Fast Design Method of Variable Flux Reluctance Machines


Abstract—In this paper, a fast design method is developed based on a combination of analytical and finite element (FE) methods for variable flux reluctance machines (VFRMs). Firstly, the feasibility of using analytical method in optimization under unsaturated condition is confirmed. Then, by applying the FE method, the influence of magnetic saturation is considered. Compared with the unsaturated case, the optimal split ratio remains the same. Based on this, the optimal structural parameters can be initially set by analytical method and then refined by the FE method. Due to the fast speed of analytical method, less variable counts and narrowed variation ranges, the proposed method is significantly faster than the conventional pure FE based global optimization. Finally, the ranges, the proposed method is significantly faster than the analytical method, less variable counts and narrowed variation and then refined by the FE method. Due to the fast speed of analytical method, less variable counts and narrowed variation ranges, the proposed method is significantly faster than the conventional pure FE based global optimization. Finally, the proposed method is used for optimizing the 6-stator-slots VFRMs having different numbers of rotor poles. The 6-stator-slot/7-rotor-pole (6s/7r) VFRM is found to have the highest torque density. It is prototyped and tested to verify the analyses.

Index Terms—Analytical method, optimal design, torque density, variable flux reluctance machine.

NOMENCLATURE

$A_s$ Total stator slot area
$F$ Magnetomotive force (MMF)
$F_{a}, F_f$ MMFs of armature and field currents
$F_s$ Modulated MMF
$F_{sis}, F_{sf}$ Modulated MMFs of armature and field currents
$g_0$ Airgap length
$h_s, h_r$ Yoke thickness of stator and rotor
$I_{a}, I_b, I_c$ Currents of phases A, B and C
$I_f$ Current of field winding
$I_{rms}, I_{dc}$ RMS values of armature and field currents
$k_T$ Torque coefficient
$L_{stk}$ Machine stack length
$P_{cu}$ Total copper loss of VFRM
$P_{a}, P_{cuf}$ Copper losses of armature and field currents
$R_{si}$ Stator inner radius
$R_{so}$ Stator outer radius
$T_e$ Electromagnetic torque
$T_s, T_r, T_c$ Synchronous/reluctance/cogging torques
$W_a, W_b, W_c$ Winding functions of phases A, B and C
$W_f$ Winding function of field winding
$W_i$ Stator tooth width
$\beta_s, \beta_r$ Stator and rotor slot opening ratios
$\theta$ Mechanical angle
$\theta_s$ Stator slot pitch
$\lambda$ Split ratio
$\Lambda_r$ Magnitude of fundamental rotor radial permeance component
$\Lambda_s, \Lambda_r$ Stator and rotor permeance functions obtained by single-side saliency model
$\mu_0$ Vacuum permeability

I. INTRODUCTION

DUE to the increasing concerns on the price of rare-earth magnet material and the risk of demagnetization in permanent magnet (PM) machines [1-3], many magnetless machines, including induction machines (IMs) [4], rotor-wound-field synchronous machines (RWFSMs) [5-6], switched reluctance machines (SRMs) [7-8], synchronous reluctance machine (SynRMs) [9-10], vernier reluctance machines (VRM) [11], stator-wound-field flux switching machines (SWFFSMs) [12-13], and variable flux reluctance machines (VFRMs) [14-18] have been extensively investigated.

VFRMs are developed in [14] and [15]. Fig. 1 shows the configurations of two typical VFRMs, i.e. 6-stator-slot/4-rotor-pole (6s/4r) and 6s/5r VFRMs. They have doubly-saliency structure, which are similar to that of switched reluctance machine (SRM), and two sets of concentrated windings, i.e., AC armature and DC field windings. Apart from the advantages inherited from SRMs, such as robust rotor and compact windings, VFRMs show significantly smaller torque ripple and acoustic noise [16], and more flexible rotor pole number selection [17-18] than the SRMs. Moreover, the stator-located winding structure avoids the requirement of slip-rings/brushes and the heat can be easily dissipated from the stator. All these merits extend the application of VFRMs.

In order to obtain the highest torque density, design methods of VFRMs are important. In [19], the torque density of a 48s/40r VFRM is proved to be closely related to the rotor pole arc and rotor tooth height. In [20], four 6-stator-pole VFRMs
with 4, 5, 7 and 8-rotor poles are globally optimized. It is concluded that the maximum torque is achieved when the optimal rotor pole arc to pole pitch ratio is around 1/3 and the stator pole arc is equal or slightly smaller than rotor pole arc. Then, a weighted evaluation function is introduced in [21] to take the torque density, torque ripple, cogging torque, power factor, field winding voltage fluctuation and copper consumption into account during the optimization of stator wound field synchronous machine. However, in all these existing works, the optimization purely relies on the finite element (FE) based global optimization method, which is known to be time consuming.

In this paper, a fast optimization method is developed by the synergy of analytical and FE methods. In Section II, the feasibility of using analytical method in machine optimization under magnetically unsaturated condition is verified. In Section III, the influence of magnetic saturation on the optimal structural parameters is investigated by FE analyses. Based on this, a fast optimization method is developed in Section IV. It is then employed to optimize the 6-stator-pole VFRMs having different numbers of rotor poles to verify its capability. Finally, the experimental validation on a 6s/7r VFRM is presented in Section V.

II. ANALYTICAL OPTIMIZATION METHOD

In this section, the optimization by analytical method is discussed. Since the analytical method can only be applicable to the linear case (the permeability of cores is set to infinity), the influence of magnetic saturation will be investigated later in Section III.

A. Analytical torque calculation model

Based on the Lorentz force law, the instantaneous torque expression of VFRMs is given by (1). The detailed derivation procedure can be found in [22].

$$T_c(t) = -R_o L_{st} \int_0^{2\pi} F_s(\theta, t) \Lambda_s(\theta, t) d\theta$$ (1)

where $R_o$ is the radius of stator inner surface; $L_{st}$ is the machine stack length; $\theta$ is the mechanical angle in the stator reference frame; $\Lambda_s$ is the rotor radial permeance function obtained by salient rotor and slotless stator model; and $F_s$ is the “Modulated MMF” defined by

$$F_s(\theta, t) = F(\theta, t) g_0 \Lambda_s(\theta) / \mu_0$$ (2)

where $g_0$ is the airgap length; $\mu_0$ is the vacuum permeability; $\Lambda_s(\theta, t)$ is the stator radial permeance obtained by smooth rotor and slotted stator model; and $F(\theta, t)$ is the MMF generated by the armature and field windings, i.e.

$$F(\theta, t) = F_a(\theta, t) + F_f(\theta, t)$$ (3)

where $F_a(\theta, t)$ and $F_f(\theta, t)$ are the MMF functions [23] of armature winding and field winding, respectively. They can be deduced by the product of corresponding winding functions and excitations, i.e.

$$\begin{cases} F_a(\theta, t) = W_a(\theta) I_a(t) + W_b(\theta) I_b(t) + W_c(\theta) I_c(t) \\ F_f(\theta, t) = W_f(\theta) I_f(t) \end{cases}$$ (4)

where $W_a(\theta)$, $W_b(\theta)$, $W_c(\theta)$ and $W_f(\theta)$ are the winding functions and currents of phase A, phase B, phase C and field windings, respectively.

Regarding the stator and rotor radial permeance, both of

![Fig. 1](image1.png) Cross sections and winding configurations of the 6s/4r and 6s/5r VFRMs. (a) 6s/4r VFRM. (b) 6s/5r VFRM

![Fig. 2](image2.png) Distributions of analytically and FE predicted stator radial permeances for different stator slot opening ratios ($R_s=22.5\text{mm}$, $g_0=5.5\text{mm}$, $\theta_s=60\text{deg}$.).

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Unit</th>
<th>6s/4r</th>
<th>6s/5r</th>
</tr>
</thead>
<tbody>
<tr>
<td>Stator outer diameter</td>
<td>mm</td>
<td>90</td>
<td>90</td>
</tr>
<tr>
<td>Airgap length</td>
<td>mm</td>
<td>0.5</td>
<td>0.5</td>
</tr>
<tr>
<td>Total copper loss</td>
<td>W</td>
<td>30</td>
<td>30</td>
</tr>
<tr>
<td>Turns per coil (AC/DC)</td>
<td>-</td>
<td>144/144</td>
<td>144/144</td>
</tr>
<tr>
<td>Split ratio</td>
<td>-</td>
<td>0.5</td>
<td>0.52</td>
</tr>
<tr>
<td>Stator pole arc</td>
<td>deg</td>
<td>27</td>
<td>24</td>
</tr>
<tr>
<td>Rotor pole arc</td>
<td>deg</td>
<td>34.6</td>
<td>26</td>
</tr>
</tbody>
</table>

![Fig. 3](image3.png) Torque waveforms of optimized 6s/4r and 6s/5r VFRMs predicted by analytical and FE methods ($P_{in}=30W$).
them can be calculated by the model analytical for single-sided saliency motor [24]. Taking the stator radial permeance for example, its distribution under one stator slot pitch is

\[
\Lambda_s(\theta) = \mu_0 \int \left[ g_b + g(\theta) \right] d\theta
\]

\[
g(\theta) = \begin{cases} 
\pi R_s \sin(\theta) \sin(\beta \theta_1/2-\theta/2) & \theta \in [0, \beta \theta_1] \\
2 \sin(\beta \theta_1/4) \cos(\theta/2-\beta \theta_1/4) & \theta \in [\beta \theta_1, \theta_1] \\
0 & \text{otherwise}
\end{cases}
\]

(5)

where \( \beta \) is the stator slot opening ratio and \( \theta_1 \) is the slot pitch.

A comparison between analytically and FE predicted stator radial permeance distributions over one stator slot pitch for different stator slot opening ratios is presented in Fig. 2. Good accuracy is found for the analytical method.

Similarly, the rotor radial permeance function can also be deduced but is not presented here for simplicity.

After obtaining the permeance and MMF distributions, the instantaneous torque of VFRMs can be predicted analytically. For verification, the 6s/4r and 6s/5r VFRMs are chosen as examples. Their main specifications are listed in Table I. Fig. 3 compares the analytically and FE predicted torque waveforms. Good agreement is observed, indicating the accuracy of using analytical method in torque estimation under magnetic saturation condition.

### B. Optimal AC/DC ratio for maximum torque

Further, by substituting (2) and (3) into (1), the torque equation can be divided into three components: synchronous torque \( T_s \), reluctance torque \( T_r \), and cogging torque \( T_c \), as shown in (6).

\[
T_e(t) = -R_s I_{sd} \int_0^\pi \Lambda_s(\theta) dF_s^2(\theta,t) d\theta - R_r I_{rd} \int_0^\pi \Lambda_r(\theta) dF_r^2(\theta,t) d\theta - R_f I_{df} \int_0^\pi \Lambda_r(\theta) dF_r^2(\theta,t) d\theta
\]

(6)

where \( F_s \) and \( F_r \) are modulated MMFs of armature and field currents, respectively.

Based on the harmonic analysis, the torque principle of VFRMs is comprehensively illustrated with the concept of magnetic gearing effect in [22]. It is found that the average torque of VFRMs is mainly generated by synchronous torque and can be concisely expressed by:

\[
T_{e,avg} = T_{s,avg} = k_T R_s I_{sm} N \Lambda_s I_{rd} I_{dc}
\]

(7)

where \( k_T \) is a coefficient determined by the stator radial permeance and winding functions; \( \Lambda_{s1} \) is the magnitude of the 1st rotor radial permeance harmonic; \( I_{rms} \) and \( I_{dc} \) are the rms values of armature and field currents, respectively.

Assuming the total copper loss (the sum of armature copper loss \( P_{cu} \) and field copper loss \( P_{cu} \)) of VFRM, \( P_{cu} \), is a constant, i.e.

\[
P_{cu} = P_{cu} + P_{cu} = I_{rms}^2 R_s + I_{dc}^2 R_f
\]

(8)

where \( R_s \) and \( R_f \) are the total resistances of armature and field windings, respectively.

Then, the torque equation (7) can be rewritten as:

\[
T_{e,avg} = k_T R_s I_{rd} N \Lambda_s I_{rd} I_{dc} \left[ \frac{P_{cu} - I_{rms}^2 R_s}{R_f} \right]
\]

(9)

In order to maximize the average torque, \( dT_{e,avg}/dI_{rms}=0 \):
\[ dT_{\text{avg}}/dt_{\text{avg}} = k R s L_{\text{stk}} N_{s} \Lambda_{s} N_{r} \left( \left( P_{sa} - P_{r}^{2} R_{r} \right)^{1/2} - \left( P_{sa} - P_{r}^{2} R_{r} \right)^{1/3} \right) = 0 \]

\[ \Rightarrow P_{sa} = 2P_{r}^{2} R_{r} = 2P_{\text{cuf}} \]

\[ \Rightarrow P_{\text{cuf}} = P_{sa} - P_{\text{cuf}} = P_{\text{cuf}} \]

Therefore, the largest average torque/copper loss can be obtained when the copper losses of field and armature windings are the same, i.e. \( P_{\text{cuf}} = P_{\text{cuf}} \). Further, if the field and armature windings share the same slot area and turns number, the optimal AC/DC ratio is \( I_{\text{mag}}/I_{dc} = 1 \). This conclusion is confirmed by FE in [15] and will be used for all the optimizations in this paper.

C. Analytical optimization method

From the torque expression (1), it can be seen that \( L_{\text{stk}}, F_{s} \) and \( \Lambda_{r} \) are three variables directly related to output torque. Hence, five structural parameters, i.e., split ratio, stator and rotor pole arcs, stator/rotor yoke thicknesses, need to be optimized due to their close relationships with the aforementioned three variables, as shown in Fig. 4. Since the core saturation is neglected for analytical method, the stator/rotor yoke thicknesses are kept minimum according to the mechanical requirement and will be optimized later with the FE method.

With the design constraints listed in Table II, the optimal split ratio and stator/rotor pole arc ratios can be obtained with parametric calculation using the analytical model, as shown in Fig. 5. Finally, the globally optimized split ratio, stator and rotor pole arc ratios by analytical and FE methods are compared in Table III. Good agreements are found between analytical and FE results, indicating the feasibility of using analytical method for machine design under linear condition.

Further, it is worth noticing that the optimal rotor pole arc ratio is found to be 0.44 for all VFRMs under linear case. This is mainly due to the fact that the average torque of VFRMs is proportional to the fundamental rotor permeance component \( \Lambda_{r} \) (see equation (7)), and \( \Lambda_{r} \) peaks when the rotor pole arc ratio is 0.44, as shown in Fig. 4.

III. INFLUENCE OF MAGNETIC SATURATION

In this section, the magnetic saturation is taken into account by using the nonlinear FE method. Several 6s/4r and 6s/5r VFRMs are designed under different copper loss levels with pure global optimization method. The obtained optimal structural parameters are compared to those calculated by analytical method under linear case to investigate the influence of core saturation.

A. Split ratio

The influence of load and magnetic saturation on the optimal split ratio \( \lambda \) is shown in Fig. 7. It can be seen that the optimal split ratio shows an upward trend with the increase of copper loss. This can be explained by Fig. 8. On one hand, the increase of \( \lambda \) will lead to a reduction in slot area \( A_{s} \) and electric loading when the copper loss is fixed. On the other hand, owing to the reduced current, the magnetic saturation will be alleviated. Also, the rotor outer radius \( R_{ro} \) becomes longer. A compromise should be made between these two aspects and the split ratio tends to increase under larger load condition for the sake of alleviating the magnetic saturation. Compared with the linear case, the optimal split ratio will be increased by 1–1.2 times depending on the load condition.

B. Stator pole arc ratio

The influence of load and magnetic saturation on the optimal stator pole arc ratio \( \beta_{s} \) is illustrated in Fig. 9.

It is noted that the optimal \( \beta_{s} \) is almost independent of load condition. As shown in Fig. 10, an increase of \( \beta_{s} \) will lead to smaller slot area and larger tooth width \( W_{t} \), both of which will alleviate the stator saturation. However, a larger \( \beta_{s} \) also means an increase in average airgap permeance \( \Lambda_{g} \) and more severe magnetic saturation. Hence, \( \beta_{s} \) has insignificant influence on saturation. Its optimal value is almost constant with the increase of copper loss. In this case, the optimal value of \( \beta_{s} \) for linear case is also applicable in nonlinear case.

![Fig. 7. Variation of optimal split ratio against copper loss. (a) Optimal value. (b) Per unit value](image)

![Fig. 8. Relationship between split ratio and output torque.](image)

![Fig. 9. Variation of optimal stator pole arc ratio against copper loss.](image)
C. Rotor pole arc ratio

The influence of load and magnetic saturation on the optimal rotor pole arc ratio $\beta_r$ is illustrated in Fig. 11. The optimal value shows a downward trend with the increase of copper loss. Its variation range is between 0.33~0.44.

D. Stator/rotor yoke thickness

Compared with the linear condition, the stator and rotor yoke thicknesses are expected to increase when the cores are saturated. The optimal yoke thicknesses can be obtained by global optimization method, as shown in Fig. 14.

IV. FAST OPTIMIZATION METHOD

Based on the revealed influence of magnetic saturation on the optimal structural parameters, a fast optimization method is developed by combining analytical and FE methods.

A. Conventional optimization method

For conventional method, the torque calculation relies fully on FE method. The global optimization module of ANSYS Maxwell 15.0 can be used to get the optimal specification. The procedure is:

Step 1: The parametric optimization is firstly used to get the rough variation range of all the structural parameters.

Step 2: All five structural variables are further globally optimized using the genetic algorithm.

In this case, 2~3 days are usually required.

B. Proposed fast optimization method

The procedure of proposed fast optimization method is:

Step 1: Set the rotor pole arc ratio as 0.44 (optimal value...
obtained from the analytical model) and stator/rotor yoke thicknesses as minimum values for mechanical consideration, the stator pole arc ratio and split ratio are optimized using analytical method.

Step 2: Globally optimize split ratio (1−1.2 times of linear optimal value), rotor pole arc ratio (0.33−0.44) and stator/rotor yoke thicknesses, whereas the stator pole arc ratio is fixed as the obtained optimal value of Step 1.

By using the fast optimization method, only 6−12 hours are required to obtain the optimal results, which are much shorter than the conventional method. The low time consumption is mainly benefited from the fast calculation speed of analytical method, less variable counts and narrowed variation ranges, as shown in Table IV.

<table>
<thead>
<tr>
<th>TABLE IV</th>
<th>COMPARISON OF CONVENTIONAL AND PROPOSED METHODS</th>
</tr>
</thead>
<tbody>
<tr>
<td>Step</td>
<td>Conventional method</td>
</tr>
<tr>
<td></td>
<td>Parametric optimization</td>
</tr>
<tr>
<td>I</td>
<td>Split ratio</td>
</tr>
<tr>
<td></td>
<td>Stator pole arc ratio</td>
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<tr>
<td></td>
<td>Rotor pole arc ratio (3 variables, 1−2 hours)</td>
</tr>
<tr>
<td></td>
<td>(2 variables, 1−2 mins)</td>
</tr>
<tr>
<td></td>
<td>Global optimization</td>
</tr>
<tr>
<td>II</td>
<td>Split ratio (1−1.2 times of step I)</td>
</tr>
<tr>
<td></td>
<td>Stator pole arc ratio</td>
</tr>
<tr>
<td></td>
<td>Rotor pole arc ratio</td>
</tr>
<tr>
<td></td>
<td>Stator yoke thickness</td>
</tr>
<tr>
<td></td>
<td>Rotor yoke thickness</td>
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<tr>
<td></td>
<td>Rotor pole arc ratio</td>
</tr>
<tr>
<td></td>
<td>Rotor yoke thickness</td>
</tr>
<tr>
<td></td>
<td>Split ratio (4 variables, 6−12 hours)</td>
</tr>
</tbody>
</table>

C. Optimization of VFRMs with different stator/rotor pole number combinations

Further, the developed fast optimization method is applied to the optimization of the 6-stator-slot VFRMs with 2−20 rotor pole numbers according to the constraints listed in Table I. Their torque capabilities are shown in Fig. 15. Good agreement is found between the results of conventional and proposed methods while the proposed method can obtain the optimal design in much shorter time. Moreover, the 6s/7r and 6s/11r VFRMs are found to have the highest torque density. Considering the fact that 6s/7r VFRM has a much lower electrical frequency than 6s/11r VFRM under the same rotating speed, 6s/7r is the preferred stator/rotor pole combination under investigated specification.

![Fig. 15. Variation of the average torque against rotor pole numbers for 6-stator-slot VFRMs with different rotor pole numbers (Pcu=30W).](image)

V. EXPERIMENTAL VERIFICATION

For experimental verification, a 6s/7r VFRM is prototyped, as shown in Fig. 16. Its main specification is listed in Table V.

It can be seen that both the armature and field windings are concentrated types and wounded on all the stator teeth. During the experiment, the field winding is excited by a DC supply while the armature winding is connected to an inverter.

When the machine is operating at open-circuit and only the field windings are excited, the phase back-EMF can be measured, as shown in Fig. 17. Two different field currents are tested and good agreement can be found between FEA and experimental results. Although some harmonics can be observed from the back-EMF waveforms, they are close to sinusoidal for the 6s/7r VFRM.

Regarding the torque performance, the static torque is measured when both armature and field windings are excited by DC current with the following relationship: \( I_a = I_b = 2I_c \). Since the armature and field windings share the same slot area and turns number, the RMS current of armature winding is equal to that of field winding to achieve the maximum torque. Again, the measurements match well with the FEA results, as can be seen from Fig. 18.

Then, the average torque of the prototype is also measured under different load condition, as presented in Fig. 19. It can be found that good agreement is found between FEA and measured results under low copper loss condition. When the copper loss is around rated condition, the measured average torque is slightly smaller than the FEA prediction. This is mainly due to the flux leakage of end windings.

Finally, the torque ripple performance of the prototype is measured when copper loss is 20W, as presented in Fig. 20. The torque profile is not fluctuating in ideal periodic way. This is mainly due to the measurement error and disturbance from the
load machine. Nevertheless, the peak-to-peak values of measurement and FEA prediction match with each other, verifying the feature of small torque ripple in 6s/7r VFRM [17].

![Figure 17](image_url)  
**Fig. 17.** FEA predicted and measured phase back-EMFs at 400rpm for 6s/7r VFRM.

![Figure 18](image_url)  
**Fig. 18.** FEA predicted and measured static torques when \( I_a=I_c=-2I_b \) for 6s/7r VFRM.

![Figure 19](image_url)  
**Fig. 19.** FEA predicted and measured average torques under different copper loss for 6s/7r VFRM.

![Figure 20](image_url)  
**Fig. 20.** FEA predicted and measured torque ripples for 6s/7r VFRM (Copper loss=20W).

VI. CONCLUSION

In this paper, a fast optimization method is developed for VFRMs by a combination of analytical and FEA methods. The capability and accuracy of analytical method in torque estimation under linear situation is firstly confirmed. The optimal rotor pole arc of linear case is found to be 0.44 for all the VFRMs. Then, the influence of saturation effect on optimal structural parameters is revealed. It is found that, depending on the load condition, (1) the split ratio will increase by 1–1.2 times of the linear optimal value; (2) the rotor pole arc ratio will decrease and vary within 0.33–0.44; (3) the stator pole arc will remain the same as its linear optimal value. Based on this, a fast optimization method is developed. Its capability is verified on the 6-stator-slot VFRMs having different rotor poles. Finally, a 6s/7r VFRM is prototyped and tested to verify the analyses.

REFERENCES


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