

# Performance Comparison Between Surface-Mounted and Interior PM Motor Drives for Electric Vehicle Application

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**Abstract**—Electric vehicles make use of permanent-magnet (PM) synchronous traction motors for their high torque density and efficiency. A comparison between interior PM and surface-mounted PM (SPM) motors is carried out, in terms of performance at given inverter ratings. The results of the analysis, based on a simplified analytical model and confirmed by finite element (FE) analysis, show that the two motors have similar rated power but that the SPM motor has barely no overload capability, independently of the available inverter current. Moreover, the loss behavior of the two motors is rather different in the various operating ranges with the SPM one better at low speed due to short end connections but penalized at high speed by the need of a significant deexcitation current. The analysis is validated through FE simulation of two actual motor designs.

**Index Terms**—Constant-power speed range, electric vehicle (EV), high-speed ac drives, iron loss, permanent-magnet (PM) motor drives, PM synchronous motors.

## I. INTRODUCTION

THE diffusion of electric vehicles (EVs) in the urban context is only a matter of time, since adoption of zero emission vehicles, either powered by chemical accumulators or fuel cells, is becoming mandatory, for the well-known reasons. Indeed, this adoption will be gradual, also for the related need of infrastructures, but it is already accepted that EVs will represent the solution for urban mobility in the next future.

On the other hand, the choice of the electric drive train most suited to this application is still a matter of discussions. The most used electric motors in this sense are, up to now, induction motors and permanent-magnet (PM) synchronous motors [1]. The former are adopted for their ruggedness and availability, while the latter are generally chosen for their higher torque density and efficiency.

Among PM motors, surface-mounted (SPM) and interior PM (IPM) types are both considered [1], [2], and an exhaustive comparison between the respective performances has not been

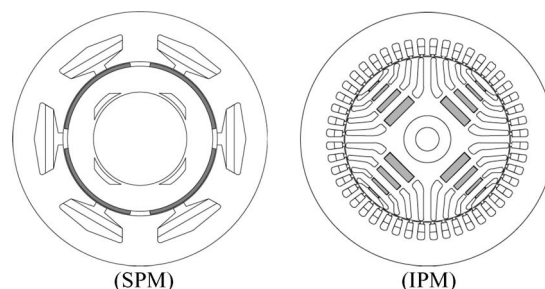


Fig. 1. Example motors having the same continuous torque, power, and current ( $P_1$ ,  $T_1$ , and  $i_1$ ). The two motors have the same stator diameter and stack length.

made yet. In [3], a thorough comparison is carried out for a starter generator. Most of the more recent research in this field has been devoted to motors with nonoverlapping windings, either with SPMs [4], [5] or IPMs [6], [7], although such IPM machines are often very similar to SPM ones for magnet layout and for having rather a low saliency. Moreover, it is recognized that concentrated windings reduce the saliency of any IPM machine and, thus, part of their specific advantages that are related to the reluctance torque component [8]. For these reasons, this paper focuses on the comparison between an SPM machine with concentrated windings and an IPM machine with distributed windings and four flux barriers per pole. As evidenced by Fig. 1, the selected machine types are at the opposite ends of the spectrum in terms of manufacturing complexity. Most of other combinations such as simpler IPM rotors and stators, including fractional slots ones, stay in between the two considered here.

This paper follows the work presented in [9] where the SPM and IPM motor drives have been compared at a given vehicle specification and inverter size. It was shown that the following characteristics are observed.

- 1) Continuous power of SPM and IPM motors is practically the same.
- 2) SPM motor is easier to manufacture and has shorter end connections and then a bit shorter overall length.
- 3) IPM motor has a very good overload capability, over the entire speed range, while the SPM motor has none, independently of the applied current.
- 4) High-speed losses affect both the motors, due to PM losses for SPM and slot harmonic losses for IPM.

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At high speed, the power factor tends to be one, since the flux and current vectors tend to be in quadrature to each other (Fig. 3, vectors in position  $C_1$ ). Thus, the power asymptotically tends to the limit value

$$P_{\text{lim}} = \frac{3}{2} \cdot V \cdot i_1 \quad (2)$$

where  $V$  is the maximum phase voltage amplitude. In Fig. 4, the per-unit (p.u.) continuous power is plotted: If power losses are disregarded, the output power curve has nearly the shape of the power factor. Therefore, at base speed (point  $A_1$ ), the p.u. power is nearly 0.7 (Fig. 4), which is also the power factor (the current leads the flux vector by  $45^\circ$  in Fig. 3).

In Fig. 3,  $\lambda_{\min}$  is representative of the flux amplitude to be respected at maximum speed to cope with the voltage limit

$$\lambda_{\min} = \frac{V}{\omega_{\max}}. \quad (3)$$

$\lambda_{\min}$  is given by (3), where the resistive drop has been disregarded. Because of the voltage limit, also at partial load and no-load, the flux amplitude must be limited at high speed, by means of a demagnetizing current. In particular, the worst case demagnetizing current  $i_{10}$  is shown in Fig. 3, and its amplitude is

$$i_{10} = i_1 \cdot \left(1 - \frac{\lambda_{\min}}{\lambda_m}\right). \quad (4)$$

The need for some flux-weakening current at light and no-load is a general drawback of this kind of motors, because it implies more copper losses. Most of the time, the drive is at partial load in the speed range above the base speed. In such cases, only a small part of the motor current is actually giving torque while *most of Joule losses are spent just for flux weakening*, as will be evidenced in Section V.

As said,  $P_1 = P_{\text{lim}}$  is the continuous power, determined by the maximum vehicle speed specification. Thus, the motor rated current  $i_1$  must match the power dissipation allowed by the motor cooling, while the PM flux  $\lambda_m$  must be maximized for obtaining an optimal torque-to-current ratio. As a consequence, the only parameter left to satisfy (1) is the motor inductance  $L_{eq}$ , which must be properly designed at that aim. This generally implies the adoption of a fractional number of slots per pole per phase, as it will be discussed in Section IV [5].

### B. Power Curve at Overload Current $i_0$

In Fig. 5, the vector diagram at overload is shown, with reference to a current  $i_0$  that is 173% of the continuous current  $i_1$  (i.e., three times the Joule losses). At low speed, the power factor is quite low and the voltage limit is met very soon because of the larger flux amplitude. From the constant-torque working point ( $A_0$  in Fig. 5), the current is rotated until the flux vector is aligned to the  $q$ -axis ( $B_0$  in Fig. 5), which represents the maximum torque per voltage (MTPV) flux condition [11]. To increase the speed further, the flux is kept along the  $q$ -axis

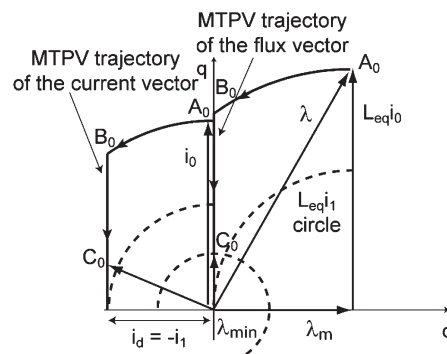


Fig. 5. Vector diagram of the SPM motor at overload current  $i_0$ .

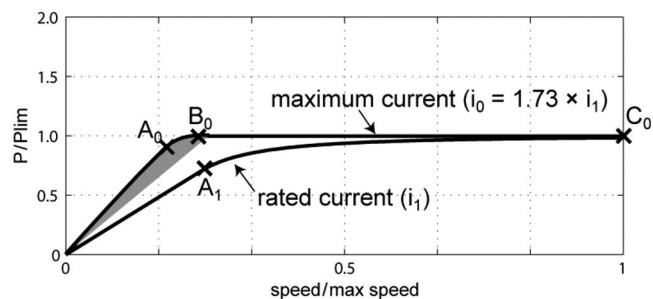


Fig. 6. Per-unit power versus speed of the SPM motor at rated current ( $i_1$ ) and 173% overload current ( $i_0$ ).

by reducing the  $i_q$  component only, with the  $i_d$  current equal to the characteristic current  $i_1$  (1).

The MTPV flux amplitude (5) is proportional to the torque current component  $i_q$

$$\lambda_{\text{MTPV}} \equiv \lambda_q = L_{eq} \cdot i_{q,\text{MTPV}} \quad (5)$$

that is reduced proportionally to the speed (6) because of the constant voltage  $V$

$$i_{q,\text{MTPV}} = \frac{\lambda_{\text{MTPV}}}{L_{eq}} = \frac{V}{\omega L_{eq}}. \quad (6)$$

As for the  $q$ -current, the torque also varies inversely with speed (7), and consequently, the power results to be constant with speed (8) and equal to the  $P_{\text{lim}}$  value (2)

$$T_{\text{MTPV}} = \frac{3}{2}p \cdot \lambda_m \cdot i_{q,\text{MTPV}} = \frac{3}{2}p \cdot \frac{\lambda_m}{L_{eq}} \cdot \frac{V}{\omega} \quad (7)$$

$$P_{\text{MTPV}} = \frac{3}{2} \cdot \frac{\lambda_m}{L_{eg}} \cdot V = \frac{3}{2} \cdot V \cdot i_1 = P_{\text{lim}}. \quad (8)$$

The condition (1) has been substituted in (8).

Once the MTPV limit is reached, the output power is clamped to  $P_{\text{lim}}$  according to (8), independently of the available current overload. In other words, the 173% overload current  $i_0$  produces an overload torque below the base speed, as shown in Fig. 6, but the power overload vanishes as the speed increases beyond that point.

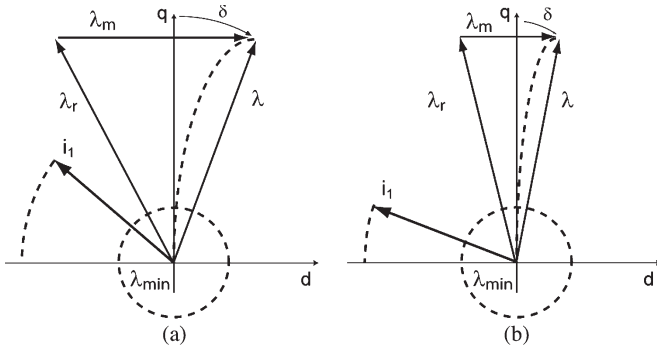


Fig. 7. Vector diagrams of two IPM motors at rated current ( $i_1$ ), in the respective MTPA conditions. The dashed curves are the trajectories of the flux and current vectors in flux weakening. (a) Motor with low anisotropy. (b) Motor with high anisotropy.

### III. IPM MOTOR DRIVE

#### A. Power Curve at Rated Current $i_1$

The torque of the IPM motor shows both PM flux and anisotropy terms

$$T = \frac{3}{2}p \cdot [\lambda_m \cdot i_q - L_d(\xi - 1) \cdot i_d \cdot i_q] \quad (9)$$

where  $\xi = L_q/L_d$  is the saliency ratio. For MTPA operation,  $i_d$  is negative (Fig. 7) with an MTPA phase angle that varies from motor to motor. For flux weakening, the current vector is rotated from the MTPA angle toward the MTPV locus, if any. As for the SPM, when the relationship (10) is true, the flux vector at rated current is driven toward zero and the MTPV locus is not met (Fig. 7) while it is met at overload current

$$\lambda_m = \frac{L_q}{\xi} \cdot i_1. \quad (10)$$

Since  $L_q$  mainly depends on the air-gap length, the  $\lambda_m$  value that fulfills (10) depends on the rotor anisotropy: The larger the anisotropy is, the lower the  $\lambda_m$  is. In the SPM case, there is a unique inductance value that depends on the stator design (type of winding, internal diameter, and slot shape) and the magnet thickness, while, here, the  $d$ -axis inductance can be varied by means of  $\xi$  that relies basically on the rotor design. If  $\xi$  is maximized, then the PM flux needed to match (10) can be reduced with no loss of torque due to the increase of the reluctance torque in (9). The vector diagrams of two IPM motors designed according to (10), with the same flux and current but different saliencies, are schematically shown in Fig. 7. The  $\lambda_r$  flux (where  $r$  stands for *reluctance*) is the one produced by the stator current.

As shown in the following, both the IPM machines can have the same rated torque of the SPM machine of the same size. At high speed, as the vectors go through the respective flux-weakening trajectories, shown as dashed curves in Fig. 7, the angle between the current and flux vectors always tends to  $90^\circ$ , as it was for the SPM motor in Fig. 3. Moreover, if the SPM and IPM machines are designed for satisfying (1) and (10), respectively, and for having the same rated flux and current, they reach the same high-speed asymptotic power [11].

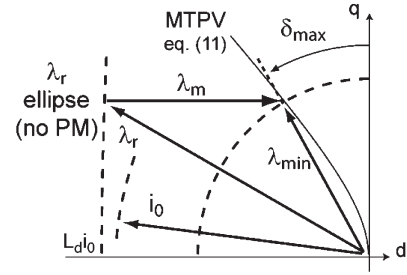


Fig. 8. IPM motor flux diagram at maximum speed and overload current  $i_0$ : The MTPV locus is supposed to be met exactly at maximum speed and maximum current.

The power curve of the two IPM motors at rated current is shown in Fig. 10 (dashed), and it is identical to the one reported in Figs. 4 and 6 for the SPM motor. For Figs. 4, 6, and 10,  $P_{lim}$  is the same. On the other hand, the low saliency IPM machine [Fig. 7(a)] has a  $\lambda_m$  flux that is comparable, in p.u. of the rated flux, to the one of the SPM machine. Also, the related side effects are then comparable, namely, the overvoltage in case of uncontrolled generator operation [12]. Instead, the high anisotropy motor [Fig. 7(b)] has much lower p.u. PM flux and side effects. In addition, the *design with high saliency and low  $\lambda_m$  improves the overload capability at large speed*, as shown in the following.

#### B. Overload Current $i_0$ and Possible Design Choices

The same current overload ratio  $i_0 = 1.73 i_1$  is considered for the IPM motor drive. The MTPV power is no longer clamped to an upper limit value (8), as it was for the SPM motor: Nevertheless, the power curve in the MTPV region tends to drop with speed. For possibly having a flat power curve at overload, it is convenient to design the IPM machine such that the MTPV zone is **encountered exactly at the maximum speed and overload current**, as it was proposed in [13]. Under such assumption, the flux vector diagram at maximum speed and overload current conditions is the one in Fig. 8. The equation describing the MTPV trajectory is

$$\sin \delta = \frac{1}{4} \cdot (-\alpha + \sqrt{\alpha^2 + 8}), \quad \alpha = \frac{\lambda_m}{\lambda} \cdot \frac{\xi}{\xi - 1} \quad (11)$$

where  $\delta$  is the flux phase angle defined in Fig. 7. In Fig. 8,  $\delta_{max}$  is the flux angle at overload current and maximum speed [ $\lambda = \lambda_{min}$  in (11)].

Equation (12) is obtained by inspection of the figure

$$\frac{L_d i_0}{\lambda_{min}} \cong \sin \delta_{max} + \frac{\lambda_m}{\lambda_{min}}. \quad (12)$$

Substituting (10) in (12), the relationship (13) is obtained

$$\frac{\lambda_{min}}{\lambda_m} = (\sin \delta_{max})^{-1} \cdot \left( \frac{i_0}{i_1} - 1 \right). \quad (13)$$

According to (13), the minimum flux increases with the current overload. As a consequence, from (3) and (13), **the maximum speed that is feasible without encountering the MTPV reduces with the overload current**. The implicit (13) [ $\delta_{max}$  depends on the flux ratio according to (11)] has been plotted in Fig. 9 for better clarity. Given the overload ratio  $i_0/i_1$ ,



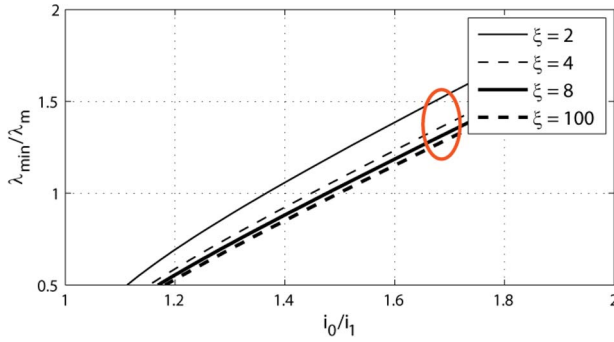
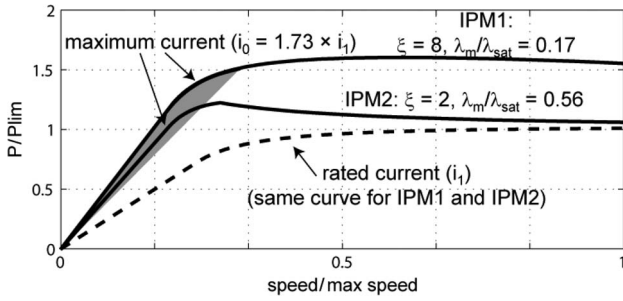


Fig. 9. Minimum flux as a function of current overload and anisotropy ratio.


 Fig. 10. Per-unit power versus speed for two IPM motor drives with different saliency ratio and PM magnet flux, designed for the same continuous torque and power ( $T_1$ ,  $P_1$ ) according to (10), and with the same rated flux ( $\lambda_{sat}$ ).

the ratio  $\lambda_{min}/\lambda_m$  follows, with little dependence on the motor saliency.

However, with a higher saliency,  $\lambda_m$  is lower (and so, it is  $\lambda_{min}$ ), and the feasible maximum speed increases accordingly. To point out this, in Fig. 10, two IPM machines are compared: one with high saliency ( $\xi = 8$ ) and low PM flux (IPM1), and the other with low saliency ( $\xi = 2$ ) and higher PM flux (IPM2).

The two machines are designed to give the same continuous power curve ( $P_1$ ) with the same current ( $i_1$ ) and voltage ( $V$ ). IPM1 is designed to meet the MTPV at maximum speed, as explained, while IPM2 encounters the MTPV around 0.3 p.u. speed. Two conclusions can be drawn by inspection of Fig. 10.

- 1) **Both IPM motors can be overloaded at low and high speed**, differently from the SPM case.
- 2) **The overload capability is much higher in those motors with a higher saliency.**

### C. Limitations of the Adopted Linear Model

The curves in Figs. 4, 6, and 10 (and also Fig. 11) have been obtained by means of linear machine models, for simplicity. The performance at rated current is correctly represented by the model, because it is assumed that the rated flux amplitude (MTPA flux at rated current  $i_1$ ) coincides with the core saturation limit ( $\lambda_{sat}$ ) for all the considered motors. However, at overload, the effects of saturation are not represented by the simplified model, which tend to overestimate the overload capability of all the considered machines (including the SPM one). For this reason, a shaded area has been adopted in Figs. 6, 10, and 12 around the low-speed overload zone, as a reminder of the limitations of this simplified model. Nevertheless, the simplified model is well representative of the behavior of the

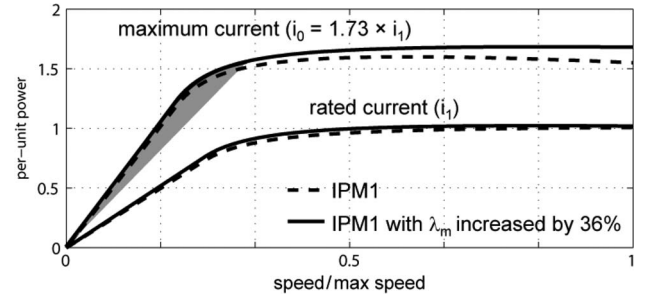
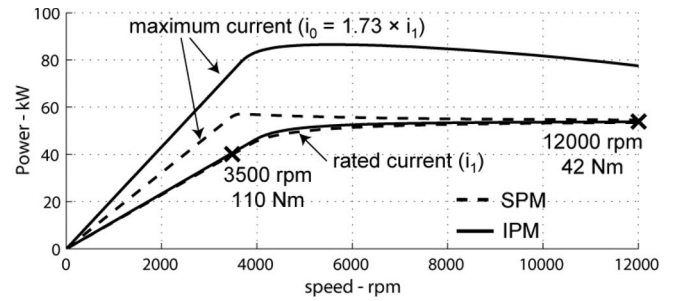


Fig. 11. Effect of increased PM flux on overload capability: Per-unit power-versus-speed curves for IPM1 (same as Fig. 8) and for IPM1 with PM flux increased by 36% according to (13).


 Fig. 12. Example motors having the same continuous torque, power, and current ( $P_1$ ,  $T_1$ , and  $i_1$ ). The two motors have the same stator diameter and stack length.

various machines in the flux-weakened region, where saturation effects are less evident. As a validation, finite element analysis (FEA) calculated power curves are reported and discussed in Section IV (Fig. 12).

### D. Effect of Reinforcing the PM Flux

The overload capability of IPM motors at high speed can be further improved if the PM flux is designed according to (14), which means higher than what is considered so far according to (10)

$$\lambda_m = \frac{L_q}{\xi} \cdot \frac{i_1 + i_0}{2}. \quad (14)$$

For a 173% overload, the PM flux is increased by 36%. As a consequence, (13) becomes

$$\frac{\lambda_{min}}{\lambda_m} = (\sin \delta_{max})^{-1} \cdot \left( \frac{i_0}{i_1} - 1 \right) \cdot \left( \frac{i_0}{i_1} + 1 \right)^{-1}. \quad (15)$$

The minimum flux ratio (15) is roughly reduced by 2.73 times with respect to (13), and the maximum speed that is feasible with no MTPV limitation is two times higher ( $2.73/1.36$ ).

In Fig. 11, the power-versus-speed curves are shown for the high-anisotropy machine IPM1 ( $\xi = 8$ ) both at rated and at overload with the PM designed according to (10) and (13), respectively. Dotted lines are the IPM1 curves of Fig. 10. As said, the rated performance has no practical modification, while the overload performance is improved. Fig. 11 also shows that **the design of PM flux is not critical for high-saliency motors**. Again, the comparison of Fig. 11 with Fig. 6 points out the

dramatic difference between SPM and IPM motors, as concerns the overload capability.

#### IV. DESIGN AND COMPARISON OF TWO EXAMPLE MOTORS

Up to this point, the motor design was not considered at all, except for the assumptions made with (1) and (10). An exhaustive comparison necessarily deals with actual design restrictions, i.e., thermal limits given by losses and the torque density and efficiency that can be obtained. To this aim, two example designs with the following common assumptions are reported:

- 1) outer diameter (216 mm) and stack length (170 mm);
- 2) 50-kW continuous power at 12 000 r/min maximum speed;
- 3) 173% current overload.

Both the machines are liquid cooled. The phase rated voltage is 173 V (pk). Due to the impact of iron loss at large speed values, a good quality steel (M250-35A) has been used for both designs. The same PM grade is also adopted (BMN-38SH).

With such design specifications, it turned out that the rated current for both machines is 208 A (pk), and the overload current is 360 A (pk). The SPM motor has shorter end connections (8% of active length against 17%) and a larger copper cross section (+33%) resulting in a phase resistance of 21 m $\Omega$  (SPM) versus 26 m $\Omega$  (IPM) at 130 °C. The numbers of turns in series per phase are 23 and 20, respectively. If the total slot cross section of the IPM motor was made the same as the one of the SPM, the motor would have still had the same continuous power with less Joule losses, but the overload capability would have been partially limited. It is worth to notice that the PM quantity of the IPM rotor is 40% higher than the one of the SPM rotor. This is not a general rule and depends on the two specific designs. The actual IPM motor was not optimized from this point of view. With different design choices, the PM quantity comparison could have given different results. Still, ***it remains true that choosing an IPM motor instead of an SPM one is not a matter of reducing the cost of the PMs***, as could have been expected.

The main ratings of the two motor designs are shown in Table I.

##### A. SPM Motor Design

The key point of the design is to satisfy the relationship (1), which is to match the short-circuit current with the thermal and demagnetization current limits. To this aim, fractional slots are used, giving the additional benefit of shorter end connections [5]. Due to the high maximum speed, the pole number must be maintained as low as possible, for limiting the iron losses. As a consequence, a six slot four pole (1/2 slots/pole phase) was chosen, with a double-layer winding (Fig. 1, left). Another tentative solution was also tested, with 12 slots and 10 poles (2/5 slots/pole phase), and abandoned due to excessive core and PM loss (the fundamental frequency was up to 1 kHz). It is supposed that the PMs are constrained into a nonconductive retaining sleeve, such as a carbon fiber sleeve, for avoiding

TABLE I  
RATINGS OF THE TWO MOTOR DESIGNS

		SPM	IPM
Pole pairs		2	2
Stator slots		48	6
Number of turns		23	20
Stator outer diameter	mm	216	
Stator inner diameter	mm	131	142
Stack length	mm	170	
Airgap	mm	0.7	
Base speed	rpm	3500	3500
Max speed	rpm	12000	
Continuous torque	Nm	110	110
Continuous current	A pk	208	208
Overload torque	Nm	135	200
Overload current	A pk	360	360
Characteristic current (PMs at 20°C)	A pk	226	230
Phase rated voltage	V pk	173	173
Phase back-emf (12000 rpm, PMs at 20°C)	V pk	554	152
Phase resistance at 130°C	$\Omega$	0.021	0.026
Steel grade		M250-35A	
PM grade		BMN-38SH	
PM quantity	kg	1.35	1.95

the additional eddy current losses related to some kind of conductive retention devices. However, the additional depth required for that sleeve has not been considered in the magnetic design.

##### B. IPM Motor Design

With the aim of minimizing the  $d$ -axis inductance, a multiple barrier rotor structure was chosen, as shown in Fig. 1 (right). Moreover, to reduce torque ripple and high speed losses, the combination 24/20 of stator and rotor slots per pole pair was conveniently chosen [14]. The PM flux was designed according to (10). The mechanical robustness of the rotor at high speed is related to the proper design of the interlayer iron ribs, in terms of placement and thickness. The rotor has been verified against maximum centrifugal stress with reference to a maximum speed of 14 000 r/min that is 20% higher than the maximum operating speed.

##### C. Power Curves

As shown in Fig. 12, the two motors give the same continuous power curve, when supplied with the same current at the same inverter voltage. The comparison of Fig. 12 with the curves based on the linear model of Figs. 6 and 10 points out the effects of core saturation. The rated power curves are slightly affected by saturation, for both the motors, although the torque curves at overload are much different from the ones forecasted by the linear model, also at low speed. This is due to saturation and cross saturation effects. This one is clearly heavier in the SPM case, which gives a definitely lower overload torque, also at low speed.

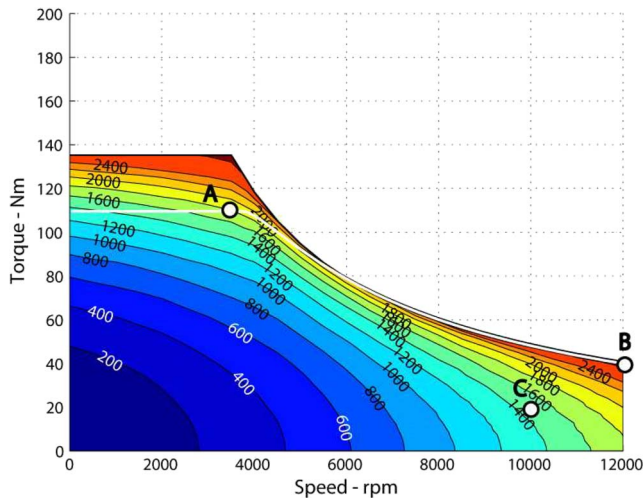


Fig. 13. Total loss map of the SPM motor. The white line refers to the continuous current  $i_1 = 208$  A (pk), and the outside limit of the map refers to the overload current  $i_0 = 360$  A (pk). PMs at  $100^\circ\text{C}$ . Copper at  $130^\circ\text{C}$ .

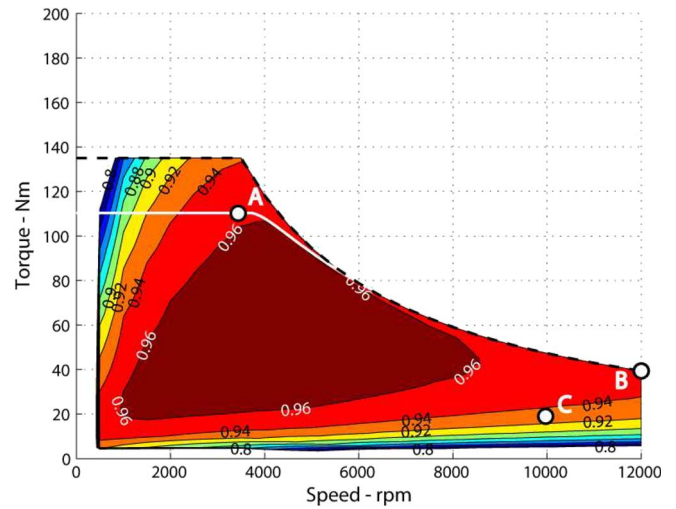


Fig. 15. Efficiency map of the SPM motor. Same conditions as those in Fig. 13.

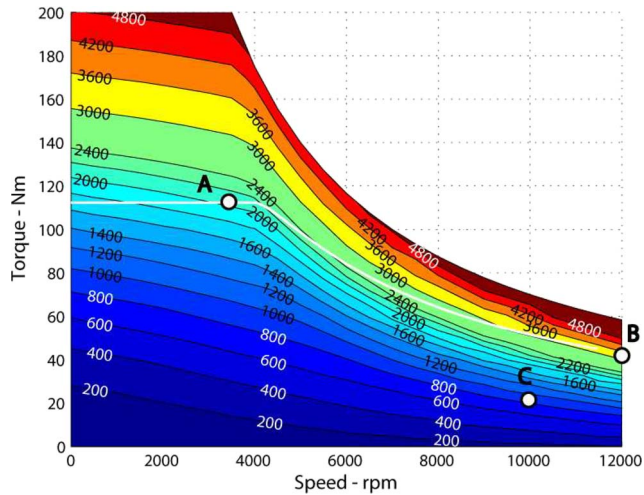


Fig. 14. Total loss map of the IPM motor. The white line refers to the continuous current  $i_1 = 208$  A (pk), and the outside limit of the map refers to the overload current  $i_0 = 360$  A (pk). PMs at  $100^\circ\text{C}$ . Copper at  $130^\circ\text{C}$ .

#### D. FEA Evaluation of the Iron Losses

The core losses have been calculated by means of transient FEA over the whole torque and speed ranges of the two motors using MagNet by Infolytica. The iron loss model is based on the Epstein frame loss measurements declared by the manufacturer and uses a modified Steinmetz equation augmented with an eddy current term to fit the loss manufacturer data. The accuracy of the model relies on the availability of loss curve data from the manufacturer at several frequencies, in particular, at high frequency [15], and the M250-35A grade is characterized up to 2500 Hz.

#### E. Loss and Efficiency Maps

The loss maps of the two motors are reported in Figs. 13 and 14. The torque profile lined in white is the torque at rated current  $i_1$  that is common to both motors, while the upper profiles of the maps are determined by the overload current  $i_0$ ,

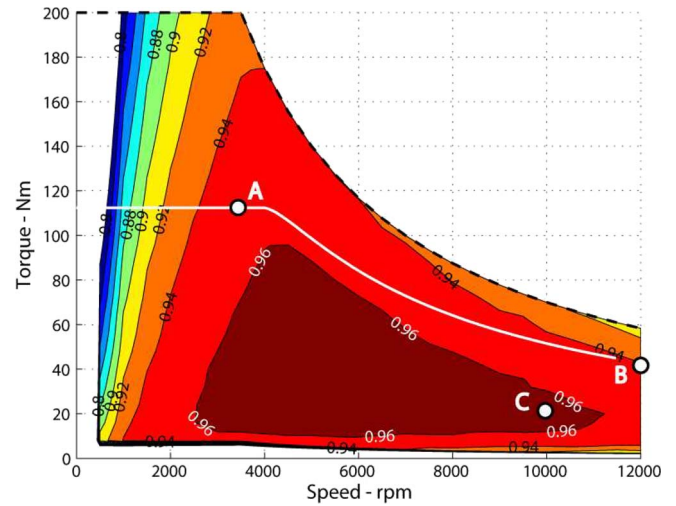


Fig. 16. Efficiency map of the IPM motor. Same conditions as those in Fig. 14.

and the overload area of the IPM motor is larger as said. The PM temperature is  $100^\circ\text{C}$ . The PMs of the surface-mounted machine are segmented tangentially in five parts, for reducing the eddy current losses, and not segmented axially. The effect of axial segmentation will be discussed later.

The two maps of Figs. 13 and 14 put in evidence that the losses of the IPM motor are progressive with the load torque at all speeds while the losses of the SPM motor are not, except for very low speed. As soon as the speed increases, the SPM suffers from two terms of losses that are independent of the torque, namely, PM losses and Joule losses due to the deexcitation current component (negative  $i_d$ ), and this explains the curled shape of the constant-loss curves in Fig. 13. On the other end, the IPM motor has little more copper losses and then higher loss overall at rated current due to the higher phase resistance. The harmonic losses of the IPM at high speed have been minimized by the specific 24-20 slot design [14]: With less rotor layers and less stator slots per pole, higher core losses could be expected.

The efficiency maps are reported in Figs. 15 and 16 showing that both the machines are rather efficient on the entire area of



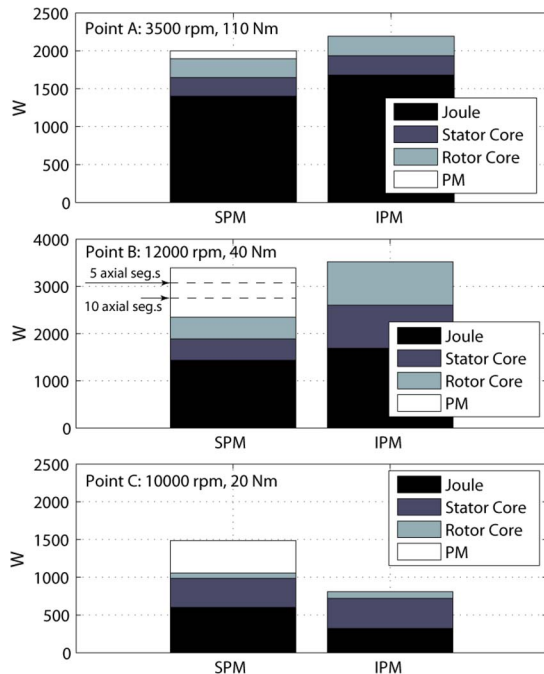


Fig. 17. Detail of motor losses in the three working points A, B, and C put in evidence in Figs. 13–16.

operation: As for the losses, the SPM motor is more efficient at low speed and much less at high speed.

#### F. Detail of Losses in Specific Points

Loss components are detailed for the three working points A, B, and C put in evidence in Figs. 13–16.

- 1) Point A (110 Nm, 3500 r/min) is representative of mild accelerations and decelerations in urban cycles.
- 2) Point B (40 Nm, 12 000 r/min) represents cruising power at maximum speed.
- 3) Point C (20 Nm, 10 000 r/min) represents cruising power at 80% of the maximum speed.

At the relatively low speed point A, the losses have a dominant Joule term and similar core terms. The losses are mainly on the stator, and the IPM motor is less efficient in this area. At maximum speed continuous power (point B), the SPM has significant PM losses that can be limited by further segmentation of the PMs also in the axial direction [16]. Two options of axial segmentation are reported in the middle subfigure of Fig. 17. They show that, for obtaining a significant reduction of the losses, a troublesome ten-part segmentation would be required, added to the already assumed five-part tangential segmentation.

Losses at point C show that the SPM Joule term is much higher despite the 20% lower resistance, due to the need of deexcitation current.

In case the operating speed specification was lower, the SPM drive might have been helped by the possible adoption of a higher number of poles. If 10 or 14 poles are feasible, the continuous power density of the SPM motor can be higher [3], but still, the IPM motor would maintain a much higher overload capability. Nevertheless, the actual trend in traction is to increase the speed as much as possible for reducing the motor size, and this makes high pole numbers unfeasible.

#### V. CONCLUSION

SPM and IPM synchronous motors have been thoroughly compared, for application to electric traction. The SPM motor has concentrated windings and a simpler construction. With equal active parts, size, and cooling, *the two motors give the same continuous power*. The IPM motor has a good overload capability over the entire speed range, if the saliency of the machine is maximized, while *the output power of the SPM motor cannot overcome the continuous power rating independently of the applied current overload*. Dealing with losses and efficiency, the SPM motor is affected by *extra-Joule losses for deexciting the PM flux* at high speed and PM losses that require segmentation in both directions (circumferential and axial). On the contrary, the IPM motor has higher Joule losses at low speed due to end connections and requires a properly *high number of stator slots and rotor segments* to keep the harmonic losses under control, which can make the fabrication more expensive.

#### REFERENCES

- [1] Z. Q. Zhu and C. C. Chan, "Electrical machine topologies and technologies for electric, hybrid, and fuel cell vehicles," in *Proc. IEEE VPPC*, Sep. 3–5, 2008, pp. 1–6.
- [2] K. T. Chau, C. C. Chan, and C. Liu, "Overview of permanent-magnet brushless drives for electric and hybrid electric vehicles," *IEEE Trans. Ind. Electron.*, vol. 55, no. 6, pp. 2246–2257, Jun. 2008.
- [3] A. M. El-Refai and T. M. Jahns, "Comparison of synchronous PM machine types for wide constant-power speed range operation," in *40th Conf. Rec. IEEE IAS Annu. Meeting*, Oct. 2–6, 2005, vol. 2, pp. 1015–1022.
- [4] A. M. EL-Refai, "Fractional-slot concentrated-windings synchronous permanent magnet machines: Opportunities and challenges," *IEEE Trans. Ind. Electron.*, vol. 57, no. 1, pp. 107–121, Jan. 2010.
- [5] A. M. EL-Refai and T. M. Jahns, "Optimal flux weakening in surface PM machines using fractional-slot concentrated windings," *IEEE Trans. Ind. Appl.*, vol. 41, no. 3, pp. 790–800, May/Jun. 2005.
- [6] J. J. Germishuizen and M. J. Kamper, "IPM traction machine with single layer non-overlapping concentrated windings," *IEEE Trans. Ind. Appl.*, vol. 45, no. 4, pp. 1387–1394, Jul./Aug. 2009.
- [7] L. Chong and M. F. Rahman, "Saliency ratio derivation and optimisation for an interior permanent magnet machine with concentrated windings using finite-element analysis," *IET Elect. Power Appl.*, vol. 4, no. 4, pp. 249–258, Apr. 2010.
- [8] S.-O. Kwon, S.-I. Kim, P. Zhang, and J.-P. Hong, "Performance comparison of IPMSM with distributed and concentrated windings," in *41st Conf. Rec. IEEE IAS Annu. Meeting*, Oct. 8–12, 2006, vol. 4, pp. 1984–1988.
- [9] A. Vagati, G. Pellegrino, and P. Guglielmi, "Comparison between SPM and IPM motor drives for EV application," in *Proc. XIX Int. Conf. ICEM*, Sep. 6–8, 2010, pp. 1–6.
- [10] M. Ehsani, K. M. Rahman, and H. A. Toliyat, "Propulsion system design of electric and hybrid vehicles," *IEEE Trans. Ind. Electron.*, vol. 44, no. 1, pp. 19–27, Feb. 1997.
- [11] W. Soong and T. J. E. Miller, "Field weakening performance of brushless synchronous AC motor drives," *Proc. Inst. Elect. Eng. Elect. Power Appl.*, vol. 141, no. 6, pp. 331–340, Nov. 1994.
- [12] T. M. Jahns and V. Caliskan, "Uncontrolled generator operation of interior PM synchronous machines following high-speed inverter shutdown," *IEEE Trans. Ind. Appl.*, vol. 35, no. 6, pp. 1347–1357, Nov./Dec. 1999.
- [13] P. Guglielmi, E. Armando, G. Pellegrino, and A. Vagati, "Optimal design of IPM-PMASR motors for wide constant power speed range," in *Proc. PCIM*, Nurnberg, Germany, 2007, pp. 1–6.
- [14] G. Pellegrino, P. Guglielmi, A. Vagati, and F. Villata, "Core loss and torque ripple in IPM machines: Dedicated modeling and design trade off," in *Proc. IEEE ECCE*, Sep. 20–24, 2009, pp. 1911–1918.
- [15] K. Yamazaki and N. Fukushima, "Experimental validation of iron loss model for rotating machines based on direct eddy current analysis of electrical steel sheets," in *Proc. IEEE Int. IEMDC*, May 3–6, 2009, pp. 851–857.
- [16] J. L. Kirtley, Jr., M. Tolikas, J. H. Lang, C. W. Ng, and R. Roche, "Rotor loss models for high speed PM motor-generators," in *Proc. ICEM*, 1998, pp. 1832–1837.





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