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Iron Loss and Parameter Measurement of Permanent Magnet Synchronous Machines

Jan Richter, Andreas Dollinger, Martin Doppelbauer

***Abstract* -- In this paper a measurement technique for synchronous machines is presented that allows the experimental identification of stator flux linkage as well as iron and friction losses in each operation point. The technique can be applied to all kinds of synchronous electric machines, even to machines showing magnetic anisotropy, iron saturation and cross-coupling. Influences of spatial harmonics, inverter switching and temperature transients are considered. The method uses steady-state measurements at constant motor speed. It is based on a combined evaluation of two operation points with corresponding magnetic states, one being in the motor and the other in the generator mode. The method is demonstrated by characterization of an automotive permanent magnet synchronous traction motor.**

***Index Terms* -- electric machines, electric resistance, friction, loss measurement, magnetic loss, measurement techniques, nonlinear magnetics, parameter extraction, permanent magnet motors, system identification.**

I. Introduction

THE performance of electric machines is usually evaluated experimentally with measurements on test benches. These measurements are used to verify finite element calculations, to parameterize machine models and to calculate optimal control parameters and current reference values. One major advantage of such an experimental approach is that detailed information about magnetics and losses can be obtained without knowledge of the machine geometry and design.

A complete analysis should be able to identify all machine parameters, i.e. the stator flux linkage, copper, iron and friction losses in each operation point. However, a precise separation of iron and friction losses is challenging. This is due to the fact that straight-forward methods like deceleration tests yield improper results because of the permanent presence of a magnet field in permanent magnet synchronous machines. Hence the corruption of the flux linkage identification caused by iron losses [1] cannot be corrected easily.

In literature two classes of measurement methods can be found that allow the characterization of synchronous machines under consideration of saturation and cross-coupling: locked rotor methods and constant speed methods. The former is based on the idea that when the rotor of the machine is locked, influences due to the back electromotive force are eliminated. By application of test signals to the motor windings, machine magnetics can therefore be observed. Dutta and Rahman for example apply a sinusoidal current to one motor phase while measuring the phase voltages to determine self and mutual inductances [2]. Ebersberger and Piepenbreier use high frequency current signals of small amplitude in the rotor-oriented dq-reference frame to identify differential inductances [3]. A pseudo random binary sequence voltage signal is used by Mink et al. for the same purpose [4]. Stumberger et al. directly identify the flux linkage by keeping the current of one axis constant while the voltage of the other axis is alternated in a stepwise manner [5]. However, all these methods suffer falsification due to spatial harmonics because characterization is only performed at one distinct rotor angle.

This can be avoided by using the constant speed method. There, these errors can be removed by averaging the measured quantities in the dq-reference frame over a whole number of mechanical revolutions [6]. The stator flux linkage is then obtained by solving the steady-state machine equations. The main drawback of constant speed methods is that iron losses falsify identification due to rotation. It is often stated that the rotor speed is sufficiently small so that iron losses are negligible [2], [4], [6]. As in none of the contributions the validity of this approximation is proven and moreover iron losses remain unknown, other methods have been developed that directly incorporate iron losses in the identification process.

Some authors use an additional series resistance in their models for that purpose [1], [7], [8]. As stated there, stator voltage can be expressed independently of iron loss current during stationary operation if an additional series resistance is included. However, this does not imply that iron losses can be calculated as ohmic losses at the additional series resistance. A simple counterexample can be given with open circuit operation at constant speed: models with an iron resistance in series predict zero iron losses which is obviously wrong for permanent magnet synchronous

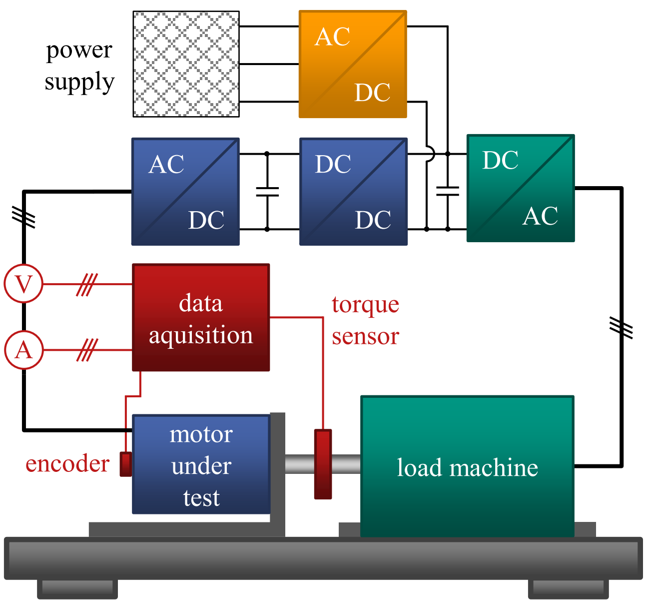

Fig. 1 Measurement setup to characterize arbitrary synchronous machines.

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machines. Instead, as shown in Section II, the iron loss resistance has to be inserted in parallel to the exciting inductance. An experimental approach to determine iron losses using such a model was published by Urasaki et al. but their method only works for machines with equal inductances in the direct and quadrature axis [9].

That is why in this contribution a new method is proposed. It allows not only the identification of magnetic quantities such as the stator flux linkage but also of iron and friction losses in each operation point. In the proposed technique the machine is operated in steady-state at constant speed in a setup depicted in Fig. 1. The basic idea of the method is to control the magnetic state of the machine by means of the induced voltage. Thus it is possible to realize a corresponding magnetic state in motor and generator mode. A combined analysis of both measurements then allows the identification and separation of iron and friction losses. Errors due to temperature effects and spatial and time harmonics are considered. Applying the presented method enables a multitude of possible studies: For instance, machines of different manufactures can be compared in detail or the influence of various materials and production parameters can be thoroughly investigated. Thereby, optimization potentials can be identified and further improvements can be realized.

In the following the machine model incorporating iron losses is introduced in Section II and the parameter identification method is theoretically developed. The test bench and experimental realization are described in Section III. The effectiveness of the method is demonstrated by characterization of a strongly nonlinear interior permanent magnet synchronous machine for automotive traction applications. Respective measurement results are discussed in Section IV.

II. Theory

The machine is assumed to have three symmetric star-connected phases with the neutral point not connected to the inverter. Dielectric currents, skin and proximity effects are neglected. Transformation of the measured quantities to the rotor-oriented dq-reference frame and subsequent averaging over a whole-number of mechanical revolutions allows eliminating effects with a zero mean like spatial harmonics, eccentricities and harmonics caused by inverter switching. Thus only the fundamental component of all measured quantities is evaluated and losses due to higher harmonics are disregarded and not identified by the method. Iron losses are understood to be hysteresis and eddy current losses in all machine materials including the permanent magnets. It should be kept in mind that iron losses caused by stray fields are in general not detectable using the machine windings. In the method these losses are therefore identified as friction losses.

For identification the machine under test is operated at constant speed which is closed-loop controlled by a load machine. The current of the machine under test is controlled by a vector controller to constant direct and quadrature currents with the direct axis being oriented to the permanent magnet flux linkage. Because the machine is operated at steady-state dynamic effects do not have to be considered. The three phase currents and voltages have to

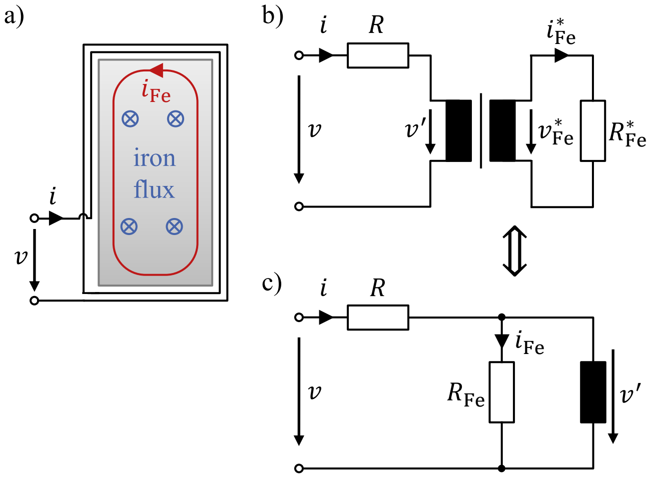

Fig. 2 Model of a single coil with a lossy iron core. The electric and magnetic behavior of the circuits b) and c) is equivalent if R_{Fe}^* is correctly transformed into R_{Fe} and stray effects are neglected.

be known, either by measurement or by estimation. Moreover the shaft torque, rotor angle and rotor speed have to be observed as illustrated in Fig. 1.

A. *Machine Model including Iron and Friction Losses*

The machine model consists of an electric and a mechanic part. In order to develop the electric part of the model, first a single machine coil with an iron core as given in Fig. 2 a) is analyzed.

As both hysteresis and eddy current losses have the same physical origin [10], all losses in the iron core are assumed to be eddy current losses. Therefore a physically motivated model of the single coil consists of a resistor R to model copper losses and a transformer loaded with an ohmic resistance R_{Fe}^* to account for iron losses as illustrated in Fig. 2 b). The current i_{Fe}^* causes iron losses at R_{Fe}^*. As the permeability of iron is several magnitudes larger than the one of all other present materials, stray effects can be neglected. Therefore the primary transformer voltage v' and secondary current i_{Fe}^* are in phase. The iron resistance R_{Fe}^* can consequently be converted to an equivalent resistance R_{Fe} in parallel to the exciting inductance as given in Fig. 2 c). The electric and magnetic behavior of the circuits of Fig. 2 b) and Fig. 2 c) are identical and conversion does not affect the modeled iron losses. An electric machine consists of three such coils with shared flux paths. In that case conversion of the iron loss resistance can be conducted similarly and with Park's transformation [11] the equivalent circuit for steady-state operation including iron losses can be deduced [9]. This derivation yields an additional parallel iron loss resistance in both axes as shown in Fig. 3. By application of Kirchhoff's voltage and current laws to both machine axes the six system equations can be obtained:

$$v_{\mathrm{d}} = R \cdot i_{\mathrm{d}} - \omega \cdot \Psi_{\mathrm{q}} \tag{1}$$

$$v_{\mathrm{q}} = R \cdot i_{\mathrm{q}} + \omega \cdot \Psi_{\mathrm{d}} \tag{2}$$

$$R_{\mathrm{Fe,d}} \cdot i_{\mathrm{di}} + \omega \cdot \Psi_{\mathrm{q}} = 0 \tag{3}$$

$$-R_{\mathrm{Fe,q}} \cdot i_{\mathrm{qi}} + \omega \cdot \Psi_{\mathrm{d}} = 0 \tag{4}$$

$$i_{\mathrm{d}} - i_{\mathrm{di}} - i_{\mathrm{dm}} = 0 \tag{5}$$

$$i_{\mathrm{q}} - i_{\mathrm{qi}} - i_{\mathrm{qm}} = 0 \tag{6}$$

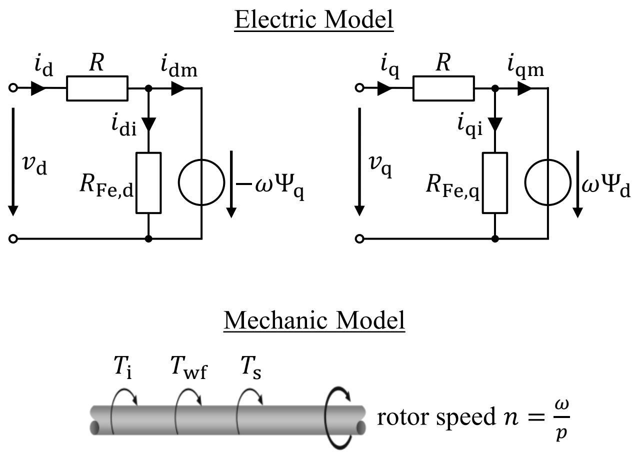

Fig. 3 Steady-state electric and mechanic model of the permanent magnet synchronous machine.

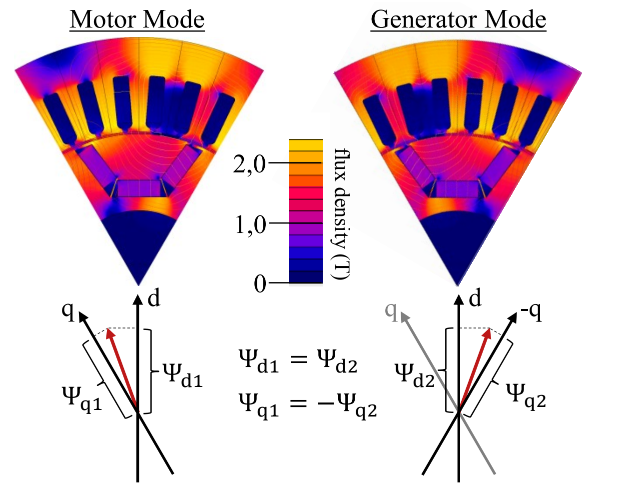

Fig. 4 Flux distribution in motor (index 1) and generator mode (index 2) for positive rotor speed. Due to rotor symmetry the flux distribution is mirror symmetrical to the direct axis if both operation points are compared.

With $k \in \{\mathrm{d}, \mathrm{q}\}$ as indicator for the direct and quadrature axis, therein v_k denotes the stator voltages, i_k the stator currents, R the stator resistance, ω the electric frequency, Ψ_k the flux linkages, $R_{\mathrm{Fe},k}$ the iron loss resistances, $i_{k\mathrm{i}}$ the iron loss currents and $i_{k\mathrm{m}}$ the magnetization currents. The flux linkages Ψ_k are two-dimensional, nonlinear functions of both magnetization currents i_{dm} and i_{qm}. The inner machine torque T_{i} can be calculated using the energy balance of (1) to (6):

$$T_{\mathrm{i}} = \frac{3}{2} p \cdot \left(\Psi_{\mathrm{d}} \cdot i_{\mathrm{qm}} - \Psi_{\mathrm{q}} \cdot i_{\mathrm{dm}}\right) \tag{7}$$

with p denoting the number of pole pairs. Equation (7) connects the electric and mechanic part of the model. Though, due to friction, the inner torque T_{i} cannot be measured at the shaft. The balance of torque is given by

$$T_{\mathrm{i}} + T_{\mathrm{wf}} + T_{\mathrm{s}} = 0 \tag{8}$$

with T_{wf} as windage and friction torque and T_{s} as shaft torque as depicted in Fig. 3.

B. Parameter Identification

In the following an identification method is developed to determine the unknown quantities of (1) to (8). In each operation point therefore

- the ohmic resistance R,
- the flux linkages Ψ_{d} and Ψ_{q},
- the magnetization currents i_{dm} and i_{qm} and the iron loss currents i_{di} and i_{qi},
- the iron loss resistances $R_{\mathrm{Fe,d}}$ and $R_{\mathrm{Fe,q}}$,
- and the windage and friction torque T_{wf}

have to be identified. There, the key task is the determination of the magnetization currents, because then all other quantities can be determined easily using straightforward methods. The basic idea of their identification is the combined evaluation of two operation points of corresponding magnetic state with one being in motor and the other in generator mode.

In order to clarify this concept, it first has to be understood that the magnetic state of the machine is basically equal if two operation points are chosen in a way that Ψ_{d} stays constant and Ψ_{q} is inverted. This is implied by the fact that rotors of common synchronous machines are mirror symmetrical to the direct axis, as it is exemplarily depicted in Fig. 4. Although the flux linkage is once left and once right of the direct axis, the distribution of magnetic field lines does not change in principle due to the symmetry of the rotor. This can be seen by mirroring one of the flux distributions in Fig. 4 at the direct axis and comparing it to the other. As a consequence, iron losses do not change when Ψ_{q} is inverted because the magnetic state itself is equal but mirrored.

The second principle to be understood is that if Ψ_{d} stays constant and Ψ_{q} is inverted, i_{dm} staying constant and i_{qm} being inverted is directly implied. This is true because the relation between flux linkages and magnetization currents is unique: exactly one $(i_{\mathrm{dm}}, i_{\mathrm{qm}})$ couple belongs to one $(\Psi_{\mathrm{d}}, \Psi_{\mathrm{q}})$ couple as it was proven in [12]. Furthermore, due to rotor symmetry, the absolute value of magnetization current i_{qm} does not change even though Ψ_{q} inverts. Consequently, also the inner torque T_{i} is inverted, as it can be deduced with (7). This also implies that the absolute value of the inner torque does not change. Since friction is assumed independent of torque direction, the friction torque stays constant as well. This knowledge is sufficient to determine all parameters.

In the following all parameters from the stator windings to the machine's shaft are derived. There the variable $l \in \{1,2\}$ denotes measurements in motor mode with the index 1 and in generator mode with the index 2. The rotor-oriented values $v_{\mathrm{d}l}$, $v_{\mathrm{q}l}$, $i_{\mathrm{d}l}$ and $i_{\mathrm{q}l}$ are calculated from the measured phase voltages, phase currents and rotor angle using Park's transformation [11]. Electric frequency ω and the shaft torques $T_{\mathrm{s}l}$ are directly measured. The ohmic resistance is determined in a separate direct current measurement. The practical realization and the proof that the so determined resistance can be used within the proposed identification scheme are given in Section III *C*. The flux linkages can be calculated by rearranging (1) and (2):

$$\Psi_{\mathrm{d1}} = \Psi_{\mathrm{d2}} = \frac{v_{\mathrm{q1}} - R \cdot i_{\mathrm{q1}}}{\omega} = \frac{v_{\mathrm{q2}} - R \cdot i_{\mathrm{q2}}}{\omega} \tag{9}$$

$$\Psi_{q1} = -\Psi_{q2} = \frac{v_{d1} - R \cdot i_{d1}}{-\omega} = -\frac{v_{d2} - R \cdot i_{d2}}{-\omega} \quad (10)$$

Since the absolute values of the flux linkages are equal in both operation points also the magnetization currents are, as explained above. Moreover, because iron losses do not change, the absolute values of the iron loss currents i_{ki1} and i_{ki2} are identical. Thus the measured currents i_{k1} and i_{k2} differ in twice the amount of the iron loss currents i_{kil}. Fig. 3 additionally clarifies that point: When operation is switched from motor to generator mode, both the direct axis iron loss current i_{di} and the quadrature axis magnetization current i_{qm} are inverted. Hence iron loss currents are covered by i_{k1} in motor mode and by the magnetization currents i_{km2} in generator mode. Therefore the following equations hold true:

$$i_{dm1} = i_{dm2} = \frac{i_{d1} + i_{d2}}{2} \quad (11)$$

$$i_{qm1} = -i_{qm2} = \frac{i_{q1} - i_{q2}}{2} \quad (12)$$

Using (5) and (6) the iron loss currents can then be derived:

$$i_{di1} = i_{di2} = i_{d1} - i_{dm1} = i_{d2} - i_{dm2} \quad (13)$$

$$i_{qi1} = -i_{qi2} = i_{q1} - i_{qm1} = -\left(i_{q2} - i_{qm2}\right) \quad (14)$$

Further, iron loss resistances can be calculated using (3) and (4):

$$R_{Fe,d} = -\omega \cdot \frac{\Psi_{q1}}{i_{di1}} = -\omega \cdot \frac{-\Psi_{q2}}{i_{di2}} \quad (15)$$

$$R_{Fe,q} = \omega \cdot \frac{\Psi_{d1}}{i_{qi1}} = \omega \cdot \frac{\Psi_{d2}}{i_{qi2}} \quad (16)$$

Finally, using (7) the inner torques T_{i1} and T_{i2} can be computed and with (8) the friction torques are given by:

$$T_{wf1} = -T_{s1} - T_{i1} \quad (17)$$

$$T_{wf2} = -T_{s2} - T_{i2} \quad (18)$$

Thus all parameters and quantities are known and the according powers can be calculated:

$$P_{el l} = \frac{3}{2}\left(v_{dl} \cdot i_{dl} + v_{ql} \cdot i_{ql}\right) \quad (19)$$

$$P_{Cul} = \frac{3}{2}\left(R \cdot i_{dl}^2 + R \cdot i_{ql}^2\right) \quad (20)$$

$$P_{Fel} = \frac{3}{2}\left(R_{Fe,d} \cdot i_{dil}^2 + R_{Fe,q} \cdot i_{qil}^2\right) \quad (21)$$

TABLE I
MACHINE PROPERTIES

Parameter	Nominal Values
Voltage nom.	212 V
Current nom. / max.	169 A / 300 A
Shaft power nom. / max.	57 kW / 97 kW
Speed nom. / max.	4200 min^{-1} / 11000 min^{-1}
Torque nom. / max.	130 Nm / 220 Nm
Ohmic stator resistance typ.	10.5 mΩ
Number of pole pairs	3

$$P_{wfl} = T_{wfl} \cdot \frac{\omega}{p} \quad (22)$$

$$P_{sl} = T_{sl} \cdot \frac{\omega}{p} \quad (23)$$

The variable $l \in \{1,2\}$ represents motor and generator mode, P_{ell} denotes the electric power, P_{Cul} the copper losses, P_{Fel} the iron losses, P_{wfl} the windage and friction losses and P_{sl} the mechanic shaft power.

III. EXPERIMENTAL

A. *Test Bench and Measurement Equipment*

The permanent magnet reluctance torque synchronous machine manufactured by *Brusa* of type *HSM1-6.17.12-CO1* is used as motor under test. The machine properties are given in Table I. A specifically designed control algorithm for strongly nonlinear machines described in [12] is employed to control the machine. A speed controlled asynchronous machine by *Wittur* is utilized as load.

Four self-developed inverters for grid connection and power supply of both machines as shown in Fig. 1 are based on *Semikron SkiiP 513GD122-3DUL* modules. Grid connection is realized by one module operating as active front end. A second module uses the DC link voltage supplied by the active front end to control the load machine. A third module operates as DC/DC converter to reduce the DC link voltage for the fourth module that controls the motor under test. All modules switch at a frequency of 8 kHz. The inverters are controlled by a custom signal processing system that is based on the digital signal processor (DSP) *TMS320C6748* produced by *Texas Instruments*. Analog-digital converter cards to measure necessary quantities as well as custom modulator cards to create the inverter gate signals are employed using field programmable gate arrays (FPGA) of the *Cyclone* series by *Altera*.

High-precision measurement equipment is crucial in order to identify the relatively small iron and friction losses. Currents are observed by a current transducer *LEM IT 1000-S/SP1 ULTRASTAB*. Torque is measured with a *HBM T10F* torque meter. Speed and rotor angle are calculated using the machine's built-in encoder signals. The stator resistance is measured with a *Sefelec MGR 10*. For central data recording the high-precision digital power analyzer *Yokogawa WT3000* is used. Machine line voltages, phase currents, torque, speed and rotor angle data are captured with a sampling rate of 200 kHz. The measurement routine is fully automated using *LabVIEW* by *National Instruments*.

B. *Encoder Alignment*

Perfect alignment of the direct axis from the encoder signal to the machine axis of the permanent magnet flux linkage is necessary for precise measurements. The alignment procedure is conducted at open-circuit operation while the motor under test is driven by the load machine. As can be seen in Fig. 3 both machine currents i_d and i_q are zero during open-circuit operation. Then the induced voltage due to the permanent magnets can be measured in the quadrature axis at constant rotor speed. The induced voltage $v_{q,+n} = \omega\Psi_d$ causes an iron loss current i_{qi} that has

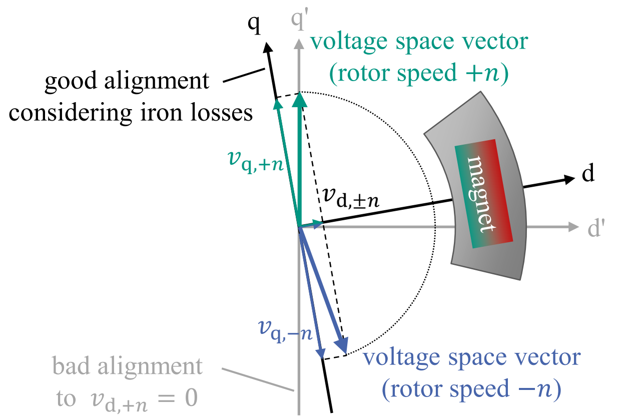

Fig. 5 Encoder alignment to the machine's permanent magnet flux linkage. A modified method taking iron losses into account is used.

to be covered by a magnetization current i_{qm}. This yields a quadrature flux linkage Ψ_{q}, an induced voltage $v_{\mathrm{d},+n} = -\omega\Psi_{\mathrm{q}}$ and an iron loss current i_{di} in the direct axis. Consequently iron losses cause induced voltages in both axes during open-circuit operation. If iron losses are ignored and the encoder is aligned to $v_{\mathrm{d}} = 0$ at positive rotor speed $+n$ noticeable misalignment occurs as shown by the grey axes in Fig. 5. Misalignment can be proven by inverting the rotor speed to the same absolute value $-n$: Due to iron losses, the absolute value of the quadrature voltage decreases and the direct voltage increases although the absolute value of the induced voltage stays constant.

Therefore, a modified alignment procedure is used taking iron losses into account. The induced voltages of both axes are repeatedly recorded for positive and negative rotor speeds with identical absolute values $|n|$. The encoder offset is varied until $v_{\mathrm{q},+n} = -v_{\mathrm{q},-n}$ and $v_{\mathrm{d},+n} = v_{\mathrm{d},-n}$ after speed inversion as depicted by the black axes in Fig. 5. If that is the case, the direct axis of the encoder signal is aligned to the permanent magnet flux linkage of the machine. This is repeated and checked for various rotor speeds until alignment accuracy is sufficient in the whole operation range.

C. Measurement Routine

In generator mode the same direct flux linkage Ψ_{d} and the inverted quadrature flux linkage Ψ_{q} have to be applied in comparison to motor mode as deduced in Section II *B*. From (9) and (10) it can be seen that the induced voltages $(v_k - R \cdot i_{\mathrm{k}})$ with $k \in \{\mathrm{d},\mathrm{q}\}$ have to be controlled to reach that goal. Hence, the machine is current controlled in motor mode and the induced voltages are determined. Then in generator mode the same induced voltage in the direct axis and the inverted induced voltage in the quadrature axis are applied. A superposed induced voltage controller that adjusts current reference values until the induced voltages correctly match the given values is used for that purpose.

The proper determination of the machine's induced voltages depends on the permanent magnetic flux linkage and the stator resistance. Since both values change with temperature, it is of great importance to stay close to thermal quasi-equilibrium during characterization. This can be achieved by insertion of heating or cooling phases and by a specific timing of the measurements. Deviations from thermal quasi-equilibrium are detected by repeated measurements of the open circuit voltage and the stator resistance. The resistance is measured at motor standstill with an additional direct current measurement using 4-wire sensing. Moreover, the winding temperature is monitored by a built-in thermocouple. Thus, invalid measurements due to thermal drift are detected and discarded.

Finally, the measurement routine is run by the following procedure: Before starting the measurements the machine is pre-heated for several hours to reach thermal quasi-equilibrium at 60 °C. Then for each operation point, given by the currents in both axes and the rotor speed, the following is repeated:

- A heating or cooling phase is inserted if the temperature differs from thermal quasi-equilibrium.
- Motor mode: The reference currents are set and all quantities are measured.
- A measurement of the stator resistance is conducted.
- A second heating or cooling phase is inserted.
- Generator mode: The induced voltage is controlled until the operation point fits to the one in motor mode. Then all quantities are measured.
- The open circuit voltage and the stator resistance are measured.

The machine has been measured for rotor speeds of 2000 min^{-1}, 4000 min^{-1}, 6000 min^{-1}, 8000 min^{-1} and 10000 min^{-1}.

IV. Results and Discussion

Measurement results of the automotive traction machine with the properties given in Table I are presented and discussed in the following. Starting with the analysis of a single operation point, results of the whole dq-current plane are shown. Through extension to multiple rotor speeds a three-dimensional graph is presented. From this the common speed-torque diagram is derived by calculating reference values with minimal current amplitude.

The machine powers of one operation point in motor mode and the corresponding operation point in generator mode are given in Fig. 6. There it can be observed that the absolute value of the electric power in motor mode $|P_{\mathrm{el1}}|$ is greater than the according electric power $|P_{\mathrm{el2}}|$ in generator mode. This is due to the fact that the absolute value of the inner power, defined by the product of inner torque and speed, is controlled to an equal absolute value. Since copper losses $P_{\mathrm{Cu}l}$ and iron losses $P_{\mathrm{Fe}l}$ have to be supplied by the electric power $P_{\mathrm{el}l}$ in motor mode while they are supplied by the shaft power $P_{\mathrm{s}l}$ in generator mode, the electric powers differ. The slight mismatch between copper losses $P_{\mathrm{Cu}l}$ can be explained by different phase currents, as they are necessary in motor and generator mode in order to achieve identical absolute values of magnetization currents. Iron losses $P_{\mathrm{Fe}l}$ are exactly equal which follows from the way they are calculated (see (13) to (16)). Friction losses $P_{\mathrm{wf}l}$ are almost equal. Differences can be explained by measurement errors of the torque and errors in the

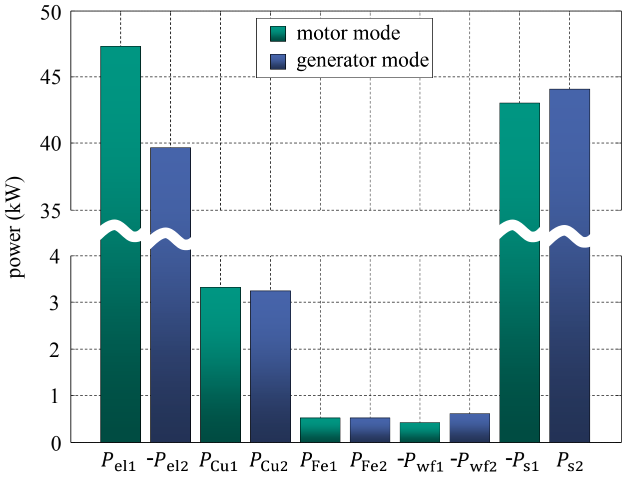

Fig. 6 Measurement results evaluated with (9) to (23) for the operation point i_{d1} = -343 A and i_{q1} = 198 A at 2000 min^{-1}. This yields i_{d2} = -336 A and i_{q2} = -200 A and motor efficiencies of $\eta_1 \approx \eta_2 \approx$ 90.5 %.

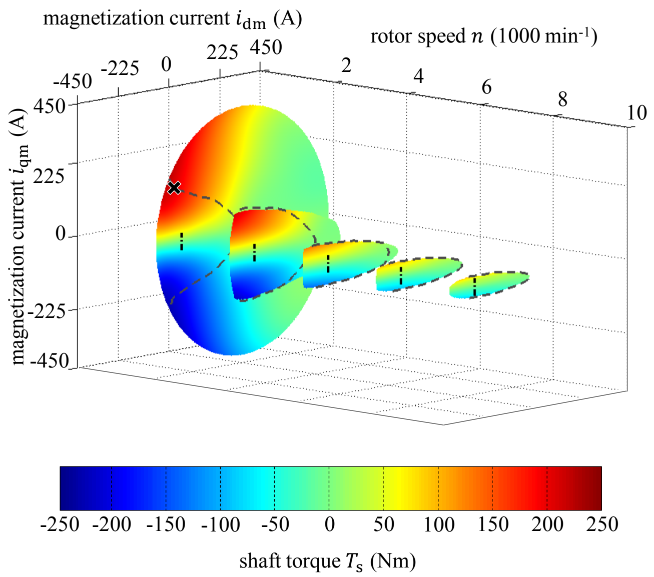

Fig. 8 Three-dimensional plot of the measured shaft torque in dependence on machine current and rotor speeds. The motor mode operation point of Fig. 6 is marked by a black cross. The black lines (−·−) indicate a current path with $i_{dm} = -300$ A and $i_{qm} = -30$ A … 30 A used in Fig. 9. The grey lines (---) show the computed current reference values of minimal amplitude used for Fig. 10.

determination and control of the induced voltages due to a thermal drift.

Falsifications due to thermal drift are low. This is the case because the standard deviations of the temperature sensitive stator resistance and permanent flux linkage are low during characterization. Measurements of the ohmic resistance yield an average of 14.1 mΩ with a standard deviation of 0.14 mΩ (stator windings including supply lines). Measurements of the permanent magnet flux linkage calculated from the open circuit voltage result in an average of 63.9 mVs with a standard deviation of 0.61 mVs. Both standard deviations are only about 1 % of their respective mean values.

If all operation points at one distinct rotor speed are analyzed, the measurement results can be displayed in the dq-current plane as depicted in Fig. 7. There, all measurements are marked with dots while the motor mode operation point of Fig. 6 is highlighted by a black cross. The absolute value of the quadrature flux linkage $|\Psi_q|$ is color coded. Data in between measurement points is calculated using biharmonic interpolation. Since the influence of iron losses is considered in the identification method, the flux linkage $|\Psi_q|$ is obtained mirror symmetrically to the direct axis. The influence of cross-coupling is clearly visible in the plot as lines of constant quadrature flux linkage are distorted with changing direct axis magnetization current i_{dm}.

The complete operation area given by multiple rotor speeds and arbitrary combinations of direct and quadrature currents can be illustrated in a three-dimensional graph as given in Fig. 8. There the shaft torque T_s is color coded for all operation points (i_d, i_q, n) with n as rotor speed. The hyperbolas of constant torque and the ellipsoids caused by the inverter voltage limit are clearly visible.

First the same current operation points are analyzed at different rotor speeds. This is exemplarily done for operations points on the black line (−·−) depicted in Fig. 8. They mark a current path with changing quadrature but constant direct magnetization current at all measured rotor speeds. The respective iron losses are given in Fig. 9. As expected, iron losses both increase with rotor speed and with a rising absolute value of quadrature magnetization current.

Second, reference values with minimal current

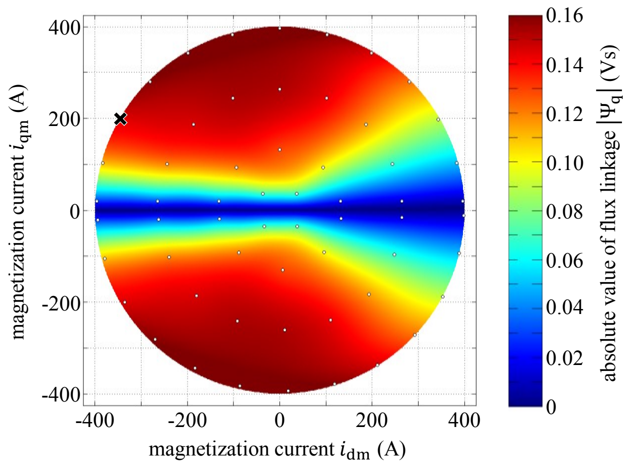

Fig. 7 Absolute value of the quadrature flux linkage in dependence on the currents measured at 2000 min^{-1}. The dots indicate measurement points. The motor mode operation point of Fig. 6 is marked by a black cross.

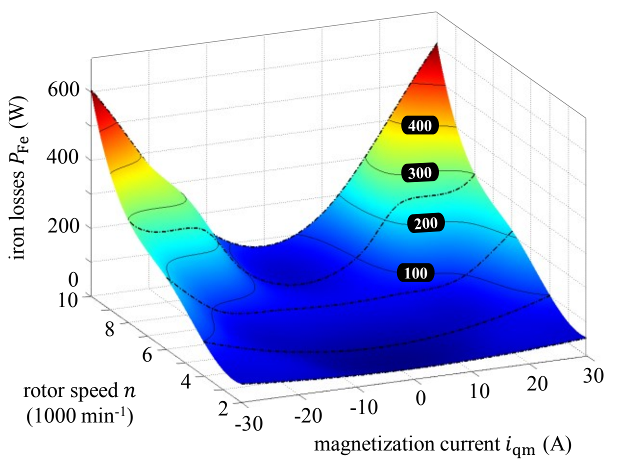

Fig. 9 Measured iron losses P_{Fe} at a constant direct magnetization current $i_{dm} = -300$ A as indicated by the black line in Fig. 8. Iron losses increase with the absolute value of the flux linkage and the rotor speed.

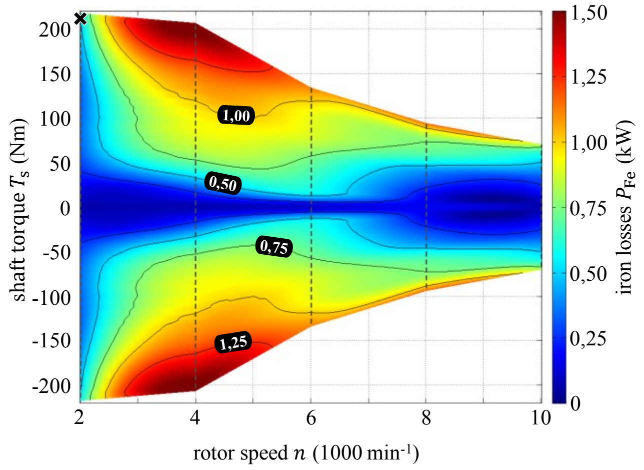

Fig. 10 Measured iron losses P_{Fe} of the machine as a function of shaft torque T_s and rotor speed n when control with minimal current amplitude is used. The black cross marks the motor mode operation point of Fig. 6.

amplitude for all shaft torques with respect to the voltage constraint [13] are calculated for the measured rotor speeds. The results are given by the grey lines (---) in Fig. 8. As a consequence, one torque value represents one current couple (i_d, i_q) and the dimension of Fig. 8 can be reduced by one. The measurement results can then be plotted in a two-dimensional graph in dependence on rotor speed and torque as given in Fig. 10. There the experimentally identified iron losses P_{Fe} are shown. Iron losses increase with the shaft torque and reach their maximal value at nominal speed at maximal torque. Due to different current reference values at different rotor speeds and the necessity of field weakening the iron losses do not always increase with the rotor speed.

It should be kept in mind that only a selection of results has been shown in Fig. 6 to Fig. 10. All quantities listed at the beginning of Section II *B* have been identified using the presented measurement and evaluation method and could therefore be thoroughly analyzed. Thereby, a deep understanding of the machine's magnetics and losses can be obtained.

V. Conclusion

A steady-state machine model of permanent magnet synchronous machines considering magnetic anisotropy, saturation, cross-coupling, iron and friction losses is introduced. All parameters necessary to describe the machine are identified experimentally by a novel technique. This technique is based on the combined evaluation of two operation points of corresponding magnetic states which is realized by controlling the currents in motor mode and the induced voltages in generator mode. Due to this principle not only the flux linkages are precisely determined, but also iron and friction losses can be identified and separated in each operation point.

Since all electric, magnetic and mechanic parameters are experimentally identified a multitude of studies is made possible: Different machines can be compared in detail or the influence of various materials and production parameters can be thoroughly investigated. This allows the identification of optimization potentials and therefore the realization of further improvements.

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VII. Biographies

Jan Richter was born in Heidelberg, Germany in 1984. He graduated from Karlsruhe Institute of Technology (KIT) with his diploma in 2011. In June 2011 he started to work as a doctoral candidate at the Institute of Electrical Engineering at KIT in the field of modelling, parameter identification and control of permanent magnet synchronous machines.

Andreas Dollinger was born in 1985 in Feuchtwangen, Germany. He received his B.Sc. and M.Sc. in electrical engineering from KIT in 2012 and 2014. He currently works on motor drive applications for electric and hybrid electric vehicles.

Martin Doppelbauer studied electrical engineering at the University of Dortmund in Germany and graduated 1995 with a doctorate thesis on the analytical calculation of universal motors. He worked in the industry from 1995 until 2010. Since 2011 he is professor at the Institute of Electrical Engineering (ETI) of the Karlsruhe Institute of Technology (KIT) where he holds a chair for Hybrid Electric Vehicles. Since 1996 Martin Doppelbauer also works in international standardization of industrial motors and holds several positions as chairman, convenor and speaker in DKE, CENELEC and IEC. He is actively involved in the Association for Electrical, Electronic & Information Technologies (VDE) and chairman of the "Fachbereich A1 - Electrical Machines and Drives".