7 On-line adaptation of the rotor time constant for IM drives

A typical problem of the field-orientated control consists of the system having to evaluate the actual value of the rotor flux without flux sensors through a model from the measurable terminal quantities of the motor and the speed (cf. section 4.4). The often used current-speed model contains the rotor time constant of the motor as an essential parameter whose exact knowledge influences decisively the quality of the control. This fact and the working point dependence of this parameter motivate the introduction of special measures to primarily compensate the temperature dependence of the rotor resistance. To achieve this, *two approaches are in principle conceivable*: Either *the rotor flux model can be completed by an on-line adaptation method* which corrects the rotor resistance permanently, or *the rotor flux is estimated by an observer* which is insensitive against variations of the rotor resistance. The first approach is subject of this chapter.

In the first section the range and effects of temperature-dependent changes of the rotor resistance on other characteristic quantities are examined. A summary of published compensation methods follows. Thereafter *adaptation methods with a parametric error model* are discussed in greater detail. Such on-line adaptation methods use error models for the tracking of the parameters which in turn contain at least another two machine parameters. Their precision therefore also influences the quality of the field orientation. Thus these dependencies form a further main emphasis in the discussion besides *adaptation dynamics and problems of the adaptation in the non-stationary operation*.

7.1 Motivation

When using the i_s-ω flux model in field coordinates (cf. section 4.4) the amplitude and phase angle of the rotor flux linkage (model quantities indicated with ^) are:

$$\frac{d\hat{i}_{md}}{dt} = \frac{1}{T_r}\left(-\hat{i}_{md} + \hat{i}_{sd}\right) \tag{7.1}$$

$$\dot{\hat{\vartheta}}_s = \hat{\omega}_s = \omega + \frac{\hat{i}_{sq}}{T_r\,\hat{i}_{md}} \tag{7.2}$$

with: $\hat{i}_{md} = \dfrac{\hat{\psi}_{rd}}{L_m}$

Thus the rotor time constant T_r is obviously the decisive parameter for both dynamics and precision. Assuming an exact initial setting and the possibility of an exact modelling of the rotor inductance, the rotor resistance R_r remains as not predictably variable parameter. Considering the temperature coefficient and the possible change of the rotor temperature it can be shown that a resistance variation of about 50% has to be expected during operation. This undoubtedly causes a loss of quality in the system behaviour. The size of it and its tolerability or non-tolerability shall be examined in the following. The following criteria will be analyzed:

- Stationary torque and flux deviation (or difference).
- Linearity between torque and torque-producing quantity (the torque-forming current component).
- Dynamics of torque impression.

A faulty rotor time constant generates according to (7.2) a flux phase error and thus a phase difference between model current and motor current in the consequence:

$$\mathbf{i}_s = \hat{\mathbf{i}}_s e^{j\tilde{\vartheta}_s}, \tilde{\vartheta}_s = \hat{\vartheta}_s - \vartheta_s \tag{7.3}$$

After solving into components, the equation (7.3) can be written as follows:

$$\begin{aligned} i_{sd} &= \hat{i}_{sd}\cos\tilde{\vartheta}_s - \hat{i}_{sq}\sin\tilde{\vartheta}_s \\ i_{sq} &= \hat{i}_{sq}\cos\tilde{\vartheta}_s + \hat{i}_{sd}\sin\tilde{\vartheta}_s \end{aligned} \tag{7.4}$$

Because the speed is measured and the slip has to adjust to the existing load after dissipation of all transient processes, the slip values in the model and motor are identical in stationary operation, and therewith the next equation is valid according to (7.2):

$$\frac{i_{sq}}{i_{sd}\,T_r} = \frac{\hat{i}_{sq}}{\hat{i}_{sd}\,\hat{T}_r} \tag{7.5}$$

Using (7.4) and (7.5) the phase error $\tilde{\vartheta}_s$ can be calculated as follows:

$$\tan\hat{\vartheta}_s = \left(\frac{T_r}{\hat{T}_r} - 1\right)\frac{\hat{i}_{sd}\,\hat{i}_{sq}}{\hat{i}_{sd}^2 + \frac{T_r}{\hat{T}_r}\hat{i}_{sq}^2} \tag{7.6}$$

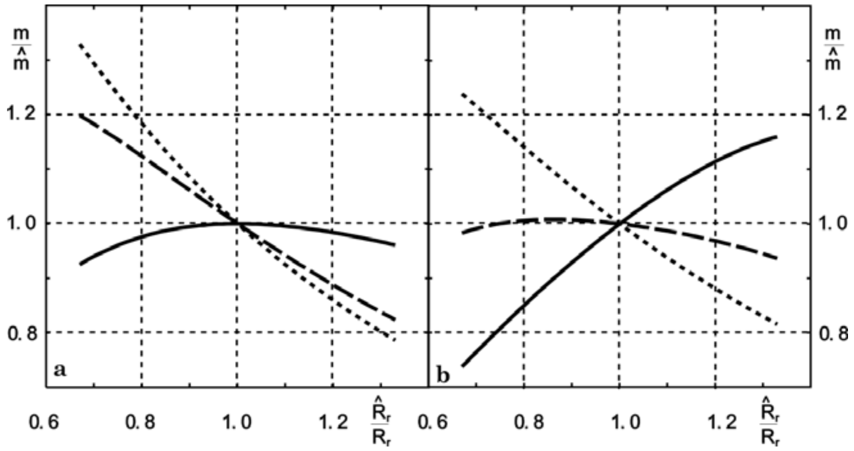

Fig. 7.1 Torque errors caused by inaccurate rotor resistance: a) without main field saturation; b) with main field saturation (—— $\hat{i}_{sq} = \hat{i}_{sd}$, ------ $\hat{i}_{sq} = 2\hat{i}_{sd}$, ······ $\hat{i}_{sq} = 3\hat{i}_{sd}$)

With the help of the stationary torque equation:

$$m_M = \frac{3}{2} z_p \frac{L_m^2}{L_r} i_{sd} i_{sq} \tag{7.7}$$

relations can now be derived for the stationary torque and flux amplitude deviation. After some intermediate steps the following formulae will be obtained:

$$\frac{m_M}{\hat{m}_M} = \frac{L_m^2}{L_r}\frac{\hat{L}_r}{\hat{L}_m^2}\frac{T_r}{\hat{T}_r}\frac{1+(\omega_r\hat{T}_r)^2}{1+(\omega_r T_r)^2} \tag{7.8}$$

$$\frac{\psi_r}{\hat{\psi}_r} = \frac{L_m}{\hat{L}_m}\sqrt{\frac{1+(\omega_r\hat{T}_r)^2}{1+(\omega_r T_r)^2}} \tag{7.9}$$

After inserting (7.4) and (7.6) into the torque equation (7.7), the torque characteristic $m_M(\hat{i}_{sq})$ will be obtained assuming constant $\hat{i}_{sd}$:

$$m_M(\hat{i}_{sq}) = \frac{3}{2} z_p \frac{L_m^2}{L_r}\frac{T_r}{\hat{T}_r}\hat{i}_{sd}\,\hat{i}_{sq}\frac{\hat{i}_{sd}^2 + \hat{i}_{sq}^2}{\hat{i}_{sd}^2 + \left(\frac{T_r}{\hat{T}_r}\right)^2\hat{i}_{sq}^2} \tag{7.10}$$

The equation (7.8) is graphically represented with and without the consideration of the main field saturation in the figure 7.1 with the data of an 11kW standard motor. Without saturation the equation (7.8) does not contain any further machine parameters apart from the rotor time constant deviation and therefore describes a generally valid relation. With the consideration of the saturation there is also $\hat{L}_m \neq L_m$ for a wrong rotor time constant in the model because of $\hat{i}_\mu \neq i_\mu$. As shown in the figure, the saturation will have a weakening influence on the torque error at larger load. The reason is that the fraction in equation (7.8) and the terms before it describe contrary trends of the torque deviation. One of them predominates depending on the load, also an approximated compensation is possible, as in figure 7.1b for $\hat{i}_{sq} = 2\hat{i}_{sd}$.

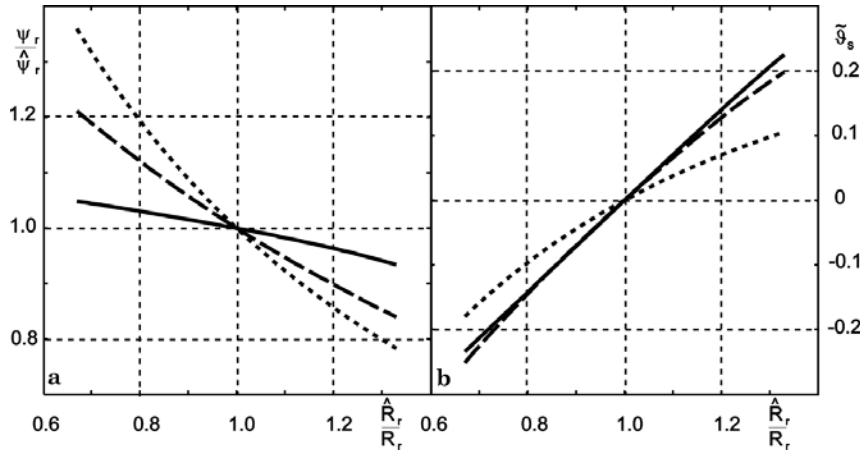

Fig. 7.2 a) Flux amplitude errors, b) Flux phase errors: by R_r deviation (with magnetic saturation); —— $\hat{i}_{sq} = \hat{i}_{sd}$; ------ $\hat{i}_{sq} = 2\hat{i}_{sd}$; ······· $\hat{i}_{sq} = 3\hat{i}_{sd}$

The size of the torque deviation is approximately half as large as the model resistance error at nominal operation and therefore actually remarkable. Whether a too small or too big model resistance represents the more critical case can be recognized in connection with the flux deviation. The corresponding characteristics using (7.9) and (7.6) are shown in figure 7.2.

For a speed controlled drive the motor torque to be produced will correspond to the load torque in any case. If the rotor flux is weakened by a wrong orientation, a higher current must be applied to achieve the demanded torque which can possibly exceed (at full load) the maximum inverter current and then leads to premature breakdown or the drive not reaching its rated speed. According to figure 7.2a this is the case for a too big model resistance. With a too small model resistance, a flux increase will follow which at corresponding speed causes a premature approaching the voltage limit. It is possible that the reference speed can not be reached at nominal torque, and the error of the rotor time constant leads to a reduction of the available power. Because the drives are usually designed with a current reserve on the inverter side, but the voltage of the DC link cannot be increased beyond a certain limit, the second case (small model resistance) has to be classified as the more critical one.

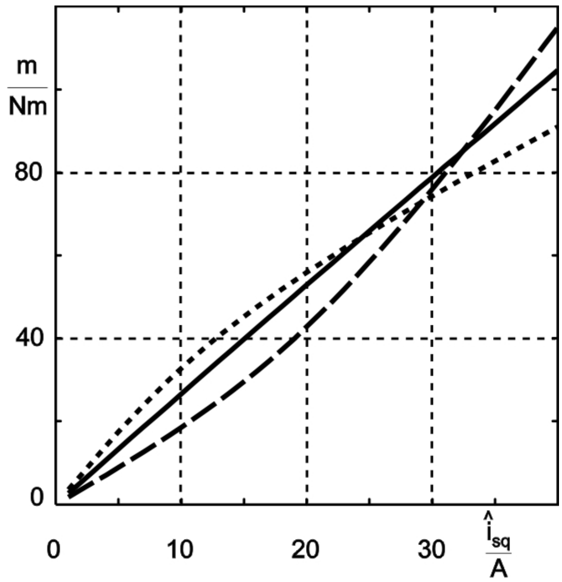

Fig. 7.3 $m_M(\hat{i}_{sq})$ characteristic: — $\hat{R}_r = R_r$, --- $\hat{R}_r = 0.66R_r$, ···· $\hat{R}_r = 1.33R_r$

The depicted area of the T_r deviation is approximately within the temperature-dependent limits which can be practically expected, if with regard to the initial settings of T_r the following two cases are considered: On one hand an initial setting on the cold machine, in which case an increase of the rotor resistance by 50% has to be taken into account during operation; and on the other hand an initial setting on the medium-warm machine with an operation dependent resistance change of ±25%.

For the pictures 7.1 and 7.2 three different load cases were analyzed in which the largest load approximately corresponds to the rated torque.

For a speed-controlled drive without high dynamic and precision demands the appearing flux and torque errors are probably tolerable. A superimposed speed control will compensate stationary torque errors. With adjustment on the warm machine an unintentional flux increase and power reduction will be avoided. However, depending on technical conditions and demands on the drive quality and on the intended optimization goals the expected errors could become too large and therefore no more acceptable. Such cases can be:

- The current reserve of the inverter is so small (or there is no overrating at all) that a flux weakening caused by wrong orientation really leads to a prematurely reaching of the current limit.
- An exact control of the state variables at variable rotor flux and in the field weakening becomes impossible.
- Drives which require an exact torque impression cannot be designed without additional measures. This becomes clear also by the stationary characteristic $m_M(\hat{i}_{sq})$ in figure 7.3.
- The recommended operation to avoid power reduction with a reduced flux automatically leads to an increase of the slip, and thus to a worse efficiency.

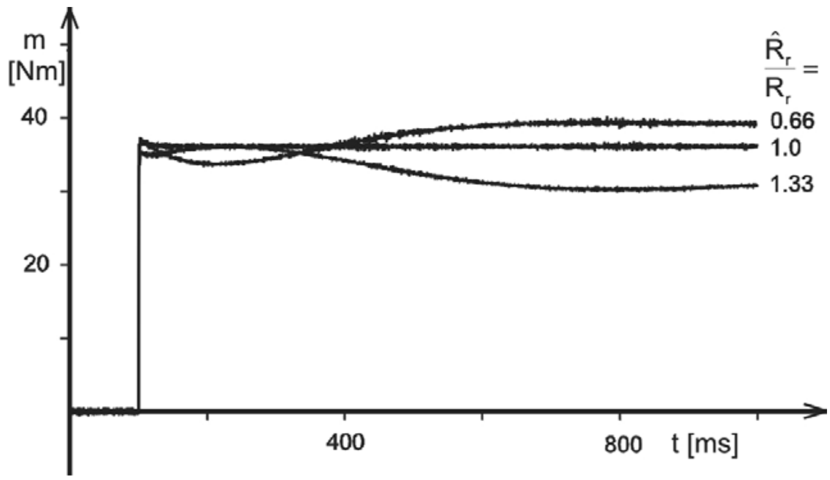

Fig. 7.4 Dynamic torque impression at faulty adjustment of T_r

The figure 7.4 shows the influence of a wrongly adjusted rotor time constant on the dynamic torque impression in the constant flux area. Because the rotor flux remains constant in the first instance after the reference step, the actual torque responds non-delayed in the first place as in the case of correctly adjusted parameters. The following settling process is determined by the transient of the rotor flux and is finished if the rotor flux also has reached its new stationary state.

Thus the consequences of a wrong adjustment are less serious in the dynamic case in the basic speed range. *Altogether, the stationary torque and flux errors represent the more serious effects and ask for the search of compensation measures in high quality and highly utilized drives.*

A high-dynamic torque impression is not conceivable in the field weakening or at low voltage reserve without an exact machine model, though. These issues will be discussed in chapter 8 more thoroughly.

7.2 Classification of adaptation methods

Because of the significance of an online adaptation of the rotor time constant outlined in the previous section these problems are a standard topic of the pertinent technical literature with a mass of papers since the first publications about FOC. An overview is found in [Krishnan 1991] for example.

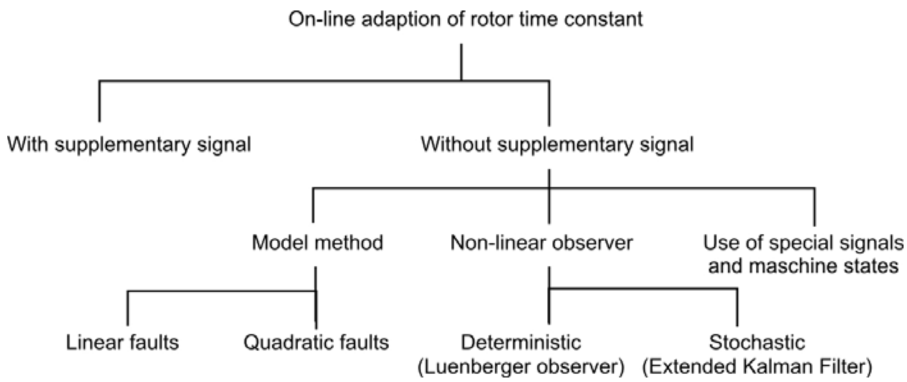

Fig. 7.5 Systematization of the methods for online adaptation of the rotor time constant

Because of the variety of different methods, only a certain group, namely the model methods with different kinds of error signals, shall be dealt with in detail subsequently. At first, a general survey will be worked out comprising a systematization and summaries of characteristic features to give to the reader a broader insight and the possibility to analyze the subject more deeply with the help of secondary literature. The figure 7.5 only shows a rough classification. In this picture only adaptation methods which work without physical changes on the motor (additional windings or the like) are included.

a) Methods using additional signal injection

The classic method in this group was published in [Gabriel 1982]. It uses the property of the rotor flux process to low-pass filter high-frequency disturbances in the flux forming current and therefore to keep the torque un-effected from such signals. But this is only valid in the case of exact field orientation, if the flux forming current does not directly contribute to torque production. If consequences of a high-frequency pseudo-noise signal injected on the flux forming current are provable in the torque (by correlation calculation), the field orientation is not exact and the noticed error can be used for the correction of the rotor time constant. A similar approach is used in [Nomura 1987]. A higher-frequency sine-wave signal, however, is fed into the *d* axis instead of the noise signal.

In the method described in [Chai 1992] with spectrum analysis the supplementary signal is added to the reference value of the rotor flux. At the same time, flux and torque forming current components are controlled in such way that no disturbance of the torque takes place. The suitable choice of the harmonic frequencies to be evaluated in relation to the stationary stator frequency enables an on-line emulation of the classical short circuit and no-load tests using simple algebraic equations for the parameter calculation following digital Fourier transformation of the measurement values. The method was used for the online adaptation of resistances and leakage inductances. Because of the harmonics produced additionally by the saturation, an estimation of the main inductance is not possible.

The method [Sng 1995] which was especially developed for extremely low speeds works similarly. A MRAS estimator for the rotor time constant is combined with an on-line estimation for resistances and leakage inductances which is excited by a high-frequency sine-wave signal.

A method which uses harmonics produced by the inverter as excitation was finally published in [Gorter 1994]. These harmonics are in the range of 300....600Hz. The rotor resistance, the leakage and main inductances are online-identified using the RLS method. The required linear machine model was derived by transition into rotor coordinates and use of a stator current - stator flux model.

b) Methods using models

The methods of this group work according to the model reference principle. A physical quantity of the motor is calculated by two different and independent models, and an error signal is derived from the output signals of both models. This error signal works as a driving quantity of an adjusting controller which corrects on-line the rotor resistance, rotor time constant or other parameters as well. Of course, the designed error signal must depend on the parameter to be estimated in a way which supports an unambiguous tracking. Input quantities of the models are measured terminal quantities of the machine, whereby a sub-model can immediately be identical with a measured quantity. In different ways stability considerations can be included, for example through an observer approach or by exploiting the theory of model reference adaptive systems (MRAS).

The methods described in the literature differ from each other primarily by the choice of the physical quantity used for the calculation of the model error. Furthermore it can be distinguished between the linear and quadratic error signals. Linear error signals are formed from stator current components [Pfaff 1989], [Reitz 1988] (in this publication all parameters are adapted by an adaptation law designed according to the Gauß-Newton method), stator voltage [Dittrich 1994], [Rowan 1991], motor EMF [Kaźmierkowski] or rotor flux [Ganji 1995]. Also the method described in [Fetz 1991], which works with a field-orientated open-loop current control and an adaptation signal derived from the output signal of a current by-pass controller implicitly uses the stator voltage components as reference values. Estimated and measured stator current trajectories are compared in [Holtz 1991] to calculate all machine parameters and rotor current components by using a gradient method.

Quadratic error signals can be derived from the amplitude [Rowan 1991] or the phase angle [Schumacher 1983] of the stator voltage or the motor EMF, from the air gap power [Dolal 1987], the active and/or reactive power [Dittrich 1994], [Koyama 1986], [Summer 1991], [Summer 1993], from the electrical torque [Lorenz 1990], [Rowan 1991], the magnitude of the stator flux [Krishnan 1986] or from especially designed signals [Vucosavić 1993], [Weidauer 1991].

c) Non-linear observers

These estimators also could be assigned to the methods with additional signal injection as far as extended Kalman filters (EKF) are used for the parameter estimation, because here the harmonics produced by the pulse-width modulated voltage are partly used as excitation signal [Zai 1987]. For the classification carried out at this place the observer approach shall play, however, the decisive role.

Compared with simple model methods, an observer approach offers the possibility of predicting the dynamic behaviour of the adaptive system in certain limits, and of targeted adapting the feedback matrix. Furthermore, certain properties like the robustness of the system, can be influenced by the suitable choice of the feedback matrix. The parameter adaptation is a by-product to the actual task of the observer, the flux estimation.

When state observers are used for parameter estimation at the same time, non-linear or extended observers [Zeitz 1979] arise. A complete observer for the electrical quantities of the induction machine with inclusion of one parameter would have the order of five. Because the currents usually are being measured, the order of a reduced observer (flux and one parameter) is cut down to three, and the realization expenditure is substantially more favourable. The observer error essentially corresponds to the stator voltage component error model mentioned above. Observers of reduced order with parameter adaptation are described in [Dittrich 1998], [Nilsen 1989], [Schrödl 1989].

As opposed to Luenberger observers, Kalman Filters (KF) or Extended Kalman Filters (EKF) take into account stochastic uncertainties of the system and measuring errors for a combined state and parameter estimation. As already mentioned they can also work with stochastic input signals. The realization effort is, however, considerable. Although the asynchronous machine represents a deterministic system, a number of papers have been published on the application of EKF [Atkinson 1991], [Loron 1993], [Pena 1993]. [Du 1993] is to mention as an interesting contribution on the topic of applying extended observers or EKF's.

d) Evaluation of special signals and machine states

All methods which work without injection of an additional signal and can not be assigned to other groups shall be assigned here. So [Vogt 1985] evaluates the speed oscillations caused by torque vibrations from an inaccurately adjusted rotor time constant. The method described in [Hung 1991] calculates the rotor flux and a correction signal for the rotor time constant from the third voltage harmonic caused by the magnetic saturation, and therefore independent of rotor parameters, this under the assumptions of an exact voltage measurement, operating the motor in the saturation and star-connection of the windings.

An essential weakness of the methods with additional signals is certainly the influence on the normal operation which can really have a disturbing effect, even if the torque remains undisturbed as indicated in [Chai 1992]. The adaptation can be carried out only in the stationary operation; a general proof of stability is barely possible. Furthermore great care is required to ensure that only answers to the excitation signals are actually evaluated and no harmonics and disturbances caused by other influences (saturation, mechanical oscillation). On the other side, an identification of the rotor parameters is also possible in no-load operation [Chai 1992] with appropriate design of the excitation signal.

This is fundamentally impossible for methods without additional signal. Furthermore it cannot be assumed that the signals appearing in the normal operation have an adequate information content suitable to carry out a multi-parameter identification (what is not intended in the context of this chapter, though). Error models for the identification of the rotor time constant always contain other machine parameters which decisively influence the precision of the adaptation. On the other hand an adaptation is conceivable and theoretically also possible in the dynamic operation. The system behaviour including stability can be designed and assessed in an uniform approach, at least with certain limitations (e.g. partial linearization).

7.3 Adaptation of the rotor resistance with model methods

In this section some approaches from the group of the model methods shall be discussed in more detail, whereat for design and stability analysis principles of the nonlinear observer theory will be applied. Linear and quadratic fault models (reactive power) are included. The online adaptation is focused on the compensation of temperature variations and therefore on the rotor resistance. The state variable dependent main inductance is adjusted in feed-forward mode. If the adaptation is implemented primarily for the optimization of the stationary operation, an immediate tracking of the rotor time constant as a whole is also conceivable and sufficient.

The observer is designed from a linearized process model based on a local approach of the system at small state errors. This approach is justified because a state observer is designed for the purpose to keep deviations minimal between observer and system state variables. Prerequisite is that the initial values of the observer states are chosen accordingly, i.e. close to the actual system states.

All fault models contain besides the rotor resistance at least two further machine parameters whose precision fundamentally influences the adaptation error and with that the precision and stability of the adjustment. For this reason corresponding sensitivity studies will occupy a relatively wide room in the following considerations.

In principle it is possible to implement the online adaptation like the flux model in arbitrary coordinate systems. But because the flux model was already established in the rotor flux orientated coordinate system, and thus the rotor flux is immediately available in this system, the adaptation methods are also designed in field-orientated coordinates.

7.3.1 Observer approach and system dynamics

As already indicated, the adaptation algorithm shall be designed using the theory of non-linear observers. This approach has the substantial advantage that the adaptation dynamics and stability can be examined in a uniform design procedure. The design is carried out for a linearized system in quasi-stationary operation. The essential design prerequisites are:

- The observer is designed exclusively for the rotor resistance, therefore being the only state quantity.
- For the analysis of the observer dynamics the steady-state condition with regard to the rotor flux vector is assumed:

$$i_m = i_{sd}\,,\ \hat{i}_m = \hat{i}_{sd}\,,\ \tilde{\vartheta}_s = \hat{\vartheta}_s - \vartheta_s = \text{const} \tag{7.11}$$

It has to be made sure for the functionality of this approach that the rotor resistance observer is assigned a sufficiently slow dynamics ensuring a dynamic decoupling to the remaining system. For the compensation of thermal resistance changes such a dynamics is completely sufficient.

With these prerequisites, for the system state and output equations can be written (for clarity the current time step k is written as an index in the following equations):

$$\begin{aligned} R_{r,k+1} &= R_{r,k} \\ \mathbf{y}_k &= \mathbf{h}(R_{r,k}, \mathbf{u}_k) \end{aligned} \tag{7.12}$$

Here $\mathbf{y}$ is the output vector, and $\mathbf{u}$ is the input vector of the system. The output equation represents the equation of the later error model. Therefore the distinction between input and output quantities has not to be understood in the strictly literal meaning. Both vectors are assumed multi-dimensional in the general case.

The observer for this system is formulated as extended Luenberger observer [Brodmann 1994], [Zeitz 1979]. It consists of a model of the system and a linear, but state variable and time dependent feedback of the output error (difference between model output and system output vector) to the observer state:

$$\begin{aligned} \hat{R}_{r,k+1} &= \hat{R}_{r,k} - \mathbf{k}(\hat{R}_{r,k}, \mathbf{u}_k)^{\mathsf{T}} \left(\hat{\mathbf{y}}_k - \mathbf{y}_k\right) \\ \hat{\mathbf{y}}_k &= \mathbf{h}(\hat{R}_{r,k}, \mathbf{u}_k) \end{aligned} \tag{7.13}$$

The equation (7.13) corresponds to a general adaptation approach with the adaptation fault

$$\varepsilon_k = -\mathbf{k}(\hat{R}_{r,k}, \mathbf{u}_k)^{\mathsf{T}} \left(\hat{\mathbf{y}}_k - \mathbf{y}_k\right) \tag{7.14}$$

in the structure of the figure 7.6. For the following design procedure two tasks remain:

- Specification of a fault model – subject of section 7.3.2.
- Design of the observer dynamics by determination of the weighting or feedback vector **k**.

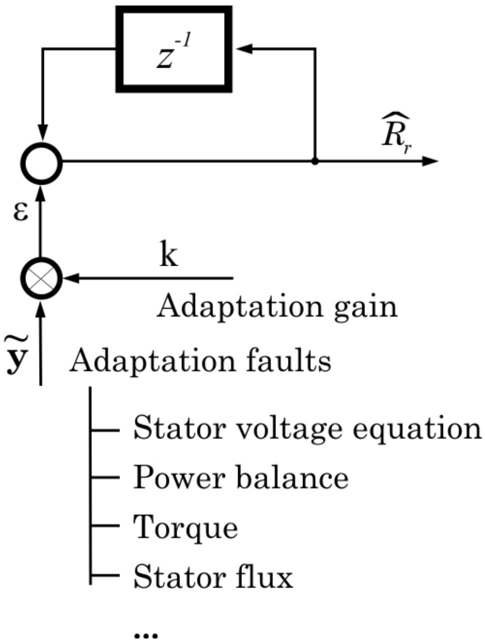

Fig. 7.6 Rotor resistance observer

The dynamic analysis is carried out using the difference equation of the observer error:

$$\widetilde{R}_{r,k+1} = \widehat{R}_{r,k+1} - R_{r,k+1} = \widetilde{R}_{r,k} - \mathbf{k}(\widehat{R}_{r,k}, \mathbf{u}_k)^{\mathsf{T}} \left(\hat{\mathbf{y}}_k - \mathbf{y}_k \right) \quad (7.15)$$

If suitable starting values which already are close to the system state are chosen for the observer state, being the rotor resistance, the design can be performed by a local analysis following the linearization around the model state. Then the equation (7.15) can be written as follows:

$$\widetilde{R}_{r,k+1} = \left(1 - \mathbf{k}^{\mathsf{T}} \left. \frac{\partial \mathbf{h}}{\partial R_r} \right|_{\widehat{R}_{r,k}, \mathbf{u}_k} \right) \widetilde{R}_{r,k} \quad (7.16)$$

From this a relation for the linearized output error can be derived by comparison with (7.13):

$$\tilde{\mathbf{y}}_k = \left. \frac{\partial \mathbf{h}}{\partial R_r} \right|_{\widehat{R}_{r,k}, \mathbf{u}_k} \widetilde{R}_{r,k} \quad (7.17)$$

The actual design of the fault dynamics is carried out with help of the characteristic equation of the linearized fault system. This is in the z domain:

$$z - 1 + \mathbf{k}^{\mathsf{T}} \left. \frac{\partial \mathbf{h}}{\partial R_r} \right|_{\hat{R}_{r,k}, \mathbf{u}_k} = 0 \tag{7.18}$$

or with (7.17):

$$z - 1 + \mathbf{k}^{\mathsf{T}} \frac{\tilde{\mathbf{y}}_k}{\tilde{R}_{r,k}} = 0 \tag{7.19}$$

From this the coefficients of the feedback vector **k** can be calculated using the corresponding terms for the special error models. The most important design goals consist of achieving a time-invariant and system signal independent fault dynamics and of obtaining a stable transient response by specification of a constant eigenvalue z_1. In addition, to satisfy the demand for adequately slow adaptation dynamics (dynamic decoupling to the remaining system) z_1 should fulfil the equation:

$$0 < 1 - z_1 \le \frac{1}{2} \frac{T}{\hat{T}_r} \tag{7.20}$$

For all simulation and test examples given subsequently the value of the upper limit was used respectively.

Because the observer is designed in field-orientated coordinates, the coordinate transformation is an integral component of the model. Therewith actual input quantities are currents and voltages in stator-fixed coordinates. If the observer equations are formulated and designed nevertheless in field-orientated coordinates, it has to be taken into account that the transformation angle ϑ_s or the phase error $\tilde{\vartheta}_s$ is a function of the state error $\tilde{R}_r$. All currents and voltages in field coordinates depend implicitly on $\tilde{R}_r$. Therefore the equation for the linearized output error has to be extended to the complete error difference:

$$\tilde{\mathbf{y}} = \left. \frac{\partial \mathbf{h}}{\partial R_r} \right|_{\hat{R}_r} \tilde{R}_r + \left. \frac{\partial \mathbf{h}}{\partial \vartheta_s} \right|_{\hat{\vartheta}_s} \tilde{\vartheta}_s \tag{7.21}$$

Here $\tilde{\vartheta}_s$ is given by (7.6) for the interesting stationary case. For the relation between the phase error and the rotor resistance, it follows then in linearized form:

$$\tilde{\vartheta}_s = \left. \frac{\partial \tilde{\vartheta}_s}{\partial \tilde{R}_r} \right|_{R_r = \hat{R}_r} \tilde{R}_r = \frac{1}{\hat{R}_r} \frac{\hat{i}_m \hat{i}_{sq}}{\hat{i}_m^2 + \hat{i}_{sq}^2} \tilde{R}_r \tag{7.22}$$

Using

$$\mathbf{i}_s^f = \mathbf{i}_s^s \, \mathrm{e}^{-j\vartheta_s} \tag{7.23}$$

the next equation will be obtained for the derivative of the stator vector in field coordinates with respect to the phase angle:

$$\frac{\partial \mathbf{i}_s^f}{\partial \vartheta_s} = -j\mathbf{i}_s^s \, \mathrm{e}^{-j\vartheta_s} = -j\mathbf{i}_s^f \tag{7.24}$$

Analogously the following equation is valid for the stator voltage:

$$\frac{\partial \mathbf{u}_s^f}{\partial \vartheta_s} = -j\mathbf{u}_s^s \, \mathrm{e}^{-j\vartheta_s} = -j\mathbf{u}_s^f \tag{7.25}$$

7.3.2 Fault models

The stator voltage equations provide the first approach for the derivation of the output equation and in due course for the observer fault. In addition, the active and reactive power balance were chosen as an example for quadratic fault models. The reason for this special choice consists in the fact that characteristic model parameter constellations, which allow representative statements also for other methods, are produced in the context of these methods. These relations will be more exactly examined in the next section. At first the fault models shall be assembled, and the associated feedback coefficients shall be derived. The magnetic saturation remains so far unconsidered, and linear magnetic conditions or a constant rotor flux are assumed. If the saturation of the main inductance shall be taken into account for the adaptation in the field weakening area or for rotor flux transients, the corresponding relations from section 6.2.3 have to be used for the derivation of model equations.

7.3.2.1 Stator voltage models

In this case the system output equations can be derived immediately from the time-discrete stator voltage equation of the IM in field-orientated coordinates. To avoid the flux derivation it is started from the state equation with Euler discretization (cf. chapter 3):

$$\begin{aligned} \mathbf{u}_{s,k} = R_s \mathbf{i}_{s,k} &+ \frac{\sigma L_s}{T}\left(\mathbf{i}_{s,k+1} - \mathbf{i}_{s,k}\right) + j\omega_s \sigma L_s \mathbf{i}_{s,k} \\ &-(1-\sigma) R_r \left(\mathbf{i}_{m,k} - \mathbf{i}_{s,k}\right) + j\omega(1-\sigma) L_s \mathbf{i}_{m,k} \end{aligned} \tag{7.26}$$

Models which use voltage equations in d or q axis or as a combination of both components are practicable.

a) Stator voltage model in d axis

The output equation of the system is obtained by using the real component of (7.26):

$$y_{d,k+1} = \frac{1}{1-\sigma}\left[-u_{sd,k} + R_s i_{sd,k} + \frac{\sigma L_s}{T}\left(i_{sd,k+1} - i_{sd,k}\right) - \sigma L_s \omega_s i_{sq,k}\right] - R_r\left(i_{m,k} - i_{sd,k}\right) = 0 \tag{7.27}$$

and the output error follows to:

$$\hat{y}_d - y_d = \frac{1}{1-\sigma}\left[-\hat{u}_{sd,k} + R_s \hat{i}_{sd,k} + \frac{\sigma L_s}{T}\left(\hat{i}_{sd,k+1} - \hat{i}_{sd,k}\right) - \sigma L_s \omega_s \hat{i}_{sq,k}\right] - \hat{R}_{r,k}\left(\hat{i}_{m,k} - \hat{i}_{sd,k}\right) \tag{7.28}$$

To obtain the adaptation gain the linearized output error has to be calculated using (7.17) and (7.19). The calculation is carried out according to the assumption for the steady-state condition indicated by (7.11). The explicit indication of the current time step is omitted subsequently. Using (7.21), (7.24) and (7.25) it follows from (7.27):

$$\frac{\partial h}{\partial \vartheta_s} = \frac{1}{1-\sigma}\left(-u_{sq} + R_s i_{sq} + \sigma L_s \omega_s i_{sd}\right) = -\omega_s L_s i_m \tag{7.29}$$

Finally, the stationary linearized output error is found using (7.22):

$$\tilde{y}_d = -\omega_s \hat{T}_r \hat{i}_m \frac{\hat{i}_m \hat{i}_{sq}}{\hat{i}_m^2 + \hat{i}_{sq}^2} \tilde{R}_r \tag{7.30}$$

After substituting this expression into (7.19) the adaptation gain can now be calculated with the predefined eigenvalue z_1:

$$k_d = \frac{1 - z_1}{\omega_s \hat{T}_r \hat{i}_m \dfrac{\hat{i}_m \hat{i}_{sq}}{\hat{i}_m^2 + \hat{i}_{sq}^2}} \tag{7.31}$$

b) Stator voltage model in q axis

Analog to the *d* axis model the output equation immediately results from the imaginary component of (7.26),

$$y_{q,k+1} = \frac{1}{1-\sigma}\left[-u_{sq,k} + R_s i_{sq,k} + \frac{\sigma L_s}{T}\left(i_{sq,k+1} - i_{sq,k}\right) + \sigma L_s \omega_s i_{sd,k}\right] + \omega_s L_s i_{m,k} = 0 \tag{7.32}$$

with the output error:

$$\hat{y}_q - y_q = \frac{1}{1-\sigma}\left[-\hat{u}_{sq,k} + R_s \hat{i}_{sq,k} + \frac{\sigma L_s}{T}\left(\hat{i}_{sq,k+1} - \hat{i}_{sq,k}\right) + \sigma L_s \omega_s \hat{i}_{sd,k}\right] + \omega_s L_s \hat{i}_{m,k} \tag{7.33}$$

For the stationary linearized output error we obtain in the same way with:

$$\frac{\partial h}{\partial \vartheta_s} = \frac{1}{1-\sigma}\left(u_{sd} - R_s i_{sd} + \sigma L_s \omega_s i_{sq}\right) + \omega_s L_s i_{sq} = \omega_s L_s i_{sq} \tag{7.34}$$

the following equation:

$$\tilde{y}_q = \omega_s \hat{T}_r \hat{i}_{sq} \frac{\hat{i}_m \hat{i}_{sq}}{\hat{i}_m^2 + \hat{i}_{sq}^2} \tilde{R}_r \tag{7.35}$$

Accordingly the adaptation gain can be calculated as follows:

$$k_q = -\frac{1 - z_1}{\omega_s \hat{T}_r \hat{i}_{sq} \dfrac{\hat{i}_m \hat{i}_{sq}}{\hat{i}_m^2 + \hat{i}_{sq}^2}} \tag{7.36}$$

c) Voltage vector fault model

So far several approaches have already been suggested in the literature to combine both error components [Rowan 1991], [Schumacher 1983]. Thereat the amplitude or phase angle of the error vector was calculated. Furthermore it is conceivable to simply add both weighted error components derived above. However, this way shall not be followed here, because in spite of more information flowing into the adaptation the possibility to use the additional degree of freedom for dedicated weightings of the error components will be given away. The combination of the error components shall be aimed to define the dynamics with one weighting factor and to balance the contributions of both error components to the total error with a second factor. The addition of both components has to ensure that the sign of the total error (= adjustment direction of the rotor resistance) is only determined by the direction of the resistance deviation or the phase error, and not distorted by the combination of the error components. The following approach is chosen:

$$\begin{aligned} \varepsilon_{dq} &= k_{d1}(\hat{y}_d - y_d) - k_{q1}(\hat{y}_q - y_q)\operatorname{sign}\hat{i}_{sq} \\ &= k_{d1}\left[(\hat{y}_d - y_d) - k_{dq}(\hat{y}_q - y_q)\operatorname{sign}\hat{i}_{sq}\right] \\ & k_{d1} > 0\,, \quad k_{q1} > 0 \end{aligned} \tag{7.37}$$

The adaptation dynamics is determined by k_{d1}, and the error weighting by $k_{dq} = k_{q1}/k_{d1}$. By inserting (7.30) and (7.35) the linearized output error is obtained to:

$$\tilde{y}_{dq} = -\omega_s \hat{T}_r \left(k_{d1}\hat{i}_m + k_{q1}\left|\hat{i}_{sq}\right|\right)\frac{\hat{i}_m \hat{i}_{sq}}{\hat{i}_m^2 + \hat{i}_{sq}^2}\tilde{R}_r \tag{7.38}$$

At positive $\hat{i}_m$ the bracket term is always positive, and thus the above-mentioned condition is fulfilled. The adaptation gain can be calculated as follows:

$$k_{d1} = \frac{1 - z_1}{\omega_s \hat{T}_r \left(\hat{i}_m + k_{dq}\left|\hat{i}_{sq}\right|\right)\dfrac{\hat{i}_m \hat{i}_{sq}}{\hat{i}_m^2 + \hat{i}_{sq}^2}} \tag{7.39}$$

The derivation of a suitable value for k_{dq} is subject of section 7.3.3.

7.3.2.2 Power balance models

The first step at examination of an error model is to find out whether it is suitable for the rotor resistance adaptation at all. This is the case if the error signal proceeds steadily and there is an unambiguous connection between the variation of the rotor resistance and the sign of the model error. Particularly for error models of higher order these prerequisites are not obvious. With the method used here to analyze the adaptation problem with the help of the nonlinear state observer the corresponding proof can be adduced very comfortably.

Unlike to the previous models, for the power balance methods the system output vector is derived from the components of the complex power. Starting point is the equation of the complex power:

$$\mathbf{s} = \frac{3}{2}\mathbf{u}_s \mathbf{i}_s^* \tag{7.40}$$

Neglecting the factor 3/2 it follows in vector notation:

$$\mathbf{s} = \begin{pmatrix} p \\ q \end{pmatrix} = \begin{pmatrix} u_{sd} i_{sd} + u_{sq} i_{sq} \\ -u_{sd} i_{sq} + u_{sq} i_{sd} \end{pmatrix} \tag{7.41}$$

The output or model equations are obtained after inserting the voltage equations into (7.41). To simplify the representation they are written in the following in time-continuous form. As in the case of the stator voltage methods the dynamic analysis is carried out for steady-state condition regarding the rotor flux linkage and the stator current ($\mathrm{d}i_{sd}/\mathrm{d}t = \mathrm{d}i_{sq}/\mathrm{d}t = 0$).

a) Reactive power method

Following the described procedure the system output equation is:

$$\begin{aligned} y_b = u_{sd} i_{sq} - u_{sq} i_{sd} + \sigma L_s \left(\frac{\mathrm{d} i_{sq}}{\mathrm{d}t} i_{sd} - \frac{\mathrm{d} i_{sd}}{\mathrm{d}t} i_{sq} \right) + \\ + \omega_s L_s \left[\sigma i_s^2 + (1-\sigma) i_m^2 \right] \\ + (1-\sigma)\omega L_s i_m (i_{sd} - i_m) = 0 \end{aligned} \tag{7.42}$$

With:

$$\frac{\partial h}{\partial \vartheta_s} = 2(1-\sigma)\omega_s L_s i_m i_{sq} \tag{7.43}$$

the following expression is obtained for the stationary linearized error:

$$\tilde{y}_b = 2(1-\sigma)\omega_s \hat{T}_r \frac{\hat{i}_m^2 \hat{i}_{sq}^2}{\hat{i}_m^2 + \hat{i}_{sq}^2} \tilde{R}_r \tag{7.44}$$

The adaptation gain then will be:

$$k_b = -\frac{1 - z_1}{2\omega_s \hat{T}_r (1-\sigma) \dfrac{\hat{i}_m^2 \hat{i}_{sq}^2}{\hat{i}_m^2 + \hat{i}_{sq}^2}} \tag{7.45}$$

b) Active power method

The output equation arises as described:

$$\begin{aligned} y_w = -u_{sd} i_{sd} - u_{sq} i_{sq} + \sigma L_s \left(\frac{\mathrm{d} i_{sd}}{\mathrm{d}t} i_{sd} + \frac{\mathrm{d} i_{sq}}{\mathrm{d}t} i_{sq} \right) + \\ + R_s i_s^2 + (1-\sigma)\omega_s L_s i_m i_{sq} \\ + (1-\sigma) R_r i_{sd} (i_{sd} - i_m) \end{aligned} \tag{7.46}$$

For the stationary linearized output error and for the adaptation gain it can be derived in the same way as above:

$$\frac{\partial h}{\partial \vartheta_s} = (1-\sigma)\omega_s L_s \left(i_{sq}^2 - i_m^2\right) \tag{7.47}$$

$$\tilde{y}_w = (1-\sigma)\omega_s T_r \left(\hat{i}_{sq}^2 - \hat{i}_m^2\right) \frac{\hat{i}_m \hat{i}_{sq}}{\hat{i}_m^2 + \hat{i}_{sq}^2} \tilde{R}_r \tag{7.48}$$

$$k_w = -\frac{1 - z_1}{\omega_s \hat{T}_r (1-\sigma)\left(\hat{i}_{sq}^2 - \hat{i}_m^2\right) \dfrac{\hat{i}_m \hat{i}_{sq}}{\hat{i}_m^2 + \hat{i}_{sq}^2}} \tag{7.49}$$

It shall be noted that obviously the active power method cannot produce any observer fault in the area $i_{sq} \approx i_m$, and therefore its usefulness is limited substantially. Thus further consideration is abandoned.

7.3.3 Parameter sensitivity

All error models contain machine model parameters which practically always show a certain deviation compared to the actual motor parameters. This means, however, that at use of such a faulty model the rotor resistance too cannot be estimated exactly. The knowledge of the relations between model parameter errors and a wrong adjustment of the rotor time constant resulting from these errors is therefore of essential importance for the choice and assessment of an adaptation method. Such an analysis shall be carried out now for the error models introduced in the previous section.

The vector of the error model parameters is referenced by $\hat{\mathbf{p}}$, and the vector of the machine parameters by $\mathbf{p}$. At first the adaptation is looked at in stationary operation. Stability of the overall system assumed, the adaptation algorithm will regulate the adaptation error to zero in every case:

$$\lim_{t \to \infty} \varepsilon(\hat{u}_s, \hat{i}_s, \hat{\mathbf{p}}) = 0 \tag{7.50}$$

For a speed and rotor flux controlled system, the system state and the model state are unambiguously determined by the motor torque m_M (= load torque) and by the set point $\hat{i}_m^*$ (= $\hat{i}_m$) of the rotor flux linkage. The current component $\hat{i}_{sq}$ of the model is adjusted by the speed controller according to the required torque. The connection between system and model currents is given by (7.4). A statement about the model parameter dependent adjustment error of the rotor resistance can be obtained if an analytical connection:

$$\tilde{R}_r = f(m_M, \hat{i}_m, \tilde{\mathbf{p}}) \tag{7.51}$$

can be derived. Unfortunately, this is not possible in explicit form, though, so that a simulation study or an iterative calculation must be undertaken. A possible approach for the iterative solution consists in computing the resistance error $\tilde{R}_r$ for a given $\tilde{\mathbf{p}}$ for which the adaptation error ε becomes zero. This way the searched dependencies (7.51) can be determined point-wise.

In the first step the model current component $\hat{i}_{sq}$ shall be determined in dependence of m_M and $\hat{i}_{sd}$. For this purpose (7.4) and (7.6) are inserted into the torque equation (7.7). After some rewriting the following expression arises:

$$m_M \left[\hat{i}_{sq}^2 + \left(\frac{\hat{T}_r}{T_r} \right)^2 \hat{i}_m^2 \right] = \frac{3}{2} z_p \frac{L_m^2}{L_r} \frac{\hat{T}_r}{T_r} \hat{i}_m \hat{i}_{sq} \left(\hat{i}_{sq}^2 + \hat{i}_m^2 \right) \tag{7.52}$$

which can be solved to $\hat{i}_{sq}$ iteratively. After that the motor currents can be calculated by (7.4), and after inserting the result into the stator voltage equation the motor voltages will be obtained. With that the model voltages are finally found out also by (7.4) making all required quantities for the calculation of the model error complete. Unstable areas of the overall system are indicated by the fact that no solution $\varepsilon = 0$ for a given parameter mistuning exists. In a dynamic system simulation this case can be recognized by the fact that no stable stationary operating point will be reached.

The results of the sensitivity calculations for the error models introduced above are summarized in figure 7.7. They were obtained by using the data of an 11kW standard motor. The voltage vector error method still remains excluded in this figure because the second weighting factor to be determined shall specifically used for the robustness improvement. Although the validity of the results remains limited to the used motor, characteristic trends can be recognized, which result from the structure of the fault models and which also are transferable to other motors.

In the calculations the model parameters $\hat{R}_s$, $\hat{L}_m$ and $\sigma\hat{L}_s$ were varied, and the motor parameters were held constant. For the discrimination of the parameter influences only one parameter was changed at one time. Therefore it is practically definitely possible that divergent results arise by the overlapping of parameter errors. A parameter variation range of -

50%...+50% was examined for two characteristic rotational frequencies (1.6Hz and nominal speed) and two loads (nominal torque and half nominal torque). If a curve is not drawn over the full area, the overall control system reaches a stable operating point only in the marked area.

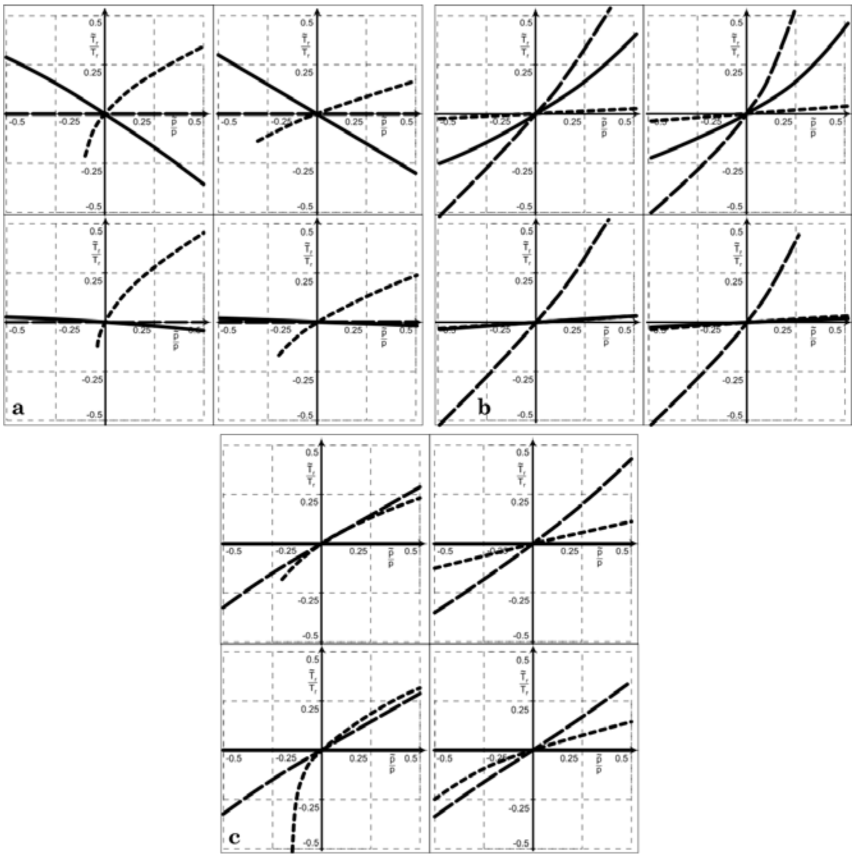

Fig. 7.7 (**a**) *d* axis voltage model, (**b**) *q* axis voltage model, (**c**) reactive power model: —— $\tilde{R}_s / R_s$, ---- $\tilde{L}_m / L_m$, ····· $\sigma\tilde{L}_s / \sigma L_s$, top: ω=10s^{-1}, bottom: ω=300s^{-1}, left: m=73 Nm, right: m=36 Nm

At first all parameter mistunings have an effect on the adjusted rotor time constant which thus represents a characteristic quantity representing the parameter errors. From this it can be concluded to flux amplitude and phase errors corresponding to section 7.1.

As a trend common to all measurements, it can be noticed that the sensitivity to the stator resistance drastically decreases at higher frequencies while the sensitivity to leakage and main inductance is only weakly or not frequency-dependent. This connection can be proved also arithmetically [Dittrich 1994]. In the models where the leakage inductance appears as a parameter (d axis voltage model and reactive power model) the influence of the leakage inductance is strongly load-dependent and a tendency towards a restriction of the stability area exists at increasing frequency and too small model values.

Among the examined methods the reactive power model proves to be the one with the most favorable robustness qualities despite the stability problems at leakage inductance errors. These can be avoided if the leakage inductance of the model is prevented from becoming smaller than the leakage inductance of the motor. The complete independence on the stator resistance must be highlighted as particularly positive.

At a more exact comparison of the results for the voltage models two facts can be noticed: The stator resistance sensitivity curves show a contrary trend, and at higher frequencies the main inductance or the leakage inductance determine the sensitivity characteristic, with their plots stretching from the third to the first quadrant.

From that it can be concluded, that it is possible to compensate the sensitivity to R_s almost completely if a combination of both fault models according to the voltage vector fault model established in the previous section is used. Only a certain balance of the sensitivity to the inductances will be reached, though, thereat the primary objective consists in extending the stability area. For the complete compensation of the R_s sensitivity the factor k_{dq} in (7.37) should be chosen to:

$$k_{dq} = \frac{\hat{i}_{sd}}{\hat{i}_{sq}} \tag{7.53}$$

The factor k_{dq} must approach this value at low stator frequencies. For weak load the weighting must be shifted to $(\hat{y}_d - y_d)$ because of the more favourable σL_s characteristics of the d-axis model, and at rising load and frequency a balanced weighting of both components should be obtained. One possibility to achieve this characteristic is provided by the following approach for the weighting factor k_{dq}:

$$k_{dq}(\hat{i}_s, \omega_s) = \frac{k_R \hat{i}_m + k_L \left|\hat{i}_{sq}\right|}{\hat{i}_m + \left|\hat{i}_{sq}\right| + k_L \hat{i}_m} \quad \text{and} \quad k_{dq} \le 1 \tag{7.54}$$

with $\quad k_R = 1 - 2\dfrac{|\omega_s|}{\omega_{sN}} \quad \text{and} \quad k_R \ge 0$

$$k_L = \frac{1}{2}\frac{|\omega_s|}{\omega_{sN}}\frac{\left|\hat{i}_{sq}\right|}{\hat{i}_m}\frac{I_0}{I_N}$$

The factor k_R has the effect that k_{dq} will be shifted in the direction (7.53) at low stator frequencies because k_L also is small at low frequencies. At rising load and frequency a balanced weighting of both fault components is reached by k_L.

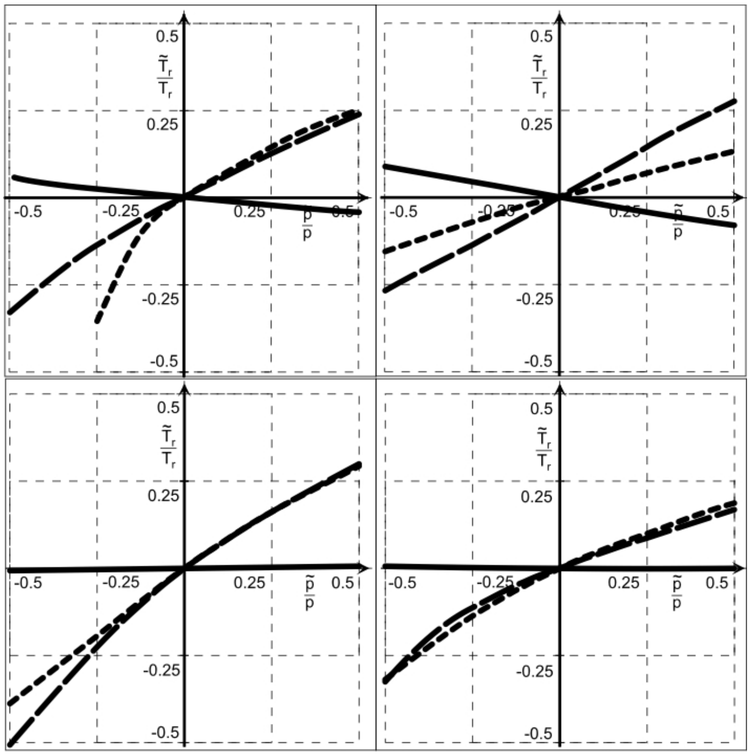

Fig. 7.8 Voltage vector fault model: —— $\tilde{R}_s / R_s$, ---- $\tilde{L}_m / L_m$, ····· $\sigma\tilde{L}_s / \sigma L_s$; top: $\omega = 10\text{s}^{-1}$, bottom: $\omega = 300\ \text{s}^{-1}$, left: m = 73 Nm, right: m = 36 Nm

The obtained results are represented in the figure 7.8. The stator resistance sensitivity is reduced drastically compared to the individual methods, and the stability area extended significantly. The balance between leakage and main inductance sensitivity can be described as optimal in the upper frequency area. Also compared with the reactive power method, a reduction of the leakage inductance sensitivity at high frequency and strong load and of the main inductance sensitivity at weak load is established.

Altogether it is recognizable, that the influence of stator resistance inaccuracies can be suppressed almost completely. Despite compensation measures, the model errors of the inductances should not become greater than 10%, though.

7.3.4 Influence of the iron losses

The iron losses are a parameter which generally falls into the category "neglected or negligible quantity" at modeling for control design. This is generally justified because the iron loss resistance lying virtually parallel to the rotor resistance (cf. section 6.2.1) is about 1000 times greater than the rotor resistance, and the consequences of the neglection still remain acceptable for the normal operation of the field-orientated control. With inverter feeding and accordingly higher eddy current losses however, significant amplitude and phase errors of the rotor flux are already provable [Levi 1994]. Furthermore, for some operating states and control goals the perspective renders fundamentally different. One of these cases is the adaptation of the rotor time constant.

At first this can clearly be explained from the stationary equivalent circuit. The iron loss resistance is located quasi-parallel to the slip-dependent resistance R_r/s (section 6.2.1). Thus not R_r but the parallel connection of both resistances is estimated in reality. Particularly at small slip values near the no-load operation R_{fe} reaches the range of R_r/s and influences the estimation result significantly.

In order to approach the problem quantitatively the equation system introduced in section 6.2.1 is now pursued further. After some transformations the following stator and rotor voltage equations in the Laplace domain will be obtained assuming $R_s \ll R_{fe}$ (G_{fe} ... iron loss conductance):

$$\mathbf{u}_s = \frac{R_s \mathbf{i}_s + \sigma L_s \mathbf{i}_s \left(s + j\omega_s\right) + (1-\sigma) L_s \mathbf{i}_m \left(s + j\omega_s\right)}{1 + G_{fe} \sigma L_s \left(s + j\omega_s\right)} \tag{7.55}$$

$$0 = \left[1 + \left(s + j\omega_r\right) T_r\right] \mathbf{i}_m - \mathbf{i}_s + G_{fe} \mathbf{i}_s \tag{7.56}$$

The rotor voltage equation can be solved into real and imaginary components:

$$i_m = \frac{i_{sd} - G_{fe} u_{sd}}{1 + sT_r} \tag{7.57}$$

$$\omega_r = \frac{i_{sq} - G_{fe} u_{sq}}{T_r i_m} \tag{7.58}$$

The disorientating influence of the iron losses on the field orientation is owed primarily by the G_{fe} term in the slip equation. The R_{fe} model error produces an additional phase error, which also exists at no-load conditions and overlaps the phase errors produced by a rotor resistance model fault. Because all error models eventually derive their output error from the rotor flux phase error, the rotor resistance estimator may yield completely wrong results.

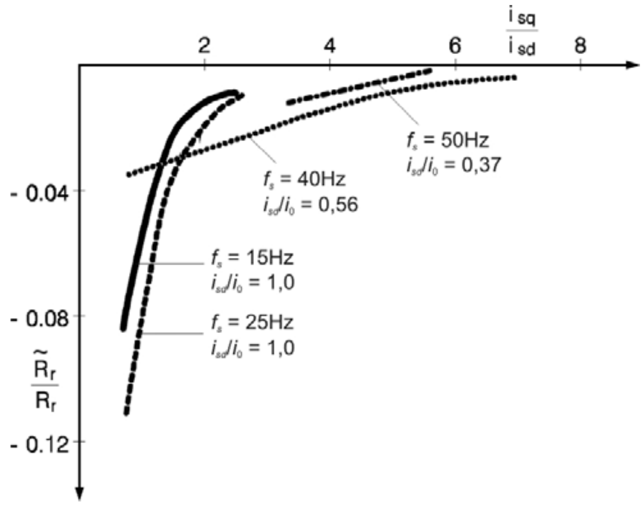

Fig. 7.9 Measured R_r estimation faults caused by iron losses

Some stationary measurements shall give a picture about the quantitative estimation error to be expected at different loads and stator frequencies. The results are shown in figure 7.9. For the R_r estimation the voltage vector error method is used. The ratio between torque and flux forming current components i_{sq}/i_{sd} serves as an equivalent for the motor load. It can clearly be recognized that the misadjustment is most critical at the upper limit of the constant flux area (greatest hysteresis losses). It diminishes considerably in the field weakening area, and also with increasing load. Altogether, it turns out that in stationary operation the estimation error can be safely kept below 4% if the adaptation is only allowed at current ratios of $i_{sq}/i_{sd} > 1,5$.

The conditions are more unfavorable in dynamic operation. The reason is that after starting a transient the error due to the rotor time constant difference is built-up delayedly, but the iron loss dependent error already exists in the no-load state. Therefore a restriction of the adaptation to appropriately great values of i_{sq} proves ineffective. Only the inclusion of the iron losses in the system equations according to the model in section 6.2.1 would make the additional error disappear completely.

7.3.5 Adaptation in the stationary and dynamic operation

At first the adaptation algorithms were developed for the stationary operation of the drive regarding current and rotor flux, and the parameter sensitivity examinations were also carried-out for this operation mode. Therefore it is interesting to examine, whether the methods can work stably also in dynamic operation and are able to adapt the rotor resistance. The discussion of the influence of the iron losses has already shown that different properties have to be expected in dynamic operation with regard to parameter sensitivity.

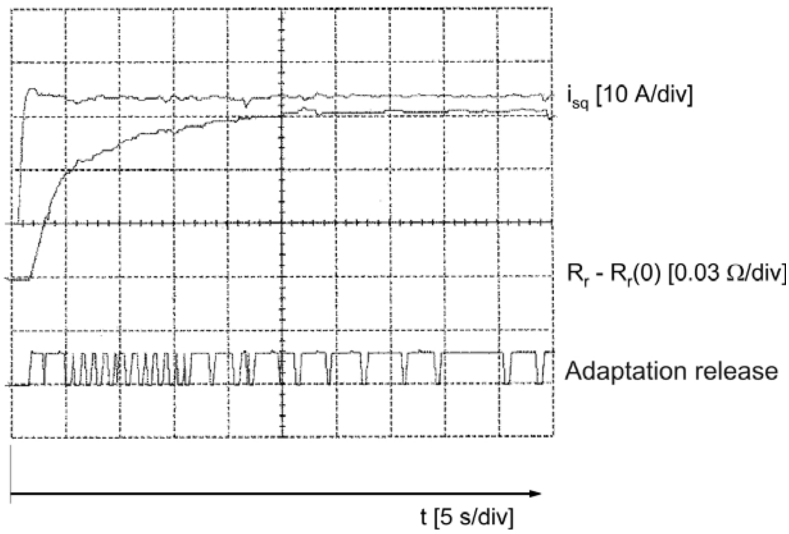

Fig. 7.10 $\hat{R}_r$ adaptation cycles with voltage vector error method

At first the functionality of the adaptation shall, however, be illustrated by some examples during stationary operation. The oscillogram in the figure 7.10 shows a settling transient of the estimated rotor resistance after a load step. The initial error of the model rotor resistance is 30%. As already indicated in the previous section, the influences of the iron losses can be suppressed by switching-off the adaptation at insufficient torque. The detection of the stationary operation with adequate reliability is relatively easy by using a high pass filter for the torque forming current and the rotor flux. Together with the rotor resistance and the torque forming current the figure shows this adaptation release.

As pointed out above, the proportion between torque and flux forming currents is shifted by the rotor flux phase error at wrong model rotor resistance. In the case without adaptation a slow drift of the torque producing current in the model, caused by the warming-up of the machine, will be noticed at constant load until the thermal balance is reached. The effectiveness of the adaptation can thus be shown by the torque forming current keeping constant at constant load over long time. The oscillograms in the figure 7.11 show the corresponding plots during a warm-up process.

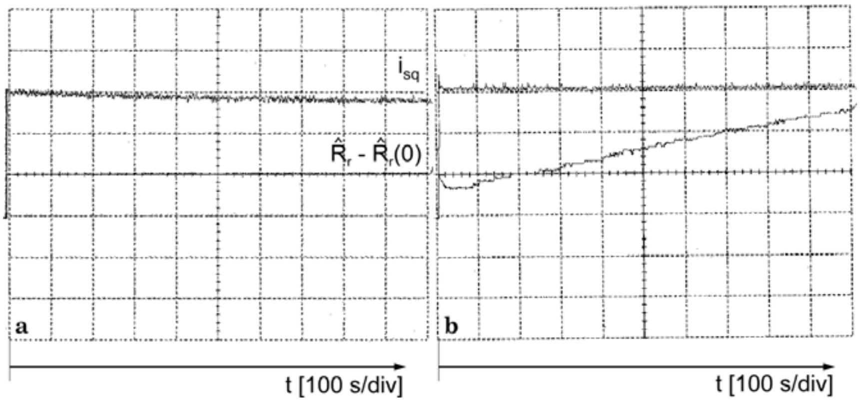

Fig. 7.11 $\hat{i}_{sq}$ and $\hat{R}_r$ at constant load, $\hat{i}_{sq}$ [10 A/div], $\hat{R}_r - \hat{R}_r(0)$ [0.03 Ω/div]: (**a**) without adaptation; (**b**) with adaptation

Similarly to the iron losses the parameter sensitivity to other model parameters also becomes more critical in the dynamic operation, and the demand to increase the precision of the error model increases. Like in the stationary operation, the influence of the leakage inductance is particularly strong and therefore shall be treated here with priority. Primarily this is caused by the following reasons:

- A dynamic speed change is connected to a short-time impression of a high torque forming current. The σL_s sensitivity assumes its most critical values just at high torque.
- Parameter errors in the error model have an immediate effect to the adaptation error. The model error of the rotor resistance, however, delayedly adds to the adaptation error through detuning of the phase angle $\tilde{\vartheta}_s$. Thus it is very probable at sufficiently short transients that the adaptation can only be activated (and detuned) by the model parameter errors.

For an appropriately exact adjustment of the error model the derived methods are definitely able to adapt the rotor resistance also in dynamic operation without an additional steady-state load torque. The figure 7.12 exemplarily shows such an adaptation process for the voltage vector error method recorded for longer time. The prerequisite is that the error equations, as done in section 7.3.2, are programmed using the dynamic machine equations.

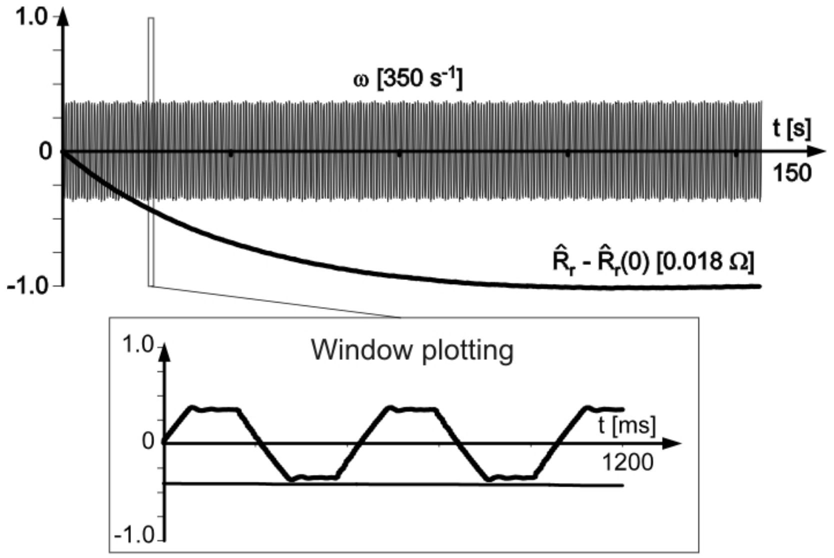

Fig. 7.12 Adaptation cycle with voltage vector error model, $\hat{R}_r(150\,s) = 0.27\,\Omega$

The result of a comparison of different methods with regard to the sensitivity to leakage inductance changes and iron losses in dynamic operation is represented in the figure 7.13. The adaptation was excited by speed transients between 200 and 700 rpm. The error of the model value of the leakage inductance was +5% for all tests. The initial error of the model rotor time constant is zero.

The reference curve (curve 1) was taken with a simultaneous adaptation of rotor resistance and leakage inductance. The used method is not transferable to arbitrary operating states, though. It uses different properties of the model error contributions caused by the leakage inductance and the rotor resistance deviation in regenerative and motor operation.

The adaptation to the motor warm-up is already visible in the second part of the plots. As far as possible, the tests were recorded until achieving a stationary state of the adaptation.

Related to the final stationary value of curve 1 the following final deviations develop:

- Curve 2: -1.6 %
- Curve 3: -5.4 %
- Curve 4: -45 %

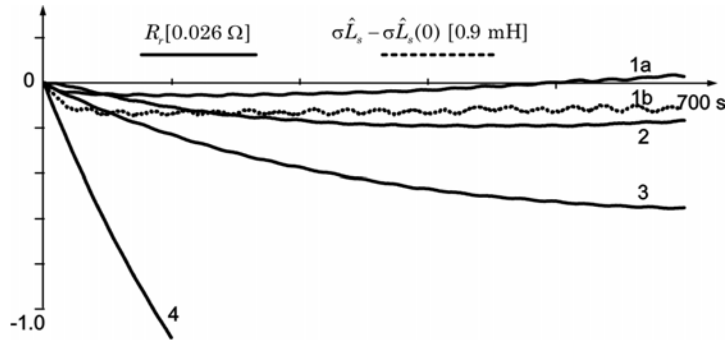

Fig. 7.13 $\hat{R}_r$ detuning caused by model error of leakage inductance and iron losses: curves (**1** – **3**) with compensation of iron losses, (**1**) combined adaptation of $\hat{R}_r$ and $\sigma\hat{L}_s$, voltage vector error, (**2**) reactive power method, (**3**) voltage vector error, (**4**) like curve (**3**), without compensation of iron losses

With that it is clear that an adaptation without consideration of the iron losses in dynamic operation must be considered impossible. The reactive power model proves to be the most suitable method for the dynamic operation here. The explanation can be found in that for this model the σL_s part of the flux phase error $\tilde{\vartheta}_s$, representing the rotor resistance deviation, is weighted approximately twice stronger than in the linear error model of the voltage vector method. This can be shown by deriving an error expression including all parameter errors according to (7.38) or (7.44) [Dittrich 1998]. Thus its share on the total error is increased with the consequence of a better suppression of parameter errors of the model.

7.4 References to chapter 7

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