Analysis of a Novel Magnetic-Geared Dual-Rotor Motor With Complementary Structure

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Abstract—A permanent-magnet dual-rotor motor is particularly suitable for constructing the power split device in hybrid electric vehicle application. This paper proposes a new magnetic-geared dual-rotor motor (MGDRM) design with complementary structure, in which both the inner and outer rotors are divided into three modules with a proper angular displacement for each other along the axis direction. This complementary design makes the flux linkage symmetrical and total cogging torque significantly reduced, without impairing the torque production. A simplified magnetic circuit model is developed to illustrate the complementary principle. By finite-element analysis (FEA), the effectiveness of such complementary structure is verified through the comparison with the conventional design. A prototype motor has been manufactured, and experiments have been carried out. Both FEA and experiments show that this new MGDRM offers symmetrical back-EMF waveforms, smaller cogging torque, and lower torque ripple.

Index Terms—Cogging torque, complementary structure, hybrid electric vehicle (HEV), magnetic-geared dual-rotor motor (MGDRM), torque ripple.

I. INTRODUCTION

T HE series–parallel hybrid electric vehicle (SP-HEV) is considered as one of the feasible new energy vehicles [1]. Such series–parallel structure consists of a power split device which coordinates the power output from the internal combustion engine (ICE) with the power requirement of the wheels. At present, the series–parallel structure used in Toyota Prius, which is composed of an electric generator and one planetary gear, has been well known. To reduce the mechanical abrasion and avoid using the expensive lubrication system, various series–parallel topologies, with the concept of an electric continuous variable transmission (ECVT), are proposed [2]–[6].

In the domain of the electrical solutions, permanent-magnet dual-rotor motor (PMDRM) has three ports: two rotors, which are two mechanical ports for ICE and wheels, respectively, and one electrical interface for the electrical system, which is the stator armature winding. As an important feature, the two rotors can rotate mechanically independent of each other, similar to the carrier and ring gears of the planetary gear.

For some of the systems with winding set at one of the rotors, power flow from the ICE to the electrical system must go through the slip rings and brushes which limit the application of this topology.

To eliminate slip rings and brushes, magnetic-geared dual-rotor motors (MGDRMs) have been proposed by merging the concept of magnetic gear and permanent-magnet machines [8]–[14], and Fig. 1 shows a typical exploded drawing of the MGDRM.

The available configurations in [10]–[14] have numerous poles in the field modulation ring which may lead to a high electrical operation frequency, especially for HEV application, consequently induce severe losses, increase the control complexity, and require precise sensors to measure the rotor position. However, for an ECVT system with a given transmission ratio, direct reduction of the pole number might lead to unacceptable cogging torque.

Another problem in the current configurations is the conspicuous asymmetry in the flux linkage and back-EMF waveforms in most configurations, which will definitely lead to the severe torque ripple as shown in [11]; no effective solution has been reported according to the best of the author’s knowledge.

The objective of this paper is to propose a novel topology of the MGDRM to make it practicable for HEV application. To
overcome the drawbacks of the current MGDRM designs, both outer rotor and inner rotor are divided into three equal modules with a proper angular displacement for each other along the axis direction. The phase back-EMF waveforms of the three modules could be accumulated in one stator winding and then form a symmetrical phase back-EMF waveform. Such structure is the so-called complementary structure in this paper. Its operation principle and characteristics will be illustrated in detail. The effectiveness of the novel design is verified by both finite-element analysis (FEA) and experiments on a prototype machine.

II. TOPOLOGY AND OPERATION PRINCIPLE

A. Basic Structure and Configuration

The configuration of the motor discussed in this paper should not be only correct in theory but also practicable in application and manufacture. For this reason, there are some design principles that should be considered.

1) Generally, the speed of the two rotors of the MGDRM must abide by

\[ p_w \times n_w + p_{ir} \times n_{ir} = p_{or} \times n_{or} \]  (1)

\[ |p_w \times n_w| = 60 \times f \]  (2)

where \( p_w, p_{ir}, \) and \( p_{or} \) are the pole-pair numbers of the stator winding, inner rotor (mounted with magnets), and outer rotor (field modulation ring), respectively, \( n_w, n_{ir}, \) and \( n_{or} \) are the rotation speeds of the stator magnetic field, inner rotor, and outer rotor, respectively, and \( f \) is the electrical frequency in the windings.

Such a speed relationship is similar to that of the planetary gear

\[ n_{sun} + p \times n_{ring} = (1 + p) \times n_{carrier} \]  (3)

where \( n_{sun}, n_{ring}, \) and \( n_{carrier} \) are the rotation speeds of the sun, ring gears, and carrier of the planetary gear, respectively, and \( p \) is the characteristic parameter, which is considered as the transmission ratio. This analogy can be summarized as

\[ p = \frac{p_{ir}}{p_w}. \]  (4)

Such analogy provides a reference to configure the parameter of the MGDRM. For balance of power and fuel consumption, this transmission ratio \( p \) in HEV application is always set in the range of 1.5–3 [15], for instance, 2.6 in Prius [16].

2) The pole-pair number of rotors should not be numerous, so as to reduce frequency. Combining (1) and (2) yields

3) To avoid the unbalanced magnetic force, an even number of poles should be employed in the field modulation ring rotor (outer rotor).

4) A typical full pitch distributed winding is selected. Although fractional-slot concentrated winding has advantages in reducing winding ends and improving flux weakening capability [18], more inductance is not always beneficial in such a magnetic-gear motor, for which the power factor is a weakness [14].

Based on the aforementioned discussion, the MGDRM will have three parts: the outer rotor (plays the role as the field modulation ring of the magnetic gear), the inner rotor (with PM mounted), and the stator (with full pitch distributed windings). According to the principle of magnetic gear, the pole-pair number relationship should follow

\[ p_w + p_{ir} = p_{or}. \]  (6)

Referring to the parameter \( p \) mentioned in (3), \( p_{ir}/p_w \) is selected as 2. All of the three numbers should be small integers, so they are chosen as follows: \( p_w = 2, p_{or} = 6, \) and \( p_{ir} = 4. \)

For convenience of manual manufacture, permanent magnets are mounted on the outside surface of the inner rotor and magnetized radially. The stator slot number is 24, then the slot-per-phase-per-pole is equal to 2, and a relatively short end-winding could be gotten. The topology of the novel MGDRM is shown in Fig. 2, and the design specifications are listed in Table I.
TABLE I

<table>
<thead>
<tr>
<th>Items</th>
<th>basic machine</th>
</tr>
</thead>
<tbody>
<tr>
<td>Rated power</td>
<td>1.5 kW</td>
</tr>
<tr>
<td>Outer rotor rated speed</td>
<td>1600 rpm</td>
</tr>
<tr>
<td>Outer rotor rated torque</td>
<td>9.2 Nm</td>
</tr>
<tr>
<td>Inner rotor rated speed</td>
<td>1200 rpm</td>
</tr>
<tr>
<td>Inner rotor rated torque</td>
<td>6.1 Nm</td>
</tr>
<tr>
<td>Number of stator slot</td>
<td>24</td>
</tr>
<tr>
<td>Pole-pairs of armature windings</td>
<td>2</td>
</tr>
<tr>
<td>Pole-pairs of outer rotor</td>
<td>6</td>
</tr>
<tr>
<td>Pole-pairs of inner rotor</td>
<td>4</td>
</tr>
<tr>
<td>Inside radius of inner rotor ($R_{oi}$)</td>
<td>22 mm</td>
</tr>
<tr>
<td>Outside radius of inner rotor ($R_{o0}$)</td>
<td>46.4 mm</td>
</tr>
<tr>
<td>Stator outside radius ($R_{o}$)</td>
<td>85 mm</td>
</tr>
<tr>
<td>Stator inside radius ($R_{s}$)</td>
<td>60.6 mm</td>
</tr>
<tr>
<td>Height of stator yoke ($h_{back}$)</td>
<td>10 mm</td>
</tr>
<tr>
<td>Magnets</td>
<td>NdFeB</td>
</tr>
<tr>
<td>PM thickness ($h_{PM}$)</td>
<td>3 mm</td>
</tr>
<tr>
<td>Stator slot area</td>
<td>152 mm$^2$</td>
</tr>
<tr>
<td>Winding turns per slot</td>
<td>34</td>
</tr>
<tr>
<td>Wires in parallel per turn</td>
<td>4</td>
</tr>
<tr>
<td>Wire size</td>
<td>20 (AWG)</td>
</tr>
<tr>
<td>Stack length ($L_s$)</td>
<td>45 mm</td>
</tr>
<tr>
<td>Air-gap length ($h_{gap}$, $h_{gapg}$)</td>
<td>0.6 mm</td>
</tr>
<tr>
<td>Width of stator teeth ($w_{tooth}$)</td>
<td>6.8 mm</td>
</tr>
<tr>
<td>Width of field modulation ring ($w_{ring}$)</td>
<td>30 deg</td>
</tr>
<tr>
<td>Height of field modulation ring ($h_{ring}$)</td>
<td>10 mm</td>
</tr>
</tbody>
</table>

In an actual HEV application, the outer rotor will connect to the ICE crankshaft, and the inner rotor will connect to the wheels. With the example of Prius 2004 in a highway driving cycle [19], the maximum speed of the generator is 10,000 r/min, which means that the winding magnetic field of the proposed MGDRM rotates at the speed of 10,000 r/min, and then, the maximum electrical operation frequency will be 333 Hz.

B. Introduction of the Potential Problem

In conventional three-phase motors, there used to be a strict symmetrical relationship among phase A, phase B, and phase C. Moreover, such symmetrical relationship contributes to less torque ripple, especially in the overload situation.

However, for the MGDRM in Fig. 1, this symmetrical relationship cannot be achieved [11]. This character leads to serious impacts on the motor performance. One approach to achieving a better understanding of the asymmetry of the MGDRM is to calculate out the waveforms of the phase flux linkages and back-EMFs. When the inner rotor is kept standstill and the outer rotor rotates, the motor operates as a stator permanent-magnet brushless machine [20]. Fig. 3 shows the waveforms of the phase flux linkages and back-EMFs.

It can be seen from Fig. 3 that both the flux linkage and back-EMF waveforms of phase A are different from phase B and phase C. Although the difference between the flux linkages is not noticeable, the back-EMF waveforms, which are the first derivative of flux linkage, present serious asymmetry and are nonsinusoidal. Similar results have also been reported in [11], but it did not attract enough attention.

The root cause of this phenomenon is the asymmetrical magnetic circuits for three-phase windings. For a conventional PM motor, during one electrical period of the rotor rotation, the magnetic circuit of phase A at 0° should be the same as the counterpart of phase C at 120° (also the same as the counterpart of phase B at 240°). However, this could not be achieved by this MGDRM when the outer rotor rotates. From the perspective of magnetic field, it implies that there are some harmonic components that could not be canceled by the winding itself.

Furthermore, the cogging torque will definitely be a problem, which also is ignored in the traditional design with high pole numbers. The dominating reason for the cogging torque is the interaction between the PMs mounted on the inner rotor and the field modulation ring.

The cogging torque in the MGDRM exists in both outer rotor and inner rotor. Fig. 4 shows the cogging torque waveform between the inner and outer rotors.

Both of the cogging torque waveforms obviously have four cycles and are approximately sinusoidal. Moreover, it seems that the cogging torque values of the two rotors have equal amplitudes but opposite direction. These two features indicate that the cogging torque of the two rotors is mainly caused by each other, while the counter-torque of the cogging torque on the stator is much lower.
III. PRINCIPLE OF COMPLEMENTARY STRUCTURE

To resolve the asymmetry problem mentioned previously, a complementary structure of the MGDRM is proposed. As shown in Fig. 5, both the outer rotor and inner rotor are divided into three equal modules along the axis direction shifted by 60 electrical degrees (corresponding to 10 mechanical degrees in the outer rotor and 15 mechanical degrees in the inner rotor) along the circumference of each other. A flux barrier is purposely designed between the adjacent modules. Fig. 6 illustrates the effect of the barrier width on flux linkage by 3-D FEA.

![Fig. 4](image1) Cogging torque of the two rotors when the inner rotor is latched but the outer rotor rotates (2-D FEA result).

![Fig. 5](image2) MGDRM based on the proposed structure: exploded view of the motor with complementary structure.

![Fig. 6](image3) PM flux linkage values in different widths of the flux barrier (3-D FEA result).

It seems that the flux barrier width affects little on the flux linkage value. For convenience of manual manufacture, the width in this prototype is set at 5 mm. A thinner width may be used in mass production. As shown in Fig. 6, the PM flux linkage value calculated by 3-D FEA is 0.08 Wb, 93% of the value calculated by 2-D FEA (0.086 Wb).

A. Principle of Back-EMF Complementary

Phase angle differences of the three rotor modules in both inner rotors and outer rotors result in transformations of the back-EMF waveform shapes. Moreover, all of the three modules share the same armature winding; consequently, the three different back-EMFs produced by the three modules could be accumulated to form a new back-EMF waveform. As an example, such transformation of phase A has been illustrated in Fig. 7(a).

In the MGDRM with complementary structure, the flux linkage and back-EMF are the summation of those of the three modules. Fig. 7(b) illustrates the three-phase EMF waveforms of the MGDRM with complementary structure, when both of the rotors are running at the rated speed, showing symmetrical and much more sinusoidal waveforms as compared with Fig. 3(b).
B. Retention of Effective Magnetic Field

The essential reason of the nonsinusoidal waveform of the flux linkage is the existence of harmonic magnetic fields, whose pole-pair number is not identical with the armature winding. Therefore, the essential function of the complementary structure is to eliminate the harmonic magnetic fields. As well known, rotor skewing is bound to impair the flux linkage amplitude and then reduce the torque production [21]. The complementary structure, however, will not degrade the effective magnetic field (namely, the field component with the same pole-pair number as the armature windings). Thus, the torque production of the proposed MGDRM will be kept unchanged.

In order to illustrate the retention of the effective magnetic field component more clearly, a simplified magnetic circuit is developed to analyze the field distribution, as shown in Fig. 8, where the modulation effect of the stator teeth is ignored, and the back-EMFs in the windings can be considered to be produced by the magnetic field in the region between the outer gap and the inner gap.

In Fig. 8, the magnetomotive force (MMF) of PM excitation is denoted as \( F_{PM}(\theta, \theta_{ir}) \). \( R_{\text{ingap}} \) is the inner airgap reluctance, which also includes the reluctance of the PM poles. \( R_{\text{outgap}} \) is the reluctance of the outer airgap. \( R_{\text{ring}}(\theta, \theta_{or}) \) indicates the reluctance of the field modulation ring region, which is changing with the outer rotor position. \( \theta_{ir} \) is the angular position of the inner rotor, and \( \theta_{or} \) is the angular position of the outer rotor.

The Fourier series expansions of the MMF excited by PMs can be described as

\[
F_{PM}(\theta, \theta_{ir}) = \sum_{l=1, \text{ odd}}^{\infty} a_l \cos (lp_{ir}(\theta - \theta_{ir} + \theta_{ir0})) \cdot (7)
\]

Permeance coefficient, which represents the permeance in a unit area, will be used to calculate the field distribution instead of reluctance. It can be described as

\[
P(\theta, \theta_{or}) = \frac{1}{R_{\text{ingap}} + R_{\text{outgap}} + R_{\text{ring}}(\theta, \theta_{or})} = c_0 + \sum_{m=1, \text{ odd}}^{\infty} c_m \cos (mp_{or}(\theta - \theta_{or} + \theta_{or0})) \cdot (8)
\]

Then, the magnetic field distribution in the outer airgap can be approximately considered as

\[
B(\theta, \theta_{ir}, \theta_{or}) = F_{PM}(\theta, \theta_{ir}) \times P(\theta, \theta_{or})
\]

\[
= \sum_{l=1, \text{ odd}}^{\infty} a_l c_0 \cos (lp_{ir}(\theta - \theta_{ir} + \theta_{ir0})) + \sum_{l=1, \text{ odd}}^{\infty} m \sum_{m=1, \text{ odd}}^{\infty} \frac{a_l c_m}{2} \times \cos \left( mp_{or} + lp_{ir} \right) \left( \theta - \frac{mp_{or} \theta_{or} + lp_{ir} \theta_{ir}}{mp_{or} + lp_{ir}} \right.
\]

\[
+ \frac{mp_{or} \theta_{or0} + lp_{ir} \theta_{ir0}}{mp_{or} + lp_{ir}} \right)
\]

\[
+ \sum_{l=1, \text{ odd}}^{\infty} m \sum_{m=1, \text{ odd}}^{\infty} \frac{a_l c_m}{2} \times \cos \left( mp_{or} - lp_{ir} \right) \left( \theta - \frac{mp_{or} \theta_{or} - lp_{ir} \theta_{ir}}{mp_{or} - lp_{ir}} \right.
\]

\[
+ \frac{mp_{or} \theta_{or0} - lp_{ir} \theta_{ir0}}{mp_{or} - lp_{ir}} \right) \). (9)
\]

The coefficients \( l, m, a_l, \) and \( c_m \) are all constants. \( \theta_{ir0} \) and \( \theta_{or0} \) are used to describe the initial mechanical angular positions of the inner rotor and outer rotor, respectively. \( \theta_{ir} \) and \( \theta_{or} \) denote the mechanical angular positions of the inner and outer rotors, respectively. According to the magnetic gear’s theory [22], [23], only the third item of (9) can produce the effective magnetic field with two-pole-pairs to interact with the stator armature windings, when

\[
l = m = 1. \quad (10)
\]

For the MGDRM without complementary structure, the magnetic density distribution of the effective magnetic field can be calculated as

\[
B_{1,1}(\theta, \theta_{ir}, \theta_{or}) = \frac{a_1 c_1}{2} \cos \left( (p_{or} - p_{ir}) \theta - (p_{or} \theta_{or} - p_{ir} \theta_{ir}) \right.
\]

\[
+ (p_{or} \theta_{or0} - p_{ir} \theta_{ir0}) \). \quad (11)
\]

Simply, the complementary structure could be considered as an association of three modules having different initial rotor positions. According to (11), as long as the initial rotor position differences comply with

\[
p_{or} \times \Delta \theta_{or0} = p_{ir} \times \Delta \theta_{ir0} \quad (12)
\]

the effective magnetic fields or fundamental components of the three complementary modules will be identical at any time.

Fig. 9 shows the outer airgap flux density waveforms of the three modules at a moment. In this application, the two-pole-pair field is the effective magnetic field for the proposed prototype. It can be seen that, although the outer airgap magnetic fields of the three modules are different, the fundamental
Fig. 9. Outer gap flux density distributions of the three modules at the same moment (2-D FEA result). (a) Module 1. (b) Module 2. (c) Module 3.

Fig. 10. Cogging torque of the MGDRM when the outer rotor rotates (2-D FEA result). (a) Cogging torque of the outer rotor. (b) Cogging torque of the inner rotor.

flux density waveforms of the two-pole-pair fields in the three modules are identical in both amplitude and phase. Namely, the effective magnetic fields of the three modules reinforce each other. Thus, the complementary structure does not impair torque production unlike the traditional rotor skewing which always leads to average torque reduction.

C. Principle of Cogging Torque Reduction

To illustrate the principle of cogging torque reduction, the cogging torque of all of the three modules is calculated. By repeating the simulation method in Fig. 4 for the new MGDRM, its cogging torque can be obtained as shown in Fig. 10.

It can be seen from Fig. 10 that the cogging torque waveforms of the three modules in the MGDRM are similar but with 30 electrical degrees shifts. Consequently, the composite cogging torque of the whole motor is much less than that shown in Fig. 4 (the inner rotor cogging torque has been reduced to 9.5% of Fig. 4, and the outer rotor cogging torque has been reduced to 5.3% of Fig. 4), due to the cancelation with each other in three modules. Such “cancel effect” appears in both the inner rotor and the outer rotor.

IV. Performance Comparison of the Two Motors

FEA has been carried out to verify the performances of the proposed MGDRM and the effect of the complementary structure.

For comparison, the motor without complementary structure is named as motor_1, and the topology with complementary structure is named as motor_2, in which the rotors contain a stack length of 15 mm × 3 and a flux barrier length of 5 mm × 2. The three symmetrical modules of motor_2 are numbered as motor_2m1, motor_2m2, and motor_2m3.
A. Self-Inductance and Mutual Inductance

In this paper, the method mentioned in [24] has been used to calculate the inductance as

\[
L_{aa} = \frac{(\Psi_{aa} - \Psi_m)}{i} \quad \text{(13)}
\]

\[
M_{ba} = \frac{(\Psi_{ba} - \Psi_m)}{i} \quad \text{(14)}
\]

where \(\Psi_{aa}\) is the total excitation flux linkage in phase A produced by the magnet and phase A current, \(\Psi_{ba}\) is the total excitation flux linkage in phase B produced by the magnet and phase A current, \(\Psi_m\) is the magnet flux linkage, \(L_{aa}\) is the self-inductance of phase A, \(M_{ba}\) is the mutual inductance between phase A and phase B, and \(i\) is the applied phase current (dc 8.5 A).

Fig. 11 illustrates the inductance characteristics, where “+8.5 A,” “PM+8.5 A,” and “PM−8.5 A” denote three different excited modes, namely, only armature current without PM excitation, strengthening, and weakening actions of the armature flux (applied +8.5- and −8.5-A phase current to the stator winding) to the PM flux, respectively.

For both motor_1 and motor_2, it is obvious that the inductance variation caused by the rotation of the inner rotor should be similar with a conventional surface-mounted PM synchronous machine. However, the rotation of the outer rotor might cause some special inductance variations. Therefore the following analysis will concentrate on the inductance variation caused by outer rotor rotation.

As shown in Fig. 11(a) and (c), self-inductance and mutual inductance demonstrate an obvious salient pole property. There are two peak values of the inductance waveform during one electrical period.

For motor_2, (13) and (14) should be modified as

\[
L_{aa} = \frac{(\Psi_{aam1} + \Psi_{aam2} + \Psi_{aam3} - \Psi_m)}{i} \quad \text{(15)}
\]

\[
M_{ba} = \frac{(\Psi_{bam1} + \Psi_{bam2} + \Psi_{bam3} - \Psi_m)}{i} \quad \text{(16)}
\]

where \(\Psi_{aam1}, \Psi_{aam2},\) and \(\Psi_{aam3}\) are the total excitation flux linkages in the coils of phase A produced by the magnet and phase A current for one module, and \(\Psi_{bam1}, \Psi_{bam2},\) and \(\Psi_{bam3}\) are the total excitation flux linkages in the coils of phase B produced by the magnet and phase A current for one module.

As shown in Fig. 11(b), although the inductance of each module varies with outer rotor position, the summation of the three modules, i.e., the phase inductance, is almost constant due to the fact that the inductance fluctuations of the three modules cancel each other. This leads to an important conclusion that the complementary structure eliminates the salient pole property of the MGDRM, which means that there is no reluctance torque component. Inductance waveform fluctuation under “PM+8.5 A” and “PM−8.5 A” has also been reduced. The same phenomenon has also appeared in the waveform of mutual inductance as shown in Fig. 11(d).

It should be noted that, in 2-D FEA, the effect of the end turns is neglected, resulting in less calculated inductance than the real one. To account the effect of end turns on inductance,
3-D FEA has been carried out to calculate the $d-q$ inductance. The result will be shown in Section V for comparison with the experimental results.

**B. Torque Production**

As shown in Fig. 7(b), sinusoidal back-EMF implies that the brushless ac operation is an appropriate choice.

The electromagnetic torque with $i_d = 0$ control is obtained and shown in Fig. 12, when the inner rotor speed is 1200 r/min, the outer rotor speed is 1600 r/min, and the stator current is 8.5 A (rms). The torque outputs of motor_1 and motor_2 are compared in Table II.

Obviously, the torque ripple of motor_1 is much higher than that of motor_2, but the average torque of the two motors are almost the same. This phenomenon indicates that the complementary structure could help to reduce torque ripple sharply but without obvious torque reduction, which is a significant merit of the proposed complementary structure as compared with the well-known rotor skewing method.

**V. Experiments**

To verify the aforementioned analysis, a prototype motor based on the aforementioned structure has been manufactured, as shown in Fig. 13. The flux barrier is made of epoxy material which could endure more than 200°C. The modules of the outer rotor are riveted by six stainless bolts.

**A. Back-EMF**

In order to measure the no-load back-EMF under rated situation by the test bench depicted in Fig. 13, an induction motor fed by an inverter is used to drive the outer rotor to 1600 r/min, and the load motor drives the inner rotor to 1200 r/min. The measured back-EMF waveforms are shown in Fig. 14. It can be seen that the three-phase back-EMF waveforms are almost symmetrical. As compared with the simulated waveforms in Fig. 7(b), the amplitude of the back-EMF exhibits the discrepancies between the experimental and simulation results by about 7.2% (92.8% of the 2-D FEA result). This is mainly due to two reasons. First, the end-effect is neglected in the 2-D FEA. Three rotor modules have six ends rather than two ends in traditional motors, introducing more error. Second, there is a little axial misalignment among three components (inner rotor, outer rotor, and stator) due to imperfection in manufacture, resulting in less effective stack length. This result also agrees well with the result in Fig. 6: the PM flux calculated by 3-D FEA (0.08 Wb) is 93% of 2-D FEA (0.086 Wb).
B. Inductance

The $d-q$ inductances are important parameters for establishment of the control strategy. The real prototype motor core is designed to be saturated to some extent, so the inductance may vary with the load condition. With the method introduced in [25], the $d-q$ inductances under different load conditions are measured and compared with the 3-D FEA results in Fig. 15.

Because the test exciter is a sinusoidal ac signal (50 Hz), which magnetizes and demagnetizes the core simultaneously in one period, the average level of core saturation is less impacted by the current amplitude until the test current is near the rated value. However, the simulation current used in FEA is a dc exciter, which induces more saturation of the core, especially when the current is strengthening magnetization, and this leads to a reduction of $L_d$. The discrepancy between the FEA and experiment is mainly due to the imperfect modeling end-winding in 3-D FEA and measurement error.

C. Cogging Torque

Based on the test bench, the inner rotor is latched, the induction motor drives the outer rotor to 50 r/min, and a torque transducer is used to measure the cogging torque of the outer rotor. Fig. 16 shows the cogging torque during one electrical period. It can be seen that the amplitude of the cogging torque is about 0.1 Nm and the frequency of the cogging torque is 12 times that of the back-EMF, agreeing well with the FEA results in Fig. 10(a).

D. Output Torque

In order to measure the output torque of the prototype under the rated condition, the load motor in Fig. 13 is replaced by a brake, of which the load torque is precisely controlled by a dc current excitation.

The induction motor drives the outer rotor to 1600 r/min, and the MGDRM works as a generator, for which $i_d = 0$ control is adopted. The input power from the induction motor is split into two parts: electrical power outputted to the armature windings and mechanical power outputted to the brake.

When the stator current reaches the rated value (rms 8.5 A) at the rated speed (the inner rotor is driven to 1200 r/min), the system reaches its power rating. The outer rotor torque (input torque) is measured by a torque transducer. The measured phase current and torque waveforms are shown in Fig. 17.

As shown in Fig. 17, the average torque is 9.7 Nm. Considering the no-load friction torque of 1.1 Nm, the real outer rotor torque is 8.6 Nm (93% of 2-D FEA result).

VI. Conclusion

In this paper, a novel MGDRM has been proposed for HEV application.
To overcome the problem of asymmetrical phase back-EMF waveforms and high cogging torque in the MGDRM, a complementary structure has been proposed, in which both the outer rotor and the inner rotor are divided into three equal modules shifted by a proper angle with each other along the axis direction. A simplified magnetic circuit has been developed to clarify the principle of the proposed structure. Moreover, for the proposed complementary MGDRM has been carried out, and thus, the flux linkage, back-EMF, cogging torque, and torque production are deduced. Finally, a prototype MGDRM has been designed and fabricated. The back-EMF, inductance, cogging torque, and torque production have been measured, verifying not only the theoretical analysis but also the effectiveness of the proposed complementary MGDRM. From the analytical and experimental results, the following conclusion can be drawn.

1) The complementary structure with three rotor modules shifted by 60 electrical degrees with each other can make the three-phase flux linkage and back-EMF of the machine much more symmetrical and sinusoidal than the ones without complementary structure.

2) The complementary structure can effectively minimize the cogging torque of the machine.

3) Different from the common measures for minimizing cogging torque and harmonic field, such as rotor skewing, which will definitely degrade the effective flux linkage, back-EMF, and thus torque production, the proposed complementary structure has no negative effect on flux linkage and torque.

4) As expected, the complementary MGDRM can split the input power into electrical and mechanical powers, acting the same function as a planetary gear.

Fig. 17. Phase current (5 A/div) and input torque (4 Nm/div) under the rated condition.

The results of this ongoing research initiate a practice for a compact power split device, which is completely electrified. Optimal design of the prototype will go ahead. More complete details of deeper research, for instance, losses, cooling, and control system, will be presented in separate papers.

REFERENCES


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