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RESEARCH ARTICLE

Pitch System Input Contribution on Wind Turbine Main Shaft Bearing Currents

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ABSTRACT Drivetrain reliability is a critical challenge for modern large-scale wind turbines, with electrically induced main shaft bearing currents acting as a major threat to bearing longevity. This paper investigates common-mode voltage (CMV) propagation in wind turbines by comprehensively evaluating and comparing two distinct industrial pitch power topologies: grid-connected and converter-linked configurations. Numerical finite-element simulations demonstrate that slip ring parasitic capacitance and tower cable inductive coupling serve as the dominant transmission pathways to the drivetrain. Our analysis reveals a crucial finding: the coupling magnitude and spectral complexity of the main bearing voltage are highly dynamic, governed by the primary transmission mechanism where intermodulation sidebands dominate the converter-linked circuit, whereas discrete grid-harmonic unbalances dictate the grid-connected baseline. Based on these mechanisms, equivalent circuit models are established to derive the bearing voltage ratio (BVR) under various operation states. The proposed theoretical and simulation frameworks are fully validated through multi-level experiments, encompassing both white-box laboratory tests and on-site wind turbine measurements. The quantitative insights obtained from this study provide a solid analytical framework and concrete guidance for optimizing subsystem insulation and implementing targeted mitigation strategies in future wind turbine designs.

INDEX TERMS Wind energy, bearing currents, common mode voltage, impedance model, electrical drive, pulse width modulation.

I. INTRODUCTION

Wind energy has experienced rapid development over the past few decades, with wind turbine systems growing significantly in both power capacity and system integration [1]. This evolution has led to increasingly complex architectures, introducing new challenges in terms of reliability and maintainability. As the scale and complexity of wind turbines rise, ensuring high availability becomes more difficult, particularly when subsystem faults or failures cause unplanned outages and loss of energy production [2], [3], [4].

Among various reliability concerns, the drivetrain has a prominent influence on the overall availability of wind

turbines [2], [5]. Within the drivetrain, the main shaft bearing serves as a critical mechanical link between the rotating and stationary components. Although main bearing failures may not be the most frequent [6], they are among the most costly due to the bearing's size, its deep integration within the structure, and the complexity involved in replacement and maintenance. Consequently, the reliability of the main bearing has become an area of focused attention [4].

Most existing studies addressing main bearing failure mechanisms concentrate on mechanical and material factors, such as overload, misalignment, and lubrication degradation [7], [8]. In contrast, the electrical aspects—specifically bearing currents—have received relatively little attention [9], [10]. While the degradation caused by electrical bearing currents tends to occur over extended operational periods, these

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currents can induce early-stage damage such as localized heating, electro-erosion, and pitting [7], [11]. These initial defects may not be catastrophic on their own but can significantly accelerate other failure processes, ultimately leading to bearing failure. Given the high costs and operational risks associated with main shaft bearing damage, the contribution of electrically induced stresses must not be neglected in reliability assessments and failure mitigation strategies.

The issue of bearing currents is not new and was first identified in electric machines, where it has been extensively studied over the past decades [12], [13], [14]. These currents are primarily induced by CMV in the stator windings, and the research focus has historically been placed on understanding the mechanisms of CMV generation and their transmission paths to motor bearings. Among the various influencing factors, the adoption of power electronic devices—particularly those employing pulse-width modulation (PWM) technology—has significantly intensified the severity of bearing current problems. The high switching frequencies and steep voltage edges associated with PWM converters have been shown to produce substantial CMV, leading to increased dielectric stress across bearing insulation and subsequent electrical erosion [15], [16], [17], [18].

In contrast to the extensive research on bearing currents in electric machines, the problem of main shaft bearing currents in wind turbines has received considerably less attention [19]. The specific sources of CMV and their transmission mechanisms within the wind turbine environment remain largely unexplored. In study [20] investigated the effects of external environmental charge accumulation impact on main shaft currents but did not present a systematic circuit-based transmission model. In another effort, Study [21] proposed a basic coupling model for CMV-induced shaft voltage but lacked a detailed analysis of the underlying coupling mechanisms, such as capacitive paths through structural elements or cable bundling effects. These gaps highlight the need for a more rigorous and system-level investigation into CMV sources and their transmission in wind turbines, particularly regarding its impact on main shaft bearing integrity.

To address these gaps, this work presents a systematic investigation of CMV transmission within the power supply path of the pitch system. The analysis begins by identifying all possible CMV sources that may contribute to the bearing current problem. The corresponding transmission paths from these sources to the main shaft are analyzed in detail, with special emphasis on two key coupling mechanisms: the slip ring coupling and the cable-to-cable electromagnetic coupling inside the tower. For each mechanism, theoretical modeling and numerical simulations are employed to characterize the coupling behavior and quantify its influence.

Building upon these analyses, comprehensive equivalent circuit models are constructed to represent the entire CMV transmission network under two typical power supply configurations. These models enable further analytical derivation of the contribution from each CMV source to the bearing

voltage. By quantifying the BVR of each source, the study provides insights into the dominant contributors to shaft bearing stress and lays a foundation for mitigation strategies in future wind turbine designs.

Building upon the identified research gaps and the objectives outlined above, this paper is structured as follows. Section II introduces the pitch system power supply configurations in wind turbines and analyzes the possible transmission paths of CMV to the main shaft bearing. Section III identifies the primary CMV sources along these paths. Section IV focuses on key coupling mechanisms and develops equivalent circuit models for CMV propagation. Section V derives the BVR and quantitatively assesses the contribution of each CMV source. Section VI presents laboratory and field measurements to validate the proposed models and theoretical analysis.

II. PITCH SYSTEM POWER SUPPLY TOPOLOGIES IN DIFFERENT WIND TURBINES

The pitch control system is essential for optimizing wind energy capture by adjusting blade pitch angles in response to wind conditions. In extreme wind events, it performs automatic feathering to reduce aerodynamic loads and protect the turbine from damage.

A. PITCH SYSTEMS IN WIND TURBINES

Over the past decades, pitch control systems have evolved alongside wind power technology. Early designs, such as the Gedser Wind Turbine (1957) [22], employed mechanical linkages and springs to adjust blade angles. While structurally simple, these systems lacked responsiveness and precision under varying wind conditions. To enable active power regulation, hydraulic pitch systems became widespread in the 1980s, exemplified by the Vestas V15-30kW [23]. Despite their improved control capabilities, hydraulic systems suffered from large volume, complex piping, and frequent maintenance issues due to oil leakage. To address these limitations, modern turbines increasingly adopt fully electric pitch systems. By integrating independent converter and electric motors on each blade, these systems achieve a distributed architecture that enhances both control flexibility and system reliability. Although modern wind turbines may differ in drivetrain configurations—such as geared and gearless types—their pitch control systems share a largely consistent structure and arrangement, as illustrated in Fig. 1.

B. POWER SUPPLY IN TYPICAL WIND TURBINE

In practical wind turbine applications, the pitch system consists of more than just the core components (pitch converter and pitch motor) shown in Fig. 1. Since the entire system is installed within the rotating hub, additional subsystems are required to ensure stable operation. Among these, the power supply is particularly critical, as it must reliably deliver energy to the rotating assembly under varying mechanical and environmental conditions. Fig. 2 illustrates a typical wind

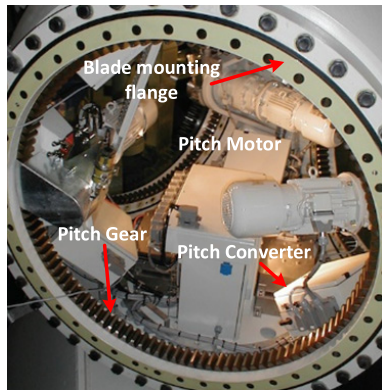


FIGURE 1. Typical electrical pitch system in modern wind turbine.

turbine pitch system configuration, highlighting the associated power supply path. In the figure, different colored dashed boxes are used to represent various physical locations within the wind turbine, helping to distinguish the different sections and their connections.

Each blade in a modern wind turbine is typically equipped with its own pitch motor and converter. For clarity, Fig. 2 illustrates only one set of pitch components within the hub to better highlight the other subsystems. Beyond the core control elements, the hub also includes a backup power system. This system typically consists of a battery or super capacitor unit coupled with a DC/DC converter, whose output is directly connected to the DC bus of the pitch converter.

This backup power system ensures emergency actuation capability in the event of grid failure or system faults. However, the main power supply for the pitch control system is drawn from the grid side. As shown in Fig. 2, the grid voltage is stepped down to 690V by the main transformer. The pitch system taps power from the secondary side of the transformer at 690V, which is subsequently converted to 400V via the auxiliary transformer. Then the power is transmitted through long cables along the tower and reaches the slip ring installed on the rear of main shaft. After passing through the slip ring, the power travels through the hollow main shaft and arrives at the pitch converters and pitch motors located within the hub. The power transmission path is concluded as:

Grid (3.5kV) → main transformer (690V) → auxiliary transformer (400V) → pitch power cable bundle → slip ring → hollow shaft → pitch convert → pitch motor.

Once power enters the nacelle, it passes through the slip ring, which establishes both mechanical and indirect electrical connections between the rotating hub and other nacelle components. In all wind turbines, the slip ring is mounted at the rear end of the main shaft, which is sequentially connected to the hub frame, grounding disk, and main bearing. The arrangement differs slightly across turbine types: in geared turbines, the shaft connects to the gearbox after the bearing; in gearless turbines, it passes directly through the generator. These components are linked via metal-to-metal interfaces, forming unintended electrical paths. Such paths can lead to

shaft voltage buildup, causing potential differences across the main bearing, which may result in dielectric breakdown and reduced bearing lifespan. This issue will be further discussed in section II-D.

The slip ring serves as a key interface in the power supply system, enabling electrical connection between stationary and rotating components. Beyond its intended function, it also forms part of the unintended electrical network established through various metal-to-metal mechanical connections within the drivetrain. As shown in Fig. 3, a typical slip ring consists of a stator, rotor, and bearing assembly. The stator includes an external mounting flange and internal brushes connected to input cables. The rotor side features both a mounting flange and a set of conductive tracks on its outer surface, which connect to output cables. Through this brush-track interface, the slip ring maintains a stable electrical connection between the stator and rotor during continuous rotation.

From an electrical perspective, both the brushes (shown in red in Fig. 3) and the conductive tracks (shown in golden) within the slip ring are metallic components, while the remaining structural parts—namely the stator (blue) and rotor (green)—are made of insulating materials. When the slip ring is mounted on a hollow metal shaft, the rotor components rotate synchronously with the shaft and are therefore electrically and mechanically stationary with respect to it. In this configuration, the conductive rotor tracks, the hollow shaft, and the insulating layer in between (green area) effectively form a parallel-plate capacitor. This parasitic capacitance is inherently built into the mechanical structure of the slip ring and the shaft, creating an unintended electrical coupling path. As a result, the CMV within the slip ring's power chain can capacitively couple to the main shaft, introducing CMV into the drivetrain—particularly across the main shaft bearing, where it may induce harmful circulating currents.

C. COMPACT INTEGRATED WIND TURBINE PITCH SYSTEM POWER SUPPLY

In addition to the standard pitch system supply architecture shown in Fig. 2, some offshore gearless wind turbines adopt alternative power delivery strategies to accommodate compact mechanical designs and system integration needs [24], [25], [26], [27], [28], [29]. These configurations are particularly relevant in space-constrained nacelle environments. A representative layout is illustrated in Fig. 4. The generator is mechanically connected to the hub via the main shaft and electrically interfaced with the grid through a back-to-back converter, which feeds power to the grid via the main transformer.

In this setup, the pitch system draws power directly from the main converter's DC bus. The DC power is transmitted through the slip ring into the hub, where the pitch converter operates without a rectifier stage. Instead, it utilizes the supplied DC voltage directly to drive the pitch motor and perform pitch operations.

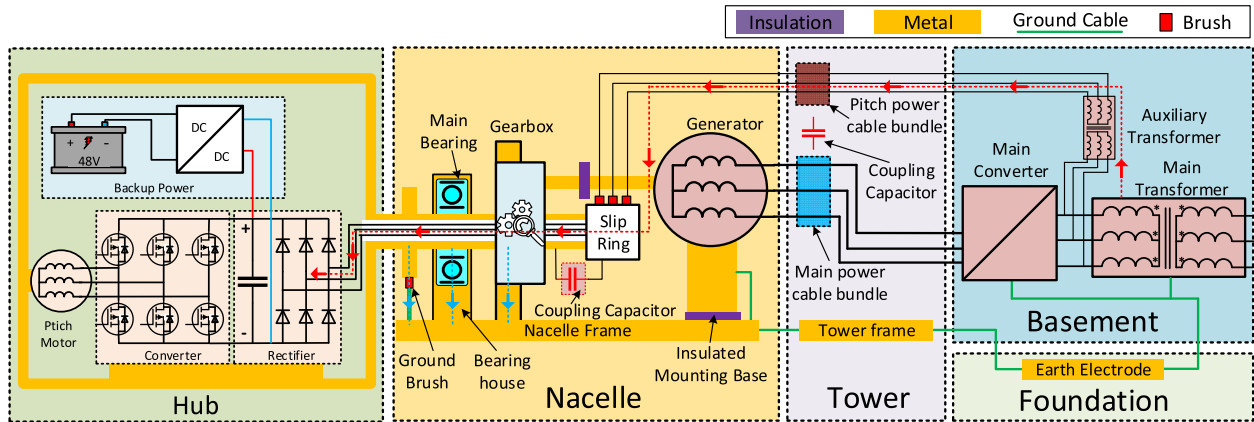


FIGURE 2. Electrical configuration of typical wind turbine.

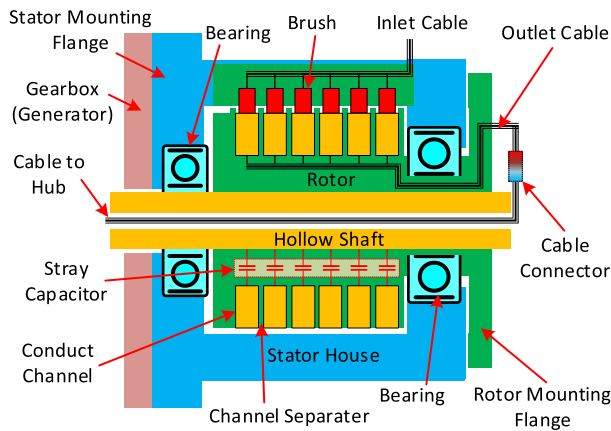


FIGURE 3. Cross section view of typical slip ring.

This design enables a more compact turbine structure with higher system integration, reducing the number of external connections and thereby enhancing overall reliability and reducing the manufacturing cost. However, sourcing power directly from the main converter's DC bus increases the CMV present at the slip ring and the main shaft. This direct electrical coupling between the pitch converter and the main converter can introduce a range of potential issues, including increased electromagnetic interference and circulating currents.

D. INDUCED CMV IMPACT ON MAIN SHAFT BEARING

In electrical systems, voltage and current components are typically classified into differential-mode (DM) and common-mode (CM) components. The DM components circulate within closed loops formed by the active conductors of the system, while CM components are superimposed equally on all conductors and return through a common reference path—typically ground. When a well-defined return path to ground is absent, CM components manifest as floating potentials and tend to establish current paths through the lowest impedance to ground available in the system.

As analyzed in section B, due to the capacitive coupling effect of the slip ring, the CMV can be induced onto the main shaft. This CMV seeks return paths through components electrically connected to the shaft, favoring those with lower impedance to ground. According to the configurations shown in Fig. 2 and Fig. 4, the main shaft is sequentially connected to the hub frame, ground brush, main bearing, and gearbox. In principle, the shaft is grounded via the designated ground brush, which connects to the grounded nacelle frame, while the hub frame remains electrically floating—grounded indirectly through the shaft.

In practice, however, multiple alternative return paths may exist. The inner race of the main bearing is fixed to the shaft, while the outer race is mounted within the grounded bearing housing, forming a conductive path. Similarly, the gearbox—comprising shafts, bearings, and a grounded metallic housing—provides another effective return path from the shaft to ground. These three paths—the ground brush, main bearing, and gearbox—form a parallel network for CMV return currents. These paths are indicated by the blue dashed arrow line in Fig 2 and Fig 4.

Among them, the main shaft bearing, typically a large-size roller bearing, presents the lowest impedance due to its construction and contact area, making it the most likely path for common-mode current. This poses a significant risk to bearing reliability, which will be further examined in subsequent sections.

III. CMV SOURCES IN PITCH SYSTEM POWER SUPPLY

Section II examined the power supply configuration of wind turbine pitch systems, highlighting the mechanisms through which CMV couples onto the drivetrain and affects main shaft bearings. However, the root causes of CMV at the supply side remain insufficiently characterized. This section aims to identify the primary CMV generation mechanisms, and to establish both an equivalent circuit model and a simulation-based analysis framework to quantify their impact.

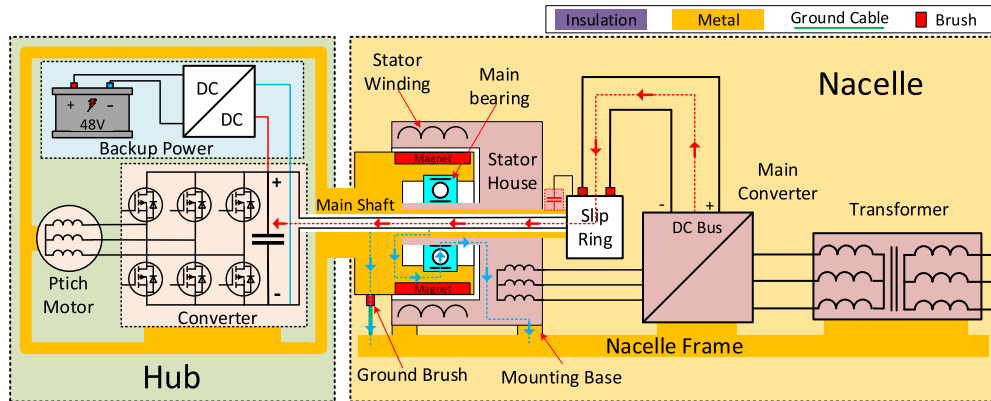


FIGURE 4. Electrical configuration of compact integrated wind turbine.

TABLE 1. Max allowed harmonic under class 2 grid power supply.

Harmonic order	Frequency (Hz)	Max allowed (%)	Peak amplitude (V)
Fundamental	50	2	6.5
2 nd	100	2	6.5
3 rd	150	5	16.3
5 th	250	6	19.6
7 th	350	5	16.3
9 th	450	1.5	4.9
11 th	550	3.5	11.4
13 th	650	3	9.8

A. GRID CMV AND PHASE IMBALANCE

In conventional pitch supply configurations (Fig. 2), the power for the pitch system originates from the grid. Although the voltage passes through a main transformer and an auxiliary transformer, only its amplitude is modified—its inherent CMV content remains unchanged. Thus, grid-side CMV is considered the primary source within the supply system.

The 400 V source delivered to the nacelle feeds multiple three-phase loads, including the pitch system, yaw motors (rated at 200–300 kW), and gearbox cooling equipment, as well as single-phase auxiliary loads such as lighting, air-conditioning systems, and blade heating units, etc. Both unbalanced operation of the three-phase loads and the presence of single-phase loads operating asynchronously can lead to frequent phase imbalances. Such imbalances cause a shift in the system neutral point, thereby giving rise to a 50 Hz CMV component. Additionally, residual unbalance inherent in the upstream grid supply further exacerbates CMV presence.

According to IEC 61000-2-4:2024 [30], the grid power supply in wind energy systems is classified as Class 2, corresponding to general industrial environments. Based on the standard's specified limits for voltage unbalance and individual harmonics, the peak values of the major components under a 400 V AC supply are calculated. The results are summarized in Table 1.

B. MAIN CONVERTER CMV CABLE COUPLING

In the configuration illustrated in Fig. 2, the power supply to the pitch system is delivered through long cables routed along the wind turbine tower into the nacelle. For a typical 2 MW turbine, the cable length is approximately 80 m. In such installations, several physical effects may arise, including resistive voltage drop, dielectric loss, wave reflection, and electromagnetic coupling. Among these, the voltage drop and dielectric loss do not contribute to CMV propagation. The wave reflection effect becomes significant only when the cable length exceeds one-tenth of the effective wavelength. However, since the dominant frequency components in the cables are below 10 kHz—with a corresponding wavelength of approximately 20 km ($\lambda/10 \approx 2 \text{ km} \gg 80 \text{ m}$)—wave reflection can be safely neglected. Under these conditions, electromagnetic coupling, including both mutual inductive and capacitive interactions, becomes the dominant mechanism governing CMV propagation between adjacent cable groups.

Within the tower section shown in Fig. 5, the pitch system power cables (Group A) are routed in parallel with the generator-to-main-converter cables (Group B). The latter carries CMV components not only at the generator's fundamental frequency f_G but also at the main converter's switching frequency f_M and its harmonics.

For practical reasons, both cable groups are typically bundled or routed closely together along the tower. This close proximity within a conductive enclosure influences the mutual coupling between the two cable sets. As illustrated in Fig. 5(a) and Fig. 5(b), the cables may be mounted either directly face-to-face or supported on mounting frames. The long parallel layout extends the electromagnetic interaction path, while the grounded tower wall alters the distribution of electric and magnetic fields, thereby affecting the coupling characteristics. Consequently, CMV originating from the main converter side can propagate into the pitch system through distributed coupling, potentially impacting the electrical performance and reliability of the main bearing.

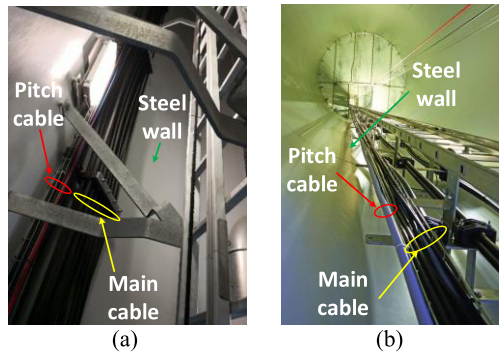


FIGURE 5. Cable bundle inside wind turbine tower.

TABLE 2. CMV components in shared dc bus.

Component Type	Frequency Expression	Description
Rectification	f_R	$3f_G=150\text{Hz}$
Switching Frequency	f_M, f_P	kHz range
Harmonic Components	kf_R, kf_M, kf_P	k is integer
Sidebands	$f_M \pm kf_R, f_P \pm kf_R$	
Beat Frequency	$ f_M \pm f_P $	
Intermodulation Components	$ mf_M \pm nf_P $	m, n is integer

C. SHARED DC BUS CMV IN COMPACT INTEGRATED WIND TURBINE

In the pitch system power supply configuration shown in Fig. 4, the compact layout eliminates the influence of grid-side disturbances and cable coupling effects. However, this configuration introduces a new challenge: the direct electrical coupling of the DC buses of the main converter and the pitch converter. As a result, the CMV components generated by both converters are directly superimposed at the shared bus node. Moreover, the rectification process inherently introduces low-frequency CMV components into the DC bus. Consequently, the resulting CMV in this configuration not only combines switching-related harmonics from both converters but is also influenced by low-frequency components from the rectifier stage, making its spectral content considerably more complex.

In the shared DC bus configuration, a significant common-mode component at three times the generator frequency (f_G) is introduced by the generator and the rectifier (f_R). It coexists with the switching frequencies of the main and pitch converters (f_M and f_P), along with their respective harmonics (kf_R, kf_M, kf_P). These components interact to produce complex spectral features: high-frequency harmonics modulate low-frequency components, forming sidebands, while beat-frequency and intermodulation effects emerge from interactions between f_M and f_P . The resulting CMV spectrum is significantly enriched, as summarized in Table 2.

These mixed-frequency components can propagate through the slip ring and induce bearing voltage stress due to their broad spectral distribution.

IV. CIRCUIT MODEL AND SIMULATION

In Sections II and III, the power supply configurations of the pitch system and the possible sources of CMV were analyzed, along with a preliminary assessment of CMV-induced impacts on the main shaft bearing. Within the identified CMV transmission path, two dominant coupling mechanisms were highlighted: the slip ring interface and cable coupling. To accurately evaluate their influence, it is essential to further investigate these coupling mechanisms and develop effective models. Based on this, a systematic transmission model should be established to quantify the contribution of CMV to the shaft bearing voltage.

A. SLIP RING COUPLING

As analyzed in Section II-B, the coupling effect within the slip ring plays a critical role in the CMV transmission path. Since the strength of this coupling directly influences the voltage induced on the main shaft, it is essential to conduct a detailed modeling and analysis of the slip ring's internal coupling behavior.

The coupling effect in the slip ring is primarily caused by parasitic capacitance, which requires field-based computational modeling for accurate analysis. Based on the geometric parameters of the laboratory slip ring described in Section VI, a detailed Ansys Q3D model was developed to quantify the stray capacitance. As shown in Fig. 3, the stator-side brushes are mounted on an insulating support and located far from the rotor shaft, resulting in low parasitic capacitance and high impedance. Compared with the capacitive coupling between the rotor-side tracks and the rotor body, the brush contribution is negligible and further reduced in practical wind turbine applications due to larger slip-ring dimensions. Therefore, the model was simplified to include only the rotor section, consisting of the slip-ring tracks, rotor body, and main shaft, while preserving the dominant electrical characteristics.

The simplified model is illustrated in Fig. 6a, where the red regions represent the conductive copper tracks, the green structure corresponds to the insulated rotor body (which supports the slip rings tracks and connects the shaft), and the steel hollow main shaft is shown in orange. In the simulation setup, the far end of the main shaft (left side in the figure) is defined as the reference ground.

The stray capacitance curve between the slip-ring tracks and the main shaft is shown in Fig. 6b, it remains nearly constant at approximately 24.5 pF for frequencies below 10 kHz. A noticeable drops in capacitance are observed around 50 kHz, with the value decreasing to approximately 23.9 pF at 150 kHz. Although these reductions are visible in the curve, the absolute variation in capacitance remains small. Moreover, since the CMV components of our study are typically below 10 kHz, the stray capacitance in this range can be considered as a constant value. A detailed comparison between simulation and experimental results will be further discussed in Section VI-A3.

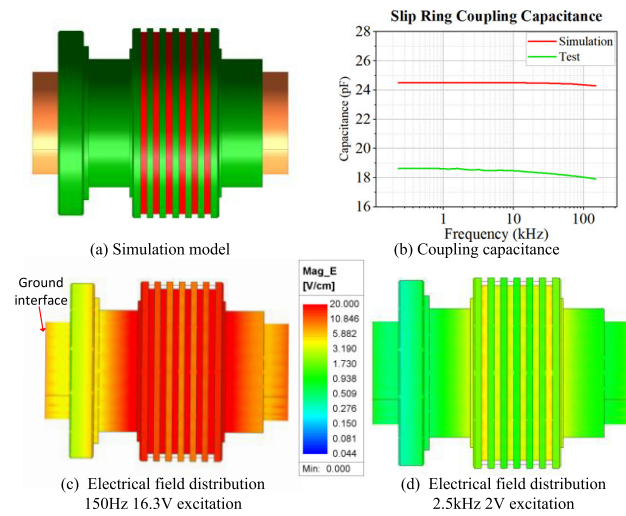


FIGURE 6. Slipring coupling simulation result.

The simulated electric field distributions under low- and high-frequency conditions are illustrated in Fig. 6c and Fig. 6d, respectively. In these cases, a 150 Hz, 16.3 V peak voltage and a 2.5 kHz, 2 V peak voltage—representing typical low- and high-frequency excitations from Table 1—were applied between the slip-ring track and the shaft to evaluate the field behavior.

As shown in the field distribution plots, the electric field intensity is higher at low frequency. In both cases, the field gradient is primarily concentrated around the slip-ring tracks and rotor body, while little variation is observed between adjacent rings due to the lack of potential difference. Moreover, in the low-frequency case, the field strength along the shaft decreases toward the grounded end, indicating a small but non-negligible potential difference along the conductive shaft.

B. CABLE COUPLING

In this study, cable coupling is identified as the major transmission path for the CMV originating from the main converter. To analyze the influence of electromagnetic coupling, different cable mounting conditions shown in Fig. 5 should be modeled. Since intra-group crosstalk has negligible impact on inter-group coupling, each three-phase bundle can be equivalently represented by a single conductor. Accordingly, the coupling between the two cable groups is simplified to the problem of two parallel long conductors mounted on a grounded metallic plate, as illustrated in Fig. 7.

In the modeled configuration, the pitch cable and main cable have radii (r_1) and (r_2), respectively, with a spacing gap (g) between them. The tower structure is represented by an infinitely large grounded steel plate. The primary difference among installation methods lies in the vertical distance (h) between the cables and the metallic surface. Therefore, the key geometric variables in this model are g and h . Furthermore, since different turbines may employ either unshielded

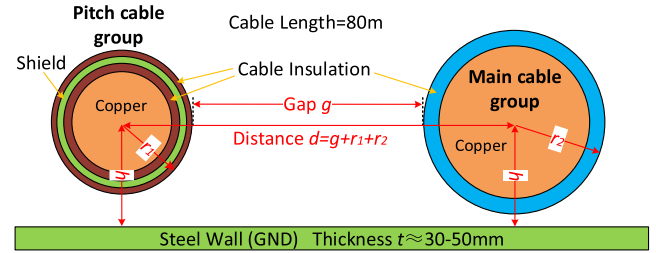


FIGURE 7. Cable coupling simulation model.

or shielded pitch cables, both configurations should be analyzed to evaluate their respective effects on electromagnetic coupling.

The proposed model is generally consistent with the classical parallel-conductor coupling configuration, with added complexity from the grounded metallic plate and possible cable shielding. Given that the tower wall thickness (30–50 mm) satisfies $t > 10\delta$ within the 50 Hz–10 kHz range (δ is the skin depth, $\delta \leq 1.35$ mm), the steel plate can be regarded as a perfect electric conductor (PEC). Thus, the coupling between the cables can be analyzed using the image method. In order to simplify the analytical derivation, assume the cable radius $r_1 = r_2 = r$, $h_1 = h_2 = h$. For the unshielded case, both mutual inductance and capacitance can be analytically derived [32], as expressed in (1) and (2).

$$M_{cable} = \frac{\mu_0 l}{4\pi} \ln \left(1 + \frac{4h^2}{d^2} \right) \quad (1)$$

$$C_{cable} = \frac{\pi \epsilon_0 l}{\ln \left(\frac{d}{r} \right) - \frac{1}{2} \ln \left(1 + \left(\frac{2h}{d} \right)^2 \right)} \quad (2)$$

where $l = 80$ m is the cable length, $\mu_0 = 4\pi \times 10^{-7}$ H/m is the vacuum permeability, $\epsilon_0 = 8.85 \times 10^{-12}$ F/m is vacuum Permittivity.

For the shielded configuration, analytical derivation is complex due to additional conductive boundaries. Thus, the coupling parameters were obtained with ANSYS Q3D field simulations based on the model shown in Fig. 7. The analysis focused on single-variable comparisons—evaluating M_{cable} and C_{cable} by varying either the inter-cable gap g (with $h = r$ and 45 mm) or the cable-to-ground distance h (with $g = 15$ mm and 45 mm). The simulation results under these different conditions are summarized in Fig. 8.

When h is fixed, both M_{cable} and C_{cable} decrease with increasing inter-cable spacing (g). Conversely, when g is fixed, both M_{cable} and C_{cable} increase as the cable-to-ground distance (h) increases. It is consistent with the trends predicted by the analytical equations. In all cases, the values of M_{cable} and C_{cable} are lower for the shielded configuration than for the unshielded one. Notably, in the presence of a shielding layer, the mutual capacitance C_{cable} effectively drops to zero, indicating that the shielding provides strong attenuation of electric-field coupling while offering only limited suppression of magnetic-field coupling. In terms of

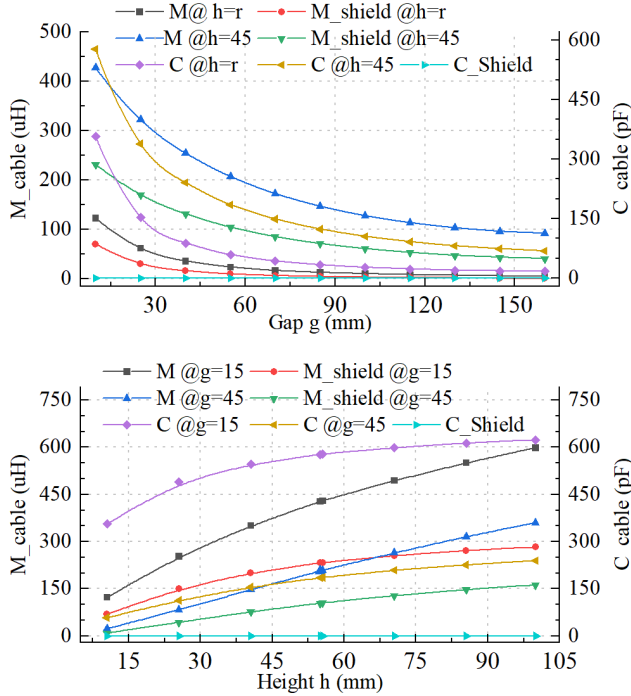


FIGURE 8. Cable coupling parasitic parameter simulation curve.

magnitude, M_{cable} is on the order of several hundred Micro Henries (10^{-4} H), whereas C_{cable} is on the order of several hundred Pico Farads (10^{-10} F), indicating that the magnetic inductive coupling dominates in this long-cable case and the electric capacitance coupling is negligible.

C. SHARED DC BUS CMV SIMULATION

In the compact power supply configuration, the main and pitch converters share a directly connected DC bus, despite operating at different switching frequencies. To analyze the CMV behavior under this configuration, a detailed simulation was performed with Ansys Simplorer.

Based on typical wind turbine configurations, the main converter switching frequency f_M was set to 2.5 kHz, and the pitch converter frequency f_P to 4 kHz. The output current of main converter and pitch converter is about 3000 Arms and 50 Arms which is similar with their typical normal operating current level. Both converter are operated under SVPWM control, and the DC bus voltage was configured at the standard level of 690 V.

The simulation output of the CMV waveform on the shared DC bus is shown in Fig. 9. To further analyze its spectral characteristics, a fast Fourier transform (FFT) was performed, revealing not only the expected switching harmonics but also intermodulation and beat frequency components caused by the interaction between the two converters.

The DC bus CMV shown in Fig. 9a exhibits an approximate isosceles triangular waveform, superimposed with multiple high-frequency harmonic components. The

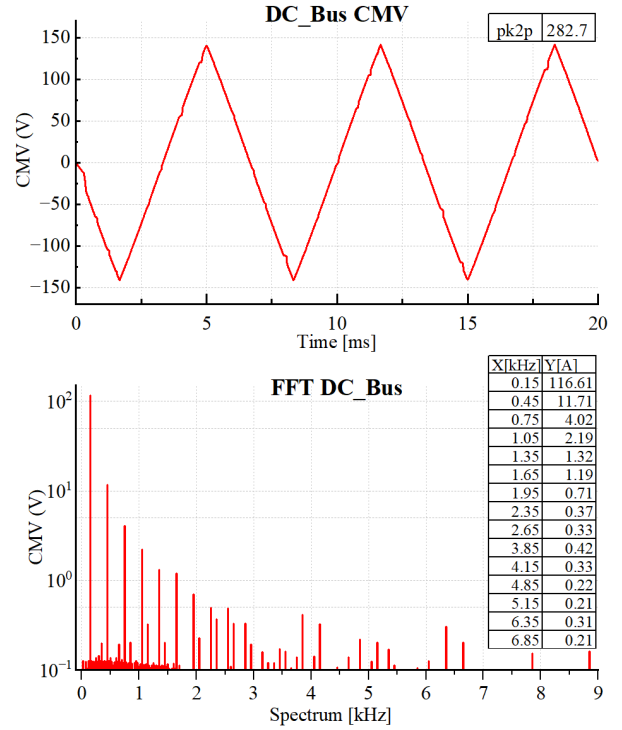


FIGURE 9. Shared DC bus CMV waveform and its FFT.

corresponding frequency-domain analysis, presented in Fig. 9b, reveals that the fundamental frequency component appears at 150 Hz, which corresponds to three times the generator output frequency ($3f_G$). This low-frequency CMV component originates primarily from the generator and rectifier stages and serves as a significant source of low-order harmonic excitation in the system.

In the high-frequency range, the dominant components are theoretically expected to appear at the switching frequencies of the main and pitch converters, denoted as $f_M = 2.5$ kHz and $f_P = 4$ kHz, respectively, along with their integer harmonics $m \cdot f_M$ and $n \cdot f_P$. Additionally, intermodulation between the two converters gives rise to sum and difference frequency components such as $f_M \pm f_P$. However, in the actual waveform, these high-frequency components undergo significant spectral shaping due to modulation with the low-frequency component f_r . As a result, sideband effects emerge, and the high-frequency spectral energy is redistributed into components of the form $m \cdot f_M \pm k \cdot f_G$, $n \cdot f_P \pm k \cdot f_G$, and $|f_M \pm f_P| \pm k \cdot f_G$. This modulation effect causes the original high-frequency components f_m , f_p , and $|f_m \pm f_p|$ to attenuate significantly in amplitude, making them less prominent in the observed spectrum.

Therefore, the high-frequency components observed in Fig. 9b, are primarily formed of modulated sidebands around several key frequencies. These include the difference frequency $|f_M - f_P| \pm 3f_G = 1.5$ kHz $\pm$ 150 Hz, the main converter switching frequency $f_M \pm 3f_G = 2.5$ kHz $\pm$ 150 Hz, the pitch converter switching frequency $f_P \pm 3f_G = 4$ kHz

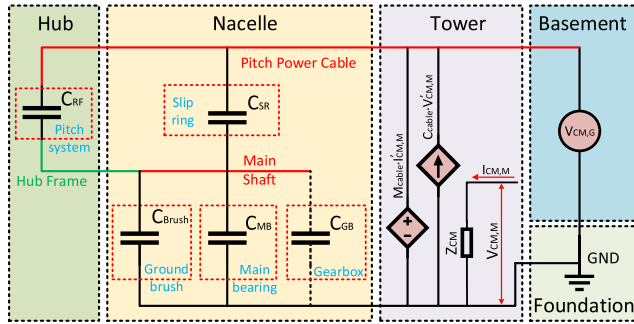


FIGURE 10. Typical pitch power supply CMV transmission circuit model.

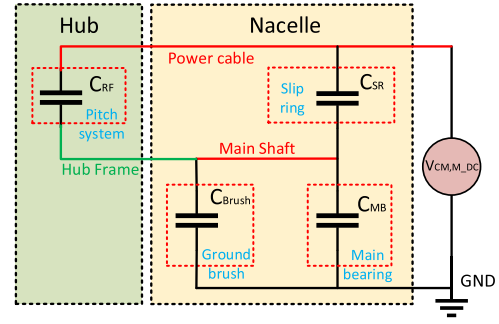


FIGURE 11. Compact pitch power supply CMV transmission circuit model.

± 150 Hz, and the sum frequency $|f_M + f_P| \pm 3f_G = 6.5$ kHz ± 150 Hz. These sideband components dominate the high-frequency region of the CMV spectrum, highlighting the strong intermodulation and modulation effects between the converter switching actions and the rectifier-induced low-frequency harmonics.

D. CIRCUIT MODEL

With the slip ring and cable coupling effects identified, circuit models are required to evaluate the impact of pitch power supply side CMV on shaft bearing currents. Owing to the significant differences between the configurations in Fig. 2 and Fig. 4, separate circuit models are constructed accordingly.

1) TYPICAL PITCH POWER SUPPLY CONFIGURATION

In the Fig. 2 configuration, two CMV sources exist: one from the grid via the auxiliary transformer and the other from the main converter. The corresponding circuit model is illustrated in Fig. 10, where each physical location is represented by dashed boxes with color coding consistent with Fig. 2.

The grid-side CMV travels within the power cable along the tower, partly coupling to the main shaft through the slip ring. The rest reaches the pitch converter's rectifier side, where it couples to the hub frame via internal stray capacitance C_{RF} . Since the hub frame is mechanically connected to the main shaft, both paths converge and return to ground through the parallel branches of ground brush, main bearing, and possibly the gearbox, completing the loop via the nacelle frame.

The CMV originating from the main converter is coupled into the pitch system power cables through the coupling effect with adjacent cables inside the tower. After reaching the nacelle, it shares the same propagation path as the grid-side CMV, ultimately contributing to the common-mode current flowing through the drivetrain.

As discussed in Section IV-B, the cable-to-cable coupling—via mutual capacitance and inductance—introduces greater complexity than the grid-side CMV. These couplings can be represented in the secondary side (pitch power cables) as an equivalent voltage source V_{CM,M_2nd} and

current source I_{CM,M_2nd} , expressed as:

$$V_{CM,M_2nd} = M_{cable} \cdot \frac{dI_{CM}(t)}{dt} = M_{cable} \cdot I'_{CM,M} \quad (3)$$

$$I_{CM,M_2nd} = C_{cable} \cdot \frac{dV_{CM}(t)}{dt} = C_{cable} \cdot V'_{CM,M} \quad (4)$$

Here, $I_{CM,M}$ and $V_{CM,M}$ are the common-mode current and voltage in the primary side (main power cables).

In the circuit model, mutual inductive and capacitive coupling effects are represented by a current-controlled voltage source (CCVS) and a voltage-controlled current source (VCCS), respectively. Based on (3) and (4), the mutual inductance and capacitance translate into equivalent circuit parameters: a transfer conductance $g = M_{cable} \cdot I'_{CM,M}$ for the VCCS and a transfer resistance $r = C_{cable} \cdot V'_{CM,M}$ for the CCVS. These parameters quantify the strength of coupling from the primary side to the secondary side, and effectively model the propagation of common-mode disturbances through electromagnetic coupling.

2) COMPACT PITCH POWER SUPPLY CONFIGURATION

In the configuration shown in Fig. 4, multiple CMV components are introduced due to the interaction and superposition of the two converters. However, since all CMVs are present within the same set of power cables, they can be modeled as a single equivalent CMV source. The corresponding model is illustrated in Fig. 11, where the propagation paths within the hub and nacelle remain largely consistent with those described in Fig. 8.

V. PITCH POWER SUPPLY CONTRIBUTION ON MAIN SHAFT BEARING CURRENTS

To further evaluate the impact of different CMV sources on the main shaft bearing, it is necessary to analyze their respective contributions to bearing stress. Under the proposed circuit models, various CMV sources corresponding to different power supply configurations are identified. These CMV sources differ in both frequency content and amplitude, and the variations in their propagation paths further lead to differences in transmission efficiency. Therefore, it is essential to analyze the individual contributions of each CMV source to the resulting bearing voltage and bearing current.

In the study of motor bearing currents, the Bearing Voltage Ratio (BVR) is commonly adopted as an indicator to evaluate the extent to which the CMV contributes to the bearing voltage. A higher BVR value implies a stronger influence of the CMV on the shaft voltage, indicating a higher likelihood of bearing stress or damage caused by electric discharge through the bearing.

For the circuit model in Fig. 10, due to the presence of several common-mode sources, it is necessary to analyze their individual BVRs. As the system is linear, the BVRs can be treated separately, and their corresponding expressions can be solved independently. The specific expressions for the BVR corresponding to the grid-side CMV BVR_G presented in (5).

$$\begin{aligned} BVR_G &= \frac{Z_{Brush} \| Z_{MB} \| Z_{GB}}{(Z_{Brush} \| Z_{MB} \| Z_{GB}) + (Z_{RF} \| Z_{SR})} \\ &= \frac{C_{RF} + C_{SR}}{C_{Brush} + C_{MB} + C_{GB} + C_{RF} + C_{SR}} \end{aligned} \quad (5)$$

The common-mode component from the main converter, coupled through cable interaction, exhibits a relatively complex behavior. In the circuit model, they are represented by both a CCVS and a VCCS. The contribution from the CCVS can be analyzed in a similar manner to the grid-side source. In contrast, the VCCS contribution is more difficult to express analytically, but the resulting voltage on the secondary side of the coupled cable, which contribute directly to the bearing, can still be obtained through analytical calculation. The total bearing voltage contribution from the main converter, denoted as $V'_{CM,M}$, is then calculated by summing the effects of both sources.

The voltages generated at the bearing by the CCVS and VCCS are denoted as V_{MC} and V_{MM} , respectively, with their expressions given by (6) and (7):

$$\begin{aligned} V_{MC} &= (Z_{Brush} \| Z_{MB} \| Z_{GB}) \cdot I_{CM,M2nd} \\ &= \frac{C_{cable} \cdot V'_{CM,M}}{C_{Brush} + C_{MB} + C_{GB}} \end{aligned} \quad (6)$$

$$\begin{aligned} V_{MM} &= BVR_G V_{CM,M2nd} = BVR_G M_{cable} I'_{CM,M} \\ &= BVR_G M_{cable} \frac{V'_{CM,M}}{Z_{CM}} \\ &= \frac{(C_{RF} + C_{SR}) \cdot M_{cable}}{(C_{Brush} + C_{MB} + C_{GB} + C_{RF} + C_{SR}) \cdot Z_{CM}} \end{aligned} \quad (7)$$

where Z_{CM} is the common mode impedance of the primary side (main power cable).

Thus, the voltage coupled to the bearing on the secondary side of the cable V_M can be summarized as:

$$V_M = \left(\frac{C_{cable}}{C_{Brush} + C_{MB} + C_{GB}} + \frac{BVR_G \cdot M_{cable}}{Z_{CM}} \right) \cdot V'_{CM,M} \quad (8)$$

Therefore, the shaft voltage on the bearing in typical pitch power supply configuration can be expressed with the BVRs, as shown in equation (9).

$$V_{Bearing} = BVR_G \cdot V_{CM,G} + V_M \quad (9)$$

Similarly, for the circuit model in Fig. 11, the BVR for the shared DC bus CMV is presented in equation (10).

$$\begin{aligned} BVR_{M_dc} &= \frac{Z_{Brush} \| Z_{MB}}{(Z_{Brush} \| Z_{MB}) + (Z_{RF} \| Z_{SR})} \\ &= \frac{C_{RF} + C_{SR}}{C_{Brush} + C_{MB} + C_{RF} + C_{SR}} \end{aligned} \quad (10)$$

The bearing voltage in the compact power supply configuration is shown in equation (11).

$$V_{Bearing} = BVR_{M_dc} \cdot V_{CM,M_dc} \quad (11)$$

VI. EXPERIMENTAL VERIFICATION

Sections II to V present theoretical analysis and finite element simulations. To validate the accuracy of these findings, experimental verification is necessary.

Although the configuration shown in Fig. 4 differs in overall topology from that in Fig. 2, the two share most of the critical components—particularly the slip-ring assembly, which dominates the coupling behavior under investigation. However, since the Fig. 4 configuration is less commonly adopted in practical wind-turbine systems, the experimental validation in this study is primarily carried out on the configuration depicted in Fig. 2.

A. LAB TEST

Due to safety regulations and physical inaccessibility of key components along the CMV propagation path in actual wind turbines, direct measurement is not feasible in real installations. Therefore, a downsized laboratory wind turbine was constructed to enable the white-box experimentation under safe conditions. Theoretical predictions and simulation results were first validated step by step on this lab turbine, serving as a foundational verification prior to conducting measurements on an actual full-scale wind turbine.

In the laboratory experiments, three key aspects are addressed:

- Construction of a laboratory turbine to emulate the operational conditions of an actual wind turbine;
- Independent verification of the cable coupling effects;
- Evaluation of the Bearing Voltage Ratio (BVR) and measurement of bearing voltage on the lab-scale turbine.

1) LAB TURBINE CONSTRUCTION

The configuration of the laboratory wind turbine is depicted in Fig. 12. Its overall system architecture closely mirrors that of the full-scale turbine in Fig. 2, comprising the pitch system housed in the hub, the main bearing and slip-ring assembly within the nacelle, and the main converter together with the auxiliary transformer located at the basement. To simplify the mechanical layout, the generator is directly connected to the shaft, eliminating the gearbox typically present in full-scale machines. Under this condition, the gearbox path can be regarded as open, corresponding to $Z_{GB} \rightarrow \infty$ ($C_{GB} \rightarrow 0$). The pitch system is represented by a single converter

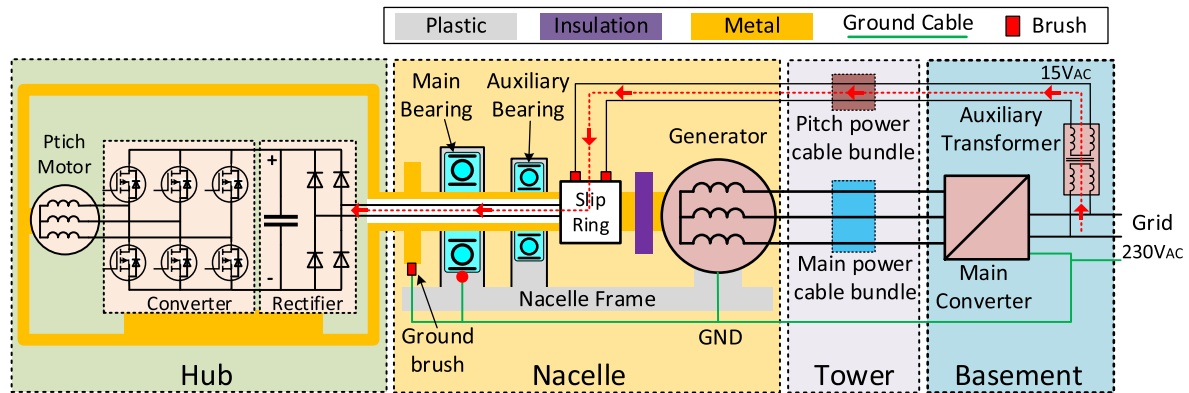


FIGURE 12. Lab turbine configuration.

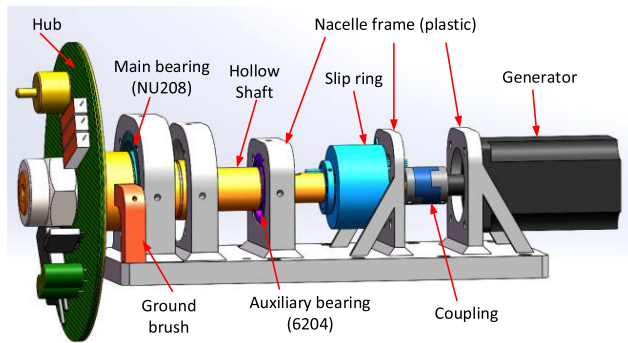


FIGURE 13. Lab turbine arrangement.

and motor which is powered from the grid via the auxiliary transformer and slip ring as indicated by the red dish arrow line.

The nacelle frame is fabricated from 3D-printed plastic to minimize parasitic coupling effects and facilitate testing, with all electrical connections between subsystems implemented using dedicated wires. The specific physical arrangement of the laboratory setup is illustrated in Fig. 13.

The shaft is supported at two points: a NU208 roller bearing as the main bearing, matching those used in actual wind turbines, and a 6204 deep-groove ball bearing as the auxiliary. The auxiliary bearing's outer ring is electrically floating to avoid forming a closed loop that could influence shaft voltage, while the main bearing's outer ring is grounded via a separate wire to provide a defined return path.

2) LAB VALIDATION OF CABLE COUPLING EFFECT

In the laboratory setup, it is impractical to employ full-scale 80 m cables as used in actual wind turbines. Nevertheless, since cable coupling constitutes an independent electromagnetic mechanism, it can be validated separately. Considering that wave reflection effects are negligible within the studied frequency range ($f \leq 10$ kHz), the electromagnetic coupling behavior can be accurately characterized with the lumped-parameter approach. Accordingly, 3 m cable samples were

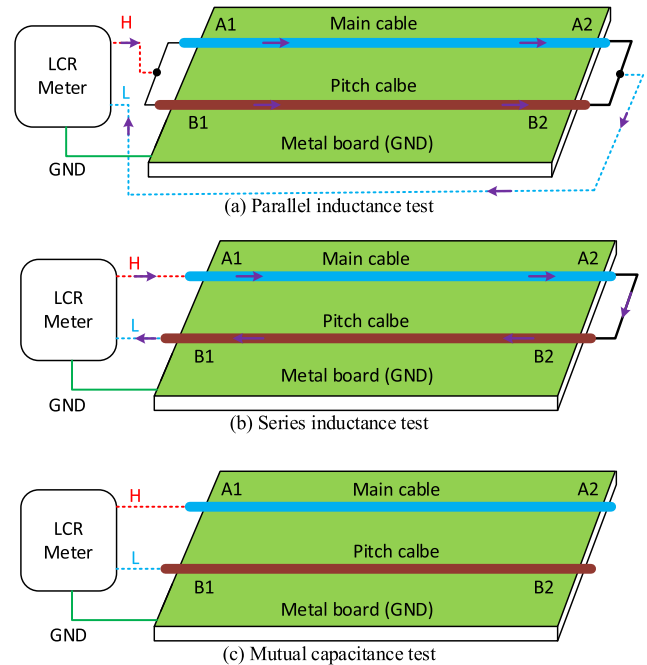


FIGURE 14. Cable coupling parasitic parameter test connection.

employed in the laboratory tests to validate the theoretical analysis and simulation results.

The mutual inductance M and the mutual capacitance C are test by LCR meter, and the test connections are indicated in Fig. 14 respectively. The cable groups (A and B) were arranged in parallel on a thick grounded metallic platform, and the tested cable radius $r_1 \approx r_2 = r = 5$ mm. In the test, the LCR meter is also connected to the platform to erase the stray noise from different ground points.

For the mutual inductance test, parasitic impedance of connections cable and joints will bring error into the test result. The mutual inductance can't be achieved by LCR meter directly. Thus, the total inductance of the cable group are test in parallel L_P and series L_S separately (shown in Fig. 14a and b). Due to the difference of current direction

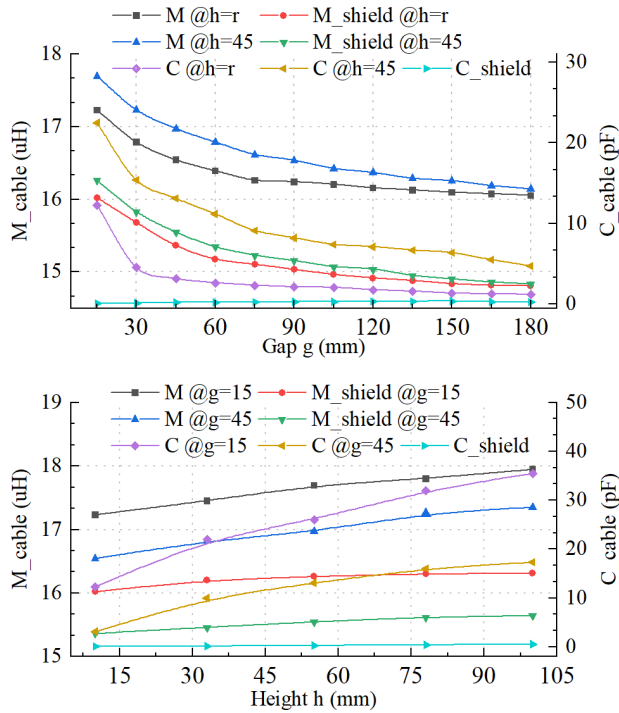


FIGURE 15. Cable coupling parasitic parameter test curve.

in different cable groups, the inductance can be expressed as $L_P = (L + M)/2$ and $L_S = 2(L - M)$. Thus, the mutual inductance M can be calculated by equation (12)

$$M = L_P - \frac{L_S}{4} \quad (12)$$

For the mutual capacitance test, the capacitance can be directly achieved by the LCR meter with the connection shown in Fig. 14c. The test results of all the groups are concluded in Fig. 15.

A comparison between the experimental and simulated results shows a consistent overall trend, highlighting several key observations:

- Both mutual inductance M and capacitance C decrease with increasing cable spacing g ;
- For a fixed d , larger distance h from the grounded plate results in higher M and C ;
- The presence of shielding reduces both parameters, with mutual capacitance nearly approaching zero;
- In magnitude, mutual inductance (10^{-5} H level) is roughly 10^6 times greater than mutual capacitance (10^{-11} F level), indicating that inductive coupling dominates the cable coupling effect.

3) PARAMETER ACQUISITION OF LAB TURBINE

Before conducting system-level validation, it is essential to achieve the capacitance of individual components or subsystems in the lab turbine. According to Fig. 10, aside from $C_{GB} \rightarrow 0$ the key parameters to be determined on the laboratory turbine include C_{SR} , C_{Brush} , C_{RF} , and C_{MB} . Among

these, C_{Brush} is strongly influenced by the installation configuration, while C_{MB} and C_{Brush} also vary with the turbine's rotational speed.

To ensure realistic measurement of these capacitances, the laboratory turbine is operated under conditions that emulate those of a full-scale system. Specifically, the generator operates in motor mode to drive the main shaft at 100 rpm, and the pitch converter is loaded at its rated condition, enabling the coupling capacitances to be characterized under representative dynamic operation.

Furthermore, to simulate potential abnormal operating conditions that may occur in actual wind turbines, a series of tests were conducted on the laboratory turbine under various installation configurations and operating states. In these tests, the parameters C_{Brush} , and C_{MB} were measured under different mechanical and electrical conditions. The specific test setups and corresponding operating scenarios for each parameter are summarized as follows.

- C_{Brush} is characterized by varying the spring pressure and surface roughness at the brush-ground disk interface. Additionally, to simulate poor maintenance scenarios in actual turbines—where grease may contaminate the brush-to-ground disk interface—a test condition was introduced in which grease was deliberately applied to the contact surface.
- C_{MB} is the main bearing capacitance. Due to the smaller bearing diameter, the lower roller speed alters the conductive/insulated mode and causes parameter deviations. To emulate full-scale conditions, insulating grease was applied to maintain insulation mode under 100rpm. For comparison, conductive grease with the same volume was also tested to evaluate its effect on reducing bearing currents.

After the assembly of the laboratory turbine and run the system under rated speed operating, the coupling capacitance of each components and also the dynamic capacitance between different components are measured by LCR meter under 2V 5 kHz excitation.

Other parasitic capacitances within the system were also measured through LCR meter under different conditions. A summary of these test results is provided in Table 3.

The measured coupling capacitance C_{SR} of the slip ring is shown in Fig. 6b. The frequency response trend closely matches the simulation results, though with slightly greater fluctuation. Overall, the measured values remain relatively stable, ranging from 17.9 pF to 18.7 pF. Compared with simulation, the measured capacitance is slightly lower because the test was conducted between the stator brush lead and the shaft, whereas the simulation modeled the capacitance between the rotor slip ring and the shaft. The measured result therefore represents the series combination of the brush-ring capacitance and the ring-shaft capacitance, leading to a smaller equivalent value.

The measured C_{MB} and C_{Brush} vary significantly under different operating or installation conditions. In particular, when

TABLE 3. Lab turbine tested parameter.

Parameter	Value	Test condition
C_{SR}	18.3 pF	Slip ring to main shaft average
C_{RF}	28 pF	Pitch rectifier to main shaft
C_{MB} @100rpm	314 pF	lithium-based grease $\epsilon_r \approx 2.78$ carbon conductive grease $\rho_v \approx 63\Omega \cdot \text{cm}$
C_{Brush} @100rpm	∞ 1.5 nF 23.4 pF	Tight contact Normal contact Grease contaminated interface

TABLE 4. Test case number definition.

	C_{Brush}	Tight brush	Normal brush	Greased brush
C_{MB}				
Normal grease		Case 1	Case 2	Case 3
Conductive grease		Case 4	Case 5	Case 6

the brush is contaminated by grease, its capacitance decreases by several tens of times (approximately $64\times$) compared with the normal condition, implying a substantial degradation of the grounding effectiveness. In contrast, under tight-contact conditions, the brush–shaft interface approaches an ideal electrical contact and no longer behaves as a capacitive coupling; from a modeling perspective, this condition can be equivalently represented by an infinitely large capacitance.

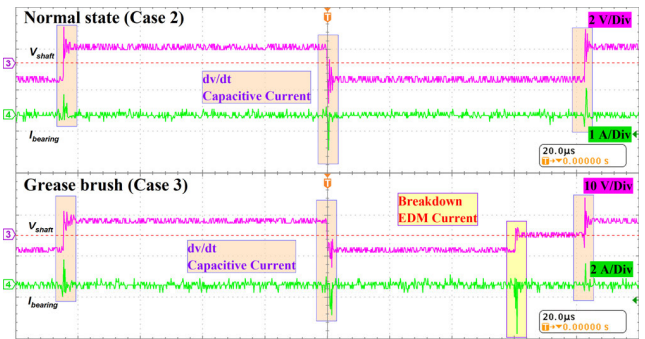
Similarly, when conductive grease is applied into the main bearing, the bearing remains in a conductive state and its equivalent impedance is greatly reduced, which can also be modeled as an infinite capacitance.

4) SYSTEM BVR VERIFICATION

With the coupling parameters of individual subsystems and components determined, the BVR is evaluated through both modeling and experimental validation. Based on the previously obtained coupling capacitances, simulation analysis with the established circuit model in section IV-D is first conducted to predict the BVR under different conditions. Corresponding measurements were then conducted on the laboratory turbine, enabling direct comparison between simulated and experimental results. Since different conditions may occur for the brush and bearing, both simulation and experimental analyses were performed for various combinations of these states, as summarized in Table 4.

Although multiple excitation sources are included in the circuit model shown in Fig. 10, their conduction paths within the nacelle and hub—and their resulting influence on the bearing voltage—are effectively identical. Moreover, the relevant coupling capacitances remain stable over the frequency range of interest. Therefore, a single excitation source applied at the slip ring is sufficient to characterize the BVR under different operating conditions.

In the laboratory turbine test, a controllable voltage source (peak 25V @6 kHz) is injected directly into the slip-ring input cable. Under this configuration, the main-shaft voltage V_{Shaft}

**FIGURE 16.** Lab turbine system test result comparison.

and the bearing current $I_{bearing}$ are measured for different combinations of brush contact conditions and bearing grease combinations.

The typical cases: normal case (Case2) and greased brush case (Case3) waveforms are shown in Fig. 16 respectively.

In the normal brush Case 2, the large dv/dt values are observed at the rising and falling edges of the shaft voltage, which induces a significant capacitive bearing current with peak amplitudes reaching up to 0.6–0.8 A. Moreover, in addition to the capacitances included in Fig. 10, the practical system also contains parasitic inductances L . During fast voltage transitions, these parasitic elements can form LC resonant paths, leading to transient oscillations and voltage overshoot on the shaft.

In the greased brush Case 3, high dv/dt transitions still generate capacitive bearing currents similar to those observed in the normal brush condition. However, the degraded grounding effectiveness of the brush results in a higher shaft-to-bearing voltage. When this voltage exceeds the breakdown threshold of the lubricant film between the rolling elements and the raceway, electrical discharge machining (EDM) events occur. These events are characterized by sudden voltage collapse accompanied by sharp current spikes, with peak amplitudes reaching approximately 2.4 A, which is about three times higher than the capacitive bearing current. The breakdown is initiated by localized electric-field enhancement during bearing rotation. Once discharge occurs, the voltage drops rapidly, weakening the electric field and limiting the discharge to a single current pulse. The localized EDM discharges produce intense Joule heating, which accelerates raceway damage and significantly reduces bearing lifetime, making this operating condition particularly critical.

The BVRs are evaluated as the ratio $BVR = V_{Shaft}/V_{CM_IN}$. In practical measurements, LC oscillations in the system lead to voltage overshoot in V_{Shaft} . To avoid distortion of the BVR results caused by such transient effects, the shaft voltage is extracted from the steady-state plateau region rather than from the overshoot peaks.

Simultaneously, the bearing current $I_{bearing}$ is recorded to examine its correlation with BVR. For consistency, $I_{bearing}$ is quantified using the peak capacitive bearing current occurring

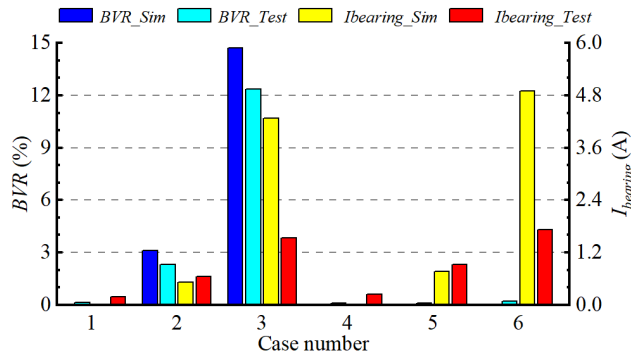


FIGURE 17. BVRs at different conditions.

at the voltage transition edges, instead of the EDM current observed during the steady-state interval. Owing to inherent measurement variability, each test case is repeated ten times and the averaged values are reported. The corresponding experimental and simulation results are summarized in Fig. 17.

Comparison between experimental and simulation results across different cases shows consistent overall trends. The measured BVR agrees well with the simulated trends,

Whereas the simulated bearing current $I_{bearing}$ is significantly higher than the measured values, as LC oscillations are not included in the simulation. Consequently, the energy associated with high dv/dt transitions is released instantaneously, producing higher current peaks, whereas in the practical system it is dissipated through damped oscillations, resulting in lower amplitudes.

Further comparison of the BVR and $I_{bearing}$ under different conditions shows that the brush state has a dominant influence on shaft electrification. Under normal or tight brush contact, the BVR and $I_{bearing}$ are effectively driven to zero, whereas poor brush contact results in significantly higher BVR and $I_{bearing}$. The use of conductive grease similarly reduces BVR to near zero; however, it simultaneously leads to a substantial increase in $I_{bearing}$, particularly when the brush contact is degraded.

B. WIND FARM FIELD TEST (KENERSYS K82)

Following laboratory validation, field experiments were carried out on a 2 MW horizontal-axis geared wind turbine (Kenersys K82) to verify the theoretical analysis and lab emulation under real operating conditions. The tested turbine configuration is shown in Fig 18, it aligns with Fig. 2. Its pitch system is powered routed from the grid, transmitted through the 80m high tower, conducted via the slip ring and finally fed to the hub, the path is shown by the red arrow dish line.

Due to safety regulations and installation constraints in the wind turbine system, most measurement points along the CMV propagation path were inaccessible. As a result, only limited data could be obtained from few available locations, making full system-level testing infeasible. Consequently, the

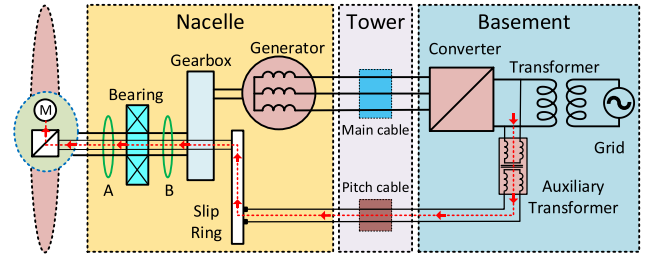


FIGURE 18. Kenersys K82 configuration & sensor arrangement.

verification relied on these restricted measurements to validate the theoretical analysis and the laboratory-based results.

To overcome the limitations, non-intrusive Rogowski coils were used to capture the bearing current under pitch operating conditions. As illustrated in Fig. 18, coils A and B were mounted on each side of the main bearing, and the differential signal between them was used to isolate the current flowing through the bearing.

The measured current flowing through the main bearing during pitch operation is shown in Fig. 19a and 19b. The bearing current reaches a peak of approximately 4 A, which is a harmful amplitude to the bearing lifetime [33], [34]. And the waveform exhibits a low-frequency envelope that is superimposed by higher-frequency alternating components.

The FFT spectrum of the measured current in Fig 19c reveals that the low-frequency components are primarily concentrated at 50 Hz, 150 Hz, and 350 Hz, corresponding to the grid frequency and its third and seventh harmonics. In contrast, the high-frequency components are mainly distributed in sideband regions centered around 2.5 kHz, 4 kHz and 5 kHz.

The low-frequency components are consistent with those identified in Section III-B, originating primarily from the grid-side CMV at its third harmonic ($3f_G = 150$ Hz) and from grid-voltage imbalance, which introduces additional components at 50 Hz, 250 Hz (5th), and 350 Hz (7th). It is noteworthy that, as analyzed in Section III-C, the main-converter CMV also contains a harmonic at $f_R = 3f_G = 150$ Hz. Therefore, the 150-Hz component observed here may result from the combined contribution of both the grid-side and main-converter CMV, which also explains its comparatively high amplitude.

The high-frequency components exhibit center frequencies at 2.5 kHz, 5 kHz and 4 kHz, corresponding to the main converter's switching frequency f_M , its harmonic $2f_M$ and pitch converter's switching f_P . The detailed spectral lines appear as $m \cdot f_M \pm k \cdot f_G$ and $n \cdot f_P \pm k \cdot f_G$, which are consistent with the modulation effects analyzed in Section III-C.

The presence of the main converter's switching-frequency component (f_M) in the measured bearing current, despite the absence of any direct electrical connection between the main converter and the main bearing, indicates the existence of the cable coupling effect described in Section IV-B. The

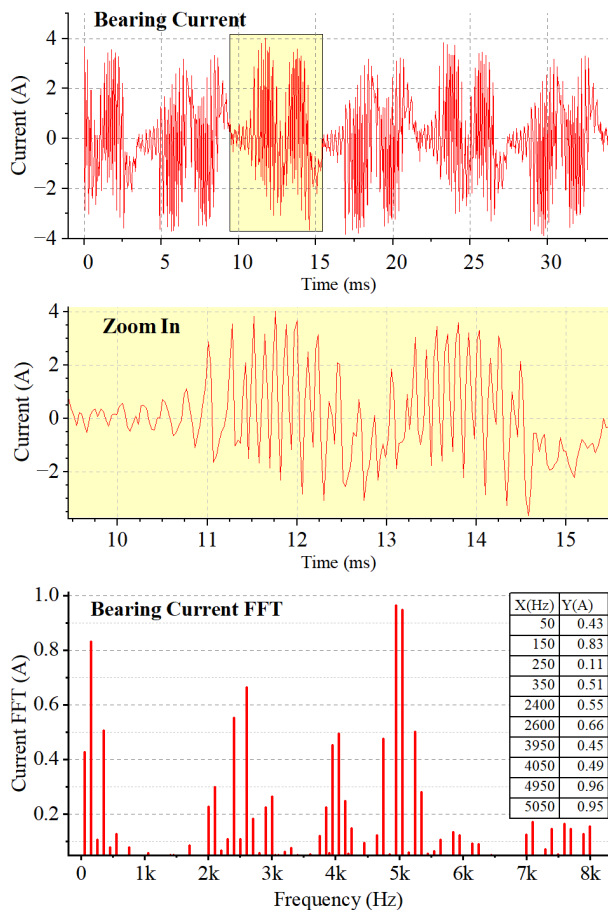


FIGURE 19. Field test bearing current waveform and its FFT.

CMV component at f_M generated by the main converter is coupled into the pitch system power supply circuit through inter-cable electromagnetic coupling, then transferred onto the main shaft, and finally forms a closed grounding path via the main bearing—thus giving rise to the observed bearing current.

Due to various practical limitations in the wind turbine system, it was not possible to acquire sufficient signals to fully analyze the BVR contributed by each source. However, the measured bearing current spectrum clearly reveals the presence of low-frequency grid CMV components, grid imbalance components, and high-frequency switching components, as well as modulation effects between them. These observations indirectly validate the theoretical analysis presented in Sections III and further corroborate the results obtained from the laboratory experiments.

C. MITIGATION METHOD DISCUSSION

The experimental results obtained above confirm the validity of both the analytical framework and the proposed coupling model. Based on this foundation, mitigation of main-shaft bearing voltage and current can be systematically organized into three complementary levels: suppression at the CMV

source, attenuation along the propagation path, and protection at the receiving components.

1) SOURCE-LEVEL SUPPRESSION

Multiple CMV sources exist within the wind turbine electrical system; however, this study focuses on the CMV propagating through the pitch-system input path. Along this path, the dominant CMV components mainly originate from grid/load imbalance and the CMV generated by the main converter. Grid-induced CMV can be mitigated by improving supply-phase balance and ensuring more uniform loading of internal three-phase subsystems. In contrast, the CMV generated by the main converter can be attenuated using CMV chokes or dedicated CMV filters installed at the converter output.

2) PATH-LEVEL ATTENUATION

Along the propagation path, CMV is primarily transferred through electromagnetic coupling between the pitch cables and generator cables, as well as through the parasitic capacitance of the slip ring. The coupling between cable bundles can be reduced by employing shielded cables, increasing the separation between cable groups, and decreasing the distance between cables and the grounded tower wall. For the slip ring, mitigation relies on structural optimization that lowers the parasitic capacitance between the slip-ring tracks and the main shaft, thereby increasing the effective coupling impedance.

3) RECEIVER-LEVEL PROTECTION

Protection at the receiver focuses on the ground brush and the main bearing. Experimental results show that maintaining good brush contact significantly reduces the BVR and the corresponding bearing current. For the bearing itself, conductive grease can lower the BVR by reducing the insulation impedance. However, it also increases the bearing current substantially, indicating that its use must be evaluated carefully depending on reliability requirements.

VII. CONCLUSION

This study investigated main-shaft bearing currents in wind turbines, with a particular focus on the influence of CMV introduced by the pitch-system power supply. A system-level analysis was first conducted to identify CMV sources and clarify their propagation paths toward the main bearing. Based on this analysis, an analytical circuit model was developed and validated through simulation to quantify the contribution of different CMV sources to the bearing voltage. By combining analytical modeling, electromagnetic field simulations, and laboratory measurements, cable inductive coupling and slip-ring parasitic capacitance were identified as the dominant CMV transmission mechanisms. A laboratory turbine emulating full-scale operating conditions enabled accurate extraction of coupling parameters and systematic model validation, revealing the strong influence of installation configuration, brush condition, and

bearing lubrication on effective coupling. Finally, based on the above analysis and experimental results, practical mitigation methods for main bearing currents were discussed. Field measurements on a 2-MW turbine further confirmed the coexistence of low-frequency grid-related components and high-frequency converter-induced components in the bearing current. Overall, the results establish a coherent understanding of CMV-induced bearing currents and provide a solid technical basis for improving drivetrain insulation design and long-term wind turbine reliability.

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