

Analytical Prediction in a Double-Rotor Single-Stator Switch Reluctance Machine with Core-less Stator Taking into Consideration Saturation for Bearingless Applications

E. Shirzad

`dr.ehsan.shirzad@tooranihe.ac.ir`

Tooran Institute of Higher Education

Research Article

Keywords: double-rotor synchronous reluctance machine, core-less stator, analytical subdomain model, magnetic saturation, saturation compensation, finite element analysis, electromagnetic torque, radial pressure

Posted Date: September 1st, 2026

DOI: <https://doi.org/10.21203/rs.3.rs-10725755/v1>

License:   This work is licensed under a Creative Commons Attribution 4.0 International License.
[Read Full License](#)

Additional Declarations: No competing interests reported.

Analytical Prediction in a Double-Rotor Single-Stator Switch Reluctance Machine with Core-less Stator Taking into Consideration Saturation for Bearingless Applications

E. Shirzad

Faculty of Electrical Engineering, Tooran Institute of Higher Education, Damghan, Iran

*Correspondence: dr.ehsan.shirzad@tooranihe.ac.ir

Abstract— This paper presents an exact analytical subdomain model for a double-rotor single-stator switch reluctance machine (DRSSRM) with a core-less stator, applicable for any arbitrary number of rotor and stator teeth. DRSSRM facilitates winding due to high space for that and reduces core loss of back iron of stator led to high efficiency. Reluctance machines are well-established in industrial applications due to their high mechanical reliability, simple structure, and low manufacturing cost, especially when compared with integrated PM-based topologies. The proposed analytical model is initially developed under the assumption of infinite iron permeability. To enhance the accuracy and bridge the gap with finite element method (FEM) results, a saturation compensation technique is then introduced, which accounts for local saturation effects in the stator teeth, rotor teeth, and rotor back-iron. The radial and tangential flux density distributions are extracted for both single- and double-layer winding configurations in the inner and outer air gaps, and the results are systematically compared with FEM simulations. The primary focus of this work is the accurate calculation of radial pressure (radial traction) exerted on the rotor surfaces, which is derived from the Maxwell stress tensor using the corrected flux densities. The proposed model provides a computationally efficient and accurate tool for radial pressure prediction, which is essential for vibration analysis in applications of bearingless, structural design, and mechanical reliability assessment of DRSSRM in high-speed applications.

Index Terms— double-rotor synchronous reluctance machine, core-less stator, analytical subdomain model, magnetic saturation, saturation compensation, finite element analysis, electromagnetic torque, radial pressure.

NOMENCLATURE

A	Magnetic vector potential (V.s/m)
B	Magnetic flux density vector (T)
H	Magnetic field intensity vector (A/m)
J	Armature current density vector (A/m ²)
M	Magnetization vector (A/m).
μ_0	Free space permeability (H/m).
μ_r	Relative permeability
N_{ss}	Number of stator slots
N_{irs}	Number of inner rotor slots
N_{ors}	Number of outer rotor slots
α_i	Central angle of i th slot of inner rotor
β_i	Central angle of i th slot of inner stator
σ_i	Central angle of i th slot of outer rotor

Superscripts and subscripts

r	Radial direction
-----	------------------

θ	Tangential direction
z	Axial direction
iry	Inner rotor yoke
irs	Inner rotor slot
ia	Inner airgap
ss	stator slot
i	The number of tooth
oa	Outer airgap
ors	Outer rotor slot
ory	Outer rotor yoke
B_r	Radial flux density
B_t	Tangential flux density

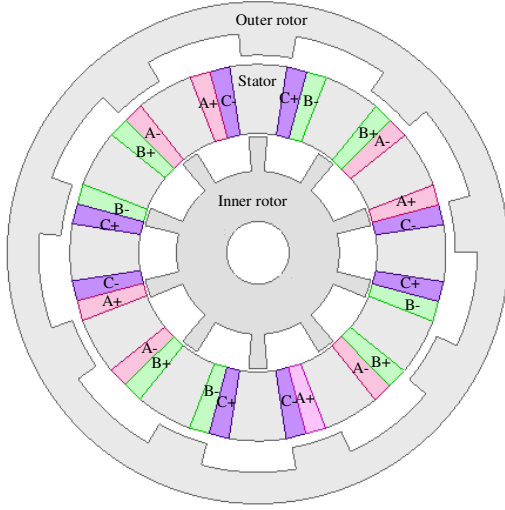
I. INTRODUCTION

Switch reluctance motors (SRMs) offer great benefits for high-speed applications, like electric compressors, compared to other motor types moreover, SRMs run at very high speeds because of not having brushes or sliding contacts that create friction or wear. The motors are also reliable in harsh, high-temperature environments and are strong competitors to permanent-magnet synchronous motors in electric vehicles. Their main advantages are simple construction, lower cost, low maintenance needs, and excellent performance at high speeds [1], [2], [3]. This machine with two electrical and mechanical ports consists of two separate rotors: an inner rotor and an outer rotor. This unique dual-rotor structure allows it to transfer power through two independent mechanical ports, making it highly suitable for applications that require energy management from multiple sources. For example, in wind turbines, the machine can receive mechanical energy from both rotors simultaneously, which increases overall efficiency and power capture. In hybrid electric vehicles, it offers a significant advantage: in generator mode, the machine can produce electrical energy from two separate sets of windings, effectively storing energy twice as much as a conventional single-rotor machine. In addition, this dual-port capability provides greater flexibility in controlling power flow, enabling the system to operate in various modes, such as charging the battery while driving, or recovering energy during braking, all with higher efficiency and reliability [4]. To extract electromagnetic parameters of electrical machines, two main approaches are commonly used: numerical and analytical methods. The Finite Element Method (FEM) is the most widely used numerical technique. It divides the machine geometry into small elements (meshes) and solves Maxwell's equations in each element. FEM provides highly accurate results, especially when considering non-linear materials and

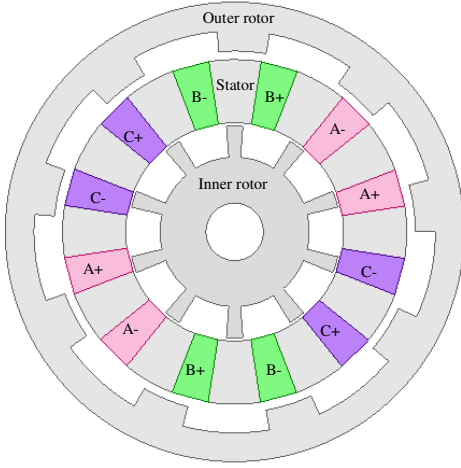
complex geometries. However, its accuracy is highly dependent on mesh size, and the computation time is significantly high, particularly for new geometries that require complete re-meshing. To overcome these drawbacks, analytical methods are often preferred, especially in the early design stages. These methods are based on mathematical formulations derived from the machine's physical structure, offering fast computation and acceptable accuracy without the need for meshing. Common analytical techniques include the Magnetic Equivalent Circuit (MEC), sub-domain method, virtual current method, and others. In summary, FEM is accurate but computationally intensive, while analytical methods provide a practical balance between speed and accuracy, making them ideal for initial design and optimization. Extensive research has been conducted on the family of reluctance motors. In [5], a multi-physics design and analysis of three double-rotor synchronous reluctance machines is presented, utilizing genetic algorithm and finite element method (FEM). A comparative analysis of double-rotor synchronous reluctance machines using FEM is provided in [6], where various topologies are investigated, including double-rotor with double-sided stator and double-rotor with single-sided stator configurations. In the area of analytical modeling, reference [7] introduces a new hybrid approach for synchronous reluctance motors, combining the improved finite difference method (IFDM), subdomain method, and equivalent magnetic network (EMN). An enhanced hybrid subdomain method (EHSDM) for concurrent prediction of magnetic and thermal fields in synchronous reluctance motors is presented in [8]. Similarly, [9] proposes an upgraded hybrid subdomain method (UHSDM) to improve the efficiency and accuracy of thermal performance prediction for these machines. To address torque ripple reduction, a double-stator synchronous reluctance motor (DS-SynRM) is proposed in [10], using a magnetic equivalent circuit (MEC) approach. Furthermore, an improved analytical calculation method for determining the nonlinear magnetic fields of synchronous reluctance motors is introduced in [11]. The objective of [12] is to investigate a novel dual-rotor hybrid synchronous machine featuring two different rotor types namely, synchronous reluctance (SyR) and surface permanent magnet (SPM) rotors using FEM analysis. In the context of electric vehicle applications, [13] presents a bridgeless rotor (BLR) design for a synchronous reluctance machine (SynRM) as a magnet-free alternative, with electromagnetic design carried out on an optimized structure. The pros and cons of double-sided stator configurations compared to single-sided stator structures are discussed in [14], along with a design process for selecting the optimal stator/rotor combination, flux barrier number, and other design parameters. For switched reluctance motors (SRMs), a new switched lumped reluctance network-based equivalent circuit for modeling SRMs under saturation is proposed in [15]. The main idea in [16] is to introduce a unique continuous C-core configuration for SRMs and to present an analytical model (MEC) for its analysis. In [17], core loss analysis of a planar switched reluctance motor (PSRM) is carried out using 3-D time-stepping finite element methods. A comprehensive and efficient electromagnetic transient model of an SRM drive system based on a circuit-

based representation is proposed in [18]. In [19], the use of neural networks for predicting magnetization surfaces in SRMs a critical aspect of design and operational efficiency is investigated. The higher-phase switched reluctance motor (HPSRM) is identified as a promising candidate for industrial applications due to its increased phase conduction overlap. A geometry parameter-dependent hybrid model for HPSRM, accounting for mutual coupling and core saturation, along with a design approach, is presented in [20]. Finally, a time-efficient framework for analyzing electromagnetic fields and NVH (noise, vibration, and harshness) toward the co-design of SRMs is introduced in [21]. This framework employs a magnetic equivalent circuit (MEC)-based approach that significantly improves accuracy over conventional MEC for arbitrary geometries.

Despite extensive research on reluctance machines [5]–[21], no analytical model has yet been developed specifically for double-rotor single-stator switched reluctance machines (DRSSRM) with a core-less stator for single and double layer winding (Fig. 1) that accounts for magnetic saturation. While FEM provides high accuracy [5], [6], [12], [17], its computational cost makes it impractical for design optimization. Existing analytical methods [7]–[11], [13]–[16], [18]–[21] are either tailored for synchronous reluctance motors or fail to capture the unique dual-rotor, core-less stator topology and saturation effects. To bridge this gap, this paper presents, for the first time, an exact analytical subdomain model for DRSSRM with a core-less stator, applicable for any arbitrary number of rotor and stator teeth. The model is enhanced with a novel saturation compensation technique that accurately accounts for local saturation in the stator teeth, rotor teeth, and rotor back-iron. The proposed model is rigorously validated through extensive comparison with FEM simulations, demonstrating excellent agreement in predicting the radial pressure distribution on both inner and outer rotor surfaces under various saturation conditions. The proposed model thus offers a powerful, computationally efficient, and accurate tool specifically for radial pressure analysis in single layer and 2-layer winding, which is essential for vibration prediction, structural design, and mechanical reliability assessment of DRSSRM, filling a longstanding void in the analytical modeling of this emerging machine topology.



(a)



(b)

Fig. 1. Configuration of proposed DRSSRM. a) Double-layer winding b) Single-layer winding

II. ANALYTICAL SUBDOMAIN MODEL CONSIDERING SATURATION EFFECT

The proposed analytical model is developed to predict the magnetic field distribution and electromagnetic performance of the DRSSRM with a core-less stator. The relation (1) is fundamental relation based Maxwell equation clarifying connection between sources and magnetic potential vector. It is noted that in the PM sub-regions $J_z=0$, in the winding sub-regions $M_r=M_\theta=0$. For other sub-regions both J and M are zero. The model is based on the subdomain method, which divides the entire machine geometry into distinct regions so that for each sub-region, Maxwell equation based on (1) is formulated. For applying boundary conditions to calculated unknown coefficient and extraction of electromagnetic quantities, radial and tangential component of flux density are mandatory obtained from (2). Relation (3) is magnetic field

intensity which is dependent on magnetization of PMs that $M_r=M_\theta=0$ in whole region except PMs moreover, due to analysis on reluctance motor, $M=0$ in regions entirely.

The machine is analyzed in polar coordinates, with the assumption that the magnetic vector potential has only a z-component, and the flux density is purely radial and tangential. End effects are neglected, and all materials are considered isotropic. The iron parts specifically the rotor teeth and back-iron are initially assumed to have infinite permeability, which simplifies the derivation of the analytical solution then, this assumption is later refined saturation compensation technique to account for the nonlinear behavior of the iron under high-load conditions. Before beginning modeling process, geometric parameters of DRSSRM are illustrated in Fig. 3 why formulation is based on the parameters.

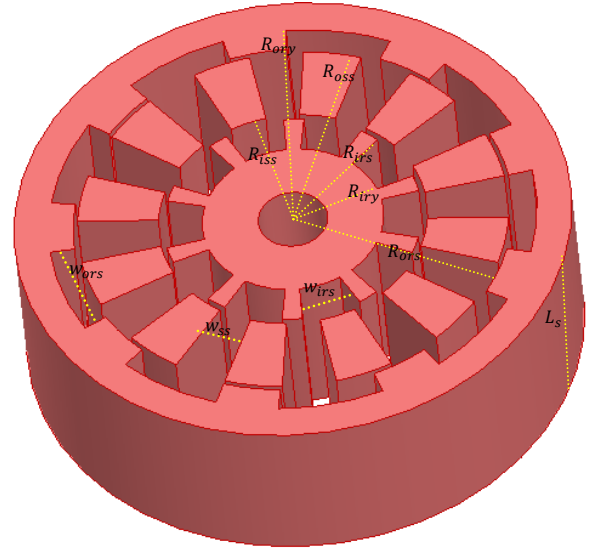


Fig. 3. Geometric parameters of DRSSRM

$$-\nabla^2 A_z^i = \mu_0 \mu_r J_z^i + \frac{\mu_0}{r} \left(\frac{\partial M_r}{\partial \theta} - r \frac{\partial M_\theta}{\partial r} \right) \quad (1)$$

$$B = \nabla \times A \quad (2)$$

$$H = \frac{B}{\mu_0 \mu_r} - \frac{M}{\mu_r} \quad (3)$$

A. SUBDOMAINS

Before considering saturation effect, relation (1) for each domain is solved to calculate A . The configuration is composed of five domains named inner rotor slots, inner airgap, stator slots, outer airgap and outer rotor slots. Inner and outer rotor slots are surrounded by iron from three-direction showing H is zero that by applying relation (4) and (5) is applicable for domains with these conditions.

$$A_z^i(r, \theta) = a_{i,0} + \sum_{n=1}^{\infty} \left(a_{i,n} f(n, r) \cos\left(\frac{n\pi}{c}(\theta - \theta_i + \frac{c}{2})\right) \right) \quad (4)$$

$$f(n, r) = r^{-\frac{n\pi}{c}} + (R^{-\frac{n\pi}{c}} r^{\frac{n\pi}{c}} / R^{\frac{n\pi}{c}}) \quad (5)$$

Where i is a symbol for each slot, c is width of each region and θ_i is central angles of permanent magnet and winding region. Where R is the radius of slot stator and rotor region moreover, $f(n, r)$ is obtained from $\frac{\partial A_z^i}{\partial r} = H = 0$ due to drop magnetic field intensity in iron is zero (infinite permeability). Domains of airgap are open circumferentially so, Fourier series is inclusion of both sine and cosine functions without limitation of arc in according to (6).

$$A_z^i(r, \theta) = \sum_{n=1}^{\infty} ((a_{1n}(r)^n + a_{2n}(r)^{-n}) \cos(n\theta) + (b_{1n}(r)^n + b(r)^{-n}) \sin(n\theta)) \quad (6)$$

For winding domains, energy source must be remarked and the equation of (1) is not homogenous. To solve, particular and general answers are added that $-\frac{1}{4}\mu_0 J_i r^2$ is particular answer also, winding domains are surrounded from two-direction because of common source for inner and outer part.

$$\begin{aligned} A_z^i(r, \theta) &= a_{i,0} + b_{i,0} \ln(r) - \frac{1}{4}\mu_0 J_i r^2 \\ &+ \sum_{n=1}^{\infty} \left((a_{i,n} r^{-\frac{n\pi}{c}} + b_{i,n} r^{\frac{n\pi}{c}}) \cos\left(\frac{n\pi}{c}(\theta - \theta_i + \frac{c}{2})\right) \right) \end{aligned} \quad (7)$$

$$J_i(\theta) = J_{i,0} + \sum_{n=1}^{\infty} \left(J_{i,n} \cos\left(\frac{n\pi}{c}(\theta - \theta_i + \frac{c}{2})\right) \right) \quad (8)$$

Where $J_{i,0} = (J_{i,1}d + J_{i,2}d)/c$, $J_{i,n} = \frac{2}{n\pi} \sin\left(\frac{n\pi d}{c}\right) (J_{i,1} + (J_{i,2})(-1)^n)$

$J_{i,1}$, $J_{i,2}$ are right and left current density and d is width of winding region respectively that in single layer winding $d=c$ and $J_{i,2} = 0$.

B. BOUNDARY CONDITIONS

Based on Fig. 3, to obtain unknown coefficients, equality A_z between each slot and airgap region in span of slot angle and

H_θ around 0-360 mechanical degree are applicable so that from (9) and (12) are between inner rotor slot and inner airgap, from (13) to (16) are between inner air gap and stator slots, from (17) to (20) are between stator slots and outer air gap, eventually from (21) to (24) are between outer airgap and outer rotor slots.

$$a_{irs,0} = \frac{1}{w_{irs}} \int_{\alpha_i - w_{irs}/2}^{\alpha_i + w_{irs}/2} A_z^{ia}(R_{irs}, \theta) d\theta \quad (9)$$

$$\begin{aligned} a_{irs,n} f(n, R_{irs}) &= \frac{2}{w_{irs}} \int_{\alpha_i - w_{irs}/2}^{\alpha_i + w_{irs}/2} A_z^{ia}(R_{irs}, \theta) \cos\left(\frac{n\pi}{w_{irs}}\left(\frac{\theta - \alpha_i}{2} + \frac{w_{irs}}{2}\right)\right) d\theta \end{aligned} \quad (10)$$

$$\begin{aligned} m(-b_{ia,1,m} R_{irs}^{m-1} + b_{ia,1,m} R_{irs}^{-m-1}) &= \sum_{i=1}^{N_{irs}} \frac{\mu_0}{\pi} \int_{\alpha_i - w_{irs}/2}^{\alpha_i + w_{irs}/2} H_\theta^{irs,i}(R_{irs}, \theta) \sin(m\theta) d\theta \end{aligned} \quad (11)$$

$$\begin{aligned} m(-a_{ia,1,m} R_{irs}^{m-1} + a_{ia,1,m} R_{irs}^{-m-1}) &= \sum_{i=1}^{N_{irs}} \frac{\mu_0}{\pi} \int_{\alpha_i - w_{irs}/2}^{\alpha_i + w_{irs}/2} H_\theta^{irs,i}(R_{irs}, \theta) \cos(m\theta) d\theta \end{aligned} \quad (12)$$

$$(a_{ss,0} + b_{ss,0} \ln(R_{iss}) - \frac{1}{4}\mu_0 J_{ss,0} R_{iss}^2) \quad (13)$$

$$\begin{aligned} &= \frac{1}{w_{ss}} \int_{\beta_i - \frac{w_{ss}}{2}}^{\beta_i + \frac{w_{ss}}{2}} A_z^{ia}(R_{iss}, \theta) d\theta \\ &\frac{2}{n\pi} \sin\left(\frac{n\pi d}{w_{ss}}\right) \left(-\frac{1}{4}\mu_0 R_{iss}^2\right) J_{ss,1} \end{aligned} \quad (14)$$

$$\begin{aligned} &+ J_{ss,2}(-1)^n (a_{ss,n} R_{iss}^{\frac{-n\pi}{w_{ss}}} + b_{ss,n} R_{iss}^{\frac{n\pi}{w_{ss}}}) = \\ &\frac{2}{w_{ss}} \int_{\beta_i - w_{ss}/2}^{\beta_i + w_{ss}/2} A_z^{ia}(R_{iss}, \theta) \cos\left(\frac{n\pi}{w_{ss}}\left(\frac{\theta - \beta_i}{2} + \frac{w_{ss}}{2}\right)\right) d\theta \end{aligned}$$

$$\begin{aligned} m(-b_{ia,1,m} R_{iss}^{m-1} + b_{ia,1,m} R_{iss}^{-m-1}) &= \sum_{i=1}^{N_{ss}} \frac{\mu_0}{\pi} \int_{\beta_i - w_{ss}/2}^{\beta_i + w_{ss}/2} H_\theta^{ss,i}(R_{iss}, \theta) \sin(m\theta) d\theta \end{aligned} \quad (15)$$

$$\begin{aligned} m(-a_{ia,1,m} R_{iss}^{m-1} + a_{ia,1,m} R_{iss}^{-m-1}) &= \sum_{i=1}^{N_{ss}} \frac{\mu_0}{\pi} \int_{\beta_i - w_{ss}/2}^{\beta_i + w_{ss}/2} H_\theta^{ss,i}(R_{iss}, \theta) \cos(m\theta) d\theta \end{aligned} \quad (16)$$

$$\begin{aligned} (a_{oss,0} + b_{oss,0} \ln(R_{oss}) - \frac{1}{4}\mu_0 J_{oss,0} R_{oss}^2) &= \frac{1}{w_{ss}} \int_{\beta_i - w_{oss}/2}^{\beta_i + w_{oss}/2} A_z^{oa}(R_{oss}, \theta) d\theta \end{aligned} \quad (17)$$

$$\begin{aligned} &\frac{2}{n\pi} \sin\left(\frac{n\pi d}{w_{ss}}\right) \left(-\frac{1}{4}\mu_0 R_{oss}^2\right) J_{ss,1} \\ &+ J_{ss,2}(-1)^n (a_{ss,n} R_{oss}^{\frac{-n\pi}{w_{ss}}} + b_{ss,n} R_{oss}^{\frac{n\pi}{w_{ss}}}) = \\ &\frac{2}{w_{ss}} \int_{\beta_i - \frac{w_{ss}}{2}}^{\beta_i + \frac{w_{ss}}{2}} A_z^{oa}(R_{oss}, \theta) \cos\left(\frac{n\pi}{w_{ss}}\left(\frac{\theta - \psi_i}{2} + \frac{w_{ss}}{2}\right)\right) d\theta \end{aligned} \quad (18)$$

$$m(-b_{oa,1,m}R_{oss}^{m-1}+b_{oa,1,m}R_{oss}^{-m-1}) = \sum_{i=1}^{N_{ss}} \frac{\mu_0}{\pi} \int_{\beta_i-w_{ss}/2}^{\beta_i+w_{ss}/2} H_{\theta}^{ss,i}(R_{oss}, \theta) \sin(m\theta) d\theta \quad (19)$$

$$m(-a_{oa,1,m}R_{oss}^{m-1}+a_{oa,1,m}R_{oss}^{-m-1}) = \sum_{i=1}^{N_{ss}} \frac{\mu_0}{\pi} \int_{\beta_i-w_{ss}/2}^{\beta_i+w_{ss}/2} H_{\theta}^{ss,i}(R_{oss}, \theta) \cos(m\theta) d\theta \quad (20)$$

$$a_{ors,0} = \frac{1}{w_{irs}} \int_{\sigma_i-w_{ors}/2}^{\sigma_i+w_{ors}/2} A_z^{oa}(R_{ors}, \theta) d\theta \quad (21)$$

$$a_{ors,nf}(n, R_{ors}) = \frac{2}{w_{ors}} \int_{\sigma_i-w_{ors}/2}^{\sigma_i+w_{ors}/2} A_z^{oa}(R_{ors}, \theta) \cos\left(\frac{n\pi}{w_{ors}}\left(\theta - \frac{\sigma_i}{2}\right)\right) d\theta \quad (22)$$

$$m(-b_{ia,1,m}R_{ors}^{m-1}+b_{ia,1,m}R_{ors}^{-m-1}) = \sum_{i=1}^{N_{ors}} \frac{\mu_0}{\pi} \int_{\sigma_i-w_{ors}/2}^{\sigma_i+w_{ors}/2} H_{\theta}^{ors,i}(R_{ors}, \theta) \sin(m\theta) d\theta \quad (23)$$

$$m(-a_{oa,1,m}R_{ors}^{m-1}+a_{oa,1,m}R_{ors}^{-m-1}) = \sum_{i=1}^{N_{ors}} \frac{\mu_0}{\pi} \int_{\sigma_i-w_{ors}/2}^{\sigma_i+w_{ors}/2} H_{\theta}^{ors,i}(R_{ors}, \theta) \cos(m\theta) d\theta \quad (24)$$

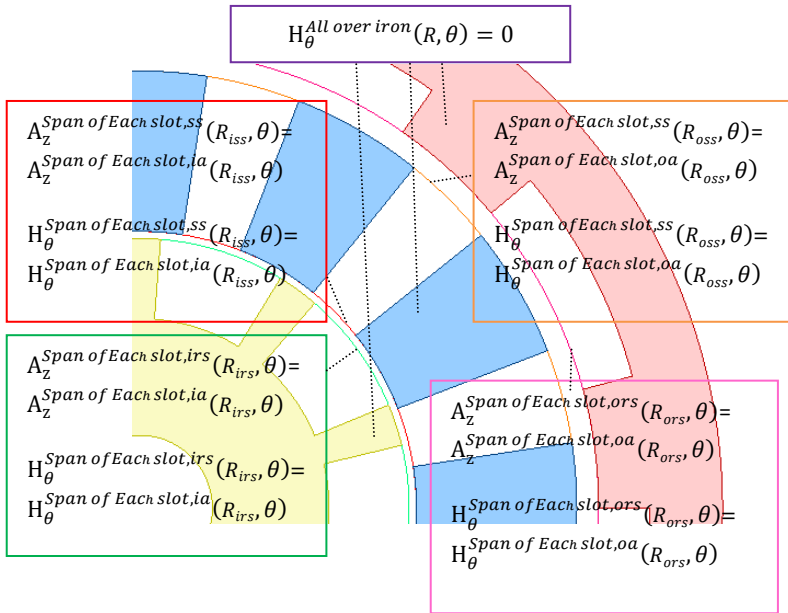


Fig. 4. Boundary conditions applied between regions

C. COMPENSATING TECHNIQUE

After solving the field distribution using the linear model with infinite permeability, the next step is to bring the results closer to reality. In practice, the iron parts especially the teeth and the back-iron do not behave like ideal magnetic conductors. When the flux density exceeds a certain level, the material saturates, and the linear assumption no longer holds. This is particularly important in switched reluctance machines, where the entire flux path passes through iron illustrated in Fig. 5. The

compensation process is carried out in a step-by-step manner, and it covers not only the teeth but also the rotor back-iron.

Step 1: Finding the Flux in Each Iron Part

The process is started by using the radial flux density obtained from the linear model. For each stator and rotor tooth, this flux density over the tooth surface is integrated to find the total flux passing through that tooth:

$$\Phi_{tooth} = R \cdot L_s \int_{\theta_{lower}}^{\theta_{upper}} B_{rlinear} d\theta \quad (25)$$

Where R, L, are respectively tooth radius, stack length and θ_{lower} and θ_{upper} for each stator and rotor tooth.

Once the tooth fluxes are known, the flux flowing through the rotor back-iron can be determined. Since the back-iron connects all the rotor teeth, the total flux in the yoke is simply the sum of the fluxes from the teeth attached to it:

$$\Phi_{yoke} = \sum_{i=1}^{N_{irs} \& N_{ors}} \Phi_{tooth, i \text{ th rotor slot}} \quad (26)$$

Step 2: Calculating Flux Density and H-Field

With the flux known, the average flux density in each tooth by dividing the flux by the corresponding cross-sectional area is calculated:

$$B_{tooth,i} = \frac{\Phi_{tooth,i}}{L_s w_{teeth} R} \quad (27)$$

Where, w_{teeth} is teeth width.

For the yoke, the flux density is obtained by dividing the yoke flux by its cross-sectional area:

$$B_{yoke} = \frac{\Phi_{yoke}}{L_s h_{yoke}} \quad (28)$$

Where, h_{yoke} is height of rotors yoke.

At this point, the B-H curve of the lamination steel (Fig. 6) is used to find the corresponding magnetic field intensity for each flux density value:

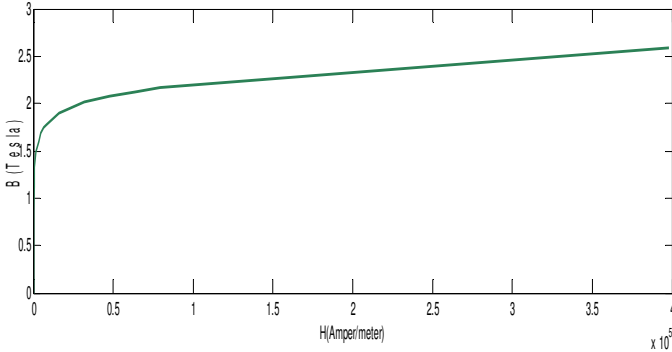


Fig. 6. Actual B-H characteristic curve for steel 1008

Step 3: Magnetic Voltage Drop and Saturation Factor

The magnetic voltage drop across each tooth is obtained by multiplying the H-field by the length of the flux path in that tooth:

$$V_{tooth,stator,i} = H_{tooth,stator,i} \cdot l_{stator,i} \quad (29)$$

$$V_{tooth,rotor,i} = H_{tooth,rotor,i} \cdot l_{rotor,i} \quad (30)$$

The same is done for the yoke, using its average path length:

$$V_{yoke} = H_{yoke} \cdot l_{yoke} \quad (31)$$

Then a saturation factor for each region is defined. This factor compares the total magnetic voltage drop in the iron path to the voltage drop across the air-gap alone:

$$K_{sat,stator,i} = 1 + \frac{V_{tooth,stator,i}}{H_{gap,i} \cdot g} \quad (32)$$

$$K_{sat,rotor,i} = 1 + \frac{V_{tooth,rotor,i}}{H_{gap,i} \cdot g} \quad (33)$$

$$K_{sat,yoke} = 1 + \frac{V_{yoke}}{H_{gap} \cdot g} \quad (34)$$

Where g length of airgap and $H_{gap,i}$ magnetic field intensity under each teeth. Unlike the teeth, where a separate saturation factor is assigned to each individual tooth, the rotor back-iron is treated as a single integrated magnetic path. Therefore, a single saturation factor is calculated for the entire yoke based on the total yoke flux. This same factor is then applied to all teeth during the final correction step. H_{gap} is yield from (33).

$$H_{gap} = \frac{\sum_{i=1}^{Nirs \& Nors} \Phi_{tooth,i} \cdot H_{gap,i}}{\sum_{i=1}^{Nirs \& Nors} \Phi_{tooth,i}} \quad (35)$$

A factor greater than one means that the iron is taking up a significant portion of the available magnetomotive force, which reduces the effective air-gap flux.

Step 4: Correcting the Air-Gap Flux Density

Finally, the linear air-gap flux density is corrected by dividing it by the saturation factors of the corresponding stator tooth, rotor tooth, and yoke section:

This correction is applied locally in term of mechanical degree(θ), so each part of the machine is adjusted based on its own level of saturation.

$$B_{r,sat}(\theta) = \frac{B_{r,linear}(\theta)}{K_{sat,stator}(\theta) K_{sat,rotor}(\theta) K_{sat,yoke}(\theta)} \quad (36)$$

5. Iterative Refinement

Because the flux density changes after correction, the saturation factors also change. Therefore, the entire process is repeated a few times until the results stabilize:

$$B_{r,sat,k+1}(\theta) = \frac{B_{r,linear}(\theta)}{K_{sat,stator,k}(\theta) K_{sat,rotor,k}(\theta) K_{sat,yoke,k}(\theta)} \quad (37)$$

The iteration continues until the relative change in flux density falls below a specified tolerance (ϵ):

$$|B_{r,sat,k+1}(\theta) - B_{r,sat,k}(\theta)| < \epsilon \quad (38)$$

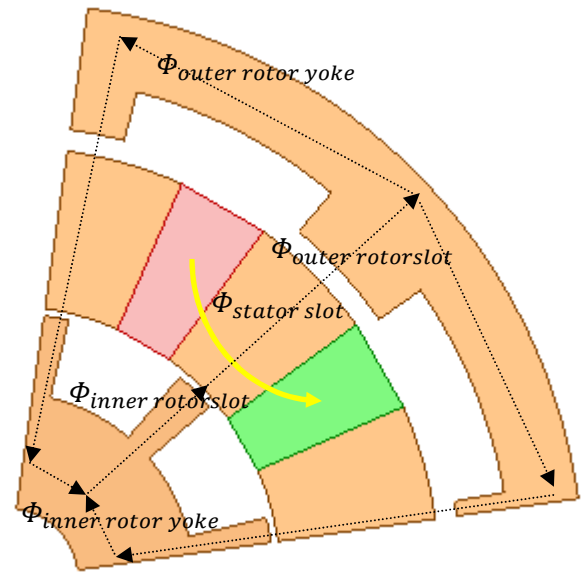


Fig. 7. Flux path into rotor and stator teeth and back iron of rotor

III. RESULTS

The proposed model is compared in three approaches: FEM, a linear analytical model, and a nonlinear are tested under saturation conditions. The model was evaluated for two winding setups, single-layer and double-layer, which are shown in Fig. 1. The geometric details used in the analytical model for the double-sided radial DRSSRM are listed in Table I.

For the double-layer winding, the normal and tangential flux density components for the inner part are presented in Figs. 9 and 10, and the outer part in Figs. 11 and 12. For the single-layer case, the inner part results are shown in Figs. 13 and 14, while the outer part ones are given in Figs. 15 and 16. All in all, the results confirm that the proposed model works well across both winding types and field components.

One thing worth pointing out is that all results whether for single or double layer were obtained at maximum current density of 6 A/mm² and with respect to high space of winding both scenarios meet saturation region which is so appropriate to test the proposed model. At the zero-crossing instant (Fig. 8), the total current sums to zero, actually what expect from a balanced three-phase supply.

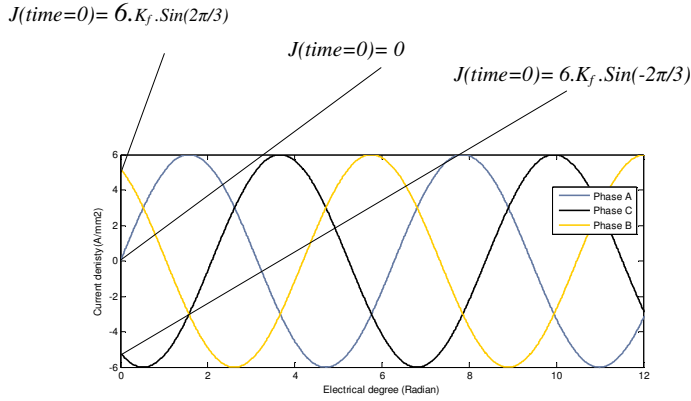


Fig. 8. Excitations of phases

TABLE I
SPECIFICATIONS OF THE DRSSRM

Parameters	Value
Number of inner phases, N_{iph}	3
Number of outer phases, N_{oph}	3
Number of inner rotor slots, N_{irs}	10
Number of outer rotor slots, N_{ors}	10
Number of stator slots, N_{ss}	12
Maximum current density of winding, J	6 A/mm ²
Inner rotor yoke radius, R_{iry}	26mm
Outer rotor yoke radius, R_{ory}	102mm
Inner radius of inner stator, R_{iss}	38mm
Outer radius of outer stator, R_{oss}	92.09mm
Inner radius of outer rotor, R_{ors}	93.5mm
Outer radius of inner rotor, R_{irs}	37mm
Width of inner rotor slot, w_{irs}	$26.99 \pi / 180$ rad
Width of outer rotor slot, w_{ors}	$27.01 \pi / 180$ rad
Width of i stator slot, w_{ss}	$17.5 \pi / 180$ rad
Stack length, L_s	100mm
Filling factor, K_f	0.7
Saturation flux density	1.7T
Tolerance, ϵ	0.01

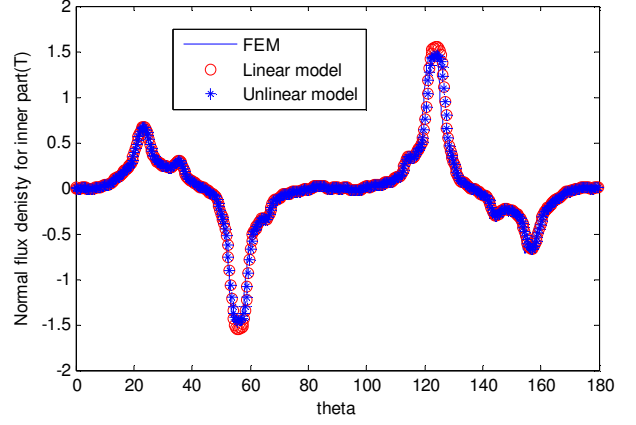


Fig.9. Normal flux density for inner part for 2-layer scenario

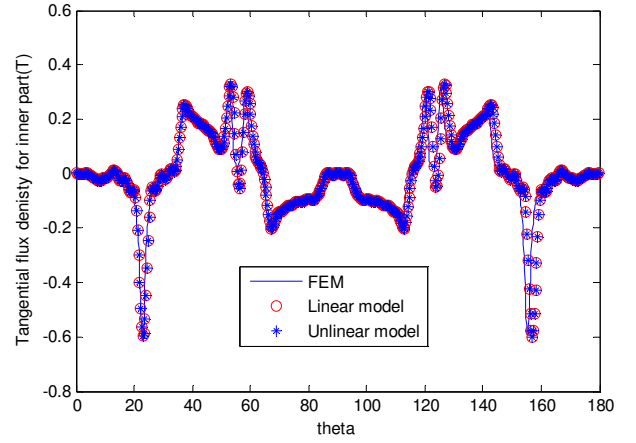


Fig.10. Tangential flux density for inner part for 2-layer scenario

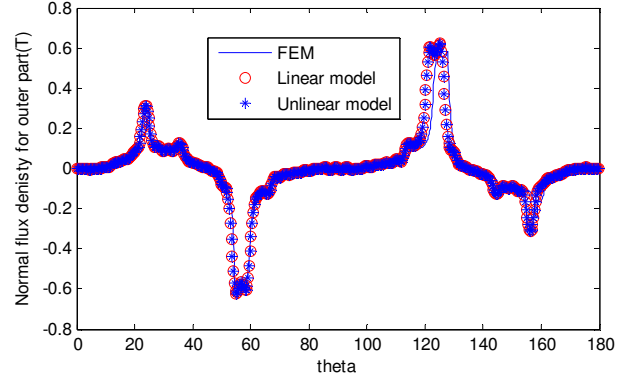


Fig.11. Normal flux density for outer part for 2-layer scenario

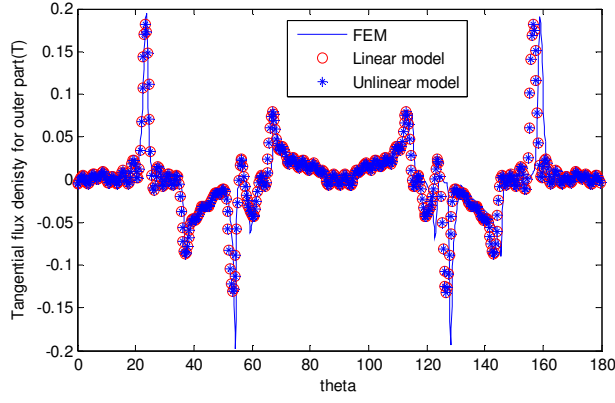


Fig.12. Tangential flux density for outer part for 2-layer scenario

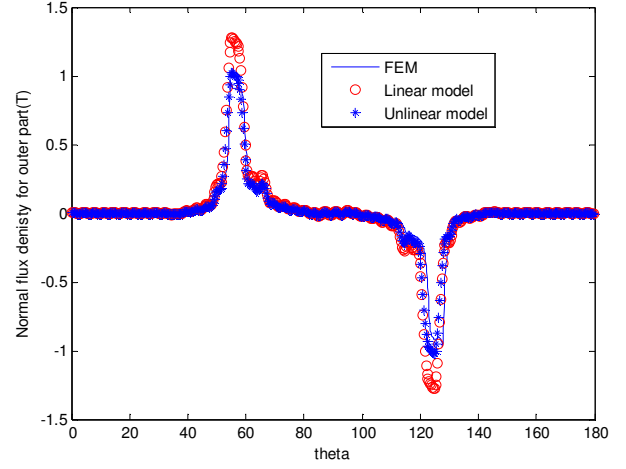


Fig.15. Normal flux density for outer part for single layer scenario

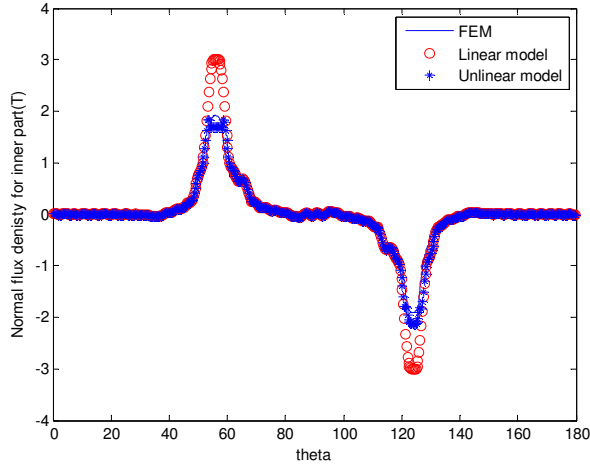


Fig.13. Normal flux density for inner part for single layer scenario

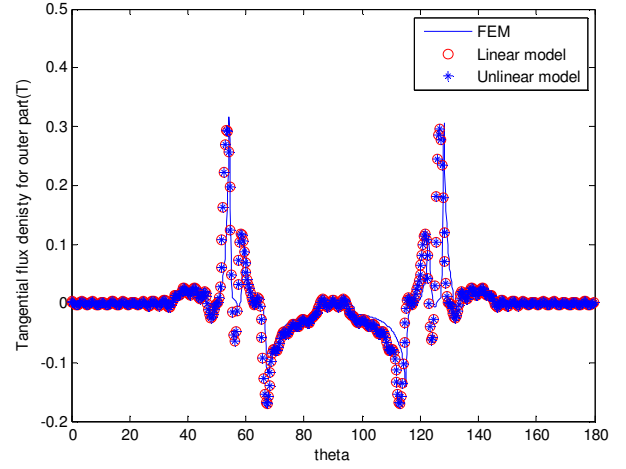


Fig.16. Tangential flux density for outer part for single layer scenario

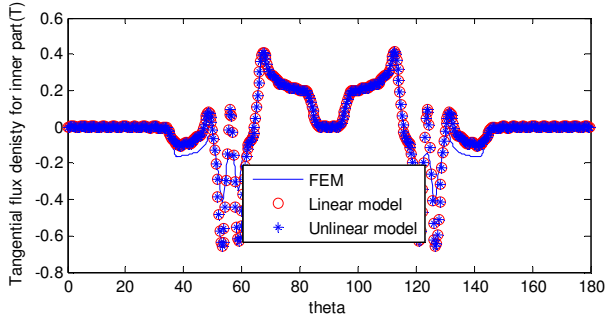


Fig.14. Tangential flux density for inner part for single layer scenario

The radial and tangential components of the local traction exerted on the rotor surface can be obtained by Maxwell stress tensor as follows:

$$f_r = \frac{(B_r^2 - B_\theta^2)}{2\mu_0} \quad (39)$$

$$f_\theta = \frac{B_r B_\theta}{\mu_0} \quad (40)$$

For applications of bearingless, f_r is appropriate to be small due to controllability of pressures in direction of radial by auxiliary winding recognized bearingless winding embedded in stator to produce pressure in radial direction in adjacent to main winding generating torque in order to rotation of rotors plus, f_θ is appropriate to be large with respect to rotation of rotor in tangential direction so, in two states of single layer and double-layer for rotor position of 0 and 45 mechanical degree as shown Fig. 17. The radial pressure results (Figures 18–21

for single-layer, and 26–29 for double-layer) demonstrate that the nonlinear model with saturation compensation matches FEM with excellent accuracy, while the linear model significantly overestimates pressure in saturated regions. This overestimation is critical for bearingless applications, because it would deceive the suspension controller into assuming more radial force is available than actually exists, risking instability or rotor-stator contact. The double-layer winding produces a more symmetric pressure distribution (Figures 26–29), which reduces unbalanced magnetic pull and simplifies active suspension control a clear advantage for bearingless designs.

The tangential pressure results (Figures 22–25 and 30–32) also show strong agreement between the nonlinear model and FEM. Although tangential pressure primarily governs torque, its accurate prediction supports bearingless operation because torque and suspension forces are often coupled in the same windings. The smoother profiles of the double-layer configuration further reduce vibrations that could destabilize magnetic suspension.

Overall, the iterative saturation compensation technique applying local corrections to each tooth and yoke based on the actual $B-H$ curve successfully bridges the gap between the simple linear model and costly FEM. By accurately predicting the radial pressure distribution that determines magnetic suspension forces, the model provides a fast, reliable tool for designing and optimizing bearingless DRSSRM systems, ensuring stable rotor levitation and mechanical integrity at high speeds.

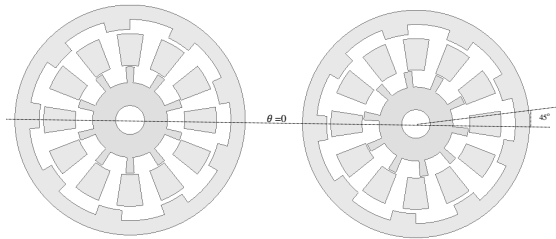


Fig.17. Rotor position at mechanical degree of $\theta = 0$ & $\theta = 45$

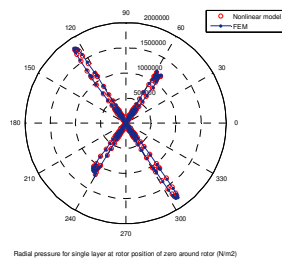


Fig.18. Radial pressure for single layer at $\theta = 0$ around rotor for inner part

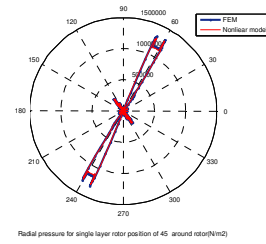


Fig.19. Radial pressure for single layer at $\theta = 45$ around rotor for inner part

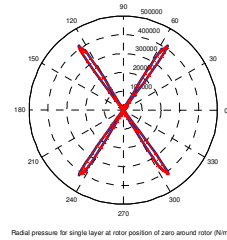


Fig.20. Radial pressure for single layer at $\theta = 0$ around rotor for outer part

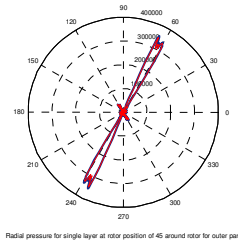


Fig.21. Radial pressure for single layer at $\theta = 45$ around rotor for outer part

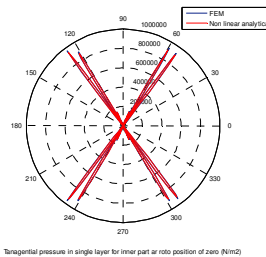


Fig.22 . Tangential pressure for single layer at $\theta = 0$ around rotor for inner part

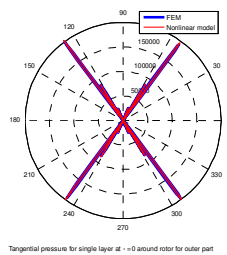


Fig.23. Tangential pressure for single layer at $\theta = 0$ around rotor for outer part

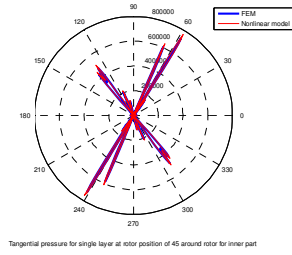


Fig. 24. Tangential pressure for single layer at $\theta = 45$ around rotor for inner part

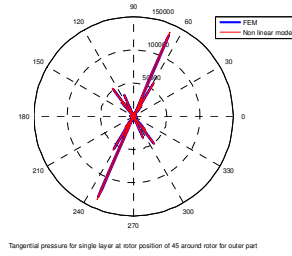


Fig. 25. Tangential pressure for single layer at $\theta = 45$ around rotor for outer part

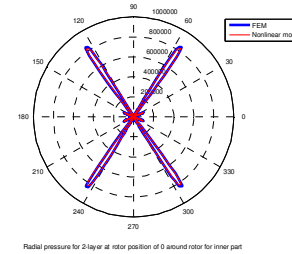


Fig.26. Radial pressure for double-layer at $\theta = 0$ around rotor for inner part

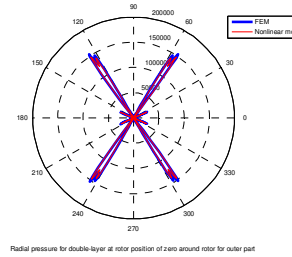


Fig.27. Radial pressure for double-layer at $\theta = 0$ around rotor for outer part

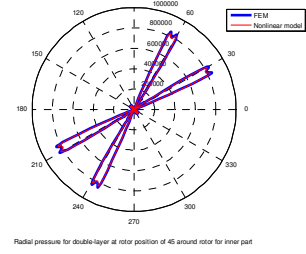


Fig.28. Radial pressure for double-layer at $\theta = 45$ around rotor for inner part

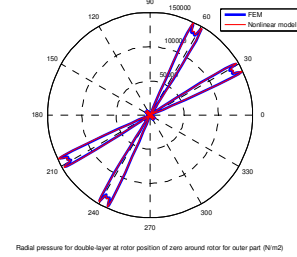


Fig.28. Radial pressure for double-layer at $\theta = 45$ around rotor for outer part

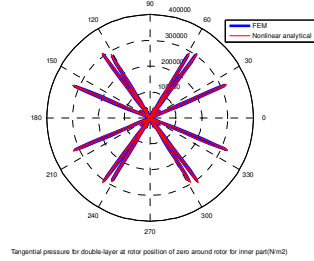


Fig. 29. Tangential pressure for double-layer at $\theta = 0$ around rotor for inner part

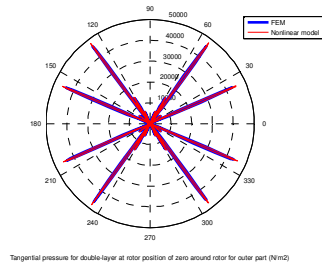


Fig. 30. Tangential pressure for double-layer at $\theta = 0$ around rotor for outer part

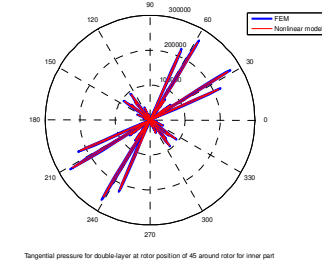


Fig. 31. Tangential pressure for double-layer at $\theta = 45$ around rotor for inner part

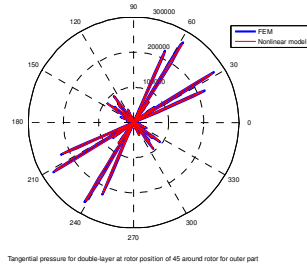


Fig. 32. Tangential pressure for double-layer at $\theta = 45$ around rotor for outer part

Author contributions

E.S. conceptualized the study, developed the mathematical framework based on the subdomain method, derived the analytical solutions for the double-rotor single-stator switched reluctance machine (DRSSRM) with core-less stator, and carried out the theoretical analysis. E.S. contributed to the analytical modeling, saturation compensation technique development, finite element analysis (FEA) simulations, validation of the results, and the analysis of radial pressure for bearingless applications. The author wrote and reviewed the manuscript. The author read and approved the final manuscript.

Funding

The authors received no specific funding for this work.

Data availability

The datasets generated and/or analysed during the current study are available from the corresponding author on reasonable request. All numerical results were obtained from analytical modeling and finite element simulations using the mathematical models, formulations, and parameter values reported in this article.

Declarations

Ethics approval and consent to participate

Not applicable. This study did not involve human participants, human data, human tissue, or animals requiring ethical approval and informed consent.

Consent for publication

The authors declare that they have no competing interests. Not Applicable.

Clinical Trial Registration

Not applicable. This research does not involve any clinical trial.

IV. CONCLUSION

This paper presents a nonlinear analytical model for predicting radial pressure in double-rotor single-stator switched reluctance machines (DRSSRM) with core-less stator, specifically for bearingless applications. The model, based on the subdomain method, incorporates an iterative saturation compensation technique that applies local corrections to stator teeth, rotor teeth, and yoke using the actual $B-H$ curve of the lamination steel.

Validation against FEM across single and double-layer winding configurations confirms that the linear model significantly overestimates radial pressure in saturated regions a dangerous flaw for bearingless systems where suspension controllers depend on accurate force predictions for stable rotor levitation. The nonlinear model achieves excellent agreement with FEM.

The double-layer winding configuration demonstrates superior performance with more symmetric pressure distribution, reduced unbalanced magnetic pull, and simpler active suspension control. The model also enables extraction of torque, suspension forces, inductances, iron losses, and harmonic analysis for vibration prediction.

By providing a computationally efficient, accurate, and saturation-aware tool, this work bridges the gap between simple analytical methods and costly FEM simulations, offering engineers a practical solution for designing and optimizing bearingless DRSSRM systems with stable rotor levitation and mechanical integrity at high speeds.

REFERENCES

- [1] C. Gong and T. Habetler, "A novel rotor design for ultra-high speed switched reluctance machines over 1 million rpm," in *2017 IEEE International Electric Machines and Drives Conference (IEMDC)*, 2017, pp. 1–6.
- [2] P. T. Hieu, D. Lee, and J. Ahn, "Design of a high speed 4/2 switched reluctance motor for blender application," in *2017 IEEE Transportation Electrification Conference and Expo, Asia-Pacific (ITEC Asia-Pacific)*, 2017, pp. 1–5.
- [3] M. Takeno, A. Chiba, N. Hoshi, S. Ogasawara, M. Takemoto, and A. Rahman, "Test Results and torque improvement of the 50-kW switched reluctance motor designed for hybrid electrical vehicles," *IEEE Trans. Ind. Appl.*, vol. 48, no. 4, pp. 1327–1334, Jul.-Aug. 2012.
- [4] E. Shirzad and A. Rahideh, "Analytical Model for Brushless Double Mechanical Port Flux-Switching Permanent Magnet Machines," *IEEE Trans. Magn.*, vol. 57, no. 10, pp. 1–13, Oct. 2021, doi: 10.1109/TMAG.2021.3097599.
- [5] M. Al-ani, "Multi-physics design and analyses of dual rotor synchronous reluctance machine," *eTransportation*, May, 2021, doi: 10.1016/j.aej.2021.02.023.
- [6] M. Al-Ani, H. Mahmoud, M. Degano, and C. Gerada, "Double rotor synchronous reluctance machine: Analysis towards torque capability," in *Proc. 2018 XIII Int. Conf. Electr. Mach. (ICEM)*, Sep. 2018, doi: 10.1109/ICELMACH.2018.8506898.
- [7] Y. Wang, J. Wang, C. Gan, Z. Song, and Y. Hu, "Electromagnetic and Stress Performance Analysis for Synchronous Reluctance Motor Using a New Hybrid

- Subdomain Method," *IEEE Trans. Ind. Electron.*, vol. 71, no. 8, pp. 8548-8559, Aug. 2024.
- [8] Y. Wang, J. Wang, C. Gan, Z. Song, and Y. Hu, "An Enhanced Hybrid Subdomain Method Toward Electromagnetic-Thermal Performance Analysis of Synchronous Reluctance Motor," *IEEE Trans. Transp. Electrific.*, vol. 11, no. 1, pp. 2851-2865, Feb. 2025.
 - [9] J. Liu, X. Wang, W. Zhao, Z. Xing, and H. Zhou, "An upgraded hybrid subdomain method toward three-dimensional thermal performance of synchronous reluctance motor," *International journal of thermal sciences*, Feb. 2026, doi: 10.1016/j.applthermaleng.2025.110284.
 - [10] A. Mahmoudi, E. Roshandel, S. Kahourzade, and W. L. Soong, "Introduction of Double-Stator Synchronous Reluctance Motor and Modeling," *IEEE Access*, vol. 11, pp. 76739-76750, Jul. 2023.
 - [11] H. Zhou, X. Wang, W. Zhao, Z. Xing, and J. Liu, "Research on Magnetic Field Prediction of Synchronous Reluctance Motor Considering Structure of Multilayer Flux Barrier and Saturation of Rotor Core," *IEEE Trans. Transp. Electrific.*, vol. 11, no. 2, pp. 5983-5998, Apr. 2025.
 - [12] L. Mo, X. Zhu, and L. Quan, "A novel dual-rotor hybrid machine with synchronous reluctance and surface permanent magnet rotors," in *Proc. 2017 IEEE Int. Elect. Mach. Drives Conf. (IEMDC)*, May 2017, doi: 10.1109/IEMDC.2017.8002335.
 - [13] J. Kolehmainen, J. Keränen, P. Ponomarev, and M. G. Tehrani, "Bridgeless rotor synchronous reluctance machine design evaluation," in *Proc. 2025 IEEE Int. Elect. Mach. Drives Conf. (IEMDC)*, doi: 10.1109/IEMDC60163.2025.11061005.
 - [14] M. Alani, M. Degano, N. Bianchi, H. Mahmoud, and C. Gerada, "Analysis and Design of Dual-Rotor Synchronous Reluctance Machine," *IEEE J. Emerg. Sel. Topics Power Electron.*, vol. 9, no. 4, pp. 4376-4383, Aug. 2021.
 - [15] D. Roy and M. Sengupta, "An Experimentally Validated Novel Analytical Approach for Modeling of a Saturated Switched Reluctance Motor," *IEEE Trans. Magn.*, vol. 58, no. 12, article number: 8207011, Dec. 2022.
 - [16] G. Davarpanah and J. Faiz, "Nonlinear Modeling of a C-Core Connected Two-Phase Switched Reluctance Motor," *IEEE Trans. Energy Convers.*, vol. 36, no. 4, pp. 2761-2769, Dec. 2021.
 - [17] J. F. Pan, F. J. Meng, and N. C. Cheung, "Core Loss Analysis for the Planar Switched Reluctance Motor," *IEEE Trans. Magn.*, vol. 50, no. 2, article number: 7020104, Feb. 2014.
 - [18] S. Mirnikjoo, N. Bracikowski, M. Naidjate, J. Mahseredjian, and P. Akiki, "Electromagnetic Transient Modeling of Switched Reluctance Motor Drive System by a Circuit-based Method," in *IECON 2025 – 51st Annual Conference of the IEEE Industrial Electronics Society*, Madrid, Spain, 2025, doi: 10.1109/IECON58223.2025.11221428.
 - [19] G. Demidova, J. J. Justo, D. Lukichev, N. Poliakov, and A. Anuchin, "Neural Network Models for Predicting Magnetization Surface Switched Reluctance Motor: Classical, Radial Basis Function, and Physics-Informed Techniques," *IEEE Access*, vol. 13, pp. 54987-54996, Feb. 2025.
 - [20] Z. Rayeen and S. Payami, "Design Methodology and Analytical Model for Higher Phase Switched Reluctance Motor Considering Mutual Coupling and Saturation Effect," *IEEE Trans. Ind. Electron.*, vol. 72, no. 12, pp. 12016-12027, Dec. 2025.
 - [21] Z. Zhang, Z. Jin, C. Chen, M. Salameh, and M. Krishnamurthy, "Hybrid Electromagnetic Modeling and NVH Co-Design of Switched Reluctance Motors," *IEEE Trans. Transp. Electrific.*, vol. 11, no. 1, pp. 3277-3286, Feb. 2025.