

Article

Investigation of Eccentricity Characteristics in a Dual-Stator Single-Rotor Axial Flux Permanent Magnet Synchronous Motor

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Abstract

Dual-stator single-rotor (DSSR) axial flux permanent magnet synchronous motors (AF-PMSMs) offer high torque density but face reliability challenges due to unbalanced magnetic forces (UMF) and bending moments caused by eccentricity faults. This study investigates the electromagnetic performance of a DSSR AFPMSM under static, dynamic, axial, and radial eccentricities to reveal specific fault signatures and physical mechanisms. The methodology relies on three-dimensional transient finite element analysis (3-D FEA) and is validated by experimental tests on a 500 W prototype. Results indicate that while static and dynamic eccentricities have negligible effects on average torque, they induce significant bending moments where static eccentricity generates a constant moment and dynamic eccentricity produces an alternating one, both proportional to the eccentricity severity. Crucially, axial eccentricity disrupts magnetic symmetry, causing a 17.6% no-load back-EMF imbalance between stators and increasing net axial UMF to 53.2 N at a 40% eccentricity factor. Conversely, radial eccentricity shows minimal impact, confirming the topology's robustness against radial misalignments. These findings provide essential baseline data for the vibration analysis and condition monitoring of DSSR AFPMSMs.

Keywords: axial flux permanent magnet synchronous motor; dual-stator single-rotor; eccentricity; PMSM; unbalanced magnetic force



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1. Introduction

Axial flux permanent magnet synchronous motors (AFPMSMs) are a prominent research focus in the field of electric drives due to their high power density, high efficiency, and compact axial structure, demonstrating significant potential in high-end applications such as electric vehicles, aerospace systems, and renewable energy generation [1–5]. Among various AFPMSM topologies, the dual-stator single-rotor (DSSR) configuration effectively mitigates the excessive unbalanced magnetic force (UMF) acting on the rotor disk—a critical issue inherent in single-stator dual-rotor (SSDR) topologies—by exerting equal and opposite axial electromagnetic forces on the central rotor from both stators. Additionally, the DSSR topology offers advantages including low rotor inertia and fast dynamic response [6,7]. However, this architecture introduces a new challenge: each individual stator must independently withstand the full magnitude of the axial UMF and its associated bending moment. In practical engineering applications, eccentricity faults are inevitably

caused by manufacturing tolerances, assembly errors, and bearing wear accumulated during long-term operation. Furthermore, external disturbances, such as severe mechanical impacts or thermal variations, can exacerbate these eccentric conditions [8].

Owing to the geometry of AFPMSMs, in which the radial and circumferential dimensions of the stator and rotor are significantly larger than the axial length, even a slight eccentricity can cause non-uniform and asymmetric variations in the air-gap length, leading to severe distortion of the air-gap magnetic field. This not only degrades electromagnetic performance—including increased losses, elevated torque ripple, and reduced efficiency—but also intensifies UMF, electromagnetic vibration, and acoustic noise. Consequently, a detrimental feedback loop may be established: eccentricity leads to increased UMF, which in turn accelerates bearing wear and further worsens eccentricity [9,10]. In severe cases, this can induce rotor bending or even direct contact between the stator and rotor, ultimately compromising operational safety and shortening the motor's service life [11].

In the aspect of eccentricity and UMF modeling for AFPMSMs, existing studies have mainly focused on static and dynamic eccentricity, with only limited work addressing axial eccentricity, whereas radial eccentricity has been largely unexplored in analytical models. Early works on single-stator single-rotor (SSSR) topologies centered almost exclusively on static eccentricity. Mirimani et al. [12,13] developed an analytical model to quantify its effect on air-gap field distribution and back electromotive force (EMF) waveform distortion. Gerlando et al. [14] further measured equivalent static eccentricity caused by manufacturing tolerances and linked it to performance degradation. Huang et al. [15,16] extended static eccentricity modeling to concentrated-winding SSSR motors and later generalized the approach to cover multiple eccentricity types in a unified framework. De Bisschop et al. [17] advanced this line of research by combining demagnetization and eccentricity in a joint analytical model, capturing their interaction effects. Li et al. [18] categorized eccentricity in single-stator dual-rotor (SSDR) in-wheel motors into three distinct types—static, dynamic, and axial—and analyzed their respective impacts. Zhang et al. [19] then provided a refined force model specifically for dynamic eccentricity, further enriching dynamic fault analysis. Collectively, these studies have established a foundation for understanding specific eccentricity types in AFPMSMs, yet a comprehensive characterization covering all possible dimensions remains to be fully developed.

For vibration behavior and online detection, research similarly concentrates on static eccentricity. Guo et al. [20] proposed a magnetic field calculation method that inherently accounts for radial–axial coupling under static eccentricity, laying the groundwork for coupled electromechanical analysis. Ogidi et al. [21,22] demonstrated that vibration signals can effectively diagnose static eccentricity in concentrated-winding AFPMs, with spectral features correlating to fault severity and location. They also revealed that rotor topology significantly modulates these vibration patterns, and that cogging torque minimization techniques further affect eccentricity detection, necessitating diagnostic strategies that account for both factors. Mirimani et al. [23] developed an online detection method for static eccentricity based on current signature analysis, enabling continuous monitoring without disassembly. Barmpatza [24] addressed mixed faults, using neutral voltage difference to distinguish combined eccentricity and demagnetization from single-type defects. Notably, existing detection studies have not systematically isolated radial eccentricity as an independent fault type, nor established a unified framework linking each eccentricity dimension to specific electromechanical responses.

While eccentricity faults in SSSR motors have been extensively studied, directly extrapolating these findings to DSSR topologies is theoretically insufficient. In DSSR motors, the rotor is sandwiched between two stators, resulting in a bidirectional electromagnetic coupling where the net UMF arises from two opposing air-gap fields. Crucially, mag-

netic saturation in the shared rotor core causes eccentricity in one air-gap to alter the rotor's magnetic reluctance, thereby distorting the flux distribution in the opposite air-gap. This nonlinear cross-coupling mechanism, arising from the shared rotor yoke in DSSR configurations, has not been quantitatively investigated in the context of eccentricity fault analysis.

In this paper, a DSSR AFPMSM is investigated with a focus on its eccentricity characteristics. The configuration of the DSSR motor with 12 slots and 16 poles is presented in detail. A systematic analysis is conducted on the electromagnetic and mechanical performance of the healthy prototype under rated operating conditions, and the results are validated through experimental measurements of the no-load back-EMF and electromagnetic torque. Furthermore, the effects of static, dynamic, axial, and radial eccentricity on relevant performance indicators are comprehensively evaluated using 3-D FEM.

The remainder of this paper is organized as follows. Section 2 presents the 3-D FE model of the DSSR motor and validates it through experimental measurements of the healthy prototype. Section 3 investigates the effects of static, dynamic, axial, and radial eccentricity on the motor's electromagnetic and mechanical characteristics, with emphasis on back-EMF asymmetry, UMFs, and bending moments. Section 4 presents dedicated experimental validation under static and axial eccentricity conditions to verify the simulation-based fault signatures. Section 5 concludes the paper.

2. Healthy Prototype

2.1. Motor Structure

The detailed structure of the DSSR motor is illustrated in Figure 1. The prototype consists of one inner rotor sandwiched between two outer stators. The winding layout and the PM magnetization direction are shown in Figure 2. The stators employ fractional-slot concentrated windings, which enable modular design and offer the advantages of simple winding and easy assembly. The PMs, made of N38UH material, are bonded to both sides of the rotor yoke, with each coaxial pair magnetized in the same direction. To reduce cogging torque, both the stator teeth and the PMs employ a circular skew. The main parameters of the prototype are summarized in Table 1.

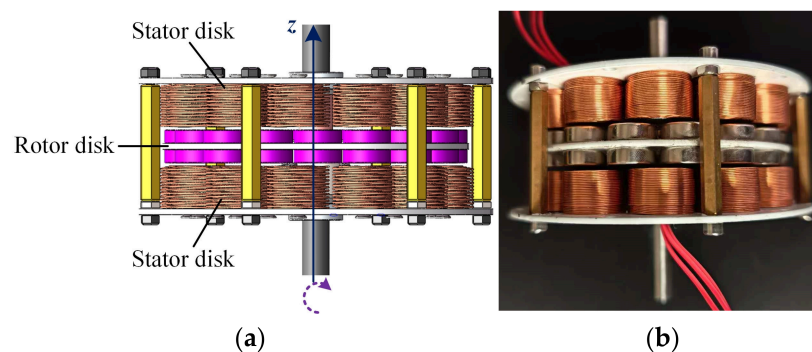


Figure 1. The DSSR motor: (a) the 3-D structure; (b) the photograph.

Table 1. Main parameters of the prototype.

Description	Symbol	Value
Active outer diameter/mm	D_o	96
Active inner diameter/mm	D_i	54
Diameter of the PM/mm	D_M	14
Number of pole pairs	p	8
Number of stator teeth	N_s	12

Table 1. Cont.

Description	Symbol	Value
Axial length of the air-gap/mm	g_0	1
Axial length of the PM/mm	h_M	4
Axial length of stator teeth/mm	L_s	12
Rated power/W	P_{rated}	500
Rated rotational speed/(r/min)	n_N	3000

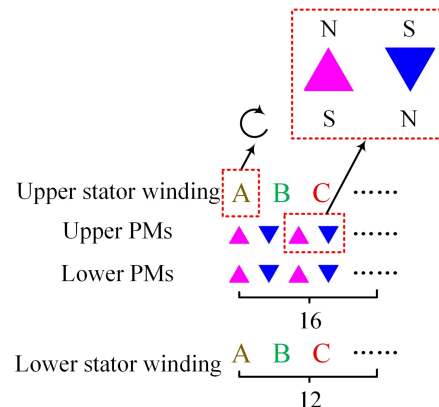


Figure 2. Schematic arrangement of the coils (A, B, C) and the PMs (N, S).

2.2. Electromagnetic Characteristics of the Healthy Prototype

The schematic of the healthy prototype is shown in Figure 3a, which includes three geometric centers: the red dashed lines denote the geometric centers of the upper and lower stators, the blue dashed line represents the geometric center of the rotor, and the purple z-axis indicates the rotational axis of the rotor. In the healthy condition, the geometric centers of both stators and the rotor coincide with the rotational axis, resulting in uniform air gaps on both sides. The nominal air-gap length, denoted as g_0 , remains constant during rotation, and the rotor mid-radius R_{mid} , defined as the distance from the rotor's geometric center to the center of the PM, is equal to the stator mid-radius. A 3-D FEM of the prototype is developed in ANSYS MAXWELL 2021 R1 to compute its electromagnetic characteristics under eccentricity conditions, and a full 3-D model is necessary because eccentricity introduces 3-D field asymmetry that cannot be captured by 2-D models. The mesh discretization of key components is shown in Figure 3b. Mesh independence is verified as shown in Figure 4, where the fundamental amplitude of the no-load back-EMF and the average electromagnetic torque converge to stable values when the number of mesh elements reaches 816,907. The simulation time step is 0.02 ms.

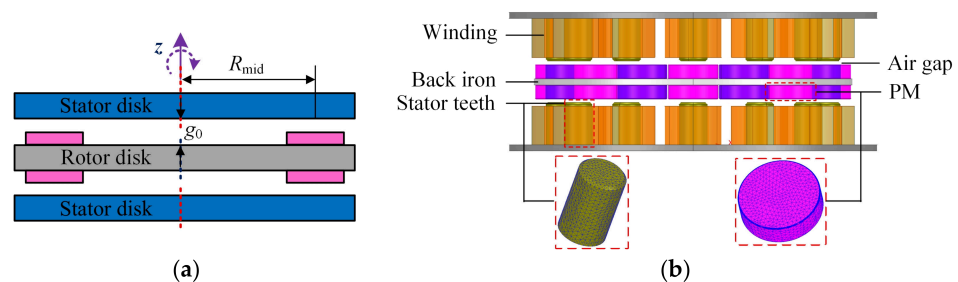


Figure 3. The healthy prototype is illustrated with (a) a schematic of the relative positions between the stator and rotor and (b) the FE model.

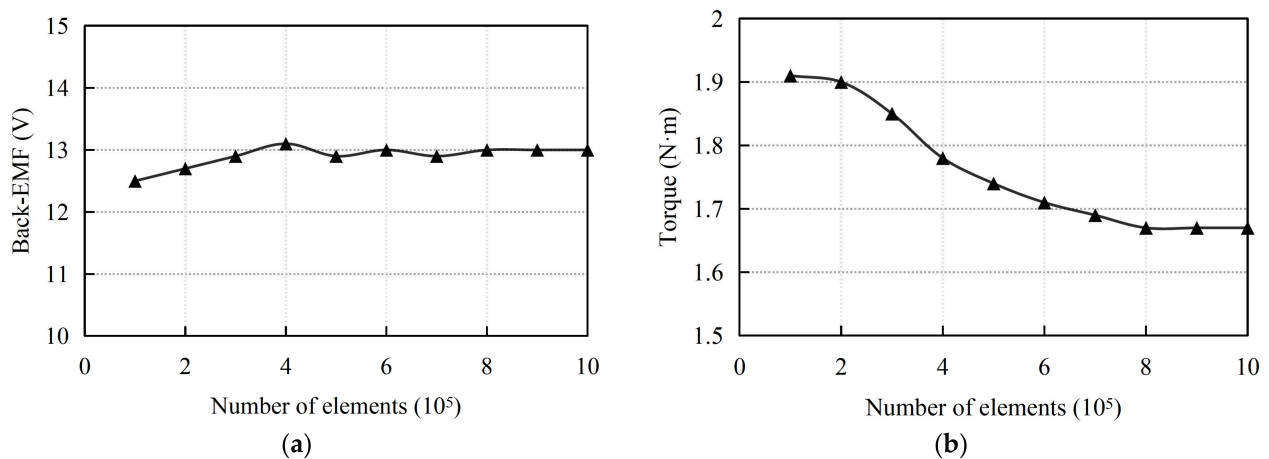


Figure 4. Mesh independence verification: (a) no-load back-EMF fundamental amplitude; (b) average electromagnetic torque.

The axial air-gap flux density $B_z(r, \theta, t)$, which is the superposition of the fields generated by the PMs and the armature windings, modulated by the air-gap permeance, can be expressed as:

$$B_z(r, \theta, t) = \sum_{k=1,3,5,\dots}^{\infty} [B_{PM,k}(r) \cos(kp\theta) + B_{S,k}(r) \cos(kp\theta - k\omega t)] \quad (1)$$

where r is the radial coordinate, θ is the circumferential angle, k is the harmonic order, p is the number of pole pairs, ω is the fundamental angular frequency, $B_{PM,k}(r)$ and $B_{S,k}(r)$ are the amplitudes of the k -th harmonic air-gap flux density generated by PMs and stator windings, respectively.

The axial air-gap flux density of the healthy prototype is shown in Figure 5. The three-phase no-load back-EMF is presented in Figure 6. The fast Fourier transform (FFT) of the phase-A no-load back-EMF yields the harmonic spectrum shown in Figure 7, where the fundamental amplitude is 12.81 V and the total harmonic distortion (THD) is about 7%. The cogging torque and electromagnetic torque are illustrated in Figures 8 and 9, respectively. The electromagnetic torque fluctuates between 1.59 N·m and 1.74 N·m, with an average value of 1.67 N·m. The torque ripple is about 9% and is defined as follows:

$$T_{\text{ripple}} = \frac{T_{\text{max}} - T_{\text{min}}}{T_{\text{avg}}} \times 100\% \quad (2)$$

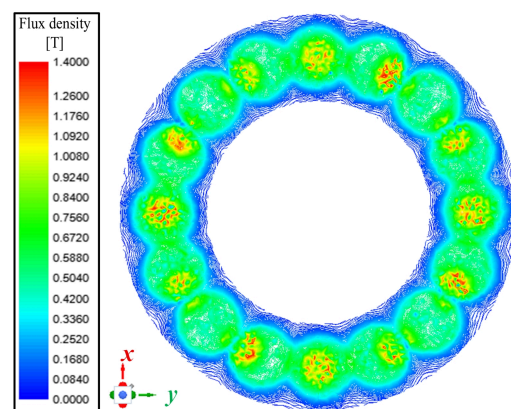


Figure 5. Axial air-gap flux density of the healthy prototype.

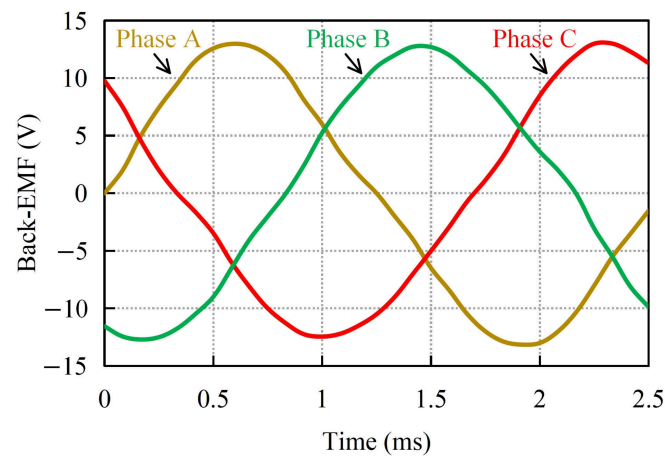


Figure 6. Three-phase no-load back-EMF of the healthy prototype.

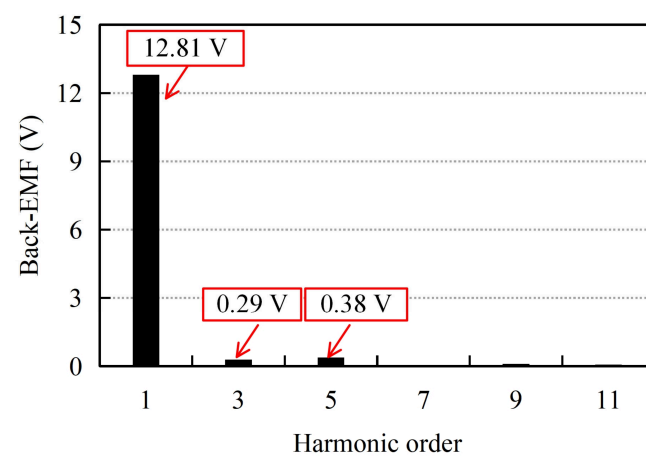


Figure 7. Harmonic amplitudes of phase-A no-load back-EMF for the healthy prototype.

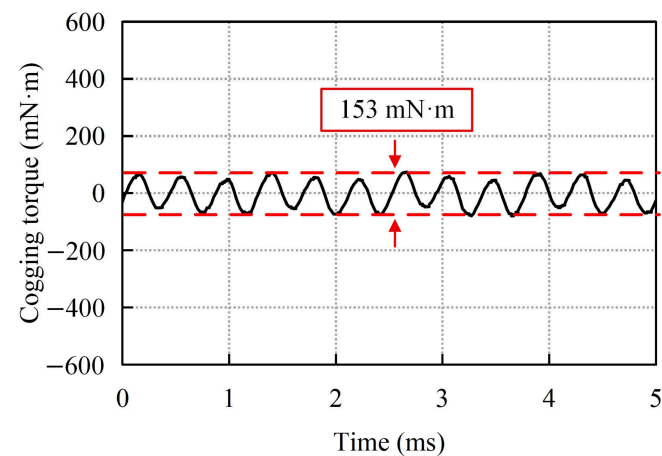


Figure 8. Cogging torque of the healthy prototype.

The axial electromagnetic force F_z can be obtained using the Maxwell stress tensor (MST) method:

$$f_z(r, \theta, t) = \frac{B_z^2(r, \theta, t)}{2\mu_0} \quad (3)$$

$$dF_z(r, \theta, t) = f_z(r, \theta, t) dS = \frac{B_z^2(r, \theta, t)}{2\mu_0} r dr d\theta \quad (4)$$

After decomposing F_z along the x - and y -axes of the air-gap plane, the UMFs are given by:

$$F_x(t) = \int_0^{2\pi} \int_{R_i}^{R_o} \frac{B_z^2(r, \theta, t)}{2\mu_0} r \sin \theta dr d\theta \quad (5)$$

$$F_y(t) = \int_0^{2\pi} \int_{R_i}^{R_o} \frac{B_z^2(r, \theta, t)}{2\mu_0} r \cos \theta dr d\theta \quad (6)$$

where R_i and R_o are the active inner and outer radii of the motor, respectively.

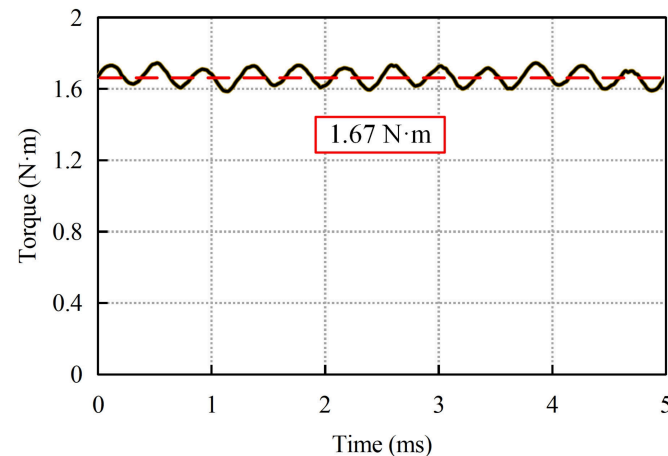


Figure 9. Electromagnetic torque of the healthy prototype.

The outer stators on both sides are subjected to UMF, as shown in Figure 10a. Due to the cogging effect, the axial electromagnetic force acting on each stator tooth is non-uniform in the circumferential direction. Each pair of back-to-back PMs with the same magnetization direction is defined as one group, and the axial electromagnetic force distribution of the 16 PM groups under no-load conditions is presented in Figure 10b. Since each PM group is subjected to equal-magnitude and opposite-direction axial UMFs from the two outer stators, the net axial force sums to zero.

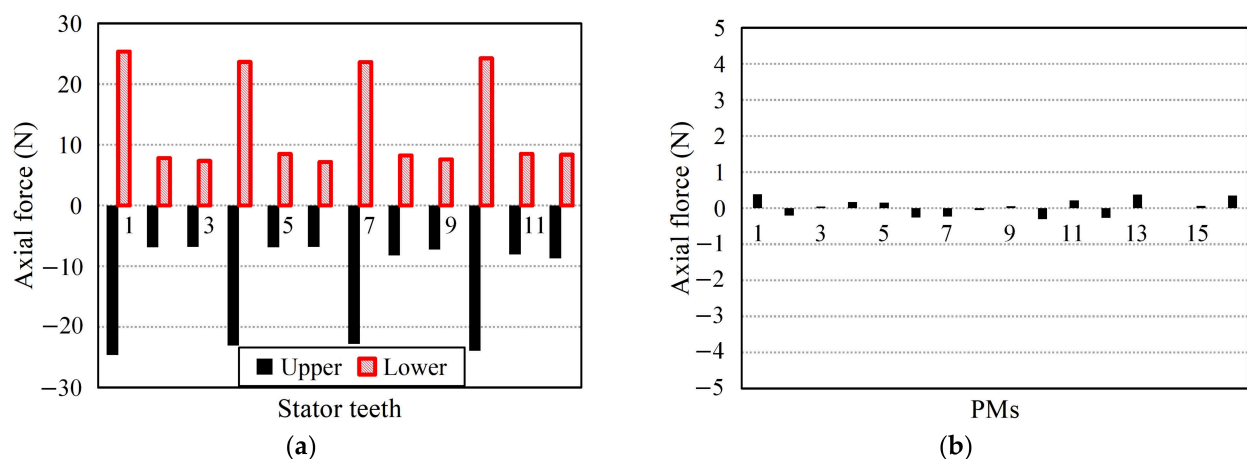


Figure 10. Axial electromagnetic force of (a) stator teeth and (b) PMs.

Figure 11 presents the flux density distribution on the axial cross-section of the shared rotor core under rated load conditions. It can be observed that the rotor yoke region, which serves as the magnetic bridge between the upper and lower stators, exhibits significant saturation. The maximum flux density in this region reaches 1.75 T, exceeding the knee point of the B-H curve. This deep saturation provides the necessary physical condition for

the cross-coupling effect, as the permeability of the shared yoke becomes highly dependent on the instantaneous, combined magnetomotive force of both stators.

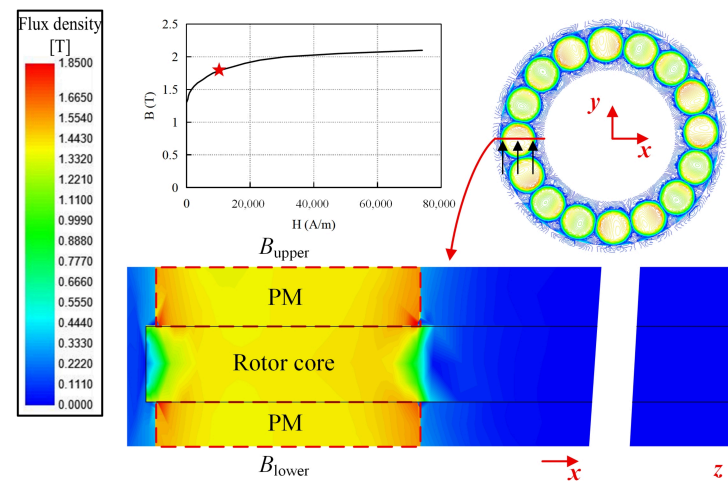


Figure 11. Flux density distribution on the axial cross-section of the rotor core.

To further quantify the coupling strength without resorting to an artificially decoupled model, the spatial correlation between the upper and lower air-gap flux densities (B_{upper} and B_{lower}) is evaluated along the circumferential direction at the same axial cross-section. Under ideal linear conditions, the flux distributions in the two air gaps are theoretically independent, and their spatial correlation coefficient approaches zero. However, under rated load, the calculated Pearson correlation coefficient between B_{upper} and B_{lower} reaches 0.82. This non-zero correlation directly demonstrates that the flux distribution in one air-gap is modulated by the magnetic state of the opposite air-gap through the saturated shared yoke. Consequently, since the analytical formulations cannot capture this load-dependent nonlinear interaction, the 3-D FEM is strictly employed for all subsequent eccentricity evaluations where this cross-coupling becomes even more pronounced and critical to the UMF predictions.

2.3. Experiments on the Healthy Prototype

The experimental platform is established as shown in Figure 12. The control system consists of a control board based on the STM32F407IGT6 microcontroller, a driver board, and a host computer for real-time monitoring and data acquisition. The sensing and measurement system includes current sensors, temperature sensors, an oscilloscope (RIGOL MSO5204, Suzhou, China), and a torque meter (YELI N200, Shanghai, China). The load system is composed of a dynamometer with its own power supply and speed regulation device. In this study, the experimental verification is first performed on the healthy prototype to validate the accuracy of the 3-D FEM model. All simulations and experiments are performed under the rated operating conditions, with a DC-bus voltage of 24 V, a rated rotational speed of 3000 r/min, a DC-bus current of 28 A, and sinusoidal current excitation with the current angle set to 0° . The corresponding rated load torque is approximately 1.6 N·m. To verify the accuracy of the 3-D FEM model, the no-load back-EMF of the healthy motor is measured at rated speed and analyzed by FFT. The harmonic amplitudes of phase-A are compared with the simulation results, as illustrated in Figure 13. Both results are basically consistent, with a THD of about 9%. In addition, the measured average electromagnetic torque of the motor is 1.6 N·m, which is in good agreement with the simulation result. Consequently, the accuracy of the 3-D FEM is verified, providing a reliable basis for the subsequent analysis of eccentricity faults.

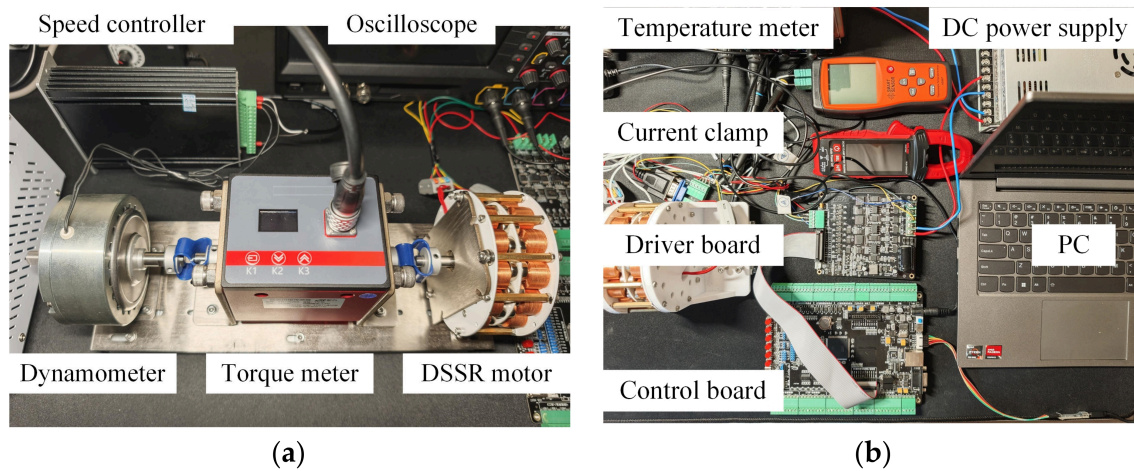


Figure 12. Experimental platform: (a) mechanical and measurement setup; (b) electrical control and monitoring setup.

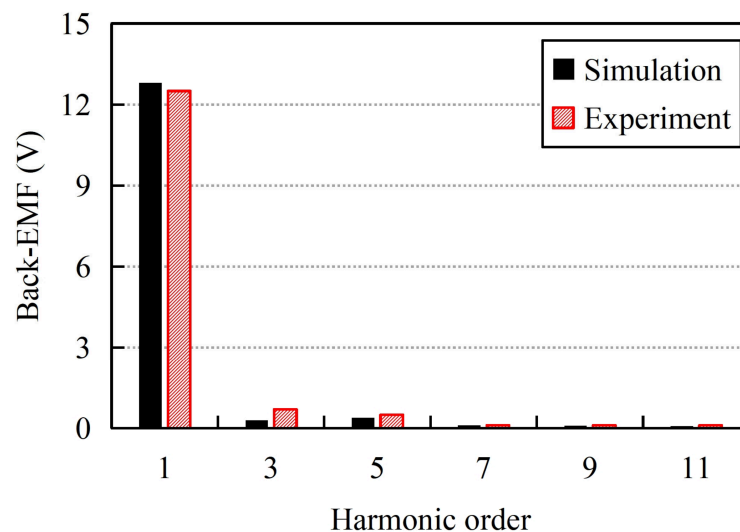


Figure 13. Experimental validation of the harmonic amplitudes of no-load back-EMF.

3. Eccentricity Characteristics

Eccentricity in DSSR motors can arise from manufacturing and assembly tolerances as well as UMFs during prolonged operation. This leads to a non-uniform and asymmetrical air-gap distribution on both sides. In practice, eccentricity typically appears as a mixed form. For analytical convenience, mixed eccentricity is commonly simplified into four basic types: static eccentricity, dynamic eccentricity, axial eccentricity, and radial eccentricity. It is worth noting that the force generation mechanism in DSSR motors is distinct from conventional models due to the coupled magnetic circuit of the shared rotor, which leads to a non-linear interaction between the upper and lower air-gap fields.

3.1. Static Eccentricity

When static eccentricity occurs in a DSSR motor, the rotational axis of the rotor coincides with its geometric center, while the geometric centers of the upper and lower stators are offset, as illustrated in Figure 14. Here, $g_{\max}$ and $g_{\min}$ denote the maximum and minimum air-gap lengths at the stator mid-radius under static eccentricity, respectively, and β represents the eccentricity angle.

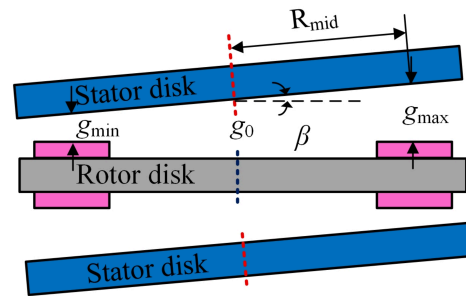


Figure 14. Schematic of the static eccentricity prototype.

The SEF of the DSSR motor is defined as follows:

$$\text{SEF} = \frac{g_{\max} - g_{\min}}{2g_0} \times 100\% = \frac{e_s}{g_0} \times 100\% \quad (7)$$

where e_s is the static eccentricity displacement at the stator mid-radius corresponding to the locations of maximum and minimum air-gap lengths, which can be expressed as:

$$e_s = g_{\max} - g_0 = g_0 - g_{\min} = \frac{g_{\max} - g_{\min}}{2} \quad (8)$$

The relationship between the eccentricity displacement, e_s , and the eccentricity angle, β , is given by:

$$e_s = R_{\text{mid}} \sin \beta \quad (9)$$

The variation in the eccentricity angle β with SEF is listed in Table 2. When the SEF increases from 10% to 40%, the stator eccentricity angle β increases from 0.1528° to 0.6112° , indicating that a very small eccentricity angle can produce a relatively large SEF. To clarify the practical meaning of the selected SEF range, each eccentricity level is further mapped to a corresponding mechanical scenario and classified according to its engineering relevance. The 10% SEF represents typical manufacturing tolerances and assembly errors, the 20% SEF corresponds to moderate fault conditions such as bearing wear or shaft deflection, and the 30% SEF represents severe fault conditions. The 40% SEF is considered an extreme or intentionally exaggerated case, which is used mainly to reveal the upper-bound trends of the electromagnetic behavior under highly asymmetric air-gap conditions rather than to represent a common operating condition.

Table 2. Variation in eccentricity angle with SEF and the corresponding practical fault scenarios.

SEF (%)	β (°)	Corresponding Physical Scenario	Category
10	0.1528	Manufacturing tolerance or assembly error	Realistic operational condition
20	0.3056	Moderate bearing wear or shaft deflection	Realistic fault condition
30	0.4584	Severe bearing clearance or shaft misalignment	Severe fault condition
40	0.6112	Extreme fault condition used to reveal upper-bound trends	Intentionally exaggerated limit case

The variation in the air-gap length at the stator mid-radius, denoted as $g_{\text{mid}}(\theta)$, with the circumferential angle θ is given by:

$$g_{\text{mid}}(\theta) = g_0 + e_s \cos(\theta - \gamma) = g_0 + g_0 \text{SEF} \cos(\theta - \gamma) \quad (10)$$

where γ is the circumferential angle corresponding to the location of maximum air-gap length, which is set to 0° by default. The variation in the air-gap length with radial coordinate r at this position, denoted as $\Delta g_\gamma(r)$, is given by:

$$\Delta g_\gamma(r) = (r - R_{\text{mid}}) \sin \beta \quad (11)$$

The variation in the air-gap length at the radial coordinate r and circumferential angle θ , denoted as $\Delta g_\gamma(r, \theta)$, is given by:

$$\Delta g(r, \theta) = (r - R_{\text{mid}}) \sin \beta \cos(\theta - \gamma) \quad (12)$$

The air-gap length at any location in the air-gap plane, under static eccentricity, is given by:

$$g(r, \theta) = g_{\text{mid}}(\theta) + \Delta g(r, \theta) = g_0 + g_0 \text{SEF} \cos(\theta - \gamma) + (r - R_{\text{mid}}) \sin \beta \cos(\theta - \gamma) = g_0 + \frac{g_0 r}{R_{\text{mid}}} \text{SEF} \cos(\theta - \gamma) \quad (13)$$

It can be seen that the variation in the air-gap length depends only on spatial position and is independent of time. When the SEF is 40%, the air-gap length as a function of radial coordinate r and circumferential angle θ is shown in Figure 15. In this case, the air-gap length is no longer uniform in the circumferential direction and also varies in the radial direction, with the variation becoming more pronounced toward the outer radius. Figure 16 illustrates the axial air-gap flux density as the SEF increases from 10% to 40%. Static eccentricity causes distortion in the air-gap flux density, and the distortion degree increases with SEF. In the range of 0° to 180° , the air-gap length is less than g_0 , resulting in an increase in air-gap flux density. In the range of 180° to 360° , the air-gap length exceeds g_0 , resulting in a reduction in air-gap flux density. The distortion is more severe near the outer radius. The air-gap permeance $\Lambda_s(r, \theta)$ can be expressed as:

$$\Lambda_s(r, \theta) = \frac{\mu_0}{g_0 + \frac{g_0 r}{R_{\text{mid}}} \text{SEF} \cos(\theta - \gamma)} \quad (14)$$

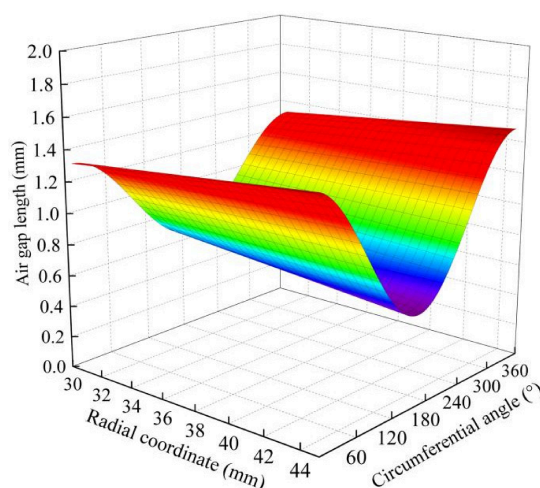


Figure 15. Variation in air-gap length with SEF.

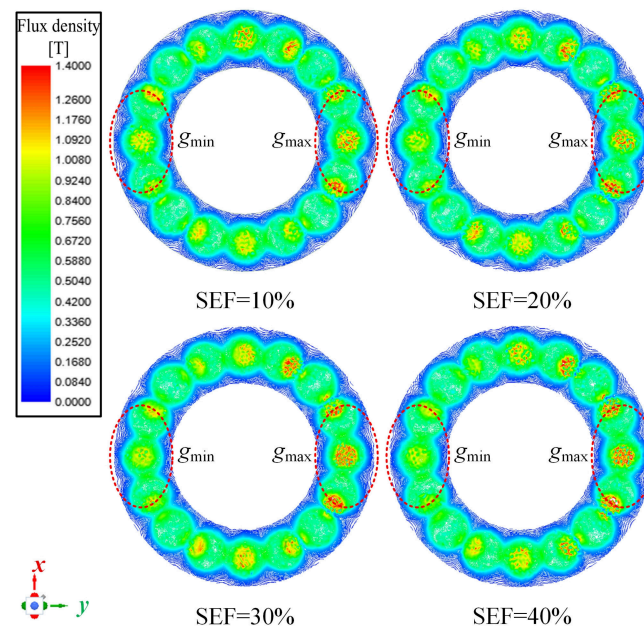


Figure 16. Axial air-gap flux density under different SEF conditions.

Its Fourier series expansion yields the following:

$$\Lambda_S(r, \theta) = \Lambda_0 + \sum_{k=1,3,5,\dots}^{\infty} \Lambda_1 \cos[k(\theta - \gamma)] \quad (15)$$

where Λ_1 is the fundamental amplitude of the air-gap permeance.

The 40% eccentricity case is emphasized in the following analysis because the fault characteristics become more pronounced at higher eccentricity levels, which facilitates a clearer identification and distinction of the specific features associated with different types of eccentricity. The harmonic amplitudes of the phase-A no-load back-EMF under static eccentricity are compared with those of the healthy prototype in Figure 17, showing that the amplitudes of all harmonics are nearly identical. The electromagnetic torque waveforms of both conditions are compared in Figure 18, where the average torque is 1.67 N·m, consistent with the healthy prototype, and the torque waveform exhibits negligible variation, indicating that static eccentricity has little effect on the no-load back-EMF and electromagnetic torque of the DSSR motor. As shown in Figure 19a, static eccentricity exacerbates the non-uniformity of the axial electromagnetic force acting on both upper and lower stator teeth. The axial force at the air-gap extremum locations increases, with the peak value reaching about 28 N, which is 14% higher than that of the healthy prototype; this intensifies the axial electromagnetic force waves. The stator tooth experiencing the maximum force remains fixed and does not change with rotor rotation. As shown in Figure 19b, the net axial electromagnetic force on the rotor remains zero under static eccentricity, but the axial force on each PM group differs. This imbalance results in additional bending moments acting on both the stator and the rotor.

Under eccentric conditions, the bending moment between the stator and rotor arises from the asymmetry of the air gaps on both sides. The bending moment at radial coordinate r and circumferential angle θ can be expressed by M_x and M_y :

$$M_x = \int_0^{2\pi} \int_{R_i}^{R_o} f_z(r, \theta) r^2 \sin \theta dr d\theta \quad (16)$$

$$M_y = \int_0^{2\pi} \int_{R_i}^{R_o} f_z(r, \theta) r^2 \cos \theta dr d\theta \quad (17)$$

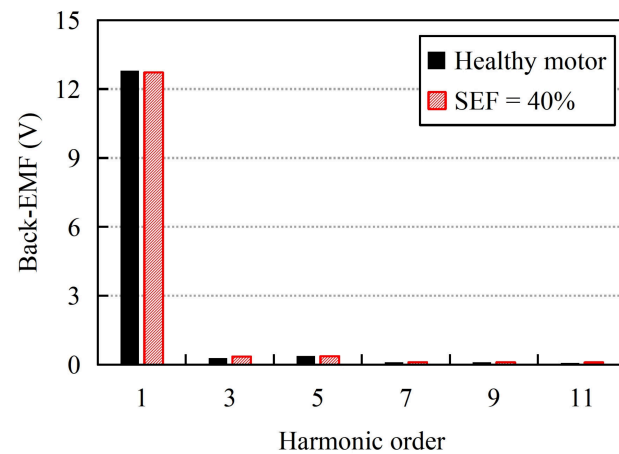


Figure 17. Harmonic amplitudes of phase-A no-load back-EMF at SEF = 40%.

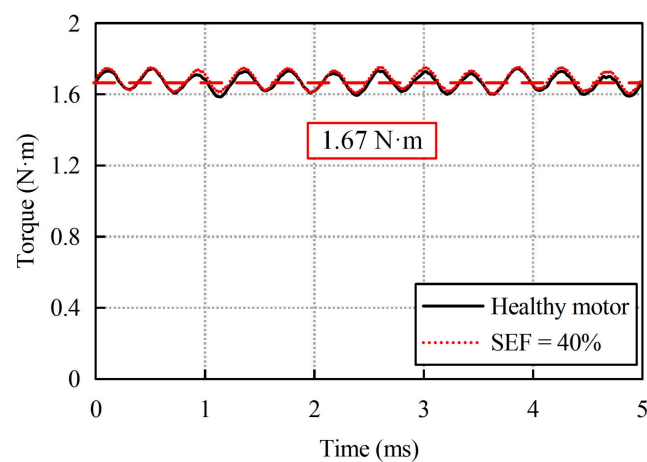


Figure 18. Electromagnetic torque at SEF = 40%.

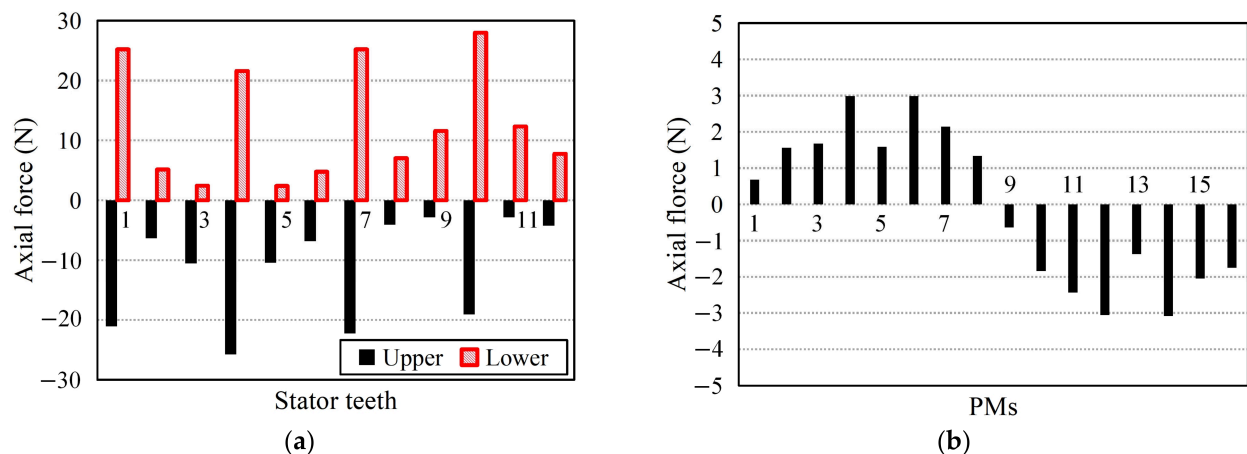


Figure 19. Axial UMF at SEF = 40%. (a) Stator teeth; (b) PMs.

The resultant bending moment is given as:

$$M = \sqrt{M_x^2 + M_y^2} \quad (18)$$

In the healthy prototype, the axial electromagnetic forces acting on the stator and rotor are symmetric with respect to both the x - and y -axes, resulting in a bending moment that is nearly zero. Figure 20 shows the variation in the rotor bending moment under different SEFs, where the bending moment is proportional to the SEF and remains constant over

time, exerting a steady reaction force on the bearings. If the stator is assumed to undergo static eccentricity about the x -axis, the prototype exhibits only a bending moment about the x -axis, while the bending moment about the y -axis is zero. When the SEF is 40%, the bending moment experienced by the bearings is 0.85 N·m.

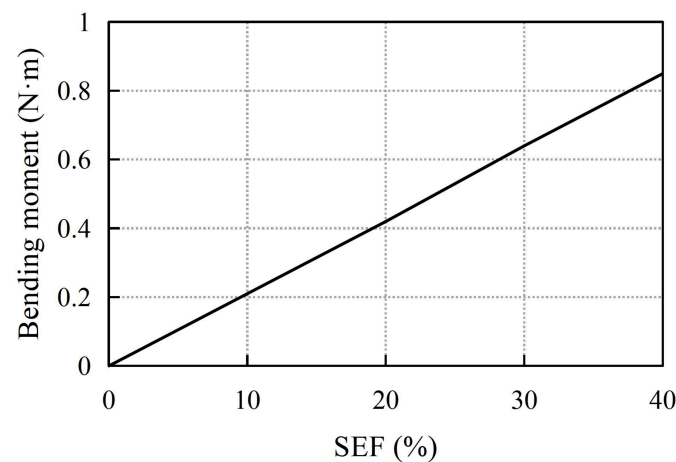


Figure 20. Rotor bending moments under different SEFs.

3.2. Dynamic Eccentricity

When dynamic eccentricity occurs in a DSSR motor, the rotational axis of the rotor coincides with the geometric center of the stator, whereas the geometric center of the rotor is offset, as illustrated in Figure 21. The locations of maximum and minimum air-gap lengths rotate synchronously with the rotor, but the maximum air-gap length $g_{\max}$ and minimum air-gap length $g_{\min}$ remain constant.

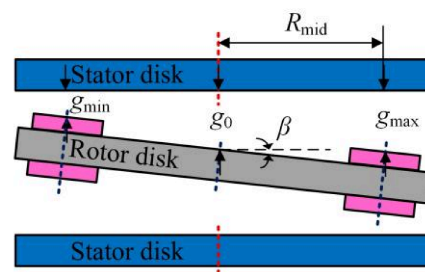


Figure 21. Schematic of the dynamic eccentricity prototype.

The dynamic eccentricity factor (DEF) of the DSSR motor is defined as follows:

$$\text{DEF} = \frac{g_{\max} - g_{\min}}{2g_0} \times 100\% = \frac{e_d}{g_0} \times 100\% \quad (19)$$

where e_d is the dynamic eccentricity displacement at the stator mid-radius, corresponding to the locations of maximum and minimum air-gap length. The variation in the air-gap length at the stator mid-radius, denoted as $g_{\text{mid}}(\theta, t)$, with rotor circumferential angle θ and time t is given by:

$$g_{\text{mid}}(\theta, t) = g_0 + g_0 \text{DEF} \cos(\theta - \omega t - \gamma) \quad (20)$$

The time-varying change in the air-gap length at radial coordinate r and circumferential angle θ , denoted as $\Delta g(r, \theta, t)$, is given by:

$$\Delta g(r, \theta, t) = (r - R_{\text{mid}}) \sin \beta \cos(\theta - \omega t - \gamma) \quad (21)$$

The air-gap length at any location in the air-gap plane, under dynamic eccentricity, is given by:

$$\begin{aligned} g(r, \theta, t) &= g_{\text{mid}}(\theta, t) + \Delta g(r, \theta, t) = g_0 + g_0 \text{DEF} \cos(\theta - \omega t - \gamma) + r \sin \beta \cos(\theta - \omega t - \gamma) - R_{\text{mid}} \cos(\theta - \omega t - \gamma) \\ &= g_0 - \frac{g_0 r}{R_{\text{mid}}} \text{DEF} \cos(\theta - \omega t - \gamma) \end{aligned} \quad (22)$$

In contrast to static eccentricity, the effect of dynamic eccentricity on the air-gap length depends on both the rotor's spatial position and time. The air-gap length remains non-uniform in both the circumferential and radial directions, with the variation becoming more pronounced toward the outer radius. Figure 22 illustrates the axial air-gap flux density as the DEF increases from 10% to 40%. Dynamic eccentricity causes distortion in the air-gap flux density, and the degree of distortion increases with DEF. In the range of 0° to 180° , the air-gap length is less than g_0 , resulting in an increase in air-gap flux density. In the range of 180° to 360° , the air-gap length exceeds g_0 , resulting in a reduction in air-gap flux density. Since the air-gap length varies periodically during rotor rotation, the air-gap flux density also exhibits periodic fluctuations over time. The air-gap permeance $\Lambda_D(r, \theta, t)$ can be expressed as:

$$\Lambda_D(r, \theta, t) = \frac{1}{g_0 - \frac{g_0 r}{R_{\text{mid}}} \text{DEF} \cos(\theta - \omega t - \gamma)} \quad (23)$$

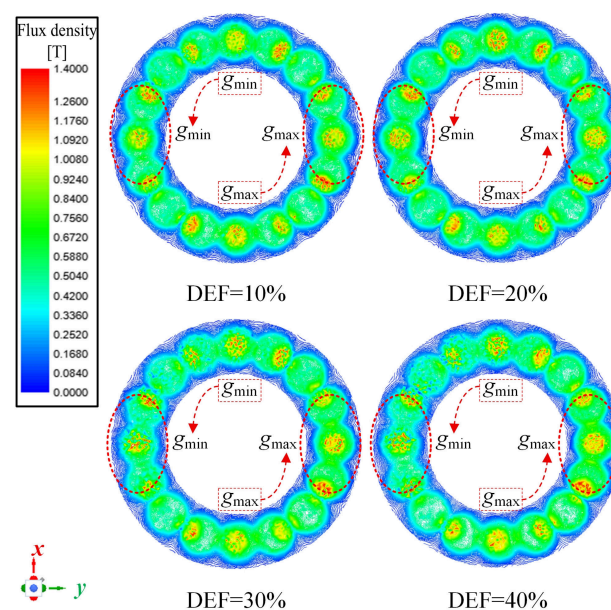


Figure 22. Axial air-gap flux density under different DEF conditions.

Its Fourier series expansion yields the following:

$$\Lambda_D(r, \theta, t) = \Lambda_0 + \sum_{k=1,3,5,\dots}^{\infty} \Lambda_n \cos n(\theta - \omega t - \gamma) \quad (24)$$

When the DEF is 40%, the harmonic amplitudes of the phase-A no-load back-EMF are compared with those of the healthy prototype in Figure 23, which shows that the amplitudes of all harmonic components are nearly identical. The electromagnetic torque waveforms of both conditions are compared in Figure 24, where the average torque is $1.67 \text{ N}\cdot\text{m}$, consistent with the healthy prototype, and the torque waveform exhibits negligible variation, indicating that dynamic eccentricity has little effect on the no-load back-EMF and electromagnetic torque of the DSSR motor.

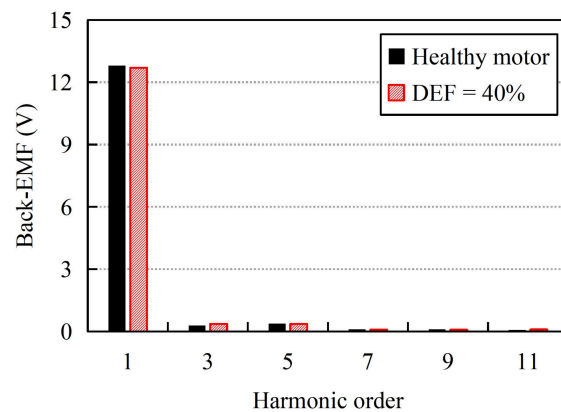


Figure 23. Harmonic amplitudes of phase-A no-load back-EMF at DEF = 40%.

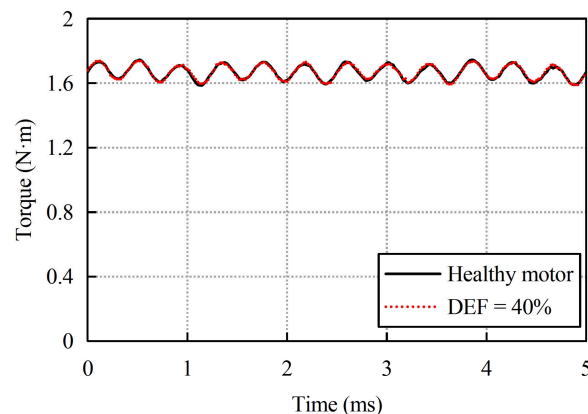


Figure 24. Electromagnetic torque at DEF = 40%.

Unlike static eccentricity, the direction of the bending moment on the stator under dynamic eccentricity varies periodically with time. The bending moments along the x - and y -axes, denoted as M_x and M_y , are given by:

$$M_x = \int_0^{2\pi} \int_{R_i}^{R_o} f_z(r, \theta, t) r^2 \sin \theta dr d\theta \quad (25)$$

$$M_y = \int_0^{2\pi} \int_{R_i}^{R_o} f_z(r, \theta, t) r^2 \cos \theta dr d\theta \quad (26)$$

The resultant bending moment can still be obtained by using (18). Unlike the constant bending moment caused by static eccentricity, this moment exerts an alternating reaction force on the bearings, with its direction varying periodically at a frequency equal to the rotor rotational frequency. Figure 25 shows the time-varying bending moment acting on the stator for DEF ranging from 10% to 40%, indicating a further increase in bending moment magnitude with increasing DEF. When the DEF is 40%, the peak bending moment reaches 0.85 N·m. This alternating characteristic is prone to coupling with the rotor's critical speed, potentially inducing resonance and posing a serious threat to the structural fatigue life of high-speed DSSR motors. Therefore, it is necessary to carry out a rotor dynamic analysis of the DSSR motor and determine the critical speeds of the rotor. The rotor shaft, rotor core, and PMs are assumed to form an integrated structure, and minor structural features are reasonably simplified. The natural frequencies and critical speeds corresponding to each modal order of the rotor are presented in Table 3. The corresponding mode shapes are shown in Figure 26. For the first-order mode, the corresponding natural frequency is 748 Hz and the critical speed is 44,910 r/min. The rotor rotational frequency at the rated speed of 3000 r/min is 75 Hz, which is far below the first natural frequency of 748 Hz. The

rated speed is only 6.68% of the first critical speed, indicating a substantial safety margin. It can be concluded that the rotor is unlikely to reach a resonant state, and the motor is able to operate reliably.

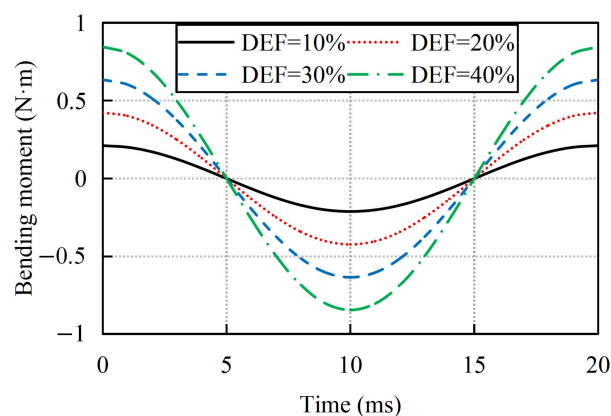


Figure 25. Stator bending moments under different DEFs.

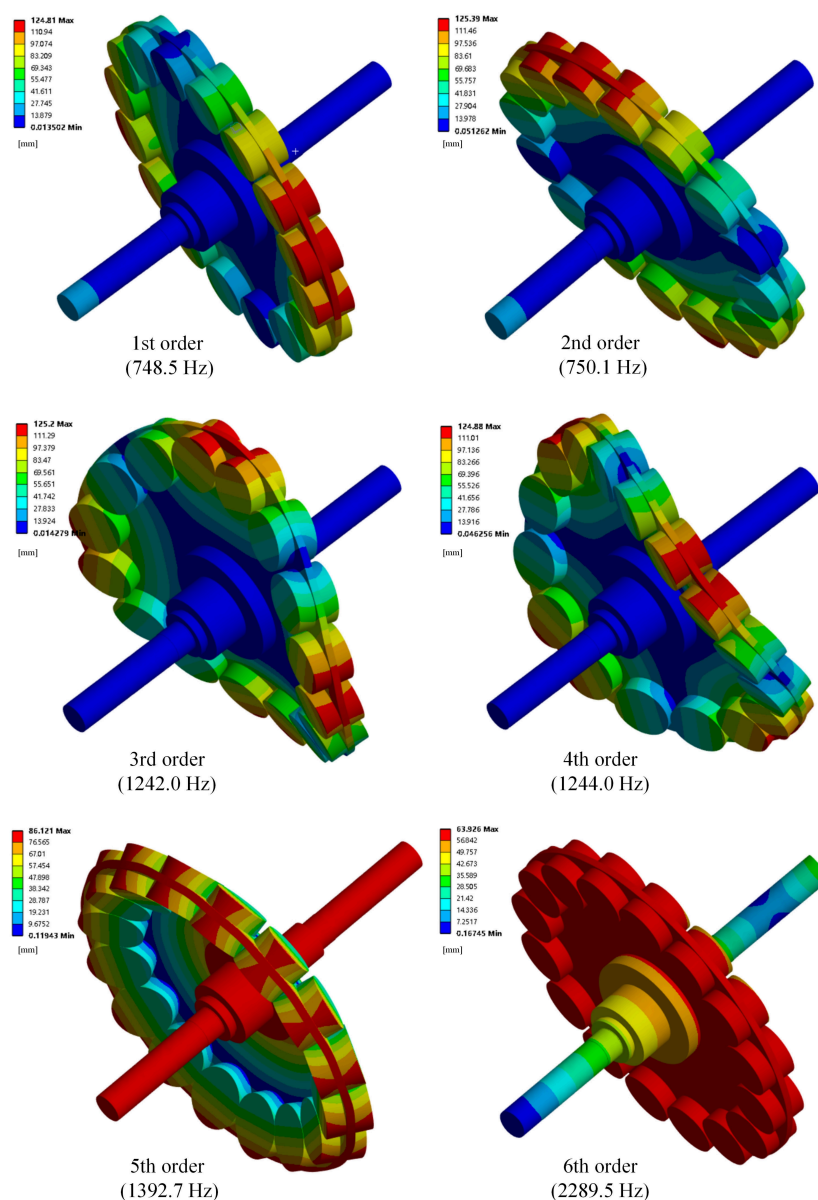


Figure 26. Mode shape diagram of the rotor.

Table 3. Rotor natural frequencies and critical speeds.

Mode	Natural Frequency (Hz)	Critical Speed (r/min)
1st	748.5	44,910
2nd	750.1	45,006
3rd	1242.0	74,520
4th	1244.0	74,640
5th	1392.7	83,562
6th	2289.5	137,370

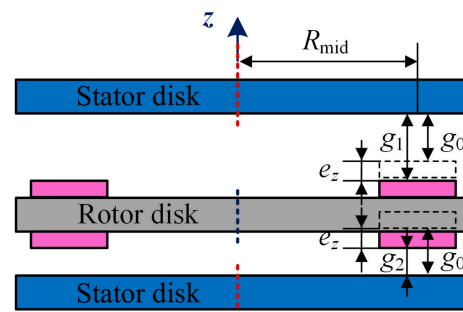
In summary, both static and dynamic eccentricity impose additional bending moments on the stator and rotor supports, subjecting the bearings to either steady or alternating reaction forces and thereby reducing bearing life. Therefore, the stator and rotor of the DSSR motor must be designed with sufficient stiffness to resist the additional bending moments induced by eccentricity.

3.3. Axial Eccentricity

When axial eccentricity occurs in a DSSR motor, the rotor shifts axially toward one of the stators, leading to an asymmetric air-gap distribution in which the air-gap on one side increases while that on the other side decreases. This directly undermines the design principle of magnetic circuit symmetry inherent in the DSSR topology, as shown in Figure 27. However, the geometric centers of the stator and rotor, as well as the rotor's rotational axis, remain coincident, and the sum of the air-gap lengths on both sides remains constant, which can be expressed as:

$$g_1 + g_2 = 2g_0 \quad (27)$$

where g_1 and g_2 are the upper and lower air-gap lengths, respectively.

**Figure 27.** Schematic of the axial eccentricity prototype.

The axial eccentricity factor (AEF) is defined as follows:

$$AEF = \frac{e_z}{g_0} \times 100\% \quad (28)$$

where e_z is the axial eccentricity displacement.

The upper and lower air-gap lengths are expressed as follows:

$$g_1(r, \theta) = g_0(1 + AEF) \quad (29)$$

$$g_2(r, \theta) = g_0(1 - AEF) \quad (30)$$

The variations in air-gap lengths on both sides with the AEF are listed in Table 4.

Table 4. Variation in air-gap lengths on both sides with respect to AEF.

AEF (%)	10	20	30	40
g_1 (mm)	1.1	1.2	1.3	1.4
g_2 (mm)	0.9	0.8	0.7	0.6

The air-gap permeances, Λ_{A1} and Λ_{A2} , for the upper and lower sides are expressed as:

$$\Lambda_{A1} = \frac{\mu_0}{g_0(1 + \text{AEF})} \quad (31)$$

$$\Lambda_{A2} = \frac{\mu_0}{g_0(1 - \text{AEF})} \quad (32)$$

As the AEF increases from 10% to 40%, the axial flux density in the upper and lower air gaps changes as shown in Figure 28, and the difference between the flux density amplitudes on the two sides increases with AEF. To explicitly characterize the internal magnetic asymmetry, the harmonic amplitudes of the phase-A no-load back-EMF for the upper and lower stator windings prior to their parallel connection are shown in Figure 29 for the 40% AEF condition. The fundamental amplitude of the no-load back-EMF for the winding on the side with increased air-gap decreases to 11.56 V, and its THD decreases to 4.5%. In contrast, the fundamental amplitude on the side with reduced air-gap rises to 14.03 V, resulting in a 17.6% deviation in fundamental amplitude between the two sides. In addition, the 3rd and 5th harmonic amplitudes increase accordingly; the THD rises to 12.5%, and the high-order harmonic distributions of the two windings are significantly different. It should be noted that in practical drive systems, the two stator windings are often connected in parallel or in series. Under axial eccentricity, the no-load back-EMF imbalance between the two stators will induce circulating currents in the parallel-connected windings, leading to additional copper losses and uneven heating. The electromagnetic torque is shown in Figure 30, where the average torque increases to 1.69 N·m and the torque ripple increases from approximately 9% under healthy conditions to approximately 18%. Figure 31a shows that the axial electromagnetic force on all stator teeth increases on the side with a smaller air-gap and decreases on the side with a larger air-gap. Figure 31b shows that the rotor experiences an increase in UMF of 53.2 N directed toward the side with the decreased air-gap. This UMF is directly transmitted to the bearing system, potentially inducing axial rotor runout. Axial eccentricity does not introduce additional bending moments on the stator or rotor. However, a larger air-gap asymmetry leads to a greater axial UMF, which simultaneously degrades electromagnetic symmetry and mechanical stability. Therefore, axial alignment tolerances must be strictly controlled during assembly, and online monitoring based on the ratio of back-EMF amplitudes from both sides is recommended.

3.4. Radial Eccentricity

When radial eccentricity occurs in a DSSR motor, the geometric center of the rotor coincides with its rotational axis but is offset from the geometric center of the stator by a radial eccentricity displacement, e_r , as shown in Figure 32. In this case, the air-gap length on both sides remains equal to g_0 :

$$g(r, \theta) = g_0 \quad (33)$$

This is because radial eccentricity in DSSR motors represents a purely lateral misalignment between parallel stator and rotor disks without any axial tilt or deformation, which inherently maintains a uniform air-gap length.

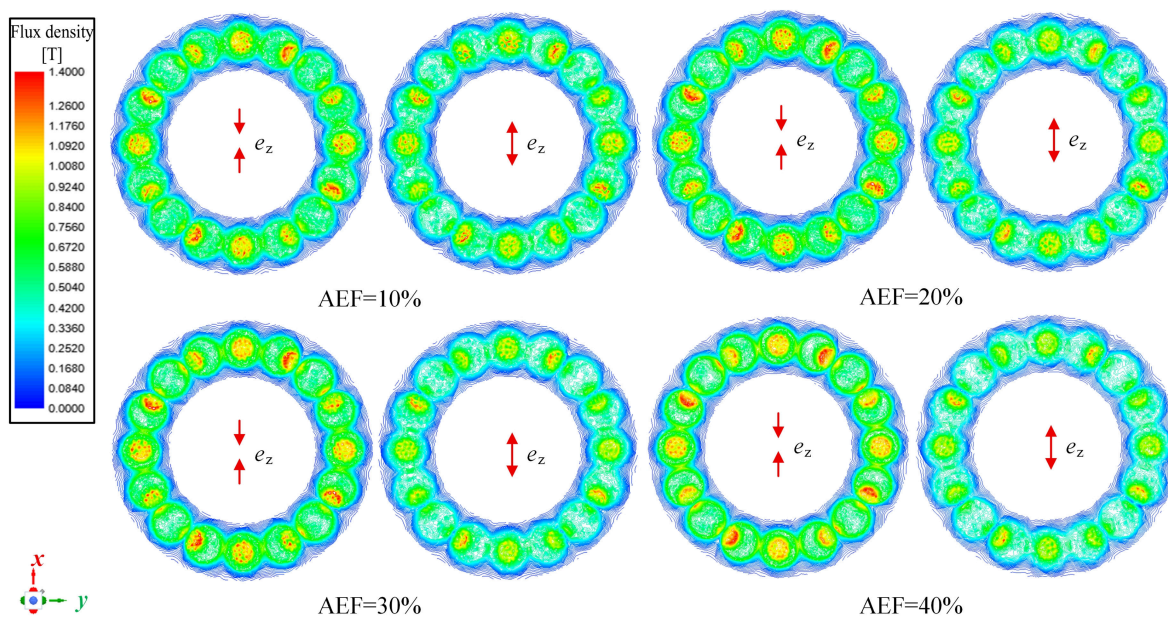


Figure 28. Axial air-gap flux density under different AEF conditions.

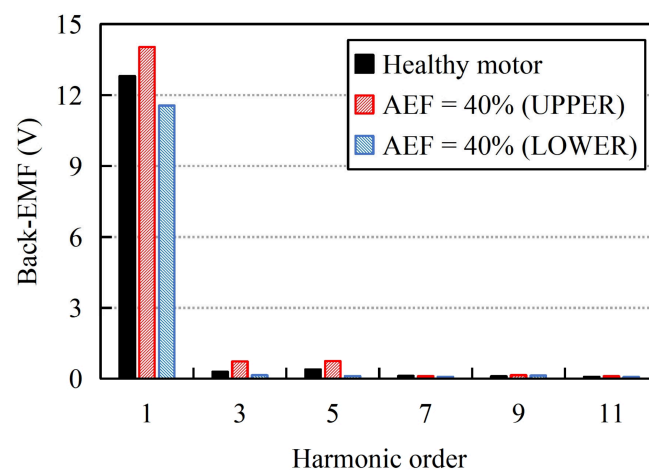


Figure 29. Harmonic amplitudes of phase-A no-load back-EMF for the upper and lower stator windings at AEF = 40%.

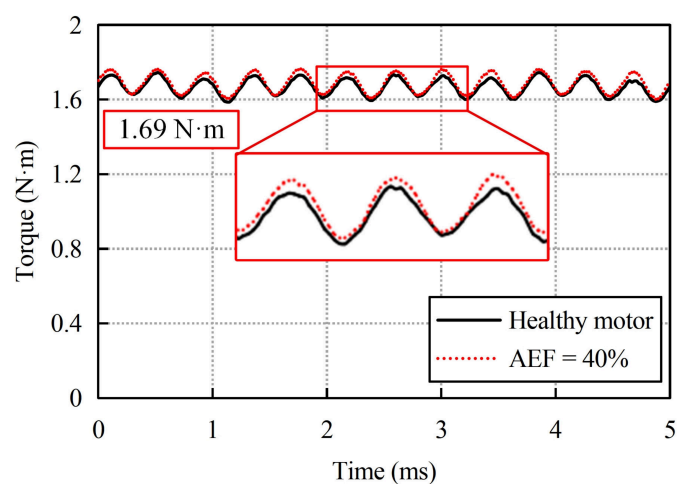


Figure 30. Electromagnetic torque at AEF = 40%.

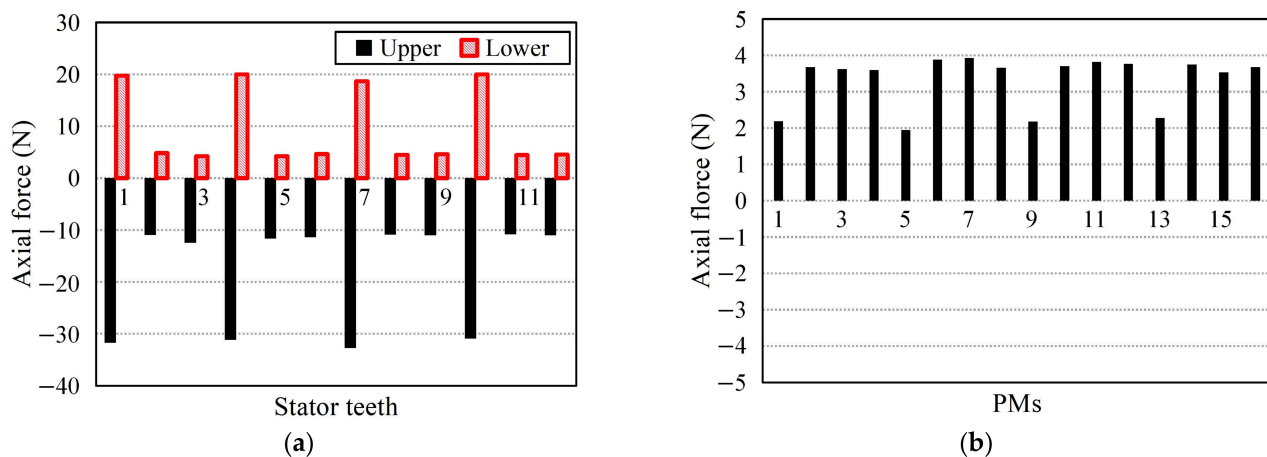


Figure 31. Axial UMF at AEF = 40%. (a) Stator teeth; (b) PMs.

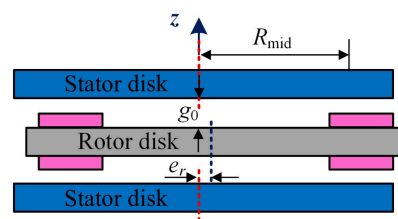


Figure 32. Schematic of the radial eccentricity prototype.

However, the radial eccentricity displacement e_r causes a misalignment between the stator and rotor, resulting in a reduction in the effective overlapping area. To quantify this effect, an area modification factor $k_r(\theta)$ is introduced. This factor represents the ratio of the effective overlapping width to the nominal radial length at any circumferential angle θ :

$$k_r(\theta) = \frac{\sqrt{R_o^2 - e_r^2 \sin^2 \theta} - \sqrt{R_i^2 - e_r^2 \sin^2 \theta}}{R_o - R_i} \quad (34)$$

The radial eccentricity factor (REF) is defined as follows:

$$\text{REF} = \frac{e_r}{g_0} \times 100\% \quad (35)$$

The variation in radial eccentricity displacement with the REF is listed in Table 5.

Table 5. Variation in radial eccentricity displacement with REF.

REF (%)	10	20	30	40
e_r (mm)	0.1	0.2	0.3	0.4

Thus, the factor simplifies to the following:

$$k_r(\theta) \approx 1 - \frac{e_r^2 \sin^2 \theta}{2(R_o - R_i)} \left(\frac{1}{R_o} + \frac{1}{R_i} \right) \quad (36)$$

Consequently, the air-gap permeance Λ_R is modulated by this area factor. For a small eccentricity, the permeance can be expressed as:

$$\Lambda_R(\theta) \approx \Lambda_0 \cdot k_r(\theta) \quad (37)$$

Since the radial dimension of the DSSR motor is typically much larger than the radial eccentricity displacement ($R_o - R_i \gg e_r$), this perturbation remains extremely small. As the REF increases from 10% to 40%, the axial air-gap flux density is shown in Figure 33, where the flux density shifts radially but exhibits minimal variation compared to other eccentricity conditions. When the REF is 40%, the harmonic amplitudes of the phase-A no-load back-EMF are compared with those of the healthy prototype in Figure 34, showing that the harmonic amplitudes are nearly identical. The electromagnetic torque is shown in Figure 35, where the average torque is 1.67 N·m, consistent with the healthy prototype, and the torque waveform shows negligible variation. This indicates that radial eccentricity has little effect on the no-load back-EMF and electromagnetic torque of the DSSR motor and does not introduce new characteristic frequency components. As shown in Figure 36, radial eccentricity has a minor impact on the axial electromagnetic forces acting on both the stator and rotor. This is because the air-gap magnetic field primarily distributes along the axial direction, and radial eccentricity does not alter the air-gap length, thus maintaining the spatial symmetry of the air-gap flux density. Consequently, no UMF or additional bending moment is generated. The axial electromagnetic forces on both stators remain highly symmetric, with a resultant force deviation less than 1 N, demonstrating the inherent electromagnetic robustness of the DSSR topology against radial assembly errors.

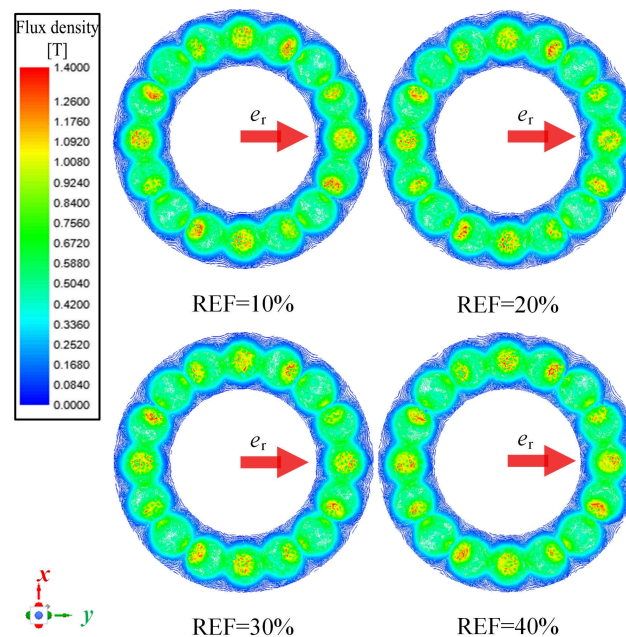


Figure 33. Axial air-gap flux density under different REF conditions.

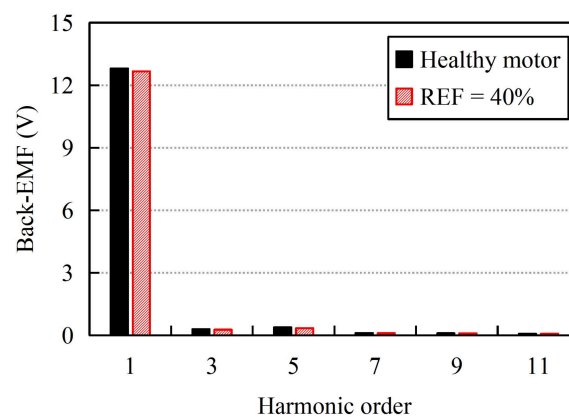


Figure 34. Harmonic amplitudes of phase-A no-load back-EMF at REF = 40%.

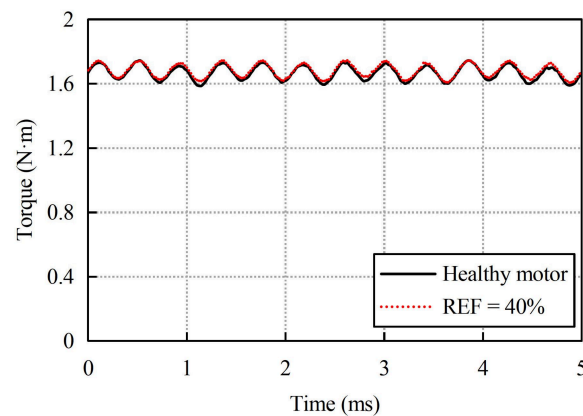


Figure 35. Electromagnetic torque at REF = 40%.

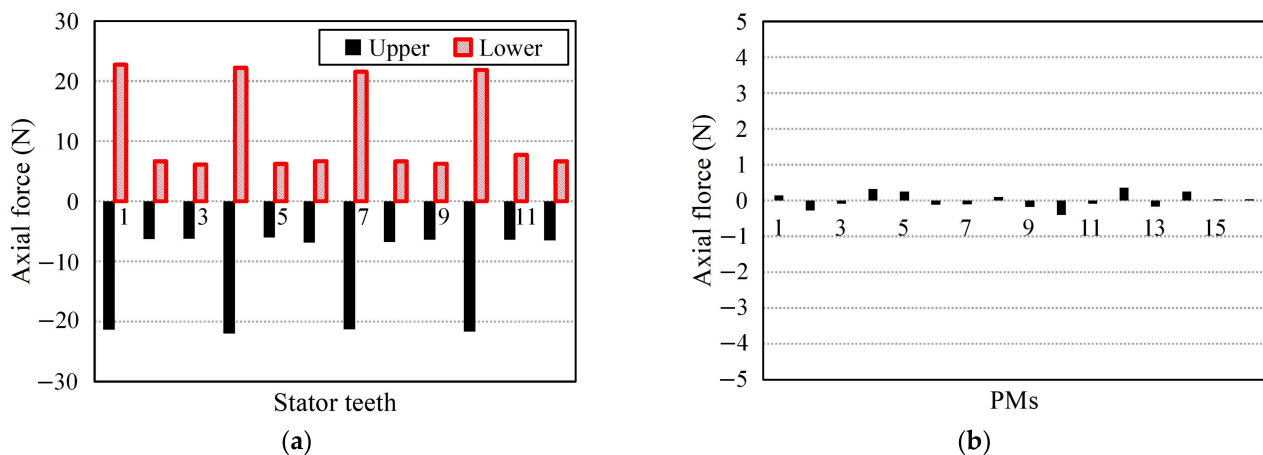


Figure 36. Axial UMF at REF = 40%. (a) Stator teeth; (b) PMs.

4. Experiments Under Eccentricity Conditions

To validate the 3-D FEM model under eccentricity conditions, experiments were conducted on the prototype with artificially introduced static and axial eccentricities. The no-load back-EMF and average electromagnetic torque are selected as the validation metrics. The back-EMF is directly determined by the air-gap flux linkage, and its harmonic content is highly sensitive to the distortion of the air-gap magnetic field caused by the non-uniform air-gap length under eccentricity. The average electromagnetic torque reflects the overall electromechanical energy conversion capability of the DSSR motor, which is also affected by the change in the air-gap field distribution. Therefore, these two quantities together provide a comprehensive validation of the 3-D FEM model from both the magnetic field perspective and the energy conversion perspective.

Experiments under static eccentricity are first performed. The experimental platform has been described in detail in Section 2.3. It allows the stators on both sides of the DSSR motor to be fixed on supporting brackets, each equipped with 12 adjustable bolts. By differentially tightening these bolts, the translational offset of each stator can be freely adjusted, with a maximum displacement of 0.4 mm per side, corresponding to a maximum SEF of 40%. The positions of the bolts are shown in Figure 37. The simulation and experimental results are compared at four SEF levels. When the SEF is varied from 10% to 40%, the fundamental amplitudes of the no-load back-EMF are compared in Figure 38a, and the average electromagnetic torque is compared in Figure 38b.

The healthy prototype has a nominal air-gap length of 1 mm on both sides. To facilitate precise control of the axial eccentricity, the bearing contact surfaces are machined to reduce the air-gap length to 0.6 mm, and shims of different thicknesses are inserted at the bearings

on both sides to adjust the air-gap length, as shown in Figure 39. Four shim thicknesses of 0.1 mm, 0.2 mm, 0.3 mm, and 0.4 mm are used. When the AEF is varied from 10% to 40%, the fundamental amplitudes of the no-load back-EMF are compared in Figure 40a, and the average electromagnetic torque is compared in Figure 40b. The good agreement between the simulation and experimental results for the back-EMF and torque under both static and axial eccentricity conditions confirms the accuracy of the 3-D FEM model, which provides confidence in the simulation-based analysis of other electromagnetic quantities under eccentricity conditions.

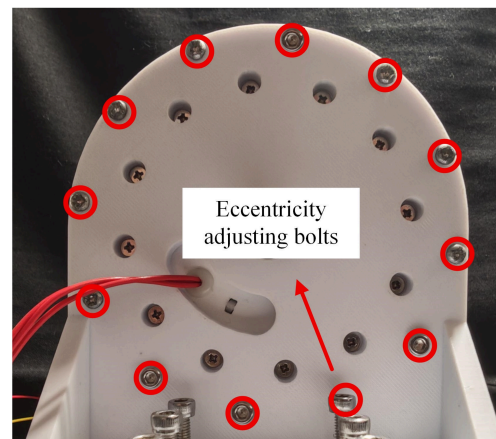


Figure 37. Experimental setup with 12 adjusting bolts for static eccentricity control.

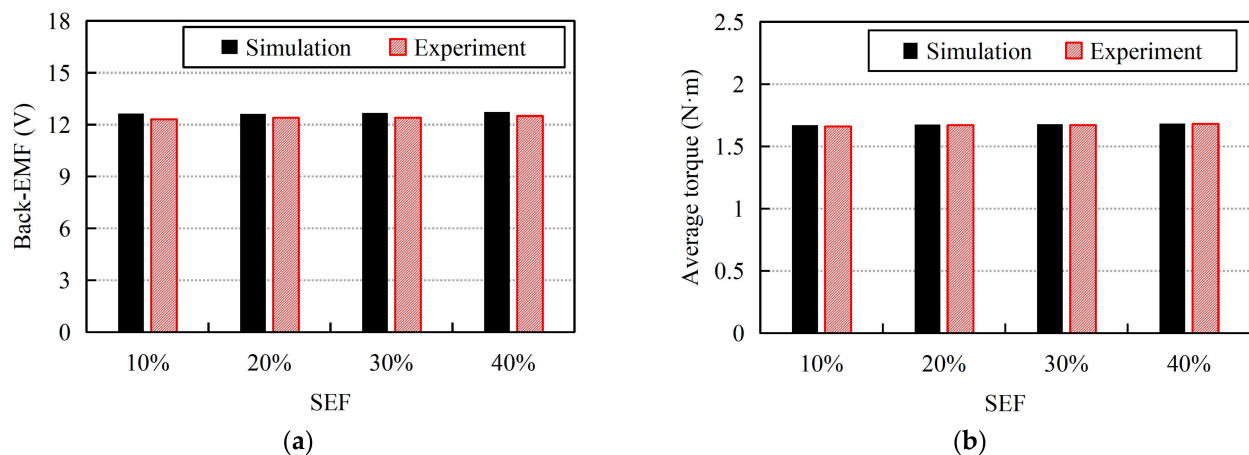


Figure 38. Comparison between experimental and simulated results under different SEFs. (a) No-load back-EMF; (b) average electromagnetic torque.

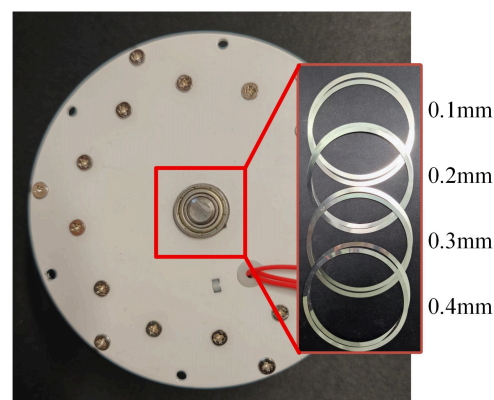


Figure 39. Bearing shims of different thicknesses used to control the axial eccentricity.

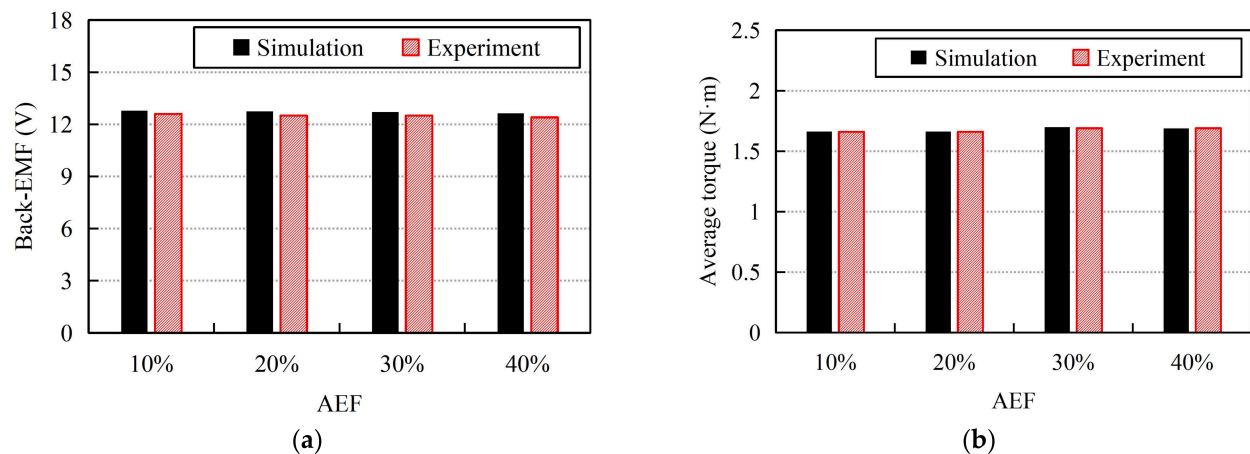


Figure 40. Comparison between experimental and simulated results under different AEFs. (a) No-load back-EMF; (b) average electromagnetic torque.

5. Conclusions

A 3-D transient FE model of the DSSR motor accounting for eccentricity conditions is developed to investigate the electromagnetic characteristics under static, dynamic, axial, and radial eccentricity. The accuracy of the simulation model is validated by experimental measurements of the healthy prototype, and the eccentricity-induced variations in back-EMF and torque are further verified through dedicated experiments on eccentric prototypes. This comprehensive comparative analysis reveals the unique fault signatures of the DSSR topology. Although static and dynamic eccentricity have negligible effects on the no-load back-EMF and average electromagnetic torque, they induce significant additional bending moments: static eccentricity generates a constant bending moment proportional to the SEF, whereas dynamic eccentricity produces an alternating bending moment at the rotor rotational frequency, with its amplitude increasing linearly with the DEF. Both types exacerbate bearing loads and thereby threaten structural reliability. Axial eccentricity directly compromises the magnetic circuit symmetry of the dual-air-gap DSSR topology, leading to an imbalance in no-load back-EMF amplitudes between the two stator sides; when the AEF is 40%, the amplitude deviation reaches 17.6% and the net axial UMF surges to 53.2 N, severely undermining the inherent advantage of the DSSR configuration in canceling axial forces. In contrast, radial eccentricity exhibits negligible impact on the electromagnetic performance of the DSSR motor, confirming its natural robustness against radial assembly errors. Crucially, this study establishes the electromagnetic baseline for fault diagnosis. The quantified excitation sources, specifically the UMF and bending moments, serve as the prerequisite data for interpreting system responses. While the specific vibration coupling and frequency-domain signatures depend on the structural dynamics, the distinct electromagnetic fault signatures identified here provide the essential input for future multi-physics modeling and vibration-based condition monitoring.

It should be noted that this study has certain limitations. First, the UMF and bending moment results are obtained from FE simulations based on the Maxwell stress tensor method. Although the electromagnetic model has been validated by no-load back-EMF and torque measurements under both healthy and eccentric conditions, direct experimental validation of the force and bending moment results has not yet been performed. Second, the present study focuses on the electromagnetic characteristics under eccentricity conditions, while the coupled mechanical and thermal effects are not considered. Third, each eccentricity type is analyzed independently in this study, whereas practical eccentricity generally occurs in mixed form. The interactions among different eccentricity types are expected to be partially coupled rather than purely additive, since the air-gap permeance function under

combined eccentricity introduces cross-coupling terms that do not exist under isolated fault conditions. Quantitative assessment of such combined effects requires dedicated simulations, which are beyond the scope of the present study. These limitations will be addressed in future work through dedicated force measurement experiments, combined eccentricity simulations, and multi-physics coupled analysis.

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Conflicts of Interest: Author Likang Fan was employed by the company Fulin Precision Machining Co, Ltd. The remaining authors declare that the research was conducted in the absence of any commercial or financial relationships that could be construed as a potential conflict of interest.

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