, the stator active power P_s precisely follows its reference, which is always negative, indicating the DFIG operates in generator mode. Finally, Figure 15d illustrates the stator currents, showing that I_{sq} is kept at zero since it is proportional to the reactive power Q_s , while I_{sd} is negative, reflecting the active power P_s .

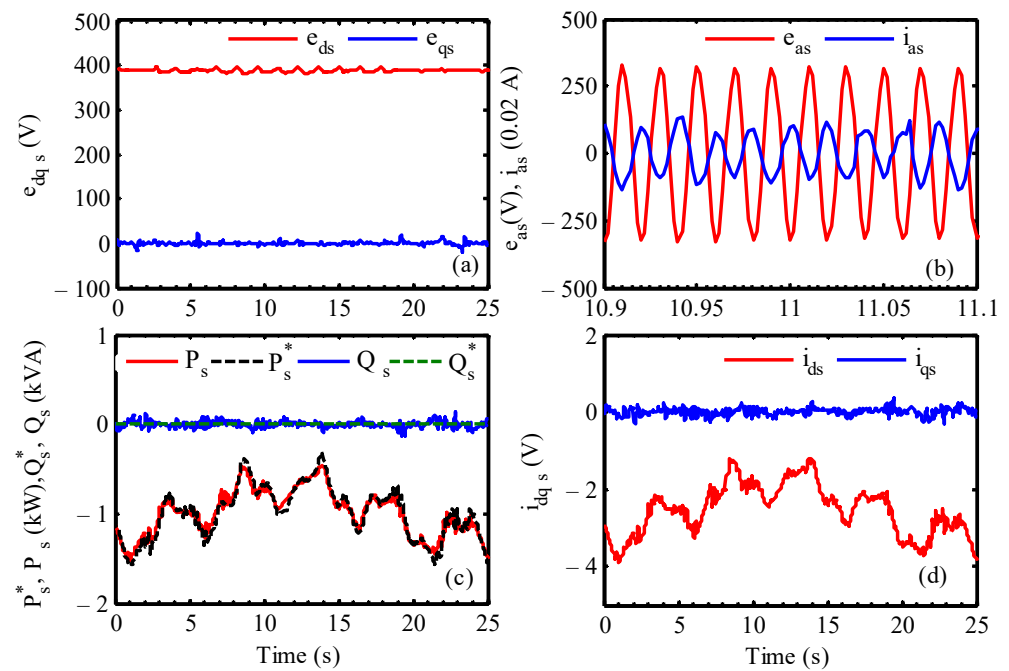


Figure 15. Experimental results for the DFIG stator quantities: (a) dq -axes voltage components, (b) phase a current and voltage, (c) active and reactive power and their references, and (d) dq -axes current components.

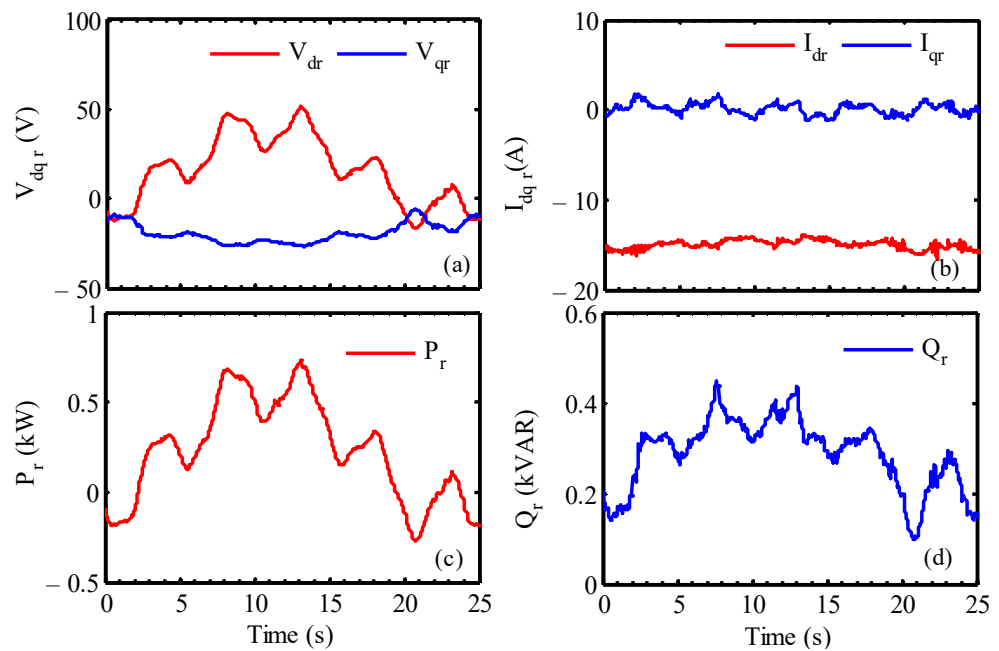


Figure 16. Experimental results for the DFIG rotor quantities: (a) dq -axes voltage components, (b) dq -axes current components, (c) active power, and (d) reactive power.

Figure 16 presents the experimental results for the rotor quantities. Figure 16a shows the control voltages V_{dr} and V_{qr} , where V_{dr} follows the shape of P_s , being negative in super synchronous mode and positive in sub synchronous mode, while V_{qr} takes very low values, ensuring decoupling and compensation. Figure 15b illustrates the rotor currents I_{rd} and I_{rq} . It is observed that I_{rq} remains very small regardless of the DFIG speed, as it is only necessary for decoupling linked to active power P_s . In contrast, I_{rd} varies depending on the power P_s delivered to the grid. Figure 15c,d show the rotor active and reactive power, respectively. The active power P_r oscillates around an average value of approximately

250 W, representing the power dissipated due to Joule heating in the machine's resistances. The variable part of P_r corresponds to the electrical power produced by the rotor in super synchronous mode or absorbed by the rotor in super synchronous mode. The reactive power Q_r remains positive in both modes as it is used to magnetize the DFIG.

6.3. GSC Results

Figure 17 shows the experimental results obtained for the GSC. In Figure 16a, the DC bus voltage V_{dc} oscillates around 220 V with a ripple rate of 5 to 10%. Figure 17b presents the waveform of the three-line currents i_g from the grid, with variable amplitudes depending on the current I_{dc} , which regulates the V_{dc} voltage. Figure 17c shows that the I_{gdq} currents closely follow their setpoints imposed by the internal control loops. In this figure, the quadrature current I_{gq} is zero to ensure unity power factor operation, as this current represents the reactive power Q_g . Similarly, the direct current I_{gd} reflects the active power P_g injected into the grid. The waveforms for active power P_g and reactive power Q_g are shown in Figure 17f. Figure 17e presents the direct and quadrature grid voltage components E_{gdq} , while Figure 17d shows the phase b alternating voltage component E_{gb} and the line current (multiplied by five) of the same synchronized phase in opposition.

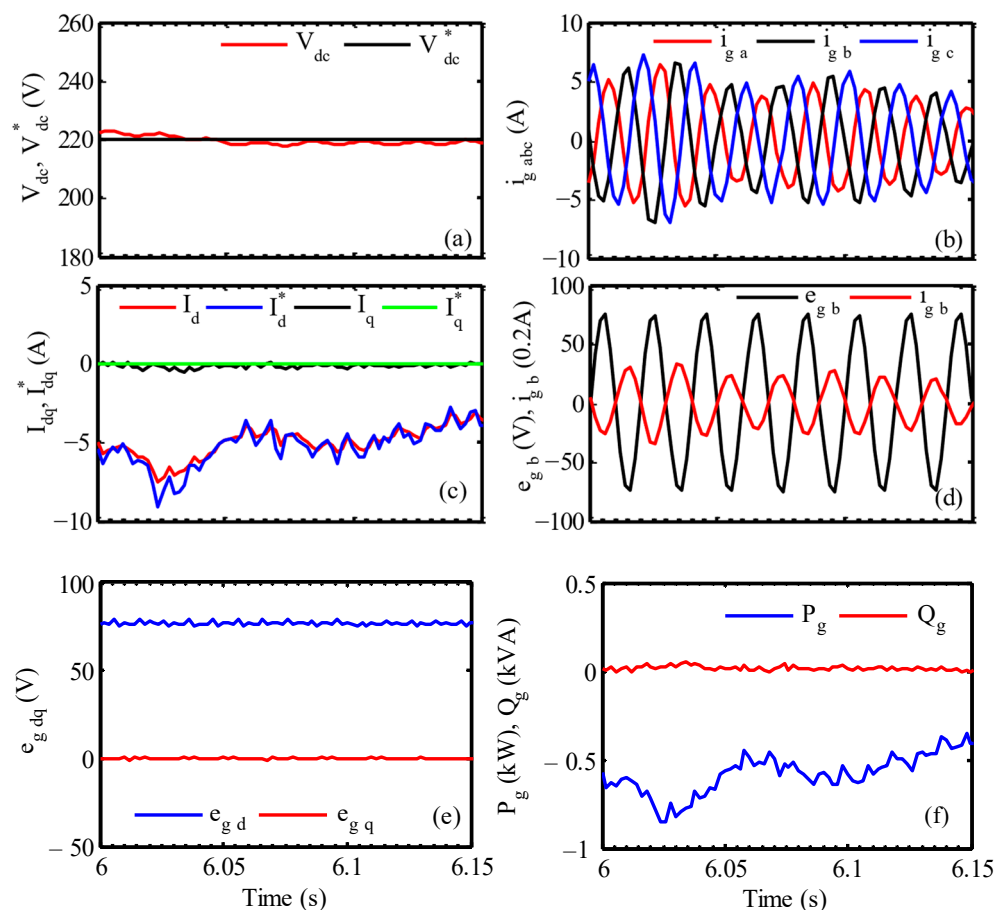


Figure 17. Experimental results for the GSC: (a) DC bus voltage and its reference, (b) three-phase currents, (c) dq -axes current components with their references, (d) voltage and current of phase b , (e) dq -axes voltage components, and (f) active and reactive power.

The experimental results for the GSC demonstrate that the DC bus voltage is well regulated around 220 V, with efficient injection of active power into the grid and reactive power maintained at zero. These results indicate that the GSC's performance meets expectations and design specifications, ensuring optimal integration with the electrical grid. Additionally, synchronization of the grid-side voltages is successfully achieved.

7. Conclusions

This paper presented the development of a comprehensive 3 kW WECS emulator with a grid-connected DFIG, enabling detailed real-time analysis of wind energy systems. The emulator successfully replicates the mechanical behavior of wind turbines using an SEDCM while allowing for independent control of active and reactive power exchanged with the grid via the DFIG. The experimental results demonstrate that the system operates effectively under various wind profiles, accurately validating the implemented MPPT strategy and grid synchronization method. This emulator offers significant potential for future research in WECS control strategies and grid integration, providing a valuable tool for the development of sustainable wind energy solutions.

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