A Magnetic-Geared Outer-Rotor Permanent-Magnet Brushless Machine for Wind Power Generation

Linni Jian, Student Member, IEEE, K. T. Chau, Senior Member, IEEE, and J. Z. Jiang

Abstract—This paper presents a new permanent-magnet (PM) brushless machine for wind power generation. This machine adopts an outer-rotor topology, aiming at capturing wind power directly. In order to achieve high power density, a high-speed PM brushless generator is artfully integrated with a coaxial magnetic gear. The design details, with emphasis on the special constraints of wind power generation, are elaborated. By using the time-stepping finite element method, the static characteristics as well as no-load and on-load operations are simulated. A prototype is also built for experimentation. Both simulation and experimental results are given to verify the validity of the proposed machine. Finally, a quantitative comparison is made to justify that the proposed machine is of smaller size, lighter weight, and lower cost than its counterparts.

Index Terms—Finite-element method (FEM), magnetic gear, outer rotor, permanent-magnet (PM) machine, wind power generation.

I. INTRODUCTION

WIND POWER as an abundant clean renewable energy resource has attracted increasing attention for solving problems arising from energy crisis and environmental pollution [1]. Wind power generation is a major method of utilizing this natural resource. Generally, it can be classified as constant-speed constant-frequency (CSCF) wind power generation and variable-speed constant-frequency (VSCF) wind power generation. In the CSCF system, the squirrel-cage induction generator is usually adopted [2]. It offers several advantages, such as a simple structure and high robustness. Furthermore, it can directly connect to the power grid without using any power converters. However, since the turbine speed is kept constant regardless of the variation of the wind speed, the CSCF system suffers from very low efficiency and high mechanical stress.

With the development of power electronics, low-cost power converters make it possible to produce constant-frequency electric power with a variable turbine speed. Since the turbine speed changes with the wind speed to capture the maximum wind power, the efficiency of the VSCF system is much higher. For the VSCF system, several types of generators have been adopted or proposed, such as the doubly fed induction machine [3], switched reluctance machine [4], doubly salient permanent-magnet (PM) machine [5], PM hybrid machine [6], and double-stator PM machine [7]. However, mechanical gears are generally engaged to match the low-speed operation of the wind turbine and the relatively high-speed operation of the generator. This not only increases the cost of manufacture and maintenance but also reduces the efficiency and robustness.

In order to get rid of the nuisances arising from mechanical gears, the direct-drive PM machine (DDM) has been proposed for wind power generation [8]. Since the machine has to operate at low speeds, it needs to have a bulky size with a very large number of poles. Therefore, it is a tradeoff between the elimination of mechanical gears and the increase of machine size. Sometimes, the use of mechanical gears plus a high-speed machine can have the reduction of overall size and weight as compared with the low-speed direct-drive machine.

Recently, the concept of magnetic gears has been proposed [9], [10]. Because of physical isolation between the input and output shafts, it offers some distinct advantages: namely, minimum acoustic noise, freedom from maintenance, improved reliability, and inherent overload protection. By adopting the coaxial topology, the utilization of PMs can be greatly improved, leading to the offering of a torque density comparable with that of the mechanical gear. Very recently, it has been integrated into a PM motor to offer high-torque low-speed operation for electric vehicles [11].

The purpose of this paper is to propose and implement a magnetic-geared outer-rotor PM brushless machine for wind power generation, which incorporates the attractive features of both the outer-rotor PM generator and the magnetic gear. In Section II, the configuration of a stand-alone wind power generation system and the machine design details will be introduced. Section III will be devoted to using finite element analysis to deduce the static characteristics of the proposed machine. In Section IV, the mathematical modeling of the proposed machine will be presented. Then, simulation and experimental results will be given to verify the validity of the proposed machine in Sections V and VI, respectively. Consequently, a quantitative comparison between the proposed machine and its counterparts will be made in Section VII. Finally, conclusions will be drawn in Section VIII.

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1 http://www.nrel.gov/docs/fy05osti/37602.pdf

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L. Jian is with the Department of Electrical and Electronic Engineering, The University of Hong Kong, Hong Kong (e-mail: lijian@eee.hku.hk).

K. T. Chau is with the Department of Electrical and Electronic Engineering, The University of Hong Kong, Hong Kong (e-mail: ktchau@eee.hku.hk).

J. Z. Jiang is with the Department of Automation, Shanghai University, Shanghai 200030, China (e-mail: jzhijiang@mail.shu.edu.cn).

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II. SYSTEM DESIGN

A. System Configuration

Fig. 1 shows the configuration of a stand-alone wind power generation system in remote areas. It consists of the proposed PM brushless machine to directly capture wind power, a three-phase bridge rectifier to perform simple ac–dc conversion, a dc–dc converter to regulate the rectified dc voltage, a battery pack for energy storage, and an inverter to perform simple dc–ac conversion.

The detailed configuration of the proposed machine for wind power generation is shown in Fig. 2. The coaxial magnetic gear is artfully integrated into the PM generator such that they can share the same high-speed rotor (namely, the outer rotor of the generator and the inner rotor of the magnetic gear). This common rotor is designed like a cup with PMs mounted on its inside and outside surfaces. The magnetic gear employs PMs on both the inner and outer rotors and has a stationary ring between the two rotors. In order to provide magnetic paths while reducing iron losses, both the stationary ring and the iron yokes of the two rotors are built of laminated ferromagnetic materials. These laminated materials are based on cold-rolled silicon steel (type BW315) with a thickness of 0.35 mm. Moreover, epoxy is filled in the slots of the stationary ring to enforce the structural strength for high torque transmission. For capturing wind power directly, the wind blades are mounted on the gear outer rotor. Fig. 3 shows the mechanical assembly of the proposed machine, in which three bearings are employed to guarantee free rotations of both rotors.

B. Machine Design

The operation of the magnetic gear relies on the use of a stationary ring to modulate the magnetic fields [9]. By defining $p_1$ and $p_2$ as the pole-pair numbers of the outer and inner rotors, respectively, and $n_s$ as the number of ferromagnetic pole pieces on the stationary ring, it yields

$$n_s = p_1 + p_2.$$  

(1)

The corresponding speed relationship can be expressed as

$$\omega_2 = -G_r\omega_1$$  

(2)

$$G_r = \frac{p_1}{p_2}$$  

(3)

where $\omega_1$ and $\omega_2$ are the rotational speeds of the outer and inner rotors, respectively, and $G_r$ is the so-called gear ratio. The minus sign indicates that the two rotors rotate in opposite directions.

Equations (1)–(3) indicate that the gear ratio is determined by the pole-pair numbers of both rotors. Thus, we should choose proper pole-pair numbers according to the wind-turbine and generator speeds. Based on the Betz theory [12], the mechanical power extracted from wind power by the wind turbine can be expressed as

$$P_{\text{mech}} = \frac{1}{2}C_p\rho v_w^3 A$$  

(4)

where $\rho$ is the air density, $v_w$ is the wind speed, $A$ is the swept area of the wind-turbine rotor, and $C_p$ is the power conversion efficiency factor. This $C_p$ is a function of the tip speed ratio $\beta$, which is defined as

$$\beta = \frac{\omega R}{v_w}$$  

(5)

where $R$ is the radius of the blades and $\omega$ is the rotational speed of the wind-turbine shaft. For the proposed machine, it equals the gear outer-rotor speed $\omega_1$. The characteristic of $C_p = f(\beta)$ is approximately parabolic. When $\beta$ takes the specific value $\beta_{\text{opt}}$, the maximum power conversion efficiency factor $C_{p,\text{max}}$ occurs. In order to extract the maximum mechanical power, $\omega_1$ should vary with the wind speed. Fig. 4 shows the mechanical power characteristics of a wind turbine under different wind...
speeds. The maximum power curve indicated by the dotted line can be expressed as

$$P_{\text{mech max}} = \frac{1}{2} \left( \frac{C_{p,\text{max}} R^3 \rho}{\beta_{\text{max}}^3} \right) \omega_1^3.$$  (6)

Thus, the rated outer-rotor speed can be determined by the wind speed according to the power curve. For an average wind speed of 7 m/s in some areas, from Fig. 4, $\omega_1$ should be rated at 136 r/min. Meanwhile, the inner-rotor speed $\omega_2$ is rated at 1000 r/min for producing 50 Hz of electricity. By choosing $p_1$, $p_2$, and $n_s$ as 3, 22, and 25, respectively, the gear ratio of 7.33 results so as to match the rated speeds of the two rotors.

Considering the power generation under gentle breeze, the reduction of the starting torque is another design goal. In PM machines, cogging torque is a major factor which results in a high starting torque and deteriorates the low-speed operation. To reduce this cogging torque, the skewed slot design is usually adopted. However, it degrades the generated output power and complicates the manufacturing process [5]. Therefore, the proposed machine does not adopt the use of skewing.

Generally, the fundamental order of the cogging torque is the smallest common multiple between the stator slot number and the rotor pole number. The higher the fundamental order, the lower the resulting harmonic magnitude. Two machines with exactly the same size but different numbers of stator slots equal to 9 and 27 are selected for analysis. Since the inner-rotor pole number is six, the fundamental order of the cogging torque existing in the former machine is 18, and that in the latter machine is 54. Fig. 5 shows the cogging torque waveforms calculated by the finite element method (FEM). The magnitude of the cogging torque in the machine with 27 stator slots is about 0.7 N·m which is much less than 2 N·m in the machine with nine stator slots. The use of more slots is helpful to reduce cogging torque; however, it leads to the increase of the manufacturing complexity. To make a compromise, the use of 27 stator slots is adopted to reduce the cogging torque.

Fig. 6 shows the stator winding connection of the proposed machine. The three-phase symmetric windings consist of 27 double-layer coils. Each coil span covers five slot pitches, while the pole pitch is 9/2 of the slot pitch.

### III. Finite-Element Analysis

#### A. Magnetic Field Distributions

The specifications of the proposed machine are listed in Table I. Fig. 7 shows the magnetic field distributions of the
proposed machine under no-load and full-load. It can be observed that a large portion of flux lines can pass through all three air gaps. These flux lines dictate the torque transmission and power conversion. There are also some flux lines directly turning around the boundaries of adjacent PMs. These flux lines are useless for transmitting torque or power and arouse power loss. Fig. 8 shows the radial flux density waveforms in the inner, middle, and outer air gaps at no-load. The radial flux density waveforms at full-load are almost the same with that at no-load, and their little differences are shown in Fig. 9. It demonstrates that the armature reaction just has a slight effect on the magnetic gear. This is due to the airlike permeability of the surface-mounted PMs which significantly increase the reluctance of the armature flux path.

Moreover, Fig. 10 shows the radial flux density waveform and its harmonic spectrum in the outer air gap produced by the inner rotor alone. It can be seen that the highest asynchronous harmonic component is that with 22 pole pairs. On the other hand, Fig. 11 shows the radial flux density waveform and its harmonic spectrum in the middle air gap produced by the outer rotor alone. It can be found that the highest asynchronous harmonic component is that with three pole pairs. Therefore, it confirms that the stationary ring can modulate the flux density from 3 to 22 pole pairs.
Fig. 10. Radial flux density in outer air gap produced by inner rotor. (a) Flux density waveform. (b) Harmonic spectrum.

Fig. 11. Radial flux density in middle air gap produced by outer rotor. (a) Flux density waveform. (b) Harmonic spectrum.

B. Torque Transmission

By calculating the Maxwell’s stress tensors in the outer and middle air gap, the torque transmission capability of the magnetic gear can be determined. While keeping the outer rotor at a standstill, the inner rotor is rotated step by step. The corresponding torque-angle curves are calculated as shown in Fig. 12. It can be found that the torque-angle curves vary sinusoidally, in which the maximum torque values denote the pull-out torques. On the inner and outer rotors, the pull-out torques are 14.2 and 103.4 N·m, respectively. Their ratio is 7.28, which has a good agreement with the ratio of the pole-pair numbers of two rotors equal to 7.33. According to the machine size listed in Table I, the overall torque density is about 87 kN·m/m³, which is much higher than that of a standard electric machine with only about 10 kN·m/m³. There is a difference between the phase angles of two torque waveforms. This implies that the two rotors rotate in opposite directions, which is in accordance with (2).

IV. MATHEMATICAL MODELING

The back electromotive force (EMF) induced in the stator windings can be expressed as

\[ E = -\frac{d\Psi}{dt} = U_o + R_s I_s \]  

(7)

where \( \Psi \) is the flux linkage in the inner air gap, \( U_o \) and \( I_s \) are the output voltage and current, respectively, and \( R_s \) is the stator resistance. The flux linkage \( \Psi \) is given by

\[ \Psi = \Psi_{pm} + \Psi_s = \Psi_{pm} + L_s I_s \]  

(8)

where \( \Psi_{pm} \) is the PM excited flux linkage, \( \Psi_s \) is the flux linkage due to the armature reaction, and \( L_s \) is the stator reactance. Ignoring the nonlinear factors in the magnetic path, it yields

\[ \Psi_{pm} = \Psi_{pm1} + \Psi_{pm2} \]  

(9)

where \( \Psi_{pm1} \) and \( \Psi_{pm2} \) are the flux linkages in the inner air gap produced by the outer- and inner-rotor PMs, respectively. Therefore, the voltage equation is given by

\[ U_o = -\frac{d\Psi_{pm1}}{dt} - \frac{d\Psi_{pm2}}{dt} - L_s \frac{dI_s}{dt} - I_s \frac{dL_s}{d\theta} \omega_2 - R_s I_s \]  

\[ \approx E_1 + E_2 - jI_s p_2 \omega_2 L_s - R_s I_s \]  

(10)

where the variation of \( L_s \) with respect to the rotor position angle \( \theta \) is negligible because of the nonsalient due to the surface-mounted PMs. The corresponding equivalent circuit and phasor diagram are shown in Figs. 13 and 14, respectively.
V. SIMULATION RESULTS

In order to assess the proposed machine, a computer simulation based on time-stepping FEM is conducted. The simulation model consists of two equations: the finite element equation of the electromagnetic field of the proposed machine and the circuit equation of the armature windings [13]. The 2-D electromagnetic equation is governed by

\[ \Omega : \frac{\partial}{\partial x} \left( v \frac{\partial A}{\partial x} \right) + \frac{\partial}{\partial y} \left( v \frac{\partial A}{\partial y} \right) = -J - v \left( \frac{\partial B_{rx}}{\partial x} - \frac{\partial B_{ry}}{\partial y} \right) + \sigma \frac{\partial A}{\partial t} \]

\[ s_1 : A = 0 \quad (11) \]

where \( \Omega \) is the region of calculation, \( A \) is the magnetic vector potential component along the \( z \)-axis, \( J \) is the current density, \( v \) is the reluctivity, \( \sigma \) is the electrical conductivity, \( B_{rx} \) and \( B_{ry} \) are the remnant flux density components of the PM along the \( x \)- and \( y \)-axes, and \( s_1 \) is the boundary of the region of calculation.

According to the equivalent circuit shown in Fig. 13, the armature circuit equation is given by

\[ (R_s + R_L)i_s + (L_s + L_L)\frac{di_s}{dt} - \frac{l}{S} \int_{\Omega} \frac{\partial A}{\partial t} d\Omega = 0 \quad (12) \]

where \( R_L \) is the load resistance, \( L_L \) is the load inductance, \( l \) is the axial length of the iron core, and \( S \) is the conductor area of each turn of phase winding.

Fig. 15 shows the output phase voltages at no-load and with a resistive load of 300 \( \Omega \), when the outer rotor rotates at the rated speed of 136 r/min.

VI. EXPERIMENTAL RESULTS

As shown in Fig. 16, the proposed machine is prototyped for experimentation. First, by driving the outer rotor at the rated speed of 136 r/min, the measured speed at the inner rotor is 993 r/min, verifying that the speed reduction ratio is 7.3 which agrees with the theoretical \( G_r = 7.33 \). Then, the corresponding no-load EMF waveform and the output-voltage waveform with a resistive load of 300 \( \Omega \) are measured, as shown in Fig. 17. It can be seen that there is a good agreement with the simulation waveforms, as shown in Fig. 15. Furthermore, Fig. 18 shows a comparison of the measured and simulated amplitudes of
the no-load EMF per phase at different outer-rotor speeds. As expected, they are in good agreement, and the voltage is linearly proportional to the rotational speed.

Moreover, the efficiency of the proposed machine is measured at different load currents and outer-rotor speeds. As shown in Fig. 19, the efficiency at the rated speed of 136 r/min and the rated power of 500 W is about 72%, whereas the maximum efficiency is about 80% which occurs at 150 r/min.

In order to figure out the reasons for a relatively low efficiency of the prototype machine, a breakdown of all losses is estimated. First, the no-load loss, which combines the iron and mechanical losses, is directly measured at the rated speed. It is found to be 174 W at 136 r/min. Second, the corresponding iron loss is calculated by using the FEM. It is estimated to be 36.7 W. Hence, the corresponding mechanical loss can be deduced as 137.3 W. Such high mechanical loss should be due to friction resulting from manufacturing imperfections. Third, the copper loss at the rated power of 500 W can be easily calculated, namely, 18.5 W. Thus, the breakdown of all losses of the machine operating at the rated power and rated speed is 9.6% in copper loss, 19.1% in iron loss, and 71.3% in mechanical loss. Based on this loss analysis, it can be realized that the major loss is due to the mechanical loss which will be less significant when the power level of the machine is elevated.

It should be noted that the prototype generator is mainly used for experimental verification. Its power level should be elevated to about 50 kW so as to justify the use of magnetic gearing with the rated rotational speed of 136 r/min.

VII. COMPARISONS

To further demonstrate the merits of the proposed magnetic-gearPM machine (MGM), a quantitative comparison with its traditional counterparts, namely, the DDM and the planetary-gearPM machine (PGM), is conducted. For a fair comparison, the machine ratings are unified as listed in Table II. Furthermore, all these machines adopt the outer-rotor topology and three-phase star-connected stator windings.

TABLE II

<table>
<thead>
<tr>
<th>Machine ratings for comparison</th>
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<tr>
<td>Rated wind-turbine speed</td>
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<td>Rated phase voltage</td>
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<tr>
<td>Rated power</td>
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<td>Rated frequency</td>
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A. Overall Size

It is formidable to make a precise assessment on the sizes of different types of machines. Nevertheless, a rough comparison can be achieved based on the following sizing equation [14]:

$$D_i^2 l = \frac{4P_r}{k_e k_i k_p B_{max} A\omega_r} \cdot (13)$$

$$A = \frac{2mNI_{rms}}{\pi D_i} \cdot (14)$$

where $D_i$ is the diameter of the air gap (inner air gap for the MGM), $l$ is the effective axial length, $P_r$ is the rated power, $k_e$ is the EMF waveform factor, $k_i$ is the current waveform factor, $k_p$ is the power waveform factor, $\omega_r$ is the rated rotor speed (inner-rotor speed for the MGM), $B_{max}$ is the maximum flux density in the air gap (inner air gap for the MGM), $A$ is the electric loading, $m$ is the number of phases, $N$ is the number
of turns of the phase winding, and $I_{\text{rms}}$ is the rms value of the phase current.

Since the rectangular waveforms can produce a higher output power than the sinusoidal ones, the waveform factors are selected as $k_e = \pi$, $k_i = 1$, and $k_p = 1$ for all three types of machines. Based on engineering practice, $A$ is selected as 15 000 A/m for all of them. Then, $B_{\text{max}}$ is selected as 1.1 T for the DDM and PGM, whereas $B_{\text{max}}$ is set as 0.85 T for the MGM since it involves a relatively long magnetic path. As desired, $\omega_r$ is 1000 r/min for the MGM and PGM, whereas $\omega_r$ is 136 r/min for the DDM. Since the ratio $\lambda = I/\delta_i$ is governed by $\omega_r$, $\lambda$ is selected as 0.33 for the MGM and PGM, whereas $\lambda$ is set as 0.15 for the DDM. By substituting all these values into (13), it can be deduced that the $D_i$’s of the MGM, DDM, and PGM are 30.7, 71.2, and 28.2 cm, respectively, and the corresponding $l$’s are 10.1, 10.7, and 9.3 cm, respectively.

Based on the previous design of the proposed MGM, the size of the magnetic gear outer part of the MGM can be estimated as 20 101 cm$^3$. Therefore, the overall size of the MGM is 27 577 cm$^3$. In the outer rotor of the DDM and PGM, the PM thickness is chosen as 0.8 cm which is sufficient to excite the desired air-gap flux density. To avoid unnecessary saturation, the thickness of the iron yoke is chosen as 2.0 cm for the DDM and 1.0 cm for the PGM. Thus, the size of the DDM can be deduced as 49 567 cm$^3$, and that of the PGM (without the planetary gear) is 7386 cm$^3$. Taking into account the size of the planetary gear, which is 24 389 cm$^3$ for the rating of 10 kW and the ratio of 1000 to 136 r/min, the overall size of the PGM is 31 775 cm$^3$.

B. Overall Weight

Based on the sizes of the different materials used in the machine, namely, the copper windings, iron cores, and PMs, the weights of these materials and, hence, the overall weight of the machine can be calculated. For the MGM, the required copper-winding material is 292 cm$^3$, the iron-core material is 15 521 cm$^3$, and the PM material is 1715 cm$^3$. Since the densities of the copper-winding, iron-core, and PM materials are 9.0, 7.8, and 7.6 g/cm$^3$, respectively, the overall weight can be calculated as 137 kg. Similarly, for the DDM, the required copper-winding material is 718 cm$^3$, the iron-core material is 42 179 cm$^3$, and the PM material is 1936 cm$^3$. Therefore, based on their densities, the overall weight can be calculated as 350 kg.

For the PGM (without the planetary gear), the required copper-winding material is 247 cm$^3$, the iron-core material is 5816 cm$^3$, and the PM material is 678 cm$^3$. Therefore, based on the corresponding densities, the weight of the PGM (without the planetary gear) can be calculated as 53 kg. Taking into account the weight of the planetary gear, which is made of steel with the density of 7.9 g/cm$^3$, the overall weight of the PGM can be obtained as 138 kg.

C. Material Cost

It is hardly possible to accurately compare the overall cost of the three machines, unless they are under mass production, since the assembly cost heavily depends on the maturity and complexity of those machines. Therefore, in this stage, the cost comparison of these three machines is simply focused on the material cost only.

According to the indicative prices of those materials used in the three machines, namely, $11.2/kg for copper windings, $1.22/kg for iron cores, $45.7/kg for PMs, and $2.43/kg for the steel planetary gear, the material costs can readily be deduced from their weights. Therefore, the overall material costs of the MGM, DDM, and PGM are $769, $1141, and $396, respectively.

D. Discussion

The estimated data of the three types of machines, namely, the overall size, overall weight, and material cost, are summarized in Table III for direct comparison. It can be observed that the proposed MGM takes the definite advantage of the smallest size and lightest weight as compared with the DDM and PGM. In terms of material cost, the MGM is more costly than the PGM, but still much cheaper than the DDM. The additional cost of the MGM over that of the PGM is well justified and outweighed by the significant reduction in size and the aforementioned advantages of the magnetic gear. Therefore, the proposed MGM is highly competitive for wind power generation.

It should be noted that the proposed generator and its prototype are mainly used to illustrate the concept of magnetic gearing for wind power generation. The corresponding equations given by (4)–(6), the comparisons listed in Tables II and III, as well as the illustrations shown in Figs. 1 and 4 are used to describe general wind-turbine applications.

VIII. Conclusion

In this paper, a new magnetic-geared PM brushless machine, which is particularly attractive for wind power generation, has been designed, analyzed, and implemented. Compared with the traditional wind power generators, the proposed machine offers some distinct advantages. First, the low-speed outer-rotor topology can enable direct coupling with the wind blades to capture wind power with high efficiency. Second, the integration of a coaxial magnetic gear can enable the PM brushless generator to be designed for high-speed operation, hence achieving high power density. Third, the use of the magnetic gear can provide physical isolation between the inner rotor and the outer rotor, thus minimizing the maintenance cost and the acoustic noise. Finally, a quantitative comparison has illustrated that the proposed machine is of smaller size and lighter weight than both the direct-drive PM brushless machine and the planetary-geared
PM brushless machine, with also lower material cost than the direct-drive one.

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J. Z. Jiang received the B.E.E. and M.E.E. degrees from Shanghai Jiao Tong University, Shanghai, China, in 1962 and 1965, respectively, and the Dr. Ing. degree from the Technical University of Braunschweig, Braunschweig, Germany, in 1988. He is currently a Professor with the Department of Automation, Shanghai University, Shanghai, China. His research interests are in high-performance variable-speed drives, electric machine design, electric vehicles, and wind power generation.

K. T. Chau (M’89–SM’04) received the first-class honors B.Sc.(Eng.), M.Phil., and Ph.D. degrees in electrical and electronic engineering from The University of Hong Kong, Hong Kong, in 1988, 1991, and 1993, respectively. He is currently a Professor with the Department of Electrical and Electronic Engineering and the Director of the International Research Center for Electric Vehicles, The University of Hong Kong. His teaching and research interests focus on three main areas: electric vehicles, electric drives, and power electronics. In these areas, he has published over 200 refereed technical papers. He is also the coauthor of a monograph, Modern Electric Vehicle Technology (Oxford University Press, 2001).

Prof. Chau is a Fellow of the Institution of Engineering and Technology. He was the recipient of the Outstanding Young Researcher Award from The University of Hong Kong in 2003, the University Teaching Fellowship Award from The University of Hong Kong in 2004, and the Award for Innovative Excellence in Teaching, Learning and Technology at the International Conference on College Teaching and Learning in 2005.

Linni Jian (S’07) received the B.Eng. degree from Huazhong University of Science and Technology, Wuhan, China, in 2003, and the M.Eng. degree from the Institute of Electrical Engineering, Chinese Academy of Science, Beijing, China, in 2006. Currently, he is working toward the Ph.D. degree in electrical and electronic engineering at The University of Hong Kong, Hong Kong.

His research interests are in the areas of electric drives, electric vehicles, and power electronics. He currently focuses on the design of magnetic gears and integrated permanent-magnet machines, as well as high-efficiency machine control strategies.

He was the recipient of the Outstanding Young Researcher Award from The University of Hong Kong in 2003, the University Teaching Fellowship Award from The University of Hong Kong in 2004, and the Award for Innovative Excellence in Teaching, Learning and Technology at the International Conference on College Teaching and Learning in 2005.