

depends merely on the hysteresis loop of the rotor material and is independent of the rate at which the loop is traversed. The motor therefore develops constant torque right up to synchronous speed, as shown in the idealized torque-speed characteristic of Fig. 9.10b. This feature is one of the advantages of the hysteresis motor. In contrast with a reluctance motor, which must “snap” its load into synchronism from an induction-motor torque-speed characteristic, a hysteresis motor can synchronize any load which it can accelerate, no matter how great the inertia. After reaching synchronism, the motor continues to run at synchronous speed and adjusts its torque angle so as to develop the torque required by the load.

The hysteresis motor is inherently quiet and produces smooth rotation of its load. Furthermore, the rotor takes on the same number of poles as the stator field. The motor lends itself to multispeed synchronous operation when the stator is wound with several sets of windings and utilizes pole-changing connections. The hysteresis motor can accelerate and synchronize high-inertia loads because its torque is uniform from standstill to synchronous speed.

9.3 REVOLVING-FIELD THEORY OF SINGLE-PHASE INDUCTION MOTORS

As discussed in Section 9.1, the stator-mmf wave of a single-phase induction motor can be shown to be equivalent to two constant-amplitude mmf waves revolving at synchronous speed in opposite directions. Each of these component stator-mmf waves induces its own component rotor currents and produces induction-motor action just as in a balanced polyphase motor. This double-revolving-field concept not only is useful for qualitative visualization but also can be developed into a quantitative theory applicable to a wide variety of induction-motor types. We will not discuss the full quantitative theory here.¹ However, we will consider the simpler, but important case of a single-phase induction motor running on only its main winding.

Consider conditions with the rotor stationary and only the main stator winding excited. The motor then is equivalent to a transformer with its secondary short-circuited. The equivalent circuit is shown in Fig. 9.11a, where $R_{1,\text{main}}$ and $X_{1,\text{main}}$ are, respectively, the resistance and leakage reactance of the main winding, $X_{m,\text{main}}$ is the magnetizing reactance, and $R_{2,\text{main}}$ and $X_{2,\text{main}}$ are the standstill values of the rotor resistance and leakage reactance referred to the main stator winding by use of the appropriate turns ratio. Core loss, which is omitted here, will be accounted for later as if it were a rotational loss. The applied voltage is \hat{V} , and the main-winding current is \hat{I}_{main} . The voltage \hat{E}_{main} is the counter emf generated in the main winding by the stationary pulsating air-gap flux wave produced by the combined action of the stator and rotor currents.

In accordance with the double-revolving-field concept of Section 9.1, the stator mmf can be resolved into half-amplitude forward and backward rotating fields. At

¹ For an extensive treatment of single-phase motors, see, for example, C. B. Veinott, *Fractional- and Subfractional-Horsepower Electric Motors*, McGraw-Hill, New York, 1970.

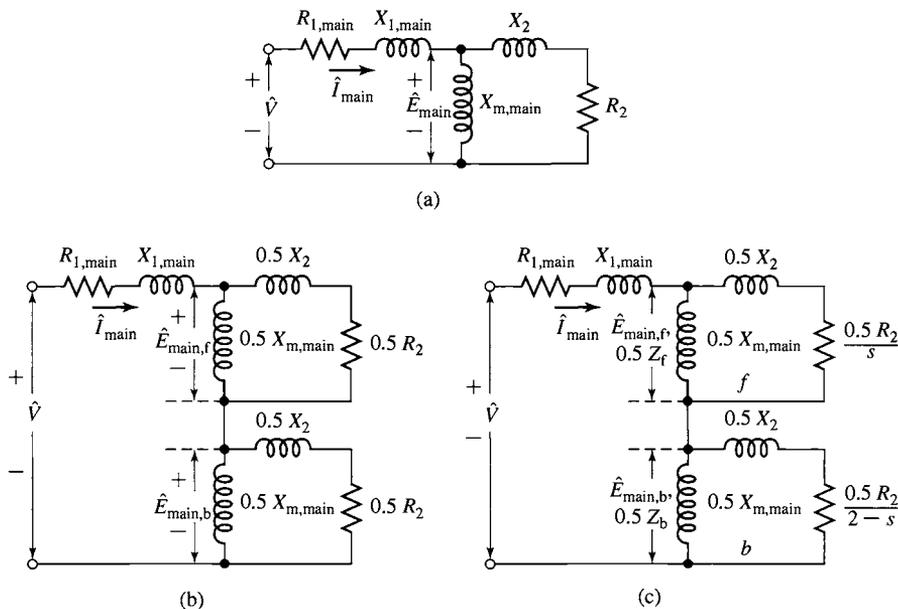


Figure 9.11 Equivalent circuits for a single-phase induction motor: (a) rotor blocked; (b) rotor blocked, showing effects of forward and backward fields; (c) running conditions.

standstill the amplitudes of the forward and backward resultant air-gap flux waves both equal half the amplitude of the pulsating field. In Fig. 9.11b the portion of the equivalent circuit representing the effects of the air-gap flux is split into two equal portions, representing the effects of the forward and backward fields, respectively.

Now consider conditions after the motor has been brought up to speed by some auxiliary means and is running on only its main winding in the direction of the forward field at a per-unit slip s . The rotor currents induced by the forward field are of slip frequency $s f_e$, where f_e is the stator applied electrical frequency. Just as in any polyphase motor with a symmetric polyphase or squirrel-cage rotor, these rotor currents produce an mmf wave traveling forward at slip speed with respect to the rotor and therefore at synchronous speed with respect to the stator. The resultant of the forward waves of stator and rotor mmf creates a resultant forward wave of air-gap flux, which generates a counter emf $\hat{E}_{\text{main},f}$ in the main winding of the stator. The reflected effect of the rotor as viewed from the stator is like that in a polyphase motor and can be represented by an impedance $0.5R_{2,\text{main}}/s + j0.5X_{2,\text{main}}$ in parallel with $j0.5X_{m,\text{main}}$ as in the portion of the equivalent circuit of Fig. 9.11c labeled ‘f’. The factors of 0.5 come from the resolution of the pulsating stator mmf into forward and backward components.

Now consider conditions with respect to the backward field. The rotor is still turning at a slip s with respect to the forward field, and its per-unit speed n in the

direction of the forward field is $n = 1 - s$. The relative speed of the rotor with respect to the backward field is $1 + n$, or its slip with respect to the backward field is $1 + n = 2 - s$. The backward field then induces rotor currents whose frequency is $(2 - s)f_e$. For small slips, these rotor currents are of almost twice stator frequency.

At a small slip, an oscilloscope trace of rotor current will therefore show a high-frequency component from the backward field superposed on a low-frequency component from the forward field. As viewed from the stator, the rotor-mmf wave of the backward-field induced rotor current travels at synchronous speed but in the backward direction. The equivalent-circuit representing these internal reactions from the viewpoint of the stator is like that of a polyphase motor whose slip is $2 - s$ and is shown in the portion of the equivalent circuit (Fig. 9.11c) labeled 'b'. As with the forward field, the factors of 0.5 come from the resolution of the pulsating stator mmf into forward and backward components. The voltage $\hat{E}_{\text{main,b}}$ across the parallel combination representing the backward field is the counter emf generated in the main winding of the stator by the resultant backward field.

By use of the equivalent circuit of Fig. 9.11c, the stator current, power input, and power factor can be computed for any assumed value of slip when the applied voltage and the motor impedances are known. To simplify the notation, let

$$Z_f \equiv R_f + jX_f \equiv \left(\frac{R_{2,\text{main}}}{s} + jX_{2,\text{main}} \right) \text{ in parallel with } jX_{m,\text{main}} \quad (9.4)$$

and

$$Z_b \equiv R_b + jX_b \equiv \left(\frac{R_{2,\text{main}}}{2 - s} + jX_{2,\text{main}} \right) \text{ in parallel with } jX_{m,\text{main}} \quad (9.5)$$

The impedances representing the reactions of the forward and backward fields from the viewpoint of the single-phase main stator winding are $0.5Z_f$ and $0.5Z_b$, respectively, in Fig. 9.11c.

Examination of the equivalent circuit (Fig. 9.11c) confirms the conclusion, reached by qualitative reasoning in Section 9.1 (Fig. 9.2b), that the forward air-gap flux wave increases and the backward wave decreases when the rotor is set in motion. When the motor is running at a small slip, the reflected effect of the rotor resistance in the forward field, $0.5R_{2,\text{main}}/s$, is much larger than its standstill value, while the corresponding effect in the backward field, $0.5R_{2,\text{main}}/(2 - s)$, is smaller. The forward-field impedance therefore is larger than its standstill value, while that of the backward field is smaller. The forward-field counter emf $\hat{E}_{\text{main,f}}$ therefore is larger than its standstill value, while the backward-field counter emf $\hat{E}_{\text{main,b}}$ is smaller; i.e., the forward air-gap flux wave increases, while the backward flux wave decreases.

Mechanical power and torque can be computed by application of the torque and power relations developed for polyphase motors in Chapter 6. The torques produced by the forward and backward fields can each be treated in this manner. The interactions of the oppositely rotating flux and mmf waves cause torque pulsations at twice stator frequency but produce no average torque.

As in Eq. 6.25, the electromagnetic torque $T_{\text{main},f}$ of the forward field in newton-meters equals $1/\omega_s$ times the power $P_{\text{gap},f}$ in watts delivered by the stator winding to the forward field, where ω_s is the synchronous angular velocity in mechanical radians per second; thus

$$T_{\text{main},f} = \frac{1}{\omega_s} P_{\text{gap},f} \quad (9.6)$$

When the magnetizing impedance is treated as purely inductive, $P_{\text{gap},f}$ is the power absorbed by the impedance $0.5Z_f$; that is,

$$P_{\text{gap},f} = I^2(0.5R_f) \quad (9.7)$$

where R_f is the resistive component of the forward-field impedance defined in Eq. 9.4.

Similarly, the internal torque $T_{\text{main},b}$ of the backward field is

$$T_{\text{main},b} = \frac{1}{\omega_s} P_{\text{gap},b} \quad (9.8)$$

where $P_{\text{gap},b}$ is the power delivered by the stator winding to the backward field, or

$$P_{\text{gap},b} = I^2(0.5R_b) \quad (9.9)$$

where R_b is the resistive component of the backward-field impedance Z_b defined in Eq. 9.5.

The torque of the backward field is in the opposite direction to that of the forward field, and therefore the net internal torque T_{mech} is

$$T_{\text{mech}} = T_{\text{main},f} - T_{\text{main},b} = \frac{1}{\omega_s} (P_{\text{gap},f} - P_{\text{gap},b}) \quad (9.10)$$

Since the rotor currents produced by the two component air-gap fields are of different frequencies, the total rotor I^2R loss is the numerical sum of the losses caused by each field. In general, as shown by comparison of Eqs. 6.17 and 6.19, the rotor I^2R loss caused by a rotating field equals the slip of the field times the power absorbed from the stator. Thus

$$\text{Forward-field rotor } I^2R = s P_{\text{gap},f} \quad (9.11)$$

$$\text{Backward-field rotor } I^2R = (2 - s) P_{\text{gap},b} \quad (9.12)$$

$$\text{Total rotor } I^2R = s P_{\text{gap},f} + (2 - s) P_{\text{gap},b} \quad (9.13)$$

Since power is torque times angular velocity and the angular velocity of the rotor is $(1 - s)\omega_s$, using Eq. 9.10, the internal power P_{mech} converted to mechanical form, in watts, is

$$P_{\text{mech}} = (1 - s)\omega_s T_{\text{mech}} = (1 - s)(P_{\text{gap},f} - P_{\text{gap},b}) \quad (9.14)$$

As in the polyphase motor, the internal torque T_{mech} and internal power P_{mech} are not the output values because rotational losses remain to be accounted for. It is obviously correct to subtract friction and windage losses from T_{mech} or P_{mech} and it

is usually assumed that core losses can be treated in the same manner. For the small changes in speed encountered in normal operation, the rotational losses are often assumed to be constant.²

EXAMPLE 9.2

A $\frac{1}{4}$ -hp, 110-V, 60-Hz, four-pole, capacitor-start motor has the following equivalent circuit parameter values (in Ω) and losses:

$$R_{1,\text{main}} = 2.02 \quad X_{1,\text{main}} = 2.79 \quad R_{2,\text{main}} = 4.12$$

$$X_{2,\text{main}} = 2.12 \quad X_{m,\text{main}} = 66.8$$

$$\text{Core loss} = 24 \text{ W} \quad \text{Friction and windage loss} = 13 \text{ W}$$

For a slip of 0.05, determine the stator current, power factor, power output, speed, torque, and efficiency when this motor is running as a single-phase motor at rated voltage and frequency with its starting winding open.

■ Solution

The first step is to determine the values of the forward- and backward-field impedances at the assigned value of slip. The following relations, derived from Eq. 9.4, simplify the computations of the forward-field impedance Z_f :

$$R_f = \left(\frac{X_{m,\text{main}}^2}{X_{22}} \right) \frac{1}{sQ_{2,\text{main}} + 1/(sQ_{2,\text{main}})} \quad X_f = \frac{X_{2,\text{main}}X_{m,\text{main}}}{X_{22}} + \frac{R_f}{sQ_{2,\text{main}}}$$

where

$$X_{22} = X_{2,\text{main}} + X_{m,\text{main}} \quad \text{and} \quad Q_{2,\text{main}} = \frac{X_{22}}{R_{2,\text{main}}}$$

Substitution of numerical values gives, for $s = 0.05$,

$$Z_f = R_f + jX_f = 31.9 + j40.3 \Omega$$

Corresponding relations for the backward-field impedance Z_b are obtained by substituting $2 - s$ for s in these equations. When $(2 - s)Q_{2,\text{main}}$ is greater than 10, as is usually the case, less than 1 percent error results from use of the following approximate forms:

$$R_b = \frac{R_{2,\text{main}}}{2 - s} \left(\frac{X_{m,\text{main}}}{X_{22}} \right)^2 \quad X_b = \frac{X_{2,\text{main}}X_{m,\text{main}}}{X_{22}} + \frac{R_b}{(2 - s)Q_{2,\text{main}}}$$

Substitution of numerical values gives, for $s = 0.05$,

$$Z_b = R_b + jX_b = 1.98 + j2.12 \Omega$$

² For a treatment of the experimental determination of motor constants and losses, see Veinott, op. cit., Chapter 18.

Addition of the series elements in the equivalent circuit of Fig. 9.11c gives

$$R_{1,\text{main}} + jX_{1,\text{main}} = 2.02 + j2.79$$

$$0.5(R_f + jX_f) = 15.95 + j20.15$$

$$\underline{0.5(R_b + jX_b)} = \underline{0.99 + j1.06}$$

$$\text{Total Input } Z = 18.96 + j24.00 = 30.6 \angle 51.7^\circ$$

$$\text{Stator current } I = \frac{V}{Z} = \frac{110}{30.6} = 3.59 \text{ A}$$

$$\text{Power factor} = \cos(51.7^\circ) = 0.620$$

$$\text{Power input} = P_{\text{in}} = VI \times \text{power factor} = 110 \times 3.59 \times 0.620 = 244 \text{ W}$$

The power absorbed by the forward field (Eq. 9.7) is

$$P_{\text{gap},f} = I^2(0.5R_f) = 3.59^2 \times 15.95 = 206 \text{ W}$$

The power absorbed by the backward field (Eq. 9.9) is

$$P_{\text{gap},b} = I^2(0.5R_b) = 3.59^2 \times 0.99 = 12.8 \text{ W}$$

The internal mechanical power (Eq. 9.14) is

$$P_{\text{mech}} = (1 - s)(P_{\text{gap},f} - P_{\text{gap},b}) = 0.95(206 - 13) = 184 \text{ W}$$

Assuming that the core loss can be combined with the friction and windage loss, the rotational loss becomes $24 + 13 = 37 \text{ W}$ and the shaft output power is the difference. Thus

$$P_{\text{shaft}} = 184 - 37 = 147 \text{ W} = 0.197 \text{ hp}$$

From Eq. 4.40, the synchronous speed in rad/sec is given by

$$\omega_s = \left(\frac{2}{\text{poles}} \right) \omega_e = \left(\frac{2}{4} \right) 120\pi = 188.5 \text{ rad/sec}$$

or in terms of r/min from Eq. 4.41

$$n_s = \left(\frac{120}{\text{poles}} \right) f_e = \left(\frac{120}{4} \right) 60 = 1800 \text{ r/min}$$

$$\text{Rotor speed} = (1 - s)(\text{synchronous speed})$$

$$= 0.95 \times 1800 = 1710 \text{ r/min}$$

and

$$\omega_m = 0.95 \times 188.5 = 179 \text{ rad/sec}$$

The torque can be found from Eq. 9.14.

$$T_{\text{shaft}} = \frac{P_{\text{shaft}}}{\omega_m} = \frac{147}{179} = 0.821 \text{ N} \cdot \text{m}$$

and the efficiency is

$$\eta = \frac{P_{\text{shaft}}}{P_{\text{in}}} = \frac{147}{244} = 0.602 = 60.2\%$$

As a check on the power bookkeeping, compute the losses:

$$I^2 R_{1,\text{main}} = (3.59)^2(2.02) = 26.0$$

$$\text{Forward-field rotor } I^2 R \text{ (Eq. 9.11)} = 0.05 \times 206 = 10.3$$

$$\text{Backward-field rotor } I^2 R \text{ (Eq. 9.12)} = 1.95 \times 12.8 = 25.0$$

$$\begin{aligned} \text{Rotational losses} &= \underline{37.0} \\ &98.3 \text{ W} \end{aligned}$$

From $P_{\text{in}} - P_{\text{shaft}}$, the total losses = 97 W which checks within accuracy of computations.

Practice Problem 9.2

Assume the motor of Example 9.2 to be operating at a slip of 0.065 and at rated voltage and frequency. Determine (a) the stator current and power factor and (b) the power output.

Solution

- 4.0 A, power factor = 0.70 lagging
- 190 W

Examination of the order of magnitude of the numerical values in Example 9.2 suggests approximations which usually can be made. These approximations pertain particularly to the backward-field impedance. Note that the impedance $0.5(R_b + jX_b)$ is only about 5 percent of the total motor impedance for a slip near full load. Consequently, an approximation as large as 20 percent of this impedance would cause only about 1 percent error in the motor current. Although, strictly speaking, the backward-field impedance is a function of slip, very little error usually results from computing its value at any convenient slip in the normal running region, e.g., 5 percent, and then assuming R_b and X_b to be constants.

Corresponding to a slightly greater approximation, the shunting effect of $jX_{m,\text{main}}$ on the backward-field impedance can often be neglected, whence

$$Z_b \approx \frac{R_{2,\text{main}}}{2-s} + jX_{2,\text{main}} \quad (9.15)$$

This equation gives values of the backward-field resistance that are a few percent high, as can be seen by comparison with the exact expression given in Example 9.2. Neglecting s in Eq. 9.15 would tend to give values of the backward-field resistance that would be too low, and therefore such an approximation would tend to counteract the error in Eq. 9.15. Consequently, for small slips

$$Z_b \approx \frac{R_{2,\text{main}}}{2} + jX_{2,\text{main}} \quad (9.16)$$

In a polyphase motor (Section 6.5), the maximum internal torque and the slip at which it occurs can easily be expressed in terms of the motor parameters; the maximum internal torque is independent of rotor resistance. No such simple expressions exist for a single-phase motor. The single-phase problem is much more involved because of the presence of the backward field, the effect of which is twofold: (1) it absorbs some of the applied voltage, thus reducing the voltage available for the forward field and decreasing the forward torque developed; and (2) the backward field produces negative torque, reducing the effective developed torque. Both of these effects depend on rotor resistance as well as leakage reactance. Consequently, unlike the polyphase motor, the maximum internal torque of a single-phase motor is influenced by rotor resistance; increasing the rotor resistance decreases the maximum torque and increases the slip at which maximum torque occurs.

Principally because of the effects of the backward field, a single-phase induction motor is somewhat inferior to a polyphase motor using the same rotor and the same stator core. The single-phase motor has a lower maximum torque which occurs at a lower slip. For the same torque, the single-phase motor has a higher slip and greater losses, largely because of the backward-field rotor I^2R loss. The volt-ampere input to the single-phase motor is greater, principally because of the power and reactive volt-amperes consumed by the backward field. The stator I^2R loss also is somewhat higher in the single-phase motor, because one phase, rather than several, must carry all the current. Because of the greater losses, the efficiency is lower, and the temperature rise for the same torque is higher. A larger frame size must be used for a single-phase motor than for a polyphase motor of the same power and speed rating. Because of the larger frame size, the maximum torque can be made comparable with that of a physically smaller but equally rated polyphase motor. In spite of the larger frame size and the necessity for auxiliary starting arrangements, general-purpose single-phase motors in the standard fractional-kilowatt ratings cost approximately the same as correspondingly rated polyphase motors because of the much greater volume of production of the former.

9.4 TWO-PHASE INDUCTION MOTORS

As we have seen, most single-phase induction motors are actually constructed in the form of two-phase motors, with two stator windings in space quadrature. The main and auxiliary windings are typically quite different, with a different number of turns, wire size, and turns distribution. This difference, in combination with the capacitor that is typically used in series with the auxiliary winding, guarantees that the mmfs produced by the two winding currents will be quite unbalanced; at best they may be balanced at one specific operating point. We will thus discuss various analytical techniques for two-phase motors, both to expand our understanding and insight into machine performance and also to develop techniques for the analysis of single- and two-phase motors.

Under balanced operating conditions, a symmetrical two-phase motor can be analyzed using techniques developed in Chapter 6 for three-phase motors, modified

only slightly to take into account the fact that there are two phases rather than three. In this section, we will first discuss one technique that can be used to analyze a symmetrical two-phase motor operating under unbalanced operating conditions. We will then formally derive an analytical model for an unsymmetrical two-phase motor that can be applied to the general case single-phase motors operating off both their main and auxiliary windings.

9.4.1 Unbalanced Operation of Symmetrical Two-Phase Machines; The Symmetrical-Component Concept

When operating from the main winding alone, the single-phase motor is the extreme case of a motor operating under unbalanced stator-current conditions. In some cases, unbalanced voltages or currents are produced in the supply network to a motor, e.g., when a line fuse is blown. In other cases, unbalanced voltages are produced by the starting impedances of single-phase motors, as described in Section 9.2. The purpose of this section is to develop the symmetrical-component theory of two-phase induction motors from the double-revolving-field concept and to show how the theory can be applied to a variety of problems involving induction motors having two stator windings in space quadrature.

First consider in review what happens when balanced two-phase voltages are applied to the stator terminals of a two-phase machine having a uniform air gap, a symmetrical polyphase or cage rotor, and two identical stator windings α and β in space quadrature. The stator currents are equal in magnitude and in time quadrature. When the current in winding α is at its instantaneous maximum, the current in winding β is zero and the stator-mmf wave is centered on the axis of winding α . Similarly, the stator-mmf wave is centered on the axis of winding β at the instant when the current in winding β is at its instantaneous maximum. The stator-mmf wave therefore travels 90 electrical degrees in space in a time interval corresponding to a 90° phase change of the applied voltage, with the direction of its travel depending on the phase sequence of the currents. A more complete analysis in the manner of Section 4.5 shows that the traveling wave has constant amplitude and constant angular velocity. This fact is, of course, the basis for the theory of the balanced operation of induction machines.

The behavior of the motor for balanced two-phase applied voltages of either phase sequence can be readily determined. Thus, if the rotor is turning at a slip s in the direction from winding α toward winding β , the terminal impedance per phase is given by the equivalent circuit of Fig. 9.12a when the applied voltage \hat{V}_β lags the applied voltage \hat{V}_α by 90°. Throughout the rest of this treatment, this phase sequence is called *positive sequence* and is designated by the subscript 'f' since positive-sequence currents result in a forward field. With the rotor running at the same speed and in the same direction, the terminal impedance per phase is given by the equivalent circuit of Fig. 9.12b when \hat{V}_β leads \hat{V}_α by 90°. This phase sequence is called *negative sequence* and is designated by subscript 'b', since negative-sequence currents produce a backward field.

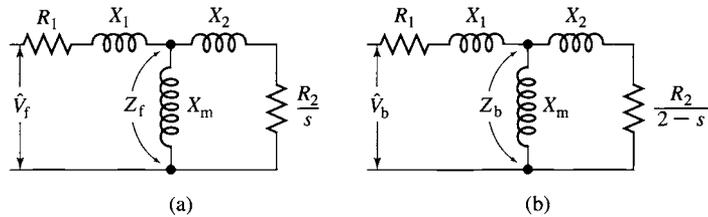


Figure 9.12 Single-phase equivalent circuits for a two-phase motor under unbalanced conditions: (a) forward field and (b) backward field.

Suppose now that *two* balanced two-phase voltage sources of *opposite phase sequence* are connected in series and applied simultaneously to the motor, as indicated in Fig. 9.13a, where phasor voltages \hat{V}_f and $j\hat{V}_f$ applied, respectively, to windings α and β form a balanced system of positive sequence, and phasor voltages \hat{V}_b and $-j\hat{V}_b$ form another balanced system but of negative sequence.

The resultant voltage V_α applied to winding α is, as a phasor,

$$\hat{V}_\alpha = \hat{V}_f + \hat{V}_b \tag{9.17}$$

and that applied to winding β is

$$\hat{V}_\beta = j\hat{V}_f - j\hat{V}_b \tag{9.18}$$

Fig. 9.13b shows a generalized phasor diagram in which the forward, or positive-sequence, system is given by the phasors \hat{V}_f and $j\hat{V}_f$ and the backward, or negative-sequence, system is given by the phasors \hat{V}_b and $-j\hat{V}_b$. The resultant voltages, given by the phasors \hat{V}_α and \hat{V}_β are not, in general, either equal in magnitude or in time

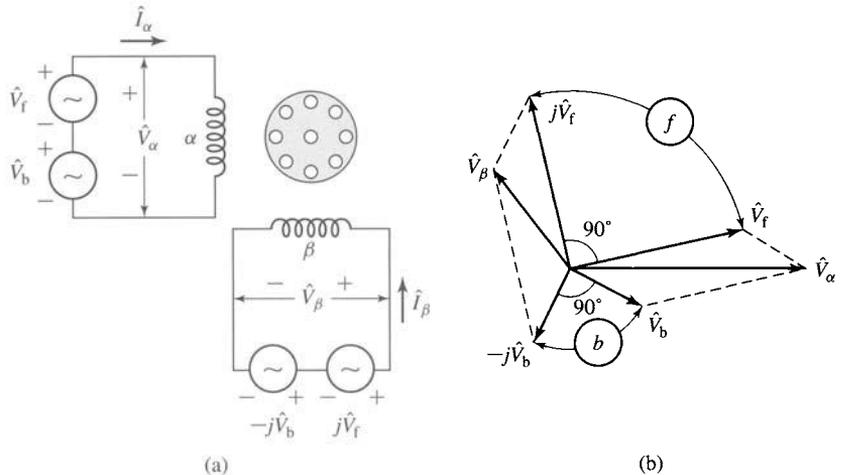


Figure 9.13 Synthesis of an unbalanced two-phase system from the sum of two balanced systems of opposite phase sequence.

quadrature. From this discussion we see that an unbalanced two-phase system of applied voltages V_α and V_β can be synthesized by combining two balanced voltage sets of opposite phase sequence.

The symmetrical-component systems are, however, much easier to work with than their unbalanced resultant system. Thus, it is easy to compute the component currents produced by each symmetrical-component system of applied voltages because the induction motor operates as a balanced two-phase motor for each component system. By superposition, the actual current in a winding then is the sum of its components. Thus, if \hat{I}_f and \hat{I}_b are, respectively, the positive- and negative-sequence component phasor currents in winding α , then the corresponding positive- and negative-sequence component phasor currents in winding β are, respectively, $j\hat{I}_f$ and $-j\hat{I}_b$, and the actual winding currents \hat{I}_α and \hat{I}_β are

$$\hat{I}_\alpha = \hat{I}_f + \hat{I}_b \quad (9.19)$$

$$\hat{I}_\beta = j\hat{I}_f - j\hat{I}_b \quad (9.20)$$

The inverse operation of finding the symmetrical components of specified voltages or currents must be performed often. Solution of Eqs. 9.17 and 9.18 for the phasor components \hat{V}_f and \hat{V}_b in terms of known phasor voltages \hat{V}_α and \hat{V}_β gives

$$\hat{V}_f = \frac{1}{2}(\hat{V}_\alpha - j\hat{V}_\beta) \quad (9.21)$$

$$\hat{V}_b = \frac{1}{2}(\hat{V}_\alpha + j\hat{V}_\beta) \quad (9.22)$$

These operations are illustrated in the phasor diagram of Fig. 9.14. Obviously, similar relations give the phasor symmetrical components \hat{I}_f and \hat{I}_b of the current in winding α in terms of specified phasor currents \hat{I}_m and \hat{I}_a in the two phases; thus

$$\hat{I}_f = \frac{1}{2}(\hat{I}_\alpha - j\hat{I}_\beta) \quad (9.23)$$

$$\hat{I}_b = \frac{1}{2}(\hat{I}_\alpha + j\hat{I}_\beta) \quad (9.24)$$

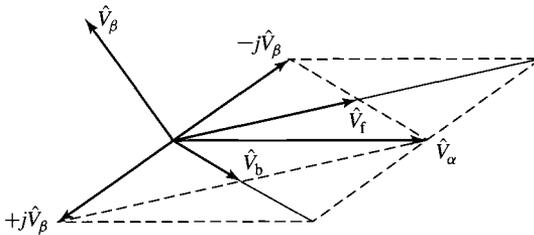


Figure 9.14 Resolution of unbalanced two-phase voltages into symmetrical components.

EXAMPLE 9.3



The equivalent-circuit parameters of a 5-hp 220-V 60-Hz four-pole two-phase squirrel-cage induction motor in ohms per phase are

$$R_1 = 0.534 \quad X_1 = 2.45 \quad X_m = 70.1 \quad R_2 = 0.956 \quad X_2 = 2.96$$

This motor is operated from an unbalanced two-phase 60-Hz source whose phase voltages are, respectively, 230 and 210 V, the smaller voltage leading the larger by 80° . For a slip of 0.05, find (a) the positive- and negative-sequence components of the applied voltages, (b) the positive- and negative-sequence components of the stator-phase currents, (c) the effective values of the phase currents, and (d) the internal mechanical power.

■ Solution

We will solve this example using MATLAB.[†]

- a. Let \hat{V}_α and \hat{V}_β denote the voltages applied to the two phases, respectively. Then

$$\hat{V}_\alpha = 230\angle 0^\circ = 230 + j0 \text{ V}$$

$$\hat{V}_\beta = 210\angle 80^\circ = 36.4 + j207 \text{ V}$$

From Eqs. 9.21 and 9.22 the forward and backward components of voltages are, respectively,

$$\hat{V}_f = 218.4 - j18.2 = 219.2\angle -4.8^\circ \text{ V}$$

$$\hat{V}_b = 11.6 + j18.2 = 21.6\angle 57.5^\circ \text{ V}$$

- b. Because of the ease with which MATLAB handles complex numbers, there is no need to use approximations such as are derived in Example 9.2. Rather, the forward- and backward-field input impedances of the motor can be calculated from the equivalent circuits of Figs. 9.12a and b. Dividing the forward-field voltage by the forward-field input impedance gives

$$\hat{I}_f = \frac{\hat{V}_f}{R_1 + jX_1 + Z_f} = 9.3 - j6.3 = 11.2\angle -34.2^\circ \text{ A}$$

Similarly, dividing the backward-field voltage by the backward-field input impedance gives

$$\hat{I}_b = \frac{\hat{V}_b}{R_1 + jX_1 + Z_b} = 3.7 - j1.5 = 4.0\angle -21.9^\circ \text{ A}$$

- c. The winding currents can be calculated from Eqs. 9.19 and 9.20

$$\hat{I}_\alpha = \hat{I}_f + \hat{I}_b = 13.0 - j7.8 = 15.2\angle -31.0^\circ \text{ A}$$

$$\hat{I}_\beta = j\hat{I}_f - j\hat{I}_b = 4.8 + j5.6 = 7.4\angle 49.1^\circ \text{ A}$$

Note that the winding currents are much more unbalanced than the applied voltages. Even though the motor is not overloaded insofar as shaft load is concerned, the losses are

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appreciably increased by the current unbalance, and the stator winding with the greatest current may overheat.

- d. The power delivered across the air gap by the forward field is equal to the forward-field equivalent-circuit input power minus the corresponding stator loss

$$P_{\text{gap},f} = 2(\text{Re}[\hat{V}_f \hat{I}_f^*] - I_f^2 R_1) = 4149 \text{ W}$$

where the factor of 2 accounts for the fact that this is a two-phase motor. Similarly, the power delivered to the backward field is

$$P_{\text{gap},b} = 2(\text{Re}[\hat{V}_b \hat{I}_b^*] - I_b^2 R_1) = 14.5 \text{ W}$$

Here, the symbol $\text{Re}[\]$ indicates the real part of a complex number, and the superscript * indicates the complex conjugate.

Finally, from Eq. 9.14, the internal mechanical power developed is equal to $(1 - s)$ times the total air-gap power or

$$P_{\text{mech}} = (1 - s)(P_{\text{gap},f} - P_{\text{gap},b}) = 3927 \text{ W}$$

If the core losses, friction and windage, and stray load losses are known, the shaft output can be found by subtracting them from the internal power. The friction and windage losses depend solely on the speed and are the same as they would be for balanced operation at the same speed. The core and stray load losses, however, are somewhat greater than they would be for balanced operation with the same positive-sequence voltage and current. The increase is caused principally by the $(2 - s)$ -frequency core and stray losses in the rotor caused by the backward field.

Here is the MATLAB script:

```

clc
clear

% Useful constants
f = 60; %60 Hz system
omega = 2*pi*f;
s = 0.05; % slip

% Parameters
R1 = 0.534;
X1 = 2.45;
Xm = 70.1;
R2 = 0.956;
X2 = 2.96;

% Winding voltages
Valpha = 230;
Vbeta = 210 * exp(j*80*pi/180);

%(a) Calculate Vf and Vb from Equations and 9-21 and 9-22
Vf = 0.5*(Valpha - j*Vbeta);
Vb = 0.5*(Valpha + j*Vbeta);

```

```

magVf = abs(Vf);
angleVf = angle(Vf)*180/pi;

magVb = abs(Vb);
angleVb = angle(Vb)*180/pi;

fprintf('\n(a)')
fprintf('\n Vf = %.1f + j %.1f = %.1f at angle %.1f degrees V', ...
    real(Vf), imag(Vf), magVf, angleVf);
fprintf('\n Vb = %.1f + j %.1f = %.1f at angle %.1f degrees V\n', ...
    real(Vb), imag(Vb), magVb, angleVb);

%(b) First calculate the forward-field input impedance of the motor from
% the equivalent circuit of Fig. 9-12(a).

Zforward = R1 + j*Xl + j*Xm*(R2/s+j*X2)/(R2/s+j*(X2+Xm));

%Now calculate the forward-field current.

If = Vf/Zforward;

magIf = abs(If);
angleIf = angle(If)*180/pi;

% Next calculate the backward-field input impedance of the motor from
% Fig. 9-12(b).

Zback = R1 + j*Xl + j*Xm*(R2/(2-s)+j*X2)/(R2/(2-s)+j*(X2+Xm));

%Now calculate the backward-field current.

Ib = Vb/Zback;

magIb = abs(Ib);
angleIb = angle(Ib)*180/pi;

fprintf('\n(b)')
fprintf('\n If = %.1f + j %.1f = %.1f at angle %.1f degrees A', ...
    real(If), imag(If), magIf, angleIf);
fprintf('\n Ib = %.1f + j %.1f = %.1f at angle %.1f degrees A\n', ...
    real(Ib), imag(Ib), magIb, angleIb);

%(c) Calculate the winding currents from Eqs. 9-19 and 9-20

Ialpha = If + Ib;

Ibeta = j*(If - Ib);

magIalpha = abs(Ialpha);
angleIalpha = angle(Ialpha)*180/pi;

magIbeta = abs(Ibeta);
angleIbeta = angle(Ibeta)*180/pi;

fprintf('\n(c)')
fprintf('\n Ialpha = %.1f + j %.1f = %.1f at angle %.1f degrees A', ...
    real(Ialpha), imag(Ialpha), magIalpha, angleIalpha);
fprintf('\n Ibeta = %.1f + j %.1f = %.1f at angle %.1f degrees A\n', ...
    real(Ibeta), imag(Ibeta), magIbeta, angleIbeta);

```

```

%(d) Power delivered to the forward field is equal to the
% forward-field input power less the stator-winding I^2R loss
Pgf = 2*(real(Vf*conj(If)) - R1*magIf^2);

% Power delivered to the backward field is equal to the
% backward-field input power less the stator-winding I^2R loss
Pgb = 2*(real(Vb*conj(Ib)) - R1*magIb^2);

% The electromagnetic power is equal to (1-s) times the
% net air-gap power
Pmech = (1-s)*(Pgf - Pgb);

fprintf('\n(d)')
fprintf('\n Power to forward field = %.1f W',Pgf)
fprintf('\n Power to backward field = %.1f W',Pgb)
fprintf('\n Pmech = %.1f W\n',Pmech)
fprintf('\n')

```

Practice Problem 9.3

For the motor of Example 9.3, use MATLAB to produce a plot of the internal mechanical power as a function of slip as the slip varies from $s = 0.04$ to $s = 0.05$ for the unbalanced voltages assumed in the example. On the same axes (using dashed lines), plot the internal mechanical power for balanced two-phase voltages of 220-V magnitude and 90° phase shift.



Solution

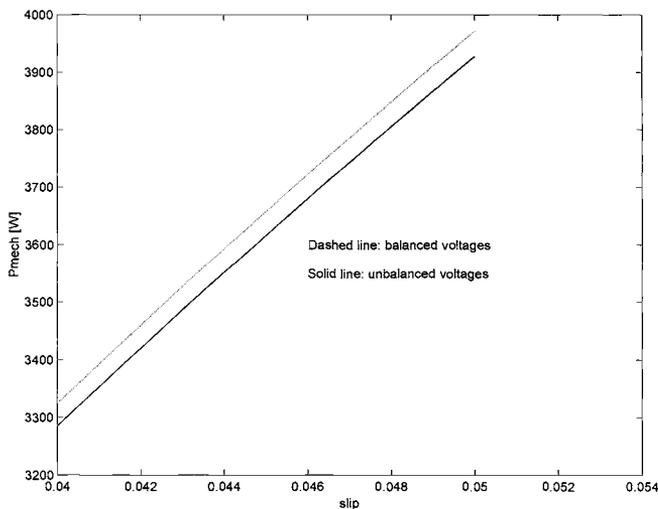


Figure 9.15 MATLAB plot for Practice Problem 9.3.

9.4.2 The General Case: Unsymmetrical Two-Phase Induction Machines

As we have discussed, a single-phase induction motor with a main and auxiliary winding is an example of an unsymmetrical two-phase induction motor. In this section we will develop a model for such a two-phase motor, using notation appropriate to the single-phase motor. We will assume, as is commonly the case, that the windings are in space quadrature but that they are unsymmetrical in that they may have a different number of turns, a different winding distribution, and so on.

Our analytical approach is to represent the rotor by an equivalent two-phase winding as shown in schematic form in Fig. 9.16 and to start with flux-linkage/current relationships for the rotor and stator of the form

$$\begin{bmatrix} \lambda_{\text{main}} \\ \lambda_{\text{aux}} \\ \lambda_{r1} \\ \lambda_{r2} \end{bmatrix} = \begin{bmatrix} L_{\text{main}} & 0 & \mathcal{L}_{\text{main},r1}(\theta_{\text{me}}) & \mathcal{L}_{\text{main},r2}(\theta_{\text{me}}) \\ 0 & L_{\text{aux}} & \mathcal{L}_{\text{aux},r1}(\theta_{\text{me}}) & \mathcal{L}_{\text{aux},r2}(\theta_{\text{me}}) \\ \mathcal{L}_{\text{main},r1}(\theta_{\text{me}}) & \mathcal{L}_{\text{aux},r1}(\theta_{\text{me}}) & L_r & 0 \\ \mathcal{L}_{\text{main},r2}(\theta_{\text{me}}) & \mathcal{L}_{\text{aux},r2}(\theta_{\text{me}}) & 0 & L_r \end{bmatrix} \begin{bmatrix} i_{\text{main}} \\ i_{\text{aux}} \\ i_{r1} \\ i_{r2} \end{bmatrix} \quad (9.25)$$

where θ_{me} is the rotor angle measured in electrical radians.

L_{main} = Self-inductance of the main winding

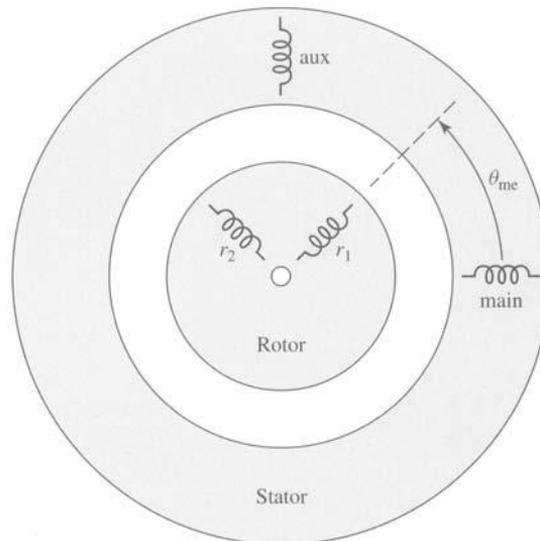


Figure 9.16 Schematic representation of a two-phase induction motor with an equivalent two-phase rotor.

L_{aux} = Self-inductance of the auxiliary winding

L_r = Self-inductance of the equivalent rotor windings

$\mathcal{L}_{\text{main,r1}}(\theta_{\text{me}})$ = Mutual inductance between the main winding and equivalent rotor winding 1

$\mathcal{L}_{\text{main,r2}}(\theta_{\text{me}})$ = Mutual inductance between the main winding and equivalent rotor winding 2

$\mathcal{L}_{\text{aux,r1}}(\theta_{\text{me}})$ = Mutual inductance between the auxiliary winding and rotor winding 1

$\mathcal{L}_{\text{aux,r2}}(\theta_{\text{me}})$ = Mutual inductance between the auxiliary winding and rotor winding 2

Assuming a sinusoidal distribution of air-gap flux, the mutual inductances between the main winding and the rotor will be of the form

$$\mathcal{L}_{\text{main,r1}}(\theta_{\text{me}}) = L_{\text{main,r}} \cos \theta_{\text{me}} \quad (9.26)$$

and

$$\mathcal{L}_{\text{main,r2}}(\theta_{\text{me}}) = -L_{\text{main,r}} \sin \theta_{\text{me}} \quad (9.27)$$

where $L_{\text{main,r}}$ is the amplitude of the mutual inductance.

The mutual inductances between the auxiliary winding will be of the same form with the exception that the auxiliary winding is displaced by 90 electrical degrees in space from the main winding. Hence we can write

$$\mathcal{L}_{\text{aux,r1}}(\theta_{\text{me}}) = L_{\text{aux,r}} \sin \theta_{\text{me}} \quad (9.28)$$

and

$$\mathcal{L}_{\text{aux,r2}}(\theta_{\text{me}}) = L_{\text{aux,r}} \cos \theta_{\text{me}} \quad (9.29)$$

Note that the auxiliary winding will typically have a different number of turns (and perhaps a different winding distribution) from that of the main winding. Thus, for modeling purposes, it is often convenient to write

$$L_{\text{aux,r}} = a L_{\text{main,r}} \quad (9.30)$$

where

$$a = \text{Turns ratio} = \frac{\text{Effective turns of auxiliary winding}}{\text{Effective turns of main winding}} \quad (9.31)$$

Similarly, if we write the self-inductance of the magnetizing branch as the sum of a leakage inductance $L_{\text{main,l}}$ and a magnetizing inductance L_m

$$L_{\text{main}} = L_{\text{main,l}} + L_m \quad (9.32)$$

then the self-inductance of the auxiliary winding can be written in the form

$$L_{\text{aux}} = L_{\text{aux,l}} + a^2 L_m \quad (9.33)$$

The voltage equations for this machine can be written in terms of the winding currents and flux linkages as

$$v_{\text{main}} = i_{\text{main}} R_{\text{main}} + \frac{d\lambda_{\text{main}}}{dt} \quad (9.34)$$

$$v_{\text{aux}} = i_{\text{aux}} R_{\text{aux}} + \frac{d\lambda_{\text{aux}}}{dt} \quad (9.35)$$

$$v_{r1} = 0 = i_{r1} R_r + \frac{d\lambda_{r1}}{dt} \quad (9.36)$$

$$v_{r2} = 0 = i_{r2} R_r + \frac{d\lambda_{r2}}{dt} \quad (9.37)$$

where R_{main} , R_{aux} and R_r are the resistances of the main, auxiliary, and rotor windings, respectively. Note that the rotor-winding voltages are set equal to zero because the rotor windings of an induction motor are internally shorted.

When modeling a split-phase induction motor (Section 9.2.1) the main and auxiliary windings are simply connected in parallel, and thus v_{main} and v_{aux} are both set equal to the single-phase supply voltage when the motor is started. Following the time that the auxiliary winding is disconnected, the auxiliary-winding current is zero, and the motor is represented by a reduced-order model which includes only the main winding and the two equivalent rotor windings.

When modeling the various capacitor motors of Section 9.2.2, the circuit equations must take into account the fact that, while the main winding is connected directly to the single-phase supply, a capacitor is connected between the supply and the auxiliary-winding terminals. Depending upon the type of motor being modeled, the auxiliary winding may or may not be switched out as the motor comes up to speed.

Finally, the techniques of Section 3.5 can be used to show that the electromagnetic torque of this motor can be written as

$$\begin{aligned} T_{\text{mech}} &= i_{\text{main}} i_{r1} \frac{d\mathcal{L}_{\text{main},r1}(\theta_{\text{me}})}{d\theta_m} + i_{\text{main}} i_{r2} \frac{d\mathcal{L}_{\text{main},r2}(\theta_{\text{me}})}{d\theta_m} \\ &\quad + i_{\text{aux}} i_{r1} \frac{d\mathcal{L}_{\text{aux},r1}(\theta_{\text{me}})}{d\theta_m} + i_{\text{aux}} i_{r2} \frac{d\mathcal{L}_{\text{aux},r2}(\theta_{\text{me}})}{d\theta_m} \\ &= \left(\frac{\text{poles}}{2} \right) [-L_{\text{main},r} (i_{\text{main}} i_{r1} \sin \theta_{\text{me}} + i_{\text{main}} i_{r2} \cos \theta_{\text{me}}) \\ &\quad + L_{\text{aux},r} (i_{\text{aux}} i_{r1} \cos \theta_{\text{me}} - i_{\text{aux}} i_{r2} \sin \theta_{\text{me}})] \end{aligned} \quad (9.38)$$

where $\theta_m = (2/\text{poles})\theta_{\text{me}}$ is the rotor angle in radians.

Analogous to the development of the equivalent circuits derived in Chapter 6 for polyphase induction machines and earlier in this chapter for single-phase machines, the equations derived in this section can be further developed by assuming steady-state operation, with constant mechanical speed ω_{me} , corresponding to a slip s , and constant electrical supply frequency ω_e . Consistent with this assumption the rotor currents will be at frequencies $\omega_r = \omega_e - \omega_{\text{me}} = s\omega_e$ (produced by the stator positive-sequence field) and $\omega_r = \omega_e + \omega_{\text{me}} = (2-s)\omega_e$ (produced by the stator negative-sequence field).

After considerable algebraic manipulation, which includes using Eqs. 9.36 and 9.37 to eliminate the rotor currents, the main- and auxiliary-winding flux-linkage/current relationships of Eq. 9.25 can be written as phasor equations

$$\hat{\lambda}_{\text{main}} = [L_{\text{main}} - jL_{\text{main,r}}^2(\hat{K}^+ + \hat{K}^-)]\hat{I}_{\text{main}} + L_{\text{main,r}}L_{\text{aux,r}}(\hat{K}^+ - \hat{K}^-)\hat{I}_{\text{aux}} \quad (9.39)$$

and

$$\hat{\lambda}_{\text{aux}} = -L_{\text{main,r}}L_{\text{aux,r}}(\hat{K}^+ - \hat{K}^-)\hat{I}_{\text{main}} + [L_{\text{aux}} - jL_{\text{aux,r}}^2(\hat{K}^+ + \hat{K}^-)]\hat{I}_{\text{aux}} \quad (9.40)$$

where

$$\hat{K}^+ = \frac{s\omega_e}{2(R_r + js\omega_e L_r)} \quad (9.41)$$

and

$$\hat{K}^- = \frac{(2-s)\omega_e}{2(R_r + j(2-s)\omega_e L_r)} \quad (9.42)$$

Similarly, the voltage equations, Eqs. 9.34 and 9.35 become

$$\hat{V}_{\text{main}} = \hat{I}_{\text{main}}R_{\text{main}} + j\omega_e\hat{\lambda}_{\text{main}} \quad (9.43)$$

$$\hat{V}_{\text{aux}} = \hat{I}_{\text{aux}}R_{\text{aux}} + j\omega_e\hat{\lambda}_{\text{aux}} \quad (9.44)$$

The rotor currents will each consist of positive- and negative-sequence components. The complex amplitudes of the positive sequence components (at frequency $s\omega_e$) are given by

$$\hat{I}_{r1}^+ = \frac{-js\omega_e[L_{\text{main,r}}\hat{I}_{\text{main}} + jL_{\text{aux,r}}\hat{I}_{\text{aux}}]}{2(R_r + js\omega_e L_r)} \quad (9.45)$$

and

$$\hat{I}_{r2}^+ = -j\hat{I}_{r1}^+ \quad (9.46)$$

while the complex amplitudes of the negative sequence components (at frequency $(2-s)\omega_e$) are given by

$$\hat{I}_{r1}^- = \frac{-j(2-s)\omega_e[L_{\text{main,r}}\hat{I}_{\text{main}} - jL_{\text{aux,r}}\hat{I}_{\text{aux}}]}{2(R_r + j(2-s)\omega_e L_r)} \quad (9.47)$$

and

$$\hat{I}_{r2}^- = j\hat{I}_{r1}^- \quad (9.48)$$

Finally, again after careful algebraic manipulation, the time-averaged electromagnetic torque can be shown to be given by

$$\begin{aligned} \langle T_{\text{mech}} \rangle = & \left(\frac{\text{poles}}{2} \right) \text{Re} \left[(L_{\text{main,r}}^2 \hat{I}_{\text{main}} \hat{I}_{\text{main}}^* + L_{\text{aux,r}}^2 \hat{I}_{\text{aux}} \hat{I}_{\text{aux}}^*) (\hat{K}^+ - \hat{K}^-)^* \right. \\ & \left. + jL_{\text{main,r}}L_{\text{aux,r}} (\hat{I}_{\text{main}}^* \hat{I}_{\text{aux}} - \hat{I}_{\text{main}} \hat{I}_{\text{aux}}^*) (\hat{K}^+ + \hat{K}^-)^* \right] \quad (9.49) \end{aligned}$$

where the symbol $\text{Re}[\]$ again indicates the real part of a complex number and the superscript $*$ indicates the complex conjugate. Note that Eq. 9.49 is derived based upon the assumption that the various currents are expressed as rms quantities.

EXAMPLE 9.4

Consider the case of a symmetrical two-phase motor such as is discussed in Section 9.4.1. In this case, Eqs. 9.25 through 9.37 simplify with equal self and mutual inductances and resistances for the two windings. Using the notation of Section 9.4.1, ' α ' and ' β ' replacing 'main' and 'aux', the flux-linkage/current relationships of Eq. 9.39 and 9.40 become

$$\begin{aligned}\hat{\lambda}_\alpha &= [L_\alpha - jL_{\alpha,r}^2(\hat{K}^+ + \hat{K}^-)]\hat{I}_\alpha + L_{\alpha,r}^2(\hat{K}^+ - \hat{K}^-)\hat{I}_\beta \\ \hat{\lambda}_\beta &= -L_{\alpha,r}^2(\hat{K}^+ - \hat{K}^-)\hat{I}_\alpha + [L_\alpha - jL_{\alpha,r}^2(\hat{K}^+ + \hat{K}^-)]\hat{I}_\beta\end{aligned}$$

and the voltage equations (Eqs. 9.43 and 9.44) become

$$\begin{aligned}\hat{V}_\alpha &= \hat{I}_\alpha R_\alpha + j\omega_e \hat{\lambda}_\alpha \\ \hat{V}_\beta &= \hat{I}_\beta R_\alpha + j\omega_e \hat{\lambda}_\beta\end{aligned}$$

Show that when operated from a positive sequence set of voltages such that $\hat{V}_\beta = -jV_\alpha$ the single-phase equivalent circuit is that of the forward-field (positive-sequence) equivalent circuit of Fig. 9.12a.

■ Solution

Substitution of the positive-sequence voltages in the above equations and solution for the impedance $Z_\alpha = \hat{V}_\alpha / \hat{I}_\alpha$ gives

$$\begin{aligned}Z_\alpha &= R_\alpha + j\omega_e L_\alpha + \frac{(\omega_e L_{\alpha,r})^2}{(R_r/s + j\omega_e L_r)} \\ &= R_\alpha + jX_\alpha + \frac{X_{\alpha,r}^2}{(R_r/s + jX_r)}\end{aligned}$$

This equation can be rewritten as

$$Z_\alpha = R_\alpha + j(X_\alpha - X_{\alpha,r}) + \frac{jX_{\alpha,r}[j(X_r - X_{\alpha,r}) + R_r/s]}{(R_r/s + jX_r)}$$

Setting $R_\alpha \Rightarrow R_1$, $(X_\alpha - X_{\alpha,r}) \Rightarrow X_1$, $X_{\alpha,r} \Rightarrow X_m$, $(X_r - X_{\alpha,r}) \Rightarrow X_2$, and $R_r \Rightarrow R_2$, we see that this equation does indeed represent an equivalent circuit of the form of Fig. 9.12a.

Practice Problem 9.4

Analogous to the calculation of Example 9.4, show that when operated from a negative sequence set of voltages such that $\hat{V}_\beta = jV_\alpha$ the single-phase equivalent circuit is that of the backward-field (negative-sequence) equivalent circuit of Fig. 9.12b.

Solution

Under negative-sequence conditions, the impedance Z_α is equal to

$$\begin{aligned} Z_\alpha &= R_\alpha + j\omega_c L_\alpha + \frac{(\omega_c L_{\alpha,r})^2}{(R_r/(2-s) + j\omega_c L_r)} \\ &= R_\alpha + jX_\alpha + \frac{X_{\alpha,r}^2}{(R_r/(2-s) + jX_r)} \end{aligned}$$

As in Example 9.4, this can be shown to correspond to an equivalent circuit of the form of Fig. 9.12b.

EXAMPLE 9.5

A two-pole, single-phase induction motor has the following parameters

$$L_{\text{main}} = 80.6 \text{ mH} \quad R_{\text{main}} = 0.58 \ \Omega$$

$$L_{\text{aux}} = 196 \text{ mH} \quad R_{\text{aux}} = 3.37 \ \Omega$$

$$L_r = 4.7 \ \mu\text{H} \quad R_r = 37.6 \ \mu\Omega$$

$$L_{\text{main},r} = 0.588 \text{ mH} \quad L_{\text{aux},r} = 0.909 \text{ mH}$$

It is operated from a single-phase, 230-V rms, 60-Hz source as a permanent-split-capacitor motor with a $35 \ \mu\text{F}$ capacitor connected in series with the auxiliary winding. In order to achieve the required phase shift of the auxiliary-winding current, the windings must be connected with the polarities shown in Fig. 9.17. The motor has a rotational losses of 40 W and 105 W of core loss.

Consider motor operation at 3500 r/min.

- Find the main-winding, auxiliary-winding and source currents and the magnitude of the capacitor voltage.
- Find the time-averaged electromagnetic torque and shaft output power.

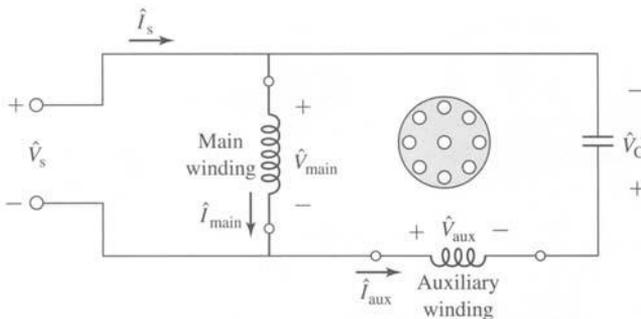


Figure 9.17 Permanent-split-capacitor induction-motor connections for Example 9.5.

- c. Calculate the motor input power and its electrical efficiency. Note that since core loss isn't explicitly accounted for in the model derived in this section, you may simply consider it as an additional component of the input power.
- d. Plot the motor time-averaged electromagnetic torque as a function of speed from standstill to synchronous speed.

■ Solution

MATLAB, with its ease of handling complex numbers, is ideal for the solution of this problem.

- a. The main winding of this motor is directly connected to the single-phase source. Thus we directly set $\hat{V}_{\text{main}} = \hat{V}_s$. However, the auxiliary winding is connected to the single-phase source through a capacitor and its polarity is reversed. Thus we must write

$$\hat{V}_{\text{aux}} + \hat{V}_C = -\hat{V}_s$$

where the capacitor voltage is given by

$$\hat{V}_C = j\hat{I}_{\text{aux}}X_C$$

Here the capacitor impedance X_C is equal to

$$X_C = -\frac{1}{(\omega_e C)} = -\frac{1}{(120\pi \times 35 \times 10^{-6})} = -75.8 \Omega$$

Setting $\hat{V}_s = V_0 = 230 \text{ V}$ and substituting these expressions into Eqs. 9.43 and 9.44 and using Eqs. 9.39 and 9.40 then gives the following matrix equation for the main- and auxiliary-winding currents.

$$\begin{bmatrix} (R_{\text{main}} + j\omega_e \hat{A}_1) & j\omega_e \hat{A}_2 \\ -j\omega_e \hat{A}_2 & (R_{\text{aux}} + jX_C + j\omega_e \hat{A}_3) \end{bmatrix} \begin{bmatrix} \hat{I}_{\text{main}} \\ \hat{I}_{\text{aux}} \end{bmatrix} = \begin{bmatrix} V_0 \\ -V_0 \end{bmatrix}$$

where

$$\hat{A}_1 = L_{\text{main}} - jL_{\text{main},r}^2(\hat{K}^+ + \hat{K}^-)$$

$$\hat{A}_2 = L_{\text{main},r}L_{\text{aux},r}(\hat{K}^+ - \hat{K}^-)$$

and

$$\hat{A}_3 = L_{\text{aux}} - jL_{\text{aux},r}^2(\hat{K}^+ + \hat{K}^-)$$

The parameters \hat{K}^+ and \hat{K}^- can be found from Eqs. 9.41 and 9.42 once the slip is found using Eq. 6.1

$$s = \frac{n_s - n}{n_s} = \frac{3600 - 3500}{3600} = 0.278$$

This matrix equation can be readily solved using MATLAB with the result

$$\hat{I}_{\text{main}} = 15.9 \angle -37.6^\circ \text{ A}$$

$$\hat{I}_{\text{aux}} = 5.20 \angle -150.7^\circ \text{ A}$$

and

$$\hat{I}_s = 18.5 \angle -22.7^\circ \text{ A}$$

The magnitude of the capacitor voltage is

$$|\hat{V}_C| = |\hat{I}_{\text{aux}} X_C| = 374 \text{ V}$$

- b. Using MATLAB the time-averaged electromagnetic torque can be found from Eq. 9.49 to be

$$\langle T_{\text{mech}} \rangle = 9.74 \text{ N} \cdot \text{m}$$

The shaft power can then be found by subtracting the rotational losses P_{rot} from the air-gap power

$$\begin{aligned} P_{\text{shaft}} &= \omega_m \langle T_{\text{mech}} \rangle - P_{\text{rot}} \\ &= \left(\frac{2}{\text{poles}} \right) (1-s) \omega_e (\langle T_{\text{mech}} \rangle) - P_{\text{rot}} \\ &= 3532 \text{ W} \end{aligned}$$

- c. The power input to the main winding can be found as

$$P_{\text{main}} = \text{Re} [V_0 \hat{I}_{\text{main}}^*] = 2893 \text{ W}$$

and that into the auxiliary winding, including the capacitor (which dissipates no power)

$$P_{\text{aux}} = \text{Re} [-V_0 \hat{I}_{\text{aux}}^*] = 1043 \text{ W}$$

The total input power, including the core loss power P_{core} is found as

$$P_{\text{in}} = P_{\text{main}} + P_{\text{aux}} + P_{\text{core}} = 4041 \text{ W}$$

Finally, the efficiency can be determined

$$\eta = \frac{P_{\text{shaft}}}{P_{\text{in}}} = 0.874 = 87.4\%$$

- d. The plot of $\langle T_{\text{mech}} \rangle$ versus speed generated by MATLAB is found in Fig. 9.18.

Here is the MATLAB script:

```
clc
clear
% Source parameters
V0 = 230;
omegae = 120*pi;
% Motor parameters
poles = 2;
Lmain = .0806;
Rmain = 0.58;
Laux = 0.196;
Raux = 3.37;
Lr = 4.7e-6;
```

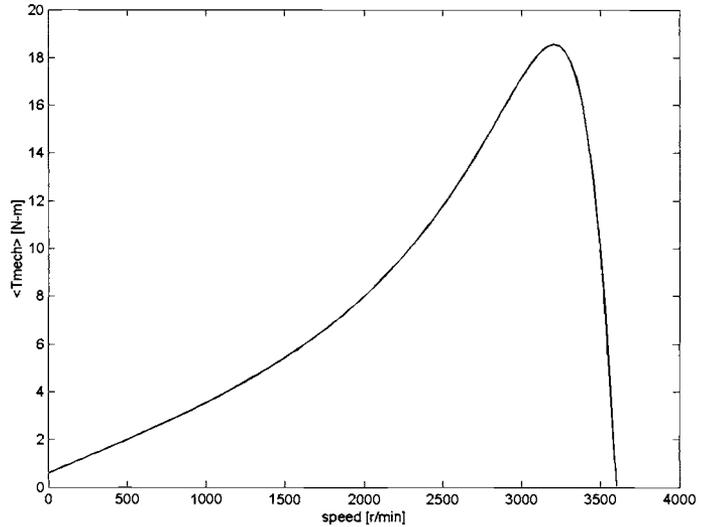


Figure 9.18 Time-averaged electromagnetic torque versus speed for the single-phase induction motor of Example 9.5.

```

Rr = 37.6e-6;
Lmainr = 5.88e-4;
Lauxr = 9.09e-4;
C = 35e-6;
Xc = -1/(omegae*C);
Prot = 40;
Pcore = 105;

% Run through program twice. If calcswitch = 1, then
% calculate at speed of 3500 r/min only. The second time
% program will produce the plot for part (d).

for calcswitch = 1:2
    if calcswitch == 1
        mmax = 1;
    else
        mmax = 101;
    end

    for m = 1:mmax
        if calcswitch == 1
            speed(m) = 3500;
        else
            speed(m) = 3599*(m-1)/100;
        end
    end
end

```

```

% Calculate the slip
ns = (2/poles)*3600;
s = (ns-speed(m))/ns;

% part (a)
% Calculate the various complex constants
Kplus = s*omegae/(2*(Rr + j*s*omegae*Lr));
Kminus = (2-s)*omegae/(2*(Rr + j*(2-s)*omegae*Lr));
A1 = Lmain - j*Lmainr^2*(Kplus+Kminus);
A2 = Lmainr*Lauxr*(Kplus-Kminus);
A3 = Laux - j*Lauxr^2*(Kplus+Kminus);

% Set up the matrix
M(1,1) = Rmain + j*omegae*A1;
M(1,2) = j*omegae*A2;
M(2,1) = -j*omegae*A2;
M(2,2) = Raux + j*Xc+ j*omegae*A3;

% Here is the voltage vector
V = [V0 ; -V0];

% Now find the current matrix
I = M\V;

Imain = I(1);
Iaux = I(2);
Is = Imain-Iaux;

magImain = abs(Imain);
angleImain = angle(Imain)*180/pi;
magIaux = abs(Iaux);
angleIaux = angle(Iaux)*180/pi;
magIs = abs(Is);
angleIs = angle(Is)*180/pi;

%Capacitor voltage
Vcap = Iaux*Xc;
magVcap = abs(Vcap);

% part (b)
Tmech1 = conj(Kplus-Kminus);
Tmech1 = Tmech1*(Lmainr^2*Imain*conj(Imain)+Lauxr^2*Iaux*conj(Iaux));
Tmech2 = j*Lmainr*Lauxr*conj(Kplus+Kminus);
Tmech2 = Tmech2*(conj(Imain)*Iaux-Imain*conj(Iaux));
Tmech(m) = (poles/2)*real(Tmech1+Tmech2);
Pshaft = (2/poles)*(1-s)*omegae*Tmech(m)-Prot;

%part (c)
Pmain = real(V0*conj(Imain));
Paux = real(-V0*conj(Iaux));

```

```

Pin = Pmain+Paux+Pcore;
eta = Pshaft/Pin;
if calcswitch == 1
    fprintf('part (a):')
    fprintf('\n Imain = %g A at angle %g degrees',magImain,angleImain)
    fprintf('\n Iaux = %g A at angle %g degrees',magIaux,angleIaux)
    fprintf('\n Is = %g A at angle %g degrees',magIs,angleIs)
    fprintf('\n Vcap = %g V\n',magVcap)
    fprintf('\npart (b):')
    fprintf('\n Tmech = %g N-m',Tmech)
    fprintf('\n Pshaft = %g W\n',Pshaft)
    fprintf('\npart (c):')
    fprintf('\n Pmain = %g W',Pmain)
    fprintf('\n Paux = %g W',Paux)
    fprintf('\n Pin = %g W',Pin)
    fprintf('\n eta = %g percent\n\n',100*eta)
else
    plot(speed,Tmech)
    xlabel('speed [r/min]')
    ylabel('<Tmech> [N-m]')
end
end %end of for m loop
end %end of for calcswitch loop

```

Practice Problem 9.5



(a) Calculate the efficiency of the single-phase induction motor of Example 9.5 operating at a speed of 3475 r/min. (b) Search over the range of capacitor values from $25 \mu\text{F}$ to $45 \mu\text{F}$ to find the capacitor value which will give the maximum efficiency at this speed and the corresponding efficiency.

Solution

- a. 86.4%
- b. $41.8 \mu\text{F}$, 86.6%

9.5 SUMMARY

One theme of this chapter is a continuation of the induction-machine theory of Chapter 6 and its application to the single-phase induction motor. This theory is expanded by a step-by-step reasoning process from the simple revolving-field theory of the symmetrical polyphase induction motor. The basic concept is the resolution of the stator mmf wave into two constant-amplitude traveling waves revolving around the air gap at synchronous speed in opposite directions. If the slip for the forward field is s ,

then that for the backward field is $(2 - s)$. Each of these component fields produces induction-motor action, just as in a symmetrical polyphase motor. From the viewpoint of the stator, the reflected effects of the rotor can be visualized and expressed quantitatively in terms of simple equivalent circuits. The ease with which the internal reactions can be accounted for in this manner is the essential reason for the usefulness of the double-revolving-field theory.

For a single-phase winding, the forward- and backward-component mmf waves are equal, and their amplitude is half the maximum value of the peak of the stationary pulsating mmf produced by the winding. The resolution of the stator mmf into its forward and backward components then leads to the physical concept of the single-phase motor described in Section 9.1 and finally to the quantitative theory developed in Section 9.3 and to the equivalent circuits of Fig. 9.11.

In most cases, single-phase induction motors are actually two-phase motors with unsymmetrical windings operated off of a single phase source. Thus to complete our understanding of single-phase induction motors, it is necessary to examine the performance of two-phase motors. Hence, the next step is the application of the double-revolving-field picture to a symmetrical two-phase motor with unbalanced applied voltages, as in Section 9.4.1. This investigation leads to the symmetrical-component concept, whereby an unbalanced two-phase system of currents or voltages can be resolved into the sum of two balanced two-phase component systems of opposite phase sequence. Resolution of the currents into symmetrical-component systems is equivalent to resolving the stator-mmf wave into its forward and backward components, and therefore the internal reactions of the rotor for each symmetrical-component system are the same as those which we have already investigated. A very similar reasoning process, not considered here, leads to the well-known three-phase symmetrical-component method for treating problems involving unbalanced operation of three-phase rotating machines. The ease with which the rotating machine can be analyzed in terms of revolving-field theory is the chief reason for the usefulness of the symmetrical-component method.

Finally, the chapter ends in Section 9.4.2 with the development of an analytical theory for the general case of a two-phase induction motor with unsymmetrical windings. This theory permits us to analyze the operation of single-phase motors running off both their main and auxiliary windings.

9.6 PROBLEMS

- 9.1 A 1-kW, 120-V, 60-Hz capacitor-start motor has the following parameters for the main and auxiliary windings (at starting):

$$Z_{\text{main}} = 4.82 + j7.25 \, \Omega \quad \text{main winding}$$

$$Z_{\text{aux}} = 7.95 + j9.21 \, \Omega \quad \text{auxiliary winding}$$

- a. Find the magnitude and the phase angles of the currents in the two windings when rated voltage is applied to the motor under starting conditions.

- b. Find the value of starting capacitance that will place the main- and auxiliary-winding currents in time quadrature at starting.
- c. Repeat part (a) when the capacitance of part (b) is inserted in series with the auxiliary winding.



- 9.2 Repeat Problem 9.1 if the motor is operated from a 120-V, 50-Hz source.
- 9.3 Given the applied electrical frequency and the corresponding impedances Z_{main} and Z_{aux} of the main and auxiliary windings at starting, write a MATLAB script to calculate the value of the capacitance, which, when connected in series with the starting winding, will produce a starting winding current which will lead that of the main winding by 90° .
- 9.4 Repeat Example 9.2 for slip of 0.045.
- 9.5 A 500-W, four-pole, 115-V, 60-Hz single-phase induction motor has the following parameters (resistances and reactances in Ω /phase):

$$R_{1,\text{main}} = 1.68 \quad R_{2,\text{main}} = 2.96$$

$$X_{1,\text{main}} = 1.87 \quad X_{m,\text{main}} = 60.6 \quad X_{2,\text{main}} = 1.72$$

$$\text{Core loss} = 38 \text{ W} \quad \text{Friction and windage} = 11.8 \text{ W}$$

Find the speed, stator current, torque, power output, and efficiency when the motor is operating at rated voltage and a slip of 4.2 percent.



- 9.6 Write a MATLAB script to produce plots of the speed and efficiency of the single-phase motor of Problem 9.5 as a function of output power over the range $0 \leq P_{\text{out}} \leq 500 \text{ W}$.
- 9.7 At standstill the rms currents in the main and auxiliary windings of a four-pole, capacitor-start induction motor are $I_{\text{main}} = 20.7 \text{ A}$ and $I_{\text{aux}} = 11.1 \text{ A}$ respectively. The auxiliary-winding current leads the main-winding current by 53° . The effective turns per pole (i.e., the number of turns corrected for the effects of winding distribution) are $N_{\text{main}} = 42$ and $N_{\text{aux}} = 68$. The windings are in space quadrature.
- a. Determine the peak amplitudes of the forward and backward stator-mmfs waves.
 - b. Suppose it were possible to adjust the magnitude and phase of the auxiliary-winding current. What magnitude and phase would produce a purely forward mmf wave?
- 9.8 Derive an expression in terms of $Q_{2,\text{main}}$ for the nonzero speed of a single-phase induction motor at which the internal torque is zero. (See Example 9.2.)
- 9.9 The equivalent-circuit parameters of an 8-kW, 230-V, 60-Hz, four-pole, two-phase, squirrel-cage induction motor in ohms per phase are

$$R_1 = 0.253 \quad X_1 = 1.14 \quad X_m = 32.7 \quad R_2 = 0.446 \quad X_2 = 1.30$$

This motor is operated from an unbalanced two-phase, 60-Hz source whose phase voltages are, respectively, 223 and 190 V, the smaller voltage leading the larger by 73° . For a slip of 0.045, find

- a. the phase currents in each of the windings and
- b. the internal mechanical power.

- 9.10** Consider the two-phase motor of Example 9.3.
- Find the starting torque for the conditions specified in the example.
 - Compare the result of part (a) with the starting torque which the motor would produce if 220-V, balanced two-phase voltages were applied to the motor.
 - Show that if the stator voltages \hat{V}_α and \hat{V}_{beta} of a two-phase induction motor are in time quadrature but unequal in magnitude, the starting torque is the same as that developed when balanced two-phase voltages of magnitude $\sqrt{V_\alpha V_\beta}$ are applied.
- 9.11** The induction motor of Problem 9.9 is supplied from an unbalanced two-phase source by a four-wire feeder having an impedance $Z = 0.32 + j1.5 \Omega/\text{phase}$. The source voltages can be expressed as

$$\hat{V}_\alpha = 235\angle 0^\circ \quad \hat{V}_\beta = 212\angle 78^\circ$$

For a slip of 5 percent, show that the induction-motor terminal voltages correspond more nearly to a balanced two-phase set than do those of the source.

- 9.12** The equivalent-circuit parameters in ohms per phase referred to the stator for a two-phase, 1.0 kW, 220-V, four-pole, 60-Hz, squirrel-cage induction motor are given below. The no-load rotational loss is 65 W.

$$R_1 = 0.78 \quad R_2 = 4.2 \quad X_1 = X_2 = 5.3 \quad X_m = 93$$

- The voltage applied to phase α is $220\angle 0^\circ$ V and that applied to phase β is $220\angle 65^\circ$ V. Find the net air-gap torque at a slip $s = 0.035$.
 - What is the starting torque with the applied voltages of part (a)?
 - The applied voltages are readjusted so that $\hat{V}_\alpha = 220\angle 65^\circ$ V and $\hat{V}_\beta = 220\angle 90^\circ$ V. Full load on the machine occurs at $s = 0.048$. At what slip does maximum internal torque occur? What is the value of the maximum torque?
 - While the motor is running as in part (c), phase β is open-circuited. What is the power output of the machine at a slip $s = 0.04$?
 - What voltage appears across the open phase- β terminals under the conditions of part (d)?
- 9.13** A 120-V, 60-Hz, capacitor-run, two-pole, single-phase induction motor has the following parameters:

$$L_{\text{main}} = 47.2 \text{ mH} \quad R_{\text{main}} = 0.38 \Omega$$

$$L_{\text{aux}} = 102 \text{ mH} \quad R_{\text{aux}} = 1.78 \Omega$$

$$L_r = 2.35 \mu\text{H} \quad R_r = 17.2 \mu\Omega$$

$$L_{\text{main},r} = 0.342 \text{ mH} \quad L_{\text{aux},r} = 0.530 \text{ mH}$$



You may assume that the motor has 48 W of core loss and 23 W of rotational losses. The motor windings are connected with the polarity shown in Fig. 9.17 with a 40 μF run capacitor.

- a. Calculate the motor starting torque.

With the motor operating at a speed of 3490 r/min, calculate

- b. the main and auxiliary-winding currents,
- c. the total line current and the motor power factor,
- d. the output power and
- e. the electrical input power and the efficiency.

Note that this problem is most easily solved using MATLAB.



- 9.14** Consider the single-phase motor of Problem 9.13. Write a MATLAB script to search over the range of capacitor values from $25 \mu\text{F}$ to $75 \mu\text{F}$ to find the value which will maximize the motor efficiency at a motor speed of 3490 r/min. What is the corresponding maximum efficiency?



- 9.15** In order to raise the starting torque, the single-phase induction motor of Problem 9.13 is to be converted to a capacitor-start, capacitor-run motor. Write a MATLAB script to find the minimum value of starting capacitance required to raise the starting torque to $0.5 \text{ N} \cdot \text{m}$.



- 9.16** Consider the single-phase induction motor of Example 9.5 operating over the speed range 3350 r/min to 3580 r/min.
- a. Use MATLAB to plot the output power over the given speed range.
 - b. Plot the efficiency of the motor over this speed range.
 - c. On the same plot as that of part (b), plot the motor efficiency if the run capacitor is increased to $45 \mu\text{F}$.

Introduction to Power Electronics

Until the last few decades of the twentieth century, ac machines tended to be employed primarily as single-speed devices. Typically they were operated from fixed-frequency sources (in most cases this was the 50- or 60-Hz power grid). In the case of motors, control of motor speed requires a variable-frequency source, and such sources were not readily available. Thus, applications requiring variable speed were serviced by dc machines, which can provide highly flexible speed control, although at some cost since they are more complex, more expensive, and require more maintenance than their ac counterparts.

The availability of solid-state power switches changed this picture immensely. It is now possible to build power electronics capable of supplying the variable-voltage/current, variable-frequency drive required to achieve variable-speed performance from ac machines. Ac machines have now replaced dc machines in many traditional applications, and a wide range of new applications have been developed.

As is the case with electromechanics and electric machinery, power electronics is a discipline which can be mastered only through significant study. Many books have been written on this subject, a few of which are listed in the bibliography at the end of this chapter. It is clear that a single chapter in a book on electric machinery cannot begin to do justice to this topic. Thus our objectives here are limited. Our goal is to provide an overview of power electronics and to show how the basic building blocks can be assembled into drive systems for ac and dc machines. We will not focus much attention on the detailed characteristics of particular devices or on the many details required to design practical drive systems. In Chapter 11, we will build on the discussion of this chapter to examine the characteristics of some common drive systems.

10.1 POWER SWITCHES

Common to all power-electronic systems are switching devices. Ideally, these devices control current much like valves control the flow of fluids: turn them “ON,” and they present no resistances to the flow of current; turn them “OFF,” and no current flow is possible. Of course, practical switches are not ideal, and their specific characteristics significantly affect their applicability in any given situation. Fortunately, the essential performance of most power-electronic circuits can be understood assuming the switches to be ideal. This is the approach which we will adopt in this book. In this section we will briefly discuss some of the common switching devices and present simplified, idealized models for them.

10.1.1 Diodes

Diodes constitute the simplest of power switches. The general form of the v - i characteristics of a diode is shown in Fig. 10.1.

The essential features of a diode are captured in the idealized v - i characteristic of Fig. 10.2a. The symbol used to represent a diode is shown in Fig. 10.2b along with the reference directions for the current i and voltage v . Based upon terminology developed when rectifier diodes were electron tubes, diode current flows into the *anode* and flows out of the *cathode*.

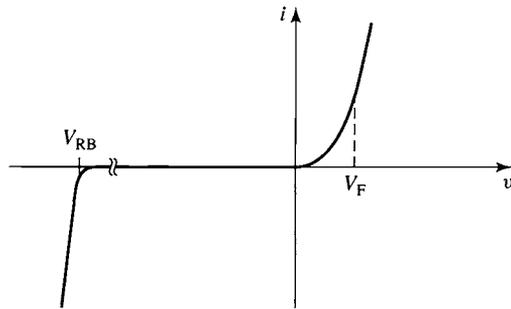


Figure 10.1 v - i characteristic of a diode.

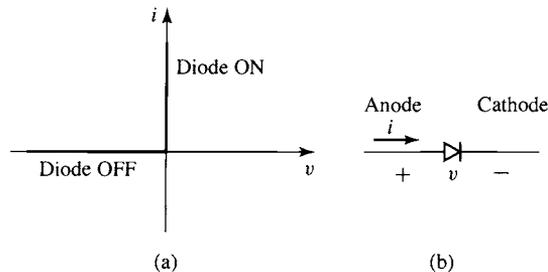


Figure 10.2 (a) v - i characteristic of an ideal diode. (b) Diode symbol.

We can see that the ideal diode blocks current flow when the voltage is negative ($i = 0$ for $v < 0$) and passes positive current without voltage drop ($v = 0$ for $i \geq 0$). We will refer to the negative-voltage region as the diode's OFF state and the positive-current region as the diode's ON state. Comparison with the v - i characteristic shows that a practical diode varies from an ideal diode in that:

- There is a finite *forward voltage drop*, labeled V_F in Fig. 10.1, for positive current flow. For low-power devices, this voltage range is typically on the order of 0.6–0.7 V while for high-power devices it can exceed 3 V.
- Corresponding to this voltage drop is a power dissipation. Practical diodes have a maximum power dissipation (and a corresponding maximum current) which must not be exceeded.
- A practical diode is limited in the negative voltage it can withstand. Known as the *reverse-breakdown voltage* and labeled V_{RB} in Fig. 10.1, this is the maximum reverse voltage that can be applied to the diode before it starts to conduct reverse current.

The diode is the simplest power switch in that it cannot be controlled; it simply turns ON when positive current begins to flow and turns OFF when the current attempts to reverse. In spite of this simple behavior, it is used in a wide variety of applications, the most common of which is as a rectifier to convert ac to dc.

The basic performance of a diode can be illustrated by the simple example shown in Example 10.1.

EXAMPLE 10.1

Consider the *half-wave rectifier* circuit of Fig. 10.3a in which a resistor R is supplied by a voltage source $v_s(t) = V_0 \sin \omega t$ through a diode. Assume the diode to be ideal. (a) Find the resistor voltage $v_R(t)$ and current $i_R(t)$. (b) Find the dc average resistor voltage V_{dc} and current I_{dc} .

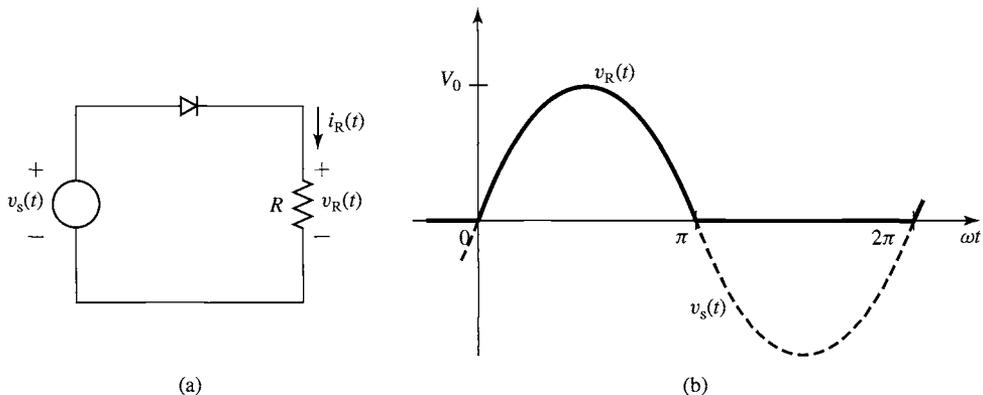


Figure 10.3 (a) Half-wave rectifier circuit for Example 10.1. (b) Resistor voltage.

■ Solution

- a. This is a nonlinear problem in that it is not possible to write an analytic expression for the v - i characteristic of the ideal diode. However, it can readily be solved using the *method-of-assumed-states* in which, for any given value of the source voltage, the diode is alternately assumed to be ON (a short-circuit) or OFF (an open-circuit) and the current is found. One of the two solutions will violate the v - i characteristic of the diode (i.e., there will be negative current flow through the short-circuit or positive voltage across the open-circuit) and must be discarded; the remaining solution will be the correct one.

Following the above procedure, we find that the solution is given by

$$v_R(t) = \begin{cases} v_s(t) = V_0 \sin \omega t & v_s(t) \geq 0 \\ 0 & v_s(t) < 0 \end{cases}$$

This voltage is plotted in Fig. 10.3b. The current is identical in form and is found simply as $i_R(t) = v_R(t)/R$. The terminology *half-wave rectification* is applied to this system because voltage is applied to the resistor during only the half cycle for which the supply voltage waveform is positive.

- b. The dc or average value of the voltage waveform is equal to

$$V_{dc} = \frac{\omega}{\pi} \int_0^{\frac{\pi}{\omega}} V_0 \sin(\omega t) dt = \frac{V_0}{\pi}$$

and hence the dc current through the resistor is equal to

$$I_{dc} = \frac{V_0}{\pi R}$$

Practice Problem 10.1

Calculate the average voltage across the resistor of Fig. 10.3 if the sinusoidal voltage source of Example 10.1 is replaced by a source of the same frequency but which produces a square wave of zero average value and peak-peak amplitude $2V_0$.

Solution

$$V_{dc} = \frac{V_0}{2}$$

10.1.2 Silicon Controlled Rectifiers and TRIACs

The characteristics of a *silicon controlled rectifier*, or *SCR*, also referred to as a *thyristor*, are similar to those of a diode. However, in addition to an anode and a cathode, the SCR has a third terminal known as the *gate*. Figure 10.4 shows the form of the v - i characteristics of a typical SCR.

As is the case with a diode, the SCR will turn ON only if the anode is positive with respect to the cathode. Unlike a diode, the SCR also requires a pulse of current i_G into the gate to turn ON. Note however that once the SCR turns ON, the gate signal

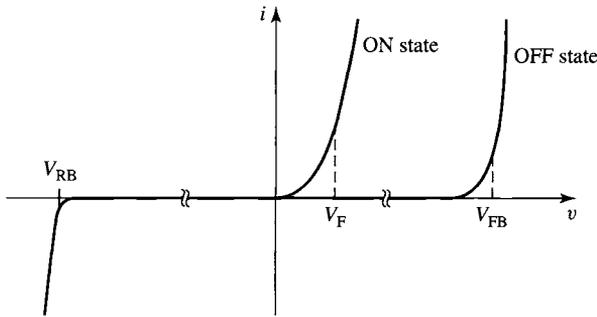


Figure 10.4 v - i characteristic of an SCR.

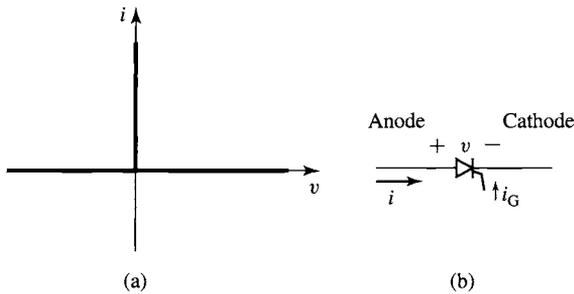


Figure 10.5 (a) Idealized SCR v - i characteristic.
(b) SCR symbol.

can be removed and the SCR will remain ON until the SCR current drops below a small value referred to as the *holding current*, at which point it will turn OFF just as a diode does.

As can be seen from Fig. 10.4, the ON-state characteristic of an SCR is similar to that of a diode, with a forward voltage drop V_F and a reverse-breakdown voltage V_{RB} . When the SCR is OFF, it does not conduct current over its normal operating range of positive voltage. However it will conduct if this voltage exceeds a characteristic voltage, labeled V_{FB} in the figure and known as the *forward-breakdown voltage*. As is the case for a diode, a practical SCR is limited in its current-carrying capability.

For our purposes, we will simplify these characteristics and assume the SCR to have the idealized characteristics of Fig. 10.5a. Our idealized SCR appears as an open-circuit when it is OFF and a short-circuit when it is ON. It also has a holding current of zero; i.e., it will remain ON until the current drops to zero and attempts to go negative. The symbol used to represent an SCR is shown in Fig. 10.5b.

Care must be taken in the design of gate-drive circuitry to insure that an SCR turns on properly; e.g., the gate pulse must inject enough charge to fully turn on the SCR, and so forth. Similarly, an additional circuit, typically referred to as a *snubber* circuit, may be required to protect an SCR from being turned on inadvertently, such as might occur if the rate of rise of the anode-to-cathode voltage is excessive. Although

these details must be properly accounted for to achieve successful SCR performance in practical circuits, they are not essential for the present discussion.

The basic performance of an SCR can be understood from the following example.

EXAMPLE 10.2

Consider the *half-wave rectifier* circuit of Fig. 10.6 in which a resistor R is supplied by a voltage source $v_s(t) = V_0 \sin \omega t$ through an SCR. Note that this is identical to the circuit of Example 10.1, with the exception that the diode has been replaced by an SCR.

Assume that a pulse of gate current is applied to the SCR at time t_0 ($0 \leq t_0 < \pi/\omega$) following each zero-crossing of the source voltage, as shown in Fig. 10.7a. It is common to describe this *firing-delay time* in terms of a *firing-delay angle*, $\alpha_0 \equiv \omega t_0$. Find the resistor voltage $v_R(t)$ as a function of α_0 . Assume the SCR to be ideal and that the gate pulses supply sufficient charge to properly turn ON the SCR.

■ Solution

The solution follows that of Example 10.1 with the exception that, independent of the polarity of the voltage across it, once the SCR turns OFF, it will remain OFF until both the SCR voltage becomes positive and a pulse of gate current is applied. Once a gate pulse has been applied, the method-of-assumed-states can be used to solve for the state of the SCR.

Following the above procedure, we find the solution is given by

$$v_R(t) = \begin{cases} 0 & v_s(t) \geq 0 \quad (\text{prior to the gate pulse}) \\ v_s(t) = V_0 \sin \omega t & v_s(t) \geq 0 \quad (\text{following the gate pulse}) \\ 0 & v_s(t) < 0 \end{cases}$$

This voltage is plotted in Fig. 10.7b. Note that this system produces a half-wave rectified voltage similar to that of the diode system of Example 10.1. However, in this case, the dc value of the rectified voltage can be controlled by controlling the timing of the gate pulse. Specifically, it is given by

$$V_{dc} = \frac{V_0}{2\pi} (1 + \cos \alpha_0)$$

Note that when there is no delay in firing the SCR ($\alpha_0 = 0$), this system produces a dc voltage of V_0/π , equal to that of the diode rectifier system of Example 10.1. However, as the gate pulse of the SCR is delayed (i.e., by increasing α_0), the dc voltage can be reduced. In fact, by delaying the gate pulse a full half cycle ($\alpha_0 = \pi$) the dc voltage can be reduced to

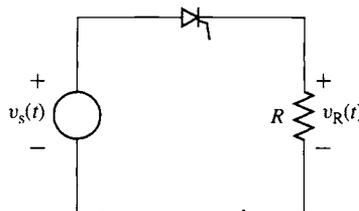


Figure 10.6 Half-wave SCR rectifier circuit for Example 10.2.

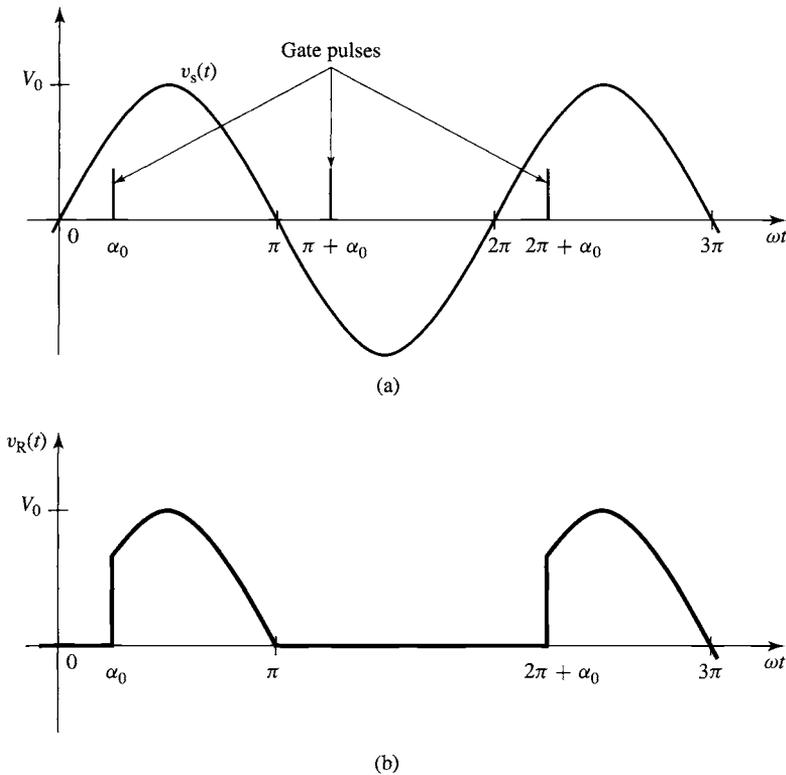


Figure 10.7 (a) Gate pulses for Example 10.2. (b) Resistor voltage.

zero. This system is known as a *phase-controlled rectifier* because the dc output voltage can be varied by controlling the phase angle of the gate pulse relative to the zero crossing of the source voltage.

Practice Problem 10.2

Calculate the resistor average voltage as a function of the delay angle α_0 if the sinusoidal source of Example 10.2 is replaced by a source of the same frequency, but which produces a square wave of zero average value and peak-peak amplitude $2V_0$.

Solution

$$V_{dc} = \frac{V_0}{2} \left(1 - \frac{\alpha_0}{\pi} \right)$$

Example 10.2 shows that the SCR provides a significant advantage over the diode in systems where voltage control is desired. However, this advantage comes at the additional expense of the SCR as well as the circuitry required to produce the gate pulses used to fire the SCR.

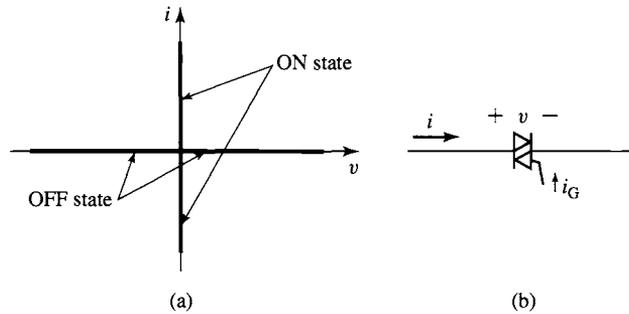


Figure 10.8 (a) Idealized TRIAC v - i characteristic. (b) TRIAC symbol.

Another phase-controlled device is the *TRIAC*, which behaves much like two back-to-back SCRs sharing a common gate. The idealized v - i characteristic of a TRIAC is shown in Fig. 10.8a and its symbol in Fig. 10.8b. As with an SCR, TRIACs can be turned ON by the application of a pulse of current at their gate. Unlike an SCR, provided the current pulses inject sufficient charge, both positive and negative gate current pulses can be used to turn ON a TRIAC.

The use of a TRIAC is illustrated in the following example.

EXAMPLE 10.3

Consider the circuit of Fig. 10.9 in which the SCR of Example 10.2 has been replaced by a TRIAC.

Assume again that a short gate pulse is applied to the SCR at a delay angle α_0 ($0 \leq \alpha_0 < \pi$) following each zero-crossing of the source voltage, as shown in Fig. 10.10a. Find the resistor voltage $v_R(t)$ and its rms value $V_{R,rms}$ as a function of α_0 . Assume the TRIAC to be ideal and that the gate pulses inject sufficient charge to properly turn it ON.

■ Solution

The solution to this example is similar to that of Example 10.2 with the exception that the TRIAC, which will permit current to flow in both directions, turns on each half cycle of the source-voltage waveform.

$$v_R(t) = \begin{cases} 0 & \text{(prior to the gate pulse)} \\ v_s(t) = V_0 \sin \omega t & \text{(following the gate pulse)} \end{cases}$$

Unlike the rectification of Example 10.2, in this case the resistor voltage, shown in Fig. 10.10b, has no dc component. However, its rms value varies with α_0 :

$$\begin{aligned} V_{R,rms} &= V_0 \sqrt{\left(\frac{\omega}{\pi} \int_{\frac{\alpha_0}{\omega}}^{\frac{\pi}{\omega}} \sin^2(\omega t) dt \right)} \\ &= V_0 \sqrt{\left(\frac{1}{2} - \frac{\alpha_0}{2\pi} + \frac{1}{4\pi} \sin(2\alpha_0) \right)} \end{aligned}$$

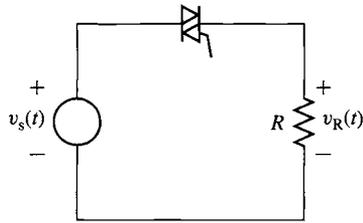


Figure 10.9 Circuit for Example 10.3.

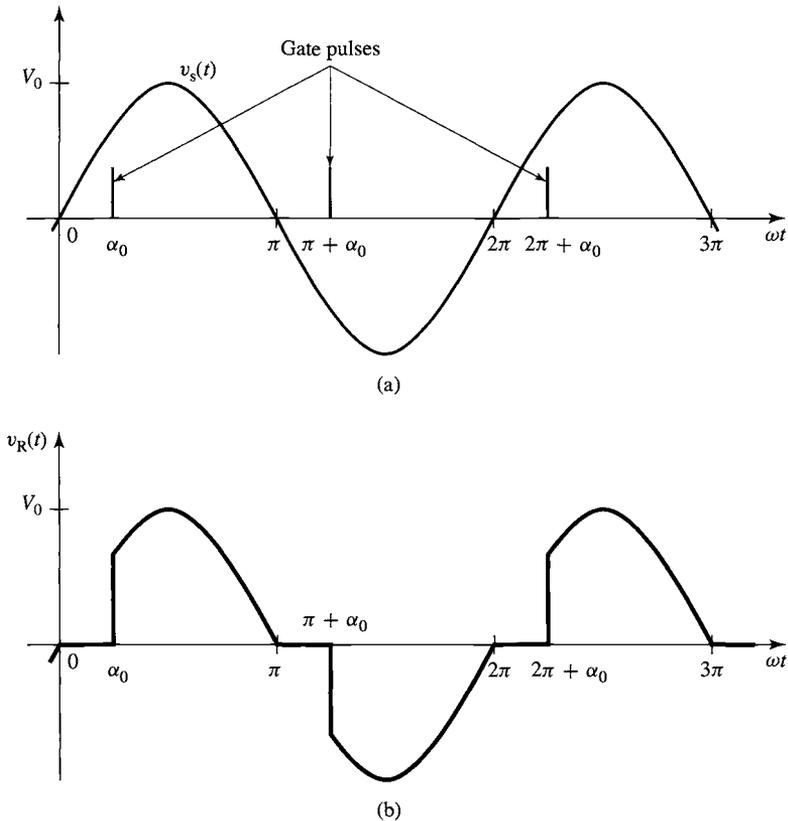


Figure 10.10 (a) Gate pulses for Example 10.3. (b) Resistor voltage.

Notice that when $\alpha_0 = 0$, the TRIAC is ON all the time and it appears that the resistor is connected directly to the voltage source. In this case, $V_{R,\text{rms}} = V_0/\sqrt{2}$ as expected. As α_0 is increased to π , the rms voltage decreases towards zero.

This simple type of controller can be applied to an electric light bulb (in which case it serves as *light dimmer*) as well as to a resistive heater. It is also used to vary the speed of a

universal motor and finds widespread application as a speed-controller in small ac hand tools, such as hand drills, as well as in small appliances, such as electric mixers, where continuous speed variation is desired.

Practice Problem 10.3

Find the rms resistor voltage for the system of Example 10.3 if the sinusoidal source has been replaced by a source of the same frequency but which produces a square wave of zero average value and peak-peak amplitude $2V_0$.

Solution

$$V_{R,\text{rms}} = V_0 \sqrt{\left(1 - \frac{\alpha_0}{\pi}\right)}$$

10.1.3 Transistors

For power-electronic circuits where control of voltages and currents is required, power transistors have become a common choice for the controllable switch. Although a number of types are available, we will consider only two: the *metal-oxide-semiconductor field effect transistor (MOSFET)* and the *insulated-gate bipolar transistor (IGBT)*.

MOSFETs and IGBTs are both three-terminal devices. Figure 10.11a shows the symbols for n- and p-channel MOSFETs, while Fig. 10.11b shows the symbol for n- and p-channel IGBTs. In the case of the MOSFET, the three terminals are referred to as the *source*, *drain*, and *gate*, while in the case of the IGBT the corresponding

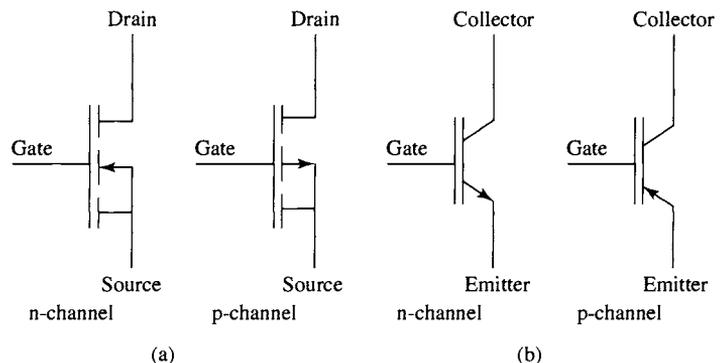


Figure 10.11 (a) Symbols for n- and p-channel MOSFETs.
(b) Symbols for n- and p-channel IGBTs.

terminals are the *emitter*, *collector*, and *gate*. For the MOSFET, the control signal is the gate-source voltage, v_{GS} . For the IGBT, it is the gate-emitter voltage, v_{GE} . In both the MOSFET and the IGBT, the gate electrode is capacitively coupled to the remainder of the device and appears as an open circuit at dc, drawing no current, and drawing only a small capacitive current under ac operation.

Figure 10.12a shows the v - i characteristic of a typical n-channel MOSFET. The characteristic of the corresponding p-channel device looks the same, with the exception that the signs of the voltages and the currents are reversed. Thus, in an n-channel device, current flows from the drain to the source when the drain-source and

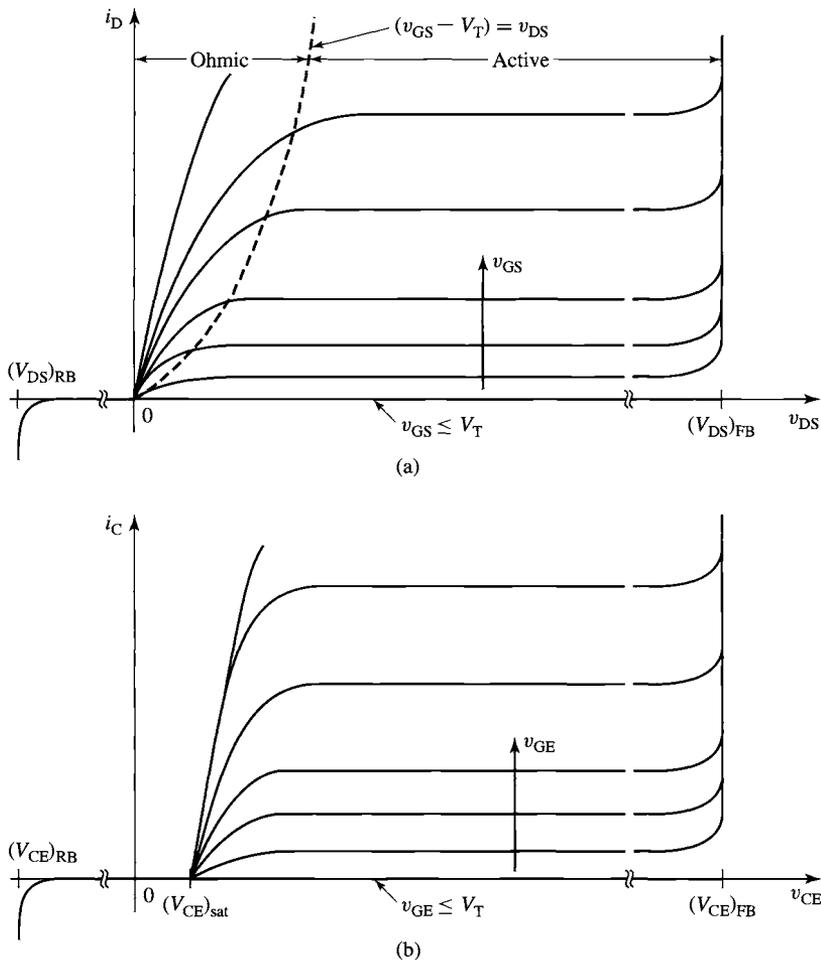


Figure 10.12 (a) Typical v - i characteristic for an n-channel MOSFET. (b) Typical v - i characteristic for an n-channel IGBT.

gate-source voltages are positive, while in a p-channel device current flows from the source to the drain when the drain-source and gate-source voltages are negative.

Note the following features of the MOSFET and IGBT characteristics:

- In the case of the MOSFET, for positive drain-source voltage v_{DS} , no drain current will flow for values of v_{GS} less than a *threshold voltage* which we will refer to by the symbol V_T . Once v_{GS} exceeds V_T , the drain current i_D increases as v_{GS} is increased.

In the case of the IGBT, for positive collector-emitter voltage v_{CE} , no collector current will flow for values of v_{GE} less than a threshold voltage V_T . Once v_{GE} exceeds V_T , the collector current i_C increases as v_{GE} is increased.

- In the case of the MOSFET, no drain current flows for negative drain-source voltage.

In the case of the IGBT, no collector current flows for negative collector-emitter voltage.

- Finally, the MOSFET will fail if the drain-source voltage exceeds its breakdown limits; in Fig. 10.12a, the forward breakdown voltage is indicated by the symbol $(V_{DS})_{FB}$ while the reverse breakdown voltage is indicated by the symbol $(V_{DS})_{RB}$.

Similarly, the IGBT will fail if the collector-emitter voltage exceeds its breakdown values; in Fig. 10.12b, the forward breakdown voltage is indicated by the symbol $(V_{CE})_{FB}$ while the reverse breakdown voltage is indicated by the symbol $(V_{CE})_{RB}$.

- Although not shown in the figure, a MOSFET will fail due to excessive gate-source voltage as well as excessive drain current which leads to excessive power dissipation in the device. Similarly an IGBT will fail due to excessive gate-emitter voltage and excessive collector current.

Note that for small values of v_{CE} , the IGBT voltage approaches a constant value, independent of the drain current. This *saturation voltage*, labeled $(V_{CE})_{sat}$ in the figure, is on the order of a volt or less in small devices and a few volts in high-power devices. Correspondingly, in the MOSFET, for small values of v_{DS} , v_{DS} is proportional to the drain current and the MOSFET behaves as a small resistance whose value decreases with increasing v_{GS} .

Fortunately, for our purposes, the details of these characteristics are not important. As we will see in the following example, with a sufficient large gate signal, the voltage drop across both the MOSFET and the IGBT can be made quite small. In this case, these devices can be modeled as a short circuit between the drain and the source in the case of the MOSFET and between the collector and the emitter in the case of the IGBT. Note, however, these “switches” when closed carry only unidirectional current, and hence we will model them as a switch in series with an ideal diode. This *ideal-switch model* is shown in Fig. 10.13a.

In many cases, these devices are commonly protected by reverse-biased protection diodes connected between the drain and the source (in the case of a MOSFET) or between the collector and emitter (in the case of an IGBT). These protection

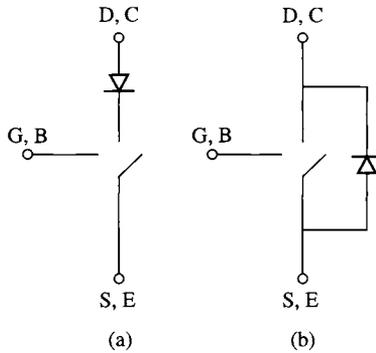


Figure 10.13 (a) Ideal-switch model for a MOSFET or an IGBT showing the series ideal diode which represents the unidirectional-current device characteristic. (b) Ideal-switch model for devices which include a reverse-biased protection diode. The symbols G, D, and S apply to the MOSFET while the symbols B, C, and E apply to the IGBT.

devices are often included as integral components within the device package. If these protection diodes are included, there is actually no need to include the series diode, in which case the model can be reduced to that of Fig. 10.13b.

EXAMPLE 10.4

Consider the circuit of Fig. 10.14a. Here we see an IGBT which is to be used to control the current through the resistor R as supplied from a dc source V_0 . Assume that the IGBT characteristics are those of Fig. 10.12b and that V_0 is significantly greater than the saturation voltage. Show a graphical procedure that can be used to find v_{CE} as a function of v_{GE} .

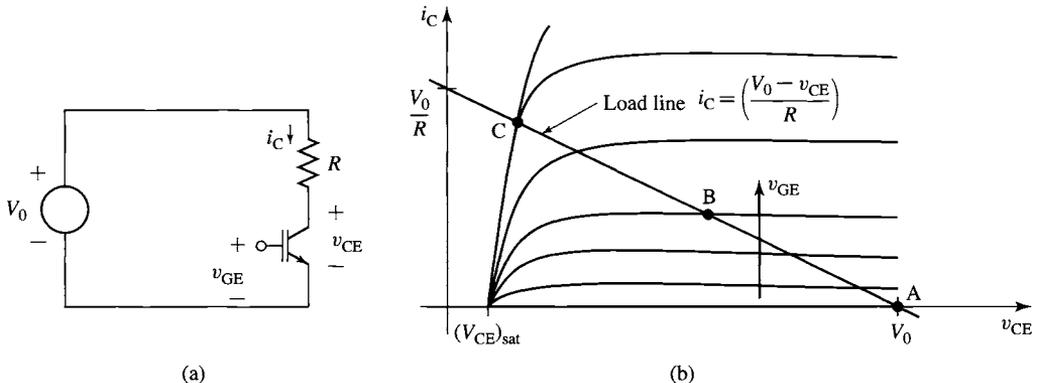


Figure 10.14 (a) Circuit for Example 10.4. (b) IGBT characteristic showing load line and operating point.

■ Solution

Writing KVL for the circuit of Fig. 10.14a gives

$$V_0 = i_C R + v_{CE}$$

Solving for i_C gives

$$i_C = \frac{(V_0 - v_{CE})}{R}$$

Note that this linear relationship, referred to as the *load line*, represents a constraint imposed by the external circuit on the relationship between the IGBT terminal variables i_C and v_{CE} . The corresponding constraint imposed by the IGBT itself is given by the v - i characteristic of Fig. 10.12b.

The operating point of the circuit is that point at which both these constraints are simultaneously satisfied. It can be found most easily by plotting the load line on the v - i relationship of the IGBT. This is done in Fig. 10.14b. The operating point is then found from the intersection of the load line with the v - i characteristic of the IGBT.

Consider the operating point labeled A in Fig. 10.14b. This is the operating point corresponding to values of v_{GE} less than or equal to the threshold voltage V_T . Under these conditions, the IGBT is OFF, there is no collector current, and hence $v_{CE} = V_0$. As v_{GE} is increased past V_T , collector current begins to flow, the operating point begins to climb up the load line, and v_{CE} decreases; the operating point labeled B is a typical example.

Note however that as v_{GE} is further increased, the operating point approaches that portion of the IGBT characteristic for which the curves crowd together (see the operating point labeled C in Fig. 10.14b). Once this point is reached, any further increase in v_{GE} will result in only a minimal decrease in v_{CE} . Under this condition, the voltage across the IGBT is approximately equal to the saturation voltage $(V_{CE})_{sat}$.

If the IGBT of this example were to be replaced by a MOSFET the result would be similar. As the gate-source voltage v_{GS} is increased, a point is reached where the voltage drop across approaches a small constant value. This can be seen by plotting the load line on the MOSFET characteristic of Fig. 10.12a.

The load line intersects the vertical axis at a collector current of $i_C = V_0/R$. Note that the larger the resistance, the lower this intersection and hence the smaller the value of v_{GE} required to saturate the transistor. Thus, in systems where the transistor is to be used as a switch, it is necessary to insure that the device is capable of carrying the required current and that the gate-drive circuit is capable of supplying sufficient drive to the gate.

Example 10.4 shows that when a sufficiently large gate voltage is applied, the voltage drop across a power transistor can be reduced to a small value. Under these conditions, the IGBT will look like a constant voltage while the MOSFET will appear as a small resistance. In either case, the voltage drop will be small, and it is sufficient to approximate it as a closed switch (i.e., the transistor will be ON). When the gate drive is removed (i.e., reduced below V_T), the switch will open and the transistor will turn OFF.

10.2 RECTIFICATION: CONVERSION OF AC TO DC

The power input to many motor-drive systems comes from a constant-voltage, constant-frequency source (e.g., a 50- or 60-Hz power system), while the output must provide variable-voltage and/or variable-frequency power to the motor. Typically such systems convert power in two stages: the input ac is first *rectified* to dc, and the dc is then converted to the desired ac output waveform. We will thus begin with a discussion of rectifier circuits. We will then discuss inverters, which convert dc to ac, in Section 10.3.

10.2.1 Single-Phase, Full-Wave Diode Bridge

Example 10.1 illustrates a half-wave rectifier circuit. Such rectification is typically used only in small, low-cost, low-power applications. Full-wave rectifiers are much more common. Consider the *full-wave rectifier* circuit of Fig. 10.15a. Here the resistor R is supplied from a voltage source $v_s(t) = V_0 \sin \omega t$ through four diodes connected in a *full-wave bridge* configuration.

If we assume the diodes to be ideal, we can use the method-of-assumed states to show that the allowable diode states are:

Diodes D1 and D3 ON, diodes D2 and D4 OFF for $v_s(t) > 0$

Diodes D2 and D4 ON, diodes D1 and D3 OFF for $v_s(t) < 0$

The resistor voltage, plotted in Fig. 10.15b, is then given by

$$v_R(t) = \begin{cases} v_s(t) = V_0 \sin \omega t & v_s(t) \geq 0 \\ -v_s(t) = -V_0 \sin \omega t & v_s(t) < 0 \end{cases} \quad (10.1)$$

Now notice that the resistor voltage is positive for both polarities of the source voltage, hence the terminology *full-wave rectification*. The dc or average value of this waveform can be seen to be twice that of the half-wave rectified waveform of Example 10.1.

$$V_{dc} = \left(\frac{2}{\pi}\right) V_0 \quad (10.2)$$

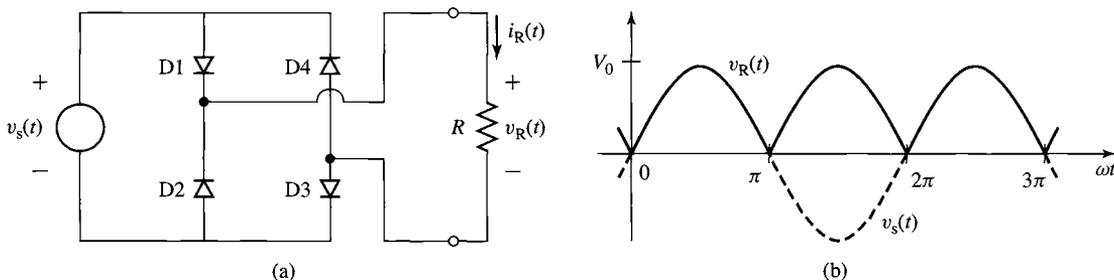


Figure 10.15 (a) Full-wave bridge rectifier. (b) Resistor voltage.

The rectified waveforms of Figs. 10.3b and 10.15b are clearly not the sort of “dc” waveforms that are considered desirable for most applications. Rather, to be most useful, the rectified dc should be relatively constant and ripple free. Such a waveform can be achieved using a filter capacitor, as illustrated in Example 10.5.

EXAMPLE 10.5



As shown in Fig. 10.16, a filter capacitor has been added in parallel with the load resistor in the full-wave rectifier system of Fig. 10.15. For the purposes of this example, assume that $v_s(t) = V_0 \sin \omega t$ with $V_0 = \sqrt{2} (120) \text{ V}$, $\omega = (2\pi)60 \approx 377 \text{ rad/sec}$ and that $R = 10 \Omega$ and $C = 10^4 \mu\text{F}$. Plot the resistor voltage, $v_R(t)$, current, $i_R(t)$, and the total bridge current, $i_B(t)$.

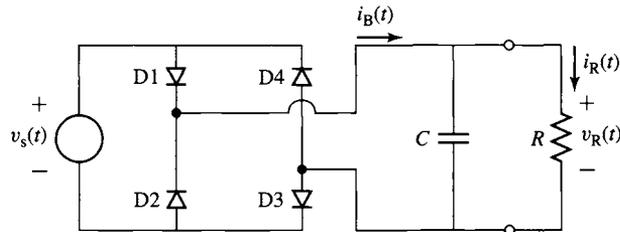


Figure 10.16 Full-wave bridge rectifier with capacitive filter for Example 10.5.

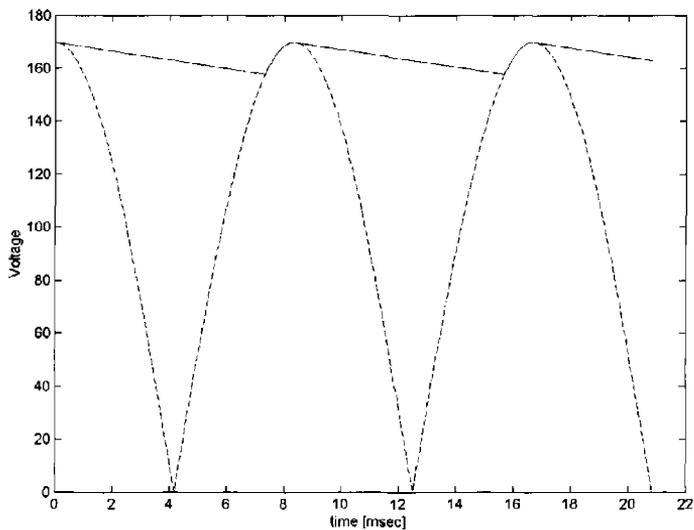
■ Solution

The addition of the filter capacitor will tend to maintain the resistor voltage $v_R(t)$ as the source voltage drops. The diodes will remain ON as long as the bridge output current remains positive and will switch OFF when this current starts to reverse.

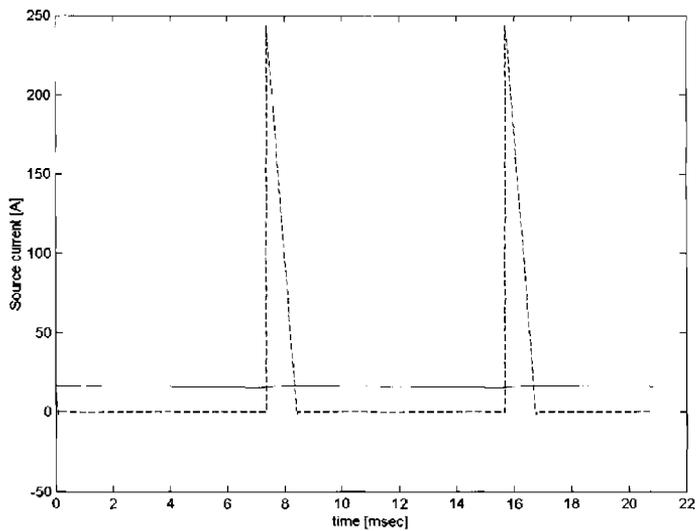
This example can be readily solved using MATLAB.[†] Figure 10.17a shows the resistor voltage $v_R(t)$ plotted along with the rectified source voltage. During the time that the bridge is ON, i.e., one pair of diodes is conducting, the resistor voltage is equal to the rectified source voltage. When the bridge is OFF, the resistor voltage decays exponentially.

Notice that because the capacitor is relatively large (the RC time constant is 100 msec as compared to the period of the rectified source voltage, which is slightly over 8.3 msec) the diodes conduct only for a short amount of time around the peak of the rectified-source-voltage waveform. This can be readily seen from the expanded plots of the resistor current and the bridge current in Fig. 10.17b. Although the resistor current remains continuous and relatively constant, varying between 15.8 and 17 A, the bridge output current consists essentially of a

[†] MATLAB is a registered trademark of The MathWorks, Inc.



(a)



(b)

Figure 10.17 Example 10.5. (a) Resistor voltage and rectified source voltage (dashed). (b) Resistor current and total bridge current (dashed).

current pulse which flows for less than 0.9 msec near the peak of the rectified voltage waveform and has a peak value of 250 A. It should be pointed out that the peak current in a practical circuit will be smaller than 250 A, being limited by circuit impedances, diode drops, and so on.

Using MATLAB, it is possible to calculate the rms value of the resistor current to be 16.4 A while that of the bridge current is 51.8 A. We see therefore that the bridge diodes in such a system must be rated for rms currents significantly in excess of that of the load. The data sheets for power-supply diodes typically indicate their rms current ratings, specifically with these sorts of applications in mind. Such peaked supply currents are characteristic of rectifier circuits with capacitive loads and can significantly affect the voltage waveforms on ac power systems when they become a significant fraction of the overall system load.

The *ripple voltage* in the resistor voltage is defined as the difference between its maximum and minimum values. In this example, the maximum value is equal to the peak value of the source voltage, or 169.7 V. The minimum value can be found from the MATLAB solution to be 157.8 V. Thus the ripple voltage is 11.9 V. Clearly the ripple voltage can be decreased by increasing the value of the filter capacitor. Note however that this comes at the expense of increased cost as well as shorter current pulses and higher rms current through the rectifier diodes.

Here is the MATLAB script for Example 10.5.

```

clc
clear

%parameters
omega = 2*pi*60;
R = 10;
C = 0.01;
V0 = 120*sqrt(2);
tau = R*C;
Nmax = 800;

% diode = 1 when rectifier bridge is conducting
diode = 1;

%Here is the loop that does the work.
for n = 1:Nmax+1
    t(n) = (2.5*pi/omega)*(n-1)/Nmax; %time
    vs(n) = V0*cos(omega*t(n)); %source voltage
    vrect(n) = abs(vs(n)); %full-wave rectified source voltage

%Calculations if the rectifier bridge is ON
if diode == 1

%If the bridge is ON, the resistor voltage is equal to the rectified
%source voltage.
vR(n) = vrect(n);

%Check the total current out of rectifiers
if (omega*t(n)) <= pi/2.
    iB(n) = vR(n)/R - V0*C*omega*sin(omega*t(n));
elseif (omega*t(n)) <= 3.*pi/2.

```

```

iB(n) = vR(n)/R + V0*C*omega*sin(omega*t(n));
else
iB(n) = vR(n)/R - V0*C*omega*sin(omega*t(n));
end

%If the current tries to go negative, the diodes will switch OFF
if iB(n) < 0;
diode = 0;
toff = t(n);
Voff = vrect(n);
end
    else
%When the diodes are off, the resistor/capacitor voltage decays
%exponentially.
vR(n) = Voff*exp(-(t(n)-toff)/tau);
iB(n) = 0;
if (vrect(n) - vR(n)) > 0;
diode = 1;
end
    end
end

%Calculate the resistor current
iR = vR/R;

%Now plot vR as a solid line and vrect as a dashed line
plot(1000*t,vR)
xlabel('time [msec]')
ylabel('Voltage [V]')
axis ([0 22 0 180])
hold
plot(1000*t,vrect,'-')
hold

fprintf('\nHit any key to continue\n')
pause

%Now plot iR as a solid line and iL as a dashed line
plot(1000*t,iR)
xlabel('time [msec]')
ylabel('Source current [A]')
axis ([0 22 -50 250])
hold
plot(1000*t,iB,'--')
hold

```

Practice Problem 10.4



Use MATLAB to calculate the ripple voltage and rms diode current for the system of Example 10.5 for (a) $C = 5 \times 10^4 \mu\text{F}$ and (b) $C = 5 \times 10^3 \mu\text{F}$. (Hint: Note that the rms current must be calculated over an integral number of cycles of the current waveform).

Solution

- a. 2.64 V and 79.6 A rms
- b. 21.6 V and 42.8 A rms

In Example 10.5 we have seen that a capacitor can significantly decrease the ripple voltage across a resistive load. However, this comes at the cost of large bridge current pulses since the current must be delivered to the capacitor in the short time period during which the rectified source voltage is near its peak value.

Figure 10.18 shows the addition of an inductor L at the output of the bridge, in series with the filter capacitor and its load. If the impedance of the inductor is chosen to be large compared to that of the capacitor/load combination at the frequency of the rectified source voltage, very little of the ac component of the rectified source voltage will appear across the capacitor, and thus the resultant L - C filter will produce low voltage ripple while drawing a relatively constant current from the diode bridge.

We have seen how the addition of a filter capacitor across a dc load can significantly reduce the ripple voltage seen by the load. In fact, the addition of significant capacitance can “stiffen” the rectified voltage to the point that it appears as a constant-voltage dc source to a load. In an analogous fashion, an inductor in series with a load will reduce the current ripple out of a rectifier. Under these conditions, the rectified source will appear as a constant-current dc source to a load.

The combination of a rectifier and an inductor at the output to supply a constant dc current to a load is of significant importance in power-electronic applications. It can be used, for example, as the front end of a current-source inverter which can be used to supply ac current waveforms to a load. We will investigate the behavior of such rectifier systems in the next section.

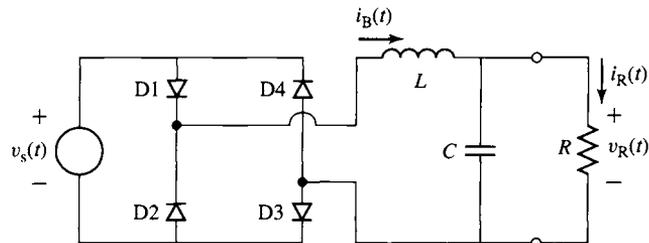


Figure 10.18 Full-wave bridge rectifier with an L - C filter supplying a resistive load.

10.2.2 Single-Phase Rectifier with Inductive Load

In this section we will examine the performance of a single-phase rectifier driving an inductive load. This situation covers both the case where the inductor is included as part of the rectifier system as a filter to smooth out current pulses as well as the case where the load itself is primarily inductive.

Let us examine first the half-wave rectifier circuit of Fig. 10.19. Here, the load consists of an inductor L in series with a resistor R . The source voltage is equal to $v_s(t) = V_0 \cos \omega t$.

Consider first the case where L is small ($\omega L \ll R$). In this case, the load looks essentially resistive and the load current $i_L(t)$ will vary only slightly from the current for a purely resistive load as seen in Example 10.1. This current, obtained from a detailed analytical solution, is plotted in Fig. 10.20a, along with the current for a purely resistive load.

Note that the effect of the inductance is to decrease both the initial rate of rise of the current and the peak current. More significantly, the diode *conduction angle* increases; current flows for longer than the half-period that is the case for a purely resistive load. As can be seen in Fig. 10.20a, this effect increases as the inductance is increased; current flows for a greater fraction of the cycle, and the peak current as well as the current ripple is reduced.

Figure 10.20b, which shows the inductor voltage, illustrates an important point that applies to all situations in which an inductor is subjected to steady-state, periodic conditions: *the time-averaged voltage across the inductor must equal zero*. This can be readily seen from the basic v - i relationship for an inductor

$$v = L \frac{di}{dt} \quad (10.3)$$

If we consider the operation of an inductor over a period of the excitation frequency and recognize that, under steady-state conditions, the change in the inductor current over that period must equal zero (i.e., it must have the same value at time t at the beginning of the period as it does one period later at time $t + T$), then we can write

$$i(t + T) - i(t) = 0 = \frac{1}{L} \int_t^{t+T} v dt \quad (10.4)$$

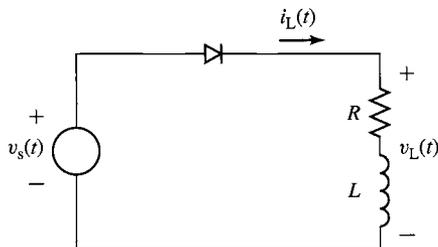


Figure 10.19 Half-wave rectifier with an inductive load.

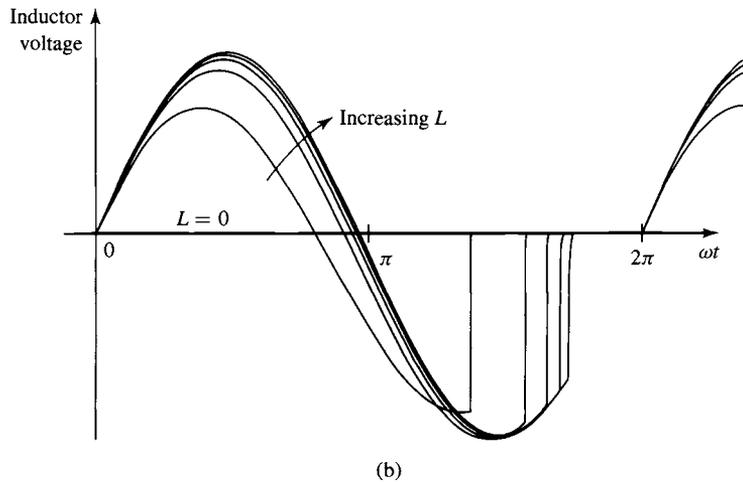
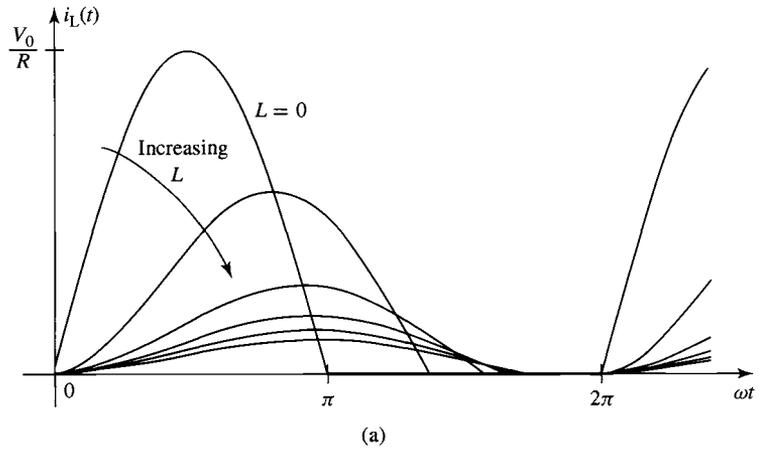


Figure 10.20 Effect of increasing the series inductance in the circuit of Fig. 10.19 on (a) the load current and (b) the inductor voltage.

from which we can see that the net volt-seconds (and correspondingly the average voltage) across the inductor during a cycle must equal zero

$$\int_t^{t+T} v \, dt = 0 \quad (10.5)$$

For this half-wave rectifier, note that as the inductance increases both the ripple current and the dc current will decrease. In fact, for large inductance ($\omega L \gg R$) the dc load current will tend towards zero. This can be easily seen by the following

argument:

As the inductance increases, the conduction angle of the diode will increase from 180° and approach 360° for large values of L .

In the limit of a 360° conduction angle, the diode can be replaced by a continuous short circuit, in which case the circuit reduces to the ac voltage source connected directly across the series combination of the resistor and the inductor.

Under this situation, no dc current will flow since the source is purely ac. In addition, since the impedance $Z = R + j\omega L$ becomes large with large L , the ac (ripple) current will also tend to zero.

Figure 10.21a shows a simple modification which can be made to the half-wave rectifier circuit of Fig. 10.19. The *free-wheeling diode* D2 serves as an alternate path for inductor current.

To understand the behavior of this circuit, consider the condition when the source voltage is positive, and the rectifier diode D1 is ON. The equivalent circuit for this operating condition is shown in Fig. 10.21b. Note that under this condition, the voltage across diode D2 is equal to the negative of the source voltage and diode D2 is turned OFF.

This operating condition will remain in effect as long as the source voltage is positive. However, as soon as the source voltage begins to go negative, the voltage across diode D2 will begin to go positive and it will turn ON. Since the load is

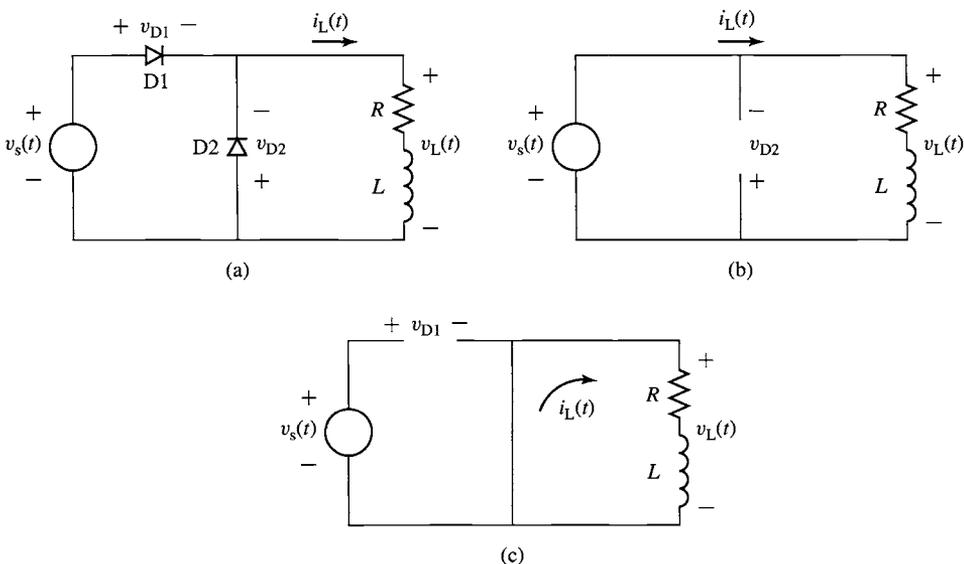


Figure 10.21 (a) Half-wave rectifier with an inductive load and a free-wheeling diode. (b) Equivalent circuit when $v_s(t) > 0$ and diode D1 is conducting. (c) Equivalent circuit when $v_s(t) < 0$ and the free-wheeling diode D2 is conducting.

inductive, a positive load current will be flowing at this time, and that load current will immediately transfer to the short circuit corresponding to diode D2. At the same time, the current through diode D1 will immediately drop to zero, diode D1 will be reverse biased by the source voltage, and it will turn OFF. This operating condition is shown in Fig. 10.21c. Thus, the diodes in this circuit alternately switch ON and OFF each half cycle: D1 is ON when $v_s(t)$ is positive, and D2 is ON when it is negative.

Based upon this discussion, we see that the voltage $v_L(t)$ across the load (equal to the negative of the voltage across diode D2) is a half-wave rectified version of $v_s(t)$ as seen in Fig. 10.22a. As shown in Example 10.1, the average of this voltage is $V_{dc} = V_0/\pi$. Furthermore, the average of the steady-state voltage across the inductor must equal zero, and hence the average of the voltage $v_L(t)$ will appear across the resistor. Thus the dc load current will equal $V_0/(\pi R)$. This value is independent of the inductor value and hence does not approach zero as the inductance is increased.

Figure 10.22b shows the diode and load currents for a relatively small value of inductance ($\omega L < R$), and Fig. 10.22c shows these same currents for a large inductance $\omega L \gg R$. In each case we see the load current, which must be continuous due to the presence of the inductor, instantaneously switching between the diodes depending on the polarity of the source voltage. We also see that during the time diode D1 is ON, the load current increases due to the application of the sinusoidal source voltage, while during the time diode D2 is ON, the load current simply decays with the L/R time constant of the load itself.

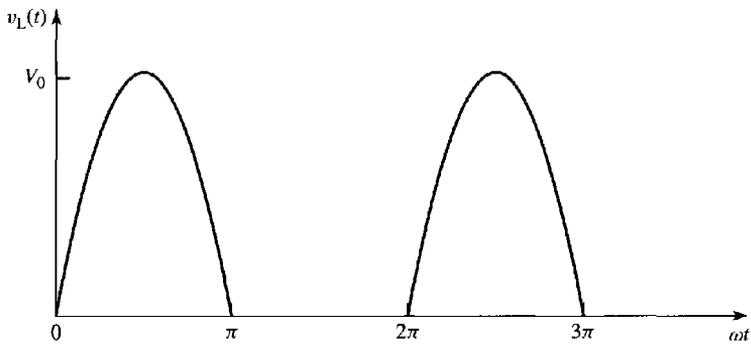
As expected, in each case the average current through the load is equal to $V_0/(\pi R)$. In fact, the presence of a large inductor can be seen to reduce the ripple current to the point that the load current is essentially a dc current equal to this value.

Let us now consider the case where the half-wave bridge of Fig. 10.19 is replaced by a full-wave bridge as in Fig. 10.23a. In this circuit, the voltage applied to the load is the full-wave-rectified source voltage as shown in Fig. 10.15 and the average (dc) voltage applied to the load will equal $2V_0/\pi$. Here again, the presence of the inductor will tend to reduce the ac ripple. Figure 10.24, again obtained from a detailed analytical solution, shows the current through the resistor as the inductance is increased.

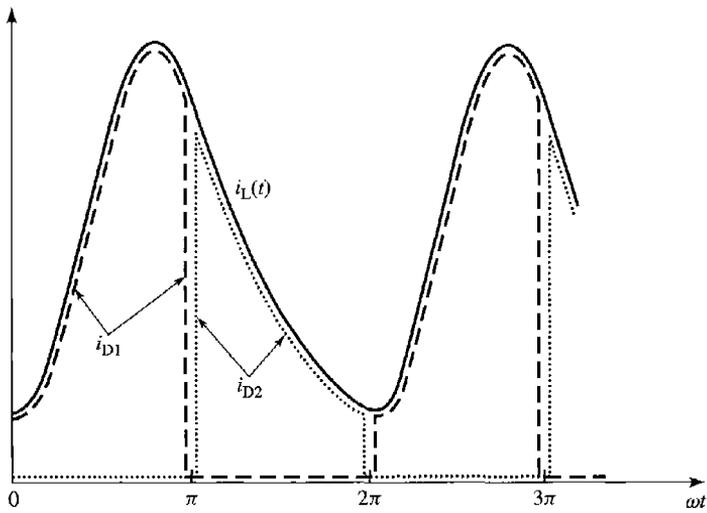
If we assume a large inductor ($\omega L \gg R$), the load current will be relatively ripple free and constant. It is therefore common practice to analyze the performance of this circuit by replacing the inductor by a dc current source I_{dc} as shown in Fig. 10.23b, where $I_{dc} = 2V_0/(\pi R)$. This is a commonly used technique in the analysis of power-electronic circuits which greatly simplifies their analysis.

Under this assumption, we can easily show that the diode and source currents of this circuit are given by waveforms of Fig. 10.25. Figure 10.25a shows the current through one pair of diodes (e.g., diodes D1 and D3), and Fig. 10.25b shows the source current $i_s(t)$. The essentially constant load current I_{dc} flows through each pair of diodes for one half cycle and appears as a square wave of amplitude I_{dc} at the source.

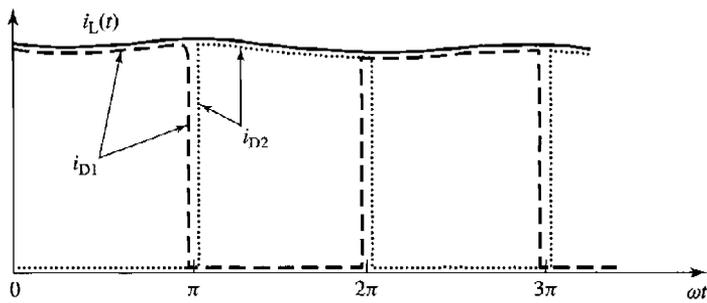
In a fashion similar to that which we saw in the half-wave rectifier circuit with the free-wheeling diode, here a pair of diodes (e.g., diodes D1 and D3) are carrying current when the source voltage reverses, turning ON the other pair of diodes and switching OFF the pair that were previously conducting. In this fashion, the load current remains continuous and simply switches between the diode pairs.



(a)



(b)



(c)

Figure 10.22 (a) Voltage applied to the load by the circuit of Fig. 10.21. (b) Load and diode currents for small L . (c) Load and diode currents for large L .

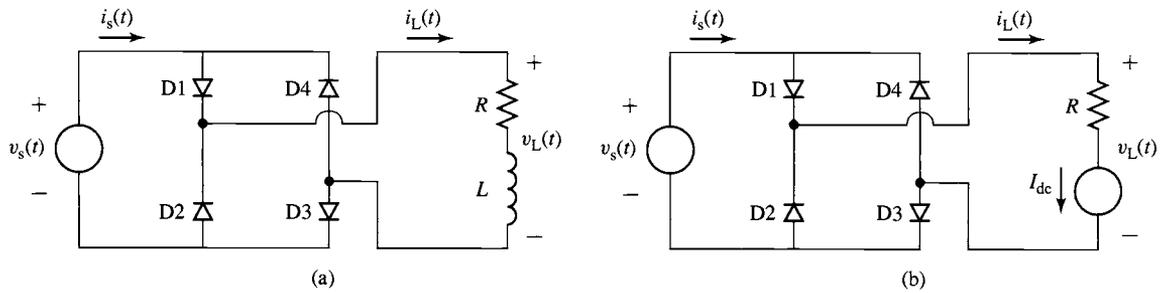


Figure 10.23 (a) Full-wave rectifier with an inductive load. (b) Full-wave rectifier with the inductor replaced by a dc current source.

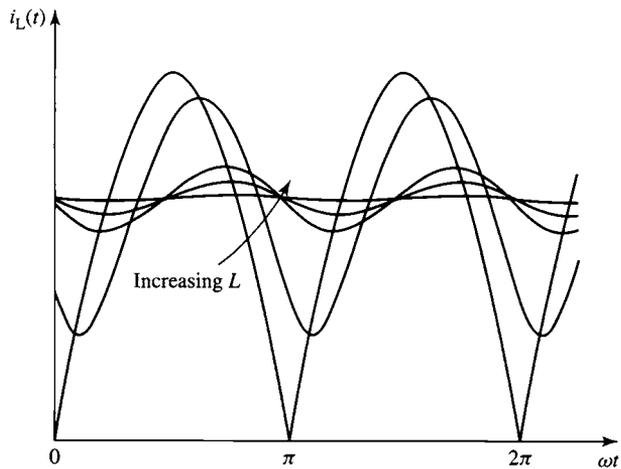


Figure 10.24 Effect of increasing the series inductance in the circuit of Fig. 10.23a on the load current.

10.2.3 Effects of Commutating Inductance

Our analysis and the current waveforms of Fig. 10.25 show that the current commutes instantaneously from one diode pair to the next. In practical circuits, due to the presence of source inductance, *commutation* of the current between the diode pairs does not occur instantaneously. The effect of source inductance, typically referred to as *commutating inductance*, will be examined by studying the circuit of Fig. 10.26 in which a source inductance L_s has been added in series with the voltage source in the full-wave rectifier circuit of Fig. 10.23b. We have again assumed that the load time constant is large ($\omega L/R \gg 1$) and have replaced the inductor with a dc current source I_{dc} .

Figure 10.27a shows the situation which occurs when diodes D2 and D4 are ON and carrying current I_{dc} and when $v_s < 0$. Commutation begins when v_s reaches zero and begins to go positive, turning ON diodes D1 and D3. Note that *because the current in the source inductance L_s cannot change instantaneously*, the circuit condition at

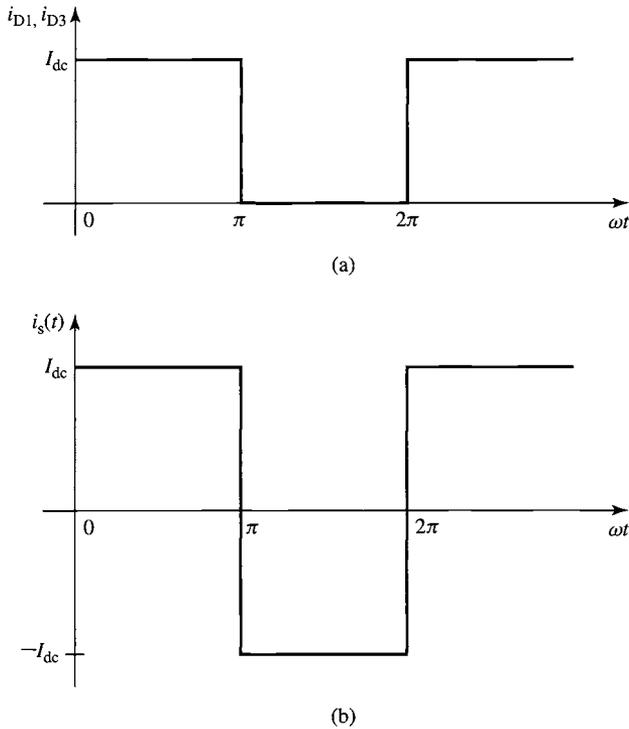


Figure 10.25 (a) Current through diodes D1 and D3 and (b) source current for the circuit of Fig. 10.23, both in the limit of large inductance.

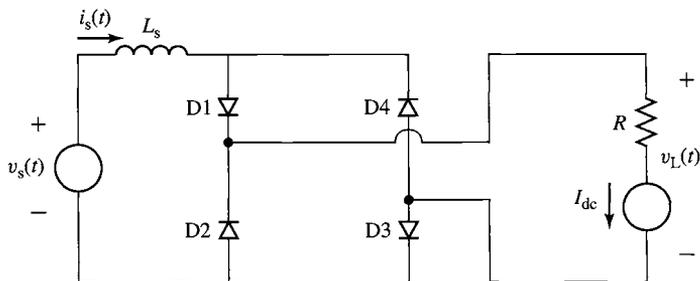


Figure 10.26 Full-wave bridge rectifier with source inductance. Dc load current assumed.

this time is described by Fig. 10.27b: the current through L_s is equal to $-I_{dc}$, the current through diodes D2 and D4 is equal to I_{dc} , while the current through diodes D1 and D3 is zero.

Under this condition with all four diodes ON, the source voltage $v_s(t)$ appears directly across the source inductance L_s . Noting that commutation starts at the time

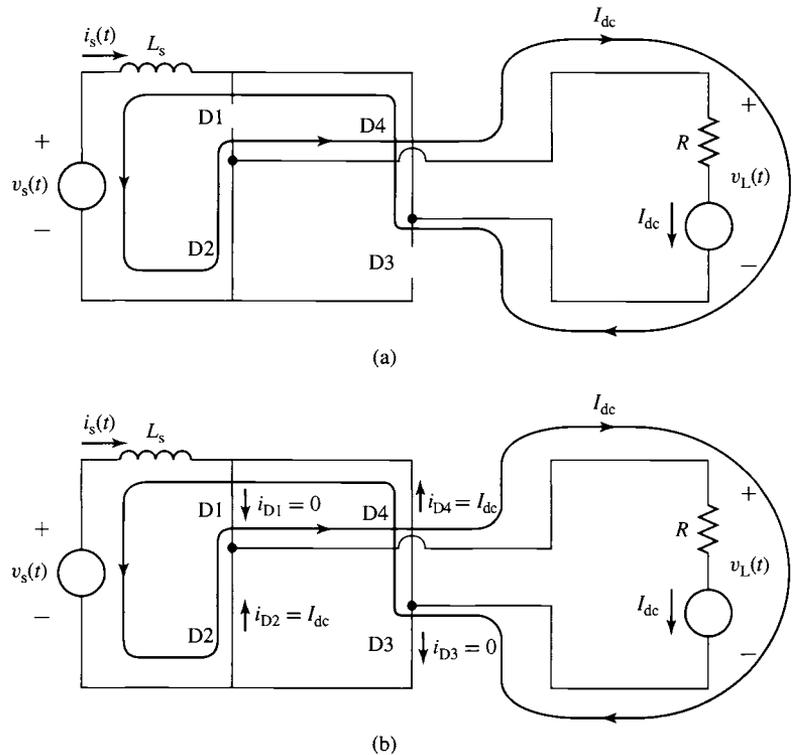


Figure 10.27 (a) Condition of the full-wave circuit of Fig. 10.26 immediately before diodes D1 and D3 turn ON. (b) Condition immediately after diodes D1 and D3 turn ON.

when $v_s(t) = 0$, the current through L_s can be written as

$$\begin{aligned} i_s(t) &= -I_{dc} + \frac{1}{L_s} \int_0^t v_s(t) dt \\ &= -I_{dc} + \left(\frac{V_0}{\omega L_s} \right) (1 - \cos \omega t) \end{aligned} \quad (10.6)$$

Noting that $i_s = i_{D1} - i_{D4}$, that $i_{D1} + i_{D2} = I_{dc}$ and, that by symmetry, $i_{D4} = i_{D2}$, we can write that

$$i_{D2} = \frac{I_{dc} - i_s(t)}{2} \quad (10.7)$$

Diode D2 (and similarly diode D4) will turn OFF when i_{D2} reaches zero, which will occur when $i_s(t) = I_{dc}$. In other words, commutation will be completed at time t_c , when the current through L_s has completely reversed polarity and when all of the load current is flowing through diodes D1 and D3.

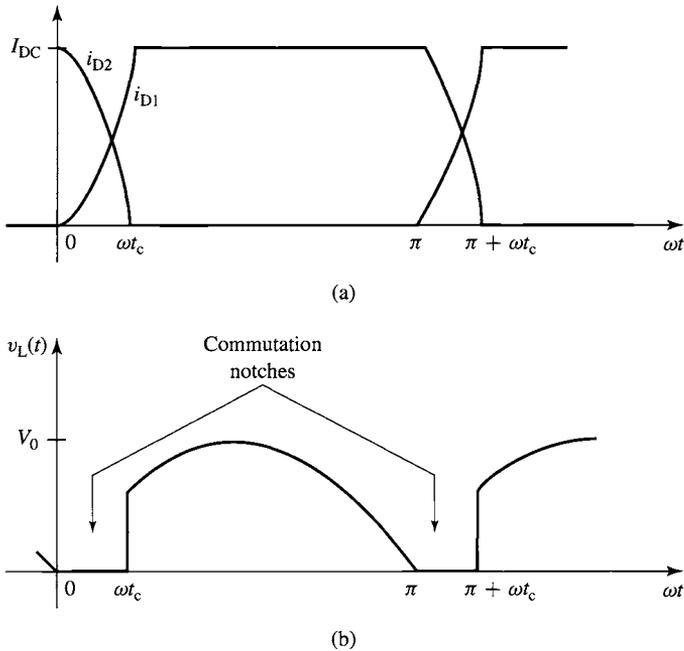


Figure 10.28 (a) Currents through diodes D1 and D2 showing the finite commutation interval. (b) Load voltage showing the commutation notches due to the finite commutation time.

Setting $i_s(t_c) = I_{dc}$ and solving Eq. 10.6 gives an expression for the *commutation interval* t_c as a function of I_{dc}

$$t_c = \frac{1}{\omega} \cos^{-1} \left[1 - \left(\frac{2I_{dc}\omega L_s}{V_0} \right) \right] \quad (10.8)$$

Figure 10.28a shows the currents through diodes D1 and D2 as the current commutates between them. The finite commutation time t_c can clearly be seen. There is a second effect of commutation which can be clearly seen in Fig. 10.28b which shows the rectified load voltage $v_L(t)$. Note that during the time of commutation, with all the diodes on, the rectified load voltage is zero. These intervals of zero voltage on the rectified voltage waveform are known as *commutation notches*.

Comparing the ideal full-wave rectified voltage of Fig. 10.15b to the waveform of Fig. 10.28b, we see that the effect of the commutation notches is to reduce the dc output of the rectifier. Specifically, the dc voltage in this case is given by

$$\begin{aligned} V_{dc} &= \left(\frac{\omega}{\pi} \right) \int_{t_c}^{\pi} V_0 \sin \omega t \, dt \\ &= \frac{V_0}{\pi} (1 + \cos \omega t_c) \end{aligned} \quad (10.9)$$

where t_c is the commutation interval as calculated by Eq. 10.8.

Finally, the dc load current can be calculated as function of t_c

$$I_{dc} = \frac{V_{dc}}{R} = \frac{V_0}{\pi R} (1 + \cos \omega t_c) \quad (10.10)$$

Substituting Eq. 10.8 into Eq. 10.10 gives a closed-form solution for I_{dc}

$$I_{dc} = \frac{2V_0}{\pi R + 2\omega L_s} \quad (10.11)$$

and hence

$$V_{dc} = I_{dc} R = \frac{2V_0}{\pi + \frac{2\omega L_s}{R}} \quad (10.12)$$

We have seen that commutating inductance (which is to a great extent unavoidable in practical circuits) gives rise to a finite commutation time and produces commutation notches in the rectified-voltage waveform which reduces the dc voltage applied to the load.

EXAMPLE 10.6



Consider a full-wave rectifier driving an inductive load as shown in Fig. 10.29. For a 60-Hz, 230-V rms source voltage, $R = 5.6 \Omega$ and large L ($\omega L \gg R$), plot the dc current through the load I_{dc} and the commutation time t_c as the source inductance L_s varies from 1 to 100 mH.

■ Solution

The solution can be obtained by substitution into Eqs. 10.8 and 10.11. This is easily done using MATLAB, and the plots of I_{dc} and t_c are shown in Figs. 10.30a and b respectively. Note that the maximum achievable dc current, corresponding to $L_s = 0$, is equal to $2V_0/(\pi R) = 37$ A. Thus, commutating inductances on the order of 1 mH can be seen to have little effect on the performance of the rectifier and can be ignored. On the other hand, a commutating inductance of 100 mH can be seen to reduce the dc current to approximately 7 A, significantly reducing the capability of the rectifier circuit.

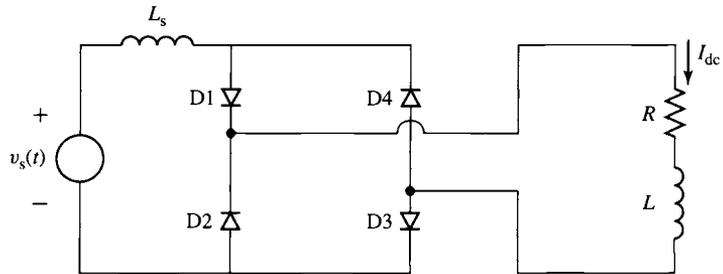
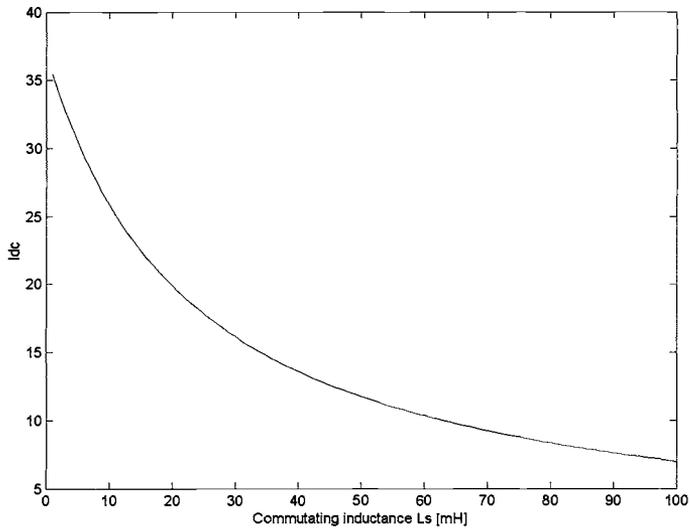
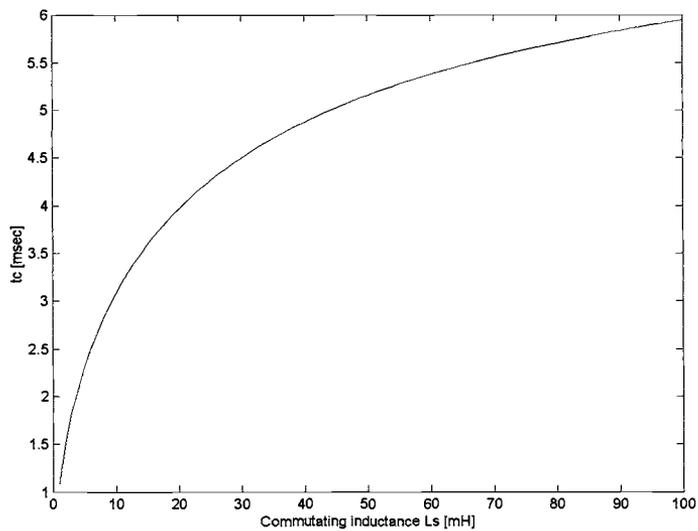


Figure 10.29 Full-wave bridge rectifier with source inductance for Example 10.6.



(a)



(b)

Figure 10.30 (a) Dc current I_{dc} and (b) commutation time t_c for Example 10.6.

Here is the MATLAB script for Example 10.6.

```

clc
clear

%parameters
omega = 2*pi*60;
R = 5.6;
V0 = 230*sqrt(2);

for n = 1:100
    Ls(n) = n*1e-3;
    Idc(n) = 2*V0/(pi*R + 2*omega*Ls(n));
    tc(n) = (1/omega)*acos(1-(2*Idc(n)*omega*Ls(n))/V0);
end

plot(Ls*1000,Idc)
xlabel('Commutating inductance Ls [mH]')
ylabel('Idc')

fprintf('\nHit any key to continue\n')
pause

plot(Ls*1000,tc*1000)
xlabel('Commutating inductance Ls [mH]')
ylabel('tc [msec]')

```

Practice Problem 10.5

Calculate the commutating inductance and the corresponding commutation time for the circuit of Example 10.6 if the dc load current is observed to be 29.7 A.

Solution

$$L_s = 5.7 \text{ mH and } t_c = 2.4 \text{ msec}$$

10.2.4 Single-Phase, Full-Wave, Phase-Controlled Bridge

Figure 10.31 shows a full-wave bridge in which the diodes of Fig. 10.15 have been replaced by SCRs. We will assume that the load inductance L is sufficiently large that the load current is essentially constant at a dc value I_{dc} . We will also ignore any effects of commutating inductance, although they clearly would play the same role in a phase-controlled rectifier system as they do in a diode-rectifier system.

Figure 10.32 shows the source voltage and the timing of the SCR gate pulses under a typical operating condition for this circuit. Here we see that the firing pulses are delayed by an angle α_d after the zero-crossing of the source-voltage waveform, with the firing pulses for SCRs T1 and T3 occurring after the positive-going transition of $v_s(t)$ and those for SCRs T2 and T4 occurring after the negative-going transition.

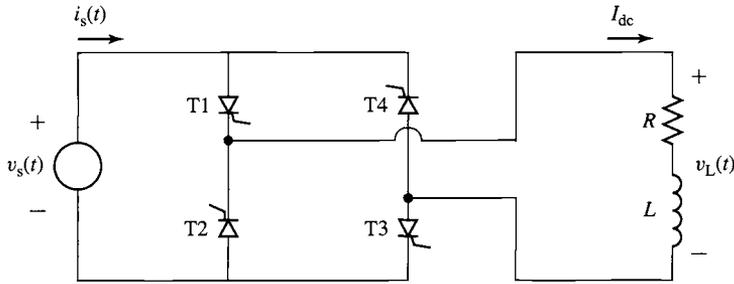


Figure 10.31 Full-wave, phase-controlled SCR bridge.

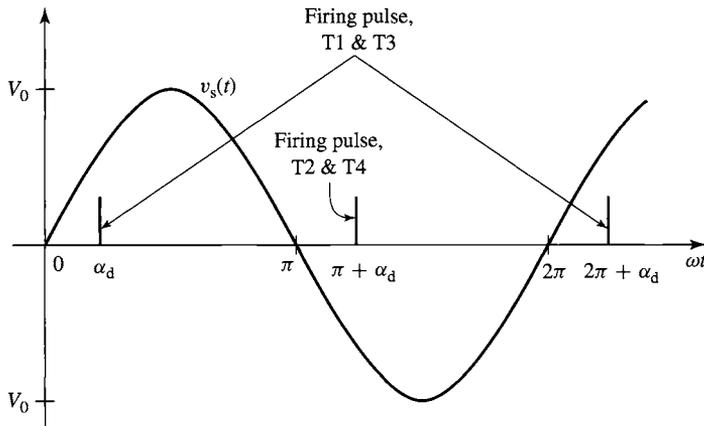


Figure 10.32 Source voltage and firing pulses for the phase-controlled SCR bridge of Fig. 10.31.

Figure 10.33a shows the current through SCRs T1 and T3. Note that these SCRs do not turn ON until they receive firing pulses at angle α_d after they are forward biased following positive-going zero crossing of the source voltage. Furthermore, note that SCRs T2 and T4 do not turn ON following the next zero crossing of the source voltage. Hence, SCRs T1 and T3 remain ON, carrying current until SCRs T2 and T4 are turned ON by gate pulses. Rather, T2 and T4 turn ON only after they receive their respective gate pulses (for example, at angle $\pi + \alpha_d$ in Fig. 10.33). This is an example of *forced commutation*, in that one pair of SCRs does not naturally commutate OFF but rather must be forcibly commutated when the other pair is turned ON.

Figure 10.33b shows the resultant load voltage $v_L(t)$. We see that the load voltage now has a negative component, which will increase as the firing-delay angle α_d is increased. The dc value of this waveform is equal to

$$V_{dc} = \left(\frac{2V_0}{\pi} \right) \cos \alpha_d \quad (0 \leq \alpha_d \leq \pi) \quad (10.13)$$

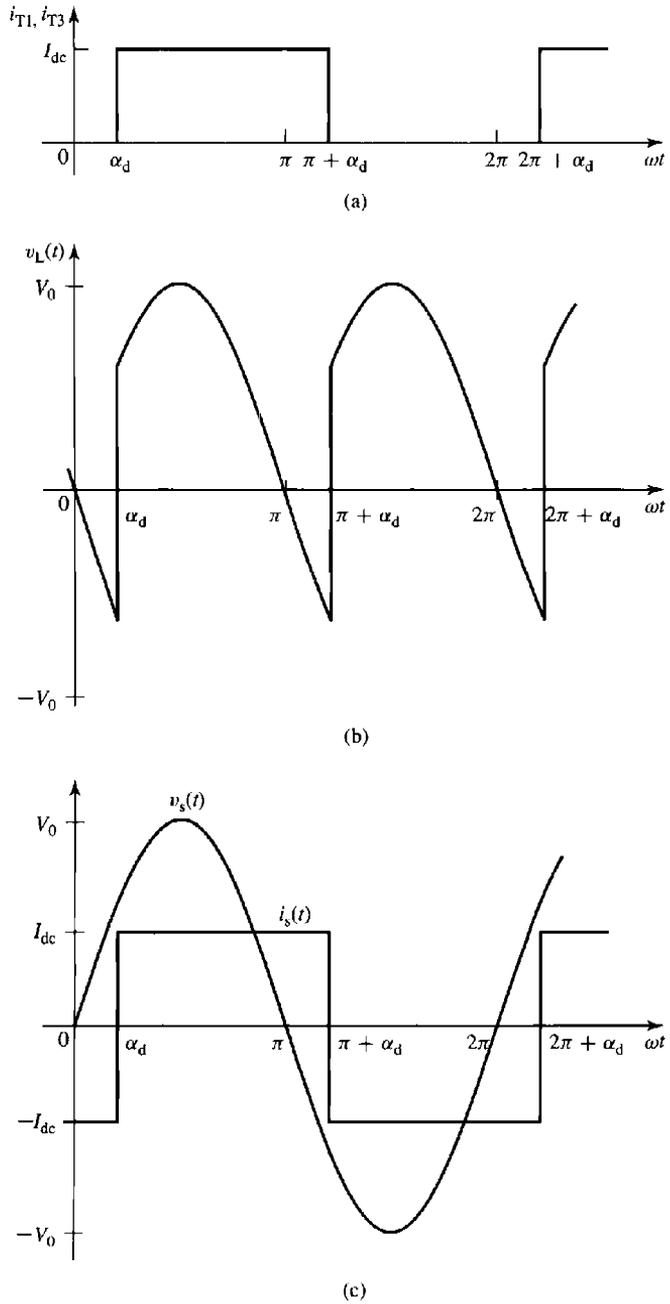


Figure 10.33 Waveforms for the phase-controlled SCR bridge of Fig. 10.31. (a) Current through SCRs T1 and T3. (b) Load voltage. (c) Source voltage and current.

from which the firing-delay angle corresponding to a given value of dc voltage can be seen to be

$$\alpha_d = \cos^{-1} \left(\frac{\pi V_{dc}}{2V_0} \right) \quad (10.14)$$

From Eq. 10.13 we see that the dc voltage applied to the load can vary from $2V_0/\pi$ to $-2V_0/\pi$. This is a rather surprising result in that it is hard to understand how a rectifier bridge can supply negative voltage. However, in this case, it is necessary to recognize that this result applies to an inductive load which maintains positive current flow through the SCRs in spite of the reversal of polarity of the source voltage. If the load were purely resistive, the current through the conducting SCRs would go to zero as the source-voltage reversed polarity, and they would simply turn OFF; no load current would flow until the next pair of SCRs is turned ON.

Figure 10.33c shows the source voltage and current waveforms for the phase-controlled SCR-bridge. We see that the square-wave source current is out of phase with the source voltage. Its fundamental-harmonic is given by

$$i_{s,1}(t) = \left(\frac{4}{\pi} \right) I_{dc} \sin(\omega t - \alpha_d) \quad (10.15)$$

and thus the real power supplied to the load is given by

$$P = V_{dc} I_{dc} = \frac{2}{\pi} V_0 I_{dc} \cos \alpha_d \quad (10.16)$$

and the reactive power supplied is

$$Q = -\frac{2}{\pi} V_0 I_{dc} \sin \alpha_d \quad (10.17)$$

Under steady-state operation at a load current I_{dc} , $V_{dc} = I_{dc}R$ and the steady-state firing-delay angle can be found from Eq. 10.14 to be $\alpha_{ss} = \cos^{-1} \left(\frac{\pi I_{dc} R}{2V_0} \right)$. Under this condition, the real power simply supplies the losses in the resistor and hence $P = I_{dc}^2 R$. It may seem strange to be supplying reactive power to a “dc” load. However, careful analysis will show that this reactive power supplies the energy associated with the small but finite ripple current through the inductor.

If the delay angle is suddenly reduced ($\alpha_d < \alpha_{ss}$), the dc voltage applied to the load will increase (see Eq. 10.13) as will the power supplied to the load (see Eq. 10.16). As a result, I_{dc} will begin to increase and the increased power will increase the energy storage in the inductor. Similarly, if the delay time is suddenly increased ($t_d > t_{d0}$), V_{dc} will decrease (it may even go negative) and the power into the load will decrease, corresponding to a decrease in I_{dc} and a decrease in the energy storage in the inductor.

Note that if $\alpha_d > \pi/2$, V_{dc} will be negative, a condition which will continue until I_{dc} reaches zero at which time the SCR bridge will turn OFF. Under this condition, the real power P will also be negative. Under this condition, power is being supplied from the load to the source and the system is said to be *regenerating*.

EXAMPLE 10.7



A small superconducting magnet has an inductance $L = 1.2$ H. Although the resistance of the magnet itself is essentially zero, the resistance of the external leads is 12.5 m Ω . Current is supplied to the magnet from a 60-Hz, 15-V peak, single-phase source through a phase-controlled SCR bridge as in Fig. 10.31.

- The magnet is initially operating in the steady state at a dc current of 35 A. Calculate the dc voltage applied to the magnet, the power supplied to the magnet, and the delay angle α_d in msec. Plot the magnet voltage $v_L(t)$.
- In order to quickly discharge the magnet, the delay angle is suddenly increased to $\alpha_d = 0.9\pi = 162^\circ$. Plot the corresponding magnet voltage. Calculate the time required to discharge the magnet and the maximum power regenerated to the source.

■ Solution

The example is most easily solved using MATLAB, which can easily produce the required plots.

- Under this steady-state condition, $V_{dc} = I_{dc}R = 35 \times 0.0125 = 0.438$ V. The power supplied to the magnet is equal to $P = V_{dc}I_{dc} = 0.438 \times 35 = 15.3$ W, all of which is going into supplying losses in the lead resistance. The delay angle can be found from Eq. 10.14.

$$\begin{aligned}\alpha_d &= \cos^{-1} \left(\frac{\pi R I_{dc}}{2V_0} \right) = \cos^{-1} \left(\frac{\pi \times 0.0125 \times 35}{2 \times 15} \right) \\ &= 1.52 \text{ rad} = 87.4^\circ\end{aligned}$$

A plot of $v_L(t)$ for this condition is given in Fig. 10.34a.

- For a delay angle of 0.9π , the dc load voltage will be

$$V_{dc} = \left(\frac{2V_0}{\pi} \right) \cos \alpha_d = \left(\frac{2 \times 15}{\pi} \right) \cos(0.9\pi) = -9.1 \text{ V}$$

A plot of $v_L(t)$ for this condition is given in Fig. 10.34b.

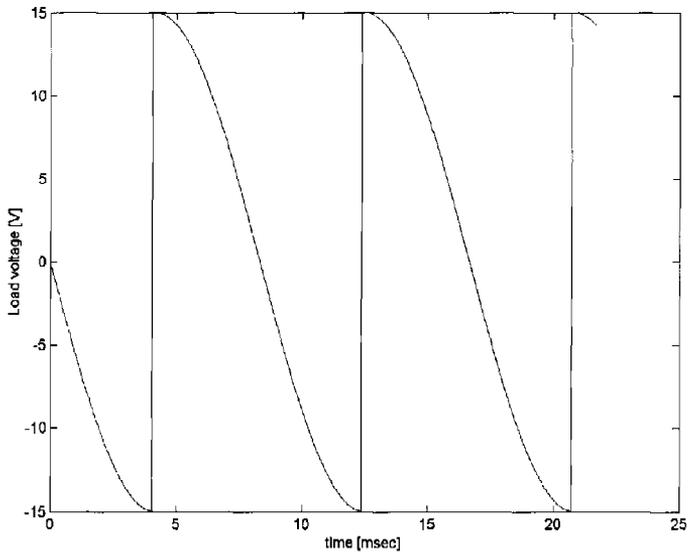
The magnet current i_m can be calculated from the differential equation

$$V_{dc} = i_m R + L \frac{di_m}{dt}$$

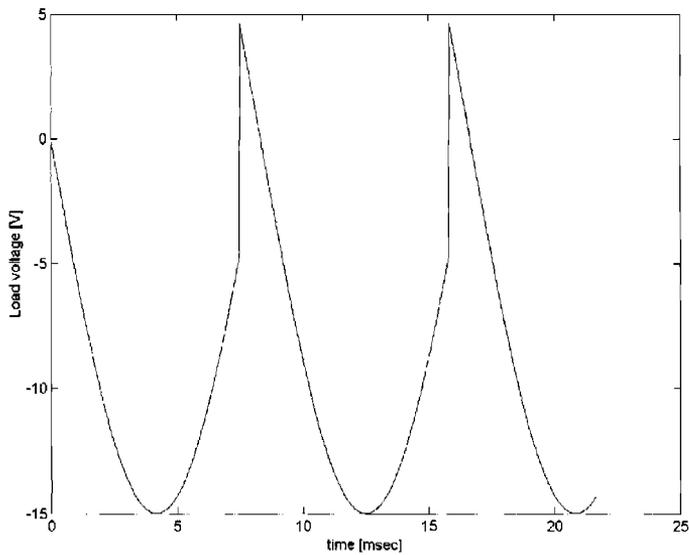
subject to the initial condition that $i_m(0) = 35$ A. Thus

$$i_m(t) = \frac{V_{dc}}{R} + \left(i_m(0) - \frac{V_{dc}}{R} \right) e^{-(\frac{R}{L})t}$$

From this equation we find that the magnet current will reach zero at time $t = 4.5$ seconds, at which time the bridge will shut off. The power regenerated to the source will be given by $-V_{dc}i_m(t)$. It has a maximum value of $9.1 \times 35 = 318$ W at time $t = 0$.



(a)



(b)

Figure 10.34 Waveforms for Example 10.7. (a) Magnet-voltage for $\alpha_d = 87.4^\circ$, $V_{dc} = 0.438$ V. (b) Magnet-voltage for $\alpha_d = 162^\circ$, $V_{dc} = -9.1$ V.

Here is the MATLAB script for Example 10.7.

```

clc
clear

% system parameters
R = 12.5e-3;
L = 1.2;
V0 = 15;
omega = 120*pi;

% part (a)

% dc current
Idc = 35;

% dc voltage
Vdc_a = R*Idc;

% Power
P = Vdc_a*Idc;

%Calculate the delay angle
alpha_da = acos(pi*R*Idc/(2*V0));

%Now calculate the load voltage
for n = 1:1300
    theta(n) = 2*pi*(n-1)/1000;
    t(n) = theta(n)/omega;
    vs(n) = V0*sin(theta(n));

    if theta(n) < alpha_da
vL(n) = -vs(n);
    elseif (theta(n) < pi + alpha_da)
vL(n) = vs(n);
    elseif theta(n) < 2*pi + alpha_da
vL(n) = -vs(n);
    elseif theta(n) < 3*pi + alpha_da
vL(n) = vs(n);
    elseif theta(n) < 4*pi + alpha_da
vL(n) = -vs(n);
    else
vL(n) = vs(n);
    end

end

plot(1000*t,vL)
xlabel('time [msec]')
ylabel('Load voltage [V]')

pause

% part (b)

```

```

% delay angle
alpha_db = 0.9*pi;

% Find the new dc voltage
Vdc_b = (2*V0/pi)*cos(alpha_db);

% Time constants
tau = L/R;

% Initial current
im0 = Idc;

% Calculate the time at which the current reaches zero
tzero = -tau*log((-Vdc_b/R)/(im0-Vdc_b/R));

% Now plot the load voltage
for n = 1:1300
    theta(n) = 2*pi*(n-1)/1000;
    t(n) = theta(n)/omega;
    vs(n) = V0*sin(theta(n));

    if theta(n) < alpha_db
        vL(n) = -vs(n);
    elseif (theta(n) < pi + alpha_db)
        vL(n) = vs(n);
    elseif theta(n) < 2*pi + alpha_db
        vL(n) = -vs(n);
    elseif theta(n) < 3*pi + alpha_db
        vL(n) = vs(n);
    elseif theta(n) < 4*pi + alpha_db
        vL(n) = -vs(n);
    else
        vL(n) = vs(n);
    end
end

plot(1000*t,vL)
xlabel('time [msec]')
ylabel('Load voltage [V]')

fprintf('part (a):')
fprintf('\n Vdc_a = %g [mV]',1000*Vdc_a)
fprintf('\n Power = %g [W]',P);
fprintf('\n alpha_d = %g [rad] = %g [degrees]',alpha_da,180*alpha_da/pi)
fprintf('\npart (b):')
fprintf('\n alpha_d = %g [rad] = %g [degrees]',alpha_db,180*alpha_db/pi)
fprintf('\n Vdc_b = %g [V]',Vdc_b)
fprintf('\n Current will reach zero at %g [sec]',tzero)
fprintf('\n')

```

Practice Problem 10.6

The field winding of a small synchronous generator has a resistance of 0.3Ω and an inductance of 250 mH . It is fed from a 24-V peak, single-phase 60-Hz source through a full-wave phase-controlled SCR bridge. (a) Calculate the dc voltage required to achieve a dc current of 18 A through the field winding and the corresponding firing-delay angle. (b) Calculate the field current corresponding to a delay angle of 45° .

Solution

- a. 5.4 V , 69°
- b. 36.0 A

10.2.5 Inductive Load with a Series DC Source

As we have seen in Chapter 9, dc motors can be modeled as dc voltage sources in series with an inductor and a resistor. Thus, it would be useful to briefly investigate the case of a dc voltage source in series with an inductive load.

Let us examine the full-wave, phase-controlled SCR rectifier system of Fig. 10.35. Here we have added a dc source E_{dc} in series with the load. Again assuming that $\omega L \gg R$ so that the load current is essentially dc, we see that the load voltage $v_L(t)$ depends solely on the timing of the SCR gate pulses and hence is unchanged by the presence of the dc voltage source E_{dc} . Thus the dc value of $v_L(t)$ is given by Eq. 10.13 as before.

In the steady-state, the dc current through the load can be found from the net dc voltage across the resistor as

$$I_{dc} = \frac{V_{dc} - E_{dc}}{R} \quad (V_{dc} \geq E_{dc}) \quad (10.18)$$

where V_{dc} is found from Eq. 10.13. Under transient conditions, it is the difference voltage, $V_{dc} - E_{dc}$, that drives a change in the dc current through the series R - L combination, in a fashion similar to that illustrated in Example 10.7.

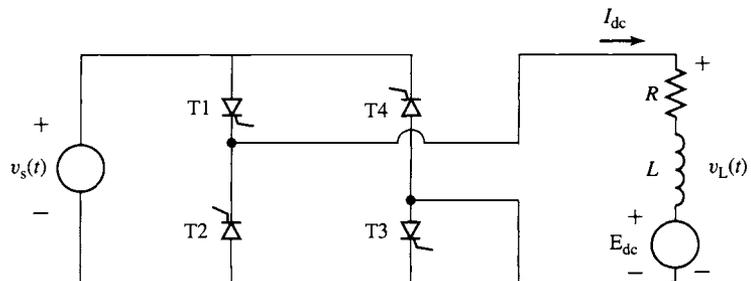


Figure 10.35 Full-wave, phase-controlled SCR bridge with an inductive load including a dc voltage source.

EXAMPLE 10.8

A small permanent-magnet dc motor is to be operated from a phase-controlled bridge. The 60-Hz ac waveform has an rms voltage of 35 volts. The dc motor has an armature resistance of 3.5Ω and an armature inductance of 17.5 mH. It achieves a no-load speed of 8000 r/min at an armature voltage of 50 V.

Calculate the no-load speed in r/min of the motor as a function of the firing delay angle α_d .

■ Solution

In Section 7.7 we see that the equivalent circuit for a permanent-magnet dc motor consists of a dc source (proportional to motor speed) in series with an inductance and a resistance. Thus, the equivalent circuit of Fig. 10.35 applies directly to the situation of this problem.

From Eq. 7.26, the generated voltage from the dc motor (E_{dc} in Fig. 10.35) is proportional to the speed of the dc motor. Thus,

$$n = \left(\frac{8000}{50} \right) E_{dc} = 160E_{dc} \text{ r/min}$$

Under steady state operation, the dc voltage drop across the armature inductance will be zero. In addition, at no load, the armature current will be sufficiently small that the voltage drop across the armature resistance can be neglected. Thus, setting $E_{dc} = V_{dc}$ and substituting the expression for V_{dc} from Eq. 10.13 give,

$$\begin{aligned} E_{dc} = V_{dc} &= \left(\frac{2V_0}{\pi} \right) \cos \alpha_d \\ &= \left(\frac{2\sqrt{2}35}{\pi} \right) \cos \alpha_d = 31.5 \cos \alpha_d \end{aligned}$$

Note that because the bridge can only supply positive current to the dc motor (and hence, in the steady-state, the dc voltage must be positive), this expression is valid only for $0 \leq \alpha_d \leq \pi/2$.

Finally, substituting the expression for the speed n in terms of E_{dc} gives

$$n = 160 \times (31.5 \cos \alpha_d) = 5040 \cos \alpha_d \text{ r/min} \quad (0 \leq \alpha_d \leq \pi/2)$$

Practice Problem 10.7

The dc-motor of Example 10.8 is observed to be operating at a speed of 3530 r/min and drawing a dc current of 1.75 ampere. Calculate the corresponding firing delay angle α_d .

Solution

$$\alpha_d = 0.15\pi \text{ rad} = 27^\circ$$

10.2.6 Three-Phase Bridges

Although many systems with ratings ranging up to five or more kilowatts run off single-phase power, most large systems are supplied from three-phase sources. In general, all of the issues which we have discussed with regard to single-phase,

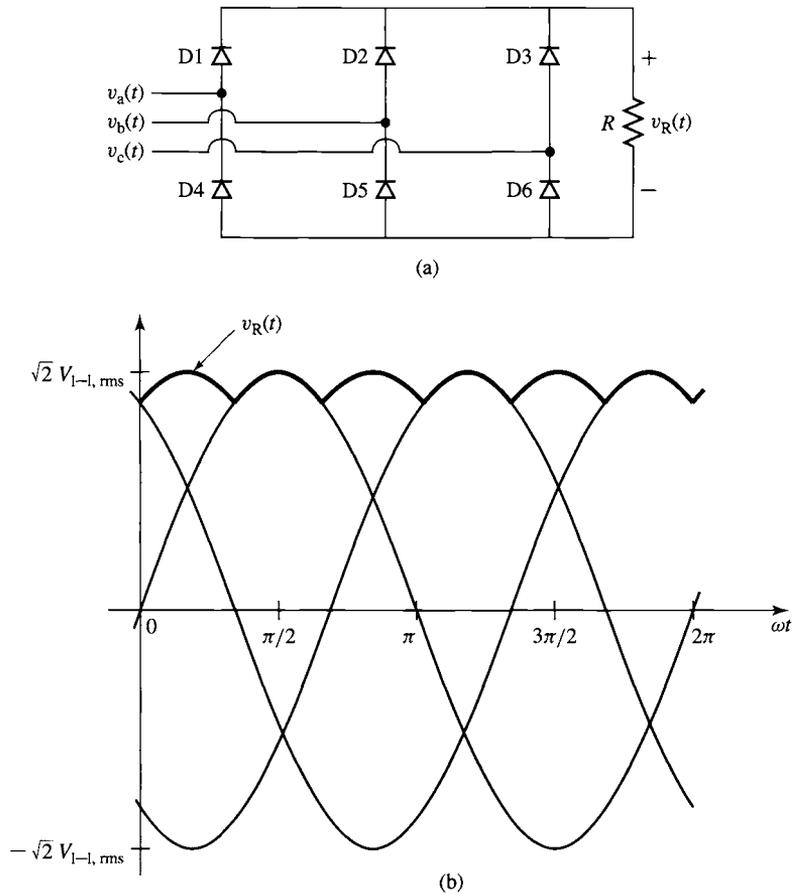


Figure 10.36 (a) Three-phase, six-pulse diode bridge with resistive load. (b) Line-to-line voltages and resistor voltage.

full-wave bridges apply directly to situations with three-phase bridges. As a result, we will discuss three-phase bridges only briefly.

Figure 10.36a shows a system in which a resistor R is supplied from a three-phase source through a *three-phase, six-pulse diode bridge*. Figure 10.36b shows the three-phase line-to-line voltages (peak value $\sqrt{2}V_{l-l, rms}$ where $V_{l-l, rms}$ is the rms value of the line-to-line voltage) and the resistor voltage $v_R(t)$, found using the method-of-assumed-states and assuming that the diodes are ideal.

Note that v_R has six pulses per cycle. Unlike the single-phase, full-wave bridge of Fig. 10.15a, the resistor voltage does not go to zero. Rather, the three-phase diode bridge produces an output voltage equal to the instantaneous maximum of the absolute value of the three line-to-line voltages. The dc average of this voltage (which can

be obtained by integrating over 1/6 of a cycle) is given by

$$\begin{aligned} V_{dc} &= \frac{3\omega}{\pi} \int_0^{\frac{\pi}{3\omega}} -v_{bc}(t) dt \\ &= -\frac{3\omega}{\pi} \int_0^{\frac{\pi}{3\omega}} \sqrt{2}V_{1-1,\text{rms}} \sin\left(\omega t - \frac{2\pi}{3}\right) dt \\ &= \left(\frac{3\sqrt{2}}{\pi}\right) V_{1-1,\text{rms}} \end{aligned} \quad (10.19)$$

where $V_{1-1,\text{rms}}$ is the rms value of the line-to-line voltage.

Table 10.1 shows the diode-switching sequence for the three-phase bridge of Fig. 10.36a corresponding to a single period of the three-phase voltage of waveforms of Fig. 10.36b. Note that only two diodes are on at any given time and that each diode is on for 1/3 of a cycle (120°).

Analogous to the single-phase, full-wave, phase-controlled SCR bridge of Figs. 10.31 and 10.35, Fig. 10.37 shows a three-phase, phase-controlled SCR bridge. Assuming continuous load current, corresponding for example to the condition $\omega L \gg R$, in which case the load current will be essentially a constant dc current I_{dc} , this bridge is capable of applying a negative voltage to the load and of regenerating power in a fashion directly analogous to the single-phase, full-wave, phase-controlled SCR bridge which we discussed in Section 10.2.4.

Table 10.1 Diode conduction times for the three-phase diode bridge of Fig. 10.36a.

α_a	$0-\pi/3$	$\pi/3-2\pi/3$	$2\pi/3-\pi$	$\pi-4\pi/3$	$4\pi/3-5\pi/3$	$5\pi/3-2\pi$
D1	OFF	ON	ON	OFF	OFF	OFF
D2	OFF	OFF	OFF	ON	ON	OFF
D3	ON	OFF	OFF	OFF	OFF	ON
D4	OFF	OFF	OFF	OFF	ON	ON
D5	ON	ON	OFF	OFF	OFF	OFF
D6	OFF	OFF	ON	ON	OFF	OFF

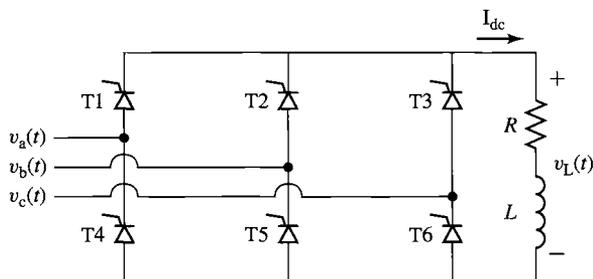


Figure 10.37 Three-phase, phase-controlled SCR bridge circuit with an inductive load.

It is a relatively straightforward matter to show that maximum output voltage of this bridge configuration will occur when the SCRs are turned ON at the times when the diodes in a diode bridge would naturally turn ON. These times can be found from Table 10.1. For example, we see that SCR T5 must be turned ON at angle $\alpha_d = 0$ (i.e., at the positive zero crossing of $v_{ab}(t)$). Similarly, SCR T1 must be turned ON at time $\alpha_d = \pi/3$, and so on.

Thus, one possible scheme for generating the SCR gate pulses is to use the positive-going zero crossing of $v_{ab}(t)$ as a reference from which to synchronize a pulse train running at six times the fundamental frequency (i.e., there will be six uniformly-spaced pulses in each cycle of the applied voltage). SCR T5 would be fired first, followed by SCRs T1, T6, T2, T4, and T3 in that order, each separated by 60° in phase delay.

If the firing pulses are timed to begin immediately following the zero crossing of $v_{ab}(t)$ the load voltage waveform $v_L(t)$ will be that of Fig. 10.36b. If the firing pulses are delayed by an angle α_d , then the load-voltage waveforms will appear as in Fig. 10.38a (for $\alpha_d = 0.1\pi$) and Fig. 10.38b (for $\alpha_d = 0.9\pi$).

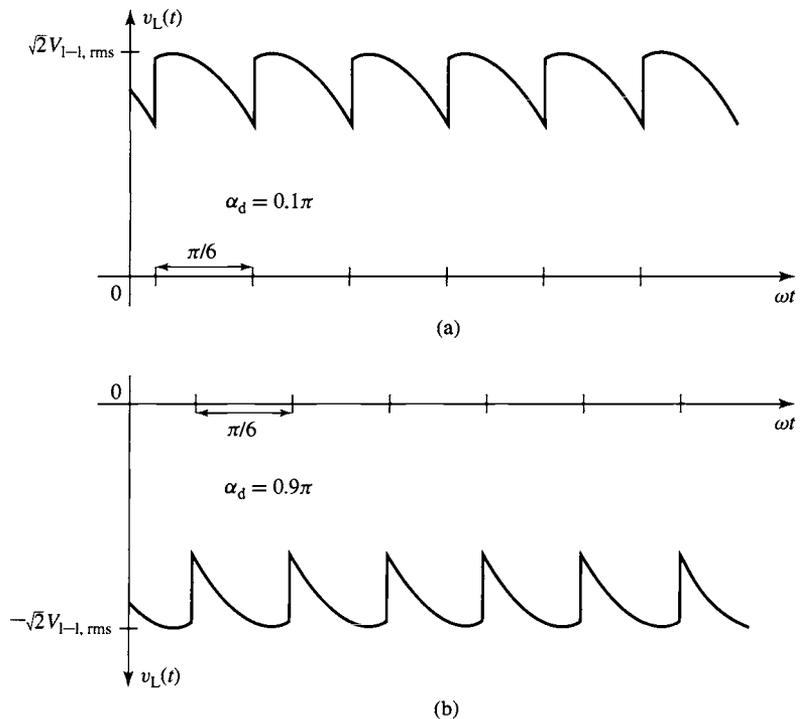


Figure 10.38 Typical load voltages for delayed firing of the SCRs in the three-phase, phase-controlled rectifier of Fig. 10.37; (a) $\alpha_d = 0.1\pi$, (b) $\alpha_d = 0.9\pi$.

The dc average of the output voltage of the phase-controlled bridge can be found as

$$\begin{aligned}
 V_{dc} &= \frac{3\omega}{\pi} \int_{\frac{\alpha_d}{\omega}}^{\frac{\alpha_d + \pi/3}{\omega}} -v_{bc}(t) dt \\
 &= -\frac{3\omega}{\pi} \int_{\frac{\alpha_d}{\omega}}^{\frac{\alpha_d + \pi/3}{\omega}} \sqrt{2} V_{1-1,\text{rms}} \sin\left(\omega t - \frac{2\pi}{3}\right) dt \\
 &= \left(\frac{3\sqrt{2}}{\pi}\right) V_{1-1,\text{rms}} \cos \alpha_d \quad (0 \leq \alpha_d \leq \pi) \quad (10.20)
 \end{aligned}$$

where $V_{1-1,\text{rms}}$ is the rms value of the line-to-line voltage.

EXAMPLE 10.9

A large magnet with an inductance of 14.7 H and resistance of 68 Ω is to be supplied from a 60-Hz, 460-V, three-phase source through a phase-controlled SCR bridge as in Fig. 10.37. Calculate (a) the maximum dc voltage $V_{dc,\text{max}}$ and current $I_{dc,\text{max}}$ which can be supplied from this source and (b) the delay angle α_d required to achieve a magnet current of 2.5 A.

■ Solution

a. From Eq. 10.20, the maximum voltage (corresponding to $\alpha_d = 0$) is equal to

$$V_{dc,\text{max}} = \left(\frac{3\sqrt{2}}{\pi}\right) V_{1-1,\text{rms}} = \left(\frac{3\sqrt{2}}{\pi}\right) 460 = 621 \text{ V}$$

and $I_{dc,\text{max}} = V_{dc,\text{max}}/R = 9.1 \text{ A}$

b. The delay angle for a current of 2.5 A, corresponding to $V_{dc} = I_{dc}R = 170 \text{ V}$, can be found from inverting Eq. 10.20 as

$$\alpha_d = \cos^{-1} \left[\left(\frac{\pi}{3\sqrt{3}} \right) \left(\frac{V_{dc}}{V_{1-1,\text{rms}}} \right) \right] = 1.29 \text{ rad} = 74.1^\circ$$

Practice Problem 10.8

Repeat Example 10.9 for the case in which the 60-Hz source is replaced by a 50-Hz, 400-V, three-phase source.

Solution

- $V_{dc,\text{max}} = 540 \text{ V}$, $I_{dc,\text{max}} = 7.94 \text{ A}$
- $\alpha_d = 1.25 \text{ rad} = 71.6^\circ$

The derivations for three-phase bridges presented here have ignored issues such as the effect of commutating inductance, which we considered during our examination of single-phase rectifiers. Although the limited scope of our presentation does not

permit us to specifically discuss them here, the effects in three-phase rectifiers are similar to those for single-phase systems and must be considered in the design and analysis of practical three-phase rectifier systems.

10.3 INVERSION: CONVERSION OF DC TO AC

In Section 10.2 we discussed various rectifier configurations that can be used to convert ac to dc. In this section, we will discuss some circuit configurations, referred to as *inverters*, which can be used to convert dc to the variable-frequency, variable-voltage power required for many motor-drive applications. Many such configurations and techniques are available, and we will not attempt to discuss them all. Rather, consistent with the aims of this chapter, we will review some of the common inverter configurations and highlight their basic features and characteristics.

For the purposes of this discussion, we will assume the inverter is preceded by a “stiff” dc source. For example, in Section 10.2, we saw how an LC filter can be used to produce a relatively constant dc output voltage from a rectifier. Thus, as shown in Fig. 10.39a, for our study of inverters we will represent such rectifier systems by a constant dc voltage source V_0 , known as the *dc bus voltage* at the inverter input. We will refer to such a system, with a constant-dc input voltage, as a *voltage-source inverter*.

Similarly, we saw that a “large” inductor in series with the rectifier output produces a relatively constant dc current, known as the *dc link current*. We will therefore

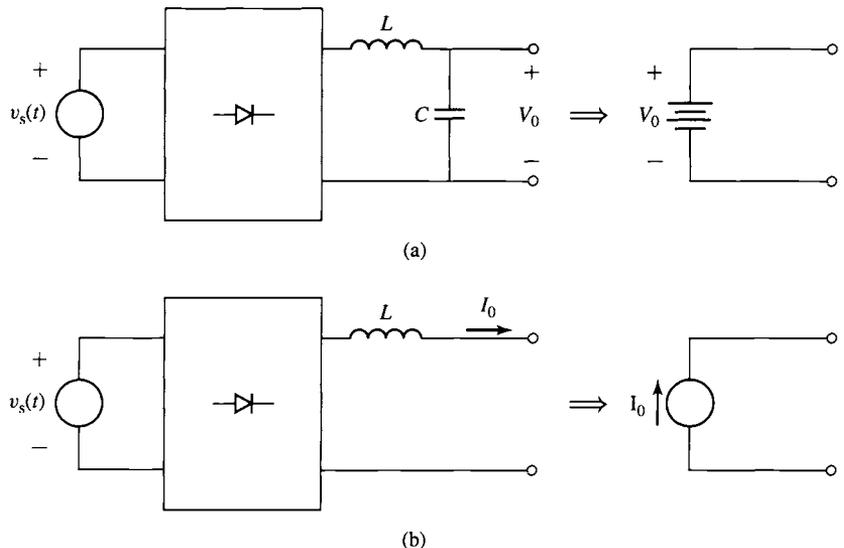


Figure 10.39 Inverter-input representations. (a) Voltage source. (b) Current source.

represent such a rectifier system by a current source I_0 at the inverter input. We will refer to this type of inverter as a *current-source inverter*.

Note that, as we have seen in Section 10.2, the values of these dc sources can be varied by appropriate controls applied to the rectifier stage, such as the timing of gate pulses to SCRs in the rectifier bridge. Control of the magnitude of these sources in conjunction with controls applied to the inverter stage provides the flexibility required to produce a wide variety of output waveforms for various motor-drive applications.

10.3.1 Single-Phase, H-bridge Step-Waveform Inverters

Figure 10.40a shows a single-phase inverter configuration in which a load (consisting here of a series RL combination) is fed from a dc voltage source V_0 through a set of four IGBTs in what is referred to as an *H-bridge* configuration. MOSFETs or other switching devices are equally applicable to this configuration. As we discussed in Section 10.1.3, the IGBTs in this system are used simply as switches. Because the IGBTs in this H-bridge include protection diodes, we can analyze the performance of this circuit by replacing the IGBTs by the ideal-switch model of Fig. 10.13b as shown in Fig. 10.40b.

For our analysis of this inverter, we will assume that the switching times of this inverter (i.e., the length of time the switches remain in a constant state) are much longer than the load time constant L/R . Hence, on the time scale of interest, the load current will simply be equal to V_L/R , with V_L being determined by the state of the switches.

Let us begin our investigation of this inverter configuration assuming that switches S1 and S3 are ON and that i_L is positive, as shown in Fig. 10.41a. Under this condition the load voltage is equal to V_0 and the load current is thus equal to V_0/R .

Let us next assume that switch S1 is turned OFF, while S3 remains ON. This will cause the load current, which cannot change instantaneously due to the presence of the inductor, to commute from switch S1 to diode D2, as shown in Fig. 10.41b. Note that under this condition, the load voltage is zero and hence there will be zero load current. Note also that this same condition could have been reached by turning switch S3 OFF with S1 remaining ON.

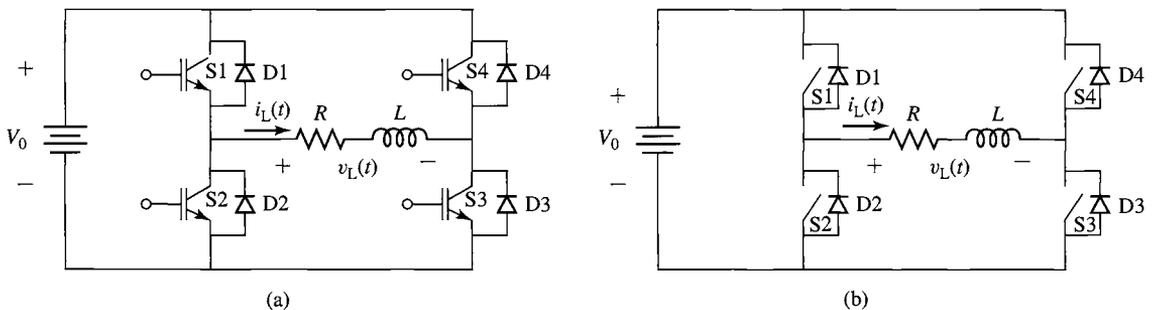


Figure 10.40 Single-phase H-bridge inverter configuration. (a) Typical configuration using IGBTs. (b) Generic configuration using ideal switches.

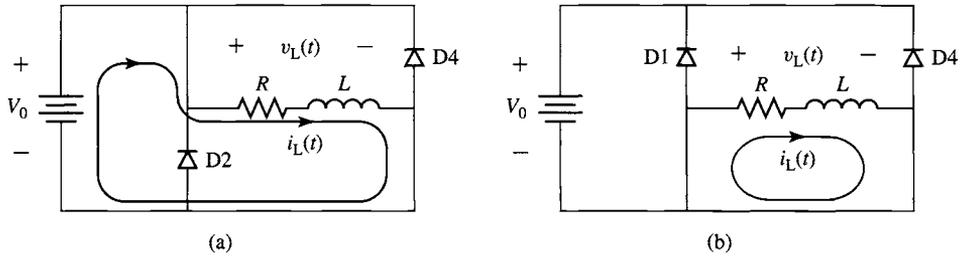


Figure 10.41 Analysis of the H-bridge inverter of Fig. 10.40b. (a) Switches S1 and S3 ON. (b) Switch S3 ON.

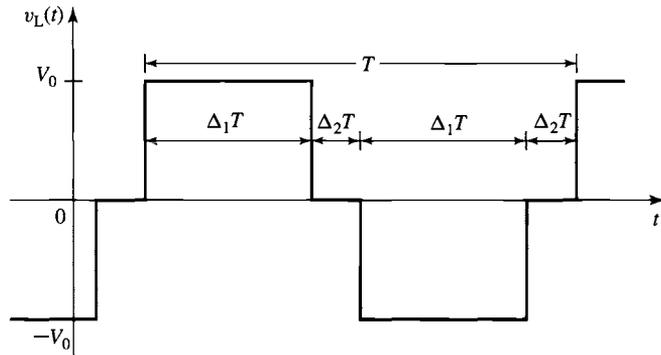


Figure 10.42 Typical output-voltage waveform for the H-bridge of Fig. 10.40.

At this point, it is possible to reverse the load voltage and current by turning ON switches S2 and S4, in which case $V_L = -V_0$ and $i_L = -V_0/R$. Finally, the current can be again brought to zero by turning OFF either switch S2 or switch S4. At this point, one cycle of an applied load-voltage waveform of the form of Fig. 10.42 has been completed.

A typical waveform produced by the switching sequence described above is shown in Fig. 10.42, with an ON time of $\Delta_1 T$ and OFF time of $\Delta_2 T$ ($\Delta_2 = 0.5 - \Delta_1$) for both the positive and negative portions of the cycle. Such a waveform consists of a fundamental ac component of frequency $f_0 = 1/T$, where T is the period of the switching sequence, and components at odd-harmonics frequencies of that fundamental.

The waveform of Fig. 10.42 can be considered a simple one-step approximation to a sinusoidal waveform. Fourier analysis can be used to show that it has a fundamental component of peak amplitude

$$V_{L,1} = \left(\frac{4}{\pi}\right) V_0 \sin(\Delta_1 \pi) \quad (10.21)$$

and n 'th-harmonic components ($n = 3, 5, 7, \dots$) of peak amplitude

$$V_{L,n} = \left(\frac{4}{n\pi} \right) V_0 \sin(n \Delta_1 \pi) \quad (10.22)$$

Although this stepped waveform appears to be a rather crude approximation to a sinusoid, it clearly contains a significant fundamental component. In many applications it is perfectly adequate as the output voltage of a motor-drive. For example, three-such waveforms, separated by 120° in time phase, could be used to drive a three-phase motor. The fundamental components would combine to produce a rotating flux wave as discussed in Chapter 4. In some motor-drive systems, LC filters, consisting of shunt capacitors operating in conjunction with the motor phase inductances, are used to reduce the harmonic voltages applied to the motor phase windings.

In general, the higher-order harmonics, whose amplitudes vary inversely with their harmonic number, as seen from Eq. 10.22, will produce additional core loss in the stator as well as dissipation in the rotor. Provided that these additional losses are acceptable both from the point of view of motor heating as well as motor efficiency, a drive based upon this switching scheme will be quite adequate for many applications.

EXAMPLE 10.10

A three-phase, H-bridge, voltage-source, step-waveform inverter will be built from three H-bridge inverter stages of the type shown in Fig. 10.40b. Each phase will be identical, with the exception that the switching pattern of each phase will be displaced by $1/3$ of a period in time phase. This system will be used to drive a three-phase, four-pole motor with $N_{ph} = 34$ turns per phase and winding factor $k_w = 0.94$. The motor is Y-connected, and the inverters are each connected phase-to-neutral.

For a dc supply voltage of 125 V, a switching period T of 20 msec and with $\Delta_1 = 0.44$, calculate (a) the frequency and synchronous speed in rpm of the resultant air-gap flux wave and (b) the rms amplitude of the line-to-line voltage applied to the motor.

■ Solution

- a. The frequency f_e of the fundamental component of the drive voltage will equal $f_e = 1/T = 50$ Hz. From Eq. 4.41 this will produce an air-gap flux wave which rotates at

$$n_s = \left(\frac{120}{\text{poles}} \right) f_e = \left(\frac{120}{4} \right) 50 = 1500 \text{ r/min}$$

- b. The peak of the fundamental component of the applied line-to-neutral voltage can be found from Eq. 10.21.

$$V_{a,\text{peak}} = \left(\frac{4}{\pi} \right) V_0 \sin(\Delta_1 \pi) = \left(\frac{4}{\pi} \right) 125 \sin(0.44 \pi) = 156 \text{ V}$$

The resultant rms, line-to-line voltage is thus given by

$$V_{l-l,\text{rms}} = \sqrt{\frac{3}{2}} V_{a,\text{peak}} = 191 \text{ V}$$

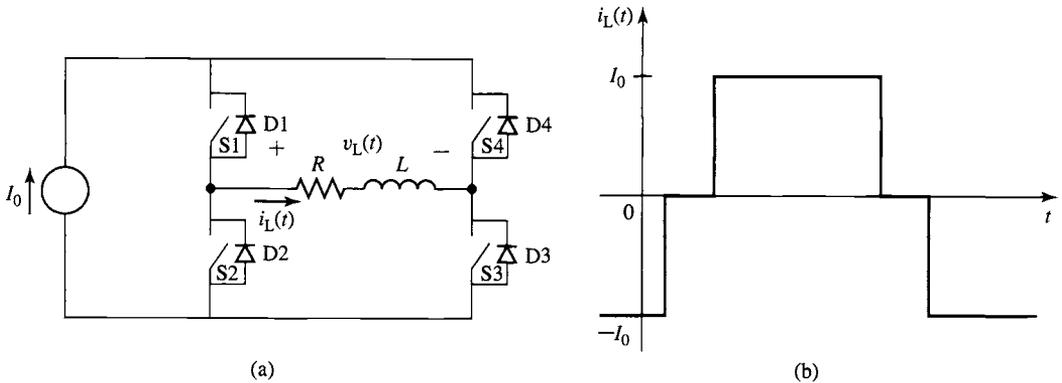


Figure 10.43 (a) H-bridge inverter configuration fed by a current source. (b) Typical stepped load-current waveform.

Practice Problem 10.9

For the three-phase inverter system of Example 10.10, (a) find the ON-time fraction Δ_1 for which the 5'th-harmonic component of the applied voltage will be zero. (b) Calculate the corresponding peak amplitude of the fundamental-component of the line-to-neutral voltage.

Solution

- 0.2
- 93 V

Figure 10.43a shows an H-bridge current-source inverter. This inverter configuration is analogous to the voltage-source configuration of Fig. 10.40. In fact, the discussion of the voltage-source inverter applies directly to the current-source configuration with the exception that the switches control the load current instead of the load voltage. Thus, again assuming that the load time constant (L/R) is much shorter than the switching time, a typical load current waveform would be similar to that shown in Fig. 10.43b.

EXAMPLE 10.11

Determine a switching sequence for the inverter of Fig. 10.43a that will produce the stepped waveform of Fig. 10.43b.

■ Solution

Table 10.2 shows one such switching sequence, starting at time $t = 0$ at which point the load current $i_L(t) = -I_0$.

Note that zero load current is achieved by turning on two switches so as to bypass the load and to directly short the current source. When this is done, the load current will quickly decay to zero, flowing through one of the switches and one of the reverse-polarity diodes. In general, one would not apply such a direct short across the voltage source in a voltage-source inverter

Table 10.2 Switching sequence used to produce the load current waveform of Fig. 10.43b.

$i_L(t)$	S1	S2	S3	S4
$-I_0$	OFF	ON	OFF	ON
0	ON	ON	OFF	OFF
I_0	ON	OFF	ON	OFF
0	OFF	OFF	ON	ON
$-I_0$	OFF	ON	OFF	ON

because the resultant current would most likely greatly exceed the ratings of the switches. However, in the case of a current-source inverter, the switch current cannot exceed that of the current source, and hence the direct short can be (and, in fact, must be) maintained for as long as it is desired to maintain zero load current.

EXAMPLE 10.12

Consider the current-source inverter of Fig. 10.44a. Here the load consists of a sinusoidal voltage source $V_a \cos \omega t$. Assume the inverter switches are controlled such that the load current is a square-wave, also at frequency $f = \omega/(2\pi)$, as shown in Fig. 10.44b. Calculate the time-average power delivered to the load as a function of the delay angle α_d as defined in Fig. 10.44b.

■ Solution

Because the load voltage is sinusoidal, time-average power will only be produced by the fundamental component of the load current. By analogy to Eq. 10.21, with I_0 replacing V_0 and with $\Delta_1 = 0.5$, the amplitude of the fundamental-component of the load current is

$$I_{L,1} = \left(\frac{4}{\pi} \right) I_0$$

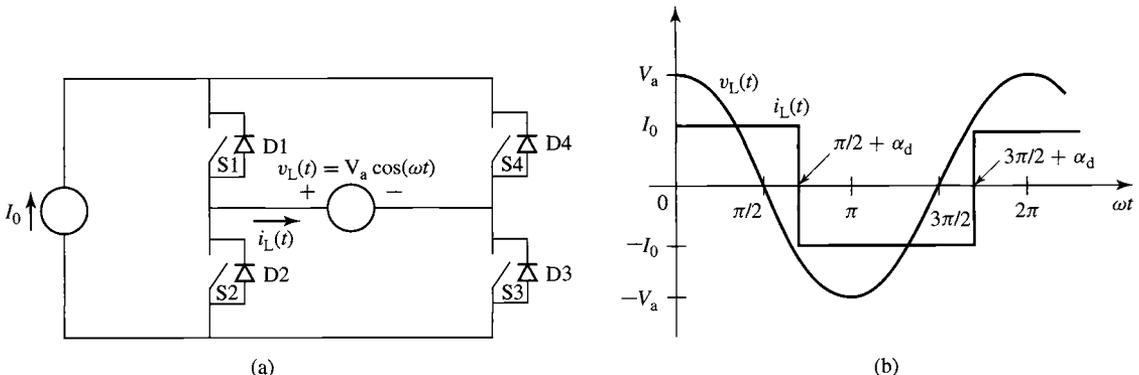


Figure 10.44 (a) Current-source inverter for Example 10.12. (b) Load-current waveform.

and therefore the fundamental component of the load current is equal to

$$i_{L,1}(t) = I_{L,1} \cos(\omega t - \alpha_d) = \left(\frac{4}{\pi}\right) I_0 \cos(\omega t - \alpha_d)$$

The complex amplitude of the load voltage is thus given by $\hat{V}_L = V_a$ and that of the load current is $\hat{I}_L = I_{L,1} e^{-j\alpha_d}$. Thus the time average power is equal to

$$P = \frac{1}{2} \operatorname{Re}[\hat{I}_L \hat{V}_L^*] = \left(\frac{2}{\pi}\right) V_a I_0 \cos \alpha_d$$

By varying the firing-delay angle α_d , the power transferred from the source to the load can be varied. In fact, as α_d is varied over the range $0 \leq \alpha_d \leq \pi$, the power will vary over the range

$$\left(\frac{2}{\pi}\right) V_a I_0 \geq P \geq -\left(\frac{2}{\pi}\right) V_a I_0$$

Note that this inverter can regenerate; i.e., for $\pi/2 < \alpha_d \leq \pi$, $P < 0$ and hence power will flow from the load back into the inverter.

Practice Problem 10.10

The inverter of Example 10.12 is operated with a fixed delay angle $\alpha_d = 0$ but with a variable ON-time fraction Δ_1 . Find an expression for the time-average power delivered to the load as a function of Δ_1 .

Solution

$$P = \left(\frac{2}{\pi}\right) V_a I_0 \sin(\Delta_1 \pi)$$

10.3.2 Pulse-Width-Modulated Voltage-Source Inverters

Let us again consider the H-bridge configuration of Fig. 10.40b, repeated again in Fig. 10.45. Again, an RL load is fed from a voltage source through the H-bridge. However, in this case, let us assume that the switching time of the inverter is much shorter than the load time constant (L/R).

Consider a typical operating condition as shown in Fig. 10.46. Under this condition, the switches are operated with a period T and a *duty cycle* D ($0 \leq D \leq 1$). As can be seen from Fig. 10.46a, for a time DT switches S1 and S3 are ON, and the load voltage is V_0 . This is followed by a time $(1 - D)T$ during which switches S1 and S3 are OFF, and the current is transferred to diodes D2 and D4, setting the load voltage equal to $-V_0$. The duty cycle D is thus a fraction of the total period, in this case the fraction of the period during which the load voltage is V_0 .

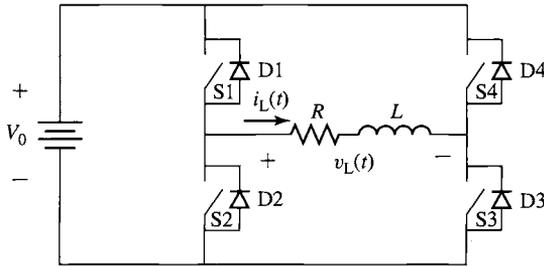


Figure 10.45 Single-phase H-bridge inverter configuration.

Note that although switches S2 and S4 would normally be turned ON after switches S1 and S3 are turned OFF (but not before they are turned OFF, to avoid a direct short across the voltage source) because they are actually semiconductor devices, they will not carry any current unless the load current goes negative. Rather, the current will flow through the protection diodes D1 and D3. Alternatively, if the load current is negative, then the current will be controlled by the operation of switches S2 and S4 in conjunction with diodes D1 and D3. Under this condition, switches S1 and S3 will not carry current.

This type of control is referred to as *pulse-width modulation*, or *PWM*, because it is implemented by varying the width of the voltage pulses applied to the load. As can be seen from Fig. 10.46a, the average voltage applied to the load is equal to

$$(v_L)_{\text{avg}} = (2D - 1)V_0 \quad (10.23)$$

As we will now show, varying the duty cycle under PWM control can produce a continuously varying load current.

A typical load-current waveform is shown in Fig. 10.46b. In the steady state, the average current through the inductor will be constant and hence voltage across the inductor must equal zero. Thus, the average load current will equal the average

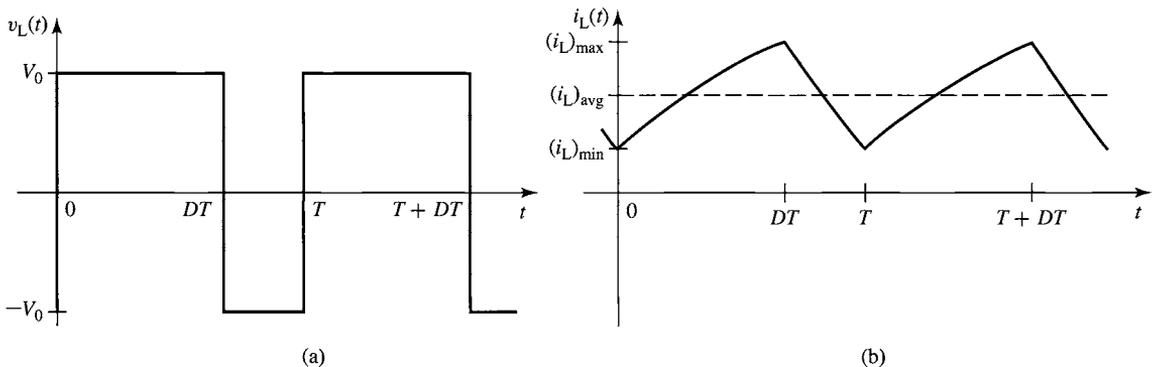


Figure 10.46 Typical (a) voltage and (b) current waveform under PWM operation.

voltage divided by the resistance or

$$(i_L)_{\text{avg}} = \frac{(v_L)_{\text{avg}}}{R} = \frac{[2D - 1]V_0}{R} \quad (10.24)$$

Thus we see that by varying the duty cycle D over the range of 0 to 1, we can vary the load current over the range $-V_0/R \leq (i_L)_{\text{avg}} \leq V_0/R$.

Because the current waveform is periodic, the maximum and minimum current, and hence the current ripple, can be easily calculated. Assigning time $t = 0$ to the time switches S1 and S3 are first turned ON and the load current is minimum, the current during this time period will be given by

$$i_L(t) = \frac{V_0}{R} + \left((i_L)_{\text{min}} - \frac{V_0}{R} \right) e^{-\frac{t}{\tau}} \quad (0 \leq t \leq DT) \quad (10.25)$$

where $\tau = L/R$. The maximum load current $(i_L)_{\text{max}}$ is reached at time DT

$$(i_L)_{\text{max}} = \frac{V_0}{R} + \left((i_L)_{\text{min}} - \frac{V_0}{R} \right) e^{-\frac{DT}{\tau}} \quad (10.26)$$

After switches S1 and S3 are turned OFF, the load voltage is $-V_0$ and the current is given by

$$i_L(t) = -\frac{V_0}{R} + \left((i_L)_{\text{max}} + \frac{V_0}{R} \right) e^{-\frac{(t-DT)}{\tau}} \quad (DT < t \leq T) \quad (10.27)$$

Because the current is periodic with period T , $i_L(t)$ again will be equal to $(i_L)_{\text{min}}$ at time T . Thus

$$(i_L)_{\text{min}} = -\frac{V_0}{R} + \left((i_L)_{\text{max}} + \frac{V_0}{R} \right) e^{-\frac{(T-DT)}{\tau}} \quad (10.28)$$

Solving Eqs. 10.26 and 10.28 gives

$$(i_L)_{\text{min}} = -\left(\frac{V_0}{R} \right) \frac{[1 - 2e^{-\frac{T(1-D)}{\tau}} + e^{-\frac{T}{\tau}}]}{(1 - e^{-\frac{T}{\tau}})} \quad (10.29)$$

and

$$(i_L)_{\text{max}} = \left(\frac{V_0}{R} \right) \frac{[1 - 2e^{-\frac{DT}{\tau}} + e^{-\frac{T}{\tau}}]}{(1 - e^{-\frac{T}{\tau}})} \quad (10.30)$$

The current ripple Δi_L can be calculated as the difference between the maximum and minimum current.

$$\Delta i_L = (i_L)_{\text{max}} - (i_L)_{\text{min}} \quad (10.31)$$

In the limit that $T \ll \tau$, this can be written as

$$\Delta i_L \approx \left(\frac{2V_0}{R} \right) \left(\frac{T}{\tau} \right) D(1 - D) \quad (10.32)$$

EXAMPLE 10.13

A PWM inverter such as that of Fig. 10.45 is operating from a dc voltage of 48 V and driving a load with $L = 320$ mH and $R = 3.7 \Omega$. For a switching frequency of 1 kHz, calculate the average load current, the minimum and maximum current, and the current ripple for a duty cycle $D = 0.8$.

■ Solution

From Eq. 10.24, the average load current will equal

$$(i_L)_{\text{avg}} = \frac{[2D - 1]V_0}{R} = \frac{0.6 \times 48}{3.7} = 7.78 \text{ A}$$

For a frequency of 1 kHz, the period $T = 1$ msec. The time constant $\tau = L/R = 86.5$ msec. $(i_L)_{\text{min}}$ and $(i_L)_{\text{max}}$ can then be found from Eqs. 10.29 and 10.30 to be $(i_L)_{\text{min}} = 7.76$ A and $(i_L)_{\text{max}} = 7.81$ A. Thus the current ripple, calculated from Eq. 10.31, is 0.05 A, which is equal to 0.6 percent of the average load current. Alternatively, using the fact that $T/\tau = 0.012 \ll 1$, the current ripple could have been calculated directly from Eq. 10.32

$$\Delta i_L = \left(\frac{2V_0}{R} \right) \left(\frac{T}{\tau} \right) D(1 - D) = \left(\frac{2 \times 48}{3.7} \right) \left(\frac{1}{86.5} \right) \times 0.8 \times 0.2 = 0.05 \text{ A}$$

Practice Problem 10.11

Calculate (a) the average current and (b) the current ripple for the PWM inverter of Example 10.13 if the switching frequency is reduced to 250 Hz.

Solution

- 7.78 A (unchanged from Example 10.13)
- 0.19 A

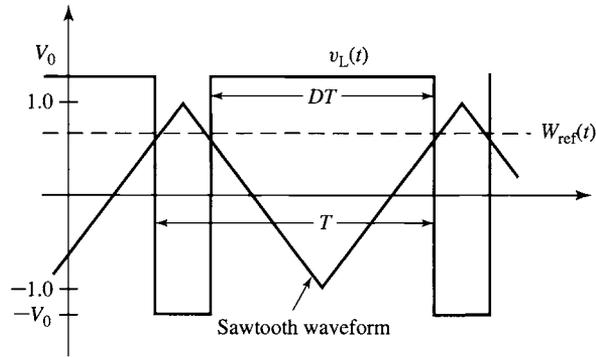
Now let us consider the situation for which the duty cycle is varied with time, i.e., $D = D(t)$. If $D(t)$ varies slowly in comparison to the period T of the switching frequency, from Eq. 10.23, the average load voltage will be equal to

$$(v_L)_{\text{avg}} = [2D(t) - 1]V_0 \quad (10.33)$$

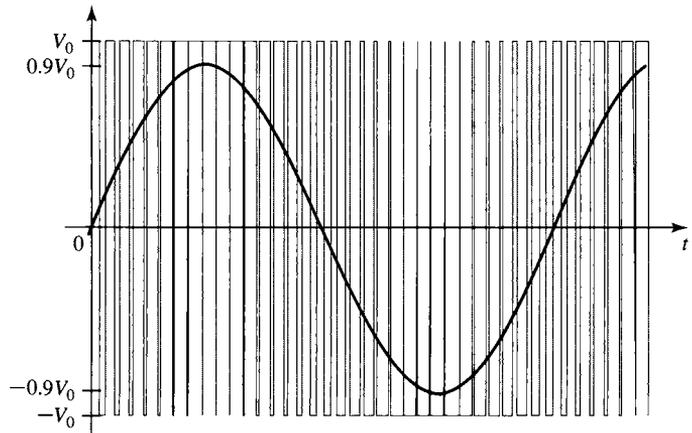
and the average load current will be

$$(i_L)_{\text{avg}} = \frac{[2D(t) - 1]V_0}{R} \quad (10.34)$$

Figure 10.47a illustrates a method for producing the variable duty cycle for this system. Here we see a saw-tooth waveform which varies between -1 and 1 . Also shown is a reference waveform $W_{\text{ref}}(t)$ which is constrained to lie within the range -1 and 1 . The switches will be controlled in pairs. During the time that $W_{\text{ref}}(t)$ is greater than the saw-tooth waveform, switches S1 and S3 will be ON and the load voltage will be V_0 . Similarly, when $W_{\text{ref}}(t)$ is less than the saw-tooth waveform, switches S2



(a)



(b)

Figure 10.47 (a) Method for producing a variable duty cycle from a reference waveform $W_{\text{ref}}(t)$. (b) Load voltage and average load voltage for $W_{\text{ref}}(t) = 0.9 \sin \omega t$.

and S4 will be ON and the load voltage will be $-V_0$. Thus,

$$D(t) = \frac{(1 + W_{\text{ref}}(t))}{2} \quad (10.35)$$

and thus

$$(v_L)_{\text{avg}} = \left[2 \left(\frac{1 + W_{\text{ref}}(t)}{2} \right) - 1 \right] V_0 = W_{\text{ref}}(t) V_0 \quad (10.36)$$

Figure 10.47b shows the load voltage $v_L(t)$ for a sinusoidal reference waveform $W_{\text{ref}}(t) = 0.9 \sin \omega t$. The average voltage across the load, $(v_L)_{\text{avg}} = W_{\text{ref}}(t) V_0$, is also shown.

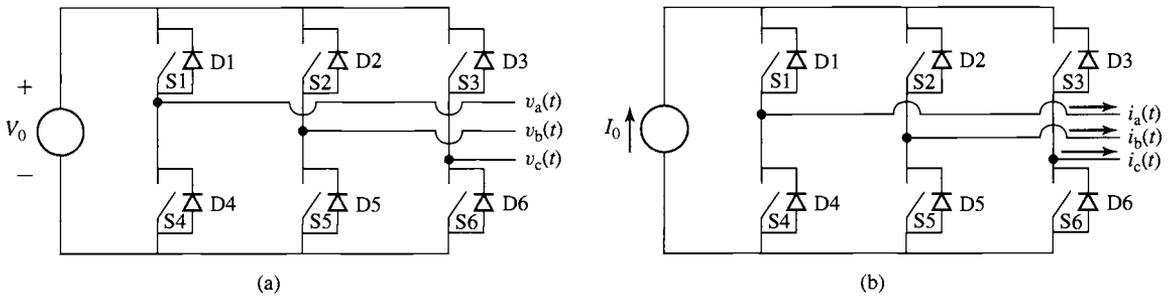


Figure 10.48 Three-phase inverter configurations. (a) Voltage-source. (b) Current-source.

Note that the H-bridge inverter configuration of Fig. 10.43 can be used to produce a PWM current-source inverter. In a fashion directly analogous to the derivation of Eq. 10.36, one can show that such an inverter would produce an average current of the form

$$(\dot{i}_L)_{\text{avg}} = W_{\text{ref}}(t)I_0 \quad (10.37)$$

where I_0 is the magnitude of the dc link current feeding the H-bridge. Note, however, that the sudden current swings between I_0 and $-I_0$ associated with such an inverter will produce large voltages should the load have any inductive component. As a result, practical inverters of this type require large capacitive filters to absorb the harmonic components of the PWM current and to protect the load against damage due to voltage-induced insulation failure.

10.3.3 Three-Phase Inverters

Although the single-phase motor drives of Section 10.3.2 demonstrate the important characteristics of inverters, most variable-frequency drives are three phase. Figures 10.48a and 10.48b show the basic configuration of three-phase motor inverters (voltage- and current-source respectively). Here we have shown the switches as ideal switches, recognizing that in a practical implementation, bidirectional capability will be achieved by a combination of a semiconductor switching device, such as an IGBT and a MOSFET, and a reverse-polarity diode.

These configurations can be used to produce both stepped waveforms (either voltage-source or current-source) as well as pulse-width-modulated waveforms. This will be illustrated in the following example.

EXAMPLE 10.14

The three-phase current-source inverter configuration of Fig. 10.48b is to be used to produce a three-phase stepped current waveform of the form shown in Fig 10.49. (a) Determine the switch sequence over the period $0 \leq t \leq T$ and (b) calculate the fundamental, third, fifth, and seventh harmonics of the phase-*a* current waveform.

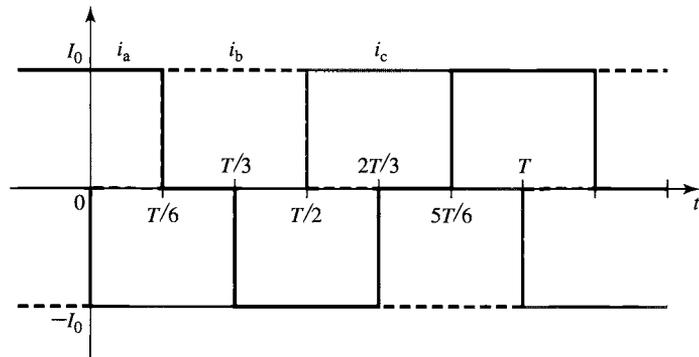


Figure 10.49 Three-phase stepped current waveform for Example 10.14.

■ Solution

- a. By observing that switch S1 is ON when the phase-*a* current is positive, switch S4 is ON when it is negative, and so forth, the following table of switching operations can be produced.

t	$0-(T/6)$	$(T/6)-(T/3)$	$(T/3)-(T/2)$	$(T/2)-(2T/3)$	$(2T/3)-(5T/6)$	$(5T/6)-T$
S1	ON	OFF	OFF	OFF	OFF	ON
S2	OFF	ON	ON	OFF	OFF	OFF
S3	OFF	OFF	OFF	ON	ON	OFF
S4	OFF	OFF	ON	ON	OFF	OFF
S5	OFF	OFF	OFF	OFF	ON	ON
S6	ON	ON	OFF	OFF	OFF	OFF

- b. The amplitudes of the harmonic components of the phase current can be determined from Eqs. 10.21 and 10.22 by setting $\Delta_1 = 1/3$. Thus,

$$I_{a,1} = \left(\frac{2\sqrt{3}}{\pi} \right) I_0 \quad I_{a,3} = 0$$

$$I_{a,5} = - \left(\frac{2\sqrt{3}}{5\pi} \right) I_0 \quad I_{a,7} = \left(\frac{2\sqrt{3}}{7\pi} \right) I_0$$

10.4 SUMMARY

The goal of this chapter is relatively modest. Our focus has been to introduce some basic principles of power electronics and to illustrate how they can be applied to the design of various power-conditioning circuits that are commonly found in motor drives. Although the discussion in this chapter is neither complete nor extensive, it

is intended to provide the background required to support the various discussions of motor control which are presented in this book.

We began with a brief overview of a few of the available solid-state switching devices: diodes, SCRs, IGBTs and MOSFETs, and so on. We showed that, for the purposes of a preliminary analysis, it is quite sufficient to represent these devices as ideal switches. To emphasize the fact that they typically can pass only unidirectional current, we included ideal diodes in series with these switches. The simplest of these devices is the diode, which has only two terminals and is turned ON and OFF simply by the conditions of the external circuit. The remainder have a third terminal which can be used to turn the device ON and, in the case of transistors such as MOSFETs and IGBTs, OFF again.

A typical variable-frequency, variable-voltage motor-drive system can be considered to consist of three sections. The input section rectifies the power-frequency, fixed-voltage ac input and produces a dc voltage or current. The middle section filters the rectifier output, producing a relatively constant dc current or voltage, depending upon the type of drive under consideration. The output inverter section converts the dc to variable-frequency, variable-voltage ac voltages or currents which can be applied to the terminals of a motor.

The simplest inverters we investigated produce stepped voltage or current waveforms whose amplitude is equal to that of the dc source and whose frequency can be controlled by the timing of the inverter switches. To produce a variable-amplitude output waveform, it is necessary to apply additional control to the rectifier stage to vary the amplitude of the dc bus voltage or link current supplied to the inverter.

We also discussed pulse-width-modulated voltage-source inverters. In this type of inverter, the voltage to the load is switched between V_0 and $-V_0$ such that the average load voltage is determined by the duty cycle of the switching waveform. Loads whose time constant is long compared to the switching time of the inverter will act as filters, and the load current will then be determined by the average load voltage. Pulse-width modulated current-source inverters were also discussed briefly.

The reader should approach the presentation here with great caution. It is important to recognize that a complete treatment of power electronics and motor drives is typically the topic of a multiple-course sequence of study. Although the basic principles discussed here apply to a wide range of motor drives, there are many details which must be included in the design of practical motor drives. Drive circuitry to turn ON the “switches” (gate drives for SCRs, MOSFETs, IGBTs, etc.) must be carefully designed to provide sufficient drive to fully turn on the devices and to provide the proper switching sequences. The typical inverter includes a controller and a protection system which is quite elaborate. Typically, the design of a specific drive is dominated by the current and voltage ratings of available switches devices. This is especially true in the case of high-power drive systems in which switches must be connected in series and/or parallel to achieve the desired power rating. The reader is referred to references in the bibliography for a much more complete discussion of power electronics and inverter systems than has been presented here.

Motor drives based upon the configurations discussed here can be used to control motor speed and motor torque. In the case of ac machines, the application of

power-electronic based motor drives has resulted in performance that was previously available only with dc machines and has led to widespread use of these machines in most applications.

10.5 BIBLIOGRAPHY

This chapter is intended to serve as an introduction to the discipline of power electronics. For readers who wish to study this topic in more depth, this bibliography lists a few of the many textbooks which have been written on this subject.

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10.6 PROBLEMS

- 10.1** Consider the half-wave rectifier circuit of Fig. 10.3a. The circuit is driven by a triangular voltage source $v_s(t)$ of amplitude $V_0 = 9$ V as shown in Fig. 10.50. Assuming the diode to be ideal and for a resistor $R = 1.5$ k Ω :
- Plot the resistor voltage $v_R(t)$.
 - Calculate the rms value of the resistor voltage.
 - Calculate the time-averaged power dissipation in the resistor.
- 10.2** Repeat Problem 10.1 assuming the diode to have a fixed 0.6 V voltage drop when it is ON but to be otherwise ideal. In addition, calculate the time-averaged power dissipation in the diode.
- 10.3** Consider the half-wave SCR rectifier circuit of Fig. 10.6 supplied from the triangular voltage source of Fig. 10.50. Assuming the SCR to be ideal, calculate the rms resistor voltage as a function of the firing-delay time t_d ($0 \leq t_d \leq T/2$).
- 10.4** Consider the rectifier system of Example 10.5. Write a MATLAB script to plot the ripple voltage as a function of filter capacitance as the filter



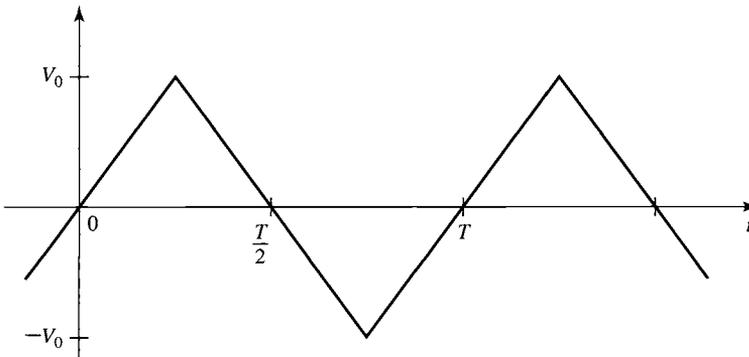


Figure 10.50 Triangular voltage waveform.

capacitance is varied over the range $3000 \mu\text{F} \leq C \leq 10^5 \mu\text{F}$. Assume the diode to be ideal. Use a log-scale for the capacitance.

- 10.5** Consider the full-wave rectifier system of Fig. 10.16 with $R = 500 \Omega$ and $C = 200 \mu\text{F}$. Assume each diode to have a constant voltage drop of 0.7 V when it is ON but to be otherwise ideal. For a 220 V rms , 50 Hz sinusoidal source, write a MATLAB script to calculate



- the peak voltage across the load resistor.
- the magnitude of the ripple voltage.
- the time-averaged power supplied to the load resistor.
- the time-averaged power dissipation in the diode bridge.

- 10.6** Consider the half-wave rectifier system of Fig. 10.51. The voltage source is $v_s(t) = V_0 \sin \omega t$ where $V_0 = 15 \text{ V}$, and the frequency is 100 Hz . For $L = 1 \text{ mH}$ and $R = 1 \Omega$, plot the inductor current $i_L(t)$ for the first $1\text{-}1/2$ cycles of the applied waveform assuming the switch closes at time $t = 0$.

- 10.7** Repeat Problem 10.6, using MATLAB to plot the inductor current for the first 10 cycles following the switch closing at time $t = 0$. (Hint: This problem can be easily solved, using simple Euler integration to solve for the current.)

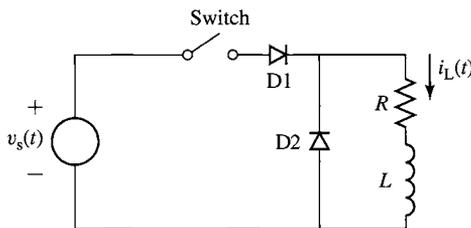


Figure 10.51 Half-wave rectifier system for Problem 10.6.

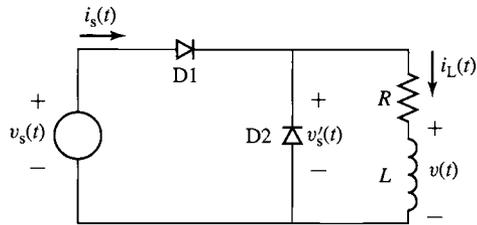


Figure 10.52 Half-wave rectifier system for Problem 10.8.

- 10.8** Consider the half-wave rectifier system of Fig. 10.52 as L becomes sufficiently large such that $\omega(L/R) \gg 1$, where ω is the supply frequency. In this case, the inductor current will be essentially constant. For $R = 5 \Omega$ and $v_s(t) = V_0 \sin \omega t$ where $V_0 = 45 \text{ V}$ and $\omega = 100\pi \text{ rad/sec}$. Assume the diodes to be ideal.
- Calculate the average (dc) value V_{dc} of the voltage $v'_s(t)$ across the series resistor/inductor combination.
 - Using the fact that, in the steady state, there will be zero average voltage across the inductor, calculate the dc inductor current I_{dc} .
 - Plot the instantaneous inductor voltage $v(t)$ over one cycle of the supply voltage.
 - Plot the instantaneous source current $i_s(t)$.
- 10.9** Consider the half-wave, phase-controlled rectifier system of Fig. 10.53. This is essentially the same circuit as that of Problem 10.8 with the exception that diode D1 of Fig. 10.52 has been replaced by an SCR, which you can consider to be ideal. Let $R = 5 \Omega$ and $v_s(t) = V_0 \sin \omega t$, where $V_0 = 45 \text{ V}$ and $\omega = 100\pi \text{ rad/sec}$. Assume that the inductor L is sufficiently large such that $\omega(L/R) \gg 1$ and that the SCR is triggered ON at time t_d ($0 \leq t_d \leq \pi/\omega$).
- Find an expression for the average (dc) value V_{dc} of the voltage $v'_s(t)$ across the series resistor/inductor combination as a function of the delay time t_d .

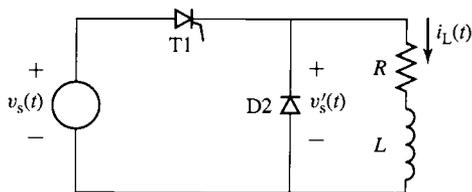


Figure 10.53 Half-wave, phase-controlled rectifier system for Problem 10.9.

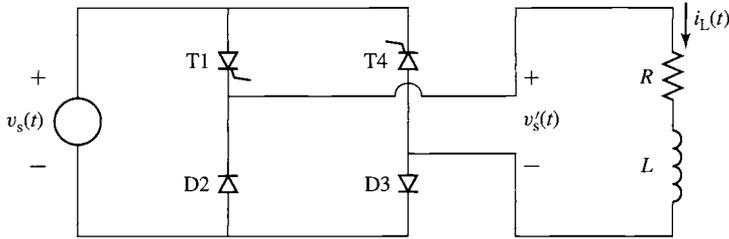


Figure 10.54 Full-wave, phase-controlled rectifier system for Problem 10.10.

- b. Using the fact that, in the steady state, there will be zero average voltage across the inductor, find an expression for the dc inductor current I_{dc} , again as a function of the delay time t_d .
- c. Plot I_{dc} as a function of t_d for $(0 \leq t_d \leq \pi/\omega)$.
- 10.10** The half-wave, phase-controlled rectifier system of Problem 10.9 and Fig. 10.53 is to be replaced by the full-wave, phase-controlled system of Fig. 10.54. SCR T1 will be triggered ON at time t_d ($0 \leq t_d \leq \pi/\omega$), and SCR T4 will be triggered on exactly one half cycle later.
- a. Find an expression for the average (dc) value V_{dc} of the voltage $v'_s(t)$ across the series resistor/inductor combination as a function of the delay time t_d .
- b. Using the fact that, in the steady state, there will be zero average voltage across the inductor, find an expression for the dc inductor current I_{dc} , again as a function of the delay time t_d .
- c. Plot I_{dc} as a function of t_d for $(0 \leq t_d \leq \pi/\omega)$.
- d. Plot the source current $i_s(t)$ for one cycle of the source voltage for $t_d = 3$ msec.
- 10.11** The full-wave, phase-controlled rectifier of Fig. 10.55 is supplying a highly inductive load such that the load current can be assumed to be purely dc, as represented by the current source I_{dc} in the figure. The source voltage is a

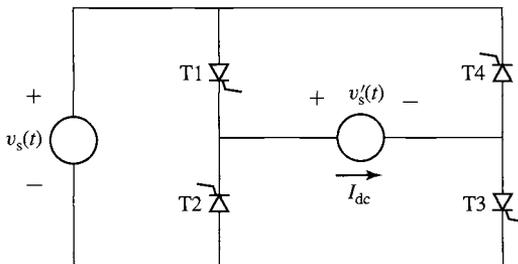


Figure 10.55 Full-wave, phase-controlled rectifier for Problem 10.11.

sinusoid, $v_s(t) = V_0 \sin \omega t$. As shown in Fig. 10.31, SCRs T1 and T3 are triggered together at delay angle α_d ($0 \leq \alpha_d \leq \pi$), and SCRs T2 and T4 are triggered exactly one-half cycle later.

- a. For $\alpha_d = \pi/4$:
 - (i) Sketch the load voltage $v'_s(t)$.
 - (ii) Calculate the average (dc) value V_{dc} of $v'_s(t)$.
 - (iii) Calculate the time-averaged power supplied to the load.
 - b. Repeat part (a) for $\alpha_d = 3\pi/4$.
- 10.12** A full-wave diode rectifier is fed from a 50-Hz, 220-V rms source whose series inductance is 12 mH. It drives a load with a resistance 8.4Ω which is sufficiently inductive that the load current can be considered to be essentially dc.
- a. Calculate the dc load current I_{dc} and the commutation time t_c .
 - b. Compare the dc current of part (a) with the dc current which would result if the commutating inductance could be eliminated from the system.
- 10.13** A 1-kW, 85-V, permanent-magnet dc motor is to be driven from a full-wave, phase-controlled bridge such as is shown in Fig. 10.56. When operating at its rated voltage, the dc-motor has a no-load speed of 1725 r/min and an armature resistance $R_a = 0.82 \Omega$. A large inductor ($L = 580$ mH) with resistance $R_L = 0.39 \Omega$ has been inserted in series with the output of the rectifier bridge to reduce the ripple current applied to the motor. The source voltage is a 115-V rms, 60-Hz sinusoid.
- With the motor operating at a speed of 1650 r/min, the motor current is measured to be 7.6 A.
- a. Calculate the motor input power.
 - b. Calculate the firing delay angle α_d of the SCR bridge.
- 10.14** Consider the dc-motor drive system of Problem 10.13. To limit the starting current of the dc motor to twice its rated value, a controller will be used to adjust the initial firing-delay angle of the SCR bridge. Calculate the required firing-delay angle α_d .

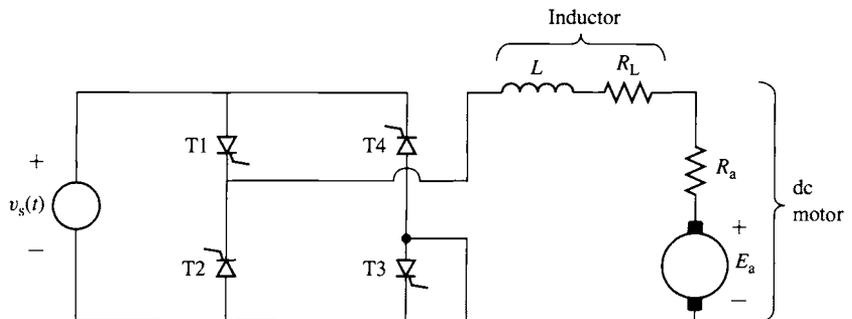


Figure 10.56 Dc motor driven from a full-wave, phase-controlled rectifier. Problem 10.13.

- 10.15** A three-phase diode bridge is supplied by a three-phase autotransformer such that the line-to-line input voltage to the bridge can be varied from zero to 230 V. The output of the bridge is connected to the shunt field winding of a dc motor. The resistance of this winding is 158Ω . The autotransformer is adjusted to produce a field current of 1.75 A. Calculate the rms output voltage of the autotransformer.
- 10.16** A dc-motor shunt field winding of resistance 210Ω is to be supplied from a 220-V rms, 50-Hz, three-phase source through a three-phase, phase-controlled rectifier. Calculate the delay angle α_d which will result in a field current of 1.1 A.
- 10.17** A superconducting magnet has an inductance of 4.9 H, a resistance of $3.6 \text{ m}\Omega$, and a rated operating current of 80 A. It will be supplied from a 15-V rms, three-phase source through a three-phase, phase-controlled bridge. It is desired to “charge” the magnet at a constant rate to achieve rated current in 25 seconds.
- Calculate the firing-delay angle α_d required to achieve this objective.
 - Calculate the firing-delay angle required to maintain a constant current of 80 A.
- 10.18** A voltage-source H-bridge inverter is used to produce the stepped waveform $v(t)$ shown in Fig. 10.57. For $V_0 = 50 \text{ V}$, $T = 10 \text{ msec}$ and $D = 0.3$:
- Using Fourier analysis, find the amplitude of the fundamental time-harmonic component of $v(t)$.
 - Use the MATLAB ‘fft()’ function to find the amplitudes of the first 10 time harmonics of $v(t)$.
- 10.19** Consider the stepped voltage waveform of Problem 10.18 and Fig. 10.57.
- Using Fourier analysis, find the value of D ($0 \leq D \leq 0.5$) such that the amplitude of the third-harmonic component of the voltage waveform is zero.

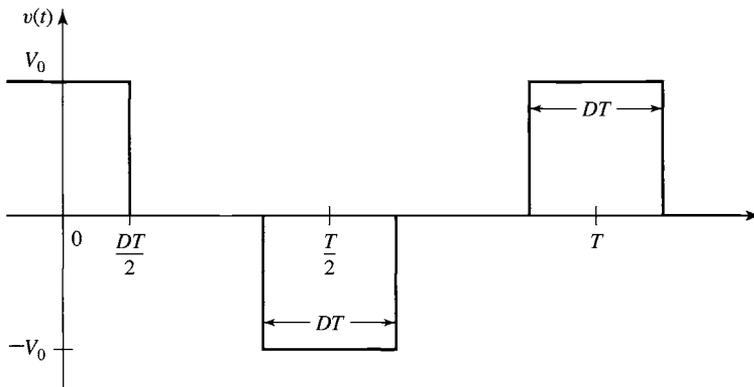


Figure 10.57 Stepped voltage waveform for Problem 10.18.

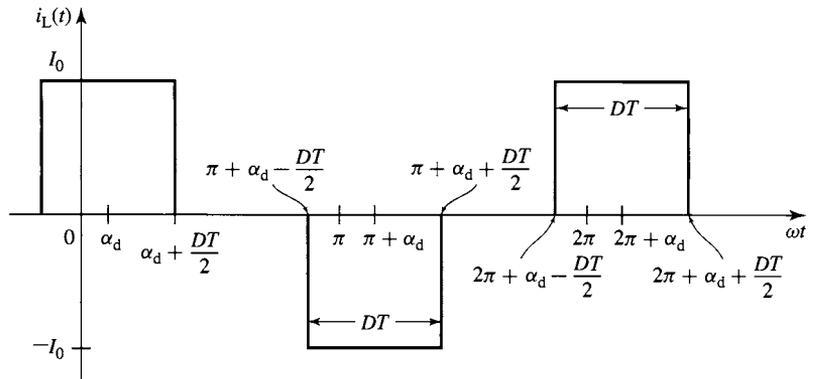


Figure 10.58 Stepped current waveform for Problem 10.20.

- b. Use the MATLAB 'fft()' function to find the amplitudes of the first 10 time harmonics of the resulting waveform.
- 10.20** Consider Example 10.12 in which a current-source inverter is driving a load consisting of a sinusoidal voltage. The inverter is controlled to produce the stepped current waveform shown in Fig. 10.58.
- Create a table showing the switching sequence required to produce the specified waveform and the time period during which each switch is either ON or OFF.
 - Express the fundamental component of the current waveform in the form

$$i_1(t) = I_1 \cos(\omega t + \phi_1)$$
 where I_1 and ϕ_1 are functions of I_0 , D and the delay angle α_d .
 - Derive an expression for the time-averaged power delivered to the voltage source $v_L(t) = V_a \cos \omega t$.
- 10.21** A PWM inverter such as that of Fig. 10.45 is operating from a dc voltage of 75 V and driving a load with $L = 53$ mH and $R = 1.7 \Omega$. For a switching frequency of 1500 Hz, calculate the average load current, the minimum and maximum current, and the current ripple for a duty cycle $D = 0.7$.

Speed and Torque Control

The objective of this chapter is to discuss various techniques for the control of electric machines. Since an in-depth discussion of this topic is both too extensive for a single chapter and beyond the scope of this book, the presentation here will necessarily be introductory in nature. We will present basic techniques for speed and torque control and will illustrate typical configurations of drive electronics that are used to implement the control algorithms. This chapter will build upon the discussion of power electronics in Chapter 10.

Note that the discussion of this chapter is limited to steady-state operation. The steady-state picture presented here is quite adequate for a wide variety of electric-machine applications. However, the reader is cautioned that system dynamics can play a critical role in some applications, with concerns ranging from speed of response to overall system stability. Although the techniques presented here form the basis for dynamic analyses, the constraints of an introductory textbook are such that a more extensive discussion, including transient and dynamic behavior, is not possible.

In the discussion of torque control for synchronous and induction machines, the techniques of field-oriented or vector control are introduced and the analogy is made with torque control in dc motors. This material is somewhat more sophisticated mathematically than the speed-control discussion and requires application of the $dq0$ transformations developed in Appendix C. The chapter is written such that this material can be omitted at the discretion of the instructor without detracting from the discussion of speed control.

11.1 CONTROL OF DC MOTORS

Before the widespread application of power-electronic drives to control ac machines, dc motors were by far the machines of choice in applications requiring flexibility of control. Although in recent years ac drives have become quite common, the ease of control of dc machines insure their continued use in many applications.

11.1.1 Speed Control

The three most common speed-control methods for dc motors are adjustment of the flux, usually by means of field-current control, adjustment of the resistance associated with the armature circuit, and adjustment of the armature terminal voltage.

Field-Current Control In part because it involves control at a relatively low power level (the power into the field winding is typically a small fraction of the power into the armature of a dc machine), *field-current control* is frequently used to control the speed of a dc motor with separately excited or shunt field windings. The equivalent circuit for a separately excited dc machine is found in Fig. 7.4a and is repeated in Fig. 11.1. The method is, of course, also applicable to compound motors. The shunt field current can be adjusted by means of a variable resistance in series with the shunt field. Alternatively, the field current can be supplied by power-electronic circuits which can be used to rapidly change the field current in response to a wide variety of control signals.

Figure 11.2a shows in schematic form a switching scheme for pulse-width modulation of the field voltage. This system closely resembles the pulse-width modulation system discussed in Section 10.3.2. It consists of a rectifier which rectifies the ac input voltage, a dc-link capacitor which filters the rectified voltage, producing a dc voltage V_{dc} , and a pulse-width modulator.

In this system, because only a unidirectional field current is required, the pulse-width modulator consists of a single switch and a free-wheeling diode rather than the more complex four-switch arrangement of Fig. 10.45. Assuming both the switch and diode to be ideal, the average voltage across the field winding will be equal to

$$V_f = DV_{dc} \quad (11.1)$$

where D is the duty cycle of the switching waveform; i.e., D is the fraction of time that the switch S is on.

Figure 11.2b shows the resultant field current. Because in the steady-state the average voltage across the inductor must equal zero, the average field current I_f will thus be equal to

$$I_f = \frac{V_f}{R_f} = D \left(\frac{V_{dc}}{R_f} \right) \quad (11.2)$$

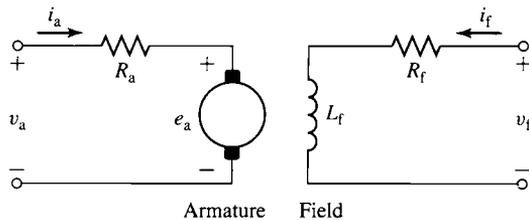


Figure 11.1 Equivalent circuit for a separately excited dc motor.

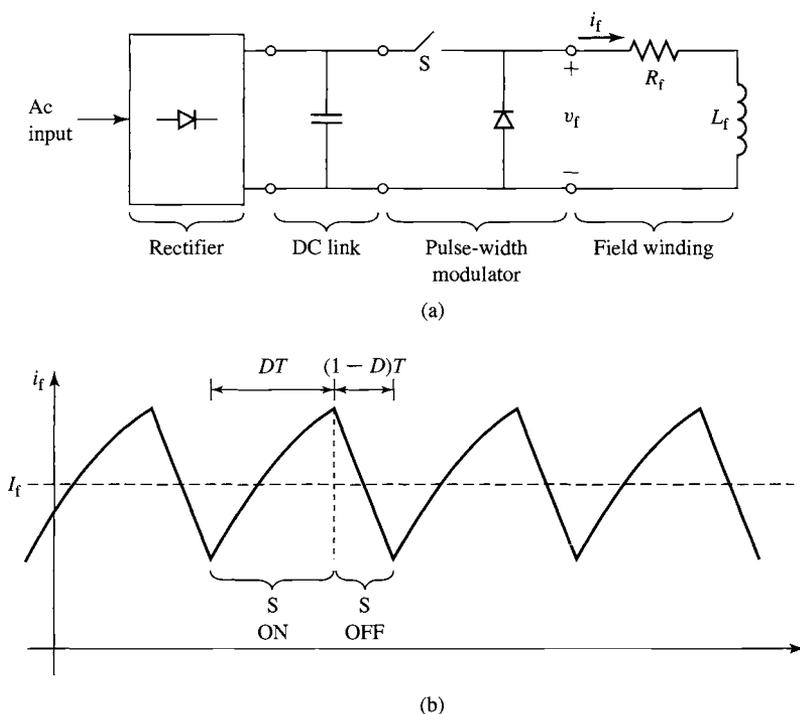


Figure 11.2 (a) Pulse-width modulation system for a dc-machine field winding. (b) Field-current waveform.

Thus, the field current can be controlled simply by controlling the duty cycle of the pulse-width modulator. If the field-winding time constant L_f/R_f is long compared to the switching time, the ripple current Δi_f will be small compared to the average current I_f .

EXAMPLE 11.1

A 25-kW, 3600 r/min, 240-V dc motor has an armature resistance of 47 m Ω and a shunt-field with a resistance of 187 Ω and an inductance of 4.2 H. Calculate (a) the average field current I_f and (b) the magnitude of the current ripple Δi_f when the field winding is supplied from a 240 V dc source by pulse-width modulation with a duty cycle $D = 0.75$ and a switching period of 1 msec.

■ Solution

a. The average field current is readily found from Eq. 11.2

$$I_f = D \left(\frac{V_{dc}}{R_f} \right) = 0.75 \left(\frac{240}{187} \right) = 0.96 \text{ A}$$

- b. The field time constant $\tau = L_f/R_f = 22.5$ msec is much longer than the switching period of 1 msec. Thus, the ripple current can be calculated using Eq. 10.32 as

$$\begin{aligned}\Delta i_f &= \left(\frac{2V_{dc}}{R_f}\right) \left(\frac{T}{\tau}\right) D(1-D) \\ &= \left(\frac{2 \times 240}{187}\right) \left(\frac{1}{22.5}\right) 0.75 \times (1 - 0.75) \\ &= 21.4 \text{ mA}\end{aligned}$$

Practice Problem 11.1

The duty-cycle D of the dc-motor controller of Example 11.1 is suddenly switched from 0.75 to 1.0. Calculate (a) the resultant steady-state field current and (b) the time constant for the change from the initial value of 1.08 A to the new final value.

Solution

- 1.28 A
- 22.5 msec

To examine the effect of field-current control, let us begin with the case of a dc motor driving a load of constant torque T_{load} . From Eqs. 7.9 and 7.14, the generated voltage of a dc motor can be written as

$$E_a = K_f I_f \omega_m \quad (11.3)$$

where I_f is the average field current, ω_m is the angular velocity in rad/sec, and $K_f = K_a \mathcal{P}_d N_f$ is a geometric constant which depends upon the dimensions of the motor, the properties of the magnetic material used to construct the motor, as well as the number of turns in the field winding. Note that strictly speaking, K_f is not constant since it is proportional to the direct-axis permeance, which typically varies as the flux-level in the motor increases to the point that the effects of magnetic saturation become significant.

The electromagnetic torque is given by Eq. 7.16 as

$$T_{mech} = \frac{E_a I_a}{\omega_m} = K_f I_f I_a \quad (11.4)$$

and the armature current can be seen from the equivalent circuit of Fig. 11.1 to be given by

$$I_a = \frac{(V_a - E_a)}{R_a} \quad (11.5)$$

Setting the motor torque equal to T_{load} , Eqs. 11.3 through 11.5 can be solved for ω_m

$$\omega_m = \frac{(V_a - I_a R_a)}{K_f I_f} = \frac{\left(V_a - \frac{T_{\text{load}} R_a}{K_f I_f}\right)}{K_f I_f} \quad (11.6)$$

From Eq. 11.6, recognizing that the armature resistance voltage drop $I_a R_a$ is generally quite small in comparison to the armature voltage V_a , we see that for a given load torque, the motor speed will increase with decreasing field current and decrease as the field current is increased. The lowest speed obtainable is that corresponding to maximum field current (the field current is limited by heating considerations); the highest speed is limited mechanically by the mechanical integrity of the rotor and electrically by the effects of armature reaction under weak-field conditions giving rise to poor commutation.

Armature current is typically limited by motor cooling capability. In many dc motors, cooling is aided by a shaft-driven fan whose cooling capacity is a function of motor speed. To examine in an approximate fashion the limitations on the allowable continuous motor output as the speed is changed, we will neglect the influence of changing ventilation and assume that the armature current I_a cannot exceed its rated value, in order to insure that the motor will not overheat. In addition, in our approximate argument we will neglect the effect of rotational losses (which of course also change with motor speed). Because the voltage drop across the armature resistance is relatively small, the speed voltage E_a will remain essentially constant at a value slightly below the applied armature voltage; any change in field current will be compensated for by a change in motor speed.

Thus under constant-terminal-voltage operation with varying field current, the $E_a I_a$ product, and hence the allowable motor output power, remain substantially constant as the speed is varied. A dc motor controlled in this fashion is referred to as a *constant-power drive*. Torque, however, varies directly with field flux and therefore has its highest allowable value at the highest field current and hence lowest speed. Field-current control is thus best suited to drives requiring increased torque at low speeds. When a motor so controlled is used with a load requiring constant torque over the speed range, the rating and size of the machine are determined by the product of the torque and the highest speed. Such a drive is inherently oversized at the lower speeds, which is the principal economic factor limiting the practical speed range of large motors.

EXAMPLE 11.2

With an armature terminal voltage of 240 V and with a shunt-field current of 0.34 A, the no-load speed of the dc motor of Example 11.1 is found to be 3600 r/min. In this example, the motor is assumed to be driving a load which varies with speed as

$$P_{\text{load}} = 22.4 \left(\frac{n}{3600}\right)^3 \text{ kW}$$

where n is the motor speed in r/min. A rheostat is to be installed in series with the shunt field to vary the speed. Assuming the armature terminal voltage to remain constant at 240 V, calculate

the required resistance range if the speed is to be varied between 1800 and 3600 r/min. The effect of rotational losses can be ignored.

■ Solution

The load torque is equal to the load power divided by the motor speed ω_m expressed in rad/sec. First expressing the power in terms of ω_m

$$P_{\text{load}} = 22.4 \left(\frac{\omega_m}{120\pi} \right)^3 \text{ kW}$$

The load torque is then given by

$$T_{\text{load}} = \frac{P_{\text{load}}}{\omega_m} = 22.4 \left(\frac{\omega_m^2}{(120\pi)^3} \right) = 4.18 \times 10^{-4} \omega_m^2 \text{ N} \cdot \text{m}$$

Thus, at 1800 r/min, $\omega_m = 60\pi$ and $T_{\text{load}} = 14.9 \text{ N} \cdot \text{m}$. At 3600 r/min, $\omega_m = 120\pi$ and $T_{\text{load}} = 59.4 \text{ N} \cdot \text{m}$.

Before solving for I_f , we must find the value of K_f , which can be found from the no-load data. Specifically, we see that with a terminal voltage of 240-V and at a no-load speed of 3600 r/min ($\omega_m = 120\pi$), the corresponding field current is 0.34 A. Since under no-load conditions $E_a \approx V_a$, we can find K_f from Eq. 11.3 as

$$K_f = \frac{E_a}{I_f \omega_m} = \frac{240}{0.34 \times 120\pi} = 1.87 \text{ V}/(\text{A} \cdot \text{rad}/\text{sec})$$

To find the required field current, we can solve Eq. 11.6 for I_f

$$I_f = \frac{V_a}{2K_f \omega_m} \left(1 \pm \sqrt{1 - \frac{4\omega_m T_{\text{load}} R_a}{V_a^2}} \right)$$

Recognizing that R_a is small and hence that $I_f \approx V_a / (K_f \omega_m)$ we see that the positive sign should be used and thus

$$I_f = \frac{V_a}{2K_f \omega_m} \left(1 + \sqrt{1 - \frac{4\omega_m T_{\text{load}} R_a}{V_a^2}} \right)$$

Once the field current has been found, the total field resistance can be found as

$$(R_f)_{\text{total}} = \frac{V_a}{I_f} = \frac{240}{I_f}$$

and the required added rheostat resistance can be found by subtracting the resistance of the shunt-field winding (187 Ω) from $(R_f)_{\text{total}}$.

This leads to the following table:

r/min	T_{load} [N · m]	I_f [A]	$(R_f)_{\text{total}}$ [Ω]	R_{rheostat} [Ω]
1800	14.9	0.678	354	167
3600	59.4	0.334	719	532

Thus, the rheostat must be able to cover the range from 166 Ω to 532 Ω .

Practice Problem 11.2

The rheostat of Example 11.2 is to be replaced by a duty cycle controller operating from the 240-V dc supply. Calculate the duty-cycle range required to achieve operation over the speed range of 1800–3600 r/min as specified in Example 11.2.

Solution

$$0.26 \leq D \leq 0.53$$

Armature-Circuit Resistance Control *Armature-circuit resistance control* provides a means of obtaining reduced speed by the insertion of external series resistance in the armature circuit. It can be used with series, shunt, and compound motors; for the last two types, the series resistor must be connected between the shunt field and the armature, not between the line and the motor. It is a common method of speed control for series motors and is generally analogous in action to wound-rotor-induction-motor control by the addition of external series rotor resistance.

Depending upon the value of the series armature resistance, the speed may vary significantly with load, since the speed depends on the voltage drop in this resistance and hence on the armature current demanded by the load. For example, a 1200-r/min shunt motor whose speed under load is reduced to 750 r/min by series armature resistance will return to almost 1200-r/min operation if the load is removed because the no-load current produces a voltage drop across the series resistance which is insignificant. The disadvantage of poor speed regulation may not be important in a series motor, which is used only where varying-speed service is required or can be tolerated.

A significant disadvantage of this method of speed control is that the power loss in the external resistor is large, especially when the speed is greatly reduced. In fact, for a constant-torque load, the power input to the motor plus resistor remains constant, while the power output to the load decreases in proportion to the speed. Operating costs are therefore comparatively high for lengthy operation at reduced speeds. Because of its low initial cost however, the series-resistance method (or the variation of it discussed in the next paragraph) will often be attractive economically for applications which require only short-time or intermittent speed reduction. Unlike field-current control, armature-resistance control results in a *constant-torque drive* because both the field-flux and, to a first approximation, the allowable armature current remain constant as speed changes.

A variation of this control scheme is given by the *shunted-armature method*, which may be applied to a series motor, as in Fig. 11.3a, or a shunt motor, as in Fig. 11.3b. In effect, resistors R_1 and R_2 act as a voltage divider applying a reduced voltage to the armature. Greater flexibility is possible because two resistors can now be adjusted to provide the desired performance. For series motors, the no-load speed can be adjusted to a finite, reasonable value, and the scheme is therefore applicable to the production of slow speeds at light loads. For shunt motors, the speed regulation in the low-speed range is appreciably improved because the no-load speed is definitely lower than the value with no controlling resistors.

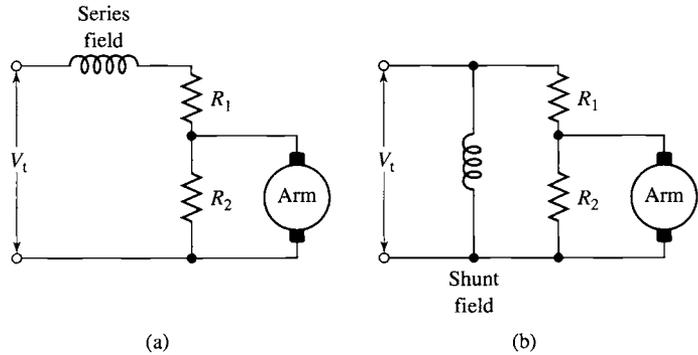


Figure 11.3 Shunted-armature method of speed control applied to (a) a series motor and (b) a shunt motor.

Armature-Terminal Voltage Control *Armature-terminal voltage control* can be readily accomplished with the use of power-electronic systems such as those discussed in Chapter 10. Figure 11.4 shows in somewhat schematic form three possible configurations. In Fig. 11.4a, a phase-controlled rectifier in combination with a dc-link filter capacitor can be used to produce a variable dc-link voltage which can be applied directly to the armature terminals of the dc motor.

In Fig. 11.4b, a constant dc-link voltage is produced by a diode rectifier in combination with a dc-link filter capacitor. The armature terminal voltage is then varied by a pulse-width modulation scheme in which switch S is alternately opened and closed. When switch S is closed, the armature voltage is equal to the dc-link voltage V_{dc} , and when the switch is opened, current transfers to the freewheeling diode, essentially setting the armature voltage to zero. Thus the average armature voltage under this condition is equal to

$$V_a = DV_{dc} \quad (11.7)$$

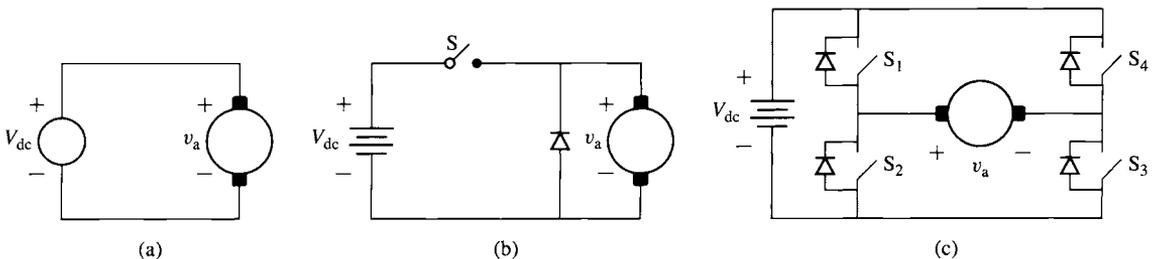


Figure 11.4 Three typical configurations for armature-voltage control. (a) Variable dc-link voltage (produced by a phase-controlled rectifier) applied directly to the dc-motor armature terminals. (b) Constant dc-link voltage with single-polarity pulse-width modulation. (c) Constant dc-link voltage with a full H-bridge.

where

V_a = average armature voltage (V)

V_{dc} = dc-link voltage (V)

D = PWM duty cycle (fraction of time that switch S is closed)

Figure 11.4c shows an H-bridge configuration as is discussed in the context of inverters in Section 10.3.3. Note that if switch S3 is held closed while switch S4 remains open, this configuration reduces to that of Fig. 11.4b. However, the H-bridge configuration is more flexible because it can produce both positive- and negative-polarity armature voltage. For example, with switches S1 and S3 closed, the armature voltage is equal to V_{dc} while with switches S2 and S4 closed, the armature voltage is equal to $-V_{dc}$. Clearly, using such an H-bridge configuration in combination with an appropriate choice of control signals to the switches allows this PWM system to achieve any desired armature voltage in the range $-V_{dc} \leq V_a \leq V_{dc}$.

Armature-voltage control takes advantage of the fact that, because the voltage drop across the armature resistance is relatively small, a change in the armature terminal voltage of a shunt motor is accompanied in the steady state by a substantially equal change in the speed voltage. With constant shunt field current and hence field flux, this change in speed voltage must be accompanied by a proportional change in motor speed. Thus, motor speed can be controlled directly by means of the armature terminal voltage.

EXAMPLE 11.3

A 500-V, 100-hp, 2500 r/min, separately excited dc motor has the following parameters:

Field resistance:	$R_f = 109 \Omega$
Rated field voltage:	$V_{f0} = 300 \text{ V}$
Armature resistance:	$R_a = 0.084 \Omega$
Geometric constant:	$K_f = 0.694 \text{ V}/(\text{A} \cdot \text{rad}/\text{sec})$



Assuming the field voltage to be held constant at 300 V, use MATLAB[†] to plot the motor speed as a function of armature voltage with the motor operating under no-load and also under rated full-load torque as the armature voltage is varied from 250 V to 500 V.

■ Solution

From Eq. 11.4

$$I_a = \frac{T_{\text{mech}}}{K_f I_f}$$

and from Eq. 11.5

$$I_a = \frac{V_a - E_a}{R_a} = \frac{V_a - K_f I_f \omega_m}{R_a}$$

[†] MATLAB is a registered trademark of The MathWorks, Inc.

Hence we can solve for ω_m

$$\omega_m = \frac{V_a - \left(\frac{T_{\text{mech}} R_a}{K_f I_f} \right)}{K_f I_f}$$

and the speed in r/min as

$$n = \left(\frac{30}{\pi} \right) \omega_m$$

Finally, the field current is

$$I_f = \frac{V_f}{R_f} = \frac{300}{109} = 2.75 \text{ A}$$

and the rated full-load torque is given by

$$T_{\text{rated}} = \frac{P_{\text{rated}}}{(\omega_m)_{\text{rated}}} = \frac{100 \times 746}{2500 \times \left(\frac{\pi}{30} \right)} = 285 \text{ N} \cdot \text{m}$$

Figure 11.5 is the desired plot. Notice that the speed drops approximately 63 r/min as the torque is increased from zero to full-load, independent of the armature voltage and machine speed.

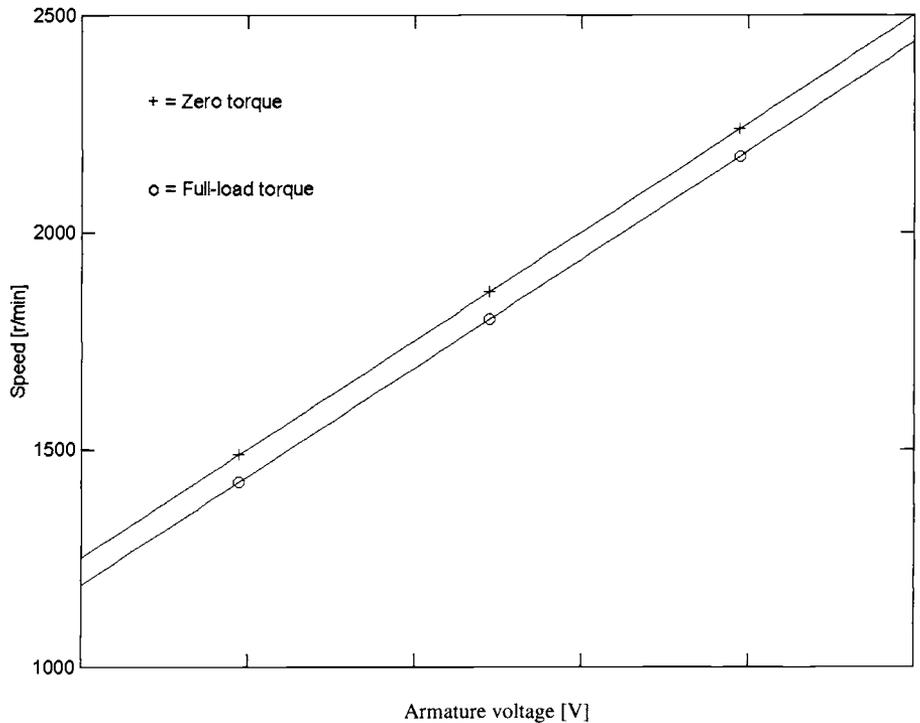


Figure 11.5 A plot of speed versus armature voltage for the dc motor of Example 11.3.

Here is the MATLAB script:

```

clc
clear

% Motor parameters
Rf = 109;
Ra = 0.084;
Kf = 0.694;

% Constant field voltage
Vf = 300;

% Resulting field current
If = Vf/Rf;

% Rated speed in rad/sec
omegarated = 2500*(pi/30);

% Rated power in Watts
Prated = 100*746;

% Rated torque in N-m
Trated = Prated/omegarated;

% Vary the armature voltage from 250 to 500 V
% and calculate speed.

for n=1:101
    Va(n) = 250 * (1 + (n-1)/100);

    % Zero torque
    T = 0;
    omega = (Va(n) - T*Ra/(Kf*If))/(Kf*If);
    NoLoadRPM(n) = omega*30/pi;

    % Full-load torque
    T = Trated;
    omega = (Va(n) - T*Ra/(Kf*If))/(Kf*If);
    FullLoadRPM(n) = omega*30/pi;
end

plot(Va,NoLoadRPM)
hold
plot(Va(20),NoLoadRPM(20),'+')
plot(Va(50),NoLoadRPM(50),'+')
plot(Va(80),NoLoadRPM(80),'+')
plot(Va,FullLoadRPM)
plot(Va(20),FullLoadRPM(20),'o')
plot(Va(50),FullLoadRPM(50),'o')
plot(Va(80),FullLoadRPM(80),'o')
hold
xlabel('Armature voltage [V]')

```

```
ylabel('Speed [r/min]')
text(270,2300,'+ = Zero torque')
text(270,2100,'o = Full-load torque')
```

Practice Problem 11.3

Calculate the change in armature voltage required to maintain the motor of Example 11.3 at a speed of 2000 r/min as the load is changed from zero to full-load torque.

Solution

12.5 V

Frequently the control of motor voltage is combined with field-current control in order to achieve the widest possible speed range. With such dual control, base speed can be defined as the normal-armature-voltage, full-field speed of the motor. Speeds above base speed are obtained by reducing the field current; speeds below base speed are obtained by armature-voltage control. As discussed in connection with field-current control, the range above base speed is that of a constant-power drive. The range below base speed is that of a constant-torque drive because, as in armature-resistance control, the flux and the allowable armature current remain approximately constant. The overall output limitations are therefore as shown in Fig. 11.6a for approximate allowable torque and in Fig. 11.6b for approximate allowable power. The constant-torque characteristic is well suited to many applications in the machine-tool industry, where many loads consist largely of overcoming the friction of moving parts and hence have essentially constant torque requirements.

The speed regulation and the limitations on the speed range above base speed are those already presented with reference to field-current control; the maximum speed thus does not ordinarily exceed four times base speed and preferably not twice base speed. For conventional machines, the lower limit for reliable and stable operation is

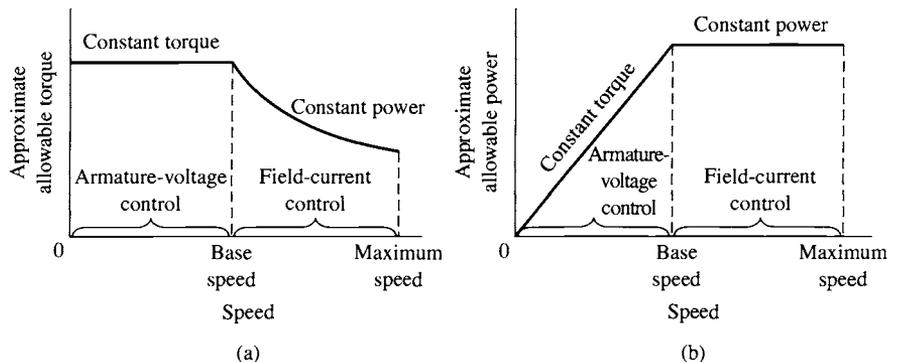


Figure 11.6 (a) Torque and (b) power limitations of combined armature-voltage and field-current methods of speed control.

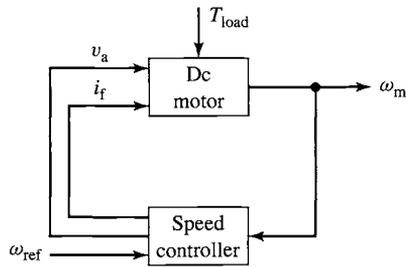


Figure 11.7 Block diagram for a speed-control system for a separately excited or shunt-connected dc motor.

about one-tenth of base speed, corresponding to a total maximum-to-minimum range not exceeding 40:1.

With armature reaction ignored, the decrease in speed from no-load to full-load torque is caused entirely by the full-load armature-resistance voltage drop in the dc generator and motor. This full-load armature-resistance voltage drop is constant over the voltage-control range, since full-load torque and hence full-load current are usually regarded as constant in that range. When measured in r/min, therefore, the speed decrease from no-load to full-load torque is a constant, independent of the no-load speed, as we saw in Example 11.3. The torque-speed curves accordingly are closely approximated by a series of parallel straight lines for the various motor-field adjustments. Note that a speed decrease of, say, 40 r/min from a no-load speed of 1200 r/min is often of little importance; a decrease of 40 r/min from a no-load speed of 120 r/min, however, may at times be of critical importance and require corrective steps in the layout of the system.

Figure 11.7 shows a block diagram of a feedback-control system that can be used to regulate the speed of a separately excited or shunt-connected dc motor. The inputs to the dc-motor block include the armature voltage and the field current as well as the load torque T_{load} . The resultant motor speed ω_m is fed back to a controller block which represents both the control logic and power electronics and which controls the armature voltage and field current applied to the dc motor, based upon a reference speed signal ω_{ref} . Depending upon the design of the controller, with such a scheme it is possible to control the steady-state motor speed to a high degree of accuracy independent of the variations in the load torque.

EXAMPLE 11.4

Figure 11.8 shows the block diagram for a simple speed control system to be applied to the dc motor of Example 11.3. In this controller, the field voltage is held constant (not shown) at its rated value of 300 V. Thus, the control is applied only to the armature voltage and takes the form

$$V_a = V_{a0} + G(\omega_{ref} - \omega_m)$$

where V_{a0} is the armature voltage when $\omega_m = \omega_{ref}$ and G is a multiplicative constant.

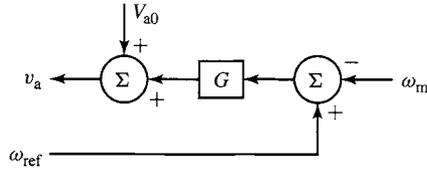


Figure 11.8 Simple dc-motor speed controller for Example 11.4.

With the reference speed set to 2000 r/min ($\omega_{ref} = 2000 \times \pi/30$), calculate V_{a0} and G so that the motor speed is 2000 r/min at no load and drops only by 25 r/min when the torque is increased to its rated full-load value.

■ Solution

As was found in Example 11.3, the field current under this condition will be 2.75 A. At no load, 2000 r/min,

$$V_a \approx E_a = K_f I_f \omega_m = 0.694 \times 2.75 \times 2000 \left(\frac{\pi}{30} \right) = 400 \text{ V}$$

and thus $V_{a0} = 400 \text{ V}$.

The full load torque was found in Example 11.3 to be $T_{rated} = 285 \text{ N}\cdot\text{m}$ and thus the armature current required to achieve rated full-load torque can be found from Eq. 11.4

$$I_a = \frac{T_{rated}}{K_f I_f} = \frac{285}{0.694 \times 2.75} = 149 \text{ A}$$

At a speed of 1975 r/min, E_a will be given by

$$E_a = K_f I_f \omega_m = 0.694 \times 2.75 \times 1975 \left(\frac{\pi}{30} \right) = 395 \text{ V}$$

and thus

$$V_a = E_a + I_a R_a = 395 + 149 \times 0.084 = 408 \text{ V}$$

Solving for G gives

$$G = \frac{V_a - V_{a0}}{\omega_{ref} - \omega_m} = \frac{408 - 400}{(2000 - 1975) \left(\frac{\pi}{30} \right)} = 3.06 \text{ V} \cdot \text{sec/rad}$$

Practice Problem 11.4

If the load torque in Example 11.4 is equal to half of the rated full-load torque, calculate (a) the speed of the motor and (b) the corresponding load power.

Solution

- 1988 r/min
- 29.6 kW

In the case of permanent-magnet dc motors, the field flux is, of course, fixed by the permanent magnet (with the possible exception of any effects of temperature changes on the magnet properties as the motor heats up). From Eqs. 11.3 and 11.4, we see that the voltage generated voltage can be written in the form

$$E_a = K_m \omega_m \quad (11.8)$$

and that the electromagnetic torque can be written as

$$T_{\text{mech}} = K_m I_a \quad (11.9)$$

Comparison of Eqs. 11.8 and 11.9 with Eqs. 11.3 and 11.4 show that the analysis of a permanent-magnet dc motor is identical to that of a shunt or separately excited dc motor with the exception that the torque-constant K_m must be substituted for the term $K_f I_f$.

EXAMPLE 11.5

The permanent-magnet dc motor of Example 7.9 has an armature resistance of $1.03 \, \Omega$ and a torque constant $K_m = 0.22 \, \text{V}/(\text{rad}/\text{sec})$. Assume the motor to be driving a constant power load of $800 \, \text{W}$ (including rotational losses), and calculate the motor speed as the armature voltage is varied from 40 to $50 \, \text{V}$.

■ Solution

The motor power output (including rotational losses) is given by the product $E_a I_a$ and thus we can write

$$P_{\text{load}} = E_a I_a = K_m \omega_m I_a$$

Solving for ω_m gives

$$\omega_m = \frac{P_{\text{load}}}{K_m I_a}$$

The armature current can be written as

$$I_a = \frac{(V_a - E_a)}{R_a} = \frac{(V_a - K_m \omega_m)}{R_a}$$

These two equations can be combined to give an equation for ω_m of the form

$$\omega_m^2 - \left(\frac{V_a}{K_m} \right) \omega_m + \frac{P_{\text{load}} R_a}{K_m^2} = 0$$

from which we can find

$$\omega_m = \frac{V_a}{2K_m} \left[1 \pm \sqrt{1 - \frac{4P_{\text{load}} R_a}{V_a^2}} \right]$$

Recognizing that, if the voltage drop across the armature resistance is small, $V_a \approx E_a = K_m \omega_m$, we pick the positive sign and thus

$$\omega_m = \frac{V_a}{2K_m} \left[1 + \sqrt{1 - \frac{4P_{\text{load}} R_a}{V_a^2}} \right]$$

Substituting values, we find that for $V_a = 40$ V, $\omega_m = 169.2$ rad/sec (1616 r/min) and for $V_a = 50$ V, $\omega_m = 217.5$ rad/sec (2077 r/min).

Practice Problem 11.5

Calculate the speed variation (in r/min) of the permanent-magnet dc motor of Example 11.5 if the armature voltage is held constant at 50 V and the load power varies from 100 W to 500 W.

Solution

2077 r/min to 1540 r/min

11.1.2 Torque Control

As we have seen, the electromagnetic torque of a dc motor is proportional to the armature current I_a and is given by

$$T_{\text{mech}} = K_f I_f I_a \quad (11.10)$$

in the case of a separately excited or shunt motor and

$$T_{\text{mech}} = K_m I_a \quad (11.11)$$

in the case of a permanent-magnet motor.

From these equations we see that torque can be controlled directly by controlling the armature current. Fig. 11.9 shows three possible configurations. In Fig. 11.9a, a phase-controlled rectifier, in combination with a dc-link filter inductor, can be used to create a variable dc-link current which can be applied directly to the armature terminals of the dc motor.

In Fig. 11.9b, a constant dc-link current is produced by a diode rectifier. The armature terminal voltage is then varied by a pulse-width modulation scheme in which switch S is alternately opened and closed. When switch S is opened, the current I_{dc} flows into the dc-motor armature while when switch S is closed, the armature is

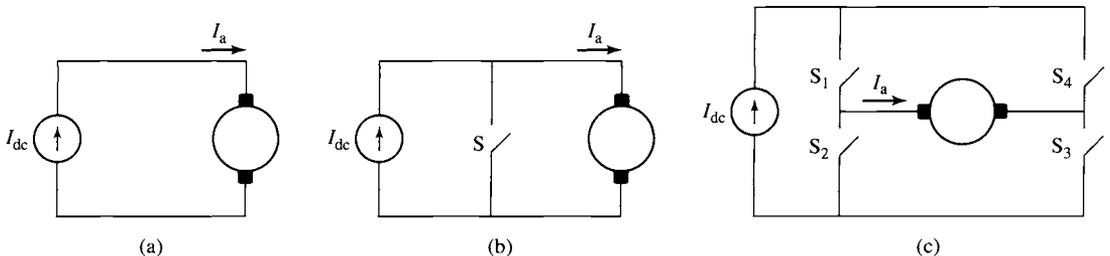


Figure 11.9 Three typical configurations for armature-current control. (a) Variable dc-link current (produced by a phase-controlled rectifier) applied directly to the dc-motor armature terminals. (b) Constant dc-link current with single-polarity pulse-width modulation. (c) Constant dc-link current with a full H-bridge.

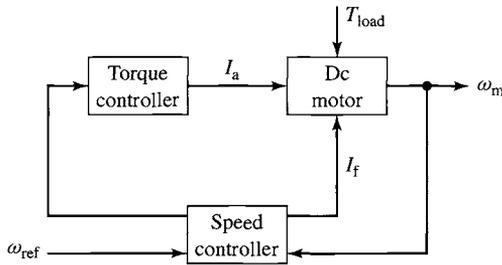


Figure 11.10 Block diagram of a dc-motor speed-control system using direct-control of motor torque.

shorted and I_a decays. Thus, the duty cycle of switch S will control the average current into the armature.

Finally, Fig 11.9c shows an H-bridge configuration as is discussed in the context of inverters in Section 10.3.2. Appropriate control of the four switches S1 through S4 allows this PWM system to achieve any desired armature average current in the range $-I_{dc} \leq I_a \leq I_{dc}$.

Note that in each of the PWM configurations of Fig. 11.9b and c, rapid changes in instantaneous current through the dc machine armature can give rise to large voltage spikes, which can damage the machine insulation as well as give rise to flashover and voltage breakdown of the commutator. In order to eliminate these effects, a practical system must include some sort of filter across the armature terminals (such as a large capacitor) to limit the voltage rise and to provide a low-impedance path for the high-frequency components of the drive current.

Figure 11.10 shows a typical configuration in which the torque control is surrounded by a speed-feedback loop. This looks similar to the speed control of Fig. 11.7. However, instead of controlling the armature voltage, in this case the output of the speed controller is a torque reference signal T_{ref} which in turn serves as the input to the torque controller. One advantage of such a system is that it automatically limits the dc-motor armature current to acceptable levels under all operating conditions, as is shown in Example 11.6.

EXAMPLE 11.6

Consider the 100-hp dc motor of Examples 11.3 and 11.4 to be driving a load whose torque varies linearly with speed such that it equals rated full-load torque (285 N·m) at a speed of 2500 r/min. We will assume the combined moment of inertia of the motor and load to equal $0.92 \text{ kg}\cdot\text{m}^2$. The field voltage is to be held constant at 300 V.

- Calculate the armature voltage and current required to achieve speeds of 2000 and 2500 r/min.
- Assume that the motor is operated from an armature-voltage controller and that the armature voltage is suddenly switched from its 2000 r/min to its 2500 r/min value. Calculate the resultant motor speed and armature current as a function of time.

- c. Assume that the motor is operated from an armature-current controller and that the armature current is suddenly switched from its 2000 r/min to its 2500 r/min value. Calculate the resultant motor speed as a function of time.

■ Solution

- a. Neglecting any rotational losses, the armature current can be found from Eq. 11.4 by setting $T_{\text{mech}} = T_{\text{load}}$

$$I_a = \frac{T_{\text{load}}}{K_f I_f}$$

Substituting

$$T_{\text{load}} = \left(\frac{n}{n_f} \right) T_{f1}$$

where n is the motor speed in r/min, $n_f = 2500$ r/min and $T_{f1} = 285$ N·m gives

$$I_a = \frac{n T_{f1}}{n_f K_f I_f}$$

Solving for $V_a = E_a + I_a R_a$ then allows us to complete the following table:

r/min	ω_m [rad/sec]	V_a [V]	I_a [A]	T_{load} [N·m]
2000	209	410	119	228
2500	262	513	149	285

- b. The dynamic equation governing the speed of the motor is

$$J \frac{d\omega_m}{dt} = T_{\text{mech}} - T_{\text{load}}$$

Substituting $\omega_m = (\pi/30)n$ and $\omega_r = (\pi/30)n_f$ we can write

$$T_{\text{load}} = \left(\frac{T_{f1}}{\omega_r} \right) \omega_m$$

Under armature-voltage control,

$$\begin{aligned} T_{\text{mech}} &= K_f I_f I_a = K_f I_f \left(\frac{V_a - E_a}{R_a} \right) \\ &= K_f I_f \left(\frac{V_a - K_f I_f \omega_m}{R_a} \right) \end{aligned}$$

and thus the governing differential equation is

$$J \frac{d\omega_m}{dt} = K_f I_f \left(\frac{V_a - K_f I_f \omega_m}{R_a} \right) - \left(\frac{T_{f1}}{\omega_r} \right) \omega_m$$

or

$$\begin{aligned} \frac{d\omega_m}{dt} + \frac{1}{J} \left(\frac{T_{f1}}{\omega_r} + \frac{(K_f I_f)^2}{R_a} \right) \omega_m - \frac{K_f I_f V_a}{J R_a} \\ = \frac{d\omega_m}{dt} + 48.4 \omega_m - 24.7 V_a = 0 \end{aligned}$$

From this differential equation, we see that with the motor initially at $\omega_m = \omega_i = 209$ rad/sec, if the armature voltage V_a is suddenly switched from $V_i = 413$ V to $V_f = 513$ V, the speed will rise exponentially to $\omega_m = \omega_f = 262$ rad/sec as

$$\begin{aligned}\omega_m &= \omega_f + (\omega_i - \omega_f)e^{-t/\tau} \\ &= 262 - 53e^{-t/\tau} \text{ rad/sec}\end{aligned}$$

where $\tau = 1/48.4 = 20.7$ msec. Expressed in terms of r/min

$$n = 2500 - 50e^{-t/\tau} \text{ r/min}$$

The armature current will decrease exponentially with the same 20.7 msec time constant from an initial value of $(V_f - V_i)/R_a = 1190$ A to its final value of 149 A. Thus,

$$I_a = 149 + 1041e^{-t/\tau} \text{ A}$$

Notice that it is unlikely that the supply to the dc motor can supply this large initial current (eight times the rated full-load armature current) and, in addition, the high current and corresponding high torque could potentially cause damage to the dc motor commutator, brushes, and armature winding. Hence, as a practical matter, a practical controller would undoubtedly limit the rate of change of the armature voltage to avoid such sudden steps in voltage, with the result that the speed change would not occur as rapidly as calculated here.

- c. The dynamic equation governing the speed of the motor remains the same as that in part (b) as does the equation for the load torque. However, in this case, because the motor is being operated from a current controller, the electromagnetic torque will remain constant at $T_{\text{mech}} = T_f = 285$ N·m after the current is switched from its initial value of 119 A to its final value of 149 A.

Thus

$$J \frac{d\omega_m}{dt} = T_{\text{mech}} - T_{\text{load}} = T_f - \left(\frac{T_f}{\omega_f} \right) \omega_m$$

or

$$\begin{aligned}\frac{d\omega_m}{dt} + \left(\frac{T_f}{J\omega_f} \right) \omega_m - \frac{T_f}{J} \\ = \frac{d\omega_m}{dt} + 1.18\omega_m - 310 = 0\end{aligned}$$

In this case, the speed will rise exponentially to $\omega_m = \omega_f = 262$ rad/sec as

$$\begin{aligned}\omega_m &= \omega_f + (\omega_i - \omega_f)e^{-t/\tau} \\ &= 262 - 53e^{-t/\tau} \text{ rad/sec}\end{aligned}$$

where now the time constant $\tau = 1/1.18 = 845$ msec.

Clearly, the change in motor speed under the current controller is much slower. However, at no point during this transient do either the motor current or the motor torque exceed their rated value. In addition, should faster response be desired, the armature current (and hence motor torque) could be set temporarily to a fixed value higher than the rated value (e.g., two or three times rated as compared to the factor of 8 found in part (b)), thus limiting the potential for damage to the motor.

Practice Problem 11.6

Consider the dc motor/load combination of Example 11.6 operating under current (torque) control to be operating in the steady-state at a speed of 2000 r/min at an armature current of 119 A. If the armature current is suddenly switched to 250 A, calculate the time required for the motor to reach a speed of 2500 r/min.

Solution

0.22 sec

11.2 CONTROL OF SYNCHRONOUS MOTORS

11.2.1 Speed Control

As discussed in Chapters 4 and 5, synchronous motors are essentially constant-speed machines, with their speed being determined by the frequency of the armature currents as described by Eqs. 4.40 and 4.41. Specifically, Eq. 4.40 shows that the synchronous angular velocity is proportional to the electrical frequency of the applied armature voltage and inversely proportional to the number of poles in the machine

$$\omega_s = \left(\frac{2}{\text{poles}} \right) \omega_e \quad (11.12)$$

where

ω_s = synchronous spatial angular velocity of the air-gap mmf wave [rad/sec]

$\omega_e = 2\pi f_e$ = angular frequency of the applied electrical excitation [rad/sec]

f_e = applied electrical frequency [Hz]

Clearly, the simplest means of synchronous motor control is speed control via control of the frequency of the applied armature voltage, driving the motor by a polyphase voltage-source inverter such as the three-phase inverter shown in Fig. 11.11. As is discussed in Section 10.3.3, this inverter can either be used to supply stepped ac voltage waveforms of amplitude V_{dc} or the switches can be controlled to produce pulse-width-modulated ac voltage waveforms of variable amplitude. The dc-link voltage V_{dc} can itself be varied, for example, through the use of a phase-controlled rectifier.

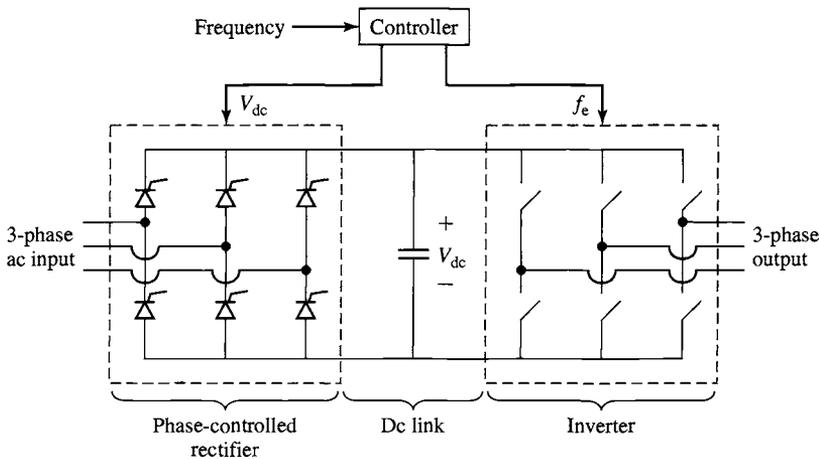


Figure 11.11 Three-phase voltage-source inverter.

The frequency of the inverter output waveforms can of course be varied by controlling the switching frequency of the inverter switches. For ac-machine applications, coupled with this frequency control must be control of the amplitude of the applied voltage, as we will now see.

From Faraday's Law, we know that the air-gap component of the armature voltage in an ac machine is proportional to the peak flux density in the machine and the electrical frequency. Thus, if we neglect the voltage drop across the armature resistance and leakage reactance, we can write

$$V_a = \left(\frac{f_e}{f_{\text{rated}}} \right) \left(\frac{B_{\text{peak}}}{B_{\text{rated}}} \right) V_{\text{rated}} \quad (11.13)$$

where V_a is the amplitude of the armature voltage, f_e is the operating frequency, and B_{peak} is the peak air-gap flux density. V_{rated} , f_{rated} , and B_{rated} are the corresponding rated-operating-point values.

Consider a situation in which the frequency of the armature voltage is varied while its amplitude is maintained at its rated value ($V_a = V_{\text{rated}}$). Under these conditions, from Eq. 11.13 we see that

$$B_{\text{peak}} = \left(\frac{f_{\text{rated}}}{f_e} \right) B_{\text{rated}} \quad (11.14)$$

Equation 11.14 clearly demonstrates the problem with constant-voltage, variable-frequency operation. Specifically, for a given armature voltage, the machine flux density is inversely proportional to frequency and thus as the frequency is reduced, the flux density will increase. Thus for a typical machine which operates in saturation at rated voltage and frequency, any reduction in frequency will further increase the flux density in the machine. In fact, a significant drop in frequency will increase

the flux density to the point of potential machine damage due both to increased core loss and to the increased machine currents required to support the higher flux density.

As a result, for frequencies less than or equal to rated frequency, it is typical to operate a machine at constant flux density. From Eq. 11.13, with $B_{\text{peak}} = B_{\text{rated}}$

$$V_a = \left(\frac{f_e}{f_{\text{rated}}} \right) V_{\text{rated}} \quad (11.15)$$

which can be rewritten as

$$\frac{V_a}{f_e} = \frac{V_{\text{rated}}}{f_{\text{rated}}} \quad (11.16)$$

From Eq. 11.16, we see that constant-flux operation can be achieved by maintaining a constant ratio of armature voltage to frequency. This is referred to as *constant-volts-per-hertz* (constant V/Hz) operation. It is typically maintained from rated frequency down to the low frequency at which the armature resistance voltage drop becomes a significant component of the applied voltage.

Similarly, we see from Eq. 11.13 that if the machine is operated at frequencies in excess of rated frequency with the voltage at its rated value, the air-gap flux density will drop below its rated value. Thus, in order to maintain the flux density at its rated value, it would be necessary to increase the terminal voltage for frequencies in excess of rated frequency. In order to avoid insulation damage, it is common to maintain the machine terminal voltage at its rated value for frequencies in excess of rated frequency.

The machine terminal current is typically limited by thermal constraints. Thus, provided the machine cooling is not affected by rotor speed, the maximum permissible terminal current will remain constant at its rated value I_{rated} , independent of the applied frequency. As a result, for frequencies below rated frequency, with V_a proportional to f_e , the maximum machine power will be proportional to $f_e V_{\text{rated}} I_{\text{rated}}$. The maximum torque under these conditions can be found by dividing the power by the rotor speed ω_s , which is also proportional to f_e as can be seen from Eq. 11.12. Thus, we see that the maximum torque is proportional to $V_{\text{rated}} I_{\text{rated}}$, and hence it is constant at its rated-operating-point value.

Similarly, for frequencies in excess of rated frequency, the maximum power will be constant and equal to $V_{\text{rated}} I_{\text{rated}}$. The corresponding maximum torque will then vary inversely with machine speed as $V_{\text{rated}} I_{\text{rated}} / \omega_s$. The maximum operating speed for this operating regime will be determined either by the maximum frequency which can be supplied by the drive electronics or by the maximum speed at which the rotor can be operated without risk of damage due to mechanical concerns such as excessive centrifugal force or to the presence of a resonance in the shaft system.

Figure 11.12 shows a plot of maximum power and maximum torque versus speed for a synchronous motor under variable-frequency operation. The operating regime below rated frequency and speed is referred to as the *constant-torque regime* and that above rated speed is referred to as the *constant-power regime*.

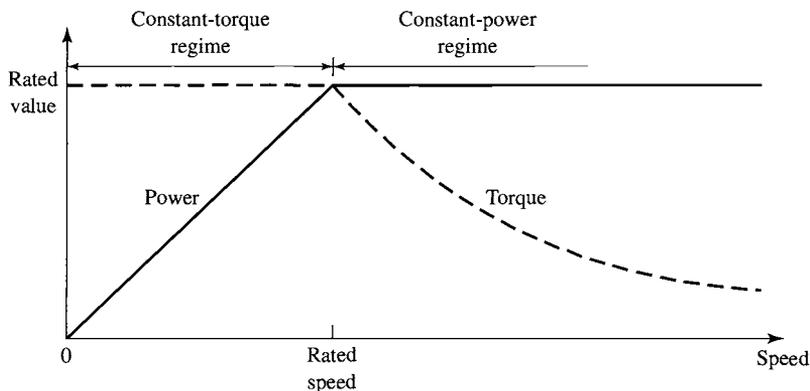


Figure 11.12 Variable-speed operating regimes for a synchronous motor.

EXAMPLE 11.7

The 45-kVA, 220-V, 60-Hz, six-pole, three-phase synchronous machine of Example 5.4 is to be operated as a motor and driven from a variable-frequency, three-phase voltage-source inverter which provides 220 V at 60 Hz and which maintains constant V/Hz as the frequency is reduced. The machine has a saturated synchronous reactance of 0.836 per unit and achieves rated open-circuit voltage at a field current of 2.84 A. For the purposes of this example, assume that the motor losses are negligible.

- With the motor operating at 60 Hz, 220 V and at rated power, unity power factor, calculate (i) the motor speed in r/min and (ii) the motor field current.
- If the inverter frequency is reduced to 50 Hz and the motor load adjusted to rated torque, calculate the (i) the resulting motor speed and (ii) the motor field current required to again achieve unity power factor.

■ Solution

- (i) The motor will operate at its synchronous speed which can be found from Eq. 4.41

$$n_s = \left(\frac{120}{\text{poles}} \right) f_e = \left(\frac{120}{6} \right) 60 = 1200 \text{ r/min}$$

- As seen in Chapter 5, the field current can be determined from the generated voltage. For motor operation,

$$\hat{E}_{af} = \hat{V}_a - jX_s \hat{I}_a = 1.0 - j0.836 \times 1.0 = 1.30 \angle -39.9^\circ$$

where V_a has been chosen as the reference phasor. Thus the field current is

$$I_f = 1.30 \times 2.84 = 3.69 \text{ A}$$

Note that we have chosen to solve for E_{af} in per unit. A solution in real units would have of course produced the same result.

- (i) When the frequency is reduced from 60 Hz to 50 Hz, the motor speed will drop from 1200 r/min to 1000 r/min.

(ii) Let us again consider the equation for the generated voltage

$$\hat{E}_{af} = \hat{V}_a - jX_s \hat{I}_a$$

where here we will assume that the equation is written in real units.

As the inverter frequency is reduced from 60 Hz, the inverter voltage will drop proportionally since the inverter maintains constant V/Hz. Thus we can write

$$V_a = \left(\frac{\omega_m}{\omega_{m0}} \right) V_{a0}$$

where the subscript 0 is used to indicate a 60-Hz value as found in part (a). Reactance is also proportional to frequency and thus

$$X_s = \left(\frac{\omega_m}{\omega_{m0}} \right) X_{s0}$$

The generated voltage is proportional to both the motor speed (and hence the frequency) and the field current, and thus we can write

$$E_{af} = \left(\frac{\omega_m}{\omega_{m0}} \right) \left(\frac{I_f}{I_{f0}} \right) E_{af0}$$

Finally, if we recognize that, to operate at rated torque and unity power factor under this reduced frequency condition, the motor armature current will have to be equal to the value found in part (a), i.e., $I_a = I_{a0}$, we can write the generated voltage equation as

$$\left(\frac{\omega_m}{\omega_{m0}} \right) \left(\frac{I_f}{I_{f0}} \right) \hat{E}_{af0} = \left(\frac{\omega_m}{\omega_{m0}} \right) \hat{V}_{a0} - j \left(\frac{\omega_m}{\omega_{m0}} \right) X_{s0} \hat{I}_{a0}$$

or

$$\left(\frac{I_f}{I_{f0}} \right) \hat{E}_{af0} = \hat{V}_{a0} - jX_{s0} \hat{I}_{a0}$$

Since the subscripted quantities correspond to the solution of part (a), they must satisfy

$$\hat{E}_{af0} = \hat{V}_{a0} - jX_{s0} \hat{I}_{a0}$$

and thus we see that we must have $I_f = I_{f0}$. In other words, the field current for this operating condition is equal to that found in part (a), or $I_f = 3.69$ A.

Practice Problem 11.7

Consider 50-Hz operation of the synchronous motor of Example 11.7, part (b). If the load torque is reduced to 75 percent of rated torque, calculate the field current required to achieve unity power factor.

Solution

3.35 A

Although during steady-state operation the speed of a synchronous motor is determined by the frequency of the drive, speed control by means of frequency control is of limited use in practice. This is due in most part to the fact that it is difficult for the rotor of a synchronous machine to track arbitrary changes in the frequency

of the applied armature voltage. In addition, starting is a major problem, and, as a result, the rotors of synchronous motors are often equipped with a squirrel-cage winding known as an *amortisseur* or *damper winding* similar to the squirrel-cage winding in an induction motor, as shown in Fig. 5.3. Following the application of a polyphase voltage to the armature, the rotor will come up almost to synchronous speed by induction-motor action with the field winding unexcited. If the load and inertia are not too great, the motor will pull into synchronism when the field winding is subsequently energized.

Problems with changing speed result from the fact that, in order to develop torque, the rotor of a synchronous motor must remain in synchronism with the stator flux. Control of synchronous motors can be greatly enhanced by control algorithms in which the stator flux and its relationship to the rotor flux are controlled directly. Such control, which amounts to direct control of torque, is discussed in Section 11.2.2.

11.2.2 Torque Control

Direct torque control in an ac machine, which can be implemented in a number of different ways, is commonly referred to as *field-oriented control* or *vector control*. To facilitate our discussion of field-oriented control, it is helpful to return to the discussion of Section 5.6.1. Under this viewpoint, which is formalized in Appendix C, stator quantities (flux, current, voltage, etc.) are resolved into components which rotate in synchronism with the rotor. *Direct-axis* quantities represent those components which are aligned with the field-winding axis, and *quadrature-axis* components are aligned perpendicular to the field-winding axis.

Section C.2 of Appendix C derives the basic machine relations in dq0 variables for a synchronous machine consisting of a field winding and a three-phase stator winding. The transformed flux-current relationships are found to be

$$\lambda_d = L_d i_d + L_{af} i_f \quad (11.17)$$

$$\lambda_q = L_q i_q \quad (11.18)$$

$$\lambda_f = \frac{3}{2} L_{af} i_d + L_{ff} i_f \quad (11.19)$$

where the subscripts d, q, and f refer to armature direct-, quadrature-axis, and field-winding quantities respectively. Note that throughout this chapter we will assume balanced operating conditions, in which case zero-sequence quantities will be zero and hence will be ignored.

The corresponding transformed voltage equations are

$$v_d = R_a i_d + \frac{d\lambda_d}{dt} - \omega_{me} \lambda_q \quad (11.20)$$

$$v_q = R_a i_q + \frac{d\lambda_q}{dt} + \omega_{me} \lambda_d \quad (11.21)$$

$$v_f = R_f i_f + \frac{d\lambda_f}{dt} \quad (11.22)$$

where $\omega_{me} = (\text{poles}/2)\omega_m$ is the electrical angular velocity of the rotor.

Finally, the electromagnetic torque acting on the rotor of a synchronous motor is shown to be (Eq. C.31)

$$T_{\text{mech}} = \frac{3}{2} \left(\frac{\text{poles}}{2} \right) (\lambda_d i_q - \lambda_q i_d) \quad (11.23)$$

Under steady-state, balanced-three-phase operating conditions, $\omega_{\text{me}} = \omega_e$ where ω_e is the electrical frequency of the armature voltage and current in rad/sec. Because the armature-produced mmf and flux waves rotate in synchronism with the rotor and hence with respect to the dq reference frame, under these conditions an observer in the dq reference frame will see constant fluxes, and hence one can set $d/dt = 0$.¹

Letting the subscripts F, D, and Q indicate the corresponding constant values of field-, direct- and quadrature-axis quantities respectively, the flux-current relationships of Eqs. 11.17 through 11.19 then become

$$\lambda_D = L_d i_D + L_{af} i_F \quad (11.24)$$

$$\lambda_Q = L_q i_Q \quad (11.25)$$

$$\lambda_F = \frac{3}{2} L_{af} i_D + L_{ff} i_F \quad (11.26)$$

Armature resistance is typically quite small, and, if we neglect it, the steady-state voltage equations (Eqs. 11.20 through 11.22) then become

$$v_D = -\omega_e \lambda_Q \quad (11.27)$$

$$v_Q = \omega_e \lambda_D \quad (11.28)$$

$$v_F = R_f i_F \quad (11.29)$$

Finally, we can write Eq. 11.23 as

$$T_{\text{mech}} = \frac{3}{2} \left(\frac{\text{poles}}{2} \right) (\lambda_D i_Q - \lambda_Q i_D) \quad (11.30)$$

From this point on, we will focus our attention on machines in which the effects of saliency can be neglected. In this case, the direct- and quadrature-axis synchronous inductances are equal and we can write

$$L_d = L_q = L_s \quad (11.31)$$

where L_s is the synchronous inductance. Substitution into Eqs. 11.24 and 11.25 and then into Eq. 11.30 gives

$$\begin{aligned} T_{\text{mech}} &= \frac{3}{2} \left(\frac{\text{poles}}{2} \right) [(L_s i_D + L_{af} i_F) i_Q - L_s i_Q i_D] \\ &= \frac{3}{2} \left(\frac{\text{poles}}{2} \right) L_{af} i_F i_Q \end{aligned} \quad (11.32)$$

¹ This can easily be derived formally by substituting expressions for the balanced-three-phase armature currents and voltages into the transformation equations.

Equation 11.32 shows that torque is produced by the interaction of the field flux (proportional to the field current) and the quadrature-axis component of the armature current, in other words the component of armature current that is orthogonal to the field flux. By analogy, we see that the direct-axis component of armature current, which is aligned with the field flux, produces no torque.

This result is fully consistent with the generalized torque expressions which are derived in Chapter 4. Consider for example Eq. 4.73 which expresses the torque in terms of the product of the stator and rotor mmfs (F_s and F_r respectively) and the sine of the angle between them.

$$T = - \left(\frac{\text{poles}}{2} \right) \left(\frac{\mu_0 \pi D l}{2g} \right) F_s F_r \sin \delta_{sr} \quad (11.33)$$

where δ_r is the electrical space angle between the stator and rotor mmfs. This shows clearly that no torque will be produced by the direct-axis component of the armature mmf which, by definition, is that component of the stator mmf which is aligned with that of the field winding on the rotor.

Equation 11.32 shows the torque in a nonsalient synchronous motor is proportional to the product of the field current and the quadrature-axis component of the armature current. This is directly analogous to torque production in a dc machine for which Eqs. 7.10 and 7.13 can be combined to show that the torque is proportional to the product of the field current and the armature current.

The analogy between a nonsalient synchronous machine and dc machine can be further reinforced. Consider Eq. 5.21, which expresses the rms value of the line-to-neutral generated voltage of a synchronous generator as

$$E_{af} = \frac{\omega_e L_{af} i_F}{\sqrt{2}} \quad (11.34)$$

Substitution into Eq. 11.32 gives

$$T_{\text{mech}} = \frac{3}{2} \left(\frac{\text{poles}}{\sqrt{2}} \right) \frac{E_{af} i_Q}{\omega_e} \quad (11.35)$$

This is directly analogous to Eq. 7.16 ($T_{\text{mech}} = E_a I_a / \omega_m$) for a dc machine in which the torque is proportional to the product of the generated voltage and the armature current.

The brushes and commutator of a dc machine force the commutated armature current and armature flux along the quadrature axis such that $I_d = 0$ and it is the interaction of this quadrature-axis current with the direct-axis field flux that produces the torque.² A field-oriented controller which senses the position of the rotor and controls the quadrature-axis component of armature current produces the same effect in a synchronous machine.

² In a practical dc motor, the brushes may be adjusted away from this ideal condition somewhat to improve commutation. In this case, some direct-axis flux will be produced, corresponding to a small direct-axis component of armature current.

Although the direct-axis component of the armature current does not play a role in torque production, it does play a role in determining the resultant stator flux and hence the machine terminal voltage, as can be readily shown. Specifically, from the transformation equations of Appendix C,

$$v_a = v_D \cos(\omega_e t) - v_Q \sin(\omega_e t) \quad (11.36)$$

and thus the rms amplitude of the armature voltage is equal to³

$$\begin{aligned} V_a &= \sqrt{\frac{v_D^2 + v_Q^2}{2}} = \omega_e \sqrt{\frac{\lambda_D^2 + \lambda_Q^2}{2}} \\ &= \omega_e \sqrt{\frac{(L_s i_D + L_{af} i_F)^2 + (L_s i_Q)^2}{2}} \end{aligned} \quad (11.37)$$

Dividing V_a by the electrical frequency ω_e , we get an expression for rms armature flux linkages

$$(\lambda_a)_{\text{rms}} = \sqrt{\frac{\lambda_D^2 + \lambda_Q^2}{2}} = \sqrt{\frac{(L_s i_D + L_{af} i_F)^2 + (L_s i_Q)^2}{2}} \quad (11.38)$$

Similarly, the transformation equations of Appendix C can be used to show that the rms amplitude of the armature current is equal to

$$I_a = \sqrt{\frac{i_D^2 + i_Q^2}{2}} \quad (11.39)$$

From Eq. 11.32 we see that torque is controlled by the product $i_F i_Q$ of the field current and the quadrature-axis component of the armature current. Thus, simply specifying a desired torque is not sufficient to uniquely determine either i_F or i_Q . In fact, under the field-oriented-control viewpoint presented here, there are actually three independent variables, i_F , i_Q and i_D , and, in general, three constraints will be required to uniquely determine them. In addition to specifying the desired torque, a typical controller will implement additional constraints on terminal flux-linkages and current using the basic relationships found in Eqs. 11.38 and 11.39.

Figure 11.13a shows a typical field-oriented torque-control system in block-diagram form. The torque-controller block has two inputs, T_{ref} , the reference value or set point for torque and $(i_F)_{\text{ref}}$, the reference value or set point for the field current, which is also supplied to the power supply which supplies the current i_F to the motor field winding. $(i_F)_{\text{ref}}$ is determined by an auxiliary controller which also determines

³ Strictly speaking, armature resistance should be included in the voltage expression, in which case the rms amplitude of the armature voltage would be given by the expression

$$V_a = \sqrt{\frac{v_D^2 + v_Q^2}{2}} = \sqrt{\frac{(R_a i_D - \omega_e \lambda_Q)^2 + (R_a i_Q + \omega_e \lambda_D)^2}{2}}$$

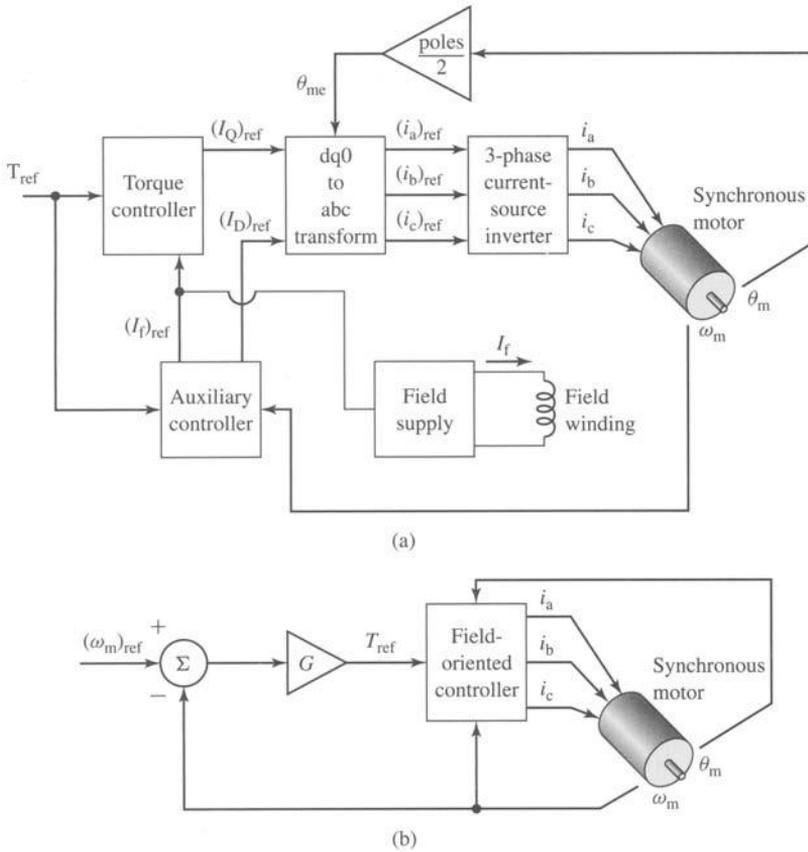


Figure 11.13 (a) Block diagram of a field-oriented torque-control system for a synchronous motor. (b) Block diagram of a synchronous-motor speed-control loop built around a field-oriented torque control system.

the reference value $(i_D)_{ref}$ of the direct-axis current, based upon desired values for the armature current and voltage. The torque controller calculates the desired quadrature-axis current from Eq. 11.32 based upon T_{ref} and $(i_F)_{ref}$

$$(i_Q)_{ref} = \frac{2}{3} \left(\frac{2}{poles} \right) \frac{T_{ref}}{L_{af}(i_F)_{ref}} \quad (11.40)$$

Note that a position sensor is required to determine the angular position of the rotor in order to implement the dq0 to abc transformation.

In a typical application, the ultimate control objective is not to control motor torque but to control speed or position. Figure 11.13b shows how the torque-control system of Fig. 11.13a can be used as a component of a speed-control loop, with speed feedback forming an outer control loop around the inner torque-control loop.

EXAMPLE 11.8

Consider again the 45-kVA, 220-V, six-pole synchronous motor of Example 11.7 operating at 60 Hz with a field current of 2.84 A. If the motor is loaded to rated torque and operating under a field-oriented control system such that $i_D = 0$, calculate (a) the phase currents $i_a(t)$, $i_b(t)$, and $i_c(t)$ as well as the per-unit value of the armature current and (b) the motor terminal voltage in per unit. Assume that $\theta_{me} = 0$ at time $t = 0$ (i.e., the rotor direct axis is aligned with phase a at $t = 0$).

■ Solution

- a. We must first calculate L_{af} . From Example 11.7, we see that the motor produces rated open-circuit voltage (220-V rms, line-to-line) at a field current of 2.84 A. From Eq. 11.34

$$L_{af} = \frac{\sqrt{2}E_{af}}{\omega_e i_F}$$

where E_{af} is the rms, line-to-neutral generated voltage. Thus

$$L_{af} = \frac{\sqrt{2}(220/\sqrt{3})}{120\pi \times 2.84} = 0.168 \text{ H}$$

Rated torque for this six-pole motor is equal to

$$\begin{aligned} T_{\text{rated}} &= \frac{P_{\text{rated}}}{(\omega_m)_{\text{rated}}} = \frac{P_{\text{rated}}}{(\omega_e)_{\text{rated}}(2/\text{poles})} \\ &= \frac{45 \times 10^3}{120\pi(2/6)} = 358 \text{ N} \cdot \text{m} \end{aligned}$$

Thus, setting $T_{\text{ref}} = T_{\text{rated}} = 358 \text{ N} \cdot \text{m}$ and $(i_F)_{\text{ref}} = 2.84 \text{ A}$, we can find i_Q from Eq. 11.40 as

$$i_Q = \frac{2}{3} \left(\frac{2}{\text{poles}} \right) \frac{T_{\text{ref}}}{L_{af}(i_F)_{\text{ref}}} = \frac{2}{3} \left(\frac{2}{6} \right) \frac{358}{0.168 \times 2.84} = 167 \text{ A}$$

Using the fact that $\theta_{me} = \omega_e t$ and setting $i_D = 0$, the transformation from dq0 variables to abc variables (Eq. C.2 of Appendix C) gives

$$i_a(t) = i_D \cos(\omega_e t) - i_Q \sin(\omega_e t) = -167 \sin(\omega_e t) = -\sqrt{2}(118) \sin(\omega_e t) \text{ A}$$

where $\omega_e = 120\pi \approx 377 \text{ rad/sec}$. Similarly

$$i_b(t) = -\sqrt{2}(118) \sin(\omega_e t - 120^\circ) \text{ A}$$

and

$$i_c(t) = -\sqrt{2}(118) \sin(\omega_e t + 120^\circ) \text{ A}$$

The rms armature current is 118 A and the machine base current is equal to

$$I_{\text{base}} = \frac{P_{\text{base}}}{\sqrt{3}V_{\text{base}}} = \frac{45 \times 10^3}{\sqrt{3}220} = 118 \text{ A}$$

Thus the per-unit machine terminal current is equal to $I_a = 118/118 = 1.0$ per unit.

b. The motor terminal voltage can most easily be found from the rms phasor relationship

$$\hat{V}_a = jX_s \hat{I}_a + \hat{E}_{af}$$

In part (a) we found that $i_a = -\sqrt{2}(118) \sin(\omega_c t)$ A and thus

$$\hat{I}_a = j118 \text{ A}$$

We can find E_{af} from Eq. 11.34 as

$$E_{af} = \frac{\omega_c L_{af} i_F}{\sqrt{2}} = \frac{120\pi \times 0.168 \times 2.84}{\sqrt{2}} = 127 \text{ V line-to-neutral}$$

and, thus, since $(\hat{E}_{af})_{\text{rms}}$ lies along the quadrature axis, as does \hat{I}_a ,

$$\hat{E}_{af} = j127 \text{ V}$$

The base impedance of the machine is equal to

$$Z_{\text{base}} = \frac{V_{\text{base}}^2}{P_{\text{base}}} = \frac{220^2}{45 \times 10^3} = 1.076 \Omega$$

and the synchronous reactance of 0.836 pu is equal to $X_s = 0.836 \times 1.076 = 0.899 \Omega$.

Thus the rms line-to-neutral terminal voltage

$$\begin{aligned} \hat{V}_a &= jX_s \hat{I}_a + \hat{E}_{af} = j0.899(j118) + j127 \\ &= -106 + j127 = 165 \angle 129.9^\circ \text{ V line-to-neutral} \end{aligned}$$

or $V_a = 287 \text{ V rms line-to-line} = 1.30$ per unit.

Note that the terminal voltage for this operating condition is considerably in excess of the rated voltage of this machine, and hence such operation would not be acceptable. As we shall now discuss, a control algorithm which takes advantage of the full capability to vary i_F , i_D , and i_Q can achieve rated torque while not exceeding rated terminal voltage.

Practice Problem 11.8

Calculate the per-unit terminal voltage and current of Example 11.8 if the field-oriented controller maintains $i_D = 0$ while reducing i_F to 2.6 A.

Solution

$V_a = 1.29$ per unit and $I_a = 1.09$ per unit.

As we have discussed, a practical field-oriented control must determine values for all three currents i_F , i_D , and i_Q . In Example 11.8 two of these values were chosen relatively arbitrarily ($i_F = 2.84$ and $i_D = 0$) and the result was a control that achieved the desired torque but with a terminal voltage 30 percent in excess of the motor-rated voltage. In a practical system, additional constraints are required to achieve an acceptable control algorithm.

One such algorithm would be to require that the motor operate at rated flux and at unity terminal power factor. Such an algorithm can be derived with reference to the

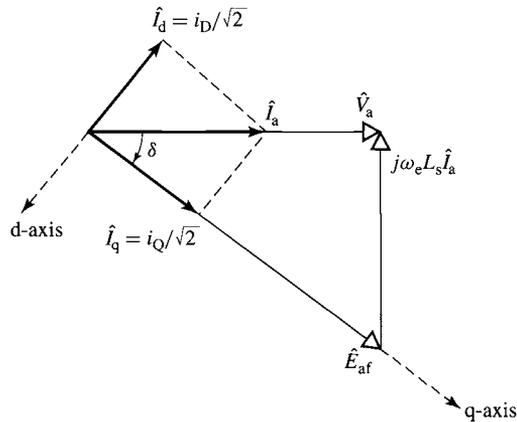


Figure 11.14 Phasor diagram for unity-power-factor field-oriented-control algorithm.

phasor diagram of Fig 11.14 and can be implemented using the following steps:

Step 1. Calculate the line-to-neutral armature voltage corresponding to rated flux as

$$V_a = (V_a)_{\text{rated}} \left(\frac{\omega_m}{(\omega_m)_{\text{rated}}} \right) \quad (11.41)$$

where $(V_a)_{\text{rated}}$ is the rated line-to-neutral armature voltage at rated motor speed, ω_m is the desired motor speed, and $(\omega_m)_{\text{rated}}$ is its rated speed.

Step 2. Calculate the rms armature current from the desired torque T_{ref} as

$$I_a = \frac{P_{\text{ref}}}{3V_a} = \frac{T_{\text{ref}} \omega_m}{3V_a} \quad (11.42)$$

where P_{ref} is the mechanical power corresponding to the desired torque.

Step 3. Calculate the angle δ based upon the phasor diagram of Fig 11.14

$$\delta = -\tan^{-1} \left(\frac{\omega_e L_s I_a}{V_a} \right) \quad (11.43)$$

where $\omega_e = \omega_{me} = (\text{poles}/2)\omega_m$ is the electrical frequency corresponding to the desired motor speed.

Step 4. Calculate $(i_Q)_{\text{ref}}$ and $(i_D)_{\text{ref}}$

$$(i_Q)_{\text{ref}} = \sqrt{2} I_a \cos \delta \quad (11.44)$$

$$(i_D)_{\text{ref}} = \sqrt{2} I_a \sin \delta \quad (11.45)$$

Step 5. Calculate $(i_F)_{\text{ref}}$ from Eq. 11.32

$$(i_F)_{\text{ref}} = \frac{2}{3} \left(\frac{2}{\text{poles}} \right) \frac{T_{\text{ref}}}{L_{af}(i_Q)_{\text{ref}}} \quad (11.46)$$

This algorithm is illustrated in Example 11.9.

EXAMPLE 11.9

The 45-kVA, 220-V synchronous motor of Example 11.8 is to be again operated at rated torque and speed from a field-oriented control system. Calculate the required motor field current and the per-unit terminal voltage and current if the unity-power-factor algorithm described above is implemented.

■ Solution

We will follow the individual steps outlined above.

Step 1. Since the motor is operating at rated speed, the desired terminal voltage will be the rated line-to-neutral voltage of the machine.

$$V_a = \frac{220}{\sqrt{3}} = 127 \text{ V} = 1.0 \text{ per unit}$$

Step 2. Setting $T_{\text{ref}} = 358 \text{ N}\cdot\text{m}$ and $\omega_m = (2/\text{poles})\omega_c = 40\pi$, the rms armature current can be calculated from Eq. 11.42

$$I_a = \frac{T_{\text{ref}} \omega_m}{3(V_a)} = \frac{358 \times (40\pi)}{3 \times 127} = 118 \text{ A}$$

As calculated in Example 11.8, $I_{\text{base}} = 118 \text{ A}$ and thus $I_a = 1.0$ per unit. This is as expected, since we want the motor to operate at rated torque, speed and terminal voltage, and at unity power factor.

Step 3. Next calculate δ from Eq. 11.43. This calculation requires that we determine the synchronous inductance L_s .

$$L_s = \frac{X_s}{(\omega_c)_{\text{rated}}} = \frac{0.899}{120\pi} = 2.38 \text{ mH}$$

Thus

$$\begin{aligned} \delta &= -\tan^{-1} \left(\frac{\omega_c L_s I_a}{V_a} \right) \\ &= -\tan^{-1} \left(\frac{120\pi \cdot 2.38 \times 10^{-3} \times 118}{127} \right) = -0.695 \text{ rad} = -39.8^\circ \end{aligned}$$

Step 4. We can now calculate the desired values of i_Q and i_D from Eqs. 11.44 and 11.45.

$$(i_Q)_{\text{ref}} = \sqrt{2} I_a \cos \delta = 128 \text{ A}$$

and

$$(i_D)_{\text{ref}} = \sqrt{2} I_a \sin \delta = -107 \text{ A}$$

Step 5. $(i_F)_{\text{ref}}$ is found from Eq. 11.46

$$(i_F)_{\text{ref}} = \frac{2}{3} \left(\frac{2}{\text{poles}} \right) \frac{T_{\text{ref}}}{L_{\text{af}}(i_Q)_{\text{ref}}} = \frac{2}{3} \left(\frac{2}{6} \right) \frac{358}{0.168 \times 128} = 3.70 \text{ A}$$

Practice Problem 11.9

Repeat Example 11.9 for the motor operating at rated torque and half of rated speed. Calculate (a) the desired motor field current, (b) the line-to-neutral terminal voltage (in volts), and (c) the armature current (in amperes).

Solution

- a. $(i_f)_{ref} = 3.70 \text{ A}$
- b. $V_a = 63.5 \text{ V line-to-neutral}$
- c. $I_a = 118 \text{ A}$

The discussion of this section has focused upon synchronous machines with field windings and the corresponding capability to control the field excitation. The basic concept, of course, also applies to synchronous machines with permanent magnets on the rotor. However, in the case of permanent-magnet synchronous machines, the effective field excitation is fixed and, as a result, there is one less degree of freedom for the field-oriented control algorithm.

For a permanent-magnet synchronous machine, since the effective equivalent field current is fixed by the permanent magnet, the quadrature-axis current is determined directly by the desired torque. Consider a three-phase permanent-magnet motor whose rated rms, line-to-neutral open-circuit voltage is $(E_{af})_{rated}$ at electrical frequency $(\omega_e)_{rated}$. From Eq. 11.34 we see that the equivalent $L_{af}I_f$ product for this motor, which we will refer to by the symbol Λ_{PM} , is

$$\Lambda_{PM} = \frac{\sqrt{2}(E_{af})_{rated}}{(\omega_e)_{rated}} \quad (11.47)$$

Thus, the direct-axis flux-current relationship for this motor, corresponding to Eq. 11.24, becomes

$$\lambda_D = L_d i_D + \Lambda_{PM} \quad (11.48)$$

and the torque expression of Eq. 11.32 becomes

$$T_{mech} = \frac{3}{2} \left(\frac{\text{poles}}{2} \right) \Lambda_{PM} i_Q \quad (11.49)$$

From Eq. 11.49 we see that, for a permanent-magnet synchronous machine under field-oriented control, the quadrature-axis current is uniquely determined by the desired torque and Eq. 11.40 becomes

$$(i_Q)_{ref} = \frac{2}{3} \left(\frac{2}{\text{poles}} \right) \frac{T_{ref}}{\Lambda_{PM}} \quad (11.50)$$

Once $(i_Q)_{ref}$ has been specified, the only remaining control choice remains to determine the desired value for the direct-axis current, $(i_D)_{ref}$. One possibility is simply to set $(i_D)_{ref} = 0$. This will clearly result in the lowest possible armature current for a given torque. However, as we have seen in Example 11.8, this is likely to result in terminal voltages in excess of the rated voltage of the machine. As a

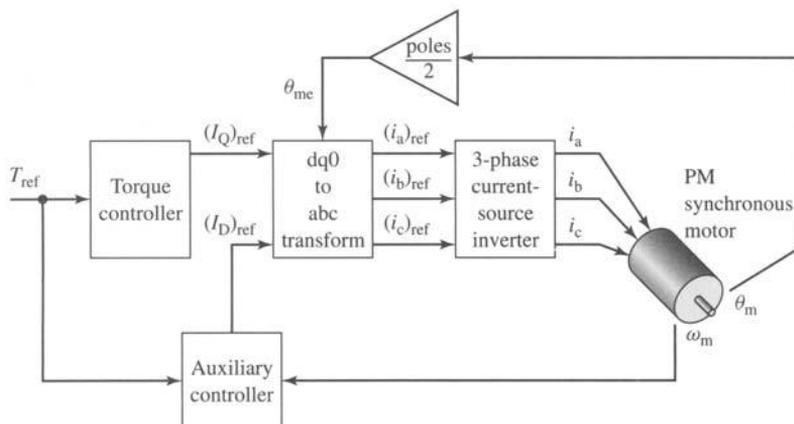


Figure 11.15 Block diagram of a field-oriented torque-control system to a permanent-magnet synchronous motor.

result, it is common to supply direct-axis current so as to reduce the direct-axis flux linkage of Eq. 11.48, which will in turn result in reduced terminal voltage. This technique is commonly referred to as *flux weakening* and comes at the expense of increased armature current.⁴ In practice, the chosen operating point is determined by a trade-off between reducing the armature voltage and an increase in armature current. Figure 11.15 shows the block diagram for a field-oriented-control system for use with a permanent-magnet motor.

EXAMPLE 11.10

A 25-kW, 4000-rpm, 220-V, two-pole, three-phase permanent-magnet synchronous motor produces rated open-circuit voltage at a rotational speed of 3200 r/min and has a synchronous inductance of 1.75 mH. Assume the motor is to be operated under field-oriented control at 2800 r/min and 65 percent of rated torque.

- Calculate the required quadrature-axis current.
- If the controller is designed to minimize armature current by setting $i_D = 0$, calculate the resultant armature flux linkage in per-unit.
- If the controller is designed to maintain the armature flux-linkage at its rated value, calculate the corresponding value of i_D and the corresponding rms and per-unit values of the armature current.

■ Solution

- The rated speed of this machine is

$$(\omega_m)_{\text{rated}} = 4000 \left(\frac{\pi}{30} \right) = 419 \text{ rad/sec}$$

⁴ See T. M. Jahns, "Flux-Weakening Regime Operation of an Interior Permanent Magnet Synchronous Motor Drive," *IEEE Transactions on Industry Applications*, Vol. 23, pp. 681–689.

and the rated torque is

$$T_{\text{rated}} = \frac{P_{\text{rated}}}{(\omega_m)_{\text{rated}}} = \frac{25 \times 10^3}{419} = 59.7 \text{ N} \cdot \text{m}$$

This motor achieves its rated-open-circuit voltage of $220/\sqrt{3} = 127 \text{ V}$ rms at a speed of $n = 3200 \text{ r/min}$. The corresponding electrical frequency is

$$\omega_e = \left(\frac{\text{poles}}{2} \right) \left(\frac{\pi}{30} \right) n = \left(\frac{\pi}{30} \right) 3200 = 335 \text{ rad/sec}$$

From Eq. 11.47,

$$\Lambda_{\text{PM}} = \frac{\sqrt{2}(E_{\text{af}})_{\text{rated}}}{\omega_e} = \frac{\sqrt{2} 127}{335} = 0.536 \text{ Wb}$$

Thus, setting $T_{\text{ref}} = 0.65T_{\text{rated}} = 38.8 \text{ N} \cdot \text{m}$, from Eq. 11.50 we find that

$$(i_Q)_{\text{ref}} = \frac{2}{3} \left(\frac{2}{\text{poles}} \right) \frac{T_{\text{ref}}}{\Lambda_{\text{PM}}} = \frac{2}{3} \left(\frac{38.8}{0.536} \right) = 48.3 \text{ A}$$

b. With $(i_D)_{\text{ref}} = 0$,

$$\lambda_D = \Lambda_{\text{PM}} = 0.536 \text{ Wb}$$

and

$$\lambda_Q = L_s i_Q = (1.75 \times 10^{-3}) 48.3 = 0.0845 \text{ Wb}$$

Thus, the rms line-to-neutral armature flux is equal to

$$\lambda_a = \sqrt{\frac{\lambda_D^2 + \lambda_Q^2}{2}} = \sqrt{\frac{0.536^2 + 0.0845^2}{2}} = 0.384 \text{ Wb}$$

The base rms line-to-neutral armature flux can be determined from the base line-to-neutral voltage $(V_a)_{\text{base}} = 127 \text{ V}$ and the base frequency $(\omega_e)_{\text{base}} = 419 \text{ rad/sec}$ as

$$(\lambda_a)_{\text{base}} = \frac{(V_a)_{\text{base}}}{(\omega_e)_{\text{base}}} = 0.303 \text{ Wb}$$

Thus, the per-unit armature flux is equal to $0.384/0.303 = 1.27$ per unit. From this calculation we see that the motor is significantly saturated at this operating condition. In fact, the calculation is probably not accurate because such a degree of saturation will most likely give rise to a reduction in the synchronous inductance as well as the magnetic coupling between the rotor and the stator.

c. In order to maintain rated armature flux linkage, the control will have to produce a direct-axis component of armature current to reduce the direct-axis flux linkage such that the total armature flux linkage is equal to the rated value $(\lambda_a)_{\text{base}}$. Specifically, we must have

$$\lambda_D = \sqrt{2(\lambda_a)_{\text{base}}^2 - \lambda_Q^2} = \sqrt{2 \times 0.303^2 - 0.0844^2} = 0.420 \text{ Wb}$$

We can now find $(i_D)_{\text{ref}}$ from Eq. 11.48 (setting $L_d = L_s$)

$$(i_D)_{\text{ref}} = \frac{\lambda_D - \Lambda_{\text{PM}}}{L_s} = \frac{0.420 - 0.536}{1.75 \times 10^{-3}} = -66.3 \text{ A}$$

The corresponding rms armature current is

$$I_a = \sqrt{\frac{(i_D)_{\text{ref}}^2 + (i_Q)_{\text{ref}}^2}{2}} = \sqrt{\frac{66.3^2 + 48.3^2}{2}} = 58.0 \text{ A}$$

The base rms armature current for this machine is equal to

$$I_{\text{base}} = \frac{P_{\text{base}}}{\sqrt{3}V_{\text{base}}} = \frac{25 \times 10^3}{\sqrt{3} 220} = 65.6 \text{ A}$$

and hence the per-unit armature current is equal to $58.0/65.6 = 0.88$ per unit.

Comparing the results of parts (b) and (c) we see how flux weakening by the introduction of direct-axis current can be used to control the terminal voltage of a permanent-magnet synchronous motor under field-oriented control.

Practice Problem 11.10

Consider again the motor of Example 11.10. Repeat the calculations of parts (b) and (c) of Example 11.10 for the case in which the motor is operating at 80 percent of rated torque at a speed of 2500 r/min.

Solution

For part (b), $\lambda_a = 1.27$ per unit.

For part (c), $I_a = 0.98$ per unit.

11.3 CONTROL OF INDUCTION MOTORS

11.3.1 Speed Control

Induction motors supplied from a constant-frequency source admirably fulfill the requirements of substantially constant-speed drives. Many motor applications, however, require several speeds or even a continuously adjustable range of speeds. From the earliest days of ac power systems, engineers have been interested in the development of adjustable-speed ac motors.

The synchronous speed of an induction motor can be changed by (a) changing the number of poles or (b) varying the line frequency. The operating slip can be changed by (c) varying the line voltage, (d) varying the rotor resistance, or (e) applying voltages of the appropriate frequency to the rotor circuits. The salient features of speed-control methods based on these five possibilities are discussed in the following five sections.

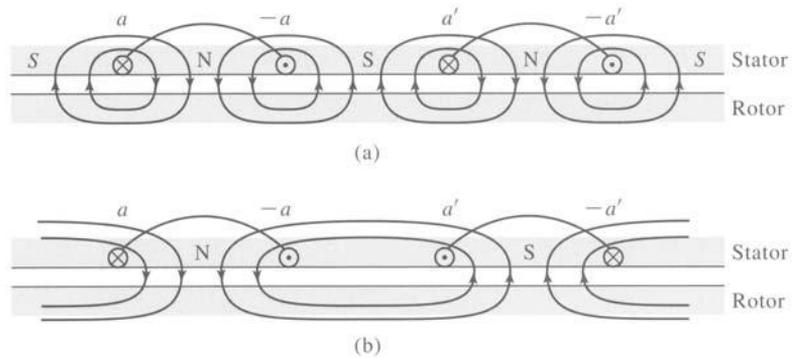


Figure 11.16 Principles of the pole-changing winding.

Pole-Changing Motors In pole-changing motors, the stator winding is designed so that, by simple changes in coil connections, the number of poles can be changed in the ratio 2 to 1. Either of two synchronous speeds can then be selected. The rotor is almost always of the squirrel-cage type, which reacts by producing a rotor field having the same number of poles as the inducing stator field. With two independent sets of stator windings, each arranged for pole changing, as many as four synchronous speeds can be obtained in a squirrel-cage motor, for example, 600, 900, 1200, and 1800 r/min for 60-Hz operation.

The basic principles of the pole-changing winding are shown in Fig. 11.16, in which aa and $a'a'$ are two coils comprising part of the phase-a stator winding. An actual winding would, of course, consist of several coils in each group. The windings for the other stator phases (not shown in the figure) would be similarly arranged. In Fig. 11.16a the coils are connected to produce a four-pole field; in Fig. 11.16b the current in the $a'a'$ coil has been reversed by means of a controller, the result being a two-pole field.

Figure 11.17 shows the four possible arrangements of these two coils: they can be connected in series or in parallel, and with their currents either in the same direction (four-pole operation) or in the opposite direction (two-pole operation). Additionally, the machine phases can be connected either in Y or Δ , resulting in eight possible combinations.

Note that for a given phase voltage, the different connections will result in differing levels of air-gap flux density. For example, a change from a Δ to a Y connection will reduce the coil voltage (and hence the air-gap flux density) for a given coil arrangement by $\sqrt{3}$. Similarly, changing from a connection with two coils in series to two in parallel will double the voltage across each coil and therefore double the magnitude of the air-gap flux density. These changes in flux density can, of course, be compensated for by changes in the applied winding voltage. In any case, they must be considered, along with corresponding changes in motor torque, when the configuration to be used in a specific application is considered.

Armature-Frequency Control The synchronous speed of an induction motor can be controlled by varying the frequency of the applied armature voltage. This method

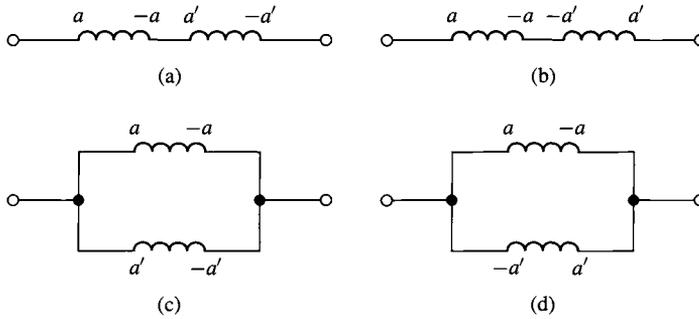


Figure 11.17 Four possible arrangements of phase-a stator coils in a pole-changing induction motor: (a) series-connected, four-pole; (b) series-connected, two-pole; (c) parallel-connected, four-pole; (d) parallel-connected, two-pole.

of speed control is identical to that discussed in Section 11.2.1 for synchronous machines. In fact, the same inverter configurations used for synchronous machines, such as the three-phase voltage-source inverter of Fig. 11.11, can be used to drive induction motors. As is the case with any ac motor, to maintain approximately constant flux density, the armature voltage should also be varied directly with the frequency (constant-volts-per-hertz).

The torque-speed curve of an induction motor for a given frequency can be calculated by using the methods of Chapter 6 within the accuracy of the motor parameters at that frequency. Consider the torque expression of Eq. 6.33 which is repeated here.

$$T_{\text{mech}} = \frac{1}{\omega_s} \left[\frac{n_{\text{ph}} V_{1,\text{eq}}^2 (R_2/s)}{(R_{1,\text{eq}} + (R_2/s))^2 + (X_{1,\text{eq}} + X_2)^2} \right] \quad (11.51)$$

where $\omega_s = (2/\text{poles})\omega_e$ and ω_e is the electrical excitation frequency of the motor in rad/sec,

$$\hat{V}_{1,\text{eq}} = \hat{V}_1 \left(\frac{jX_m}{R_1 + j(X_1 + X_m)} \right) \quad (11.52)$$

and

$$R_{1,\text{eq}} + jX_{1,\text{eq}} = \frac{jX_m(R_1 + jX_1)}{R_1 + j(X_1 + X_m)} \quad (11.53)$$

To investigate the effect of changing frequency, we will assume that R_1 is negligible. In this case,

$$\hat{V}_{1,\text{eq}} = \hat{V}_1 \left(\frac{X_m}{X_1 + X_m} \right) \quad (11.54)$$

$$R_{1,\text{eq}} = 0 \quad (11.55)$$

and

$$X_{1,\text{eq}} = \frac{X_m X_1}{X_1 + X_m} \quad (11.56)$$

Let the subscript 0 represent rated-frequency values of each of the induction-motor parameters. As the electrical-excitation frequency is varied, we can then write

$$(X_{1,\text{eq}} + X_2) = \left(\frac{\omega_e}{\omega_{e0}} \right) (X_{1,\text{eq}} + X_2)_0 \quad (11.57)$$

Under constant-volts-per-hertz control, we can also write the equivalent source voltage as

$$\hat{V}_1 = \left(\frac{\omega_e}{\omega_{e0}} \right) (\hat{V}_1)_0 \quad (11.58)$$

and hence, since $\hat{V}_{1,\text{eq}}$ is equal to \hat{V}_1 multiplied by a reactance ratio which stays constant with changing frequency,

$$\hat{V}_{1,\text{eq}} = \left(\frac{\omega_e}{\omega_{e0}} \right) (\hat{V}_{1,\text{eq}})_0 \quad (11.59)$$

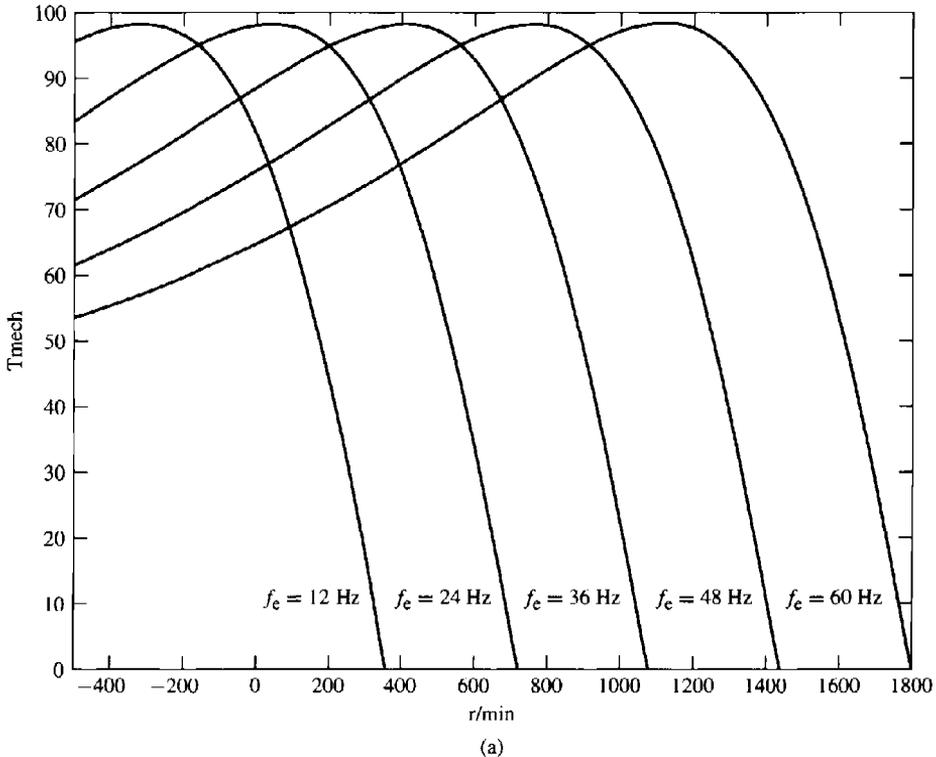


Figure 11.18 A family of typical induction-motor speed-torque curves for a four-pole motor for various values of the electrical supply frequency. (a) R_1 sufficiently small so that its effects are negligible. (b) R_1 not negligible.

Finally, we can write the motor slip as

$$s = \frac{\omega_s - \omega_m}{\omega_s} = \frac{\text{poles}}{2} \left(\frac{\Delta\omega_m}{\omega_e} \right) \quad (11.60)$$

where $\Delta\omega_m = \omega_s - \omega_m$ is the difference between the synchronous and mechanical angular velocities of the motor.

Substitution of Eqs. 11.57 through 11.60 into Eq. 11.51 gives

$$T_{\text{mech}} = \frac{n_{\text{ph}}[(V_{1,\text{eq}})_0]^2(R_2/\Delta\omega)}{\left[\left(\frac{2\omega_e}{\text{poles}} \right) (R_2/\Delta\omega) \right]^2 + [(X_{1,\text{eq}} + X_2)_0]^2} \quad (11.61)$$

Equation 11.61 shows the general trend in which we see that the frequency dependence of the torque-speed characteristic of an induction motor appears only in the term $R_2/\Delta\omega$. Thus, under the assumption that R_1 is negligible, as the electrical supply frequency to an induction motor is changed, the shape of the speed-torque curve as a function of $\Delta\omega$ (the difference between the synchronous speed and the motor speed) will remain unchanged. As a result, the torque-speed characteristic will simply shift along the speed axis as ω_e (f_e) is varied.

A set of such curves is shown in Fig. 11.18a. Note that as the electrical frequency (and hence the synchronous speed) is decreased, a given value of $\Delta\omega$ corresponds to a

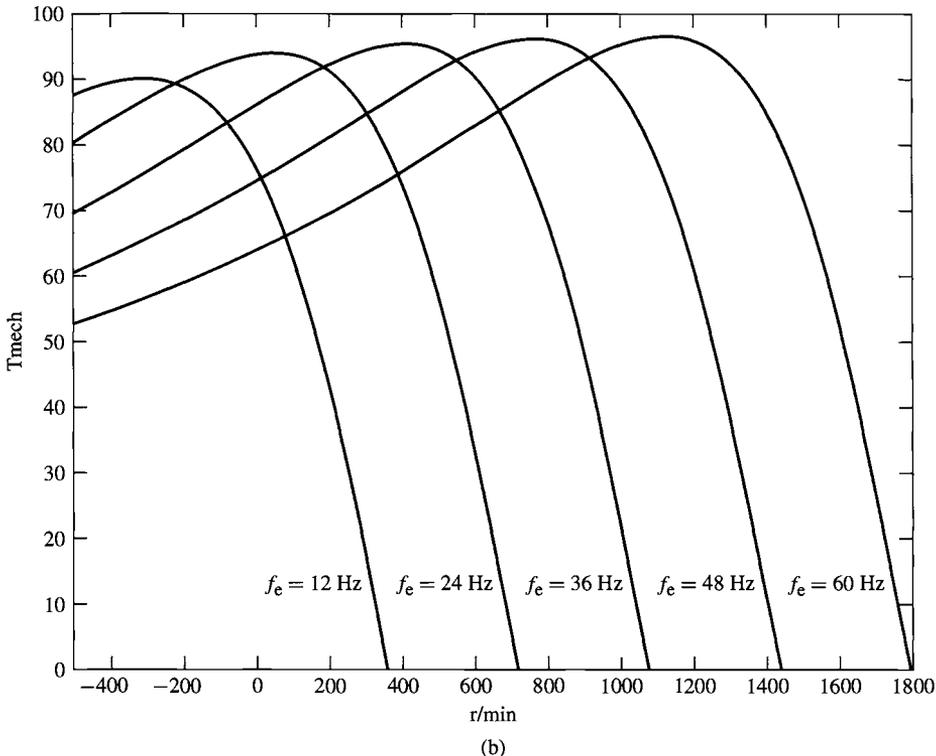


Figure 11.18 (Continued)

larger slip. Thus, for example, if the peak torque of a four-pole motor driven at 60 Hz occurs at 1638 r/min, corresponding to a slip of 9 percent, when driven at 30 Hz, the peak torque will occur at 738 r/min, corresponding to a slip of 18 percent.

In practice, the effects of R_1 may not be fully negligible, especially for large values of slip. If this is the case, the shape of the speed-torque curves will vary somewhat with the applied electrical frequency. Figure 11.18b shows a typical family of curves for this case.

EXAMPLE 11.11



The three-phase, 230-V, 60-Hz, 12-kW, four-pole induction motor of Example 6.4 (with $R_2 = 0.2 \Omega$) is to be operated from a variable-frequency, constant-volts-per-hertz motor drive whose terminal voltage is 230 V at 60 Hz. The motor is driving a load whose power can be assumed to vary as

$$P_{\text{load}} = 10.5 \left(\frac{n}{1800} \right)^3 \text{ kW}$$

where n is the load speed in r/min. Motor rotational losses can be assumed to be negligible.

Write a MATLAB script to find the line-to-line terminal voltage, the motor speed in r/min, the slip and the motor load in kW for (a) a source frequency of 60 Hz and (b) a source frequency of 40 Hz.

■ Solution

As the electrical frequency f_c is varied, the motor reactances given in Example 6.4 must be varied as

$$X = X_0 \left(\frac{f_c}{60} \right)$$

where X_0 is the reactance value at 60 Hz. Similarly, the line-to-neutral armature voltage must be varied as

$$V_1 = \frac{220}{\sqrt{3}} \left(\frac{f_c}{60} \right) = 127 \left(\frac{f_c}{60} \right) \text{ V}$$

From Eq. 4.40, the synchronous angular velocity of the motor is equal to

$$\omega_s = \left(\frac{4\pi}{\text{poles}} \right) f_c = \pi f_c \text{ rad/sec}$$

and, at any given motor speed ω_m , the corresponding slip is given by

$$s = \frac{\omega_s - \omega_m}{\omega_s}$$

Using Eqs. 11.51 through 11.53, the motor speed can be found by searching over ω_m for that speed at which $P_{\text{load}} = \omega_m T_{\text{mech}}$. If this is done, the result is:

a. For $f_c = 60$ Hz:

Terminal voltage = 230 V line-to-line

Speed = 1720 r/min

$$\text{Slip} = 4.4\%$$

$$P_{\text{load}} = 9.17 \text{ kW}$$

b. For $f_c = 40 \text{ Hz}$:

$$\text{Terminal voltage} = 153 \text{ V line-to-line}$$

$$\text{Speed} = 1166 \text{ r/min}$$

$$\text{Slip} = 2.8\%$$

$$P_{\text{load}} = 2.86 \text{ kW}$$

Here is the MATLAB script:

```

clc
clear
%Here are the 60-Hz motor parameters
Vl0 = 230/sqrt(3);
Nph = 3;
poles = 4;
fe0 = 60;
R1 = 0.095;
R2 = 0.2;
X10 = 0.680;
X20 = 0.672;
Xm0 = 18.7;
% Two frequency values
fe1 = 60;
fe2 = 40;
for m = 1:2,
    if m == 1
        fe = fe1;
    else
        fe = fe2;
    end
% Calculate the reactances and the voltage
X1 = X10*(fe/fe0);
X2 = X20*(fe/fe0);
Xm = Xm0*(fe/fe0);
V1 = Vl0*(fe/fe0);
%Calculate the synchronous speed
omegas = 4*pi*fe/poles;
ns = 120*fe/poles;
%Calculate stator Thevenin equivalent
V1eq = abs(V1*j*Xm/(R1 + j*(X1+Xm)));

```

```

Z1eq = j*Xm*(R1+j*X1)/(R1 + j*(X1+Xm));
R1eq = real(Z1eq);
X1eq = imag(Z1eq);

%Search over the slip until the Pload = Pmech

slip = 0.;
error = 1;

while error >= 0;
    slip = slip + 0.00001;
    rpm = ns*(1-slip);
    omegam = omegas*(1-slip);
    Tmech = (1/omegas)*Nph*V1eq^2*(R2/slip);
    Tmech = Tmech/((R1+R2/slip)^2 + (X1+X2)^2);
    Pmech = Tmech*omegam;
    Pload = 10.5e3*(rpm/1800)^3;

    error = Pload - Pmech;

end %End of while loop

fprintf('\nFor fe = %g [Hz]:',fe)
fprintf('\n    Terminal voltage = %g [V l-l]',V1*sqrt(3))
fprintf('\n    rpm = %g',rpm)
fprintf('\n    slip = %g [percent] ',100*slip)
fprintf('\n    Pload = %g [kW]',Pload/1000)
fprintf('\n\n')

end

```

Practice Problem 11.11



Repeat Example 11.11 for a source frequency of 50 Hz.

Solution

Terminal voltage = 192 V line-to-line

Speed = 1447 r/min

Slip = 3.6%

$P_{\text{load}} = 5.45 \text{ kW}$

Line-Voltage Control The internal torque developed by an induction motor is proportional to the square of the voltage applied to its primary terminals, as shown by the two torque-speed characteristics in Fig. 11.19. If the load has the torque-speed characteristic shown by the dashed line, the speed will be reduced from n_1 to n_2 . This method of speed control is commonly used with small squirrel-cage motors driving fans, where cost is an issue and the inefficiency of high-slip operation can be tolerated. It is characterized by a rather limited range of speed control.

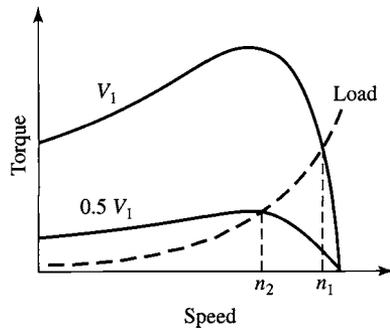


Figure 11.19 Speed control by means of line voltage.

Rotor-Resistance Control The possibility of speed control of a wound-rotor motor by changing its rotor-circuit resistance has already been pointed out in Section 6.7.1. The torque-speed characteristics for three different values of rotor resistance are shown in Fig. 11.20. If the load has the torque-speed characteristic shown by the dashed line, the speeds corresponding to each of the values of rotor resistance are n_1 , n_2 , and n_3 . This method of speed control has characteristics similar to those of dc shunt-motor speed control by means of resistance in series with the armature.

The principal disadvantages of both line-voltage and rotor-resistance control are low efficiency at reduced speeds and poor speed regulation with respect to change in load. In addition, the cost and maintenance requirements of wound-rotor induction motors are sufficiently high that squirrel-cage motors combined with solid-state drives have become the preferred option in most applications.

11.3.2 Torque Control

In Section 11.2.2 we developed the concept of field-oriented-control for synchronous machines. Under this viewpoint, the armature flux and current are resolved into two components which rotate synchronously with the rotor and with the air-gap flux wave.

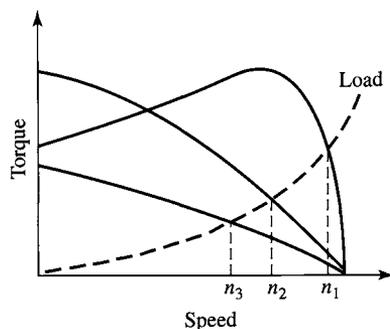


Figure 11.20 Speed control by means of rotor resistance.

The components of armature current and flux which are aligned with the field-winding are referred to as *direct-axis components* while those which are perpendicular to this axis are referred to as *quadrature-axis components*.

It turns out that the same viewpoint which we applied to synchronous machines can be applied to induction machines. As is discussed in Section 6.1, in the steady-state the mmf and flux waves produced by both the rotor and stator windings of an induction motor rotate at synchronous speed and in synchronism with each other. Thus, the torque-producing mechanism in an induction machine is equivalent to that of a synchronous machine. The difference between the two is that, in an induction machine, the rotor currents are not directly supplied but rather are induced as the induction-motor rotor slips with respect to the rotating flux wave produced by the stator currents.

To examine the application of field-oriented control to induction machines, we begin with the dq0 transformation of Section C.3 of Appendix C. This transformation transforms both the stator and rotor quantities into a synchronously rotating reference frame. Under balanced-three-phase, steady-state conditions, zero-sequence quantities will be zero and the remaining direct- and quadrature-axis quantities will be constant. Hence the flux-linkage current relationships of Eqs. C.52 through C.58 become

$$\lambda_D = L_S i_D + L_m i_{DR} \quad (11.62)$$

$$\lambda_Q = L_S i_Q + L_m i_{QR} \quad (11.63)$$

$$\lambda_{DR} = L_m i_D + L_R i_{DR} \quad (11.64)$$

$$\lambda_{QR} = L_m i_Q + L_R i_{QR} \quad (11.65)$$

In these equations, the subscripts D, Q, DR, and QR represent the constant values of the direct- and quadrature-axis components of the stator and rotor quantities respectively. It is a straight-forward matter to show that the inductance parameters can be determined from the equivalent-circuit parameters as

$$L_m = \frac{X_{m0}}{\omega_{e0}} \quad (11.66)$$

$$L_S = L_m + \frac{X_{10}}{\omega_{e0}} \quad (11.67)$$

$$L_R = L_m + \frac{X_{20}}{\omega_{e0}} \quad (11.68)$$

where the subscript 0 indicates the rated-frequency value.

The transformed voltage equations Eqs. C.63 through C.68 become

$$v_D = R_a i_D - \omega_e \lambda_Q \quad (11.69)$$

$$v_Q = R_a i_Q + \omega_e \lambda_D \quad (11.70)$$

$$0 = R_{aR} i_{DR} - (\omega_e - \omega_{me}) \lambda_{QR} \quad (11.71)$$

$$0 = R_{aR} i_{QR} + (\omega_e - \omega_{me}) \lambda_{DR} \quad (11.72)$$

where one can show that the resistances are related to those of the equivalent circuit as

$$R_a = R_1 \quad (11.73)$$

and

$$R_{aR} = R_2 \quad (11.74)$$

For the purposes of developing a field-oriented-control scheme, we will begin with the torque expression of Eq. C.70

$$T_{\text{mech}} = \frac{3}{2} \left(\frac{\text{poles}}{2} \right) \left(\frac{L_m}{L_R} \right) (\lambda_{DR} i_q - \lambda_{QR} i_d) \quad (11.75)$$

For the derivation of the dq0 transformation in Section C.3, the angular velocity of the reference frame was chosen to the synchronous speed as determined by the stator electrical frequency ω_e . It was not necessary for the purposes of the derivation to specify the absolute angular location of the reference frame. It is convenient at this point to choose the direct axis of the reference frame aligned with the rotor flux.

If this is done

$$\lambda_{QR} = 0 \quad (11.76)$$

and the torque expression of Eq. 11.75 becomes

$$T_{\text{mech}} = \frac{3}{2} \left(\frac{\text{poles}}{2} \right) \left(\frac{L_m}{L_R} \right) \lambda_{DR} i_Q \quad (11.77)$$

From Eq. 11.71 we see that

$$i_{DR} = 0 \quad (11.78)$$

and thus

$$\lambda_{DR} = L_m i_D \quad (11.79)$$

and

$$\lambda_D = L_S i_D \quad (11.80)$$

From Eqs. 11.79 and 11.80 we see that by choosing set $\lambda_{QR} = 0$ and thus aligning the synchronously rotating reference frame with the axis of the rotor flux, the direct-axis rotor flux (which is, indeed, the total rotor flux) as well as the direct-axis flux are determined by the direct-axis component of the armature current. Notice the direct analogy with a dc motor. In a dc motor, the field- and direct-axis armature fluxes are determined by the field current and in this field-oriented control scheme, the rotor and direct-axis armature fluxes are determined by the direct-axis armature current. In other words, in this field-oriented control scheme, the direct-axis component of armature current serves the same function as the field current in a dc machine.

The torque equation, Eq. 11.77, completes the analogy with the dc motor. We see that once the rotor direct-axis flux λ_{DR} is set by the direct-axis armature current, the

torque is then determined by the quadrature-axis armature current just as the torque is determined by the armature current in a dc motor.

In a practical implementation of the technique which we have derived, the direct- and quadrature-axis currents i_D and i_Q must be transformed into the three motor phase currents $i_a(t)$, $i_b(t)$, and $i_c(t)$. This can be done using the inverse dq0 transformation of Eq. C.48 which requires knowledge of θ_S , the electrical angle between the axis of phase a , and the direct-axis of the synchronously rotating reference frame.

Since it is not possible to measure the axis of the rotor flux directly, it is necessary to calculate θ_S , where $\theta_S = \omega_e t + \theta_0$ as given by Eq. C.46. Solving Eq. 11.72 for ω_e gives

$$\omega_e = \omega_{me} - R_{aR} \left(\frac{i_{QR}}{\lambda_{DR}} \right) \quad (11.81)$$

From Eq. 11.65 with $\lambda_{QR} = 0$ we see that

$$i_{QR} = - \left(\frac{L_m}{L_R} \right) i_Q \quad (11.82)$$

Eq. 11.82 in combination with Eq. 11.79 then gives

$$\omega_e = \omega_{me} + \frac{R_{aR}}{L_R} \left(\frac{i_Q}{i_D} \right) = \omega_{me} + \frac{1}{\tau_R} \left(\frac{i_Q}{i_D} \right) \quad (11.83)$$

where $\tau_R = L_R/R_{aR}$ is the rotor time constant.

We can now integrate Eq. 11.83 to find

$$\hat{\theta}_S = \left[\omega_{me} + \frac{1}{\tau_R} \left(\frac{i_Q}{i_D} \right) \right] t + \theta_0 \quad (11.84)$$

where $\hat{\theta}_S$ indicates the calculated value of θ_S (often referred to as the *estimated value* of θ_S). In the more general dynamic sense

$$\hat{\theta}_S = \int_0^t \left[\omega_{me} + \frac{1}{\tau_R} \left(\frac{i_Q}{i_D} \right) \right] dt' + \theta_0 \quad (11.85)$$

Note that both Eqs. 11.84 and 11.85 require knowledge of θ_0 , the value of θ_S at $t = 0$. Although we will not prove it here, it turns out that in a practical implementation, the effects of an error in this initial angle decay to zero with time, and hence it can be set to zero without any loss of generality.

Figure 11.21a shows a block diagram of a field-oriented torque-control system for an induction machine. The block labeled “Estimator” represents the calculation of Eq. 11.85 which calculates the estimate of θ_S required by the transformation from dq0 to abc variables.

Note that a speed sensor is required to provide the rotor speed measurement required by the estimator. Also notice that the estimator requires knowledge of the rotor time constant $\tau_R = L_R/R_{aR}$. In general, this will not be known exactly, both due to uncertainty in the machine parameters as well as due to the fact that the rotor

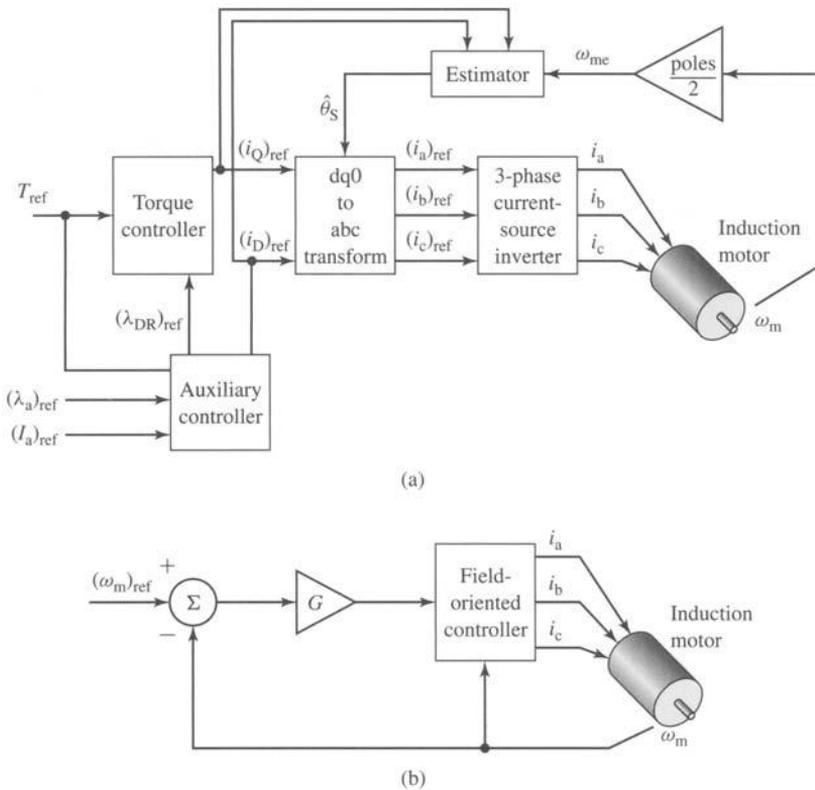


Figure 11.21 (a) Block diagram of a field-oriented torque-control system for an induction motor. (b) Block diagram of an induction-motor speed-control loop built around a field-oriented torque control system.

resistance R_{aR} will undoubtedly change with temperature as the motor is operated. It can be shown that errors in τ_R result in an offset in the estimate of θ_S , which in turn will result in an error in the estimate for the position of the rotor flux with the result that the applied armature currents will not be exactly aligned with the direct- and quadrature-axes. The torque controller will still work basically as expected, although there will be corresponding errors in the torque and rotor flux.

As with the synchronous motor, the rms armature flux-linkages can be found from Eq. 11.38 as

$$(\lambda_a)_{rms} = \sqrt{\frac{\lambda_D^2 + \lambda_Q^2}{2}} \quad (11.86)$$

Combining Eqs. 11.63 and 11.82 gives

$$\lambda_Q = L_S i_Q + L_m i_{QR} = \left(L_S - \frac{L_m^2}{L_R} \right) i_Q \quad (11.87)$$

Substituting Eqs. 11.80 and 11.87 into Eq. 11.86 gives

$$(\lambda_a)_{\text{rms}} = \sqrt{\frac{L_S^2 i_D^2 + \left(L_S - \frac{L_m^2}{L_R}\right)^2 i_Q^2}{2}} \quad (11.88)$$

Finally, as discussed in the footnote to Eq. 11.37, the rms line-to-neutral armature voltage can be found as

$$\begin{aligned} V_a &= \sqrt{\frac{v_D^2 + v_Q^2}{2}} = \sqrt{\frac{(R_a i_D - \omega_e \lambda_Q)^2 + (R_a i_Q + \omega_e \lambda_D)^2}{2}} \\ &= \sqrt{\frac{\left(R_a i_D - \omega_e \left(L_S - \frac{L_m^2}{L_R}\right) i_Q\right)^2 + (R_a i_Q + \omega_e L_S i_D)^2}{2}} \end{aligned} \quad (11.89)$$

These equations show that the armature flux linkages and terminal voltage are determined by both the direct- and quadrature-axis components of the armature current. Thus, the block marked “Auxiliary Controller” in Fig. 11.21a, which calculates the reference values for the direct- and quadrature-axis currents, must calculate the reference currents $(i_D)_{\text{ref}}$ and $(i_Q)_{\text{ref}}$ which achieve the desired torque subject to constraints on armature flux linkages (to avoid saturation in the motor), armature current, $(I_a)_{\text{rms}} = \sqrt{(i_D^2 + i_Q^2)/2}$ (to avoid excessive armature heating) and armature voltage (to avoid potential insulation damage).

Note that, as we discussed with regard to synchronous machines in Section 11.2.2, the torque-control system of Fig. 11.21a is typically imbedded within a larger control loop. One such example is the speed-control loop of Fig. 11.21b.

EXAMPLE 11.12

The three-phase, 230-V, 60-Hz, 12-kW, four-pole induction motor of Example 6.7 and Example 11.11 is to be driven by a field-oriented speed-control system (similar to that of Fig. 11.21b) at a speed of 1740 r/min. Assuming the controller is programmed to set the rotor flux linkages λ_{DR} to the machine rated peak value, find the rms amplitude of the armature current, the electrical frequency, and the rms terminal voltage, if the electromagnetic power is 9.7 kW and the motor is operating at a speed of 1680 r/min.

■ Solution

We must first determine the parameters for this machine. From Eqs. 11.66 through Eq. 11.74

$$L_m = \frac{X_{m0}}{\omega_{e0}} = \frac{18.7}{120\pi} = 49.6 \text{ mH}$$

$$L_S = L_m + \frac{X_{10}}{\omega_{e0}} = 49.6 \text{ mH} + \frac{0.680}{120\pi} = 51.41 \text{ mH}$$

$$L_R = L_m + \frac{X_{20}}{\omega_{e0}} = 49.6 \text{ mH} + \frac{0.672}{120\pi} = 51.39 \text{ mH}$$

$$R_a = R_1 = 0.095 \Omega$$

$$R_{aR} = R_2 = 0.2 \Omega$$

The rated rms line-to-neutral terminal voltage for this machine is $230/\sqrt{3} = 132.8$ V and thus the peak rated flux for this machine is

$$(\lambda_{\text{rated}})_{\text{peak}} = \frac{\sqrt{2}(V_a)_{\text{rated}}}{\omega_e} = \frac{\sqrt{2} \times 132.8}{120\pi} = 0.498 \text{ Wb}$$

For the specified operating condition

$$\omega_m = n \left(\frac{\pi}{30} \right) = 1680 \left(\frac{\pi}{30} \right) = 176 \text{ rad/sec}$$

and the mechanical torque is

$$T_{\text{mech}} = \frac{P_{\text{mech}}}{\omega_m} = \frac{9.7 \times 10^3}{176} = 55.1 \text{ N} \cdot \text{m}$$

From Eq. 11.77, with $\lambda_{\text{DR}} = \lambda_{\text{rated}} = 0.498$ Wb

$$\begin{aligned} i_Q &= \frac{2}{3} \left(\frac{2}{\text{poles}} \right) \left(\frac{L_R}{L_m} \right) \left(\frac{T_{\text{mech}}}{\lambda_{\text{DR}}} \right) \\ &= \frac{2}{3} \left(\frac{2}{4} \right) \left(\frac{51.39 \times 10^{-3}}{49.6 \times 10^{-3}} \right) \left(\frac{55.1}{0.498} \right) = 38.2 \text{ A} \end{aligned}$$

From Eq. 11.79,

$$i_D = \frac{\lambda_{\text{DR}}}{L_m} = \frac{0.498}{49.6 \times 10^{-3}} = 10.0 \text{ A}$$

The rms armature current is thus

$$I_a = \sqrt{\frac{i_D^2 + i_Q^2}{2}} = \sqrt{\frac{10.0^2 + 38.2^2}{2}} = 27.9 \text{ A}$$

The electrical frequency can be found from Eq. 11.81

$$\omega_e = \omega_{\text{me}} + \frac{R_{\text{aR}}}{L_R} \left(\frac{i_Q}{i_D} \right)$$

With $\omega_{\text{me}} = (\text{poles}/2)\omega_m = 2 \times 176 = 352$ rad/sec

$$\omega_e = 352 + \left(\frac{0.2}{51.39 \times 10^{-3}} \right) \left(\frac{38.2}{10.0} \right) = 367 \text{ rad/sec}$$

and $f_e = \omega_e/(2\pi) = 58.4$ Hz.

Finally, from Eq. 11.89, the rms line-to-neutral terminal voltage

$$\begin{aligned} V_a &= \sqrt{\frac{\left(R_a i_D - \omega_e \left(L_S - \frac{L_m^2}{L_R} \right) i_Q \right)^2 + (R_a i_Q + \omega_e L_S i_D)^2}{2}} \\ &= 140.6 \text{ V line-to-neutral} = 243.6 \text{ V line-to-line} \end{aligned}$$

Practice Problem 11.12

Consider again the induction motor and field-oriented-control system of Example 11.12. Assume that the speed is readjusted to 1700 r/min and that the electromagnetic power is known to increase to 10.0 kW. Find the rms amplitude of the armature current, the electrical frequency, and the rms terminal voltage for this new operating condition.

Solution

$$\text{Armature current} = 28.4 \text{ A}$$

$$f_e = 59.1 \text{ Hz}$$

$$\text{Terminal voltage} = 142.5 \text{ V line-to-neutral} = 246.9 \text{ V line-to-line}$$

The ability to independently control the rotor flux and the torque has important control implications. Consider, for example, the response of the direct-axis rotor flux to a change in direct-axis current. Equation C.66, with $\lambda_{qR} = 0$, becomes

$$0 = R_{aR}i_{dR} + \frac{d\lambda_{dR}}{dt} \quad (11.90)$$

Substituting for i_{dR} in terms of λ_{dR}

$$i_{dR} = \frac{\lambda_{dR} - L_m i_d}{L_R} \quad (11.91)$$

gives a differential equation for the rotor flux linkages λ_{dR}

$$\frac{d\lambda_{dR}}{dt} + \left(\frac{R_{aR}}{L_R} \right) \lambda_{dR} = \left(\frac{L_m}{L_R} \right) i_d \quad (11.92)$$

From Eq. 11.92 we see that the response of the rotor flux to a step change in direct-axis current i_d is relatively slow; λ_{dR} will change exponentially with the rotor time constant of $\tau_R = L_R/R_{aR}$. Since the torque is proportional to the product $\lambda_{dR}i_q$ we see that fast torque response will be obtained from changes in i_q . Thus, for example, to implement a step change in torque, a practical control algorithm might start with a step change in $(i_Q)_{\text{ref}}$ to achieve the desired torque change, followed by an adjustment in $(i_D)_{\text{ref}}$ (and hence λ_{dR}) to readjust the armature current or terminal voltage as desired. This adjustment in $(i_D)_{\text{ref}}$ would be coupled with a compensating adjustment in $(i_Q)_{\text{ref}}$ to maintain the torque at its desired value.

EXAMPLE 11.13

Consider again the induction motor of Example 11.12. Assuming the motor speed and electromagnetic power remain constant (at 1680 r/min and 9.7 kW), use MATLAB to plot the per-unit armature current I_a and terminal voltage V_a as a function of i_D as $(\lambda_{dR})_{\text{ref}}$ is varied between 0.8 and 1.2 per unit, where 1.0 per unit corresponds to the rated peak value.

■ Solution

The desired plot is given in Fig. 11.22. Note that the armature current decreases and the terminal voltage increases as λ_{dR} is increased. This clearly shows how i_D , which controls λ_{dR} , can be

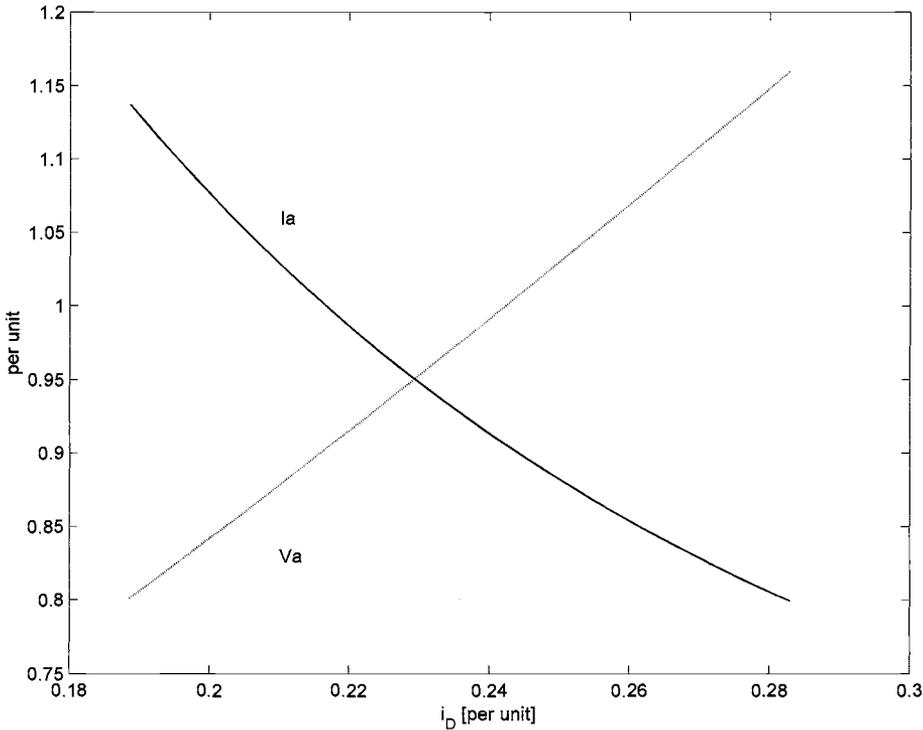


Figure 11.22 MATLAB plot for Example 11.13 showing the effect of the direct-axis current i_D on the armature voltage and current for an induction motor at constant speed and load.

chosen to optimize the tradeoff between such quantities as armature current, armature flux linkages, and terminal voltage.

Here is the MATLAB script:

```
clc
clear

%Motor rating and characteristics
Prated = 12e3;
Vrated = 230;
Varated = 230/sqrt(3);
ferated = 60;
omegaerated = 2*pi*ferated;
Lambdarated = sqrt(2)*Varated/omegaerated;
Irated = Prated/(sqrt(3)*Vrated);
Ipeakbase = sqrt(2)*Irated;
poles = 4;

%Here are the 60-Hz motor parameters
```

```

V10 = Vrated/sqrt(3);
X10 = 0.680;
X20 = 0.672;
Xm0 = 18.7;
R1 = 0.095;
R2 = 0.2;

%Calculate required dq0 parameters
Lm = Xm0/omegaerated;
LS = Lm + X10/omegaerated;
LR = Lm + X20/omegaerated;
Ra = R1;
RaR = R2;

% Operating point
n = 1680;
omegam = n*pi/30;
omegame = (poles/2)*omegam;
Pmech = 9.7e3;
Tmech = Pmech/omegam;

% Loop to plot over lambdaDR
for n = 1:41
    lambdaDR = (0.8 + (n-1)*0.4/40)*Lambdarated;
    lambdaDRpu(n) = lambdaDR/Lambdarated;
    iQ = (2/3)*(2/poles)*(LR/Lm)*(Tmech/lambdaDR);
    iD = (lambdaDR/Lm);
    iDpu(n) = iD/Ipeakbase;
    iQR = - (Lm/LR)*iQ;
    Ia = sqrt((iD^2 + iQ^2)/2);
    Iapu(n) = Ia/Irated;
    omegae = omegame - (RaR/LR)*(iQ/iD);
    fe(n) = omegae/(2*pi);
    Varms = sqrt(((Ra*iD-omegae*(LS-Lm^2/LR)*iQ)^2 + ...
        (Ra*iQ+ omegae*LS*iD)^2)/2);
    Vapu(n) = Varms/Varated;
end

%Now plot
plot(iDpu,Iapu)
hold
plot(iDpu,Vapu,':')
hold
xlabel('i_D [per unit]')
ylabel('per unit')
text(.21,1.06,'Ia')
text(.21,.83,'Va')

```

11.4 CONTROL OF VARIABLE-RELUCTANCE MOTORS

Unlike dc and ac (synchronous or induction) machines, VRMs cannot be simply “plugged in” to a dc or ac source and then be expected to run. As is discussed in Chapter 8, the phases must be excited with (typically unipolar) currents, and the timing of these currents must be carefully correlated with the position of the rotor poles in order to produce a useful, time-averaged torque. The result is that although the VRM itself is perhaps the simplest of rotating machines, a practical VRM drive system is relatively complex.

VRM drive systems are competitive only because this complexity can be realized easily and inexpensively through power and microelectronic circuitry. These drive systems require a fairly sophisticated level of controllability for even the simplest modes of VRM operation. Once the capability to implement this control is available, fairly sophisticated control features can be added (typically in the form of additional software) at little additional cost, further increasing the competitive position of VRM drives.

In addition to the VRM itself, the basic VRM drive system consists of the following components: a rotor-position sensor, a controller, and an inverter. The function of the rotor-position sensor is to provide an indication of shaft position which can be used to control the timing and waveform of the phase excitation. This is directly analogous to the timing signal used to control the firing of the cylinders in an automobile engine.

The controller is typically implemented in software in microelectronic (micro-processor) circuitry. Its function is to determine the sequence and waveforms of the phase excitation required to achieve the desired motor speed-torque characteristics. In addition to set points of desired speed and/or torque and shaft position (from the shaft-position sensor), sophisticated controllers often employ additional inputs including shaft-speed and phase-current magnitude. Along with the basic control function of determining the desired torque for a given speed, the more sophisticated controllers attempt to provide excitations which are in some sense optimized (for maximum efficiency, stable transient behavior, etc.).

The control circuitry consists typically of low-level electronics which cannot be used to directly supply the currents required to excite the motor phases. Rather its output consists of signals which control an inverter which in turn supplies the phase currents. Control of the VRM is achieved by the application of an appropriate set of currents to the VRM phase windings.

Figures 11.23a to c show three common configurations found in inverter systems for driving VRMs. Note that these are simply H-bridge inverters of the type discussed in Section 10.3. Each inverter is shown in a two-phase configuration. As is clear from the figures, extension of each configuration to drive additional phases can be readily accomplished.

The configuration of Fig. 11.23a is perhaps the simplest. Closing switches S_{1a} and S_{1b} connects the phase-1 winding across the supply ($v_1 = V_0$) and causes the winding current to increase. Opening just one of the switches forces a short across

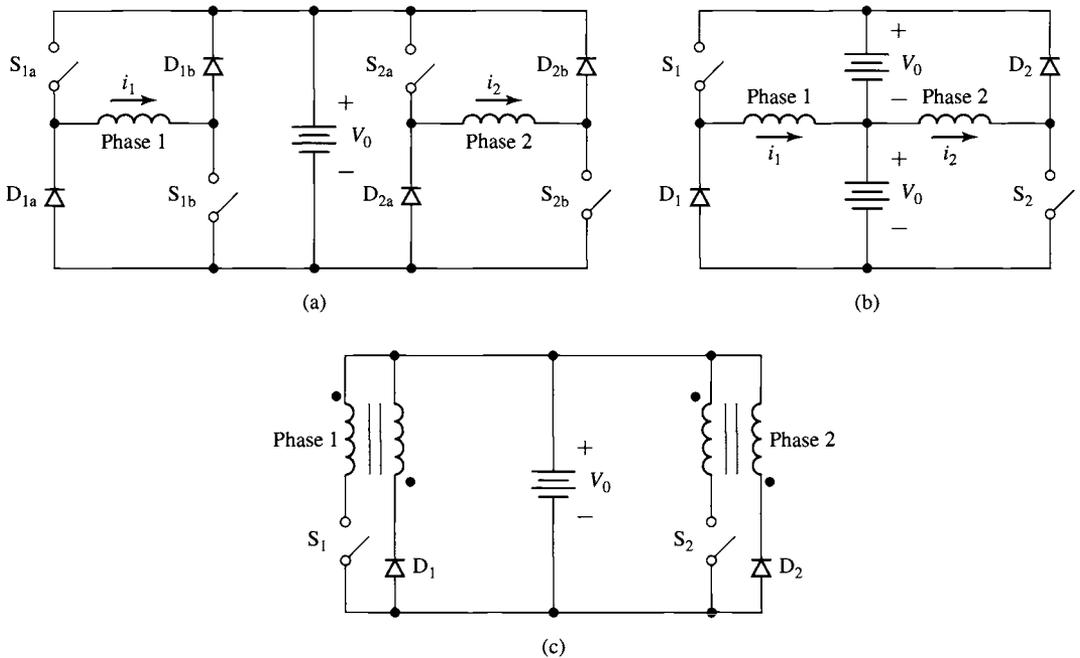


Figure 11.23 Inverter configurations. (a) Two-phase inverter which uses two switches per phase. (b) Two-phase inverter which uses a split supply and one switch per phase. (c) Two-phase inverter with bifilar phase windings and one switch per phase.

the winding and the current will decay, while opening both switches connects the winding across the supply with negative polarity through the diodes ($v_1 = -V_0$) and the winding current will decay more rapidly. Note that this configuration is capable of regeneration (returning energy to the supply), but not of supplying negative current to the phase winding. However, since the torque in a VRM is proportional to the square of the phase current, there is no need for negative winding current.

As discussed in Section 10.3.2, the process of pulse-width modulation, under which a series of switch configurations alternately charge and discharge a phase winding, can be used to control the average winding current. Using this technique, an inverter such as that of Fig. 11.23a can readily be made to supply the range of waveforms required to drive a VRM.

The inverter configuration of Fig. 11.23a is perhaps the simplest of H-bridge configurations which provide regeneration capability. Its main disadvantage is that it requires two switches per phase. In many applications, the cost of the switches (and their associated drive circuitry) dominates the cost of the inverter, and the result is that this configuration is less attractive in terms of cost when compared to other configurations which require one switch per phase.

Figure 11.23b shows one such configuration. This configuration requires a split supply (i.e., two supplies of voltage V_0) but only a single switch and diode per phase.

Closing switch S1 connects the phase-1 winding to the upper dc source. Opening the switch causes the phase current to transfer to diode D1, connecting the winding to the bottom dc source. Phase 1 is thus supplied by the upper dc source and regenerates through the bottom source. Note that to maintain symmetry and to balance the energy supplied from each source equally, phase 2 is connected oppositely so that it is supplied from the bottom source and regenerates into the top source.

The major disadvantages of the configuration of Fig. 11.23b are that it requires a split supply and that when the switch is opened, the switch must withstand a voltage of $2V_0$. This can be readily seen by recognizing that when diode D1 is forward-biased, the switch is connected across the two supplies. Such switches are likely to be more expensive than the switches required by the configuration of Fig. 11.23a. Both of these issues will tend to offset some of the economic advantage which can be gained by the elimination of one switch and one diode as compared with the inverter circuit of Fig. 11.23a.

A third inverter configuration is shown in Fig. 11.23c. This configuration requires only a single dc source and uses only a single switch and diode per phase. This configuration achieves regeneration through the use of *bifilar* phase windings. In a bifilar winding, each phase is wound with two separate windings which are closely coupled magnetically (this can be achieved by winding the two windings at the same time) and can be thought of as the primary and secondary windings of a transformer.

When switch S1 is closed, the primary winding of phase 1 is energized, exciting the phase winding. Opening the switch induces a voltage in the secondary winding (note the polarity indicated by the dots in Fig. 11.23c) in such a direction as to forward-bias D1. The result is that current is transferred from the primary to the secondary winding with a polarity such that the current in the phase decays to zero and energy is returned to the source.

Although this configuration requires only a single dc source, it requires a switch which must withstand a voltage in excess of $2V_0$ (the degree of excess being determined by the voltage developed across the primary leakage reactance as current is switched from the primary to the secondary windings) and requires the more complex bifilar winding in the machine. In addition, the switches in this configuration must include snubbing circuitry (typically consisting of a resistor-capacitor combination) to protect them from transient overvoltages. These overvoltages result from the fact that although the two windings of the bifilar winding are wound such that they are as closely coupled as possible, perfect coupling cannot be achieved. As a result, there will be energy stored in the leakage fields of the primary winding which must be dissipated when the switch is opened.

As is discussed in Section 10.3, VRM operation requires control of the current applied to each phase. For example, one control strategy for constant torque production is to apply constant current to each phase during the time that $dL/d\theta_m$ for that phase is constant. This results in constant torque proportional to the square of the phase-current magnitude. The magnitude of the torque can be controlled by changing the magnitude of the phase current.

The control required to drive the phase windings of a VRM is made more complex because the phase-winding inductances change both with rotor position and

with current levels due to saturation effects in the magnetic material. As a result, it is not possible in general to implement an open-loop PWM scheme based on a precalculated algorithm. Rather, pulse-width-modulation is typically accomplished through the use of current feedback. The instantaneous phase current can be measured and a switching scheme can be devised such that the switch can be turned off when the current has been found to reach a desired maximum value and turned on when the current decays to a desired minimum value. In this manner the average phase current is controlled to a predetermined function of the rotor position and desired torque.

This section has provided a brief introduction to the topic of drive systems for variable-reluctance machines. In most cases, many additional issues must be considered before a practical drive system can be implemented. For example, accurate rotor-position sensing is required for proper control of the phase excitation, and the control loop must be properly compensated to ensure its stability. In addition, the finite rise and fall times of current buildup in the motor phase windings will ultimately limit the maximum achievable rotor torque and speed.

The performance of a complete VRM drive system is intricately tied to the performance of all its components, including the VRM, its controller, and its inverter. In this sense, the VRM is quite different from the induction, synchronous, and dc machines discussed earlier in this chapter. As a result, it is useful to design the complete drive system as an integrated package and not to design the individual components (VRM, inverter, controller, etc.) separately. The inverter configurations of Fig. 11.23 are representative of a number of possible inverter configurations which can be used in VRM drive systems. The choice of an inverter for a specific application must be made based on engineering and economic considerations as part of an integrated VRM drive system design.

11.5 SUMMARY

This chapter introduces various techniques for the control of electric machines. The broad topic of electric machine control requires a much more extensive discussion than is possible here so our objectives have been somewhat limited. Most noticeably, the discussion of this chapter focuses almost exclusively on steady-state behavior, and the issues of transient and dynamic behavior are not considered.

Much of the control flexibility that is now commonly associated with electric machinery comes from the capability of the power electronics that is used to drive these machines. This chapter builds therefore on the discussion of power electronics in Chapter 10.

The starting point is a discussion of dc motors for which it is convenient to subdivide the control techniques into two categories: speed and torque control. The algorithm for speed control in a dc motor is relatively straight forward. With the exception of a correction for voltage drop across the armature resistance, the steady-state speed is determined by the condition that the generated voltage must be equal to the applied armature voltage. Since the generated voltage is proportional to the field

flux and motor speed, we see that the steady-state motor speed is proportional to the armature voltage and inversely proportional to the field flux.

An alternative viewpoint is that of torque control. Because the commutator/brush system maintains a constant angular relationship between the field and armature flux, the torque in a dc motor is simply proportional to the product of the armature current and the field flux. As a result, dc motor torque can be controlled directly by controlling the armature current as well as the field flux.

Because synchronous motors develop torque only at synchronous speed, the speed of a synchronous motor is simply determined by the electrical frequency of the applied armature excitation. Thus, steady-state speed control is simply a matter of armature frequency control. Torque control is also possible. By transforming the stator quantities into a reference frame rotating synchronously with the rotor (using the dq0 transformation of Appendix C), we found that torque is proportional to the field flux and the component of armature current in space quadrature with the field flux. This is directly analogous to the torque production in a dc motor. Control schemes which adopt this viewpoint are referred to as *vector* or *field-oriented* control.

Induction machines operate asynchronously; rotor currents are induced by the relative motion of the rotor with respect to the synchronously rotating stator-produced flux wave. When supplied by a constant-frequency source applied to the armature winding, the motor will operate at a speed somewhat lower than synchronous speed, with the motor speed decreasing as the load torque is increased. As a result, precise speed regulation is not a simple matter, although in most cases the speed will not vary from synchronous speed by an excessive amount.

Analogous to the situation in a synchronous motor, in spite of the fact that the rotor of an induction motor rotates at less than synchronous speed, the interaction between the rotor and stator flux waves is indeed synchronous. As a result, a transformation into a synchronously rotating reference frame results in rotor and stator flux waves which are constant. The torque can then be expressed in terms of the product of the rotor flux linkages and the component of armature current in quadrature with the rotor flux linkages (referred to as the *quadrature-axis component* of the armature current) in a fashion directly analogous to the field-oriented viewpoint of a synchronous motor. Furthermore, it can be shown that the rotor flux linkages are proportional to the direct-axis component of the armature current, and thus the direct-axis component of armature current behaves much like the field current in a synchronous motor. This field-oriented viewpoint of induction machine control, in combination with the power-electronic and control systems required to implement it, has led to the widespread applicability of induction machines to a wide range of variable-speed applications.

Finally, this chapter ends with a brief discussion of the control of variable-reluctance machines. To produce useful torque, these machines typically require relatively complex, nonsinusoidal current waveforms whose shape must be controlled as a function of rotor position. Typically, these waveforms are produced by pulse-width modulation combined with current feedback using an H-bridge inverter of the type

discussed in Chapter 10. The details of these waveforms depend heavily upon the geometry and magnetic properties of the VRM and can vary significantly from motor to motor.

11.6 BIBLIOGRAPHY

Many excellent books are available which provide a much more thorough discussion of electric-machinery control than is possible in the introductory discussion presented here. This bibliography lists a few of the many textbooks available for readers who wish to study this topic in more depth.

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11.7 PROBLEMS

- 11.1** When operating at rated voltage, a 3-kW, 120-V, 1725 r/min separately excited dc motor achieves a no-load speed of 1718 r/min at a field current of 0.70 A. The motor has an armature resistance of 145 m Ω and a shunt-field resistance of 104 Ω . For the purposes of this problem you may assume the rotational losses to be negligible.

This motor will control the speed of a load whose torque is constant at 15.2 N·m over the speed range of 1500–1800 r/min. The motor will be operated at a constant armature voltage of 120 V. The field-winding will be supplied from the 120-V dc armature supply via a pulse-width modulation

system, and the motor speed will be varied by varying the duty cycle of the pulse-width modulation.

- Calculate the field current required to achieve operation at 15.2 N·m torque and 1800 r/min. Calculate the corresponding PWM duty cycle D .
- Calculate the field current required to achieve operation at 15.2 N·m torque and 1500 r/min. Calculate the corresponding PWM duty cycle.
- Plot the required PWM duty cycle as a function of speed over the desired speed range of 1500 to 1800 r/min.

11.2 Repeat Problem 11.1 for a load whose torque is 15.2 N·m at 1600 r/min and which varies as the speed to the 1.8 power.

11.3 The dc motor of Problem 11.1 has a field-winding inductance $L_f = 3.7$ H and a moment of inertia $J = 0.081$ kg·m². The motor is operating at rated terminal voltage and an initial speed of 1300 r/min.

- Calculate the initial field current I_f and duty cycle D .
At time $t = 0$, the PWM duty cycle is suddenly switched from the value found in part (a) to $D = 0.60$.
- Calculate the final values of the field current and motor speed after the transient has died out.
- Write an expression for the field-current transient as a function of time.
- Write a differential equation for the motor speed as a function of time during this transient.

11.4 A shunt-connected 240-V, 15-kW, 3000 r/min dc motor has the following parameters



Field resistance:	$R_f = 132 \Omega$
Armature resistance:	$R_a = 0.168 \Omega$
Geometric constant:	$K_f = 0.422 \text{ V}/(\text{A}\cdot\text{rad}/\text{sec})$

When operating at rated voltage, no-load, the motor current is 1.56 A.

- Calculate the no-load speed and rotational loss.
- Assuming the rotational loss to be constant, use MATLAB to plot the motor output power as a function of speed. Limit your plot to a maximum power output of 15 kW.
- Armature-voltage control is to be used to maintain constant motor speed as the motor is loaded. For this operating condition, the shunt field voltage will be held constant at 240-V. Plot the armature voltage as a function of power output required to maintain the motor at a constant speed of 2950 r/min.
- Consider that the situation for armature-voltage control is applied to this motor while the field winding remains connected in shunt across the armature terminals. Repeat part (c) for this operating condition. Is such operation feasible? Why is the motor behavior significantly different from that in part (c)?



11.5 The data sheet for a small permanent-magnet dc motor provides the following parameters:

Rated voltage:	$V_{\text{rated}} = 3 \text{ V}$
Rated output power:	$P_{\text{rated}} = 0.28 \text{ W}$
No-load speed:	$n_{\text{nl}} = 12,400 \text{ r/min}$
Torque constant:	$K_m = 0.218 \text{ mV}/(\text{r/min})$
Stall torque:	$T_{\text{stall}} = 0.094 \text{ oz}\cdot\text{in}$

- Calculate the motor armature resistance.
 - Calculate the no-load rotational loss.
 - Assume the motor to be connected to a load such that the total shaft power (actual load plus rotational loss) is equal 0.25 W at a speed of $12,000 \text{ r/min}$. Assuming this load to vary as the square of the motor speed, write a MATLAB script to plot the motor speed as a function of terminal voltage for $1.0 \text{ V} \leq V_a \leq 3.0 \text{ V}$.
- 11.6** The data sheet for a 350-W permanent-magnet dc motor provides the following parameters:

Rated voltage:	$V_{\text{rated}} = 24 \text{ V}$
Armature resistance:	$R_a = 97 \text{ m}\Omega$
No-load speed:	$n_{\text{nl}} = 3580 \text{ r/min}$
No-load current:	$I_{a,\text{nl}} = 0.47 \text{ A}$

- Calculate the motor torque-constant K_m in $\text{V}/(\text{rad}/\text{sec})$.
- Calculate the no-load rotational loss.
- The motor is supplied from a 30-V dc supply through a PWM inverter. Table 11.1 gives the measured motor current as a function of the PWM duty cycle D .

Complete the table by calculating the motor speed and the load power for each value of D . Assume that the rotational losses vary as the square of the motor speed.

Table 11.1 Motor-performance data for Problem 11.6.

D	I_a (A)	r/min	P_{load} (W)
0.80	13.35		
0.75	12.70		
0.70	12.05		
0.65	11.40		
0.60	10.70		
0.55	10.05		
0.50	9.30		

- 11.7** The motor of Problem 11.5 has a moment of inertia of 6.4×10^{-7} oz·in·sec². Assuming it is unloaded and neglecting any effects of rotational loss, calculate the time required to achieve a speed of 12,000 r/min if the motor is supplied by a constant armature current of 100 mA.
- 11.8** An 1100-W, 150-V, 3000-r/min permanent-magnet dc motor is to be operated from a current-source inverter so as to provide direct control of the motor torque. The motor torque constant is $K_m = 0.465$ V/(rad/sec); its armature resistance is 1.37Ω . The motor rotational loss at a speed of 3000 r/min is 87 W. Assume that the rotational loss can be represented by a constant loss torque as the motor speed varies.
- Calculate the rated armature current of this motor. What is the corresponding mechanical torque in N·m?
 - The current source supplies a current of 6.2 A to the motor armature, and the motor speed is measured to be 2670 r/min. Calculate the load torque and power.
 - Assume the load torque of part (b) to vary linearly with speed and the motor and load to have a combined inertia of 2.28×10^{-3} kg·m². Calculate the motor speed as a function of time if the armature current is suddenly increased to 7.0 A.
- 11.9** The permanent-magnet dc motor of Problem 11.8 is operating at its rated speed of 3000 r/min and no load. If rated current is suddenly applied to the motor armature in such a direction as to slow the motor down, how long will it take the motor to reach zero speed? The inertia of the motor alone is 1.86×10^{-3} kg·m². Ignore the effects of rotational loss.
- 11.10** A 1100-kVA, 4600-V, 60-Hz, three-phase, four-pole synchronous motor is to be driven from a variable-frequency, three-phase, constant V/Hz inverter rated at 1250-kVA. The synchronous motor has a synchronous reactance of 1.18 per unit and achieves rated open-circuit voltage at a field current of 85 A.
- Calculate the rated speed of the motor in r/min.
 - Calculate the rated current of the motor.
 - With the motor operating at rated voltage and speed and an input power of 1000-kW, calculate the field current required to achieve unity-power-factor operation.
The load power of part (c) varies as the speed to the 2.5 power. With the motor field-current held fixed, the inverter frequency is reduced such that the motor is operating at a speed of 1300 r/min.
 - Calculate the inverter frequency and the motor input power and power factor.
 - Calculate the field current required to return the motor to unity power factor.



11.11 Consider a three-phase synchronous motor for which you are given the following data:

- Rated line-to-line voltage (V)
- Rated volt-amperes (VA)
- Rated frequency (Hz) and speed (r/min)
- Synchronous reactance in per unit
- Field current at rated open-circuit voltage (AFNL) (A)

The motor is to be operated from a variable-frequency, constant V/Hz inverter at speeds of up to 120 percent of the motor-rated speed.

- a. Under the assumption that the motor terminal voltage and current cannot exceed their rated values, write a MATLAB script which calculates, for a given operating speed, the motor terminal voltage, the maximum possible motor input power, and the corresponding field current required to achieve this operating condition. You may consider the effects of saturation and armature resistance to be negligible.
- b. Exercise your program on the synchronous motor of Problem 11.10 for motor speeds of 1500 and 2000 r/min.



11.12 For the purposes of performing field-oriented control calculations on non-salient synchronous motors, write a MATLAB script that will calculate the synchronous inductance L_s and armature-to-field mutual inductance L_{af} , both in henries, and the rated torque in N·m, given the following data:

- Rated line-to-line voltage (V)
- Rated (VA)
- Rated frequency (Hz)
- Number of poles
- Synchronous reactance in per unit
- Field current at rated open-circuit voltage (AFNL) (A)

11.13 A 100-kW, 460-V, 60-Hz, four-pole, three-phase synchronous machine is to be operated as a synchronous motor under field-oriented torque control using a system such as that shown in Fig. 11.13a. The machine has a synchronous reactance of 0.932 per unit and $AFNL = 15.8$ A. The motor is operating at rated speed, loaded to 50 percent of its rated torque at a field current of 14.0 A with the field-oriented controller set to maintain $i_D = 0$.

- a. Calculate the synchronous inductance L_s and armature-to-field mutual inductance L_{af} , both in henries.
- b. Find the quadrature-axis current i_Q and the corresponding rms magnitude of the armature current i_a .
- c. Find the motor line-to-line terminal voltage.

11.14 The synchronous motor of Problem 11.13 is operating under field-oriented torque control such that $i_D = 0$. With the field current set equal to 14.5 A and

with the torque reference set equal to 0.75 of the motor rated torque, the motor speed is observed to be 1475 r/min.

- a. Calculate the motor output power.
 - b. Find the quadrature-axis current i_Q and the corresponding rms magnitude of the armature current i_a .
 - c. Calculate the stator electrical frequency
 - d. Find the motor line-to-line terminal voltage.
- 11.15** Consider the case in which the load on the synchronous motor in the field-oriented torque-control system of Problem 11.13 is increased, and the motor begins to slow down. Based upon some knowledge of the load characteristic, it is determined that it will be necessary to raise the torque set point T_{ref} from 50 percent to 80 percent of the motor-rated torque to return the motor to its rated speed.
- a. If the field current were left unchanged at 14.0 A, calculate the values of quadrature-axis current, rms armature current, and motor line-to-line terminal voltage (in V and in per unit) which would result in response to this change in reference torque.
 - b. To achieve this operating condition with reasonable armature terminal voltage, the field-oriented control algorithm is changed to the unity-power-factor algorithm described in the text prior to Example 11.9. Based upon that algorithm, calculate
 - (i) the motor terminal line-to-line terminal voltage (in V and in per unit).
 - (ii) the rms armature current.
 - (iii) the direct- and quadrature-axis currents, i_D and i_Q .
 - (iv) the motor field current.
- 11.16** Consider a 500-kW, 2300-V, 50-Hz, eight-pole synchronous motor with a synchronous reactance of 1.18 per unit and AFNL = 94 A. It is to be operated under field-oriented torque control using the unity-power-factor algorithm described in the text following Example 11.8. It will be used to drive a load whose torque varies quadratically with speed and whose torque at a speed of 750 r/min is 5900 N·m. The complete drive system will include a speed-control loop such as that shown in Fig. 11.13b.
- Write a MATLAB script whose input is the desired motor speed (up to 750 r/min) and whose output is the motor torque, the field current, the direct- and quadrature-axis currents, the armature current, and the line-to-line terminal voltage. Exercise your script for a motor speed of 650 r/min.
- 11.17** A 2-kVA, 230-V, two-pole, three-phase permanent magnet synchronous motor achieves rated open-circuit voltage at a speed of 3500 r/min. Its synchronous inductance is 17.2 mH.
- a. Calculate Λ_{PM} for this motor.
 - b. If the motor is operating at rated voltage and rated current at a speed of 3600 r/min, calculate the motor power in kW and the peak direct- and



quadrature-axis components of the armature current, i_D and i_Q respectively.

- 11.18** Field-oriented torque control is to be applied to the permanent-magnet synchronous motor of Problem 11.18. If the motor is to be operated at 4000 r/min at rated terminal voltage, calculate the maximum torque and power which the motor can supply and the corresponding values of i_D and i_Q .
- 11.19** A 15-kVA, 230-V, two-pole, three-phase permanent-magnet synchronous motor has a maximum speed of 10,000 r/min and produces rated open-circuit voltage at a speed of 7620 r/min. It has a synchronous inductance of 1.92 mH. The motor is to be operated under field-oriented torque control.
- Calculate the maximum torque the motor can produce without exceeding rated armature current.
 - Assuming the motor to be operated with the torque controller adjusted to produce maximum torque (as found in part (a)) and $i_D = 0$, calculate the maximum speed at which it can be operated without exceeding rated armature voltage.
 - To operate at speeds in excess of that found in part (b), flux weakening will be employed to maintain the armature voltage at its rated value. Assuming the motor to be operating at 10,000 r/min with rated armature voltage and current, calculate
 - the direct-axis current i_D .
 - the quadrature-axis current i_Q .
 - the motor torque.
 - the motor power and power factor.



- 11.20** The permanent magnet motor of Problem 11.17 is to be operated under vector control using the following algorithm.

Terminal voltage not to exceed rated value

Terminal current not to exceed rated value

$i_D = 0$ unless flux weakening is required to avoid excessive armature voltage

Write a MATLAB script to produce plots of the maximum power and torque which this system can produce as a function of motor speed for speeds up to 10,000 r/min.

- 11.21** Consider a 460-V, 25-kW, four-pole, 60-Hz induction motor which has the following equivalent-circuit parameters in ohms per phase referred to the stator:

$$R_1 = 0.103 \quad R_2 = 0.225 \quad X_1 = 1.10 \quad X_2 = 1.13 \quad X_m = 59.4$$

The motor is to be operated from a variable frequency, constant-V/Hz drive whose output is 460-V at 60-Hz. Neglect any effects of rotational loss. The motor drive is initially adjusted to a frequency of 60 Hz.

- a. Calculate the peak torque and the corresponding slip and motor speed in r/min.
- b. Calculate the motor torque at a slip of 2.9 percent and the corresponding output power.
- c. The drive frequency is now reduced to 35 Hz. If the load torque remains constant, estimate the resultant motor speed in r/min. Find the resultant motor slip, speed in r/min, and output power.

11.22 Consider the 460-V, 250-kW, four-pole induction motor and drive system of Problem 11.21.



- a. Write a MATLAB script to plot the speed-torque characteristic of the motor at drive frequencies of 20, 40, and 60 Hz for speeds ranging from -200 r/min to the synchronous speed at each frequency.
- b. Determine the drive frequency required to maximize the starting torque and calculate the corresponding torque in N·m.

11.23 A 550-kW, 2400-V, six-pole, 60-Hz three-phase induction motor has the following equivalent-circuit parameters in ohms-per-phase-Y referred to the stator:



$$R_1 = 0.108 \quad R_2 = 0.296 \quad X_1 = 1.18 \quad X_2 = 1.32 \quad X_m = 48.4$$

The motor will be driven by a constant-V/Hz drive whose voltage is 2400 V at a frequency of 60 Hz.

The motor is used to drive a load whose power is 525 kW at a speed of 1138 r/min and which varies as the cube of speed. Using MATLAB, plot the motor speed as a function of frequency as the drive frequency is varied between 20 and 60 Hz.

- 11.24** A 150-kW, 60-Hz, six-pole, 460-V three-phase wound-rotor induction motor develops full-load torque at a speed of 1157 r/min with the rotor short-circuited. An external noninductive resistance of $870 \text{ m}\Omega$ is placed in series with each phase of the rotor, and the motor is observed to develop its rated torque at a speed of 1072 r/min. Calculate the resistance per phase of the original motor.
- 11.25** The wound rotor of Problem 11.24 will be used to drive a constant-torque load equal to the rated full-load torque of the motor. Using the results of Problem 11.24, calculate the external rotor resistance required to adjust the motor speed to 850 r/min.
- 11.26** A 75-kW, 460-V, three-phase, four-pole, 60-Hz, wound-rotor induction motor develops a maximum internal torque of 212 percent at a slip of 16.5 percent when operated at rated voltage and frequency with its rotor short-circuited directly at the slip rings. Stator resistance and rotational losses may be neglected, and the rotor resistance may be assumed to be constant, independent of rotor frequency. Determine
- a. the slip at full load in percent.
 - b. the rotor I^2R loss at full load in watts.

- c. the starting torque at rated voltage and frequency N·m.

If the rotor resistance is now doubled (by inserting external series resistance at the slip rings), determine

- d. the torque in N·m when the stator current is at its full-load value.
e. the corresponding slip.

11.27 A 35-kW, three-phase, 440-V, six-pole wound-rotor induction motor develops its rated full-load output at a speed of 1169 r/min when operated at rated voltage and frequency with its slip rings short-circuited. The maximum torque it can develop at rated voltage and frequency is 245 percent of full-load torque. The resistance of the rotor winding is $0.23 \Omega/\text{phase}$. Neglect rotational and stray-load losses and stator resistance.

- a. Compute the rotor I^2R loss at full load.
b. Compute the speed at maximum torque.
c. How much resistance must be inserted in series with the rotor to produce maximum starting torque?

The motor is now run from a 50-Hz supply with the applied voltage adjusted so that, for any given torque, the air-gap flux wave has the same amplitude as it does when operated 60 Hz at the same torque level.

- d. Compute the 50-Hz applied voltage.
e. Compute the speed at which the motor will develop a torque equal to its rated value at 60-Hz with its slip rings short-circuited.

11.28 The three-phase, 2400-V, 550-kW, six-pole induction motor of Problem 11.23 is to be driven from a field-oriented speed-control system whose controller is programmed to set the rotor flux linkages λ_{DR} equal to the machine rated peak value. The machine is operating at 1148 r/min driving a load which is known to be 400 kW at this speed. Find:

- a. the value of the peak direct- and quadrature-axis components of the armature currents i_D and i_Q .
b. the rms armature current under this operating condition.
c. the electrical frequency of the drive in Hz.
d. the rms line-to-line armature voltage.



11.29 A field-oriented drive system will be applied to a 230-V, 20-kW, four-pole, 60-Hz induction motor which has the following equivalent-circuit parameters in ohms per phase referred to the stator:

$$R_1 = 0.0322 \quad R_2 = 0.0703 \quad X_1 = 0.344 \quad X_2 = 0.353 \quad X_m = 18.6$$

The motor is connected to a load whose torque can be assumed proportional to speed as $T_{\text{load}} = 85(n/1800) \text{ N}\cdot\text{m}$, where n is the motor speed in r/min.

The field-oriented controller is adjusted such that the rotor flux linkages λ_{DR} are equal to the machine's rated peak flux linkages, and the motor speed is 1300 r/min. Find

- a. the electrical frequency in Hz.
b. the rms armature current and line-to-line voltage.

- c. the motor input kVA.

If the field-oriented controller is set to maintain the motor speed at 1300 r/min, write a MATLAB script to plot the rms armature V/Hz as a percentage of the rated V/Hz as a function of λ_{DR} as λ_{DR} is varied between 80 and 120 percent of the machine's rated peak flux linkages.

- 11.30** The 20-kW induction motor-drive and load of Problem 11.29 is operating at a speed of 1450 r/min with the field-oriented controller adjusted to maintain the rotor flux linkages λ_{DR} equal to the machine's rated peak value.



- Calculate the corresponding values of the direct- and quadrature-axis components of the armature current, i_D and i_Q , and the rms armature current.
- Calculate the corresponding line-to-line terminal voltage drive electrical frequency.

The quadrature-axis current i_Q is now increased by 10 percent while the direct-axis current is held constant.

- Calculate the resultant motor speed and power output.
 - Calculate the terminal voltage and drive frequency.
 - Calculate the total kVA input into the motor.
 - With the controller set to maintain constant speed, determine the set point for λ_{DR} , as a percentage of rated peak flux linkages, that sets the terminal V/Hz equal to the rated machine rated V/Hz. (Hint: This solution is most easily found using a MATLAB script to search for the desired result.)
- 11.31** A three-phase, eight-pole, 60-Hz, 4160-V, 1250-kW squirrel-cage induction motor has the following equivalent-circuit parameters in ohms-per-phase-Y referred to the stator:

$$R_1 = 0.212 \quad R_2 = 0.348 \quad X_1 = 1.87 \quad X_2 = 2.27 \quad X_m = 44.6$$

It is operating from a field-oriented drive system at a speed of 805 r/min and a power output of 1050 kW. The field-oriented controller is set to maintain the rotor flux linkages λ_{DR} equal to the machine's rated peak flux linkages.

- Calculate the motor rms line-to-line terminal voltage, rms armature current, and electrical frequency.
- Show that steady-state induction-motor equivalent circuit and corresponding calculations of Chapter 6 give the same output power and terminal current when the induction motor speed is 828 r/min and the terminal voltage and frequency are equal to those found in part (a).

Three-Phase Circuits

Generation, transmission, and heavy-power utilization of ac electric energy almost invariably involve a type of system or circuit called a *polyphase system* or *polyphase circuit*. In such a system, each voltage source consists of a group of voltages having related magnitudes and phase angles. Thus, an n -phase system employs voltage sources which typically consist of n voltages substantially equal in magnitude and successively displaced by a phase angle of $360^\circ/n$. A *three-phase system* employs voltage sources which typically consist of three voltages substantially equal in magnitude and displaced by phase angles of 120° . Because it possesses definite economic and operating advantages, the three-phase system is by far the most common, and consequently emphasis is placed on three-phase circuits in this appendix.

The three individual voltages of a three-phase source may each be connected to its own independent circuit. We would then have three separate *single-phase systems*. Alternatively, as will be shown in Section A.1, symmetrical electric connections can be made between the three voltages and the associated circuitry to form a three-phase system. It is the latter alternative that we are concerned with in this appendix. Note that the word *phase* now has two distinct meanings. It may refer to a portion of a polyphase system or circuit, or, as in the familiar steady-state circuit theory, it may be used in reference to the angular displacement between voltage or current phasors. There is very little possibility of confusing the two.

A.1 GENERATION OF THREE-PHASE VOLTAGES

Consider the elementary two-pole, three-phase generator of Fig. A.1. On the armature are three coils aa' , bb' , and cc' whose axes are displaced 120° in space from each other. This winding can be represented schematically as shown in Fig. A.2. When the field is excited and rotated, voltages will be generated in the three phases in accordance with Faraday's law. If the field structure is designed so that the flux is

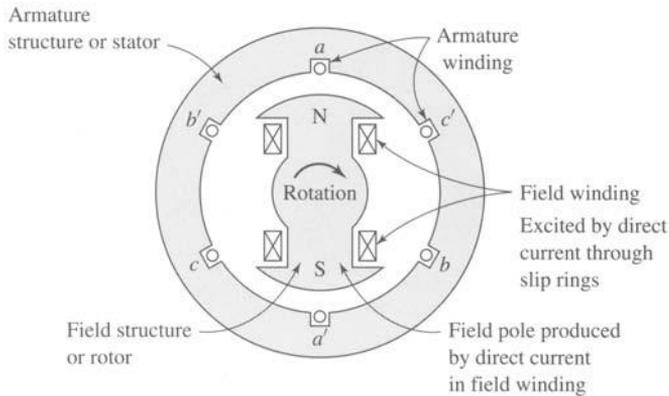


Figure A.1 Elementary two-pole, three-phase generator.

distributed sinusoidally over the poles, the flux linking any phase will vary sinusoidally with time, and sinusoidal voltages will be induced in the three phases. As shown in Fig. A.3, these three voltages will be displaced 120° electrical degrees in time as a result of the phases being displaced 120° in space. The corresponding phasor diagram is shown in Fig. A.4. In general, the time origin and the reference axis in diagrams such as Figs. A.3 and A.4 are chosen on the basis of analytical convenience.

There are two possibilities for the utilization of voltages generated in this manner. The six terminals $a, a', b, b', c,$ and c' of the three-phase winding may be connected to three independent single-phase systems, or the three phases of the winding may be interconnected and used to supply a three-phase system. The latter procedure is adopted almost universally. The three phases of the winding may be interconnected in two possible ways, as shown in Fig. A.5. Terminals $a', b',$ and c' may be joined to form the neutral o , yielding a *Y connection*, or terminals a and b', b and $c',$ and c and a' may be joined individually, yielding a Δ connection. In the *Y connection*, a neutral conductor, shown dashed in Fig. A.5a, may or may not be brought out. If a neutral conductor exists, the system is a four-wire, three-phase system; if not, it is a three-wire, three-phase system. In the Δ connection (Fig. A.5b), no neutral exists and only a three-wire, three-phase system can be formed.

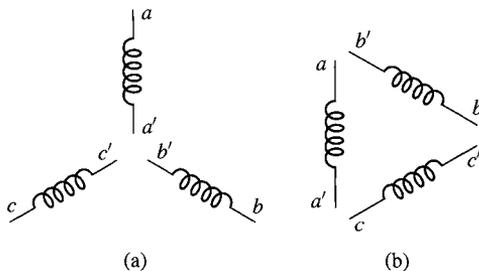


Figure A.2 Schematic representation of the windings of Fig. A.1.

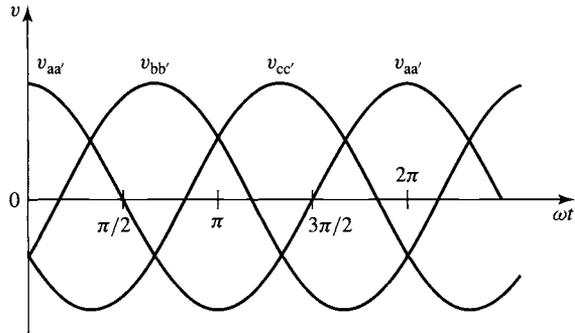


Figure A.3 Voltages generated in the windings of Figs. A.1 and A.2.

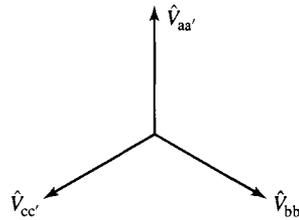


Figure A.4 Phasor diagram of generated voltages.

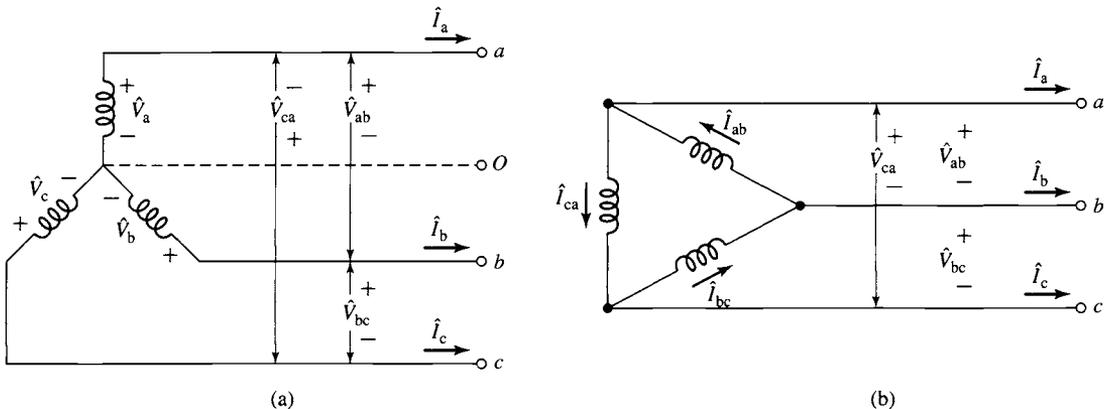


Figure A.5 Three-phase connections: (a) Y connection and (b) Δ connection.

The three phase voltages of Figs. A.3 and A.4, are equal and displaced in phase by 120 degrees, a general characteristic of a *balanced three-phase system*. Furthermore, in a balanced three-phase system the impedance in any one phase is equal to that in either of the other two phases, so that the resulting phase currents are also equal and displaced in phase from each other by 120 degrees. Likewise, equal power and equal

reactive power flow in each phase. An *unbalanced three-phase system*, however, may be unbalanced in one or more of many ways; the source voltages may be unbalanced, either in magnitude or in phase, or the phase impedances may not be equal. Note that *only balanced systems are treated in this appendix, and none of the methods developed or conclusions reached apply to unbalanced systems*. Most practical analyses are conducted under the assumption of a balanced system. Many industrial loads are three-phase loads and therefore inherently balanced, and in supplying single-phase loads from a three-phase source definite efforts are made to keep the three-phase system balanced by assigning approximately equal single-phase loads to each of the three phases.

A.2 THREE-PHASE VOLTAGES, CURRENTS, AND POWER

When the three phases of the winding in Fig. A.1 are Y-connected, as in Fig. A.5a, the phasor diagram of voltages is that of Fig. A.6. The *phase order* or *phase sequence* in Fig. A.6 is *abc*; that is, the voltage of phase *a* reaches its maximum 120° before that of phase *b*.

The three-phase voltages \hat{V}_a , \hat{V}_b , and \hat{V}_c are called *line-to-neutral voltages*. The three voltages \hat{V}_{ab} , \hat{V}_{bc} , and \hat{V}_{ca} are called *line-to-line voltages*. The use of

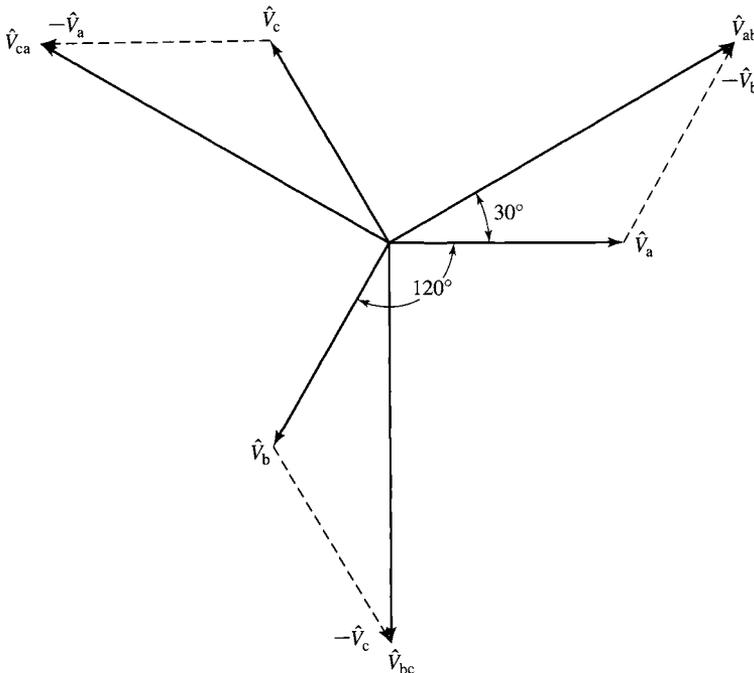


Figure A.6 Voltage phasor diagram for a Y-connected system.

double-subscript notation in Fig. A.6 greatly simplifies the task of drawing the complete diagram. The subscripts indicate the points between which the voltage is determined; for example, the voltage \hat{V}_{ab} is calculated as $\hat{V}_{ab} = \hat{V}_a - \hat{V}_b$.

By Kirchhoff's voltage law, the line-to-line voltage \hat{V}_{ab} is

$$\hat{V}_{ab} = \hat{V}_a - \hat{V}_b = \sqrt{3} \hat{V}_a \angle 30^\circ \quad (\text{A.1})$$

as shown in Fig. A.6. Similarly,

$$\hat{V}_{bc} = \sqrt{3} \hat{V}_b \angle 30^\circ \quad (\text{A.2})$$

and

$$\hat{V}_{ca} = \sqrt{3} \hat{V}_c \angle 30^\circ \quad (\text{A.3})$$

These equations show that *the magnitude of the line-to-line voltage is $\sqrt{3}$ times the line-to-neutral voltage.*

When the three phases are Δ -connected, the corresponding phasor diagram of currents is given in Fig. A.7. The Δ currents are \hat{I}_{ab} , \hat{I}_{bc} , and \hat{I}_{ca} . By Kirchhoff's current law, the line current \hat{I}_a is

$$\hat{I}_a = \hat{I}_{ab} - \hat{I}_{ca} = \sqrt{3} \hat{I}_{ab} \angle -30^\circ \quad (\text{A.4})$$

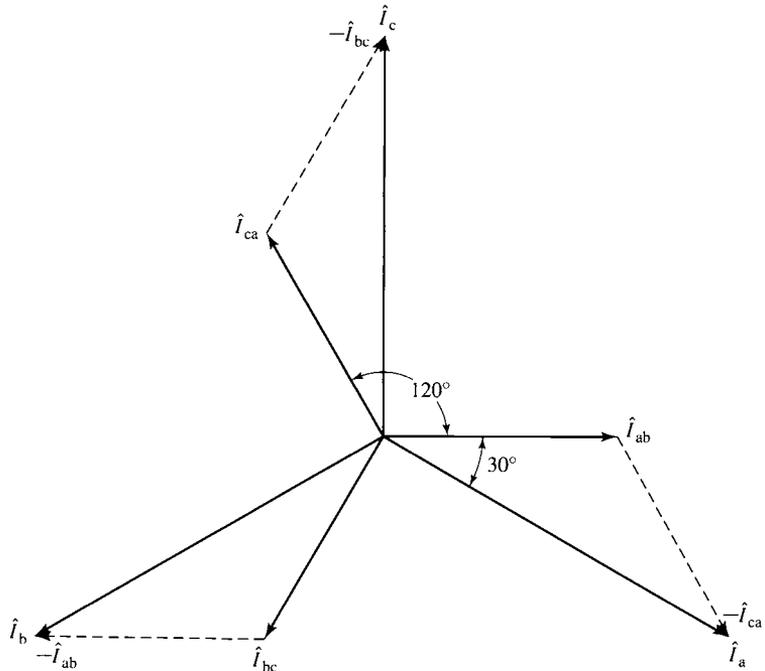


Figure A.7 Current phasor diagram for Δ connection.

as can be seen from the phasor diagram of Fig. A.7. Similarly,

$$\hat{I}_b = \sqrt{3} \hat{I}_{bc} \angle -30^\circ \quad (\text{A.5})$$

and

$$\hat{I}_c = \sqrt{3} \hat{I}_{ca} \angle -30^\circ \quad (\text{A.6})$$

Stated in words, Eqs. A.4 to A.6 show that for a Δ connection, *the magnitude of the line current is $\sqrt{3}$ times that of the Δ current*. Evidently, the relations between Δ currents and line currents of a Δ connection are similar to those between the line-to-neutral and line-to-line voltages of a Y connection.

With the time origin taken at the maximum positive point of the phase-*a* voltage wave, the instantaneous voltages of the three phases are

$$v_a(t) = \sqrt{2} V_{\text{rms}} \cos \omega t \quad (\text{A.7})$$

$$v_b(t) = \sqrt{2} V_{\text{rms}} \cos (\omega t - 120^\circ) \quad (\text{A.8})$$

$$v_c(t) = \sqrt{2} V_{\text{rms}} \cos (\omega t + 120^\circ) \quad (\text{A.9})$$

where V_{rms} is the rms value of the phase-to-neutral voltage. When the phase currents are displaced from the corresponding phase voltages by the angle θ , the instantaneous phase currents are

$$i_a(t) = \sqrt{2} I_{\text{rms}} \cos (\omega t + \theta) \quad (\text{A.10})$$

$$i_b(t) = \sqrt{2} I_{\text{rms}} \cos (\omega t + \theta - 120^\circ) \quad (\text{A.11})$$

$$i_c(t) = \sqrt{2} I_{\text{rms}} \cos (\omega t + \theta + 120^\circ) \quad (\text{A.12})$$

where I_{rms} is the rms value of the phase current.

The instantaneous power in each phase then becomes

$$p_a(t) = v_a(t)i_a(t) = V_{\text{rms}}I_{\text{rms}}[\cos (2\omega t + \theta) + \cos \theta] \quad (\text{A.13})$$

$$p_b(t) = v_b(t)i_b(t) = V_{\text{rms}}I_{\text{rms}}[\cos (2\omega t + \theta - 240^\circ) + \cos \theta] \quad (\text{A.14})$$

$$p_c(t) = v_c(t)i_c(t) = V_{\text{rms}}I_{\text{rms}}[\cos (2\omega t + \theta + 240^\circ) + \cos \theta] \quad (\text{A.15})$$

Note that the average power of each phase is equal

$$\langle p_a(t) \rangle = \langle p_b(t) \rangle = \langle p_c(t) \rangle = V_{\text{rms}}I_{\text{rms}} \cos \theta \quad (\text{A.16})$$

The phase angle θ between the voltage and current is referred to as the *power-factor angle* and $\cos \theta$ is referred to as the *power factor*. If θ is negative, then the power factor is said to be *lagging*; if θ is positive, then the power factor is said to be *leading*.

The total instantaneous power for all three phases is

$$p(t) = p_a(t) + p_b(t) + p_c(t) = 3V_{\text{rms}}I_{\text{rms}} \cos \theta \quad (\text{A.17})$$

Notice that the sum of the cosine terms which involve time in Eqs. A.13 to A.15 (the first terms in the brackets) is zero. We have shown that *the total of the instantaneous power for the three phases of a balanced three-phase circuit is constant and*

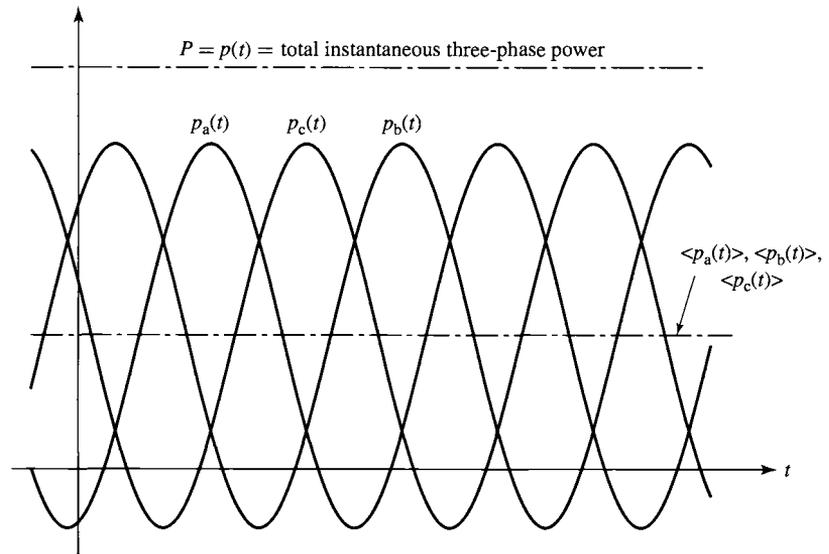


Figure A.8 Instantaneous power in a three-phase system.

does not vary with time. This situation is depicted graphically in Fig. A.8. Instantaneous powers for the three phases are plotted, together with the total instantaneous power, which is the sum of the three individual waves. *The total instantaneous power for a balanced three-phase system is equal to 3 times the average power per phase.* This is one of the outstanding advantages of polyphase systems. It is of particular advantage in the operation of polyphase motors since it means that the shaft-power output is constant and that torque pulsations, with the consequent tendency toward vibration, do not result.

On the basis of single-phase considerations, the average power per phase P_p for either a Y- or Δ -connected system connected to a balanced three-phase load of impedance $Z_p = R_p + jX_p \Omega/\text{phase}$ is

$$P_p = V_{\text{rms}} I_{\text{rms}} \cos \theta = I_p^2 R_p \quad (\text{A.18})$$

Here R_p is the resistance per phase. The total three-phase power P is

$$P = 3P_p \quad (\text{A.19})$$

Similarly, for reactive power per phase Q_p and total three-phase reactive power Q ,

$$Q_p = V_{\text{rms}} I_{\text{rms}} \sin \theta = I_p^2 X_p \quad (\text{A.20})$$

and

$$Q = 3Q_p \quad (\text{A.21})$$

where X_p is the reactance per phase.

The voltamperes per phase $(VA)_p$ and total three-phase voltamperes VA are

$$(VA)_p = V_{\text{rms}} I_{\text{rms}} = I_{\text{rms}}^2 Z_p \quad (\text{A.22})$$

$$VA = 3(VA)_p \quad (\text{A.23})$$

In Eqs. A.18 and A.20, θ is the angle between phase voltage and phase current. As in the single-phase case, it is given by

$$\theta = \tan^{-1} \frac{X_p}{R_p} = \cos^{-1} \frac{R_p}{Z_p} = \sin^{-1} \frac{X_p}{Z_p} \quad (\text{A.24})$$

The power factor of a balanced three-phase system is therefore equal to that of any one phase.

A.3 Y- AND Δ -CONNECTED CIRCUITS

Three specific examples are given to illustrate the computational details of Y- and Δ -connected circuits. Explanatory remarks which are generally applicable are incorporated into the solutions.

EXAMPLE A.1

In Fig. A.9 is shown a 60-Hz transmission system consisting of a line having the impedance $Z_l = 0.05 + j0.20 \Omega$, at the receiving end of which is a load of equivalent impedance $Z_L = 10.0 + j3.00 \Omega$. The impedance of the return conductor should be considered zero.

- a. Compute the line current I ; the load voltage V_L ; the power, reactive power, and voltamperes taken by the load; and the power and reactive-power loss in the line.

Suppose now that three such identical systems are to be constructed to supply three such identical loads. Instead of drawing the diagrams one below the other, let them be drawn in the fashion shown in Fig. A.10, which is, of course, the same electrically.

- b. For Fig. A.10 give the current in each line; the voltage at each load; the power, reactive power, and voltamperes supplied to each load; the power and reactive-power loss in each of the three transmission systems; the total power, reactive power, and voltamperes supplied to the loads; and the total power and reactive-power loss in the three transmission systems.

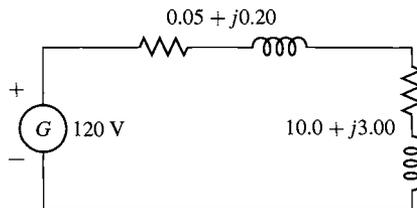


Figure A.9 Circuit for Example A.1, part (a).

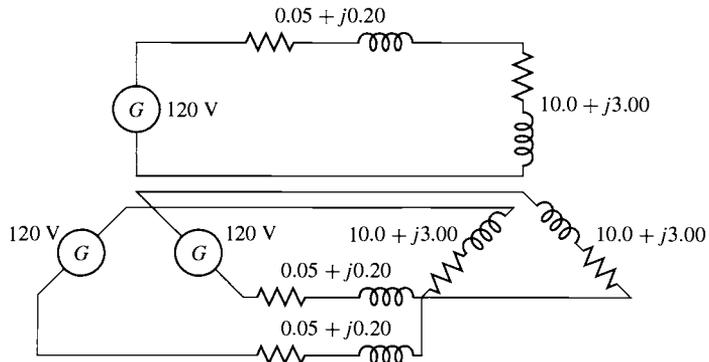


Figure A.10 Circuit for Example A.1, part (b).

Next consider that the three return conductors are combined into one and that the phase relationship of the voltage sources is such that a balanced four-wire, three-phase system results, as in Fig. A.11.

- For Fig. A.11 give the line current; the load voltage, both line-to-line and line-to-neutral; the power, reactive power, and voltamperes taken by each phase of the load; the power and reactive-power loss in each line; the total three-phase power, reactive power, and voltamperes taken by the load; and the total power and reactive-power loss in the lines.
- In Fig. A.11 what is the current in the combined return or neutral conductor?
- Can this conductor be dispensed with in Fig. A.11 if desired?

Assume now that this neutral conductor is omitted. This results in the three-wire, three-phase system of Fig. A.12.

- Repeat part (c) for Fig. A.12.
- On the basis of the results of this example, outline briefly the method of reducing a balanced three-phase Y-connected circuit problem to its equivalent single-phase problem. Be careful to distinguish between the use of line-to-line and line-to-neutral voltages.

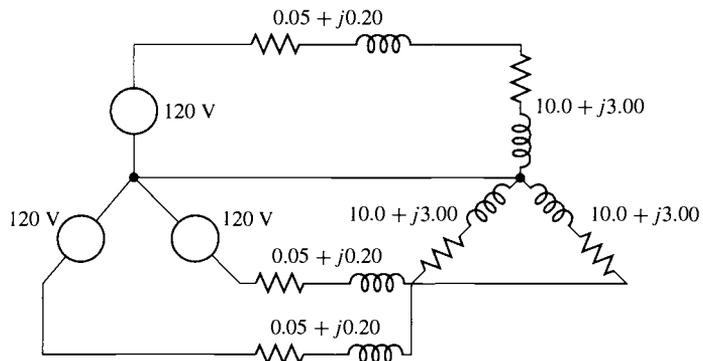


Figure A.11 Circuit for Example A.1, parts (c) to (e).

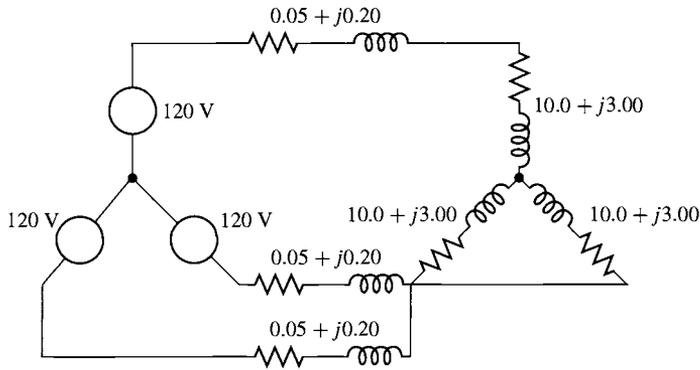


Figure A.12 Circuit for Example A.1 part (f).

■ Solution

a.

$$I = \frac{120}{\sqrt{(0.05 + 10.0)^2 + (0.20 + 3.00)^2}} = 11.4 \text{ A}$$

$$V_L = I|Z_L| = 11.4\sqrt{(10.0)^2 + (3.00)^2} = 119 \text{ V}$$

$$P_L = I^2 R_L = (11.4)^2(3.00) = 1200 \text{ W}$$

$$Q_L = I^2 X_L = (11.4)^2(3.00) = 390 \text{ VA reactive}$$

$$(\text{VA})_L = I^2 |Z_L| = (11.4)^2 \sqrt{(10.0)^2 + (3.00)^2} = 1360 \text{ VA}$$

$$P_1 = I^2 R_1 = (11.4)^2(0.05) = 6.5 \text{ W}$$

$$Q_1 = I^2 X_1 = (11.4)^2(0.20) = 26 \text{ VA reactive}$$

b. The first four obviously have the same values as in part (a).

$$\text{Total power} = 3P_L = 3(1200) = 3600 \text{ W}$$

$$\text{Total reactive power} = 3Q_L = 3(390) = 1170 \text{ VA reactive}$$

$$\text{Total VA} = 3(\text{VA})_L = 3(1360) = 4080 \text{ VA}$$

$$\text{Total power loss} = 3P_1 = 3(6.5) = 19.5 \text{ W}$$

$$\text{Total reactive-power loss} = 3Q_1 = 3(26) = 78 \text{ VA reactive}$$

c. The results obtained in part (b) are unaffected by this change. The voltage in parts (a) and (b) is now the line-to-neutral voltage. The line-to-line voltage is

$$\sqrt{3}(119) = 206 \text{ V}$$

- d. By Kirchhoff's current law, the neutral current is the phasor sum of the three line currents. These line currents are equal and displaced in phase by 120° . Since the phasor sum of three equal phasors 120° apart is zero, the neutral current is zero.
- e. The neutral current being zero, the neutral conductor can be dispensed with if desired.

- f. Since the presence or absence of the neutral conductor does not affect conditions, the values are the same as in part (c).
- g. A neutral conductor can be assumed, regardless of whether one is physically present. Since the neutral conductor in a balanced three-phase circuit carries no current and hence has no voltage drop across it, the neutral conductor should be considered to have zero impedance. Then one phase of the Y, together with the neutral conductor, can be removed for study. Since this phase is uprooted at the neutral, *line-to-neutral voltages must be used*. This procedure yields the single-phase equivalent circuit, in which all quantities correspond to those in one phase of the three-phase circuit. Conditions in the other two phases being the same (except for the 120° phase displacements in the currents and voltages), there is no need for investigating them individually. Line currents in the three-phase system are the same as in the single-phase circuit, and total three-phase power, reactive power, and voltamperes are three times the corresponding quantities in the single-phase circuit. If line-to-line voltages are desired, they must be obtained by multiplying voltages in the single-phase circuit by $\sqrt{3}$.

EXAMPLE A.2

Three impedances of value $Z_Y = 4.00 + j3.00 = 5.00\angle 36.9^\circ \Omega$ are connected in Y, as shown in Fig. A.13. For balanced line-to-line voltages of 208 V, find the line current, the power factor, and the total power, reactive power, and voltamperes.

■ Solution

The rms line-to-neutral voltage V on any one phase, such as phase a , is

$$V = \frac{208}{\sqrt{3}} = 120 \text{ V}$$

Hence, the line current

$$\hat{i} = \frac{V}{Z_Y} = \frac{120}{5.00\angle 36.9^\circ} = 24.0\angle -36.9^\circ \text{ A}$$

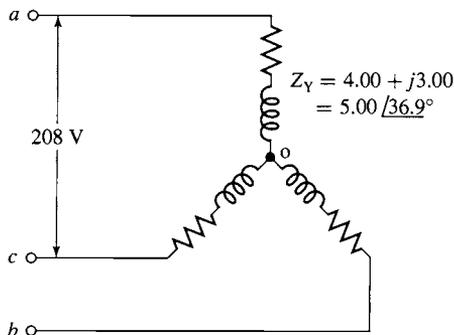


Figure A.13 Circuit for Example A.2.

and the power factor is equal to

$$\text{Power factor} = \cos \theta = \cos (-36.9^\circ) = 0.80 \text{ lagging}$$

Thus

$$P = 3I^2 R_Y = 3(24.0)^2(4.00) = 6910 \text{ W}$$

$$Q = 3I^2 X_Y = 3(24.0)^2(3.00) = 5180 \text{ VA reactive}$$

$$\text{VA} = 3VI = 3(120)(24.0) = 8640 \text{ VA}$$

Note that phases a and c (Fig. A.13) do not form a simple series circuit. Consequently, the current cannot be found by dividing 208 V by the sum of the phase- a and - c impedances. To be sure, an equation can be written for voltage between points a and c by Kirchhoff's voltage law, but this must be a phasor equation taking account of the 120° phase displacement between the phase- a and phase- c currents. As a result, the method of thought outlined in Example A.1 leads to the simplest solution.

EXAMPLE A.3

Three impedances of value $Z_\Delta = 12.00 + j9.00 = 15.00\angle 36.9^\circ \Omega$ are connected in Δ , as shown in Fig. A.14. For balanced line-to-line voltages of 208 V, find the line current, the power factor, and the total power, reactive power, and voltamperes.

■ Solution

The voltage across any one leg of the Δ , V_Δ is equal to the line-to-line voltage V_{1-1} , which is equal to $\sqrt{3}$ times the line-to-neutral voltage V . Consequently,

$$V = \frac{V_{1-1}}{\sqrt{3}} = \frac{208}{\sqrt{3}} = 120 \text{ V}$$

and the current in the Δ is given by the line-to-line voltage divided by the Δ impedance

$$\hat{I}_\Delta = \frac{V_{1-1}}{Z_\Delta} = \frac{208}{15.00\angle 36.9^\circ} = 13.87\angle -36.9^\circ \text{ A}$$

$$\text{Power factor} = \cos \theta = \cos (-36.9^\circ) = 0.80 \text{ lagging}$$

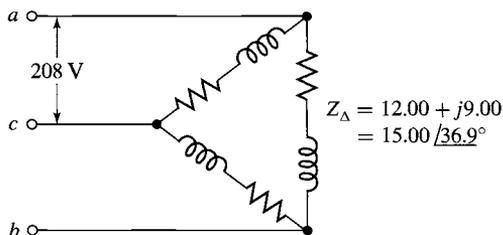


Figure A.14 Circuit for Example A.3.

From Eq. A.4 the phase current is equal to

$$I = \sqrt{3}I_{\Delta} = \sqrt{3}(13.87) = 24.0 \text{ A}$$

Also

$$P = 3P_{\Delta} = 3I_{\Delta}^2 R_{\Delta} = 3(13.87)^2(12.00) = 6910 \text{ W}$$

$$Q = 3Q_{\Delta} = 3I_{\Delta}^2 X_{\Delta} = 3(13.87)^2(9.00) = 5180 \text{ VA reactive}$$

and

$$\text{VA} = 3(\text{VA})_{\Delta} = 3V_{1-1}I_{\Delta} = 3(208)(13.87) = 8640 \text{ VA}$$

Note that phases ab and bc do not form a simple series circuit, nor does the path cba form a simple parallel combination with the direct path through the phase ca . Consequently, the line current cannot be found by dividing 208 V by the equivalent impedance of Z_{ca} in parallel with $Z_{ab} + Z_{bc}$. Kirchhoff's-law equations involving quantities in more than one phase can be written, but they must be phasor quantities taking account of the 120° phase displacement between phase currents and phase voltages. As a result, the method outlined above leads to the simplest solution.

Comparison of the results of Examples A.2 and A.3 leads to a valuable and interesting conclusion. Note that the line-to-line voltage, line current, power factor, total power, reactive power, and voltamperes are precisely equal in the two cases; in other words, conditions viewed from the terminals A , B , and C are identical, and one cannot distinguish between the two circuits from their terminal quantities. It will also be seen that the impedance, resistance, and reactance per phase of the Y connection (Fig. A.13) are exactly one-third of the corresponding values per phase of the Δ connection (Fig. A.14). Consequently, a balanced Δ connection can be replaced by a balanced Y connection providing that the circuit constants per phase obey the relation

$$Z_Y = \frac{1}{3}Z_{\Delta} \quad (\text{A.25})$$

Conversely, a Y connection can be replaced by a Δ connection provided Eq. A.25 is satisfied. The concept of this Y- Δ equivalence stems from the general Y- Δ transformation and is not the accidental result of a specific numerical case.

Two important corollaries follow from this equivalence: (1) A general computational scheme for balanced circuits can be based entirely on Y-connected circuits or entirely on Δ -connected circuits, whichever one prefers. Since it is frequently more convenient to handle a Y connection, the former scheme is usually adopted. (2) In the frequently occurring problems in which the connection is not specified and is not pertinent to the solution, either a Y or a Δ connection may be assumed. Again the Y connection is more commonly selected. In analyzing three-phase motor performance, for example, the actual winding connections need not be known unless the investigation is to include detailed conditions within the windings themselves. The entire analysis can then be based on an assumed Y connection.

A.4 ANALYSIS OF BALANCED THREE-PHASE CIRCUITS; SINGLE-LINE DIAGRAMS

By combining the principle of Δ -Y equivalence with the technique revealed by Example A.1, a simple method of reducing a balanced three-phase-circuit problem to its corresponding single-phase problem can be developed. All the methods of single-phase-circuit analysis thus become available for its solution. The end results of the single-phase analysis are then translated back into three-phase terms to give the final results.

In carrying out this procedure, phasor diagrams need be drawn for only one phase of the Y connection, the diagrams for the other two phases being unnecessary repetition. Furthermore, circuit diagrams can be simplified by drawing only one phase. Examples of such *single-line diagrams* are given in Fig. A.15, showing two three-phase generators with their associated lines or cables supplying a common substation load. Specific connections of apparatus can be indicated if desired. Thus, Fig. A.15b shows that G_1 is Y-connected and G_2 is Δ -connected. Impedances are given in ohms per phase.

When one is dealing with power, reactive power, and voltamperes, it is sometimes more convenient to deal with the entire three-phase circuit at once instead of concentrating on one phase. This possibility arises because simple expressions for three-phase power, reactive power, and voltamperes can be written in terms of line-to-line voltage and line current regardless of whether the circuit is Y- or Δ -connected. Thus, from Eqs. A.18 and A.19, three-phase power is

$$P = 3P_p = 3V_p I_p \cos \theta \quad (\text{A.26})$$

Since $V_{l-l} = \sqrt{3}V_p$, Eq. A.26 becomes

$$P = \sqrt{3}V_{l-l} I_p \cos \theta \quad (\text{A.27})$$

Similarly,

$$Q = \sqrt{3}V_{l-l} I_p \sin \theta \quad (\text{A.28})$$

and

$$VA = \sqrt{3}V_{l-l} I_p \quad (\text{A.29})$$

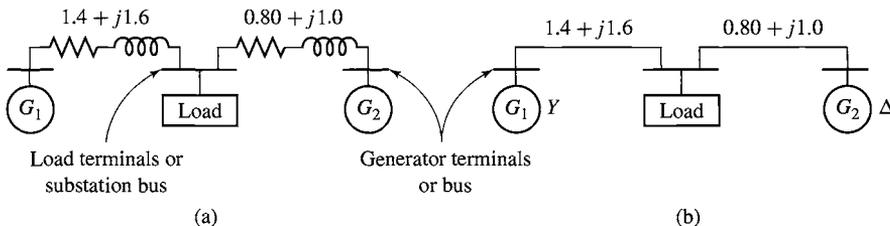


Figure A.15 Examples of single-line diagrams.

It should be borne in mind, however, that the power-factor angle θ , given by Eq. A.24, is the angle between \hat{V}_p and \hat{I}_p and not that between \hat{V}_{1-1} and \hat{I}_p .

EXAMPLE A.4

Figure A.15 is the equivalent circuit of a load supplied from two three-phase generating stations over lines having the impedances per phase given on the diagram. The load requires 30 kW at 0.80 power factor lagging. Generator G_1 operates at a terminal voltage of 797 V line-to-line and supplies 15 kW at 0.80 power factor lagging. Find the load voltage and the terminal voltage and power and reactive-power output of G_2 .

■ Solution

Let I , P , and Q , respectively, denote line current and three-phase active and reactive power. The subscripts 1 and 2 denote the respective branches of the system; the subscript r denotes a quantity measured at the receiving end of the line. We then have

$$I_1 = \frac{P_1}{\sqrt{3}E_1 \cos \theta_1} = \frac{15,000}{\sqrt{3}(797)(0.80)} = 13.6 \text{ A}$$

$$P_{r1} = P_1 - 3I_1^2 R_1 = 15,000 - 3(13.6)^2(1.4) = 14,220 \text{ W}$$

$$Q_{r1} = Q_1 - 3I_1^2 X_1 = 15,000 \tan(\cos^{-1} 0.80) - 3(13.6)^2(1.6) = 10,350 \text{ VA reactive}$$

The factor 3 appears before $I_1^2 R_1$ and $I_1^2 X_1$ in the last two equations because the current I_1 is the phase current. The load voltage is

$$\begin{aligned} V_L &= \frac{\text{VA}}{\sqrt{3}(\text{current})} = \frac{\sqrt{(14,220)^2 + (10,350)^2}}{\sqrt{3}(13.6)} \\ &= 748 \text{ V line-to-line} \end{aligned}$$

Since the load requires 30,000 W of real power and $30,000 \tan(\cos^{-1} 0.80) = 22,500$ VA of reactive power,

$$P_{r2} = 30,000 - 14,220 = 15,780 \text{ W}$$

and

$$Q_{r2} = 22,500 - 10,350 = 12,150 \text{ VA reactive}$$

$$I_2 = \frac{\text{VA}}{\sqrt{3}V_{1-1}} = \frac{\sqrt{(15,780)^2 + (12,150)^2}}{\sqrt{3}(748)} = 15.4 \text{ A}$$

$$P_2 = P_{r2} + 3I_2^2 R_2 = 15,780 + 3(15.4)^2(0.80) = 16,350 \text{ W}$$

$$Q_2 = Q_{r2} + 3I_2^2 X_2 = 12,150 + 3(15.4)^2(1.0) = 12,870 \text{ VA reactive}$$

$$\begin{aligned} V_2 &= \frac{\text{VA}}{\sqrt{3}I_2} = \frac{\sqrt{(16,350)^2 + (12,870)^2}}{\sqrt{3}(15.4)} \\ &= 780 \text{ V (1-1)} \end{aligned}$$

A.5 OTHER POLYPHASE SYSTEMS

Although three-phase systems are by far the most common of all polyphase systems, other numbers of phases are used for specialized purposes. The five-wire, four-phase system (Fig. A.16) is sometimes used for low-voltage distribution. It has the advantage that for a phase voltage of 115 V, single-phase voltages of 115 (between a , b , c , or d and o , Fig. A.16) and 230 V (between a and c or b and d) are available as well as a system of polyphase voltages. Essentially the same advantages are possessed by four-wire, three-phase systems having a line-to-neutral voltage of 120 V and a line-to-line voltage of 208 V, however.

Four-phase systems are obtained from three-phase systems by means of special transformer connections. Half of the four-phase system—the part aob (Fig. A.16), for example—constitutes a two-phase system. In some rectifier circuits, 6-, 12-, 18-, and 36-phase connections are used for the conversion of alternating to direct current. These systems are also obtained by transformation from three-phase systems.

When the loads and voltages are balanced, the methods of analysis for three-phase systems can be adapted to any of the other polyphase systems by considering one phase of that polyphase system. Of course, the basic voltage, current, and power relations must be modified to suit the particular polyphase system.

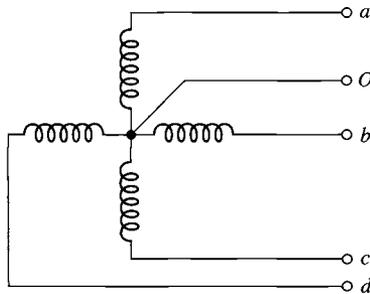


Figure A.16 A five-wire, four-phase system.

Voltages, Magnetic Fields, and Inductances of Distributed AC Windings

Both amplitude and waveform of the generated voltage and armature mmf's in machines are determined by the winding arrangements and general machine geometry. These configurations in turn are dictated by economic use of space and materials in the machine and by suitability for the intended service. In this appendix we supplement the introductory discussion of these considerations in Chapter 4 by analytical treatment of ac voltages and mmf's in the balanced steady state. Attention is confined to the time-fundamental component of voltages and the space-fundamental component of mmf's.

B.1 GENERATED VOLTAGES

In accordance with Eq. 4.50, the rms generated voltage per phase for a concentrated winding having N_{ph} turns per phase is

$$E = \sqrt{2} \pi f N_{ph} \Phi \quad (\text{B.1})$$

where f is the frequency and Φ the fundamental flux per pole.

A more complex and practical winding will have coil sides for each phase distributed in several slots per pole. Equation B.1 can then be used to compute the voltage distribution of individual coils. To determine the voltage of an entire phase group, the voltages of the component coils must be added as phasors. Such addition of fundamental-frequency voltages is the subject of this article.

B.1.1 Distributed Fractional-Pitch Windings

A simple example of a distributed winding is illustrated in Fig. B.1 for a three-phase, two-pole machine. This case retains all the features of a more general one with any integral number of phases, poles, and slots per pole per phase. At the same time, a *double-layer winding* is shown. Double-layer windings usually lead to simpler end connections and to a machine which is more economical to manufacture and are found in all machines except some small motors below 10 kW. Generally, one side of a coil, such as a_1 , is placed in the bottom of a slot, and the other side, $-a_1$, is placed in the top of another slot. Coil sides such as a_1 and a_3 or a_2 and a_4 which are in adjacent slots and associated with the same phase constitute a phase belt. All phase belts are alike when an integral number of slots per pole per phase are used, and for the normal machine the peripheral angle subtended by a phase belt is 60 electrical degrees for a three-phase machine and 90 electrical degrees for a two-phase machine.

Individual coils in Fig. B.1 all span a full pole pitch, or 180 electrical degrees; accordingly, the winding is a full-pitch winding. Suppose now that all coil sides in the tops of the slots are shifted one slot counterclockwise, as in Fig. B.2. Any coil, such as a_1 , $-a_1$, then spans only five-sixths of a pole pitch or $\frac{5}{6}(180) = 150$ electrical degrees, and the winding is a fractional-pitch, or chorded, winding. Similar shifting by two slots yields a $\frac{2}{3}$ -pitch winding, and so forth. Phase groupings are now intermingled, for some slots contain coil sides in phases a and b , a and c , and b and c . Individual phase

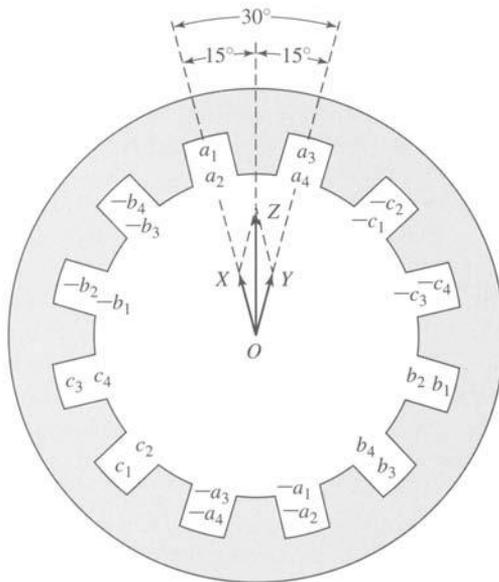


Figure B.1 Distributed two-pole, three-phase full-pitch armature winding with voltage phasor diagram.

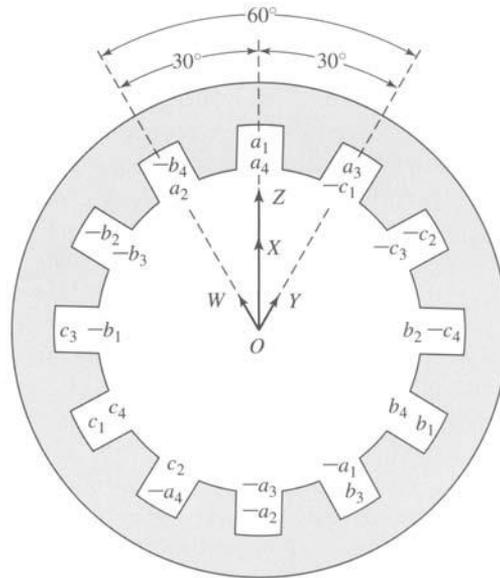


Figure B.2 Distributed two-pole, three-phase fractional-pitch armature winding with voltage phasor diagram.

groups, such as that formed by a_1, a_2, a_3, a_4 on one side and $-a_1, -a_2, -a_3, -a_4$ on the other, are still displaced by 120 electrical degrees from the groups in other phases so that three-phase voltages are produced. Besides the minor feature of shortening the end connections, fractional-pitch windings can be shown to decrease the harmonic content of both voltage and mmf waves.

The end connections between the coil sides are normally in a region of negligible flux density, and hence altering them does not significantly affect the mutual flux linkages of the winding. Allocation of coil sides in slots is then the factor determining the generated voltages, and only that allocation need be specified in Figs. B.1 and B.2. The only requisite is that all coil sides in a phase be included in the interconnection in such a manner that individual voltages make a positive contribution to the total. The practical consequence is that end connections can be made according to the dictates of manufacturing simplicity; the theoretical consequence is that when computational advantages result, the coil sides in a phase can be combined in an arbitrary fashion to form equivalent coils.

One sacrifice is made in using the distributed and fractional-pitch windings of Figs. B.1 and B.2 compared with a concentrated full-pitch winding: for the same number of turns per phase, the fundamental-frequency generated voltage is lower. The harmonics are, in general, lowered by an appreciably greater factor, however, and the total number of turns which can be accommodated on a fixed iron geometry is increased. The effect of distributing the winding in Fig. B.1 is that the voltages of coils a_1 and a_2 are not in phase with those of coils a_3 and a_4 . Thus, the voltage of

coils a_1 and a_2 can be represented by phasor OX in Fig. B.1, and that of coils a_3 and a_4 by the phasor OY . The time-phase displacement between these two voltages is the same as the electrical angle between adjacent slots, so that OX and OY coincide with the centerlines of adjacent slots. The resultant phasor OZ for phase a is obviously smaller than the arithmetic sum of OX and OY .

In addition, the effect of fractional pitch in Fig. B.2 is that a coil links a smaller portion of the total pole flux than if it were a full-pitch coil. The effect can be superimposed on that of distributing the winding by regarding coil sides a_2 and $-a_1$ as an equivalent coil with the phasor voltage OW (Fig. B.2), coil sides a_1 , a_4 , $-a_2$, and $-a_3$ as two equivalent coils with the phasor voltage OX (twice the length of OW), and coil sides a_3 and $-a_4$ as an equivalent coil with phasor voltage OY . The resultant phasor OZ for phase a is obviously smaller than the arithmetic sum of OW , OX , and OY and is also smaller than OZ in Fig. B.1.

The combination of these two effects can be included in a *winding factor* k_w to be used as a reduction factor in Eq. B.1. Thus, the generated voltage per phase is

$$E = \sqrt{2}\pi k_w f N_{ph} \Phi \quad (\text{B.2})$$

where N_{ph} is the total turns in series per phase and k_w accounts for the departure from the concentrated full-pitch case. For a three-phase machine, Eq. B.2 yields the line-to-line voltage for a Δ -connected winding and the line-to-neutral voltage for a Y-connected winding. As in any balanced Y connection, the line-to-line voltage of the latter winding is $\sqrt{3}$ times the line-to-neutral voltage.

B.1.2 Breadth and Pitch Factors

By separately considering the effects of distributing and of chording the winding, reduction factors can be obtained in generalized form convenient for quantitative analysis. The effect of distributing the winding in n slots per phase belt is to yield n voltage phasors displaced in phase by the electrical angle γ between slots, γ being equal to 180 electrical degrees divided by the number of slots per pole. Such a group of phasors is shown in Fig. B.3a and, in a more convenient form for addition, again in Fig. B.3b. Each phasor AB , BC , and CD is the chord of a circle with center at O and subtends the angle γ at the center. The phasor sum AD subtends the angle $n\gamma$, which, as noted previously, is 60 electrical degrees for the normal, uniformly distributed three-phase machine and 90 electrical degrees for the corresponding two-phase machine. From triangles OAA and OAd , respectively,

$$OA = \frac{Aa}{\sin(\gamma/2)} = \frac{AB}{2 \sin(\gamma/2)} \quad (\text{B.3})$$

$$OA = \frac{Ad}{\sin(n\gamma/2)} = \frac{AD}{2 \sin(n\gamma/2)} \quad (\text{B.4})$$

Equating these two values of OA yields

$$AD = AB \frac{\sin(n\gamma/2)}{\sin(\gamma/2)} \quad (\text{B.5})$$

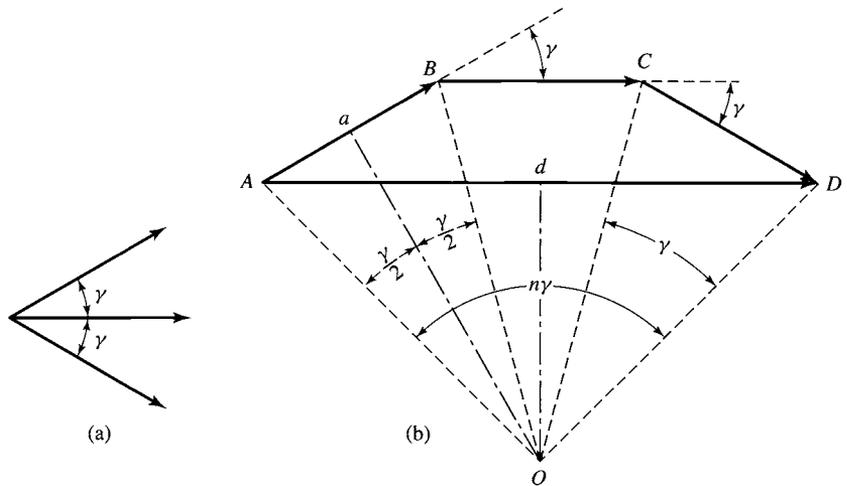


Figure B.3 (a) Coil voltage phasors and (b) phasor sum.

But the arithmetic sum of the phasors is $n(AB)$. Consequently, the reduction factor arising from distributing the winding is

$$k_b = \frac{AD}{nAB} = \frac{\sin(n\gamma/2)}{n \sin(\gamma/2)} \quad (\text{B.6})$$

The factor k_b is called the *breadth factor* of the winding.

The effect of chording on the coil voltage can be obtained by first determining the flux linkages with the fractional-pitch coil. Since there are n coils per phase and N_{ph} total series turns per phase, each coil will have $N = N_{\text{ph}}/n$ turns per coil. From Fig. B.4 coil side $-a$ is only ρ electrical degrees from side a instead of the full 180° . The flux linkages with the N -turn coil are

$$\lambda = NB_{\text{peak}}lr \left(\frac{2}{\text{poles}} \right) \int_{\rho+\alpha}^{\alpha} \sin \theta d\theta \quad (\text{B.7})$$

$$\lambda = NB_{\text{peak}}lr \left(\frac{2}{\text{poles}} \right) [\cos(\alpha + \rho) - \cos \alpha] \quad (\text{B.8})$$

where

l = axial length of coil side

r = coil radius

poles = number of poles

With α replaced by ωt to indicate rotation at ω electrical radians per second, Eq. B.8 becomes

$$\lambda = NB_{\text{peak}}lr \left(\frac{2}{\text{poles}} \right) [\cos(\omega t + \rho) - \cos \omega t] \quad (\text{B.9})$$

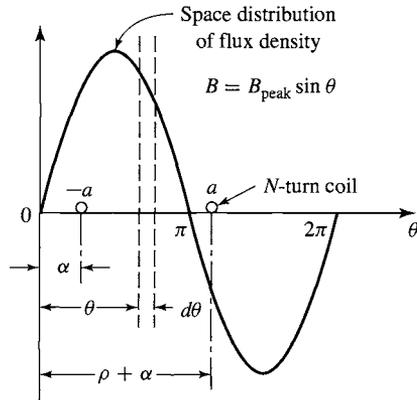


Figure B.4 Fractional-pitch coil in sinusoidal field.

The addition of cosine terms required in the brackets of Eq. B.9 may be performed by a phasor diagram as indicated in Fig. B.5, from which it follows that

$$\cos(\omega t + \rho) - \cos \omega t = -2 \cos\left(\frac{\pi - \rho}{2}\right) \cos\left(\omega t - \left(\frac{\pi - \rho}{2}\right)\right) \quad (\text{B.10})$$

a result which can also be obtained directly from the terms in Eq. B.9 by the appropriate trigonometric transformations.

The flux linkages are then

$$\lambda = -N B_{\text{peak}} l r \left(\frac{4}{\text{poles}}\right) \cos\left(\frac{\pi - \rho}{2}\right) \cos\left(\omega t - \left(\frac{\pi - \rho}{2}\right)\right) \quad (\text{B.11})$$

and the instantaneous voltage is

$$e = \frac{d\lambda}{dt} = \omega N B_{\text{peak}} l r \left(\frac{4}{\text{poles}}\right) \cos\left(\frac{\pi - \rho}{2}\right) \sin\left(\omega t - \left(\frac{\pi - \rho}{2}\right)\right) \quad (\text{B.12})$$

The phase angle $(\pi - \rho)/2$ in Eq. B.12 merely indicates that the instantaneous voltage is no longer zero when α in Fig. B.4 is zero. The factor $\cos[(\pi - \rho)/2]$ is an

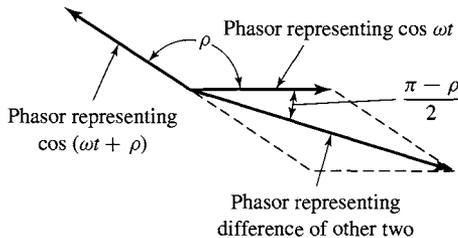


Figure B.5 Phasor addition for fractional-pitch coil.

amplitude-reduction factor, however, so that the rms voltage of Eq. B.1 is modified to

$$E = \sqrt{2}\pi k_p f N_{ph} \Phi \quad (\text{B.13})$$

where the pitch factor k_p is

$$k_p = \cos\left(\frac{\pi - \rho}{2}\right) = \sin\left(\frac{\rho}{2}\right) \quad (\text{B.14})$$

When both the breadth and pitch factors apply, the rms voltage is

$$E = \sqrt{2}\pi k_b k_p f N_{ph} \Phi = \sqrt{2}\pi k_w f N_{ph} \Phi \quad (\text{B.15})$$

which is an alternate form of Eq. B.2; the winding factor k_w is seen to be the product of the pitch and breadth factors.

$$k_w = k_b k_p \quad (\text{B.16})$$

EXAMPLE B.1

Calculate the breadth, pitch, and winding factors for the distributed fractional-pitch winding of Fig. B.2.

■ Solution

The winding of Fig. B.2 has two coils per phase belt, separated by an electrical angle of 30° . From Eq. B.6 the breadth factor is

$$k_b = \frac{\sin(n\gamma/2)}{n \sin(\gamma/2)} = \frac{\sin[2(30^\circ)/2]}{2 \sin(30^\circ/2)} = 0.966$$

The fractional-pitch coils span $150^\circ = 5\pi/6$ rad, and from Eq. B.14 pitch factor is

$$k_p = \sin\left(\frac{\rho}{2}\right) = \sin\left(\frac{5\pi}{12}\right) = 0.966$$

The winding factor is

$$k_w = k_b k_p = 0.933$$

B.2 ARMATURE MMF WAVES

Distribution of a winding in several slots per pole per phase and the use of fractional-pitch coils influence not only the emf generated in the winding but also the magnetic field produced by it. Space-fundamental components of the mmf distributions are examined in this article.

B.2.1 Concentrated Full-Pitch Windings

We have seen in Section 4.3 that a concentrated polyphase winding of N_{ph} turns in a multipole machine produces a rectangular mmf wave around the air-gap circumference. With excitation by a sinusoidal current of amplitude I , the time-maximum amplitude of the space-fundamental component of the wave is, in accordance

with Eq. 4.6,

$$(F_{\text{ag1}})_{\text{peak}} = \frac{4}{\pi} \frac{N_{\text{ph}}}{\text{poles}} (\sqrt{2}I) \text{ A} \cdot \text{turns/pole} \quad (\text{B.17})$$

where the winding factor k_w of Eq. 4.6 has been set equal to unity since in this case we are discussing the mmf wave of a concentrated winding.

Each phase of a polyphase concentrated winding creates such a time-varying standing mmf wave in space. This situation forms the basis of the analysis leading to Eq. 4.39. For concentrated windings, Eq. 4.39 can be rewritten as

$$\mathcal{F}(\theta_{\text{ae}}, t) = \frac{3}{2} \frac{4}{\pi} \left(\frac{N_{\text{ph}}}{\text{poles}} \right) (\sqrt{2}I) \cos(\theta_{\text{ae}} - \omega t) = \frac{6}{\pi} \left(\frac{N_{\text{ph}}}{\text{poles}} \right) (\sqrt{2}I) \cos(\theta_{\text{ae}} - \omega t) \quad (\text{B.18})$$

The amplitude of the resultant mmf wave in a three-phase machine in ampere-turns per pole is then

$$F_A = \frac{6}{\pi} \left(\frac{N_{\text{ph}}}{\text{poles}} \right) (\sqrt{2}I) \text{ A} \cdot \text{turns/pole} \quad (\text{B.19})$$

Similarly, for a n_{ph} -phase machine, the amplitude is

$$F_A = \frac{2n_{\text{ph}}}{\pi} \left(\frac{N_{\text{ph}}}{\text{poles}} \right) (\sqrt{2}I) \text{ A} \cdot \text{turns/pole} \quad (\text{B.20})$$

In Eqs. B.19 and B.20, I is the rms current per phase. The equations include only the fundamental component of the actual distribution and apply to concentrated full-pitch windings with balanced excitation.

B.2.2 Distributed Fractional-Pitch Winding

When the coils in each phase of a winding are distributed among several slots per pole, the resultant space-fundamental mmf can be obtained by superposition from the preceding simpler considerations for a concentrated winding. The effect of distribution can be seen from Fig. B.6, which is a reproduction of the two-pole, three-phase, full-pitch winding with two slots per pole per phase given in Fig. B.1. Coils a_1 and a_2 , b_1 and b_2 , and c_1 and c_2 by themselves constitute the equivalent of a three-phase, two-pole concentrated winding because they form three sets of coils excited by polyphase currents and mechanically displaced 120° from each other. They therefore produce a rotating space-fundamental mmf; the amplitude of this contribution is given by Eq. B.19 when N_{ph} is taken as the sum of the series turns in coils a_1 and a_2 only. Similarly, coils a_3 and a_4 , b_3 and b_4 , and c_3 and c_4 produce another identical mmf wave, but one which is phase-displaced in space by the slot angle γ from the former wave. The resultant space-fundamental mmf wave for the winding can be obtained by adding these two sinusoidal contributions.

The mmf contribution from the $a_1a_2b_1b_2c_1c_2$ coils can be represented by the phasor OX in Fig. B.6. Such phasor representation is appropriate because the waveforms concerned are sinusoidal, and phasor diagrams are simply convenient means for

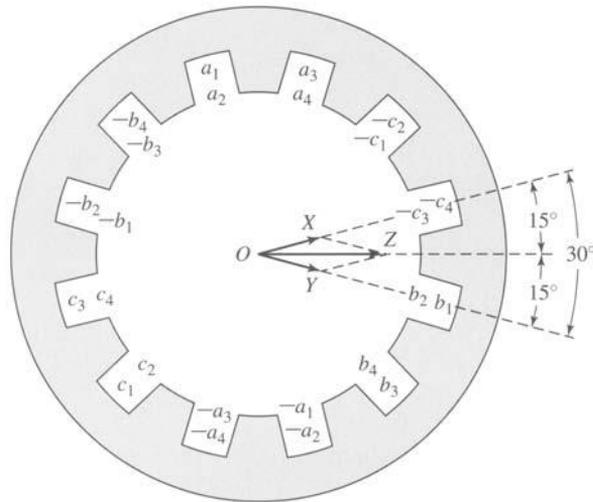


Figure B.6 Distributed two-pole, three-phase, full-pitch armature winding with mmf phasor diagram.

adding sine waves. These are space sinusoids, however, not time sinusoids. Phasor OX is drawn in the space position of the mmf peak for an instant of time when the current in phase a is a maximum. The length of OX is proportional to the number of turns in the associated coils. Similarly, the mmf contribution from the $a_3a_4b_3b_4c_3c_4$ coils may be represented by the phasor OY . Accordingly, the phasor OZ represents the resultant mmf wave. Just as in the corresponding voltage diagram, the resultant mmf is seen to be smaller than if the same number of turns per phase were concentrated in one slot per pole.

In like manner, mmf phasors can be drawn for fractional-pitch windings as illustrated in Fig. B.7, which is a reproduction of the two-pole, three-phase, fractional-pitch winding with two slots per pole per phase given in Fig. B.2. Phasor OW represents the contribution for the equivalent coils formed by conductors a_2 and $-a_1$, b_2 and $-b_1$, and c_2 and $-c_1$; OX for a_1a_4 and $-a_3 - a_2$, b_1b_4 and $-b_3 - b_2$, and c_1c_4 and $-c_3 - c_2$; and OY for a_3 and $-a_4$, b_3 and $-b_4$, and c_3 and $-c_4$. The resultant phasor OZ is, of course, smaller than the algebraic sum of the individual contributions and is also smaller than OZ in Fig. B.6.

By comparison with Figs. B.1 and B.2, these phasor diagrams can be seen to be identical with those for generated voltages. It therefore follows that pitch and breadth factors previously developed can be applied directly to the determination of resultant mmf. Thus, for a distributed, fractional-pitch, polyphase winding, the amplitude of the space-fundamental component of mmf can be obtained by using $k_b k_p N_{ph} = k_w N_{ph}$ instead of simply N_{ph} in Eqs. B.19 and B.20. These equations then become

$$F_A = \frac{6}{\pi} \left(\frac{k_b k_p N_{ph}}{\text{poles}} \right) (\sqrt{2}I) = \frac{6}{\pi} \left(\frac{k_w N_{ph}}{\text{poles}} \right) (\sqrt{2}I) \quad (\text{B.21})$$

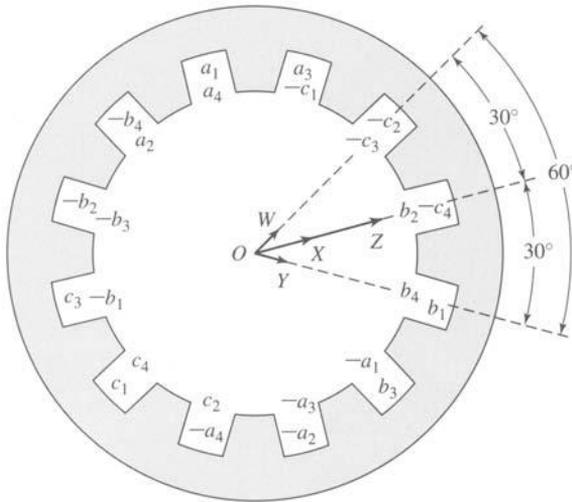


Figure B.7 Distributed two-pole, three-phase, fractional-pitch armature winding with mmf phasor diagram.

for a three-phase machine and

$$F_{\Lambda} = \frac{2n_{ph}}{\pi} \left(\frac{k_b k_p N_{ph}}{\text{poles}} \right) (\sqrt{2}I) = \frac{2n_{ph}}{\pi} \left(\frac{k_w N_{ph}}{\text{poles}} \right) (\sqrt{2}I) \quad (B.22)$$

for a n_{ph} -phase machine, where F_{Λ} is in ampere-turns per pole.

B.3 AIR-GAP INDUCTANCES OF DISTRIBUTED WINDINGS

Figure B.8a shows an N -turn, full-pitch, concentrated armature winding in a two-pole magnetic structure with a concentric cylindrical rotor. The mmf of this configuration is shown in Fig. B.8b. Since the air-gap length g is much smaller than the average air-gap radius r , the air-gap radial magnetic field can be considered uniform and equal to the mmf divided by g . From Eq. 4.3 the space-fundamental mmf is given by

$$\mathcal{F}_{ag1} = \frac{4 Ni}{\pi 2} \cos \theta_a \quad (B.23)$$

and the corresponding air-gap flux density is

$$\mathcal{B}_{ag1} = \mu_0 \frac{\mathcal{F}_{ag1}}{g} = \frac{2\mu_0 Ni}{\pi g} \cos \theta_a \quad (B.24)$$

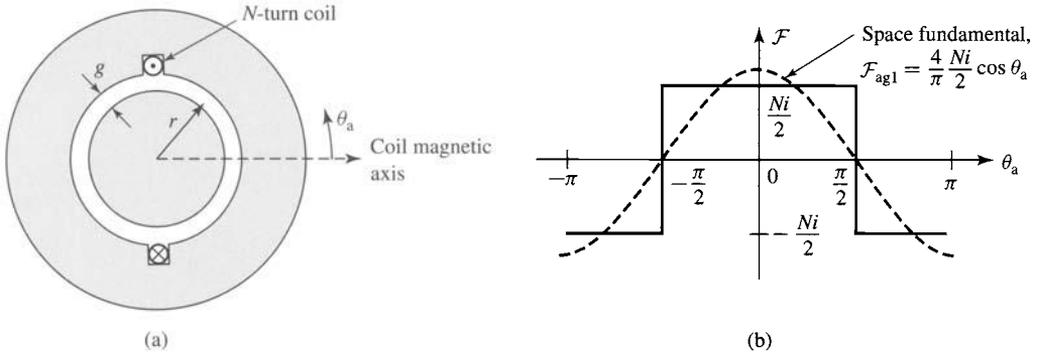


Figure B.8 (a) N -turn concentrated coil and (b) resultant mmf.

Equation B.24 can be integrated to find the fundamental air-gap flux per pole (Eq. 4.44)

$$\Phi = l \int_{-\pi/2}^{\pi/2} B_{ag1} r d\theta = \frac{4\mu_0 N l r}{\pi g} i \quad (\text{B.25})$$

where l is the axial length of the air gap. The air-gap inductance of the coil can be found from Eq. 1.29

$$L = \frac{\lambda}{i} = \frac{N\Phi}{i} = \frac{4\mu_0 N^2 l r}{\pi g} \quad (\text{B.26})$$

For a distributed multipole winding with N_{ph} series turns and a winding factor $k_w = k_b k_p$, the air-gap inductance can be found from Eq. B.26 by substituting for N the effective turns per pole pair ($2k_w N_{ph}/\text{poles}$)

$$L = \frac{4\mu_0 l r}{\pi g} \left(\frac{2k_w N_{ph}}{\text{poles}} \right)^2 = \frac{16\mu_0 l r}{\pi g} \left(\frac{k_w N_{ph}}{\text{poles}} \right)^2 \quad (\text{B.27})$$

Finally, Fig. B.9 shows schematically two coils (labeled 1 and 2) with winding factors k_{w1} and k_{w2} and with $2N_1/\text{poles}$ and $2N_2/\text{poles}$ turns per pole pair, respectively; their magnetic axes are separated by an electrical angle α (equal to poles/2 times their spatial angular displacement). The mutual inductance between these two windings is given by

$$\begin{aligned} L_{12} &= \frac{4\mu_0}{\pi} \left(\frac{2k_{w1} N_1}{\text{poles}} \right) \left(\frac{2k_{w2} N_2}{\text{poles}} \right) \frac{l r}{g} \cos \alpha \\ &= \frac{16\mu_0 (k_{w1} N_1)(k_{w2} N_2) l r}{\pi g (\text{poles})^2} \cos \alpha \end{aligned} \quad (\text{B.28})$$

Although the figure shows one winding on the rotor and the second on the stator, Eq. B.28 is equally valid for the case where both windings are on the same member.

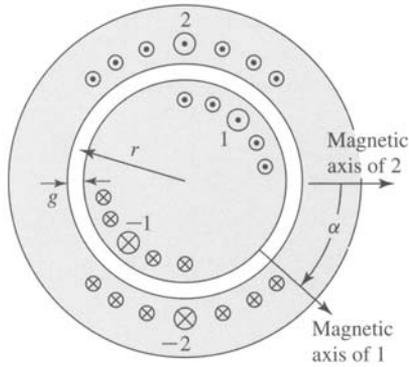


Figure B.9 Two distributed windings separated by electrical angle α .

EXAMPLE B.2

The two-pole stator-winding distribution of Fig. B.2 is found on an induction motor with an air-gap length of 0.381 mm, an average rotor radius of 6.35 cm, and an axial length of 20.3 cm. Each stator coil has 15 turns, and the coil phase connections are as shown in Fig. B.10. Calculate the phase-*a* air-gap inductance L_{aa0} and the phase-*a* to phase-*b* mutual inductance L_{ab} .

■ Solution

Note that the placement of the coils around the stator is such that the flux linkages of each of the two parallel paths are equal. In addition, the air-gap flux distribution is unchanged if, rather than dividing equally between the two legs, as actually occurs, one path were disconnected and all the current were to flow in the remaining path. Thus, the phase inductances can be found by calculating the inductances associated with only one of the parallel paths.

This result may appear to be somewhat puzzling because the two paths are connected in parallel, and thus it would appear that the parallel inductance should be one-half that of the single-path inductance. However, the inductances share a common magnetic circuit, and their

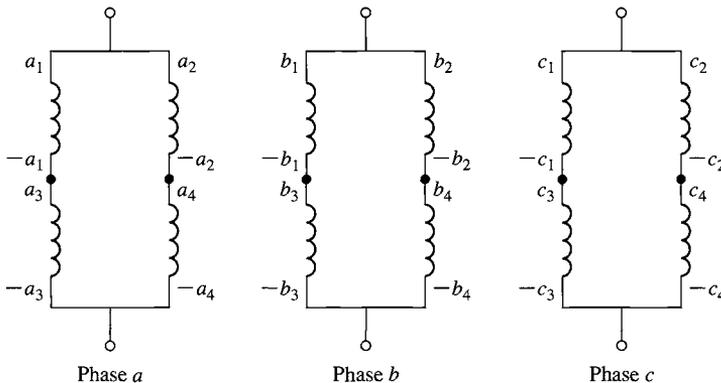


Figure B.10 Coil phase connections of Fig. B.2 for Example B.2.

combined inductance must reflect this fact. It should be pointed out, however, that the phase resistance is one-half that of each of the paths.

The winding factor has been calculated in Example B.1. Thus, from Eq. B.27,

$$\begin{aligned} L_{aa0} &= \frac{16\mu_0 lr}{\pi g} \left(\frac{k_w N_{\text{ph}}}{\text{poles}} \right)^2 \\ &= \frac{16(4\pi \times 10^{-7}) \times 0.203 \times 0.0635}{\pi(3.81 \times 10^{-4})} \left(\frac{0.933 \times 30}{2} \right)^2 \\ &= 42.4 \text{ mH} \end{aligned}$$

The winding axes are separated by $\alpha = 120^\circ$, and thus from Eq. B.28

$$L_{ab} = \frac{16\mu_0 (k_w N_{\text{ph}})^2 lr}{\pi g (\text{poles})^2} \cos \alpha = -21.2 \text{ mH}$$

The dq0 Transformation

In this appendix, the direct- and quadrature-axis (dq0) theory introduced in Section 5.6 is formalized. The formal mathematical transformation from three-phase stator quantities to their direct- and quadrature-axis components is presented. These transformations are then used to express the governing equations for a synchronous machine in terms of the dq0 quantities.

C.1 TRANSFORMATION TO DIRECT- AND QUADRATURE-AXIS VARIABLES

In Section 5.6 the concept of resolving synchronous-machine armature quantities into two rotating components, one aligned with the field-winding axis, the direct-axis component, and one in quadrature with the field-winding axis, the quadrature-axis component, was introduced as a means of facilitating analysis of salient-pole machines. The usefulness of this concept stems from the fact that although each of the stator phases sees a time-varying inductance due to the saliency of the rotor, the transformed quantities rotate with the rotor and hence see constant magnetic paths. Although not discussed here, additional saliency effects are present under transient conditions, due to the different conducting paths in the rotor, rendering the concept of this transformation all the more useful.

Similarly, this transformation is useful from the point of view of analyzing the interaction of the rotor and stator flux- and mmf-waves, independent of whether or not saliency effects are present. By transforming the stator quantities into equivalent quantities which rotate in synchronism with the rotor, under steady-state conditions these interactions become those of constant mmf- and flux-waves separated by a constant spatial angle. This indeed is the point of view which corresponds to that of an observer in the rotor reference frame.

The idea behind the transformation is an old one, stemming from the work of Andre Blondel in France, and the technique is sometimes referred to as the *Blondel two-reaction method*. Much of the development in the form used here was carried out

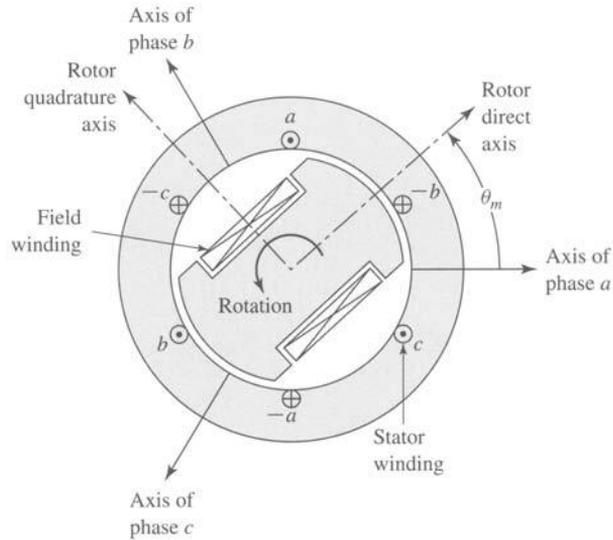


Figure C.1 Idealized synchronous machine

by R. E. Doherty, C. A. Nickle, R. H. Park, and their associates in the United States. The transformation itself, known as the *dq0 transformation*, can be represented in a straightforward fashion in terms of the electrical angle θ_{me} (equal to poles/2 times the spatial angle θ_m) between the rotor direct axis and the stator phase-a axis, as defined by Eq. 4.1 and shown in Fig. C.1.

Letting S represent a stator quantity to be transformed (current, voltage, or flux), we can write the transformation in matrix form as

$$\begin{bmatrix} S_d \\ S_q \\ S_0 \end{bmatrix} = \frac{2}{3} \begin{bmatrix} \cos(\theta_{me}) & \cos(\theta_{me} - 120^\circ) & \cos(\theta_{me} + 120^\circ) \\ -\sin(\theta_{me}) & -\sin(\theta_{me} - 120^\circ) & -\sin(\theta_{me} + 120^\circ) \\ \frac{1}{2} & \frac{1}{2} & \frac{1}{2} \end{bmatrix} \begin{bmatrix} S_a \\ S_b \\ S_c \end{bmatrix} \quad (\text{C.1})$$

and the inverse transformation as

$$\begin{bmatrix} S_a \\ S_b \\ S_c \end{bmatrix} = \begin{bmatrix} \cos(\theta_{me}) & -\sin(\theta_{me}) & 1 \\ \cos(\theta_{me} - 120^\circ) & -\sin(\theta_{me} - 120^\circ) & 1 \\ \cos(\theta_{me} + 120^\circ) & -\sin(\theta_{me} + 120^\circ) & 1 \end{bmatrix} \begin{bmatrix} S_d \\ S_q \\ S_0 \end{bmatrix} \quad (\text{C.2})$$

Here the letter S refers to the quantity to be transformed and the subscripts d and q represent the direct and quadrature axes, respectively. A third component, the *zero-sequence component*, indicated by the subscript 0 , is also included. This component is required to yield a unique transformation of the three stator-phase quantities; it corresponds to components of armature current which produce no net air-gap flux and hence no net flux linking the rotor circuits. As can be seen from Eq. C.1, under balanced-three-phase conditions, there are no zero-sequence components. Only balanced-three-phase conditions are considered in this book, and hence zero-sequence components are not discussed in any detail.

Note that the $dq0$ transformation applies to the instantaneous values of the quantities to be transformed, not rms values. Thus when applying the formal instantaneous transformations as presented here, one must be careful to avoid the use of rms values, as are frequently used in phasor analyses such as are found in Chapter 5.

EXAMPLE C.1

A two-pole synchronous machine is carrying balanced three-phase armature currents

$$i_a = \sqrt{2}I_a \cos \omega t \quad i_b = \sqrt{2}I_a \cos (\omega t - 120^\circ) \quad i_c = \sqrt{2}I_a \cos (\omega t + 120^\circ)$$

The rotor is rotating at synchronous speed ω , and the rotor direct axis is aligned with the stator phase-a axis at time $t = 0$. Find the direct- and quadrature-axis current components.

■ Solution

The angle between the rotor direct axis and the stator phase-a axis can be expressed as

$$\theta_{me} = \omega t$$

From Eq. C.1

$$\begin{aligned} i_d &= \frac{2}{3} [i_a \cos \omega t + i_b \cos (\omega t - 120^\circ) + i_c \cos (\omega t + 120^\circ)] \\ &= \frac{2}{3} \sqrt{2}I_a [\cos^2 \omega t + \cos^2 (\omega t - 120^\circ) + \cos^2 (\omega t + 120^\circ)] \end{aligned}$$

Using the trigonometric identity $\cos^2 \alpha = \frac{1}{2}(1 + \cos 2\alpha)$ gives

$$i_d = \sqrt{2}I_a$$

Similarly,

$$\begin{aligned} i_q &= -\frac{2}{3} [i_a \sin \omega t + i_b \sin (\omega t - 120^\circ) + i_c \sin (\omega t + 120^\circ)] \\ &= -\frac{2}{3} \sqrt{2}I_a [\cos \omega t \sin \omega t + \cos (\omega t - 120^\circ) \sin (\omega t - 120^\circ) \\ &\quad + \cos (\omega t + 120^\circ) \sin (\omega t + 120^\circ)] \end{aligned}$$

and using the trigonometric identity $\cos \alpha \sin \alpha = \frac{1}{2} \sin 2\alpha$ gives

$$i_q = 0$$

This result corresponds directly to our physical picture of the $dq0$ transformation. From the discussion of Section 4.5 we recognize that the balanced three-phase currents applied to this machine produce a synchronously rotating mmf wave which produces flux along the stator phase-a axis at time $t = 0$. This flux wave is thus aligned with the rotor direct axis at $t = 0$ and remains so since the rotor is rotating at the same speed. Hence the stator current produces only direct-axis flux and thus consists only of a direct-axis component.

C.2 BASIC SYNCHRONOUS-MACHINE RELATIONS IN dq0 VARIABLES

Equations 5.2 to 5.5 give the flux-linkage current relationships for a synchronous machine consisting of a field winding and a three-phase stator winding. This simple machine is sufficient to demonstrate the basic features of the machine representation in dq0 variables; the effects of additional rotor circuits such as damper windings can be introduced in a straightforward fashion.

The flux-linkage current relationships in terms of phase variables (Eqs. 5.2 to 5.5) are repeated here for convenience

$$\begin{bmatrix} \lambda_a \\ \lambda_b \\ \lambda_c \\ \lambda_f \end{bmatrix} = \begin{bmatrix} \mathcal{L}_{aa} & \mathcal{L}_{ab} & \mathcal{L}_{ac} & \mathcal{L}_{af} \\ \mathcal{L}_{ba} & \mathcal{L}_{bb} & \mathcal{L}_{bc} & \mathcal{L}_{bf} \\ \mathcal{L}_{ca} & \mathcal{L}_{cb} & \mathcal{L}_{cc} & \mathcal{L}_{cf} \\ \mathcal{L}_{fa} & \mathcal{L}_{fb} & \mathcal{L}_{fc} & \mathcal{L}_{ff} \end{bmatrix} \begin{bmatrix} i_a \\ i_b \\ i_c \\ i_f \end{bmatrix} \quad (\text{C.3})$$

Unlike the analysis of Section 5.2, this analysis will include the effects of saliency, which causes the stator self and mutual inductances to vary with rotor position.

For the purposes of this analysis the idealized synchronous machine of Fig. C.1 is assumed to satisfy two conditions: (1) the air-gap permeance has a constant component as well as a smaller component which varies cosinusoidally with rotor angle as measured from the direct axis, and (2) the effects of space harmonics in the air-gap flux can be ignored. Although these approximations may appear somewhat restrictive, they form the basis of classical dq0 machine analysis and give excellent results in a wide variety of applications. Essentially they involve neglecting effects which result in time-harmonic stator voltages and currents and are thus consistent with our previous assumptions neglecting harmonics produced by discrete windings.

The various machine inductances can then be written in terms of the electrical rotor angle θ_{me} (between the rotor direct axis and the stator phase-a axis), using the notation of Section 5.2, as follows. For the stator self-inductances

$$\mathcal{L}_{aa} = L_{aa0} + L_{al} + L_{g2} \cos 2\theta_{me} \quad (\text{C.4})$$

$$\mathcal{L}_{bb} = L_{aa0} + L_{al} + L_{g2} \cos (2\theta_{me} + 120^\circ) \quad (\text{C.5})$$

$$\mathcal{L}_{cc} = L_{aa0} + L_{al} + L_{g2} \cos (2\theta_{me} - 120^\circ) \quad (\text{C.6})$$

For the stator-to-stator mutual inductances

$$\mathcal{L}_{ab} = \mathcal{L}_{ba} = -\frac{1}{2}L_{aa0} + L_{g2} \cos (2\theta_{me} - 120^\circ) \quad (\text{C.7})$$

$$\mathcal{L}_{bc} = \mathcal{L}_{cb} = -\frac{1}{2}L_{aa0} + L_{g2} \cos 2\theta_{me} \quad (\text{C.8})$$

$$\mathcal{L}_{ac} = \mathcal{L}_{ca} = -\frac{1}{2}L_{aa0} + L_{g2} \cos (2\theta_{me} + 120^\circ) \quad (\text{C.9})$$

For the field-winding self-inductance

$$\mathcal{L}_{ff} = L_{ff} \quad (\text{C.10})$$

and for the stator-to-rotor mutual inductances

$$\mathcal{L}_{af} = \mathcal{L}_{fa} = L_{af} \cos \theta_{me} \quad (\text{C.11})$$

$$\mathcal{L}_{bf} = \mathcal{L}_{fb} = L_{af} \cos (\theta_{me} - 120^\circ) \quad (\text{C.12})$$

$$\mathcal{L}_{cf} = \mathcal{L}_{fc} = L_{af} \cos (\theta_{me} + 120^\circ) \quad (\text{C.13})$$

Comparison with Section 5.2 shows that the effects of saliency appear only in the stator self- and mutual-inductance terms as an inductance term which varies with $2\theta_{me}$. This twice-angle variation can be understood with reference to Fig. C.1, where it can be seen that rotation of the rotor through 180° reproduces the original geometry of the magnetic circuit. Notice that the self-inductance of each stator phase is a maximum when the rotor direct axis is aligned with the axis of that phase and that the phase-phase mutual inductance is maximum when the rotor direct axis is aligned midway between the two phases. This is the expected result since the rotor direct axis is the path of lowest reluctance (maximum permeance) for air-gap flux.

The flux-linkage expressions of Eq. C.3 become much simpler when they are expressed in terms of dq0 variables. This can be done by application of the transformation of Eq. C.1 to both the flux linkages and the currents of Eq. C.3. The manipulations are somewhat laborious and are omitted here because they are simply algebraic. The results are

$$\lambda_d = L_d i_d + L_{af} i_f \quad (\text{C.14})$$

$$\lambda_q = L_q i_q \quad (\text{C.15})$$

$$\lambda_f = \frac{3}{2} L_{af} i_d + L_{ff} i_f \quad (\text{C.16})$$

$$\lambda_0 = L_0 i_0 \quad (\text{C.17})$$

In these equations, new inductance terms appear:

$$L_d = L_{al} + \frac{3}{2} (L_{aa0} + L_{g2}) \quad (\text{C.18})$$

$$L_q = L_{al} + \frac{3}{2} (L_{aa0} - L_{g2}) \quad (\text{C.19})$$

$$L_0 = L_{al} \quad (\text{C.20})$$

The quantities L_d and L_q are the *direct-axis* and *quadrature-axis synchronous inductances*, respectively, corresponding directly to the direct- and quadrature-axis synchronous reactances discussed in Section 5.6 (i.e., $X_d = \omega_e L_d$ and $X_q = \omega_e L_q$). The inductance L_0 is the *zero-sequence inductance*. Notice that the transformed flux-linkage current relationships expressed in Eqs. C.14 to C.17 no longer contain inductances which are functions of rotor position. This feature is responsible for the usefulness of the dq0 transformation.

Transformation of the voltage equations

$$v_a = R_a i_a + \frac{d\lambda_a}{dt} \quad (\text{C.21})$$

$$v_b = R_a i_b + \frac{d\lambda_b}{dt} \quad (\text{C.22})$$

$$v_c = R_a i_c + \frac{d\lambda_c}{dt} \quad (\text{C.23})$$

$$v_f = R_f i_f + \frac{d\lambda_f}{dt} \quad (\text{C.24})$$

results in

$$v_d = R_a i_d + \frac{d\lambda_d}{dt} - \omega_{me} \lambda_q \quad (\text{C.25})$$

$$v_q = R_a i_q + \frac{d\lambda_q}{dt} + \omega_{me} \lambda_d \quad (\text{C.26})$$

$$v_f = R_f i_f + \frac{d\lambda_f}{dt} \quad (\text{C.27})$$

$$v_0 = R_a i_0 + \frac{d\lambda_0}{dt} \quad (\text{C.28})$$

(algebraic details are again omitted), where $\omega_{me} = d\theta_{me}/dt$ is the rotor electrical angular velocity.

In Eqs. C.25 and C.26 the terms $\omega_{me} \lambda_q$ and $\omega_{me} \lambda_d$ are speed-voltage terms which come as a result of the fact that we have chosen to define our variables in a reference frame rotating at the electrical angular velocity ω_{me} . These speed voltage terms are directly analogous to the speed-voltage terms found in the dc machine analysis of Chapter 9. In a dc machine, the commutator/brush system performs the transformation which transforms armature (rotor) voltages to the field-winding (stator) reference frame.

We now have the basic relations for analysis of our simple synchronous machine. They consist of the flux-linkage current equations C.14 to C.17, the voltage equations C.25 to C.28, and the transformation equations C.1 and C.2. When the rotor electrical angular velocity ω_{me} is constant, the differential equations are linear with constant coefficients. In addition, the transformer terms $d\lambda_d/dt$ and $d\lambda_q/dt$ in Eqs. C.25 and C.26 are often negligible with respect to the speed-voltage terms $\omega_{me} \lambda_q$ and $\omega_{me} \lambda_d$, providing further simplification. Omission of these terms corresponds to neglecting the harmonics and dc component in the transient solution for stator voltages and currents. In any case, the transformed equations are generally much easier to solve, both analytically and by computer simulation, than the equations expressed directly in terms of the phase variables.

In using these equations and the corresponding equations in the machinery literature, careful note should be made of the sign convention and units employed. Here

we have chosen the motor-reference convention for armature currents, with positive armature current flowing into the machine terminals. Also, SI units (volts, amperes, ohms, henrys, etc.) are used here; often in the literature one of several per-unit systems is used to provide numerical simplifications.¹

To complete the useful set of equations, expressions for power and torque are needed. The instantaneous power into the three-phase stator is

$$p_s = v_a i_a + v_b i_b + v_c i_c \quad (\text{C.29})$$

Phase quantities can be eliminated from Eq. C.29 by using Eq. C.2 written for voltages and currents. The result is

$$p_s = \frac{3}{2}(v_d i_d + v_q i_q + 2v_0 i_0) \quad (\text{C.30})$$

The electromagnetic torque, T_{mech} , is readily obtained by using the techniques of Chapter 3 as the power output corresponding to the speed voltages divided by the shaft speed (in mechanical radians per second). From Eq. C.30 with the speed-voltage terms from Eqs. C.25 and C.26, and by recognizing ω_{me} as the rotor speed in electrical radians per second, we get

$$T_{\text{mech}} = \frac{3}{2} \left(\frac{\text{poles}}{2} \right) (\lambda_d i_q - \lambda_q i_d) \quad (\text{C.31})$$

A word about sign conventions. When, as is the case in the derivation of this appendix, the motor-reference convention for currents is chosen (i.e., the positive reference direction for currents is into the machine), the torque of Eq. C.31 is the torque acting to accelerate the rotor. Alternatively, if the generator-reference convention is chosen, the torque of Eq. C.31 is the torque acting to decelerate the rotor. This result is, in general, conformity with torque production from interacting magnetic fields as expressed in Eq. 4.81. In Eq. C.31 we see the superposition of the interaction of components: the direct-axis magnetic flux produces torque via its interaction with the quadrature-axis mmf and the quadrature-axis magnetic flux produces torque via its interaction with the direct-axis mmf. Note that, for both of these interactions, the flux and interacting mmf's are 90 electrical degrees apart; hence the sine of the interacting angle (see Eq. 4.81) is unity which in turns leads to the simple form of Eq. C.31.

As a final cautionary note, the reader is again reminded that the currents, fluxes, and voltages in Eqs. C.29 through Eq. C.31 are instantaneous values. Thus, the reader is urged to avoid the use of rms values in these and the other transformation equations found in this appendix.

¹ See A. W. Rankin, "Per-Unit Impedances of Synchronous Machines," *Trans. AIEE* 64:569–573, 839–841 (1945).

C.3 BASIC INDUCTION-MACHINE RELATIONS IN dq0 VARIABLES

In this derivation we will assume that the induction machine includes three-phase windings on both the rotor and the stator and that there are no saliency effects. In this case, the flux-linkage current relationships can be written as

$$\begin{bmatrix} \lambda_a \\ \lambda_b \\ \lambda_c \\ \lambda_{aR} \\ \lambda_{bR} \\ \lambda_{cR} \end{bmatrix} = \begin{bmatrix} \mathcal{L}_{aa} & \mathcal{L}_{ab} & \mathcal{L}_{ac} & \mathcal{L}_{aaR} & \mathcal{L}_{abR} & \mathcal{L}_{acR} \\ \mathcal{L}_{ba} & \mathcal{L}_{bb} & \mathcal{L}_{bc} & \mathcal{L}_{baR} & \mathcal{L}_{bbR} & \mathcal{L}_{bcR} \\ \mathcal{L}_{ca} & \mathcal{L}_{cb} & \mathcal{L}_{cc} & \mathcal{L}_{caR} & \mathcal{L}_{cbR} & \mathcal{L}_{ccR} \\ \mathcal{L}_{Aa} & \mathcal{L}_{aRb} & \mathcal{L}_{aRc} & \mathcal{L}_{aRaR} & \mathcal{L}_{aRbR} & \mathcal{L}_{aRcR} \\ \mathcal{L}_{bRa} & \mathcal{L}_{bRb} & \mathcal{L}_{bRc} & \mathcal{L}_{bRaR} & \mathcal{L}_{bRbR} & \mathcal{L}_{bRcR} \\ \mathcal{L}_{cRa} & \mathcal{L}_{cRb} & \mathcal{L}_{cRc} & \mathcal{L}_{cRaR} & \mathcal{L}_{cRbR} & \mathcal{L}_{cRcR} \end{bmatrix} \begin{bmatrix} i_a \\ i_b \\ i_c \\ i_{aR} \\ i_{bR} \\ i_{cR} \end{bmatrix} \quad (\text{C.32})$$

where the subscripts (a, b, c) refer to stator quantities while the subscripts (aR, bR, cR) refer to rotor quantities.

The various machine inductances can then be written in terms of the electrical rotor angle θ_{me} (defined in this case as between the rotor phase-aR and the stator phase-a axes), as follows. For the stator self-inductances

$$\mathcal{L}_{aa} = \mathcal{L}_{bb} = \mathcal{L}_{cc} = L_{aa0} + L_{al} \quad (\text{C.33})$$

where L_{aa0} is the air-gap component of the stator self-inductance and L_{al} is the leakage component.

For the rotor self-inductances

$$\mathcal{L}_{aRaR} = \mathcal{L}_{bRbR} = \mathcal{L}_{cRcR} = L_{aRaR0} + L_{aRl} \quad (\text{C.34})$$

where L_{aRaR0} is the air-gap component of the rotor self-inductance and L_{aRl} is the leakage component.

For the stator-to-stator mutual inductances

$$\mathcal{L}_{ab} = \mathcal{L}_{ba} = \mathcal{L}_{ac} = \mathcal{L}_{ca} = \mathcal{L}_{bc} = \mathcal{L}_{cb} = -\frac{1}{2}L_{aa0} \quad (\text{C.35})$$

For the rotor-to-rotor mutual inductances

$$\mathcal{L}_{aRbR} = \mathcal{L}_{bRaR} = \mathcal{L}_{aRcR} = \mathcal{L}_{cRaR} = \mathcal{L}_{bRcR} = \mathcal{L}_{cRbR} = -\frac{1}{2}L_{aRaR0} \quad (\text{C.36})$$

and for the stator-to-rotor mutual inductances

$$\mathcal{L}_{aaR} = \mathcal{L}_{aRa} = \mathcal{L}_{bbR} = \mathcal{L}_{bRb} = \mathcal{L}_{ccR} = \mathcal{L}_{cRc} = L_{aaR} \cos \theta_{me} \quad (\text{C.37})$$

$$\mathcal{L}_{baR} = \mathcal{L}_{aRb} = \mathcal{L}_{cbR} = \mathcal{L}_{bRc} = \mathcal{L}_{acR} = \mathcal{L}_{cRa} = L_{aaR} \cos (\theta_{me} - 120^\circ) \quad (\text{C.38})$$

$$\mathcal{L}_{caR} = \mathcal{L}_{aRc} = \mathcal{L}_{abR} = \mathcal{L}_{bRa} = \mathcal{L}_{bcR} = \mathcal{L}_{cRb} = L_{aaR} \cos (\theta_{me} + 120^\circ) \quad (\text{C.39})$$

The corresponding voltage equations become

$$v_a = R_a i_a + \frac{d\lambda_a}{dt} \quad (\text{C.40})$$

$$v_b = R_a i_b + \frac{d\lambda_b}{dt} \quad (\text{C.41})$$

$$v_c = R_a i_c + \frac{d\lambda_c}{dt} \quad (\text{C.42})$$

$$v_{aR} = 0 = R_{aR} i_{aR} + \frac{d\lambda_{aR}}{dt} \quad (\text{C.43})$$

$$v_{bR} = 0 = R_{aR} i_{bR} + \frac{d\lambda_{bR}}{dt} \quad (\text{C.44})$$

$$v_{cR} = 0 = R_{aR} i_{cR} + \frac{d\lambda_{cR}}{dt} \quad (\text{C.45})$$

where the voltages v_{aR} , v_{bR} , and v_{cR} are set equal to zero because the rotor windings are shorted at their terminals.

In the case of a synchronous machine in which the stator flux wave and rotor rotate in synchronism (at least in the steady state), the choice of reference frame for the dq0 transformation is relatively obvious. Specifically, the most useful transformation is to a reference frame fixed to the rotor.

The choice is not so obvious in the case of an induction machine. For example, one might choose a reference frame fixed to the rotor and apply the transformation of Eqs. C.1 and C.2 directly. If this is done, because the rotor of an induction motor does not rotate at synchronous speed, the flux linkages seen in the rotor reference frame will not be constant, and hence the time derivatives in the transformed voltage equations will not be equal to zero. Correspondingly, the direct- and quadrature-axis flux linkages, currents, and voltages will be found to be time-varying, with the result that the transformation turns out to be of little practical value.

An alternative choice is to choose a reference frame rotating at the synchronous angular velocity. In this case, both the stator and rotor quantities will have to be transformed. In the case of the stator quantities, the rotor angle θ_{me} in Eqs. C.1 and C.2 would be replaced by θ_S where

$$\theta_S = \omega_e t + \theta_0 \quad (\text{C.46})$$

is the angle between the axis of phase a and that of the synchronously-rotating dq0 reference frame and θ_0 .

The transformation equations for the stator quantities then become

$$\begin{bmatrix} S_d \\ S_q \\ S_0 \end{bmatrix} = \frac{2}{3} \begin{bmatrix} \cos(\theta_S) & \cos(\theta_S - 120^\circ) & \cos(\theta_S + 120^\circ) \\ -\sin(\theta_S) & -\sin(\theta_S - 120^\circ) & -\sin(\theta_S + 120^\circ) \\ \frac{1}{2} & \frac{1}{2} & \frac{1}{2} \end{bmatrix} \begin{bmatrix} S_a \\ S_b \\ S_c \end{bmatrix} \quad (\text{C.47})$$

and the inverse transformation

$$\begin{bmatrix} S_a \\ S_b \\ S_c \end{bmatrix} = \begin{bmatrix} \cos(\theta_S) & -\sin(\theta_S) & 1 \\ \cos(\theta_S - 120^\circ) & -\sin(\theta_S - 120^\circ) & 1 \\ \cos(\theta_S + 120^\circ) & -\sin(\theta_S + 120^\circ) & 1 \end{bmatrix} \begin{bmatrix} S_d \\ S_q \\ S_0 \end{bmatrix} \quad (\text{C.48})$$

Similarly, in the case of the rotor, θ_S would be replaced by θ_R where

$$\theta_R = (\omega_e - \omega_{me})t + \theta_0 \quad (C.49)$$

is the angle between the axis of rotor phase aR and that of the synchronously-rotating dq0 reference frame and $(\omega_e - \omega_{me})$ is the electrical angular velocity of the synchronously rotating reference frame as seen from the rotor frame.

The transformation equations for the rotor quantities then become

$$\begin{bmatrix} S_{dR} \\ S_{qR} \\ S_{0R} \end{bmatrix} = \frac{2}{3} \begin{bmatrix} \cos(\theta_R) & \cos(\theta_R - 120^\circ) & \cos(\theta_R + 120^\circ) \\ -\sin(\theta_R) & -\sin(\theta_R - 120^\circ) & -\sin(\theta_R + 120^\circ) \\ \frac{1}{2} & \frac{1}{2} & \frac{1}{2} \end{bmatrix} \begin{bmatrix} S_{aR} \\ S_{bR} \\ S_{cR} \end{bmatrix} \quad (C.50)$$

and the inverse transformation

$$\begin{bmatrix} S_{aR} \\ S_{bR} \\ S_{cR} \end{bmatrix} = \begin{bmatrix} \cos(\theta_R) & -\sin(\theta_R) & 1 \\ \cos(\theta_R - 120^\circ) & -\sin(\theta_R - 120^\circ) & 1 \\ \cos(\theta_R + 120^\circ) & -\sin(\theta_R + 120^\circ) & 1 \end{bmatrix} \begin{bmatrix} S_{dR} \\ S_{qR} \\ S_{0R} \end{bmatrix} \quad (C.51)$$

Using this set of transformations for the rotor and stator quantities, the transformed flux-linkage current relationships become

$$\lambda_d = L_S i_d + L_m i_{dR} \quad (C.52)$$

$$\lambda_q = L_S i_q + L_m i_{qR} \quad (C.53)$$

$$\lambda_0 = L_0 i_0 \quad (C.54)$$

for the stator and

$$\lambda_{dR} = L_m i_d + L_R i_{dR} \quad (C.55)$$

$$\lambda_{qR} = L_m i_q + L_R i_{qR} \quad (C.56)$$

$$\lambda_{0R} = L_{0R} i_{0R} \quad (C.57)$$

for the rotor

Here we have defined a set of new inductances

$$L_S = \frac{3}{2} L_{aa0} + L_{al} \quad (C.58)$$

$$L_m = \frac{3}{2} L_{aaR0} \quad (C.59)$$

$$L_0 = L_{al} \quad (C.60)$$

$$L_R = \frac{3}{2} L_{aRaR0} + L_{aRl} \quad (C.61)$$

$$L_{0R} = L_{aRl} \quad (C.62)$$

The transformed stator-voltage equations are

$$v_d = R_a i_d + \frac{d\lambda_d}{dt} - \omega_e \lambda_q \quad (\text{C.63})$$

$$v_q = R_a i_q + \frac{d\lambda_q}{dt} + \omega_e \lambda_d \quad (\text{C.64})$$

$$v_0 = R_a i_0 + \frac{d\lambda_0}{dt} \quad (\text{C.65})$$

and those for the rotor are

$$0 = R_{aR} i_{dR} + \frac{d\lambda_{dR}}{dt} - (\omega_e - \omega_{me}) \lambda_{qR} \quad (\text{C.66})$$

$$0 = R_{aR} i_{qR} + \frac{d\lambda_{qR}}{dt} + (\omega_e - \omega_{me}) \lambda_{dR} \quad (\text{C.67})$$

$$0 = R_{aR} i_{0R} + \frac{d\lambda_{0R}}{dt} \quad (\text{C.68})$$

Finally, using the techniques of Chapter 3, the torque can be expressed in a number of equivalent ways including

$$T_{\text{mech}} = \frac{3}{2} \left(\frac{\text{poles}}{2} \right) (\lambda_d i_q - \lambda_q i_d) \quad (\text{C.69})$$

and

$$T_{\text{mech}} = \frac{3}{2} \left(\frac{\text{poles}}{2} \right) \left(\frac{L_m}{L_R} \right) (\lambda_{dR} i_q - \lambda_{qR} i_d) \quad (\text{C.70})$$

Engineering Aspects of Practical Electric Machine Performance and Operation

In this book the basic essential features of electric machinery have been discussed; this material forms the basis for understanding the behavior of electric machinery of all types. In this appendix our objective is to introduce practical issues associated with the engineering implementation of the machinery concepts which have been developed. Issues common to all electric machine types such as losses, cooling, and rating are discussed.

D.1 LOSSES

Consideration of machine losses is important for three reasons: (1) Losses determine the efficiency of the machine and appreciably influence its operating cost; (2) losses determine the heating of the machine and hence the rating or power output that can be obtained without undue deterioration of the insulation; and (3) the voltage drops or current components associated with supplying the losses must be properly accounted for in a machine representation. Machine efficiency, like that of transformers or any energy-transforming device, is given by

$$\text{Efficiency} = \frac{\text{output}}{\text{input}} \quad (\text{D.1})$$

which can also be expressed as

$$\text{Efficiency} = \frac{\text{input} - \text{losses}}{\text{input}} = 1 - \frac{\text{losses}}{\text{input}} \quad (\text{D.2})$$

$$\text{Efficiency} = \frac{\text{output}}{\text{output} + \text{losses}} \quad (\text{D.3})$$

Rotating machines in general operate efficiently except at light loads. For example, the full-load efficiency of average motors ranges from 80 to 90 percent for motors on the order of 1 to 10 kW, 90 to 95 percent for motors up to a few hundred kW, and up to a few percent higher for larger motors.

The forms given by Eqs. D.2 and D.3 are often used for electric machines, since their efficiency is most commonly determined by measurement of losses instead of by directly measuring the input and output under load. Efficiencies determined from loss measurements can be used in comparing competing machines if exactly the same methods of measurement and computation are used in each case. For this reason, the various losses and the conditions for their measurement are precisely defined by the American National Standards Institute (ANSI), the Institute of Electrical and Electronics Engineers (IEEE), and the National Electrical Manufacturers Association (NEMA). The following discussion summarizes some of the various commonly considered loss mechanisms.

Ohmic Losses *Ohmic*, or I^2R losses, are found in all windings of a machine. By convention, these losses are computed on the basis of the dc resistances of the winding at 75°C. Actually the I^2R loss depends on the effective resistance of the winding under the operating frequency and flux conditions. The increment in loss represented by the difference between dc and effective resistances is included with stray load losses, discussed below. In the field windings of synchronous and dc machines, only the losses in the field winding are charged against the machine; the losses in external sources supplying the excitation are charged against the plant of which the machine is a part. Closely associated with I^2R loss is the *brush-contact loss* at slip rings and commutators. By convention, this loss is normally neglected for induction and synchronous machines. For industrial-type dc machines the voltage drop at the brushes is regarded as constant at 2 V total when carbon and graphite brushes with shunts (pigtailed) are used.

Mechanical Losses *Mechanical losses* consist of brush and bearing friction, windage, and the power required to circulate air through the machine and ventilating system, if one is provided, whether by self-contained or external fans (except for the power required to force air through long or restricted ducts external to the machine). Friction and windage losses can be measured by determining the input to the machine running at the proper speed but unloaded and unexcited. Frequently they are lumped with core loss and determined at the same time.

Open-Circuit, or No-Load, Core Loss *Open-circuit core loss* consists of the hysteresis and eddy-current losses arising from changing flux densities in the iron of the machine with only the main exciting winding energized. In dc and synchronous machines, these losses are confined largely to the armature iron, although the flux variations arising from slot openings will cause losses in the field iron as well, particularly in the pole shoes or surfaces of the field iron. In induction machines the losses are confined largely to the stator iron. Open-circuit core loss can be found by measuring the input to the machine when it is operating unloaded at rated speed or frequency and under the appropriate flux or voltage conditions, and then deducting

the friction and windage loss and, if the machine is self-driven during the test, the no-load armature I^2R loss (no-load stator I^2R loss for an induction motor). Usually, data are taken for a curve of core loss as a function of armature voltage in the neighborhood of rated voltage. The core loss under load is then considered to be the value at a voltage equal to rated voltage corrected for the armature resistance drop under load (a phasor correction for an ac machine). For induction motors, however, this correction is dispensed with, and the core loss at rated voltage is used. For efficiency determination alone, there is no need to segregate open-circuit core loss and friction and windage loss; the sum of these two losses is termed the *no-load rotational loss*.

Eddy-current loss varies with the square of the flux density, the frequency, and the thickness of laminations. Under normal machine conditions it can be expressed to a sufficiently close approximation as

$$P_e = K_e (B_{\max} f \delta)^2 \quad (\text{D.4})$$

where

δ = lamination thickness

B_{\max} = maximum flux density

f = frequency

K_e = proportionality constant

The value of K_e depends on the units used, volume of iron, and resistivity of the iron.

Variation of *hysteresis loss* can be expressed in equation form only on an empirical basis. The most commonly used relation is

$$P_h = K_h f B_{\max}^n \quad (\text{D.5})$$

where K_h is a proportionality constant dependent on the characteristics and volume of iron and the units used and the exponent n ranges from 1.5 to 2.5, a value of 2.0 often being used for estimating purposes in machines. In both Eqs. D.4 and D.5, frequency can be replaced by speed and flux density by the appropriate voltage, with the proportionality constants changed accordingly.

When the machine is loaded, the space distribution of flux density is significantly changed by the mmf of the load currents. The actual core losses may increase noticeably. For example, mmf harmonics cause appreciable losses in the iron near the air-gap surfaces. The total increment in core loss is classified as part of the stray load loss.

Stray Load Loss *Stray load loss* consists of the losses arising from nonuniform current distribution in the copper and the additional core losses produced in the iron by distortion of the magnetic flux by the load current. It is a difficult loss to determine accurately. By convention it is taken as 1.0 percent of the output for dc machines. For synchronous and induction machines it can be found by test.

D.2 RATING AND HEATING

The rating of electrical devices such as machines and transformers is often determined by mechanical and thermal considerations. For example, the maximum winding current is typically determined by the maximum operating temperature which

the insulation can withstand without damage or excessive loss of life. Similarly the maximum speed of a motor or generator is typically determined by mechanical considerations related to the structural integrity of the rotor or the performance of the bearings. The temperature rise resulting from the losses considered in Section D.1 is therefore a major factor in the rating of a machine.

The operating temperature of a machine is closely associated with its life expectancy because deterioration of the insulation is a function of both time and temperature. Such deterioration is a chemical phenomenon involving slow oxidation and brittle hardening and leading to loss of mechanical durability and dielectric strength. In many cases the deterioration rate is such that the life of the insulation can be represented as an exponential

$$\text{Life} = Ae^{B/T} \quad (\text{D.6})$$

where A and B are constants and T is the absolute temperature. Thus, according to Eq. D.6, when life is plotted to a logarithmic scale against the reciprocal of absolute temperature on a uniform scale, a straight line should result. Such plots form valuable guides in the thermal evaluation of insulating materials and systems. A very rough idea of the life-temperature relation can be obtained from the old and more or less obsolete rule of thumb that the time to failure of organic insulation is halved for each 8 to 10°C rise.

The evaluation of insulating materials and insulation systems (which may include widely different materials and techniques in combination) is to a large extent a functional one based on accelerated life tests. Both normal life expectancy and service conditions will vary widely for different classes of electric equipment. Life expectancy, for example, may be a matter of minutes in some military and missile applications, may be 500 to 1000 h in certain aircraft and electronic equipment, and may range from 10 to 30 years or more in large industrial equipment. The test procedures will accordingly vary with the type of equipment. Accelerated life tests on models, called *motorettes*, are commonly used in insulation evaluation. Such tests, however, cannot be easily applied to all equipment, especially the insulation systems of large machines.

Insulation life tests generally attempt to simulate service conditions. They usually include the following elements:

- Thermal shock resulting from heating to the test temperature.
- Sustained heating at that temperature.
- Thermal shock resulting from cooling to room temperature or below.
- Vibration and mechanical stress such as may be encountered in actual service.
- Exposure to moisture.
- Dielectric testing to determine the condition of the insulation.

Enough samples must be tested to permit statistical methods to be applied in analyzing the results. The life-temperature relations obtained from these tests lead to the classification of the insulation or insulating system in the appropriate temperature class.

For the allowable temperature limits of insulating systems used commercially, the latest standards of ANSI, IEEE, and NEMA should be consulted. The three NEMA

insulation-system classes of chief interest for industrial machines are class B, class F, and class H. Class B insulation includes mica, glass fiber, asbestos, and similar materials with suitable bonding substances. Class F insulation also includes mica, glass fiber, and synthetic substances similar to those in class B, but the system must be capable of withstanding higher temperatures. Class H insulation, intended for still higher temperatures, may consist of materials such as silicone elastomer and combinations including mica, glass fiber, asbestos, and so on, with bonding substances such as appropriate silicone resins. Experience and tests showing the material or system to be capable of operation at the recommended temperature form the important classifying criteria.

When the temperature class of the insulation is established, the permissible observable temperature rises for the various parts of industrial-type machines can be found by consulting the appropriate standards. Reasonably detailed distinctions are made with respect to type of machine, method of temperature measurement, machine part involved, whether the machine is enclosed, and the type of cooling (air-cooled, fan-cooled, hydrogen-cooled, etc.). Distinctions are also made between general-purpose machines and definite- or special-purpose machines. The term *general-purpose motor* refers to one of standard rating “up to 200 hp with standard operating characteristics and mechanical construction for use under usual service conditions without restriction to a particular application or type of application.” In contrast a *special-purpose motor* is “designed with either operating characteristics or mechanical construction, or both, for a particular application.” For the same class of insulation, the permissible rise of temperature is lower for a general-purpose motor than for a special-purpose motor, largely to allow a greater factor of safety where service conditions are unknown. Partially compensating the lower rise, however, is the fact that general-purpose motors are allowed a *service factor* of 1.15 when operated at rated voltage; the service factor is a multiplier which, applied to the rated output, indicates a permissible loading which may be carried continuously under the conditions specified for that service factor.

Examples of allowable temperature rises can be seen from Table D.1. The table applies to integral-horsepower induction motors, is based on 40°C ambient temperature, and assumes measurement of temperature rise by determining the increase of winding resistances.

The most common machine rating is the *continuous rating* defining the output (in kilowatts for dc generators, kilovoltamperes at a specified power factor for ac generators, and horsepower or kilowatts for motors) which can be carried indefinitely

Table D.1 Allowable temperature rise, °C[†].

Motor type	Class B	Class F	Class H
1.15 service factor	90	115	
1.00 service factor, encapsulated windings	85	110	
Totally enclosed, fan-cooled	80	105	125
Totally enclosed, nonventilated	85	110	135

[†]Excerpted from NEMA standards.

without exceeding established limitations. For intermittent, periodic, or varying duty, a machine may be given a short-time rating defining the load which can be carried for a specific time. Standard periods for short-time ratings are 5, 15, 30, and 60 minutes. Speeds, voltages, and frequencies are also specified in machine ratings, and provision is made for possible variations in voltage and frequency. Motors, for example, must operate successfully at voltages 10 percent above and below rated voltage and, for ac motors, at frequencies 5 percent above and below rated frequency; the combined variation of voltage and frequency may not exceed 10 percent. Other performance conditions are so established that reasonable short-time overloads can be carried. Thus, the user of a motor can expect to be able to apply for a short time an overload of, say, 25 percent at 90 percent of normal voltage with an ample margin of safety.

The converse problem to the rating of machinery, that of choosing the size of machine for a particular application, is a relatively simple one when the load requirements remain substantially constant. For many motor applications, however, the load requirements vary more or less cyclically and over a wide range. The duty cycle of a typical crane or hoist motor offers a good example. From the thermal viewpoint, the average heating of the motor must be found by detailed study of the motor losses during the various parts of the cycle. Account must be taken of changes in ventilation with motor speed for open and semiclosed motors. Judicious selection is based on a large amount of experimental data and considerable experience with the motors involved. For estimating the required size of motors operating at substantially constant speeds, it is sometimes assumed that the heating of the insulation varies as the square of the load, an assumption which obviously overemphasizes the role of armature I^2R loss at the expense of the core loss. The rms ordinate of the power-time curve representing the duty cycle is obtained by the same technique used to find the rms value of periodically varying currents, and a motor rating is chosen on the basis of the result; i.e.,

$$\text{rms kW} = \sqrt{\frac{\Sigma(\text{kW})^2 \times \text{time}}{\text{running time} + (\text{standstill time}/k)}} \quad (\text{D.7})$$

where the constant k accounts for the poorer ventilation at standstill and equals approximately 4 for an open motor. The time for a complete cycle must be short compared with the time for the motor to reach a steady temperature.

Although crude, the rms-kW method is used fairly often. The necessity for rounding the result to a commercially available motor size¹ obviates the need for precise computations. Special consideration must be given to motors that are frequently started or reversed, for such operations are thermally equivalent to heavy overloads. Consideration must also be given to duty cycles having such high torque

¹ Commercially available motors are generally found in standard sizes as defined by NEMA. NEMA Standards on Motors and Generators specify motor rating as well as the type and dimensions of the motor frame.

peaks that motors with continuous ratings chosen on purely thermal bases would be unable to furnish the torques required. It is to such duty cycles that special-purpose motors with short-time ratings are often applied. Short-time-rated motors in general have better torque-producing capability than motors rated to produce the same power output continuously, although, of course, they have a lower thermal capacity. Both these properties follow from the fact that a short-time-rated motor is designed for high flux densities in the iron and high current densities in the copper. In general, the ratio of torque capacity to thermal capacity increases as the period of the short-time rating decreases. Higher temperature rises are allowed in short-time-rated motors than for general-purpose motors. A motor with a 150-kW, 1-hr, 50°C rating, for example, may have the torque ability of a 200-kW continuously rated motor; it will be able to carry only about 0.8 times its rated output, or 120 kW continuously, however. In many cases it will be the economical solution for a drive requiring a continuous thermal capacity of 120 kW but having torque peaks which require the capability of a 200-kW continuously rated motor.

D.3 COOLING MEANS FOR ELECTRIC MACHINES

The cooling problem in electric apparatus in general increases in difficulty with increasing size. The surface area from which the heat must be carried away increases roughly as the square of the dimensions, whereas the heat developed by the losses is roughly proportional to the volume and therefore increases approximately as the cube of the dimensions. This problem is a particularly serious one in large turbine generators, where economy, mechanical requirements, shipping, and erection all demand compactness, especially for the rotor forging. Even in moderate size machines, for example, above a few thousand kVA for generators, a closed ventilating system is commonly used. Rather elaborate systems of cooling ducts must be provided to ensure that the cooling medium will effectively remove the heat arising from the losses.

For turbine generators, hydrogen is commonly used as the cooling medium in the totally enclosed ventilating system. Hydrogen has the following properties which make it well suited to the purpose:

- Its density is only about 0.07 times that of air at the same temperature and pressure, and therefore windage and ventilating losses are much less.
- Its specific heat on an equal-weight basis is about 14.5 times that of air. This means that, for the same temperature and pressure, hydrogen and air are about equally effective in their heat-storing capacity per unit volume, but the heat transfer by forced convection between the hot parts of the machine and the cooling gas is considerably greater with hydrogen than with air.
- The life of the insulation is increased and maintenance expenses decreased because of the absence of dirt, moisture, and oxygen.

- The fire hazard is minimized. A hydrogen-air mixture will not explode if the hydrogen content is above about 70 percent.

The result of the first two properties is that for the same operating conditions the heat which must be dissipated is reduced and at the same time the ease with which it can be carried off is increased.

The machine and its water-cooled heat exchanger for cooling the hydrogen must be sealed in a gas-tight envelope. The crux of the problem is in sealing the bearings. The system is maintained at a slight pressure (at least 0.5 psi) above atmospheric so that gas leakage is outward and an explosive mixture cannot accumulate in the machine. At this pressure, the rating of the machine can be increased by about 30 percent above its air-cooled rating, and the full-load efficiency increased by about 0.5 percent. The trend is toward the use of higher pressures (15 to 60 psi). Increasing the hydrogen pressure from 0.5 to 15 psi increases the output for the same temperature rise by about 15 percent; a further increase to 30 psi provides about an additional 10 percent.

An important step which has made it possible almost to double the output of a hydrogen-cooled turbine-generator of given physical size is the development of *conductor cooling*, also called *inner cooling*. Here the coolant (liquid or gas) is forced through hollow ducts inside the conductor or conductor strands. Examples of such conductors can be seen in Fig. D.1. Thus, the thermal barrier presented by the electric insulation is largely circumvented, and the conductor losses can be absorbed directly by the coolant. Hydrogen is usually the cooling medium for the rotor conductors. Either gas or liquid cooling may be used for the stator conductors. Hydrogen is the coolant in the former case, and transit oil or water is commonly used in the latter. A sectional view of a conductor-cooled turbine generator is given in Fig. D.2. A large hydroelectric generator in which both stator and rotor are water-cooled is shown in Figs. 4.1 and 4.9.

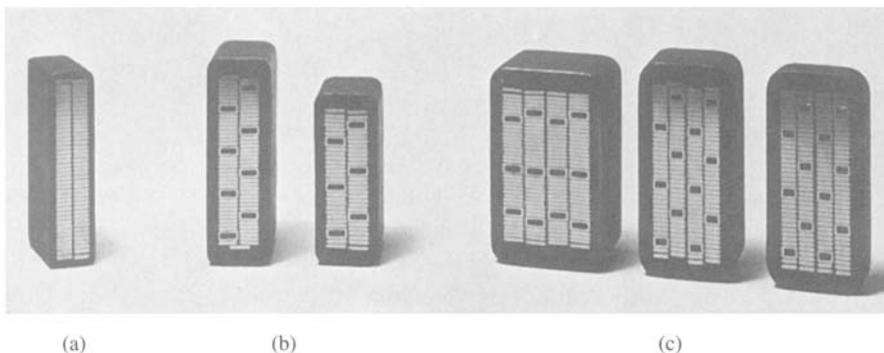


Figure D.1 Cross sections of bars for two-layer stator windings of turbine-generators. Insulation system consists of synthetic resin with vacuum impregnation. (a) Indirectly cooled bar with tubular strands; (b) water-cooled bars, two-wire-wide mixed strands, (c) water-cooled bars, four-wire-wide mixed strands. (*Brown Boveri Corporation.*)

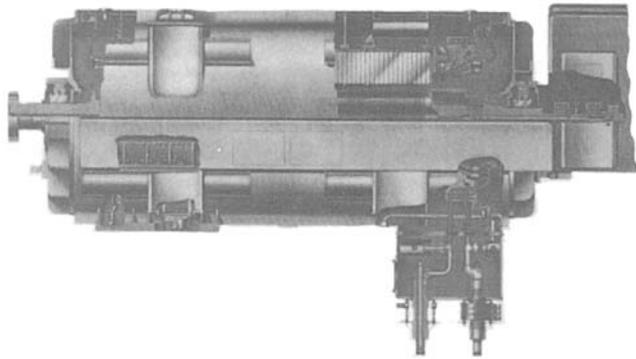


Figure D.2 Cutaway view of a two-pole 3600 r/min turbine rated at 500 MVA, 0.90 power factor, 22 kV, 60 Hz, 45 psig H₂ pressure. Stator winding is water-cooled; rotor winding is hydrogen-cooled. (*General Electric Company*.)

D.4 EXCITATION

The resultant flux in the magnetic circuit of a machine is established by the combined mmf of all the windings on the machine. For the conventional dc machine, the bulk of the effective mmf is furnished by the field windings. For the transformer, the net excitation may be furnished by either the primary or the secondary winding, or a portion may be furnished by each. A similar situation exists in ac machines. Furnishing excitation to ac machines has two different operational aspects which are of economic importance in the application of the machines.

D.4.1 Power Factor in AC Machines

The power factor at which ac machines operate is an economically important feature because of the cost of reactive kilovoltamperes. Low power factor adversely affects system operation in three principal ways. (1) Generators, transformers, and transmission equipment are rated in terms of kVA rather than kW because their losses and heating are very nearly determined by voltage and current regardless of power factor. The physical size and cost of ac apparatus are roughly proportional to kVA rating. The investment in generators, transformers, and transmission equipment for supplying a given useful amount of active power therefore is roughly inversely proportional to the power factor. (2) Low power factor means more current and greater I^2R losses in the generating and transmitting equipment. (3) A further disadvantage is poor voltage regulation.

Factors influencing reactive-kVA requirements in motors can be visualized readily in terms of the relationship of these requirements to the establishment of magnetic flux. As in any electromagnetic device, the resultant flux necessary for motor operation must be established by a magnetizing component of current. It makes no difference either in the magnetic circuit or in the fundamental energy conversion process whether

this magnetizing current be carried by the rotor or stator winding, just as it makes no basic difference in a transformer which winding carries the exciting current. In some cases, part of it is supplied from each winding. If all or part of the magnetizing current is supplied by an ac winding, the input to that winding must include lagging reactive kVA, because magnetizing current lags voltage drop by 90° . In effect, the lagging reactive kVA set up flux in the motor.

The only possible source of excitation in an induction motor is the stator input. The induction motor therefore must operate at a lagging power factor. This power factor is very low at no load and increases to about 85 to 90 percent at full load, the improvement being caused by the increased real-power requirements with increasing load.

With a synchronous motor, there are two possible sources of excitation: alternating current in the armature or direct current in the field winding. If the field current is just sufficient to supply the necessary mmf, no magnetizing-current component or reactive kVA are needed in the armature and the motor operates at unity power factor. If the field current is less, i.e., the motor is *underexcited*, the deficit in mmf must be made up by the armature and the motor operates at a lagging power factor. If the field current is greater, i.e., the motor is *overexcited*, the excess mmf must be counterbalanced in the armature and a leading component of current is present; the motor then operates at a leading power factor.

Because magnetizing current must be supplied to inductive loads such as transformers and induction motors, the ability of overexcited synchronous motors to supply lagging current is a highly desirable feature which may have considerable economic importance. In effect, overexcited synchronous motors act as generators of lagging reactive kilovoltamperes and thereby relieve the power source of the necessity for supplying this component. They thus may perform the same function as a local capacitor installation. Sometimes unloaded synchronous machines are installed in power systems solely for power-factor correction or for control of reactive-kVA flow. Such machines, called *synchronous condensers*, may be more economical in the larger sizes than static capacitors.

Both synchronous and induction machines may become self-excited when a sufficiently heavy capacitive load is present in their stator circuits. The capacitive current then furnishes the excitation and may cause serious overvoltage or excessive transient torques. Because of the inherent capacitance of transmission lines, the problem may arise when synchronous generators are energizing long unloaded or lightly loaded lines. The use of shunt reactors at the sending end of the line to compensate the capacitive current is sometimes necessary. For induction motors, it is normal practice to avoid self-excitation by limiting the size of any parallel capacitor when the motor and capacitor are switched as a unit.

D.4.2 Turbine-Generator Excitation Systems

As the available ratings of turbine-generators have increased, the problems of supplying the dc field excitation (amounting to 4000 A or more in the larger units) have grown progressively more difficult. A common excitation source is a shaft-driven dc generator whose output is supplied to the alternator field through brushes and

slip rings. Alternatively, excitation may be supplied from a shaft-driven alternator of conventional design as the main exciter. This alternator has a stationary armature and a rotating-field winding. Its frequency may be 180 or 240 Hz. Its output is fed to a stationary solid-state rectifier, which in turn supplies the turbine-generator field through slip rings.

Cooling and maintenance problems are inevitably associated with slip rings, commutators, and brushes. Many modern excitation systems have minimized these problems by minimizing the use of sliding contacts and brushes. As a result, some excitation systems employ shaft-driven ac alternators whose field windings are stationary and whose ac windings rotate. By the use of rotating rectifiers, dc excitation can be applied directly to the generator field winding without the use of slip rings.

Excitation systems of the latest design are being built without any sort of rotating exciter-alternator. In these systems, the excitation power is obtained from a special auxiliary transformer fed from the local power system. Alternatively it may be obtained directly from the main generator terminals; in one system a special armature winding is included in the main generator to supply the excitation power. In each of these systems the power is rectified using phase-controlled silicon controlled rectifiers (SCRs). These types of excitation system, which have been made possible by the development of reliable, high-power SCRs, are relatively simple in design and provide the fast response characteristics required in many modern applications.

D.5 ENERGY EFFICIENCY OF ELECTRIC MACHINERY

With increasing concern for both the supply and cost of energy comes a corresponding concern for efficiency in its use. Although electric energy can be converted to mechanical energy with great efficiency, achieving maximum efficiency requires both careful design of the electric machinery and proper matching of machine and intended application.

Clearly, one means to maximize the efficiency of an electric machine is to minimize its internal losses, such as those described in Section D.1. For example, the winding I^2R losses can be reduced by increasing the slot area so that more copper can be used, thus increasing the cross-sectional area of the windings and reducing the resistance.

Core loss can be reduced by decreasing the magnetic flux density in the iron of the machine. This can be done by increasing the volume of iron, but although the loss goes down in terms of watts per pound, the total volume of material (and hence the mass) is increased; depending on how the machine design is changed, there may be a point beyond which the losses actually begin to increase. Similarly, for a given flux density, eddy-current losses can be reduced by using thinner iron laminations.

One can see that there are trade-offs involved here; machines of more efficient design generally require more material and thus are bigger and more costly. Users will generally choose the "lowest-cost" solution to a particular requirement; if the increased capital cost of a high-efficiency motor can be expected to be offset by

energy savings over the expected lifetime of the machine, they will probably select the high-efficiency machine. If not, users are very unlikely to select this option in spite of the increased efficiency.

Similarly, some types of electric machines are inherently more efficient than others. For example, single-phase capacitor-start induction motors (Section 9.2) are relatively inexpensive and highly reliable, finding use in all sorts of small appliances, e.g., refrigerators, air conditioners, and fans. Yet they are inherently less efficient than their three-phase counterparts. Modifications such as a capacitor-run feature can lead to greater efficiency in the single-phase induction motor, but they are expensive and often not economically justifiable.

To optimize the efficiency of use of electric machinery the machine must be properly matched to the application, both in terms of size and performance. Since typical induction motors tend to draw nearly constant reactive power, independent of load, and since this causes resistive losses in the supply lines, it is wise to pick the smallest-rating induction motor which can properly satisfy the requirements of a specific application. Alternatively, capacitive power-factor correction may be used. Proper application of modern solid-state control technology can also play an important role in optimizing both performance and efficiency.

There are, of course, practical limitations which affect the selection of the motor for any particular application. Chief among them is that motors are generally available only in certain standard sizes. For example, a typical manufacturer might make fractional-horsepower ac motors rated at $\frac{1}{8}$, $\frac{1}{6}$, $\frac{1}{4}$, $\frac{1}{3}$, $\frac{1}{2}$, $\frac{3}{4}$, and 1 hp (NEMA standard ratings). This discrete selection thus limits the ability to fine-tune a particular application; if the need is 0.8 hp, the user will undoubtedly end up buying a 1-hp device and settling for a somewhat lower than optimum efficiency. A custom-designed and manufactured 0.8-hp motor can be economically justified only if it is needed in large quantities.

It should be pointed out that an extremely common source of inefficiency in electric motor applications is the mismatch of the motor to its application. Even the most efficient 50-kW motors will be somewhat inefficient when driving a 20-kW load. Yet mismatches of this type often occur in practice, due in great extent to the difficulty in characterizing operating loads and a tendency on the part of application engineers to be conservative to make sure that the system in question is guaranteed to operate in the face of design uncertainties. More careful attention to this issue can go a long way toward increasing the efficiency of energy use in electric machine applications.

Table of Constants and Conversion Factors for SI Units

Constants

Permeability of free space $\mu_0 = 4\pi \times 10^{-7} \text{ H/m}$

Permittivity of free space $\epsilon_0 = 8.854 \times 10^{-12} \text{ F/m}$

Conversion Factors

Length	1 m = 3.281 ft = 39.37 in
Mass	1 kg = 0.0685 slug = 2.205 lb (mass) = 35.27 oz
Force	1 N = 0.225 lbf = 7.23 poundals
Torque	1 N·m = 0.738 lbf·ft = 141.6 oz·in
Pressure	1 Pa (N/m ²) = 1.45 × 10 ⁻⁴ lbf/in ² = 9.86 × 10 ⁻⁶ atm
Energy	1 J (W·sec) = 9.48 × 10 ⁻⁴ BTU = 0.239 calories
Power	1 W = 1.341 × 10 ⁻³ hp = 3.412 BTU/hr
Moment of inertia	1 kg·m ² = 0.738 slug·ft ² = 23.7 lb·ft ² = 141.6 oz·in·sec ²
Magnetic flux	1 Wb = 10 ⁸ lines (maxwells)
Magnetic flux density	1 T (Wb/m ²) = 10,000 gauss = 64.5 kilolines/in ²
Magnetizing force	1 A·turn/m = 0.0254 A·turn/in = 0.0126 oersted

A

ac excitation, 23–30
 ac machines
 armature windings, 173
 distributed ac windings, 644–656
 generated voltage, 208–213
 induction machines. *See* Induction machines
 mmf wave, 189–192, 201–208
 permanent-magnet ac motors, 293–295
 power factor, 676–677
 rotating mmf waves, 201–208
 synchronous machines. *See* Synchronous machines
 AFNL (Amperes field no load), 262
 AFSC (Amperes field short circuit), 262
 Air gap, 5
 nonuniform, 200
 uniform, 197–200
 Air-gap fringing fields, 8
 Air-gap inductances of distributed windings, 653–656
 Air-gap line, 231, 257, 361
 Air-gap space-harmonic fluxes, 233–234
 Air-gap voltage, 254
 Allowable temperature rises, 672
 Alnico 5, 8, 33, 36
 Alnico materials, 36
 American National Standards Institute (ANSI), 669
 Amortisseur winding, 296, 583
 Ampere turns, 16
 Amperes field no load (AFNL), 262
 Amperes field short circuit (AFSC), 262
 Amperes per meter, 4
 Amperes per square meter, 113
 Analytical techniques, 155–158
 Anode, 494
 ANSI, 669
 Apparent coercivity, 42
 Approximate transformer equivalent circuits, 74
 Armature mmf waves, 650–653

Armature phase-to-phase mutual inductances, 251
 Armature winding, 173
 Armature-circuit resistance control, 565
 Armature-frequency control, 596–602
 Armature-terminal voltage control, 566–570
 Armature-winding heating, 276
 Asynchronous torque, 309
 Autotransformers, 82–84
 Auxiliary winding, 456
 Axes of easy magnetization, 20

B

Balanced three-phase system, 630, 641–642
 Base values, 95
 B-H curve, 21
 Bifilar phase windings, 615
 Bifilar winding, 419
 Blocked-rotor test, 333–340
 Blondel, Andre, 657
 Blondel two-reaction method, 657
 Book
 overview, xii, xiii
 website, xiii
 Breadth factor, 648
 Breakdown torque, 310, 325
 Brush-contact loss, 669
 Brushes, 176
 Brushless dc motors, 295
 Brushless excitation system, 246
 Brushless motors, 295
 Burden, 90

C

Cantilever circuits, 73
 Cantilever equivalent circuit, 77
 Capability curves, 276–277
 Capacitive power-factor correction, 679
 Capacitor-start capacitor-run motor, 458, 459
 Capacitor-start motor, 457–459
 Capacitor-type motors, 456–460
 Castleation, 420–421, 438

Cathode, 494
 Ceramic 7, 36, 39, 42
 Ceramic magnets, 36
 Ceramic permanent magnet materials, 36
 Charge density, 113
 Class A design, 345–346
 Class B design, 346–347
 Class B insulation, 672
 Class C design, 347
 Class D design, 347
 Class F insulation, 672
 Class H insulation, 672
 Coenergy, 129
 Coercivity, 30, 33
 Coil voltage phasors, 648
 Collector, 502, 503
 Collector rings, 176
 Commutating inductance, 518–524
 Commutating poles, 392
 Commutation, 518
 Commutation interval, 521
 Commutation notches, 521
 Commutator/commutation, 186, 364–367, 390–393
 Compensating windings, 393–395
 Compound motor, 364
 Compounding curve, 275
 Computer programs. *See* MATLAB
 Concentrated full-pitch windings, 650–651
 Concentrated windings, 179
 Conduction angle, 513
 Conductor cooling, 675
 Conservation of energy, 116
 Conservative system, 120
 Constant-power drive, 563
 Constant-power regime, 580
 Constant-torque drive, 565
 Constant-torque regime, 580
 Constant-volts-per-hertz, 580
 Continuous energy conversion equipment, 112
 Continuous rating, 672
 Control, 559–627
 dc motors, 559–578
 induction motors, 595–612

- Control—*Cont.*
 scope of chapter, 559
 synchronous motors, 578–595
 VRMs, 613–616
- Conversion factors (SI units), 680
- Conversion of ac to dc. *See* Rectification
- Conversion of dc to ac. *See* Inversion
- Cooling, 674–675
- Core flux, 62
- Core loss, 63, 280, 669–670, 678
- Core-loss component, 62
- Core-loss resistance, 71, 314
- Core-type transformers, 58
- Coulombs per cubic meter, 113
- Coupled-circuit viewpoint, 215–221
- Critical field resistance, 380
- Cross-magnetizing armature reaction, 368
- CTs, 90, 93–95
- Current density, 2, 113
- Current transformer, 93
- Current transformers (CTs), 90, 93–95
- Current-source inverter, 539
- Cutting-of-flux equation, 210, 211
- D**
- Damper winding, 296, 297, 583
- dc bus voltage, 538
- dc link current, 538
- dc machines, 185–187, 357–406
 armature mmf, 367–370
 armature reaction, 374–378
 armature winding, 173
 commutator/commutation, 186, 364–367, 390–393
 compensating windings, 393–395
 compound motor, 364
 electrical-circuit aspects, 370–373
 field-circuit connections, 361
 generated voltage, 213–214
 generator analysis, 379–382
 magnetic-circuit aspects, 374–378
 mmf wave, 192–197
 motor analysis, 382–384
 permanent-magnet, 384–390
 schematic connection diagram, 395
 schematic representation, 358
 series motors, 364
 series universal motors, 395–396
 shunt/separately-excited motors, 363–364
 speed control, 560–564
 speed-torque characteristics, 363
- steady-state performance, 379–384
- torque control, 574–578
- volt-ampere characteristics, 362
- dc magnetization curve, 21–22
- Deep-bar rotors, 343–347
- Definite-purpose machines, 672
- Delayed commutation, 391
- Delta-Delta connection, 86
- Delta-Y connection, 86
- Demagnetizing effect of
 cross-magnetizing armature reaction, 368
- Diode symbol, 494
- Diodes, 494–496
- Direct axis, 282, 357
- Direct-and quadrature-axis (dpO)
 theory, 657–667
- Direct-axis air-gap flux, 282, 360
- Direct-axis components, 282, 583, 604
- Direct-axis magnetizing reactances, 284
- Direct-axis permeance, 361
- Direct-axis quantity, 284
- Direct-axis synchronous inductance, 661
- Direct-axis synchronous reactances, 284
- Direct-axis theory, 282–289
- Disk armature permanent-magnet servomotor, 388
- Distributed ac windings, 644–656
- Distributed fractional-pitch windings, 645–647, 651–653
- Distributed two-pole, three-phase, fractional-pitch armature winding, 653
- Distributed two-pole, three-phase, full-pitch armature winding, 652
- Distributed windings, 179, 187, 189, 190
- Distribution transformer, 59
- Doherty, R. E., 658
- Double-layer windings, 645
- Double-revolving-field concept, 455, 463
- Double-squirrel-cage rotors, 343–347
- Doubly-salient VRM, 408–411, 416
- dqO transformation, 657–667
- Drain, 502
- Dynamic equations, 151–155
- E**
- Eddy current, 26
- Eddy-current loss, 670
- Effective permeability, 20
- Efficiency
 electric machinery, 678–679
- mechanical losses, and, 668
- power conversion device, 80
- synchronous machine, 279
- 8/6 VRM, 419
- 800-A winding, 94
- Electric field intensity, 11
- Electrical degrees, 178
- Electrical radians, 178
- Electromagnetic power, 370
- Electromagnetic relay, 119
- Electromechanical-energy-conversion principles, 112–172
 analytical techniques, 155–158
 determination of magnetic force/torque from coenergy, 129–136
 determination of magnetic force/torque from energy, 123–129
 dynamic equations, 151–155
 energy balance, 117–119
 energy in singly-excited magnetic field systems, 119–123
 forces/torques in magnetic field systems, 113–117
 forces/torques in systems with permanent magnets, 142–151
 gross motion, 156–157
 linearization, 157–158
 multiply-excited magnetic field systems, 136–142
 purpose, 112
- Electromotive force (emf), 11n, 210
- Electrostatic voltmeter, 165
- Emf, 11n, 210
- Emitter, 502, 503
- End effects, 230
- End-turn fluxes, 234
- Energy balance, 117–119
- Energy efficiency, 668, 678–679
- Energy method, 116
- Engineering aspects, 668–679
 cooling, 674–675
 energy efficiency, 668, 678–679
 excitation, 676–678
 heating, 670–674
 insulation, 671–672
 losses, 668–670
 rating, 672–674
 transformer analysis, 73–81
- Equivalent circuits
 induction machines, 313–321
 instrumentation transformer, 91

- permanent-magnet dc motor, 389
 - single-phase induction motor, 464
 - synchronous machines, 252–256
 - transformers, 68–73
 - two-phase motor, 472
 - Equivalent series impedance, 73
 - Equivalent series reactance, 73
 - Equivalent series resistance, 73
 - Equivalent-T circuit, 72
 - Excitation, 676–678
 - ac, 23–30
 - negative, 38
 - self-, 677
 - turbine-generator systems, 677–678
 - under/overexcited, 677
 - Excitation branch, 71
 - Excitation phenomena, 24
 - Excitation system, 245
 - Exciter, 245
 - Exciting component, 69
 - Exciting current, 24, 25, 60
 - Exciting impedance, 71
 - Exciting rms voltamperes per unit mass, 25
- F**
- Faraday's Law, 11, 25, 210, 579
 - Ferrite magnets, 36
 - Ferrites, 58
 - Ferromagnetic materials, 20
 - Fictitious winding, 143
 - Field axis, 357
 - Field-current control, 560–565
 - Field-current waveform, 561
 - Field-oriented control, 583–595
 - Field-resistance line, 379
 - Finite-element method, 131
 - Firing-delay angle, 498
 - Firing-delay time, 498
 - First law of thermodynamics, 117
 - Five-wire, 643
 - Flat-compounded, 381
 - Flux
 - air-gap space-harmonic, 233–234
 - direct-axis air-gap, 292, 360
 - end-turn, 234
 - leakage, 233–234
 - mmf waves, and, 282–284
 - mutual, 233
 - quadrature-axis air-gap, 283
 - slot-leakage, 234
 - Flux linkage, 11
 - Force density, 113
- F**
- Forced commutation, 525
 - Force-producing devices, 112, 116
 - Forward voltage drop, 495
 - Forward-breakdown voltage, 497
 - Fourier series, 189, 197
 - Four-phase systems, 643
 - Four-phase 8/6 VRM, 419
 - Four-pole dc machine, 196
 - Four-pole single-phase synchronous generator, 178
 - Four-pole synchronous-reluctance motor, 461
 - 4/2 VRM, 414–416, 418
 - Fractional slip, 308
 - Fractional-pitch coil, 649
 - Free-wheeling diode, 515
 - Fringing fields, 8
 - Full-pitch coil, 188
 - Full-wave bridge, 507
 - Full-wave bridge rectifier, 507
 - Full-wave phase-controlled SCR rectifier system, 532
 - Full-wave rectification, 507
 - Full-wave rectifier, 507
- G**
- Gate, 502, 503
 - General-purpose motor, 672
 - Generated voltages, 252, 644–650
 - Generator reference direction, 253
 - Grain-oriented steel, 27. *See also* M-5 grain-oriented electrical steel
 - Gross motion, 156–157
- H**
- Half-wave rectifier circuit
 - diodes, 495
 - inductive load, 513
 - SCR, 498
 - Hard magnetic materials, 33
 - H-bridge, 539–544
 - H-bridge inverter configuration, 542
 - Heating, 670–674
 - Henrys per ampere, 12
 - Henrys per meter, 4
 - High-speed synchronous motor, 183
 - High-voltage, 57n
 - High-voltage side, 80
 - Holding current, 497
 - Hunting transient, 247
 - Hybrid stepping motor, 444–446
 - Hydrogen, 674–675
 - Hydrogen cooling, 675
- I**
- Hysteresis effect, 20
 - Hysteresis loop, 21, 27
 - Hysteresis loss, 27, 670
 - Hysteresis motors, 462–463
- I**
- Ideal current transformer, 90, 93
 - Ideal potential transformers, 90
 - Ideal transformer, 64–67
 - Idealized synchronous machine, 658
 - Ideal-switch model, 504, 505
 - IEEE, 669
 - IGBT, 502–506
 - Impedance
 - equivalent series, 73
 - exciting, 71
 - referring the, 66
 - Thevenin-equivalent, 322
 - Thevenin-equivalent stator, 323
 - Indirectly cooled bar, 675
 - Induced voltage, 11
 - Inductance
 - commutating, 518–524
 - defined, 12
 - direct-axis synchronous, 661
 - leakage, 252
 - magnetizing, 71
 - mutual, 17
 - primary leakage, 68
 - quadrature-axis synchronous, 661
 - rotor self, 249–250
 - secondary leakage, 71
 - self, 17
 - stator, 250–252
 - stator-to-rotor mutual, 250
 - synchronous, 250–252
 - zero-sequence, 661
 - Induction machines, 183–185, 306–356.
 - See also* ac machines
 - blocked-rotor test, 333–340
 - currents/fluxes, 311–313
 - deep-bar rotors, 343–347
 - double-squirrel-cage rotors, 343–347
 - dqO transformation, 664–667
 - equivalent circuit, 313–321
 - no-load test, 330–333
 - rotor resistance, 340–347
 - single-phase induction motors. *See* Single-phase induction motors
 - speed control, 595–603
 - Thevenin's theorem, 322–330
 - torque control, 603–612
 - torque/power, 322–330

- Induction machines—*Cont.*
torque-speed characteristics, 345, 346
two-phase induction motors, 470–488
wound-rotor motors, 340–343
- Induction-motor speed-torque characteristic, 185
- Inductive load with series dc source, 532–533
- Infinite bus, 246
- Inner cooling, 675
- Institute of Electrical and Electronics Engineers (IEEE), 669
- Instrumentation transformers, 90
- Insulated-gate bipolar transistor (IGBT), 502–506
- Insulation, 671–672
- Insulation life tests, 671
- Integral-horsepower dc motor, 185
- Integration paths, 120
- Internal voltage, 252
- Interpoles, 392
- Inversion, 538–550
H-bridge, 539–544
pulse-width-modulated voltage-source inverters, 544–549
three-phase inverters, 549–550
- Inverter, 538
- Inverter configuration, 614–615
- Inverter-input representations, 538
- I^2R losses, 669, 678
- Iron-core transformer, 58
- J**
- Joules (J), 18
- Joules per seconds, 18
- K**
- Kirchhoff's current law, 9, 632, 637
- Kirchhoff's voltage law, 8, 632
- kVA rating, 676
- L**
- Lagging, 633
- Leading, 633
- Leakage fields, 9
- Leakage flux, 58, 233–234
- Leakage inductance, 252
- Leakage reactance, 254
- Light dimmer, 501
- Linear commutation, 366
- Linear machines, 227–230
- Linearization, 157–158
- Line-to-line voltages, 631
- Line-to-neutral voltages, 631
- Line-voltage control, 602, 603
- Load component, 69
- Load loss, 264
- Long-shunt connection, 373
- Lorentz Force Law, 113
- Loss of synchronism, 248
- Losses, 668–670
brush-contact, 669
core, 669–670
eddy-current, 670
efficiency, and, 668
hysteresis, 670
 I^2R , 669
load, 264
mechanical, 669
no-load rotational, 258, 670
ohmic, 669
open-circuit core, 669
reducing/minimizing, 678
short-circuit load, 264
stray load, 264, 270
- Lossless electric energy, 165
- Lossless-energy-storage system, 118
- Low-voltage, 57n
- M**
- M-5 electrical steel, 33
- M-5 grain-oriented electrical steel
B-H loops, 21
core loss, 28
dc magnetization curve, 22
exciting rms voltamperes, 26
- Machine efficiency, 668, 678–679
- Machine losses. *See* Losses
- Machine rating, 672–674
- Machines. *See* Motors
- Magnetic circuit model, 9
- Magnetic circuit with two windings, 17
- Magnetic circuit/material, 1–56
ac excitation, 23–30
air gaps, 5
B-H curve, 21
dc magnetization curve, 21–22
electric circuits, compared, 6
Faraday's law, 11
flux linkage, 11
fringing fields, 8
inductance, 12
leakage fields, 9
mmf drop, 8
mutual inductance, 17
permanent magnets, 30–42
properties, 19–23
right-hand rule, 4
self-inductance, 17
- Magnetic field intensity, 2
- Magnetic field viewpoint, 221–226
- Magnetic-field electromechanical energy conversion device, 116
- Magnetic fields in rotating machinery, 197–201
- Magnetic flux, 3
- Magnetic flux density, 2
- Magnetic hysteresis, 20
- Magnetic permeability, 4
- Magnetic saturation, 230–233
- Magnetic stored energy, 18
- Magnetization curve, 230, 360
- Magnetizing current, 62
- Magnetizing inductance, 71
- Magnetizing reactance, 71, 254, 314
- Magnetizing resistance, 71
- Magnetomotive force (mmf), 3
- Main winding, 456
- Main-field mmf, 374
- Matching machine to application, 679
- MATLAB, x–xiii
polyfit, 124
program code. *See* MATLAB scripts
spline(), 399–405
student version, xi
- MATLAB scripts
armature-frequency control, 601–602
armature-terminal voltage control, 569–570
block-rotor test, 339–340
current (current transformers), 94
determination of magnetic force/torque, 126
electromechanical mechanical torque, 328–329
full-wave bridge rectifier, 510–511
inductance, 15
steady-state power-angle characteristics, 271–272
symmetrical-component systems, 475–477
torque control (induction motors), 611–612
unsymmetrical two-phase induction machines, 485–488
voltage, 92
VRMs, 426–428, 436
- Maximum electromechanical torque, 325

Maximum energy product, 33
 Maxwell's equations, 2
 Mechanical losses, 669
 Metal-oxide-semiconductor field effect transistor (MOSFET), 502–506
 Microstepping, 443
 Minor hysteresis loop, 38
 mmf, 3
 mmf drop, 7, 8
 mmf waves, armature, 650–653
 MOSFET, 502–506
 Motor reference direction, 253
 Motor size, 673, 679
 Motorettes, 671
 Motors
 ac machines. *See* ac machines
 capacitor-type, 456–460
 dc machines. *See* dc machines
 general-purpose, 672
 hysteresis, 462–463
 induction machines. *See* Induction machines
 linear machines, 227–230
 pole-changing, 596
 rating, 672–674
 size, 673, 679
 special-purpose, 672
 split-phase, 456
 stepping, 437–446
 synchronous machines. *See* Synchronous machines
 VRMs. *See* Variable-reluctance machines (VRMs)
 Multicircuit transformers, 84
 Multiply-excited magnetic field systems, 136–142
 Multi-stack variable-reluctance stepping motor, 438
 Multiwinding transformers, 84–85
 Mutual flux, 233
 Mutual inductance, 17

N

National Electrical Manufacturers Association (NEMA), 669
 n-channel IGBT, 502
 n-channel MOSFET, 502
 Negative excitation, 38
 Negative sequence, 471
 NEMA, 669
 Neodymium-iron-boron, 37, 39, 42
 Newtons per cubic meter, 113

Nickle, C. A., 658
 No-load conditions, 60–64
 No-load magnetization characteristics, 375
 No-load phasor, 63
 No-load rotational loss, 670
 No-load rotational losses, 258
 No-load test, 330–333
 Nonoriented electrical steels, 28
 Nonoriented steels, 28
 Nonuniform air gaps, 200
 Normal magnetization curve, 21
N-turn concentrated coil, 654
 Numerical-analysis package, xii. *See also* MATLAB

O

Ohmic losses, 669
 Ohm's law, 379
 1.8°/step hybrid stepping motor, 444
 Open-circuit characteristics, 230, 231, 256–258
 Open-circuit core loss, 669
 Open-circuit core-loss curve, 259
 Open-circuit saturation curve, 256
 Open-circuit test, 78–80
 Open-circuited secondary, 79
 Open-delta connection, 86
 Operating temperature, 671
 Opposite phase sequence, 472
 Overexcited, 677
 Overview of book, xii, xiii

P

Park, R. H., 658
 p-channel IGBT, 502
 p-channel MOSFET, 502
 Permanence, direct axis, 361
 Permanent magnet, 30–43
 Permanent-magnet ac motors, 293–295
 Permanent-magnet dc machines, 384–390
 Permanent-magnet stepping motors, 439–441
 Permanent-split-capacitor motor, 457, 458
 Permeability
 effective, 20
 magnetic, 4
 recoil, 38, 42
 Permeance, 7
 Per-unit system, 95–103
 Phase, 628
 Phase order, 631
 Phase sequence, 631
 Phase-controlled rectifier, 499
 Phasor diagram, 63
 Pitch factor, 650
 Plugging, 323
 Pole-changing motors, 596
 Pole-face winding, 393–395
 Polyphase induction machines. *See* Induction machines
 Polyphase mmf, 207
 Polyphase synchronous machines. *See* Synchronous machines
 Polyphase systems, 628, 643
 Positive sequence, 471
 Pot-core, 47
 Potential transformers (PTs), 90–93
 Power, 18
 Power angle, 267
 Power electronics, 493–558
 diodes, 494–496
 IGBT, 502–506
 inversion, 538–550. *See also* Inversion
 MOSFET, 502–506
 power switches, 494–506
 rectification, 507–538. *See also* Rectification
 SCR, 496–499
 transistors, 502–506
 TRIAC, 500
 Power factor
 ac machines, 676–677
 three-phase systems, 633, 635
 Power switches, 494–506
 Power-angle characteristic
 defined, 267
 salient-pole machines, 289–293
 steady-state, 266–275
 Power-factor angle, 633
 Practical VRM drive systems, 415
 Primary, 57
 Primary leakage inductance, 68
 Primary leakage reactance, 69
 Primary resistance, 61
 Prime mover, 246
 Projecting poles, 179
 PTs, 90–93
 Pulling out of step, 248
 Pull-out torque, 248
 Pulse-width-modulated voltage-source inverters, 544–549
 Pulse-width modulation (PWM), 545

Q

- Quadrature axis, 282, 357
- Quadrature-axis air-gap fluxes, 283
- Quadrature-axis components, 282, 583, 604
- Quadrature-axis magnetizing reactances, 284
- Quadrature-axis quantity, 284
- Quadrature-axis synchronous inductance, 661
- Quadrature-axis synchronous reactances, 284
- Quadrature-axis theory, 282–289

R

- Rating of machinery, 672–674
- Ratio of transformation, 58
- Reactance
 - direct/quadrature-axis magnetizing, 284
 - direct/quadrature-axis synchronous, 284
 - equivalent series, 73
 - leakage, 69, 71, 254
 - magnetizing, 71, 254, 314
 - primary leakage, 69
 - secondary leakage, 71
 - synchronous, 253
 - unsaturated synchronous, 260
- Reactance voltage, 391
- Reactive kVA, 677
- Recoil line, 38
- Recoil permeability, 38, 42
- Rectification, 507–538
 - commutating inductance, 518–524
 - inductive load with series dc source, 532–533
 - single-phase full-wave diode bridge, 507–512
 - single-phase full-wave phase-controlled bridge, 524–532
 - single-phase rectifier with inductive load, 513–518
 - three-phase bridges, 533–538
- Reference direction, 253
- Referring the impedance, 66
- Regenerating, 527
- Reluctance, 6
- Remanent magnetization, 21, 30
- Residual flux density, 30
- Residual magnetism, 360
- Resistance
 - core-loss, 71, 314

- critical field, 380
- equivalent series, 73
- magnetizing, 71
 - primary, 61
 - secondary, 71
- Resistance commutation, 392
- Resultant core flux, 16
- Reverse-breakdown voltage, 495
- Revolving-field theory, 463–470
- Right-hand rule, 4, 114
- Ripple voltage, 510
- Rms, 23
- rms-kW method, 673
- Root-mean-square (rms), 23
- Rotor, 173, 175
- Rotor self inductance, 249–250
- Rotor-resistance control, 603
- Run winding, 456

S

- Salient pole, 176
- Salient-pole synchronous machine, 281–293
- Salient-pole synchronous machine and series impedance, 289
- Samarium-cobalt, 37, 39, 42
- Saturated, 20
- Saturation
 - magnetic, 230–233
 - VRMs, 431–437
- Saturation curve, 230–231
- Saturation voltage, 504
- Sawtooth armature mmf wave, 196
- Sawtooth waveform, 548
- SCR, 262, 496–499
- SCR symbol, 497
- Secondary, 57
- Secondary leakage inductance, 71
- Secondary leakage reactance, 71
- Secondary resistance, 71
- Self-excitation, 677
- Self-excited generators, 361
- Self-inductance, 17
- Self-starting synchronous-reluctance motors, 460–462
- Separately-excited generator, 361
- Separately-excited motors, 363
- Series motor, 364
- Series universal motors, 395–396
- Series-field diverter, 381
- Service factor, 672
- Shading coil, 460
- Shared-pole induction motor, 460
- Shell-type transformers, 58
- Short-circuit characteristics, 258–265
- Short-circuit load loss, 264
- Short-circuit ratio (SCR), 262
- Short-circuit test, 77–78
- Short-circuited secondary, 77
- Short-shunt connection, 373
- Short-time ratings, 673, 674
- Shunt motor, 363
- Shunted-armature method, 565, 566
- SI units, 680
- Silicon controlled rectifier (SCR), 496–499
- Simple magnetic circuit, 3
- Simulink, xiii
- Single-line diagrams, 641–642
- Single-phase full-wave diode bridge, 507–512
- Single-phase full-wave phase-controlled bridge, 524–532
- Single-phase H-bridge step-waveform inverters, 539–544
- Single-phase induction motors, 452–470, 489
 - capacitor-type motors, 456–460
 - equivalent circuits, 464
 - revolving-field theory, 463–470
 - schematic view, 453
 - shaded-pole induction motors, 460
 - split-phase motors, 456
 - torque-speed characteristic, 454
- Single-phase rectifier with inductive load, 513–518
- Single-phase systems, 628
- Single-phase winding space-fundamental air-gap mmf, 202
- Single-stack variable-reluctance stepping motor, 438
- Singly-excited electromechanical system, 151
- Singly-salient VRM, 408–410
- 6/4 VRM, 417
- 660-MVA three-phase 50-Hz transformer, 60
- Size of machine, 673
- Slip, 308
- Slip frequency, 309
- Slip rings, 176, 245
- Slot-leakage flux, 234
- Snubber circuit, 497
- Soft magnetic materials, 33
- Software packages. *See* MATLAB
- Source, 502

- Space-fundamental air-gap flux, 251
 - Sparkling, 390
 - Sparkless commutation, 391
 - Special-purpose motor, 672
 - Speed control
 - dc motors, 560–564
 - induction motors, 595–603
 - synchronous motors, 578–583
 - Speed voltage, 103, 152, 210, 359
 - Squirrel-cage induction motor, 184
 - Squirrel-cage rotor, 306–308, 343–347
 - SRMs. *See* Variable-reluctance machines (VRMs)
 - Stabilized, 42
 - Stabilizing winding, 383
 - Standard-setting bodies, 669
 - Start winding, 456
 - Starting capacitor, 457
 - State function, 123
 - State variables, 120
 - Stator, 173, 174
 - Stator inductance, 250–252
 - Stator windings, 173
 - Stator-to-rotor mutual inductances, 250
 - Steady-state power-angle characteristics, 266–275
 - Stepping motors, 437–446
 - Stray load loss, 264, 670
 - Switched-reluctance machines. *See* Variable-reluctance machines (VRMs)
 - Symmetrical-component concept, 471–477
 - Synchronous angular velocity, 206
 - Synchronous condensers, 677
 - Synchronous generator capability curve, 276–277
 - Synchronous inductance, 250–252
 - Synchronous machines, 176–183, 245–305. *See also* ac machines
 - direct-axis theory, 282–289
 - dqO transformation, 660–663
 - efficiency, 279
 - equivalent circuits, 252–256
 - flux/mmf waves, 282–284
 - inductances, 249–252
 - open-circuit characteristics, 256–258
 - permanent-magnet ac motors, 293–295
 - power-angle characteristics, 266–275, 289–293
 - quadrature-axis theory, 282–289
 - salient-pole machines, 281–293
 - self-starting reluctance machines, 460–462
 - short-circuit characteristics, 258–265
 - speed control, 578–583
 - steady-state operating characteristics, 275–281
 - steady-state power-angle characteristics, 266–275
 - torque control, 583–595
 - Synchronous reactance, 253
 - Synchronous speed, 206, 221
 - Synchronous-generator V curves, 278
 - Synchronous-machine equivalent circuits, 252–256
- T**
- Temperature rises, 672
 - Teslas, 4
 - Tests
 - blocked-rotor, 333–340
 - induction motors, 330–340
 - no-load, 330–333
 - open-circuit, 78–80, 258–262
 - short-circuit, 77–78, 256–258
 - stray-load-loss, 330
 - Thevenin-equivalent circuit, 323
 - Thevenin-equivalent impedance, 322
 - Thevenin-equivalent stator impedance, 323
 - Thevenin-equivalent voltage, 322
 - Thevenin's theory, 322
 - Three-phase ac machine, 209
 - Three-phase bridges, 533–538
 - Three-phase circuits, 628–643
 - balanced system, 630, 641–642
 - generation of voltage, 628–631
 - instantaneous power, 634
 - other polyphase systems, 643
 - schematic view of three-phase generator, 182
 - single-line diagrams, 641–642
 - transformers, 85–90
 - unbalanced system, 631
 - voltages/currents/power, 631–635
 - Y/delta-connected circuits, 635–640
 - Three-phase connections, 630
 - Three-phase inverters, 549–550
 - Three-phase six-pulse diode bridge, 534
 - Three-phase system, 628
 - Three-phase three-stack variable-reluctance stepping motor, 439
 - Three-phase transformer, 86
 - Three-phase transformer bank, 85
 - Three-phase transformer connections, 86
 - Threshold voltage, 504
 - Thyristor, 496
 - Time-averaged voltage, 513
 - Torque
 - asynchronous, 309
 - breakdown, 310, 325
 - coenergy, and, 129–136
 - coupled-circuit viewpoint, 215–221
 - energy, and, 123–129
 - induction machines, 322–330
 - magnetic field viewpoint, 221–226
 - maximum electromechanical, 325
 - nonsalient-pole machines, 214–226
 - permanent magnets, 142–151
 - pull-out, 248
 - VRM, 421–430
 - Torque angle, 247
 - Torque constant, 389
 - Torque control
 - dc motors, 574–578
 - induction motors, 603–612
 - synchronous motors, 583–595
 - Torque-angle characteristic, 247
 - Torque-angle curve, 247
 - Total ampere-turns, 16
 - Transducers, 112
 - Transformer equivalent circuit, 68–72
 - Transformer reactance, 68–72
 - Transformers, 57–111
 - autotransformers, 82–84
 - core-type, 58
 - current, 93–95
 - distribution, 59
 - engineering aspects, 73–81
 - ideal, 64–67
 - instrumentation, 90
 - multiwinding, 84–85
 - no-load conditions, 60–64
 - open-circuit test, 78–80
 - per-unit system, 95–103
 - potential, 90–93
 - reactances, 68–72
 - secondary current, 64–67
 - shell-type, 58
 - short-circuit test, 77–78
 - three-phase circuits, 85–90
 - two-winding, 82
 - voltage regulation, 81
 - Transistors, 502–506
 - TRIAC, 500
 - TRIAC symbol, 500

Turbine-generator excitation systems, 677–678

Two-phase permanent magnet stepping motor, 440

Two-phase systems, 643

Two-pole cylindrical-rotor field winding, 179

Two-pole dc machine, 194

Two-pole single-phase synchronous generator, 177

Two-pole three-phase generator, 629

Two-pole three-phase stator winding, 204

Two-pole 3600r/min turbine generator, 181

Two-winding transformer, 82

200-MVA, three-phase, 50-Hz, three-winding, 210/80/10.2-kV transformer, 87

2400-V winding, 92

U

Unbalanced operation of symmetrical two-phase machines, 471–477

Unbalanced three-phase system, 631

Undercommutation, 391

Underexcited, 677

Uniform air gaps, 197–200

Universal motor, 395

Unsaturated synchronous reactance, 260

Unsymmetrical two-phase induction machines, 478–488

V

V connection, 86

V curves, 278

Variable-frequency solid-state motor drives, 348

Variable-reluctance machines (VRMs), 407–451

 bifilar winding, 419

 castleation, 420–421

 control, 613–616

 doubly-salient VRM, 408–411, 416

 4/2 machines, 414–416, 418

 nonlinear analysis, 430–437

 practical configurations, 415

 saturation effects, 431–437

 singly-salient VRM, 408–410

 stepping motors, 437–446

 6/4 machines, 417

 torque, 421–430

Vector control, 583

Ventilating system, 674

v-i characteristic

 ideal diodes, 494

 idealized SCR, 497

 idealized TRIAC, 500

 inductor, 513

 n-channel IGBT, 503

 n-channel MOSFET, 503

 SCR, 497

Voltage behind leakage reactance, 254

Voltage commutation, 392

Voltage, generated, 644–650

Voltage ratio, 58

Voltage regulation, 81

Voltage-regulating system, 276

Voltage-source inverter, 538

VRMs. *See* Variable-reluctance machines (VRMs)

W

Water-cooled bar, 675

Water-cooled rotor, 179

Watts (W), 18

Webers per ampere-turn-meter, 4

Webers per square meter, 4

Weber-turns per ampere, 12

Winding factor, 190

Windings (distributed ac windings), 644–656

Wound rotor, 306

Wound-rotor motors, 340–343

Y

Y-delta connection, 86

Y-Y connection, 86

Z

Zero-sequence component, 658

Zero-sequence inductance, 661