28 Alternating Current Generators

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28.1 Introduction

For the generation, transmission, distribution and use of electrical power the three-phase system has large economic and practical advantages over single-phase or two-phase systems. Hence the great majority of alternating-current (a.c.) generators are three-phase machines, operating at one of the standard frequencies, 50 or 60 Hz. Some generators operate at other frequencies; examples are: (a) generators up to several megawatts in output operating at $16\frac{2}{3}$ Hz have been used for years to supply rail traction systems, mostly in Europe, (b) high frequency generators, at 500–10 000 Hz, were used extensively for induction heating in industrial processes, (c) small shaft mounted generators with more than three phases are used as exciters for synchronous generators and some hydrogenerators, (d) aircraft ground power supplies operating at 400 Hz, and (e) a growing trend for machines to operate at other variable frequencies with power electronics being used to convert their output to 50 or 60 Hz. However, due to the cost of the electronic conditioning equipment, these are largely limited to less than 150 kW in output and under 1000 V.

The form of construction of the generator depends on its output power and its speed: these are determined by the prime mover that drives it. To generate at a frequency of f hertz when driven at n rev/min the generator must have $2p$ poles, where

$$2p = \frac{120f}{n}$$

n is the synchronous speed, being the same as the speed of rotation of the magnetic field produced by currents in a three-phase winding when connected to a supply of frequency f Hz. The maximum permissible diameter of the rotor will be determined by the rotational stresses acting on it and thus the speed of rotation determines the shape of the generator. For a given output power, a high-speed machine will have a smaller diameter, and longer length than a low-speed machine.

Generators may be classified as follows.

Synchronous generator This type of generator requires a winding carrying direct current (or in small sizes a series of permanent magnets) to establish the magnetic flux. In nearly all machines this excitation winding (known as the field winding) is carried on the rotor, which for a 50 or 60 Hz output must rotate at the synchronous speed.

There are two sorts of synchronous generator which are differentiated by the type of rotor used; rotors are either cylindrical or of the salient pole type.

The first type of synchronous generators are known as *turbogenerators*. This family of machines use a cylindrical rotor in which the field winding is housed in axial slots. They are invariably driven by a steam turbine or a gas turbine. At ratings below 60 MW a gear box may be used to provide a rotational speed of 3600 (2 pole) or 1800 rev/min (4 pole) to provide power at 60 Hz, or 3000 rev/min (2 pole) or 1500 rev/min (4 pole) to provide power at 50 Hz. Alternatively, and especially at higher power ratings the generator is directly driven by the steam or gas turbine. The rotors will thus have either two or four poles. Smaller machines may use a laminated construction for the rotor while larger machines will use a forged rotor. A feature of these machines is that their length is several times their diameter. Power outputs range from a few megawatts up to about 1500 MW. The machine is cooled by circulating air or hydrogen over the active parts or water through the windings. Hydrogen was commonly used for outputs greater than about 50 MW; water was, and still is, used for the stator winding with outputs exceeding about 200 MW. Air-cooled machines are now available up to almost 200 MW.

The second type of synchronous generators use a salient pole rotor. Types of salient pole machines are:

Hydrogenerators This is a family of salient pole machines driven by water turbines at a speed in the range 50–1000 rev/min. The speed depends on the type of turbine, which in turn depends on the head and the flow rate of the water available. At low speeds, the permissible rotor diameter will be several times its active length. Generally, the largest allowable diameter of rotor is used to maximise the machine's inertia which is an important part of governing the water turbine. Outputs up to 800 MW have been achieved. A small high-speed unit will have a horizontal shaft, but for reasons of mechanical construction and stability larger machines have vertical shafts.

Reciprocating gas, diesel or petrol engine-driven generators For this application, the generator may be coupled directly to the internal combustion engine and the generator will invariably be of the salient pole construction. Many combinations of output power and speed are available, from a few kilovoltamperes, usually at four-pole speed, up to 45 MW or more at 100 rev/min, using a 2 stroke diesel engine of the type used for ship propulsion.

Steam turbine or gas turbine driven generators Salient pole generators connected to these prime movers are driven through a gearbox, usually at 4-pole speed. Ratings are limited to below 60 MW both by the gearbox capacity and by the difficulty of holding large salient pole rotors together.

Synchronous compensators (also known as synchronous capacitors) These machines draw current from the system at zero power factor, lagging or leading as required to control the voltage of the system. They also draw a small amount of real power to maintain the synchronous rotation. They will usually have six or eight salient poles, and a rating in the range from a few megavolt-amperes up to say 350 MVAR. For machines larger than say 50 MVAR, hydrogen cooling is used to reduce the windage loss. The synchronous compensator has been rendered obsolete by the development of static volt-ampere (VAR) control equipment.

Asynchronous (induction) generators In construction these machines resemble induction motors, and similarly draw their magnetising current from the power system, to which they deliver power when driven at very slightly above synchronous speed. Ratings are usually less than 3 MW, at speeds up to 1000 rev/min. Much smaller units, operating with capacitor excitation circuits, can provide isolated power supplies.

For generators of all types the practicable and economic voltage increases with the increase of rated output in order to limit the amount of current to be handled to a manageable level. Some standards specify preferred voltages, and some are normally mandatory. Typical ranges of output and voltage are:

MVA	1–6	4–16	4–100	100–400	400–800	800–1500
kV	1–4	5–7	10–13	14–16	18–23	26–30

However, a number of manufacturers have found significant markets by going away from these norms. Low voltage, high current machines together with an appropriate transformer have found favour where regulations restrict the installation of higher voltage generators. Conversely, high voltage generators, even at very modest outputs have proved attractive as the cost of a step-up transformer can be avoided.

Operating generators singly or in a group When a generating set runs alone to supply power to a load, the prime mover supplies the active power demanded. As the demand changes, small changes of speed cause the governor to adjust the mechanical input power to maintain as nearly as possible the correct speed and frequency. The generator supplies the active power and the reactive power required by the load, and this determines the power factor. Changing the generator's field current changes the terminal voltage, and the consequent changes in active and reactive power depend on the nature of the load, e.g. the proportion of motor load to static load. Usually, as the load changes, the voltage is held almost constant by adjustments to the excitation made by the automatic voltage regulator.

Most generators, however, are synchronous machines connected to an extensive supply network, and changing the excitation of one machine does not affect the system voltage, the speed or the power output of the generator. It changes only the reactive power, and a compensating change in reactive power is shared by other generators on the same or neighbouring bus-bars. Thus adjusting the generator field current when it is connected to a system results only in a change in the power factor at which the generator operates.

System engineers find it convenient to regard active power in watts (P) and reactive power in vars (Q) (the two components of the apparent power in volt-amperes (VA)) as separate but related entities to be generated, transmitted and absorbed.

Induction motors, chokes, transformers and underexcited synchronous machines all draw magnetising current, lagging the supply voltage by 90°. By convention, this is regarded as a demand for positive vars from the system. Capacitors and overexcited synchronous machines draw leading current, and so supply positive vars to the system. A synchronous machine is overexcited if its field current is more than is needed to make it operate at unity power factor.

28.2 Airgap flux and open-circuit e.m.f.[1,6,7,8,11,17,19,23]

28.2.1 Airgap flux waveforms[7,16,23,44–47]

Figure 28.1 shows the flux pattern of a turbogenerator (t.g.) on open circuit, and flux waveforms for it and for an 18-pole salient-pole (s.p.) generator. The t.g. flux waveform is inherently trapezoidal because of the distributed field winding and uniform length of airgap. Prominent ripples caused by the stator slots and teeth have been omitted (see Section 28.2.4).

The salient pole generator flux waveform is inherently rectangular, with short sections of low density between the poles. In this example, however, the shape is closer to sinusoidal, as the fundamental component shows, because the machine was designed with shaped pole shoes. These ensure that the length of the airgap at the extremities of the pole shoe is greater than on the pole centre line. The slot ripples which arise as a result of the stator slot opening have been retained: they are not quite symmetrical because the stator did not have a whole number of slots per pole (actually $10\frac{1}{2}$). Where integral slots per pole are used, great care is needed in the design and spacing of the pole face damper damper bars in order to avoid further additional ripples appearing in the voltage waveform.

These waveforms were produced using finite element analysis: the harmonic contents of the flux waves are shown in *Table 28.1*. The reference value of 100% is the peak of the fundamental component of the open-circuit wave of each machine.

Even harmonics do not normally occur on open circuit, because the waveshape is the same under all the poles, and is symmetrical about each pole centre line. All the harmonic flux density curves go through zero at the interpolar axes. The total flux per pole is, therefore, the fundamental component plus or minus the flux of one pole-pitch of each harmonic: 'plus' if the harmonic has a negative peak at the positive peak of the fundamental, and vice versa.

Wieseman[44] gave curves for a salient pole machine relating the peak flux densities of the fundamental and of the third harmonic to the peak of the actual flux wave. The multipliers

$$\frac{\text{Peak of fundamental}}{\text{Peak of actual flux wave}} = C_1, \text{ say}$$

and

$$\frac{\text{Peak of third harmonic}}{\text{Peak of fundamental}} = C_3, \text{ say}$$

were deduced as functions of the pole arc to pole pitch ratio, the minimum airgap to pole pitch ratio and maximum edge airgap to minimum airgap.

Ginsberg *et al.*[45] gave curves from which the flux wave fundamental and harmonics up to the 11th can be estimated, again in terms of the airgap lengths and pole profile.

With usual pole shoe profiles, Cl is between 1.0 and 1.1, i.e. the actual flux wave is a bit flatter than a sine wave, and the total flux per pole is rather more than the fundamental flux Φ, alone. Wieseman's factor $k\Phi$ is the total flux divided by the fundamental component. Narrow poles with an edge airgap of about twice the minimum tend to give a peaky waveform with Cl just less than 1.0 and $k\Phi$ down to about 0.93. Wider poles and a uniform airgap give $k\Phi$ *up* to 1.06. The third harmonic flux can be made small by using a pole arc: pole pitch ratio close to 2/3; this together with a ratio maximum: minimum airgap ratio of 1.5 gives a $k\Phi$ *value close to* unity. If Φ is calculated from the e.m.f. equation, total flux and harmonic fluxes can be calculated.

Ginsberg *et al.*[46,47] provide curves from which the harmonic e.m.f.s on load could be calculated, as a per unit value of the fundamental. For detailed analysis, harmonic fluxes now are calculated by computer, e.g. by finite-element solution of the magnetic-field equations.

28.2.2 Open-circuit e.m.f.: integral slots per pole

28.2.2.1 Fundamental frequency e.m.f.

The e.m.f. induced in a single conductor lying parallel to the shaft axis has the same waveform as the flux wave. It contains all the space harmonics in the same proportions as in the flux density. It may also contain ripples caused by local

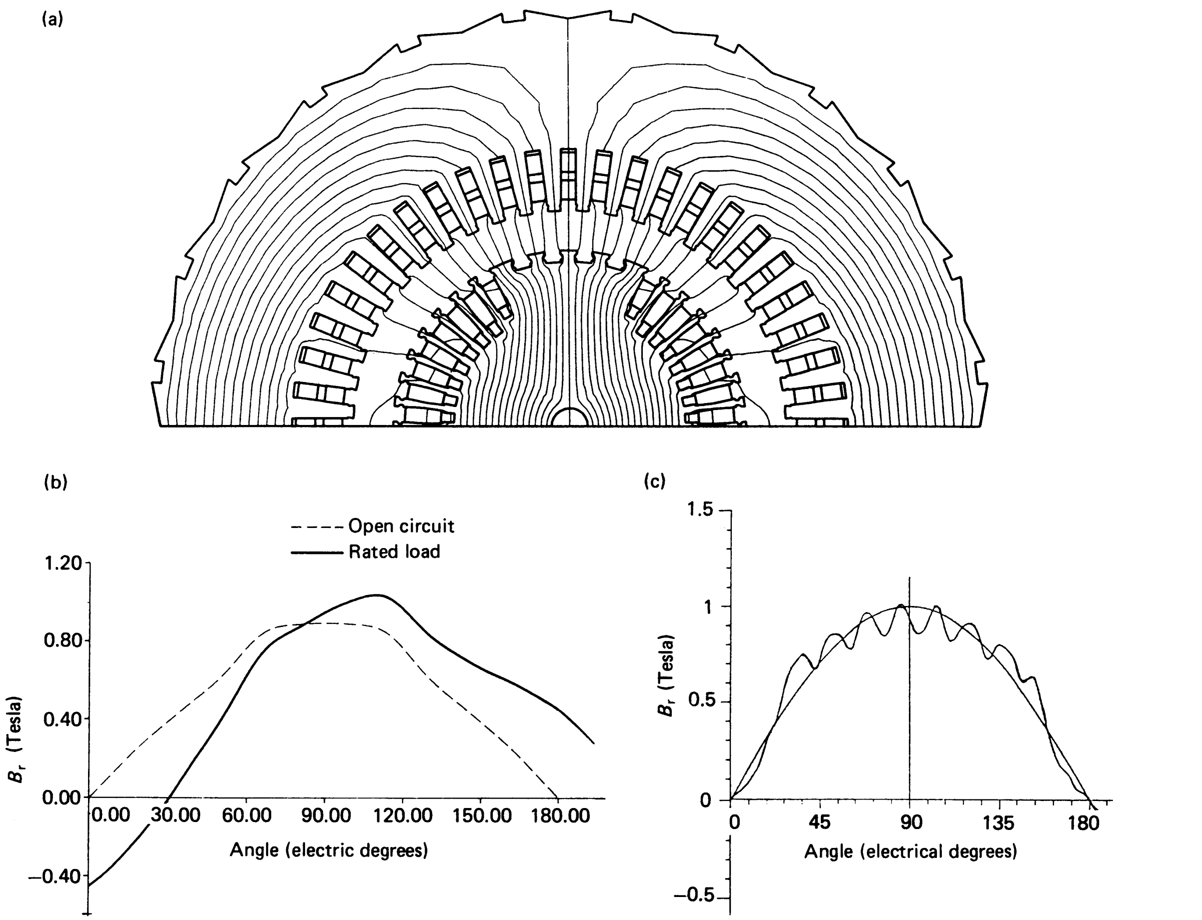

Figure 28.1 (a) Flux distribution of a turbogenerator on open circuit; (b) Flux waveforms of a turbogenerator; (c) Flux waveform of a salient pole generator on open circuit

variations in density caused by the winding slots (see Section 28.2.4). The root-mean-square (r.m.s.) value of the fundamental frequency component of the e.m.f. in a coil of one turn, spanning exactly one fundamental pole pitch, is

$$e_1 = 4.44 f \Phi_1 \text{ (volts)} \qquad (28.1)$$

where Φ_1 is the fundamental flux per pole (in webers) and f is the frequency (in hertz).

28.2.2.2 Pitch factor, k_{p1}

If there are N_p slots per pole, and the coil spans S teeth, it is short-pitched by an electrical angle

$$\beta = \left[\left(1 - \frac{S}{N_p}\right) 90°\right]$$

and the e.m.f.s generated in each of the two coil sides will be out of phase with respect to each other by β degrees.

Table 28.1 % harmonic contents of flux waves

	Harmonic order							
	Fundamental	3	5	7	9	11	13	15
Turbogenerator								
Open circuit	100	5.5	0.65	2.3	1.4	0.07	0.37	0.4
Rated load	112	13.0	2.34	2.4	1.5	0.2	0.42	0.46
Salient pole generator								
Open circuit	100	6.5	2.4	4.7	3.9	1.2	0.5	1.2
Rated load	108	18.5	3.2	4.8	4.3	2.0	0.4	1.2

The total e.m.f. is, therefore that of a full-pitch coil reduced by the factor

$$k_{p1} = \cos \tfrac{1}{2}\beta \qquad (28.2a)$$

or, in another form,

$$k_{p1} = \sin \frac{S}{N_p} 90^\circ \qquad (28.2b)$$

28.2.2.3 *Distribution factor* k_{d1}

If there are q slots per pole per phase ($q = N_p/3$ for a three-phase machine), where q is an integer, the phase difference between successive slots causes the sum of their e.m.f.s to be less than q times the e.m.f. per slot by the distribution factor, or spread factor k_{d1}. This is because vectorally each successive coil voltage is at an angle to its predecessor. This angle is determined by the total number of stator slots distributed around the bore of the stator.

$$k_{d1} = \frac{\sin q(\alpha/2)}{q \sin(\alpha/2)} \qquad (28.3)$$

where α is the electrical angle between adjacent slots. α is numerically equal to $180^\circ/N_p$ electrical degrees or $60^\circ/q$ for three phases, with the usual 60° coil phase spread. Then, for a three-phase winding,

$$k_{d1} = \frac{\sin 30}{q \sin(30/q)} \qquad (28.4)$$

28.2.2.4 *Skew factor*, k_{s1}

If the winding slot, or the longitudinal axis of the pole, is skewed at an angle δ to the shaft axis, there is a progressive change in phase of the e.m.f. along each conductor. The conductor e.m.f., and hence the phase e.m.f., is reduced by the skew factor

$$k_{s1} = \frac{2 \sin(\delta/2)}{\delta} \qquad (28.5)$$

Then, for a phase comprising of T_{ph} turns, divided into g parallel circuits, the fundamental frequency e.m.f. is

$$\begin{aligned} E_1 &= 4.44 f_1 \Phi_1 \frac{T_{ph}}{g} k_{d1} k_{p1} k_{s1} \text{ (volts)} \\ &= 4.44 f_1 \Phi_1 \frac{T_{ph}}{g} k_{w_1} \text{ (volts)} \end{aligned} \qquad (28.6)$$

28.2.2.5 *Harmonic e.m.f.s*

For the following reasons a lot of attention is now being paid to the harmonic content of the generator's voltage waveform especially with the advent of increasing amounts of private power generation equipment connected to networks.

(1) Limits of telephone interference factor (t.h.f.) are specified in many standards, and the weighting factors emphasise harmonics that can be caused by slot ripples if the design does not suppress them.
(2) Legislation is impending in the form of the Electromagnetic Compatibility Directive (EMC) 89/336/EEC. This will specify limits for the maximum levels of emitted electromagnetic radiation, and of the levels of voltage or current that can be impressed on the power system or on the load connected to the generator.
(3) Generators are being more frequently required to provide a backup supply to uninterruptible power supplies if the mains supply fails. These schemes are used to feed loads that are sensitive to voltage distortion, such as computers, television monitors and control systems.
(4) There is increased concern that harmonic currents may be large enough to cause unacceptable extra heating and losses, both in the generator and in equipment supplied by it.
(5) Inadvertent malfunction of protection and switching equipment can occur particulaly if the equipment is set to trigger at the zero crossing point of the voltage waveform.

It is difficult to estimate how large the harmonic currents arising from the harmonic voltages will be. Their values depend on the impedances of the system and of the generator at the harmonic frequency, and on the nature of the connection between their neutral points.[42]

The lower-frequency harmonics, up to say the 13th order, are produced by harmonics in the main flux wave. These result from the non-sinusoidal distribution of magnetomotive force (m.m.f.) and the non-uniform radial permeance around the circumference of the airgap. Hence they are often called *rotor permeance harmonic e.m.f.s*. The magnitude of each compared with the fundamental e.m.f. depends on the harmonic flux density relative to the fundamental density, and on the harmonic wavelength relative to the spacing and span of the stator coils, i.e. on the winding factors k_d, k_p and k_s at the relevant frequency.

E.m.f.s at frequencies associated with the number of stator slots are caused by: (a) variations in gap permeance as the poles pass the stator slots and teeth; and (b) flux waves produced by currents induced at slot frequency in the damper winding, field winding and solid steel of the rotor. These e.m.f.s are generally called *slot ripple e.m.f.s* (see Section 28.2.4).

Considering only the field form harmonics, and taking q as an integer, the pole span of the nth harmonic flux is $1/n$ of the fundamental span, and the harmonic flux per pole

$$\Phi_n = \frac{\Phi_1}{n} \cdot \frac{B_n}{B_1} \text{ (webers)}$$

where B_1 and B_n are the peak or average flux densities.

The harmonic e.m.f. generated as a result of this flux is

$$E_n = 4.44 f_n \Phi \frac{T_{ph}}{g} k_{wn} \qquad (28.7)$$

where

$$k_{wn} = k_{dn} k_{pn} k_{sn}$$

or

$$\frac{E_n}{E_1} = \frac{B_n}{B_1} \frac{k_{wn}}{k_{w1}}$$

Because any angle α on the fundamental scale embraces $n\alpha$ of the nth harmonic, the winding factors become (where q is an integer)

$$k_{pn} = \cos\frac{1}{2}n\beta;\ \text{or}\ \sin n\frac{S}{N_p}90^\circ \tag{28.8}$$

$$k_{dn} = \frac{\sin n\,30}{q\sin(n \cdot 30/q)} \tag{28.9}$$

$$k_{sn} = \frac{2\sin[(n\delta)/2]}{n\delta} \tag{28.10}$$

The r.m.s. value of the total e.m.f. per phase is then

$$E_{pn} = \sqrt{\left(E_1^2 + E_3^2 + \cdots E_n^2\right)} \tag{28.11}$$

The lower harmonic e.m.f.s are usually a few per cent of E_n and the higher orders smaller still. The total e.m.f. E_{ph} is rarely significantly greater than the fundamental, E_1 although the harmonic e.m.f.s themselves may be troublesome.

For $q = 1$, k_{d1} is 0.966, decreasing steadily to 0.955 for $q = 9$ or more. k_{dn} decreases rapidly with increasing q, *leading to a reduction in the* e.m.f. harmonics caused by space flux density harmonics in the field form. Unfortunately, k_{dn} rises periodically to equal k_{d1} for $n = m6q \pm 1$ (where m is any integer), i.e. for the slot frequency harmonics. If flux components exist at these frequencies they are usually small, but they appear in undiminished proportion as harmonics in the e.m.f. For example, for $q = 3$

n	1	3	5	7	9	11	13	15	17	19	21
k_{dn}	0.966	0.667	0.217	0.177	0.333	0.177	0.217	0.667	0.960	0.960	0.667

k_{pn} is less than k_{p1} for most slot numbers and coil pitches, but again $k_{pn} = k_{p1}$ at the slot frequencies. For comprehensive tables of k_{dn} and k_{pn}, see references 1 and 22.

k_{sn} can be made nearly zero for the slot frequencies by skewing slots or poles by one slot pitch in the length of the core. Then k_{s1} is still very nearly unity.

In principle, a particular harmonic e.m.f. can be eliminated by choosing a number of slots that allows the coil to be short-pitched by exactly half the harmonic wavelength, i.e. by $1/n$ of the pole pitch, so making k_{pn} zero. In practice, it is very rarely necessary or acceptable to impose this constraint on other design considerations. Most windings have a coil pitch close to 5/6 of the pole pitch, as this usefully reduces both the fifth and seventh harmonic e.m.f.s. This pitch is easily obtainable with any even number of slots per phase per pole.

For certain slot numbers, and harmonic orders, k_{dn} becomes negative. k_{pn} becomes negative for certain pitches and harmonic orders. Thus the harmonic e.m.f. and the harmonic flux that produces it have opposite signs, each relative to its fundamental.

In a star-connected winding, third-order and other triplen e.m.f.s do not appear between the line terminals because they are in phase with each other in all three phases and cancel out. Triplen currents cannot flow unless they have a path via a connection made to the star point. Triplen e.m.f.s act in series round a closed delta winding, and would cause circulating currents, extra losses and, perhaps, overheating. For this reason, and because a star point is usually needed for earthing, generators rarely use delta windings.

Sometimes especially for generators operating at voltages below 1000 V, 2/3 pitch coils are used to suppress the triplens in the phase e.m.f. This allows the star point to be connected directly to earth without giving rise to circulating third harmonic currents. This is especially desirable if multiple generators are to be run in parallel with the neutral point on each machine tied to earth. On higher voltage machines, large circulating triplen currents are avoided by using a high resistance or impedance in the connection of the star point to earth and the more economical and advantageous 5/6 pitch stator winding can be used.

28.2.3 Open-circuit e.m.f.: fractional slot windings

Equation (28.7) applies, but k_{wn} and the harmonic e.m.f.s are less than when q is an integer. In a winding with $q = a + (b/c)$ slots/pole/phase (where a, b and c are integers and b and c have no common factor) each phase has exactly $(ac + b)$ slots arranged in c groups in c pole pitches. The position of each phase group of a or $(a + 1)$ slots relative to its own pole is different for each of the c groups. Taking the c groups together, the $(ac + b)$ slots are uniformly distributed within an angle of 60° electrical (the coil-to-coil connections allow for the 180° phase difference between adjacent poles). Therefore, the phase angle a_c between the e.m.f.s that are electrically adjacent is $60/(ac + b)$, although the electrical angle between slots that are physically adjacent is

$$\alpha_s = \frac{60}{q} = \frac{60c}{ac + b}$$

i.e. $\alpha_e = \alpha_s/c$ as if the winding had cq slots/pole/phase. The $ac + b$ coils must be joined in series to form one repeatable section of the phase.

The winding factors that apply to the fundamental and harmonic m.m.f.s produced by a winding carrying balanced sinusoidal currents apply also to the e.m.f.s generated in it by the fundamental flux wave and any harmonic waves (including those produced by the damper winding, see Section 28.2.4). Walker and Kerruish[38] consider the e.m.f. and m.m.f. Liwschitz-Garik[36,39] considers the m.m.f. of fractional-slot windings. Their formulae for K_{dn} look different, but give the same values for a given winding. Equation (28.9), modified by putting cq in place of q, gives the same values too, except at the slot frequencies of a fractional-slot winding (see Section 28.2.4 and reference 43). In general. k_{dn}, and k_{pn} are much smaller if q is not an integer than if it is. References 34, 35, 36 and 40 describe other methods of analysis and other types of winding.

28.2.4 Slot ripple e.m.f.s[33,43]

The causes of harmonics in the open-circuit e.m.f. wave at frequencies associated with the number of stator slots are:

(1) The presence of harmonics at slot frequencies in the main flux wave produced by the rotor.
(2) Cyclic variations in the flux distribution in the airgap, as the poles pass the stator teeth and slots. There is very little change in the total flux per pole, because change is resisted by currents induced in the damper cage and field winding.
(3) Flux waves produced by currents induced in the rotor (damper cage, solid pole shoes, and field winding). These currents are caused by the ripple produced in the gap flux density by the stator slotting.

The stator e.m.f. ripples are greater if:

(1) The machine has a whole number of stator slots per pole. Ripple e.m.f.s can be greatly reduced by choosing an appropriate fractional-slot winding.

(2) The stator slot opening is large compared with the airgap length. A slot opening to airgap length ratio greater than 2 emphasises the ripple (and the eddy current losses in the damper or pole surface).
(3) The rotor has a low impedance damper cage rather than solid pole shoes, which have higher impedance.
(4) The effective pole shoe arc covers other than a whole number of stator slot pitches. However, flux fringing makes this a rather imprecise relationship.

Walker[43] investigated mathematically the occurrence of slot ripple e.m.f.s, and illustrated his findings with oscillograms in which ripples, or their absence, were related to constructional features of the machine. He concluded that the main cause of such ripples is the presence of slot frequency currents in the damper cage of a salient pole machine, unless it is designed specifically to avoid them. They arise because the radial permeance of the airgap is less opposite each slot than it is opposite adjacent teeth. This imposes on the main flux waveform a ripple of wavelength equal to one stator slot pitch. The peak-to-peak depth of the ripple (its double amplitude) is approximately proportional to the *local* mean density of the flux wave (neglecting iron saturation). Hence the ripple is greatest over the pole shoe arc and dwindles to zero at the quadrature axis. At each slot or tooth position the mean flux density, and the ripple, rise and fall as the poles sweep past. This may make the ripple appear to rotate, but because it is caused by the stator slots, it is fixed in position relative to the stator. Therefore it does not induce e.m.f. directly into the stator winding.

It does, however, induce e.m.f.s at slot frequency into any available rotor circuits, most importantly into the rather low impedance damper cage. The frequency is $2N_p f$ hertz, where $2N_p$ is the number of slots per pole pair and f is the synchronous frequency ($2N_p$ may or may not be a whole number). The number of damper bars is chosen primarily to provide effective damping, and the angular pitch of the bars is usually within 15% of the stator slot pitch. If the bars are symmetrically and similarly placed in all the poles, the ripple e.m.f.s in corresponding bars in successive poles circulate current from pole to pole. This produces a m.m.f. with a wavelength equal to two fundamental pole pitches, pulsating relative to the rotor at $2N_p f$ hertz and rotating with it. This is equivalent to two waves of half amplitude rotating relative to the rotor at $2N_p$ times synchronous speed, one forwards and one backwards. Relative to the stator the speeds are, therefore, $(2N_p \pm 1)$ times synchronous speed, and harmonic e.m.f.s of these two orders are induced in the stator. These e.m.f.s are not reduced, relative to the fundamental, by their pitch and spread factors, if N_p is an integer.

As Walker points out, with a whole number of stator slots per pole pair, the pole-to-pole damper cage currents can be avoided, or at least greatly reduced, by offsetting the bars 1/4 of a stator slot pitch to the left in say the north poles, and 1/4 slot pitch to the right in the south poles. This can dramatically improve the e.m.f. waveform, and is standard practice when N_p is an integer.

A practical point of mechanical design arises. The bars usually occupy 75–80% of the circumferential width of the pole shoe. The slots for the outermost bars must be placed so that they do not cause the pole shoe to be overstressed by centrifugal force. Alternatively, the damper bar slots in this position may be closed or omitted.

Walker also shows that if N_p is odd, or fractional, the backward-rotating field does not occur, and the e.m.f. ripple frequency is $(2N_p + 1/k)f$ hertz, where k is the denominator of $2N_p$ when it is expressed in its lowest terms, thus:

$$2N_p = \frac{N}{p} = \frac{A}{k}$$

where N is the total number of stator slots and p is the number of pole pairs. Second-order slot ripple at $(4N_p \pm 1)f$ (for integral N_p) or $(4N_p \pm 1/k)f$ may sometimes contribute significantly to the telephone harmonic factor, because the weighting factor is near its maximum at the corresponding frequencies.

Tables showing combinations of k and $2N_p$ that may, and those that will not, cause pole-to-pole currents and hence slot ripple e.m.f.s are given in reference 43. These are reproduced, for k up to 6, in *Table 28.2*. If b/c in $q = a + b/c$, is less than 1/4, ripple e.m.f.s are usually tolerably small.

In a turbogenerator, the rotor tooth-tops, slot wedges and the damper winding will carry stator-slot-frequency currents. However, the gap-flux ripple at the rotor surface is not great, because the stator slot opening is less than the gap length, except in smaller machines (say below 30 MW). Although q is usually an integer, it usually is more than 6, so the ripple e.m.f. is rarely objectionable.

Walker has also shown that the uniformly spaced rotor bars, in conjunction with integral slots per pole per phase, may cause additional e.m.f. ripples at frequencies of $[(N'/p) \pm 1]f$ Hz, where N' is a whole number given by the rotor circumference divided by the rotor slot pitch. Again this e.m.f. ripple is rarely troublesome.

In summary, design features used to reduce harmonics in the open-circuit e.m.f. are as follows.

(1) Use a fractional slot winding, choosing the number of slots per pole pair from Table 2 of reference 43 (or *Table 28.2*).
(2) Choose an optimum pole shoe arc, and increase the gap length towards the edges of the pole, to reduce the harmonics in the main flux wave. An arc of 65–70%

Table 28.2 Slots per pole pair that do and do not produce slot ripple e.m.f.s*

k	*Fundamental harmonic*	*Second harmonic*
Slots per pole-pair that produce slot ripple e.m.f.s		
1	$2K$	
2	$K \pm \frac{1}{2}$	K
3	$2K$, $2K \pm \frac{2}{3}$	$K \pm \frac{1}{4}$
4		K, $K \pm \frac{1}{3}$
5	$K \pm \frac{1}{4}$	$K \pm \frac{1}{8}$, $K \pm \frac{3}{8}$
6	$2K$, $2K \pm \frac{2}{5}$, $2K \pm \frac{4}{5}$	K, $K \pm \frac{1}{5}$, $K \pm \frac{2}{5}$
	$K \pm \frac{1}{6}$, $K \pm \frac{1}{2}$	$K \pm \frac{1}{12}$, $K \pm \frac{1}{4}$, $K \pm \frac{5}{12}$
Slots per pole-pair that do not produce slot ripple e.m.f.s		
1	$2K \pm 1$	$K \pm \frac{1}{2}$
2	K	$K \pm \frac{1}{2}$, K
3	$2K \pm 1$, $2K \pm \frac{1}{3}$	$K \pm \frac{1}{2}$, $K \pm \frac{1}{6}$
4	K, $K \pm \frac{1}{2}$	$K \pm \frac{1}{2}$, $K \pm \frac{1}{4}$, K
5	$2K \pm 1$, $2K \pm \frac{1}{5}$, $2K \pm \frac{3}{5}$	$K \pm \frac{1}{2}$, $K \pm \frac{1}{10}$, $K \pm \frac{3}{10}$
6	K, $K \pm \frac{1}{3}$	$K \pm \frac{1}{2}$, $K \pm \frac{1}{6}$, $K \pm \frac{1}{3}$, K

*K is any integer; k is the denominator of the number of slots per pole pair.

of the pole pitch is usual, with a gap length at the pole shoes edges 1.3–1.7 times the minimum gap at the pole centre. With fractional slotting, or integral q of 5 or more, a parallel gap will often suffice. This is convenient for solid pole shoes, as it allows the shoes to be skimmed to give the correct rotor diameter after they have been bolted on. With a pole shoe arc of 2/3 of the pole pitch, the third harmonic flux is theoretically zero with a parallel gap, but a graded gap may be chosen to reduce the fifth and seventh harmonics.

(3) Skew the stator slots, or the poles, by one stator slot pitch to reduce the fundamental slot ripple harmonic, or by half a slot pitch for the second order harmonic. Skewing is often used on small machines, but it becomes rather awkward and expensive on large ones.

(4) For a salient pole machine with a whole number of slots per pole pair, offset the damper bars to the left and right alternately on successive poles. An offset of 1/4 of the stator slot pitch each way will largely eliminate the fundamental slot harmonic and will somewhat reduce the second-order harmonic. If necessary, the latter can be reduced further by skewing the stator slots or the damper bars by half a stator slot pitch.

(5) With a whole number of stator slots per pole pair, it may sometimes be worthwhile to offset the poles in pairs to the left and right alternately, in order to avoid ripple caused by slot frequency currents in the field winding. Again, an offset of 1/4 stator slot pitch each way will reduce the fundamental slot ripple.

(6) Make the ratio of the gap length to the width of slot opening as large as other requirements will permit. However, the gap length is governed chiefly by the shortcircuit ratio (approximately $1/X_d$) that is specified, and to make it longer merely to reduce slot ripple would usually be unacceptably expensive.

The most effective and economically acceptable methods are to choose a suitable fractional slot winding and shape the pole shoes (for the lower harmonics): or, if an integral slot winding is selected, it is important to choose the most suitable damper bar pitch and to offset the bars as in (4) above.

Stromberg[40] explains how the fractional-slot winding reduces the harmonic e.m.f.s, gives reduction factors for various winding arrangements and indicates certain special constructions that can be used if a particularly good e.m.f. waveform is required.

28.3 Alternating current windings

In synchronous generators, the a.c. output winding is on the stator, except in some very small machines and some for particular purposes. For example, the a.c. winding and the diodes of a brushless exciter are necessarily on the rotor.

Low voltage machines with voltages up to 4160 V and with outputs of up to 2500 kVA, may have single- or two-layer windings with mush-type coils wound with round enamelled wire secured in semiclosed slots. Larger machines have two-layer windings using diamond-shaped short-pitched coils wound with insulated rectangular copper strip, secured in open slots. Each coil has one of its sides in the top layer and the other in the bottom. The coils and the connections between them are most often of the lap type but can be of the wave type. See *Figure 28.2* for a diagrammatic explanation of these terms. Where such coils would be physically too difficult to manufacture, either because of their size or weight, single half coils, known as bars, are used. Bars may also be of either the lap or wave type. Almost always, in a three-phase winding, each phase-group of coils or bars occupies an arc of exactly or nearly 60° electrical

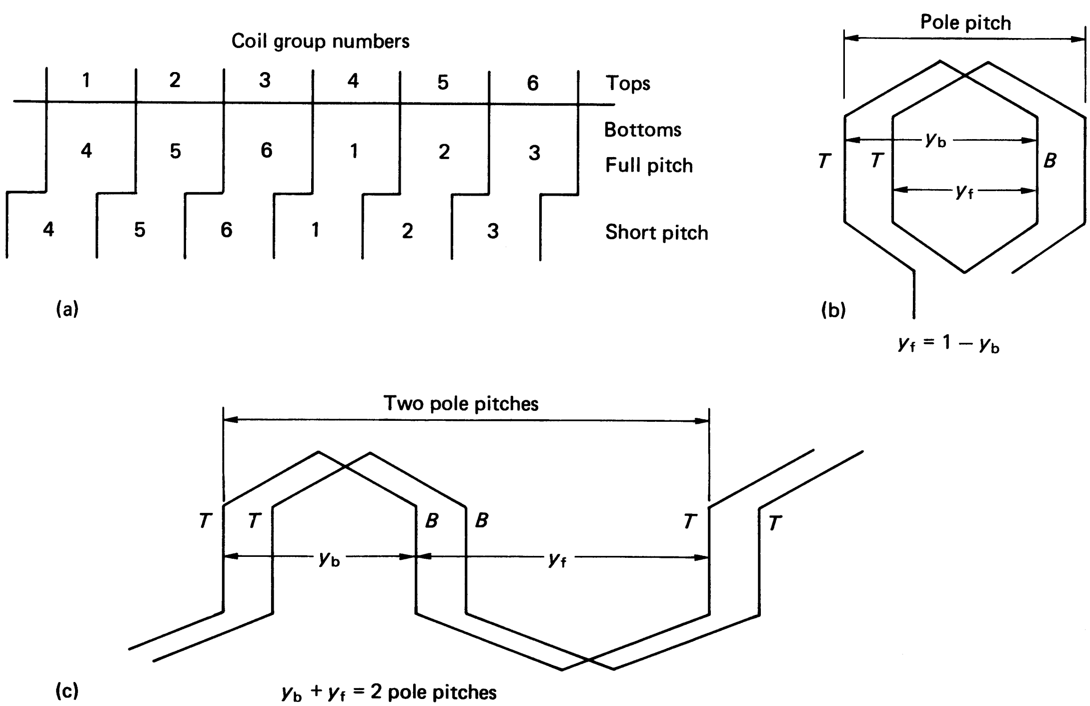

Figure 28.2 Stator coil arrangements: (a) coil groups, q an integer; (b) short-pitched lap connection; (c) short-pitched wave connection

under each pole. As explained previously, the number of slots per phase per pole for the stator winding may either be integral or non integral.

Most two-pole and four-pole turbogenerators use a bar type winding and a whole number of slots per pole per phase. Some large low-speed machines use wave-type windings; smaller machines quite often have skewed stator slots.

The two-layer short-pitched coil winding has the advantages that:

(1) the coils all have the same shape, which reduces the tooling needed for forming and insulating;
(2) a neat endwinding is obtained which is not difficult to support;
(3) the coil span can be chosen to reduce harmonic e.m.f.s produced by flux wave harmonics, and to reduce harmonic m.m.f.s produced by the load current;
(4) fractional slotting can be used, for the same purpose as in (3); and
(5) in multipolar machines, several identical parallel circuits per phase can be formed, giving greater freedom to optimise the design.

Single-layer windings are now rarely used for generators, and are not considered here (see references 11 and 22).

28.3.1 Choice of slot number

Liwschitz-Garik[22] gives a very thorough treatment of this subject, to which acknowledgement is given. Considering only three-phase windings, let:

N = total number of slots = total number of coils
$2p$ = number of poles

$$N_p = \frac{N}{2p}, \text{ slots per pole}$$

$$N_{ph} = \frac{N}{3}, \text{ slots per phase}$$

$$q = \frac{N_p}{3} = \frac{N}{6p}, \text{ slots/pole/phase}$$

g = number of parallel circuits per phase
y = full coil pitch
y_f = front coil pitch
y_b = back coil pitch

See *Figure 28.2* for a diagrammatic explanation of these terms Then:

(1) $N/3g$ must be an integer, to provide equal numbers of coils in each parallel circuit of each phase; and
(2) the coils must be arranged symmetrically around the airgap, to the same pattern in all phases, to give equal phase e.m.f.s, spaced 120° electrical apart.

28.3.2 Integral-slot windings

The quantity q can have any practicable value, usually between 2 in multipole machines (though these are more likely to have fractional-slot windings) and 10 in large two-pole turbogenerators. The $6q$ coils in $6q$ slots per pole pair are almost always arranged in six groups, each of q coils, each group occupying exactly 60° electrical in each layer. *Figure 28.2(a)* shows the arrangement of full-pitch and short-pitch coils. The pitch is usually about 5/6 of the pole pitch which is chosen to minimise both the 5th and 7th harmonics.

If the left-hand top-layer conductor of each group is called the 'start' of the group, the e.m.f.s acting from start to finish of groups 1 and 4 are 180° out of phase; similarly, for groups 2 and 5, and 3 and 6. Phase A is formed by connecting F1 to F4 to put groups 1 and 4 in series. S1 is the start of phase A, and S4 the finish. Connecting S1 to F4 and F1 to S4 puts the two groups in parallel. Similarly, for phase B (groups 3 and 6) and phase C (groups 5 and 2).

If Phase A starts at slot 1, phase B can start at any slot numbered $1 + 2q + 6nq$, and phase C at $1 + 4q + 6nq$ ($2q = 120°$ and $6nq = 360°$, n being any integer from 0 to p).

In a short-pitched wave winding, $y_b < y_f$, but $y_b + y_f$ is equivalent to two pole pitches. The step of $2p \times (y_f)$ must be one slot more or less than y_f so that a connection can be made to the coil side next to the one at which that tour round the winding started.

28.3.3 Fractional-slot windings

These windings, in which q is not an integer, have the advantage that they can generate as good a waveform with few slots per pole per phase as an integral-q winding with many more slots. Fractional slot windings are frequently used in multipolar machines where there is not room for q to exceed 3 or 4. For example with $q = 3\frac{6}{7}$ the effect of harmonics in the gap flux is reduced as effectively as with an integral value of q of 27, an impracticable number. In this example, it would be necessary to have 14, or a multiple of 14 poles to allow the full pattern of groups of coils to be achieved that are required to give an arithmetic average figure of $3\frac{6}{7}$ slots per pole per phase.

Fractional q also gives the designer a wider choice of slot numbers (for most numbers of poles), enabling flux densities to be adjusted more easily on a given frame size.

28.3.3.1 Arrangement of the coil groups

(1) Writing $q = a + (b/c)$ where a, b and c are integers and b and c have no common factor, then c is the least number of pole pitches that will contain a whole number of slots per phase, viz. $(ac + b)$ slots. In the c pole pitches, there are $3(ac + b)$ coils that form a complete three-phase unit of the winding. $2p/c$ ($= F$, say) of these units make up the whole winding, which has $N = (2p)/c\ 3(ac + b)$ slots and coils (of course $N = 6pq$).

F is the highest common factor of N and $2p$, because

$$\frac{N}{F} = \frac{N}{2p/c} = 3(ac + b)$$

and is the number of slots in the unit that occupies c pole pitches, and $c = (2p)/F$. Of course N is a multiple of 3 and of F.

(2) A balanced three-phase winding can only be obtained if c is neither 3, nor a multiple of 3. Then, in the c pole pitches in each layer of the winding, each phase occupies b groups of $(a + 1)$ slots, and $(c - b)$ groups of a slots.
(3) Starting from slot 1 of each unit of c pole pitches, the larger and smaller slot groups occur in different sequences in the three phases, but the phase e.m.f.s are balanced. The pattern of larger and smaller groups is the same in all F units.
(4) Taking all three phases together, the pattern of larger and smaller slot-groups is complete in $c/3$ pole pitches. It occurs $3F$ times in the whole winding, but does not itself form a balanced three-phase unit.

(5) The sequence of a-slot and $(a+1)$-slot groups can be found as follows which is the same for lap as for wave windings.

60° electrical contains q slot pitches. Starting from 0° at slot number 1, the centre lines of the first and last slots of successive slot groups (often called phase belts) must lie within the electrical angles 0–60°, 60–120°, ..., 420–480°, etc., from slot 1. Therefore, the centre line of the last slot of the nth group lies just within nq slot pitches of slot 1, where n is an integer. In general, nq is an integer plus a fraction, say $nq = n(ac+b)/c = X + (Y/c)$. Then, the slot number $(1+X)$ is the last slot of the nth group. But when $n = c$, $2c$, etc., $X = (ac+b)$, $2(ac+b)$, etc., and $Y = 0$. Then slot number $(1+X)$ lies exactly 60n° from slot 1, and so is the first slot of the $(n+1)$th group. *Table 28.3* illustrates the method for a 12-pole machine with 135 slots, $q = 3\frac{3}{4}$, $c = 4$, and $F = 3$. The slot sequence 4, 4, 4, 3 can be determined by considering only c steps of q slots but the complete table allocates all slot numbers to phases.

The fraction b/c determines the numbers of larger and smaller coil groups and their sequence in the winding. References 8 and 22 contain tables of coil group sequences for a wide range of values of b/c.

(6) Within the c pole pitches of the repeatable unit all the conductors of a phase have different positions relative to the poles. Thus their e.m.f.s are out of phase, and all the cq coils must be connected in series. But the e.m.f.s of the F units of a phase are in phase, and can be put in parallel if necessary (see Section 28.3.4).

(7) $2q$ slot pitches span 120°, but the smallest integral number of pitches covering 120° is $2cq$. Slots spaced at multiples of $2cq$ pitches are the obvious starting slots for phases A, B and C. In the example, slot 31 is 120°, and slot 16 is 240° from slot 1. But as with q an integer, the starts can be at other phase groups that are 2 or $(2+6n)$ phase groups apart; e.g. phase B could start at slot 9, although that is 128° away from slot 1. The sum of the coil e.m.f.s is independent of the order in which they are connected (so long as the polarities are kept correct). Starts and finishes can therefore be arranged in the most convenient positions. This can be used to advantage to ensure that the leads from each phase group are distributed around the machine, thus avoiding two adjacent leads having full line-to-line potential between them.

(8) The bottom coil sides have the same pattern as the tops but their groups are displaced to the left or right by the amount of the coil pitch.

(9) Sometimes multipolar windings are used that do not obey all these rules; for example, an existing core design may be usable with a few empty slots or unconnected coils.[8,11,22]

28.3.4 Parallel circuits

28.3.4.1 Integral slot windings

Each phase has two groups of coils per pole pair, i.e. there are $2p$ groups in the machine. Whether the coils are lap or wave connected, the groups can all be put in series, all in parallel, or in series–parallel connection. For the latter, the number of parallel circuits in each phase must be a factor of $2p$, say t, so that there can be $2p/t$ groups in series in each parallel path. For example, for $2p = 10$, only two or five parallel circuits are possible.

28.3.4.2 Fractional slot windings

In each repeatable section, occupying c pole pitches, the $ac+b$ coils of each phase must be put in series. Hence the maximum number of parallels per phase is the number of sections $F = (2p)/c$. If less are needed, their number must be a factor of $(2p)/c$, so that there will be $2p/ct$ repeatable sections in series in each parallel path. For example, for $2p = 40$ and $q = 2\frac{1}{5}$, there are $40/5 = 8$ repeatable sections and 2, 4 or 8 parallels are possible. With $2p = 40$, but $q = 2\frac{1}{4}$, $(2p)/c = 10$, only two or five parallels are possible.

28.3.4.3 Concentrated or distributed parallel circuits

Whether q is integral or fractional, in each parallel circuit the coils that are in series may be arranged under adjacent poles, or may be distributed around the machine under alternate poles. *Figure 28.3* shows two possible arrangements for two parallel circuits in one phase of an eight-pole winding. The concentrated arrangement shown in *Figure 28.3*(*a*) has the advantage that it reduces the unbalanced magnetic pull caused if the rotor is offset radially from the true centre of the stator bore. If, for example, in this 8 pole machine, the airgap becomes narrower opposite pole 2, the gap flux density tends to increase there, and to decrease opposite pole 6. The change in induced e.m.f.s circulates current round the parallel circuits, which tends to reduce the difference in flux densities and so reduce the unbalanced magnetic pull (u.m.p.).

28.4 Coils and insulation

28.4.1 Service conditions

The effects that may eventually cause a breakdown of the insulation of a winding are described below.

Table 28.3 Slot, or coil, grouping for a fractional slot winding*

Pole No.	*1*			*2*			*3*			*4*			*5, etc.*
Slot group No., n	*1*	*2*	*3*	*4*	*5*	*6*	*7*	*8*	*9*	*10*	*11*	*12*	*13*
nq	$3\frac{3}{4}$	$7\frac{1}{2}$	$11\frac{1}{4}$	15	$18\frac{3}{4}$	$22\frac{1}{2}$	$26\frac{1}{4}$	30	$33\frac{3}{4}$	$37\frac{1}{2}$	$41\frac{1}{4}$	45	$48\frac{1}{4}$
Slot Nos	1–4	5–8	9–12	13–15	16–19	20–23	24–27	28–30	31–34	35–38	39–42	43–45	46–49
Phase	A	C′	B	A′	C	B′	A	C′	B	A′	C	B′	A
No. of slots	4	4	4	3	4	4	4	3	4	4	4	3	4

*12 poles. 135 slots, $q = 3\frac{3}{4}$ slots/phase/pole, $c = 4$ poles per section, $F = 3$ sections.
Top coilsides 1–4, 13–15, 24–27, 35–38, with their associated bottoms, form one parallel circuit of phase A.

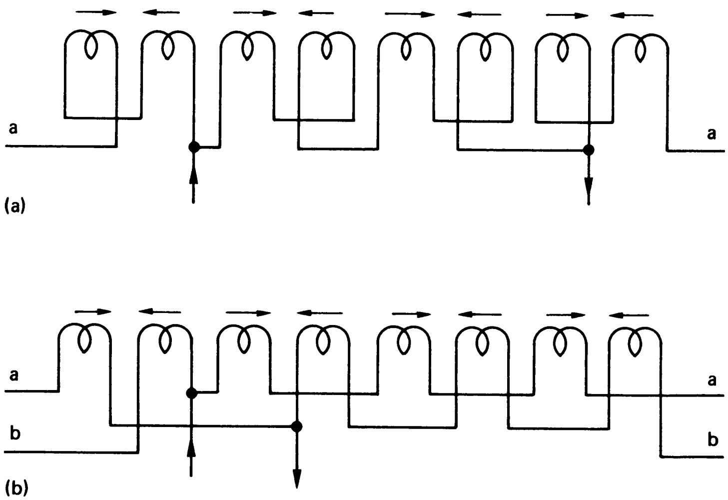

Figure 28.3 Parallel connection of coils: (a) concentrated; (b) distributed

Thermal ageing This affects all windings, and is the main reason for setting limits for winding temperature (see Section 28.5). In smaller machines running at full class H temperatures it is the most usual cause of deterioration.

Electrical stress The trend in stress levels is continually upwards as manufacturers seek to minimise costs and reduce the effect of the thermal barrier of the insulation which is a factor in determining a machine's output. This requires considerable care in the design of the insulation system, in the manufacture of the coils and their insertion in the stator slots. Values of 3 kV/mm are now common with even higher figures being achieved by some manufacturers. The voltages encountered in field windings increase with machine size up to ceiling voltages (see Section 28.14) of around 1500 V on the largest generators. Thyristor excitation systems also superimpose spike voltages sometimes requiring extra insulation.

Electromagnetic forces Stator and field coils are both subjected to forces due to the magnetic fields operating in the machine. Any looseness of an a.c. winding in its slot will quickly lead to mechanical abrasion of the insulation on the coils. On higher voltage machines, equipped with a corona shield, this will lead to slot discharge with eventual failure of the main wall of the insulation causing an earth fault in the slot portion. Field coils able to move on their poles or in their slots will also lead to failure through abrasion.

Mechanical forces Rotor windings carry centrifugal forces that become very onerous in high speed and large machines. Interturn insulation and coil-to-earth insulation placed under the pole shoe and adjacent to supporting 'V' blocks used between poles must withstand the stress caused by centrifugal force and temperature. Non-supported parts of the winding, between 'V' blocks and in the endwinding overhangs are particulaly vulnerable especially in the event of an overspeed. In long rotors, especially two-pole turbine-type rotors, expansion of the winding relative to the rotor body may damage the insulation. Stator windings of long machines may also suffer from differential expansion between the copper and the core. In large generators double frequency vibration caused by electromagnetic forces may abrade insulation or even cause conductor strands to break due to fatigue.

Vibration External vibration, imparted by the prime mover can lead to premature failure of the stator or rotor. In the case of the stator this results from lateral vibrations setting up resonance or fatigue in mechanical components or in the endwinding bracing system. Rotor failures are associated with the effect of running at or near the critical speed of the shaft system leading to high levels of vibration or high tortional perturbations leading to fatigue failures of components.

External electrical fault conditions Mal-synchronising, line-to-line faults or short duration interruptions of the supply can all impart high mechanical and electrical forces on both the stator and rotor windings. Under these conditions, mechanical failure of the stator endwinding bracing, looseness of the winding in the slot or separation of the phase groups can all occur. Rotor field coils suffer from mechanical forces under these faults which can lead to coil connection failures, failure of the insulation or damge to components in the rectifier assembly if a brushlesss machine. Lightning strikes and switching transients arising from the operation of circuit breakers can also lead to failure of the inter-turn insulation in the end groups of the stator windings if the machine is not suitably protected by lightning arresters and/or surge diverters. Similarly, opening a field circuit breaker without suitable discharge resistors fitted can give rise to very high voltages of sufficent magnitude to break down the field coil insulation.

Environmental Contamination of the stator coil insulation by dirt, moisture or oil encourages surface tracking which may eventually become severe enough to penetrate the insulation.

28.4.2 Stator coils[5,11,22,53]

The various types of stator coils most used for generators are described below. The type used depends primarily on the size, output and voltage of the generator, but the choice is, of course, influenced by the maker's facilities and practice.

28.4.2.1 Mush coils

These are used for smaller machines, up to 2500 kVA, at voltages up to 4160 V, in single layer or two-layer windings. Coils are wound with round wire insulated with enamel or enamel and glass tape. The number of turns per coil depends on the size, voltage and frequency of the machine, and the chosen number of slots. The turns are laid at random in slots that may be open, but are often semi-closed, especially if the airgap is short. The main insulation between the coil and the core is a slot liner of a tough but sufficiently flexible sheet, such as polyamide or aramid paper (Nomex). Two-layer windings may have a separate liner for the top and/or bottom coil side (a top or bottom 'box') or a separator between the coils.

28.4.2.2 Diamond coils

These are arranged in two layers in open slots and used for a wide range of outputs and voltages. Coils are wound with rectangular copper strip that is small enough to have acceptably low eddy current losses in service. Sizes are usually within the range 5 mm × 2 mm to 10 mm × 4 mm. The strip is insulated with enamel or enamel plus a thin covering (say 0.2 mm) of lapped or braided glass or polyester-glass plus resin. The number of turns needed are wound into a flat loop on a looping machine and the straight sides are then pulled sideways to form a coil with the required axial length and transverse span. Depending on the equipment used, strip up to about 10 mm wide by 4 mm deep can be pulled. The width-to-thickness ratio should not much exceed 3, to avoid buckling at the bends. If necessary, several strips are wound in parallel to provide the conductor area needed to carry the current, or to make the coil flexible enough to be pulled without damaging the insulation.

Normally a coil has either one or two strips across the width of the slot. Additional inter-turn insulation, enclosing the number of strips that form a complete conductor may be applied after the coil is pulled. However, it is often practicable to apply this insulation as the copper strips are wound on the looping machine, provided that the insulation contains enough resin (see later).

Coils too large to be pulled are shaped on formers. Those for turbogenerators and large hydrogenerators are made as half coils (also known as 'bars') because full coils would be too difficult to handle. In addition, coils made for large 2-pole machines are exceedingly stiff and are difficult to insert through the bore of the machine. *Figure 28.4* shows typical coil cross-sections, all to the same scale. *Figure 28.4*(*a*) shows a typical pulled diamond coil for a diesel-engine-driven 14-pole generator of 9.5 MVA where the conductor strands are not transposed. The coils in (*b*) and (*c*) are formed as bars and incorporate a Roebel transposition which is used to reduce the eddy currents circulating within the full coil (see para 28.4.2.3. below). Coil (*b*) is for a hydrogenerator of 110 MVA at 600 rev/min and (*c*) is for a 590 MVA two-pole turbogenerator. In (*c*) all the strands are squarish tubes to allow for direct water cooling of the conductors: a mixture of tubes and solid strips is often used, especially for the bottom coil side, where eddy current losses are lower than in the top coil side. An appropriate choice of the number and dimensions of tubes and strips can lead to a reduction in the sum of d.c. and a.c. copper losses.

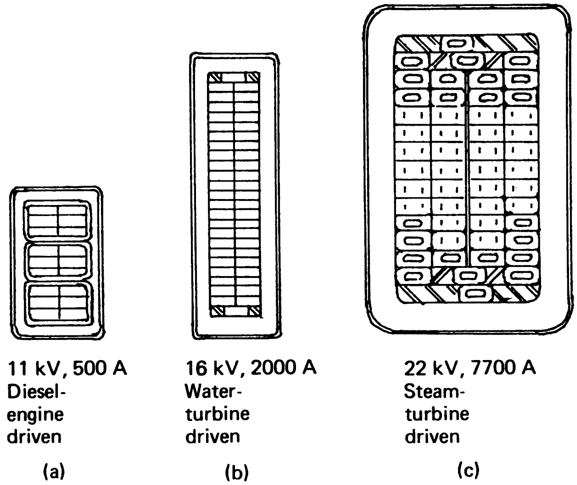

Figure 28.4 Stator coil sections: (a) three turns of six strips; (b) single-bar, two-stack Roebel; (c) single-bar four-stack Roebel

28.4.2.3 Transposition[51,52]

If each turn consists of more than six or eight strips it is desirable, and in larger conductors necessary, to reduce the eddy current losses set up by the leakage flux set up across the slots by the load current. In principle this requires the e.m.f.s induced by the leakage flux to be equal in all strips in the length between points where they are all joined together. In a multistrip coil side for a turbogenerator or hydrogenerator this is achieved within each slot length by using a 360° Roebel transposition. In this construction, each strip is given two edgewise bends separated by half the slot length and differently positioned in the various strips so that when assembled the strips mesh together. When the strips are assembled to form the coil side, each strip occupies each position in the height in turn and they all do so for equal distances. Hence the voltages induced by the cross-slot flux is equal in each and only a small current circulates between strips due to endwinding leakage fields.

In large generators (say 500 MVA or more) the endwinding leakage fields can cause significant circulating currents. The effect can be reduced by using a 540° Roebel transposition. This reverses the positions of the strips in one endwinding relative to the other and approximately cancels the e.m.f.s. arising from the endwinding leakage fields.

In all Roebel coils, the radius of the edgewise bends must be sufficiently generous to avoid damage to the strip insulation. Usually a slip of, for example, Nomex is placed between strips at the cross-overs. The undulating top and bottom surfaces are made level by applying a suitable filler, such as a filled resin dough, which is cured when the conductor stacks are consolidated, before the main insulation wall is applied.

Large turbogenerator slots are often wide enough to require four, not two, stacks of tubes, or strips and tubes. The eddy current losses are rather less if stack 2 is transposed with stack 3, and stack 1 with stack 4, rather than 1 with 2 and 3 with 4. For smaller bars with relatively few strips, and for full coils, the Roebel transposition in the slot is not justified: simpler schemes are possible which are sufficently effective in reducing circulating losses. For example, in a bar winding individual strips can be joined at the coil-to-coil connections in insulated, segregated groups, not all together as is done with a Roebel bar winding. The strips are joined so that within a phase group of coils the groups of strips occupy a succession of positions in the slots that gives a sufficiently good balance of leakage flux e.m.f.s.

With full coils the natural roll-over of the conductors at the noses inverts the strips, i.e. a strip nearest the wedge in the top coil side is nearest the slot bottom in the bottom coil side. Summers[52] gives formulae for calculating the reduced circulating current loss that can be obtained by additionally inverting one or more turns in the overhang portion. He concludes that the most generally useful arrangement is to invert the strips of one turn only at the connection end before forming the last turn; or to invert them at the back end after making only the first half-turn. These sorts of transposition are only effective on 2 or 3 turn coils—above this number it is not necessary to incorporate a transposition in the coil overhangs to reduce the circulating losses to an acceptable value.

28.4.3 High voltage insulation systems[53,58–61]

Two systems[60] are in general use: resin rich (r.r.) and vacuum pressure impregnation (v.p.i.). Both use mica paper (mica flake, which was universally used until the early 1960s, is very rarely used today). The mica paper is bonded with a thermosetting resin to a thin backing material,[54] frequently woven glass or polyester fabric or polyester film. Polyester, epoxy and epoxy-novolac resins, or mixtures of them, are most widely used for r.r. and v.p.i. systems, with appropriate hardeners and catalysts to control the curing process. Resin rich tapes contain about 30% resin, dried to the B stage, at which it can be stored in cold conditions (5–10°C) long enough to be convenient in manufacture. It can be applied at room temperature by hand or by a taping machine; the machine gives better control of lapping and tension. Resin rich tape can be applied as additional turn insulation[56] to a group of strips that is then wound and pulled to form a coil of several turns. Tape for the v.p.i. process contains 4–10% of a different resin system to make it handable and is more fragile than the resin rich tape.

Normally, the strips in the straight sides of the coil, or in the bar, are consolidated in a heated press before any turn insulation or main wall insulation is applied. If the turn insulation (r.r.) has been applied as the pulled-coil loop was wound, this and the strips are cured and bonded at the same time. If turn taping is to applied after this consolidation stage, a release film is put between turns to allow the additional taping to be applied. After any turn taping and the main wall insulation has been applied, the slot portion of the coil is again consolidated under heat and pressure to cure the resin contained in the mica tapes. For turbogenerators and large-hydrogenerators the endwinding portions are consolidated too, both before and after the insulation is applied to improve their thermal conductivity.

In the v.p.i.[59] process, the low-resin content mica paper is applied after the conductor strips have been consolidated. If individual coils or bars are being manufactured, these are placed in an autoclave. Air is drawn out of the tape under vacuum in the autoclave, which is then flooded with low-viscosity resin under pressure. After impregnation, excess resin is drained off, and the coil is pressed to size in a hot press.

Alternatively, for machines up to 5.3 m in diameter and 5 m in length (dependent on the manufacturer's facilities) the global v.p.i. impregnation process can be used.[57] Here, the coils are wound at the so-called 'white stage' into the stator core and the whole wound stator is then placed in a suitable pressure vessel. Absorbent packings and lashings are used in the endwinding bracing structure (e.g. glass or polyester tapes and polyester fleece). Again, a vacuum is drawn to remove any air trapped in the insulation materials followed by a pressure cycle where resin is introduced. Following impregnation, the wound stator is immediately transferred to an oven where it is baked to cure the resin. If treated with an epoxy resin, the stator would be rotated during the baking cycle to ensure both an even distribution and retention of the resin. Stators impregnated with a polyester resin do not require rotation during the curing cycle as the resin gels before becoming sufficently fluid to flow out of the stator. The resin fills the gap between the coils and the core, improving thermal conductivity and permitting some increase in current for a given temperature rise in service. Complete impregnation of the endwinding structure increases its strength and its resistance to moisture, dirt and contaminants.

The r.r. and v.p.i. processes can both produce good quality stator insulation systems. Essentials to achieve a high level of quality are:

(1) uniform lapping and tension of all tapes;
(2) correct choice of resin, tapes etc., to suit the process;
(3) r.r. tapes must be allowed to warm up to room temperature before being applied;
(4) v.p.i. resin must have a low enough viscosity at the impregnating temperature, and an economic life over many cycles of storage and impregnation;
(5) careful control of each cycle of the processes to ensure sound consistent results. In the consolidation stages, the soaking temperature (about 150°C), the heating and cooling rates, and the application of pressure all need careful control. During the v.p.i. process, times, temperatures, the degree of vacuum and the amount of pressure are all critical to achieving a satisfactory result.

28.4.3.1 Electric stress control

At line voltages up to about 5 kV the insulation material and thickness depend very much on mechanical and manufacturing factors. The nominal stress (phase voltage divided by insulation thickness) may be up to about 1.5 kV/mm. At higher voltages electrical stress determines the design, and nominal stress on resin-mica systems is usually 2.5 to 3.0 kV/mm.

At or above 6 kV, the outer surface of the slot part of the coil (whether r.r. or v.p.i.) must be adequately earthed to the stator core to avoid corona discharge in the gap between coil and core. This gap occurs because of the need to have a clearance to allow the coils (or bars) to be wound. The surface may be painted with a conducting paint, but preferably should be taped with a low resistance tape, e.g. a graphite loaded glass or polyester, before the final consolidating press. At the ends of the core the longitudinal stress gradient along the coil surface must be kept low enough to avoid breakdown of the air or surface tracking and eventual failure of the insulation. The surface may be painted with a higher resistance paint than is used on the slot part, but preferably is taped with, for example, a tape loaded with silicon carbide. The length of this stress grading treatment depends on the machine voltage, the insulation thickness and the voltage–current characteristic of the material used. (The resistance decreases with increasing stress.) The treatment must be effective for the short-time high-voltage tests on the coils (greater than twice line voltage to earth) as well as for the long-term operating voltage.

28.4.4 Insulation testing

28.4.4.1 Acceptance tests

Acceptance tests[66] are made on the finished generator to prove that it meets contractual requirements. Tests include:

(1) d.c. insulation resistance (IR), recorded after applying the test voltage continuously for 1 and 10 min;
(2) polarisation index (PI)

$$\mathrm{PI} = \frac{\text{IR after 10 min}}{\text{IR after 1 min}}; \text{ and}$$

(3) high-voltage test with, usually, power frequency voltage, applied after satisfactory IR and PI results have been obtained. The test voltage is maintained for 1 min between each phase and earth in turn, the other phases being earthed.

Procedures and test voltages are specified in BS EN 60034-1:1998 which is equivalent to IEC 60034-1:1994 and replaces BS 4999: Part 101:1987, in ANSI C50.10 1990, NEMA MG1: Part 32-1998, and other national specifications. (see Section 28.21.) The latest issue of the appropriate specification should be consulted for complete information; broadly, the test voltages are, except for low-voltage machines rated less than about 1 kW:

A.c. windings: 2 × rated line voltage + 1000 V.

Field windings:
Rated voltage $V_f \leq 500$; 10 V_f, minimum 1500 V.
Rated voltage $V_f > 500$; 2 V_f + 4000 V.

Document BS EN 50209:1999 specifies tests on conductor bars and coils for machines with rated voltage U_N from 5 to 24 kV. Tests include: (1) measurement of loss tangent (tan δ) and loss tangent tip-up (Δtan δ) on all or some coils of a machine set; and (2) voltage tests on the strand insulation, turn insulation and main insulation. The specification applies to generators of 5 MVA and above, and to 1–5 MVA ratings by agreement. It may or may not be specified as a contractual requirement. The tan δ limits are, for rated voltages U_N of 5–11 kV inclusive:

(1) at 0.2 U_N, tan δ not greater than 30×10^{-3} for any sample coil or bar;
(2) (tan δ at 0.6 U_N–tan δ at 0.2 U_N), $\frac{1}{2} \ngtr 2.5 \times 10^{-3}$; and
(3) Δ tan δ over any step of 0.2 U_N, $\ngtr 5 \times 10^{-3}$.

Limits (2) and (3) apply to 95% of the test samples; 5% are acceptable at 3×10^{-3} for item (2) and 6×10^{-3} for item (3).

The measurements must be made at room temperature before the samples are heated to at least 90°C, and again after they have cooled to room temperature. Guard electrodes at the ends of the slot length of the bar exclude the loss in the stress grading from the measurement. R.r. and v.p.i. systems can comfortably meet these limits. Typical values are:

Voltage	$0.2U_N$	U_N
tan δ	10–15	15–20 × 10^{-3}

Most of the increase occurs between 0.8 U_N and U_N. Of course tan δ of a globally impregnated winding can only be measured on complete phases, and the limits stated above cannot apply; comparison with individual coils is necessary.

28.4.4.2 Quality assurance tests

Quality assurance tests are made at the manufacturer's discretion at suitable stages of manufacture. They include:

(1) Dimensional, mechanical and dielectric tests on incoming materials.
(2) Voltage tests such as:
   (a) between insulated strands in a conductor, at 110–250 V r.m.s.;
   (b) between turns of multi-turn coils;
   (c) on individual coils or bars;
   (d) on groups of coils after they have been wedged in the slots, but not connected; and
   (e) on each phase of the completed winding before the acceptance tests.

   Supply frequency voltages in (c) and (d) and (e) must be rather higher than the final test voltage; (b) must be an impulse test unless the turns are cut through at the coil nose: it is obviously undesirable to cut a full-wound coil, so a Biddle or other surge tester is used.

   BS EN 60034–15:1996 specifies rated phase-to-earth impulse withstand voltages for machines rated 3–15 kV inclusive, with form-wound coils. For the standard lightning impulse, a 1.2/50 μs wave, the rated impulse voltage has a peak value $U_p = 4U_N + 5$ kV, where U_N is the rated voltage. The standard recommends that impulse test voltages should not be applied to a complete machine. It describes test procedures on sample coils, and specifies test levels of $(4U_N + 5)$ kilovolts between the conductor and a dummy slot, and half that value between turns, i.e. applied across the ends of the coil.

   International discussions are being held to agree upon a withstand level for impulse voltages with steeper wavefronts probably down to rise time of 0.2 μs. In service many generators are protected to some extent from impulse voltages by the impedances of transformers or cables. These reduce the peak voltage and steepness of the wavefront, so the generator does not suffer the full impulse generated by some forms of switchgear. Alternatively, lightning arresters and/or surge diverters may be fitted at the machine terminals to protect the stator windings from fast fronted surges.
(3) IR and PI measurements before (2)(d) and (2)(e).
(4) (a) tan δ test on all or some coils or bars.
   (b) tan δ test on each phase of the complete winding and between phases before impregnation.
(5) Measurement of integrated discharge magnitude using a dielectric loss analyser,[69] on individual coils, phases and the complete winding.
(6) Measurement of the partial discharge value on individual phases or the complete winding.
(7) Measurement of the resistance per square of the corona shield on the slot part of the coil: 2–30 kΩ/square is acceptable.

28.4.4.3 Diagnostic tests in service[67–70,79–82]

The electrical, mechanical and thermal stresses of normal service cause gradual degradation of the insulation. If this general deterioration can be adequately monitored, preventive maintenance of the winding can be co-ordinated with other planned maintenance in an attempt to avoid the cost of failures and unplanned outages. It is true that some generators work in less harsh environments than some motors do, or have closed air circuit cooling arrangements so suffer less contamination, but the cost of an unplanned outage or the damage caused through inadequate maintenance can be appreciably higher. Consequently, more and more attention is now being placed on the need for continuous on-line monitoring and preventative maintenace based on regular inspections for generating plant.

No single test can indicate the extent of deterioration at a particular time. It is necessary to review the results of several non-destructive tests made at reasonably regular intervals, preferably starting with a 'foot-print' from when the machine was first manufactured.

Simons[69] recommends measurements of:

(1) IR and PI with direct voltage at say 1, 2.5, 5.0 kV, appropriate to the machine's rated voltage U_N (r.m.s.):
(2) IR, capacitance C to earth, and integrated discharge energy at rated frequency (50 or 60 Hz) with voltages up to U_N to earth; and
(3) C and tan δ at steps of 0.2 U_N up to about U_N to earth. Tests are made, as usual, on each phase to earth with the other phases earthed, and on the whole winding to earth.

A local defect, unless it is a severe one, will not greatly affect the results. It may, therefore, be desirable to apply a proof h.v. test at say 1.2–1.5 U_N (r.m.s.) to earth at supply frequency, or a 0.1 Hz voltage with a peak of 1.7 U_N (r.m.s.).

28.4.5 Rotor coils

See Sections 28.15.3 and 28.17.2.3.

28.5 Temperature rise

Limits of temperature or of temperature rise are specified in national and international standards (see Section 28.21: BS EN 60034-1:1998, BS EN 60034-3:1996, ANSI C50: Parts 10–15 and NEMA MG1: Part 32).

The limiting values apply at rated load under specified ambient conditions, of which the most important figure is the temperature of the primary coolant (air, hydrogen or water) entering the machine or the winding. Temperatures of stator windings are measured either by resistance for machines rated below 5 MVA, or by embedded temperature detectors (e.t.d.), either of the thermocouple or resistance element type, above this rating or when supplied to NEMA MG1. Temperatures of rotating windings, usually field windings, are measured by resistance. In small machines without e.t.d.s fitted, winding resistance or surface temperatures would be measured on test, and monitoring in service may be of air temperature only. Temperature rises are calculated above the temperature of the primary coolant. Alternatively, but less often, it may be calculated above the temperature of the cooling water entering a heat exchanger.

Machines are designed to comply with the temperature rise limits specified as long as the primary coolant temperature does not exceed 40°C. If the ambient air temperature is high, or there is a water-cooled heat exchanger, the primary coolant temperature may exceed 40°C. Then the design rises are correspondingly reduced, so that the limiting total temperatures are not exceeded. This is especially likely to occur with turbogenerators using turbine condensate, which may enter the heat exchanger at a temperature of up to about 35°C. Conversely, if the temperature of the primary coolant entering the machine is less than 40°C, then the standards allow the temperature rises to be adjusted upwards to maintain the same total temperature. Below a primary coolant temperature of 30°C, any further adjustment is by agreement between the customer and the supplier.

The aim of all standards is to keep the temperature of the winding insulation down to a value at which the insulation, and therefore the generator, will have an acceptably long life. Standards do not specify or imply a lifetime. Accepted norms for life are 20 years for large capital equipment installed in prime power applications. A life expectancy of 10 years is more appropriate for industrial and smaller units. Some machines last much longer than these figures partly because they do not operate for long times near their temperature limits. Large machines or those subject to frequent load changes, or whose reliability is especially important, are commonly specified to meet class B temperature limits, although they have class F insulation in order to achieve a protracted life.

In all windings, the design must allow for the difference between the observable temperature and the hotspot temperature that ages the insulation most rapidly. In an indirectly cooled high voltage stator coil this difference may be 10–20 K, and the innermost insulation may run close to the classification temperature for the insulation system (130°C for class B and 155°C for class F). In an indirectly cooled turbogenerator rotor too, the temperature difference across the main slot insulation is a major component, but the temperature rise of the gas in the 'air' gap, and the surface-to-gas temperature difference, are important. In a salient-pole rotor, the end parts of bare strip-on-edge field coils are well cooled, but down the length of the coils turns can overheat, especially on four- to eight-pole rotors that have 'V' block coil supports between the poles.

Direct cooling avoids the temperature drops through the insulation, and permits higher current densities with acceptable temperature rises, e.g. up to about 10 A/mm^2 in a turbogenerator rotor hydrogen cooled at 5 bar absolute pressure, compared with about 3 A/mm^2 with indirect air cooling. Axially cooled rotors, with ends-to-middle gas flow, must have a modest temperature rise by resistance to avoid excessive temperatures at the midlength. Radial flow, or a combination of radial and axial, gives a mean temperature closer to the permissible hotspot temperature.

In a directly cooled stator coil the best indication of copper temperature is given by the temperature of the coolant, hydrogen or water, where it leaves the coil. The copper temperature is up to about 10 K higher than the hydrogen temperature, and only approximately 1 K above the water. The traditional e.t.d. between coil sides is still used as the routine temperature indicator in service, though some turbo generators are instrumented to measure the outlet temperature of the water from every coil. (For stator windings in new turbogenerators, water has superseded hydrogen, and it is the only choice for hydroelectric generators.) If the water flow fails the copper temperature rises very quickly, say 20 K/min, so the turbine output should be automatically reduced to avoid gross overheating and failure of insulation.

Gas turbine driven generators (see Section 28.21: BS 5000: Part 2 and ANSI C50: Part 14) are specified differently from others because the maximum output available from the turbine changes quite widely as the inlet air temperature changes. The generator must deliver the range of outputs that is the base capability of the turbine over the whole range of air temperature. If the generator is cooled by ambient air, and is allowed to operate up to limits of total temperature, the changing temperature rise allowed as the air temperature changes gives the generator a capability that matches that of the turbine more closely than if the rise were fixed at the rated-load value at all loads and ambient temperatures. Hence a smaller and cheaper generator can be used to cover the turbine rating across the operating temperature range.

The range of still higher outputs that is the peak capability of a gas turbine or internal combustion engine is handled by allowing higher total temperatures in the generator. At these, the insulation ages much more quickly than at standard temperatures. This is accepted because: (1) peak operation demands more frequent engine or turbine maintenance, so will not be used frequently or for long periods, and (2) many internal combustion engine or gas-turbine-driven sets are not expected to have a life of 10 years or more.

If a gas turbine driven generator has water-cooled heat exchangers, its range of permissible temperature rise will probably be less than the ambient air range due to the long thermal time constant of the source of the cooling water. The capability of the generator no longer matches the turbine output so closely and its frame size and design details are determined by the maximum turbine output but using the primary coolant temperature corresponding to the minimum cooling water temperature.

28.6 Output equation

The generator output in apparent power (S volt-amperes) is a function of the stator bore diameter D, the active axial length L, the speed n, the specific magnetic loading B, the specific electric loading A, and the stator winding factor K_w. The output is given by

$$\begin{aligned} S &= 11 K_w B A D^2 L n \text{ (volt-amperes)} \\ &= G D^2 L n \end{aligned} \qquad (28.12)$$

where G is the output coefficient ((V-A s)/m^3), B is the average fundamental frequency airgap flux density ($(2p\Phi)/(\pi DL)$ tesla), $2p$ is the number of poles, A is the ampere-conductor density ($(I_c N)/(\pi D)$ ampere/metre), where N is the total number of stator conductors $= 2T_{ph} \times$ number of phases, or number of slots $\times$ conductors per slot, I_c is the current in each conductor (= phase current/g amps), n is the speed in (rev/s), g is the number of parallel circuits per phase and 11 is a numerical multiplier when all dimensions are in metres. K_w is the product of the individual winding factors. $(K_d K_p K_s)$ and Φ, is the fundamental component of the flux per pole (in webers).

The stator winding is designed to develop the specified voltage with B and A close to chosen values. For different types and sizes of generator, B does not vary widely: it is limited primarily by the degree of saturation that is acceptable in the various parts of the magnetic circuit. Typical values of B are in the range 0.5–0.65 T on no load, corresponding to peak densities in the airgap of say 0.7 to 0.95 T. On load the increase of total flux and the distortion caused by armature reaction will cause the peak density to rise by about 15–20%.

The electrical loading A varies very widely with size and with the intensity of cooling, short-circuit ratio, reactances, etc.

G is numerically more convenient if expressed as

$$\frac{\text{kV-A}}{D^2 L \text{ rev/min}}$$

i.e. in (kV-A min)/m^3. *Figure 28.5* gives typical mean values of G against rated MVA; the values range from 5 to 35, but variations can easily be $\pm 15\%$ for a given output. With G in (kV-A min)/m^3, A is approximately $(6G)/B$ kA/m. Assuming $B = 0.6$ T, A ranges from 50 to 350 kA/m.[27]

The steady increase in G for salient pole generators is a result of the scale effect and progressive improvements in design, mainly in more effective cooling of the stator and field windings. For turbogenerators, a sharp increase in G occurred in the 1950s with the introduction of direct cooled rotor windings using hydrogen at 3 bar absolute pressure. Rapid increase in unit ratings and in G soon occurred, using hydrogen at pressures up to 5 bar absolute and improved methods of circulating it through the windings. It was necessary also to improve stator cooling in order to handle

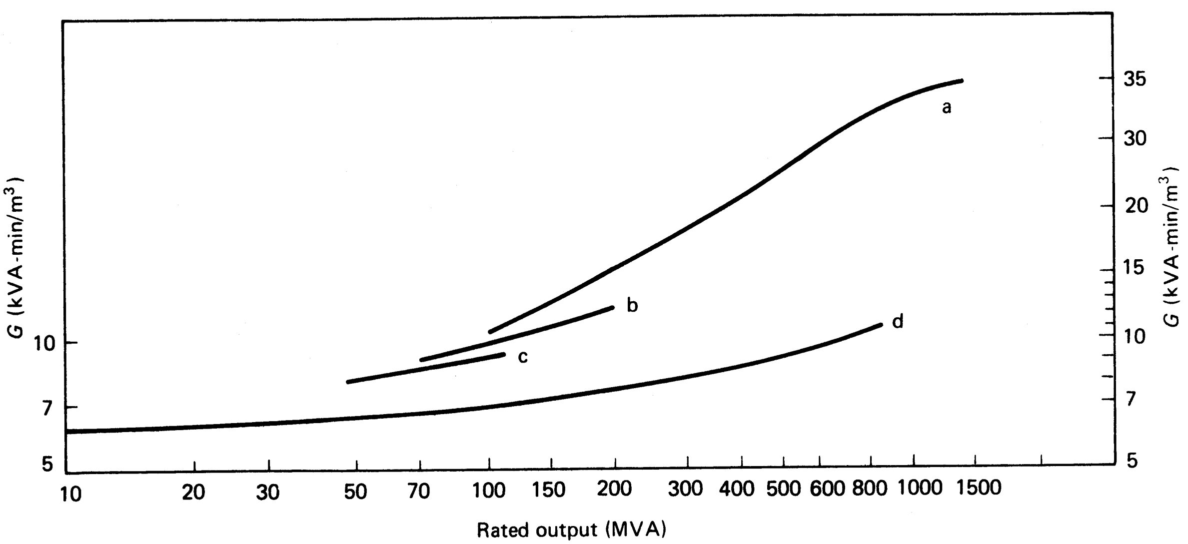

Figure 28.5 Output coefficients, G. Turbine generators: (a) directly hydrogen cooled + stator winding water; (b) directly air cooled; (c) indirectly hydrogen cooled; (d) salient-pole generator

the higher capability of the rotor. Water cooled stator windings have been universally adopted for machines of say 200 MW or more, although direct hydrogen cooling had been used for many stator windings up to 800 MVA output.

In recent years the demand for electric power has grown steadily at the rate of 5% per annum though there has been much public outcry against nuclear power and against acid rain from large fossil-fired stations. Hence there has been almost no call for turbogenerators larger than 500 MW recently, though the recent power shortages on the west coast of America and the insatiable demand for power both in the developing and the developed nations means that there is now renewed interest in nuclear power to meet those needs. In order to maximise efficiency and to satisfy the economics of building nuclear power stations, large generator units possibly up to 1500 MW may well be required in the near future. In recent years, there has been a large number of combined heat and power (CHP) systems have been installed. Here the waste heat from the prime mover, which may be a gas or diesel internal combustion engine or a gas turbine is used for a supplementary purpose such as district heating. This has the effect of increasing the efficiency of the power station. Another possibility to achieve enhanced efficiency has been the use of combined cycle systems where the exhaust gases from a gas turbine are used to raise the steam to be used in a steam turbine located alongside or on the same shaft as the gas turbine. These schemes require generators with ratings up to 150–200 MW at 3000 or 3600 rev/min. For low first cost, and for cheap installation and operation, air-cooled machines have been developed using directly cooled rotors, reaching output coefficients exceeding those of the early low hydrogen pressure machines.

28.6.1 Some design parameters

28.6.1.1 Specifed requirements

Appendix B of BS EN 60034-1:1998 lists information that should be supplied to the manufacturer.

A generator is required to meet specified values of output (kilovolt-amperes, voltage, and power factor) at a specified speed under stated operating conditions, of which the cooling conditions are most important. Specified limits of temperature rise or of total temperature must not be exceeded. The machine must maintain the performance during a life that is specified (or understood by custom and practice), and should do so with reasonable maintenance but without needing major repair. Normally a maximum value of X_{d} (or a minimum short-circuit ratio) is specified. A minimum X''_{d} and or a maximum X'_{d} may also be specified if currents under fault conditions are to be limited to a certain value or if voltage dips when starting specified loads are to be limited. National specifications recommend preferred voltages (e.g. ANSI C50: Parts 12, 13 and 14), but for large units connected by transformers to the system the generator voltage can be chosen to give the most suitable design.

28.6.1.2 Length to diameter (L/D) ratio

For two- and four-pole cylindrical rotor machines the cheapest design is usually one using the smallest diameter that does not lead to excessive length. The ratio usually lies in the range 3.5–6, perhaps up to 7 for the largest outputs (over 1000 MW) where the strength of retaining ring material limits the diameter to a surface speed of about 220 m/s (1.35 m diameter at 3000 rev/min). With such ratios, two-pole rotors for more than about 10 MW will have first and second critical speeds below the running speed. Higher ratios are avoided because they would lower the critical speed still more and make the rotor very sensitive to small (e.g. thermal) changes in balance.[93]

Cooling also becomes more difficult in very long machines. Salient pole machines with four poles and outputs of, say, 3–30 MVA may have a L/D close to unity but, because there are so many combinations of output, speed, overspeed, reactance, inertia, etc., no simple rule can cover the whole range of outputs. The ratio of L to pole pitch varies rather less than L/D, but still runs from 2.5 to 6 for outputs in the range 150–800 MVA, associated with speeds in the range 500 to 83 rev/min.

28.6.1.3 Stator winding

The most suitable type of winding is usually obvious from the specified current and voltage and the manufacturer's established practice (see Sections 28.3 and 28.4). The cost of making and installing the winding will be smaller the fewer the coils, but too few will cause difficulties. The current per slot, and the copper cross-sectional area, must be such that specified temperature rises are not exceeded. With too few slots, they are necessarily wide, and the total loss per slot may cause excessive temperature rise. With too many slots (and coils) they become narrow and deep, and much slot space is used for insulation; the coils are awkward to form and to support effectively in the endwinding. The ratio of slot depth to width should preferably not exceed 7 (4 – 6 is usual).

The ratio of slot width to airgap length should be about 1½ to 2 with solid pole shoes; with laminated shoes or poles 2½ to 3½ is permissible. Higher ratios increase the tooth ripple e.m.f., and the eddy current losses in the pole surface.[32,33] This is particularly important in salient pole machines with short airgaps especially machines with solid pole shoes. In turbogenerators the airgap is so long that this effect is rarely troublesome.

The tooth width must be enough to avoid excessive magnetic saturation on load, allowing for the radial vent ducts in the core. Roughly, the width of the tooth tip is about half the slot pitch.

28.6.1.4 Short-circuit ratio, synchronous reactance, and rotor temperature rise

The short-circuit ratio (SCR) and X_{d} are defined with reference to *Figure 28.10*.

$$\mathrm{SCR} = \frac{0\mathrm{b}}{0\mathrm{d}} \text{ and } X_{\mathrm{d}} = \frac{0\mathrm{d}}{0\mathrm{a}} = \frac{\mathrm{SF}}{\mathrm{SCR}}$$

where SF is a saturation factor, $\frac{0\mathrm{b}}{0\mathrm{a}}$

which usually has a value between 1.1 and 1.2. X_{d} is almost inversely proportional to the length of the airgap, and the SCR is approximately directly proportional to the length, l_{g}. Changing l_{g} adjusts SCR and X_{d}, but has only a small effect on other reactances.

For a given output and frame size, increased SCR requires increased excitation, especially if the length of the airgap is increased to raise the SCR, which may cause excessive field temperature rise. Then, unless the cooling can be improved, a lower electrical loading or a larger frame size becomes necessary.

Evidently, a lower SCR permits more output from a given frame size which will be determined by the thermal limit set by the rotor: of course the stator must also be adequately cooled.

As turbogenerator ratings increased, lower values of SCR came to be accepted, partly as a matter of necessity to restrain the increase in physical size, but largely because faster control of excitation and of turbine valves, and shorter fault-clearing times, made it possible to operate safely closer to the stability limit. SCRs fell from the usual range of 0.8–0.6 to 0.6–0.45 for turbogenerators.

Hydrogenerators too have benefited from improved excitation control, power system stabilisers and faster clearing times, but are still required to supply a higher line-charging capability than most turbogenerators. Their SCRs are therefore usually in the range of 0.8–1.5. Other salient pole generators up to say 40 MW (see Section 28.18) have an SCR usually between 0.5 and 0.8. with some up to 1.0 for particular locations.

28.6.1.5 Other reactances (see Section 28.8 for the identification of these reactances)

The stator leakage reactance is a large component of the subtransient and transient reactances. The slot leakage flux contributes most to the leakage reactance, as the endwinding leakage flux is comparatively small, especially in long machines.

Kilgore[84] expresses each reactance as the product of a reactance factor

$$X = \frac{AK_w}{\sqrt{2}\cdot \epsilon_1 B_g}\text{(per unit, p.u.)}$$

and a permeance λ calculated from the geometry of the path of the flux associated with the reactance concerned. For the stator slot, λ is approximately proportional to the slot depth divided by the total width of the $3q$ slots in each pole pitch. Hence for a given output and frame size, a winding with fewer, wider and, therefore, shallower slots has a smaller slot leakage reactance than one with more, narrower and deeper ones. It should cost less too, but the copper will be less well cooled, unless it is cooled directly.

$$X'_d = X_1 + X_F$$

and

$$X''_d = X_1 + \left(\frac{X_F X_D}{X_F + X_D}\right)$$

where X_F and X_D depend on the dimensions of the pole and the damper cage, and these are chosen primarily to suit the electromagnetic and mechanical design requirements. For most salient-pole machines the ratio of $X'_d/X''_d = 1.4$ to 1.6; for most turbogenerators the ratio is 1.3–1.5.

The electrical design can be manipulated to change X''_d and X'_d by changing X_1 and there is a little scope for changing X_D to alter the ratio X'_d/X''_d. Reducing the electrical loading A will reduce X'_1 but will require a larger DL product to increase the main flux with unchanged B. Leaving empty slot above the stator winding increases X'_1, but requires a corresponding increase in the outer diameter of the core to avoid increased back of core flux density. So specifying reactances different from those naturally occurring with the otherwise optimum design will raise the cost.

Some compromise between the values of X'_d and X''_d may have to be made. A high X''_d is desirable to limit the initial short-circuit current and hence reduce the duty of the associated switchgear. A low X'_d may be preferred because it would reduce the voltage dip when load is suddenly applied. For example to start a large motor on a fairly small generator or generator group. Or it may help to retain stable operation after a disturbance on the power system. In a generator with laminated salient poles, the damper cage design can be adjusted to bring X'_d/X''_d down towards 1.4, but solid poles and shoes permit almost no adjustment from the usual 1.5–1.6. In turboalternators the rotor tooth tops and wedges, and any damper winding beneath them, are in parallel. Again, the dimensions and wedge materials must first satisfy mechanical and magnetic requirements, and only a very small adjustment of the X'_d/X''_d ratio is possible.

Kostenko[11] and Ames[2] use methods similar in principle to Kilgore's for calculating reactances. Kostenko gives formulae for λ for different slot shapes; Ames presents a detailed treatment of gap leakage reactance that is considered to give better results for short gap designs. Ames also has a useful section on the currents and torques resulting from three-phase and single-phase short circuits, and on voltage dip caused by a suddenly applied load.

Today, analysis using finite element techniques is used to derive reactance figures with increased accuracy.

28.6.1.6 Inertia constant, H

A minimum inertia of the whole generator set may be needed: to help to maintain transient stability during a system fault; to limit the overspeed of a hydrogenerator set; or to limit the cyclic speed irregularity of a set driven by an internal combustion engine.

When H *is* defined as the stored energy at rated speed in watt-seconds, divided by the rated volt-amperes, then in SI units stored energy $= 1/2\, J\omega^2$ metre-newtons. i.e. watt-seconds, and

$$H = \frac{1}{2}\frac{J\omega^2}{\text{VA}}\ \text{seconds} \tag{28.13}$$

where J is the polar moment of inertia of the rotor ($= mk^2$ kgm^2, where k is the radius of gyration); ω is the speed (in rad/s); and m is the mass (in kg). The term WR^2 is also frequently used in this context where GD$^2 = 4 \times$ WR^2. If this is the case, the term 1.37 in equation 28.14 is increased to 5.48 for an inertia figure quoted as the WR^2.

Alternatively, in usual engineering units,

$$H = \frac{1.37\ GD^2(\text{rev/min})^2}{\text{Rated kVA}}\ \text{seconds} \tag{28.14}$$

where GD^2 is the polar moment of inertia (in kg-m^2)

$$GD^2 = m(2k)^2 = 4J$$

Typical values of H for the generator alone are

Salient pole	
Medium speed:	1–2 s
Low speed (100–150 rev/min):	2–4 s
Hydrogenerator	Usually 3–5 s, can be up to 8 s
Cylindrical rotor	
2 and 4 pole	2–4 s

A directly coupled steam turbine may have 2–4 times as much inertia as its generator, but a hydraulic turbine adds relatively little. A hydrogenerator may therefore have to be larger than the electrical performance would require, whereas the turbogenerator designer can usually accept the inertia of the design that meets the other criteria.

28.7 Armature reaction

Balanced three-phase sinusoidal currents in the stator winding produce an approximately sinusoidal m.m.f. wave rotating round the airgap at synchronous speed $n_s = f/p$ rev/sec. The wave continuously changes shape between two extremes, which occur in turn every 1/12 of a cycle of the current. The wave can be represented as a fundamental plus harmonics thus:

$$\text{m.m.f.} = 1.35\frac{IT_{ph}}{g}\left[k_{w1}\sin(\omega t-\theta)+\frac{1}{5}k_{w5}\sin(\omega t+5\theta)+\frac{1}{7}k_{w7}\sin(\omega t-7\theta)+\cdots\frac{1}{n}k_{wn}\sin(\omega t\pm n\theta)\right] \quad (28.15)$$

where $1.35((IT_{ph})/g)k_{W1}$ ampere-turns per pole is the amplitude of the fundamental wave and θ is a space angle in electrical degrees (one fundamental pole pitch = 180°). Harmonic orders n are $n = 6m \pm 1$ where m is any integer. The sign of n is opposite to that of 1 in $n = 6m \pm 1$.

Each harmonic rotates at a speed inversely proportional to its order; the higher order of each pair, the $(6m+1)$, goes forwards (i.e. the same direction as the fundamental) and the lower one $(6m-1)$ goes backwards.

The harmonic fluxes induce small e.m.f.s at supply frequency in the stator winding, not large enough to affect the r.m.s. value of the phase voltage. However, they induce currents at $6m$ times supply frequency in the rotor damper winding, pole faces, etc. These can cause significant extra losses and local temperature rises.[32,33] The fifth and seventh harmonic m.m.f.s are the largest, but with a 5/6 pitch winding, for which $K_{p5} = K_{p7} = 0.259$, and spread factors of about 0.2, they are reduced to about 1% of the fundamental m.m.f. The m.m.f. winding factors are the same as those applied to the calculation of open-circuit e.m.f.

Figure 28.6 shows how the gap flux wave shown in *Figure 28.1(c)* is distorted by armature reaction, and shows a small increase in the (mainly slot) harmonics. Harmonic currents supplied by the generator are not often troublesome, unless the load has large capacitance, or resonates to give a low impedance at a particular frequency. The harmonic content in the terminal voltage will depend on the impedances of the load and the generator at the harmonic frequencies.

In fractional slot windings the currents produce also even harmonics and subharmonics: the latter have wavelengths that are multiples of the fundamental double pole pitch. In large multipolar machines such as large hydrogenerators, the subharmonic flux may cause unacceptable deflection and vibration of the stator core, especially if it is a bit shallow radially behind the slots, or has a natural frequency of vibration close to a subharmonic frequency. Some such stator frames have been damaged by this effect (see Liwschitz[36,34] and Walker[38])

28.7.1 Cylindrical-rotor machine

The gap flux wave developed by the fundamental stator ('armature') m.m.f. acting alone is nearly sinusoidally distributed because of the uniform airgap. The gap flux developed by the rotor ('field') m.m.f. acting alone has a trapezoidal distribution. On load the gap flux results from the stator and rotor m.m.f.s in combination. The fundamental components of the distributed m.m.f.s can be represented by phasors of peak values F_a and F_f ampere-turns per pole, respectively, each directed along the corresponding axis of maximum m.m.f. The fundamental component of gap flux on load is proportional to the phasor sum F of F_a and F_f (neglecting magnetic saturation).

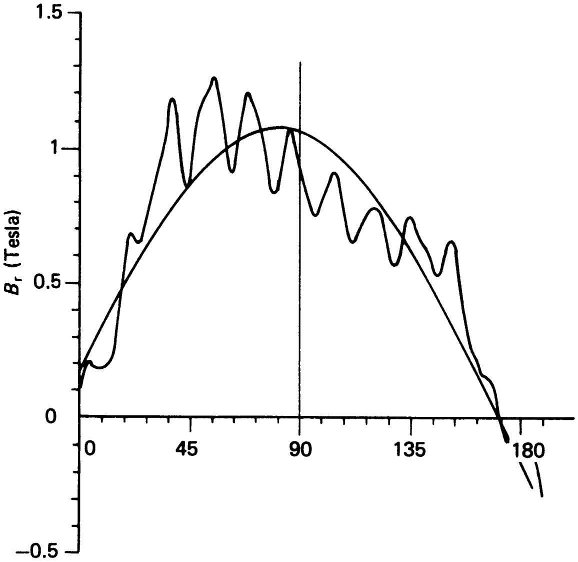

Figure 28.6 Salient-pole flux waveform on load

F_f is centred on the pole axis (the direct or d axis) to which the interpolar axis (the quadrature or q axis) is in electrical space quadrature. In general, the axis of F_a is displaced from the d axis by an angle β depending on the load and the power factor. F_a can be resolved into components F_{ad} and F_{aq}, respectively, on the d and q axes. *Figure 28.7(a)* shows, for a balanced three-phase cylindrical-rotor machine, the stator and rotor current-sheet patterns for an instant of zero current in stator phase C. (The black areas represent outward, and the cross-hatched areas inward, current direction.) The m.m.f. phasors F_a and F_f are displaced by angle, β. The resultant m.m.f. acting on the airgap is the phasor sum $F_f + P_a = F$. Assuming each m.m.f. to develop an individual flux, the e.m.f.s E_f, E_a and E_{ph} in *Figure 28.7(b)* are respectively induced by F_f, F_a and F. Then E_{ph} is the terminal e.m.f. for a representative stator phase.

28.7.2 Salient-pole rotor machine

The gap reluctance is far from uniform, and a given stator m.m.f. acting on the q axis produces less flux than it would if acting on the lower reluctance of the d axis. This is indicated in *Figure 28.7(c)*. The d axis flux is distributed almost sinusoidally, but the q axis flux contains significant space-harmonics, chiefly the third. To deduce the d and q axis fluxes, hence e.m.f.s and reactances, it is necessary to resolve F_a into the axis components F_{ad} and F_{aq}, then to evaluate separately the fluxes they produce. This is done in detail using, for example, finite element analysis of the field. Hand calculation can be done using coefficients presented

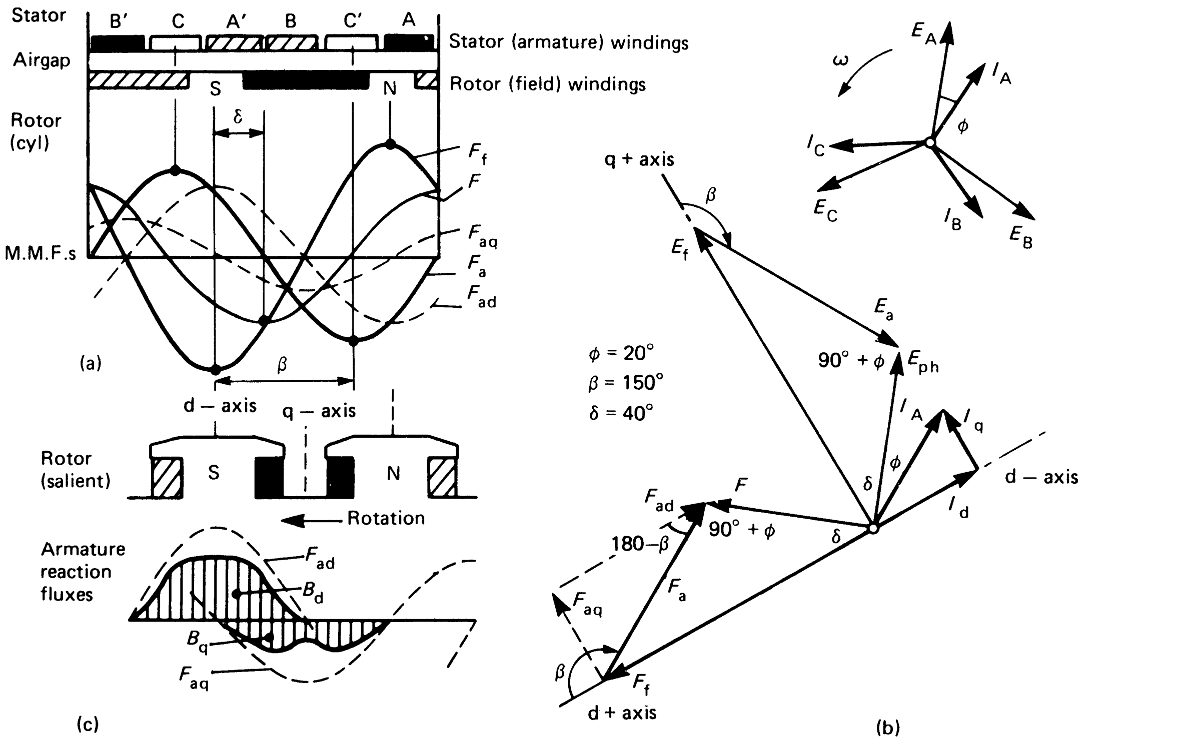

Figure 28.7 Airgap m.m.f. distribution

by Wieseman[44] and by Ginsberg *et al.*[45] in terms of the pole and airgap profile, and the minimum gap reluctance at the pole centre line.

28.7.3 Magnitudes and equivalence of stator and rotor m.m.f.

For a balanced three-phase winding with 60° phase spread, the peak of the fundamental component of the m.m.f. wave is

$$F_a = 1.35 K_w \frac{IT_{ph}}{gp} \text{ (A-t/pole)} \quad (28.16)$$

where I is the r.m.s. phase current, g is the number of parallel circuits per phase, T_{ph} is the total number of turns per phase, and $2p$ is the number of poles. With a cylindrical rotor the proportions of the trapezoidal m.m.f. waveshape and the peak of its fundamental component depend on the field form factor C_f which is a function of the ratio

$$\lambda = \frac{\text{Total slotted arc}}{\text{Total circumference}}$$

i.e.

$$\lambda = \frac{\text{Number of slots}}{\text{Number in the circumference if all were cut}}$$

$$\text{e.g. } \lambda = \frac{28}{37} = 0.757$$

$$C_f = \frac{8\pi^2}{\lambda\varsigma}\sin(\lambda \cdot 90°) \quad (28.17)$$

λ is usually 0.65 to 0.75, and C_f therefore from 1.06 to 1.0. Then the peak fundamental m.m.f. of the rotor is given by $C_f I_f N_f$ A-t/pole, where N_f turns per pole carry the field current I_f.

On load the rotor m.m.f. F_f must be such that when combined with the armature reaction m.m.f. F_a the net m.m.f. F must be sufficient to provide the flux needed to generate the e.m.f. E. Hence F_a must be put in terms of the rotor m.m.f. that would develop the same flux as F_a, i.e. equating the peak fundamental m.m.f.s of stator and rotor:

$$C_f I_f N_f = 1.35 k_w \frac{IT_{ph}}{gp} \text{ (A-t/pole)} \quad (28.18)$$

Hence

$$I_f N_f = \frac{1.35}{C_f} k_w \frac{IT_{ph}}{gp} \text{ (A-t/pole)} \quad (28.19)$$

Putting C_f at a typical value of 1.03, the rotor equivalent of the reaction m.m.f. F_a is

$$F_{af} = I_f N_f = 1.31 k_w \frac{IT_{ph}}{gp} \text{ (A-t/pole)} \quad (28.20)$$

With a salient pole rotor, with a distributed stator winding, a concentrated rotor winding, and a non-uniform gap reluctance, it is necessary in effect to calculate and equate the fundamental fluxes, not the m.m.f.s, along the direct axis.

Wieseman gives curves for estimating the factor C_{dl}, which is the ratio of the fundamental airgap flux produced by armature reaction m.m.f. directed along the direct axis to that which would be produced if the airgap were uniform and equal to the effective gap at the pole centre line. Then the peak fundamental flux density produced on the d axis by F_a is proportional to $C_{dl}F_a$. For typical profiles C_{dl} lies between 0.85 and 0.95.

The peak fundamental d axis flux produced by a rotor m.m.f. I_fN_f is proportional to $C_1I_fN_f$. The value of I_fN_f that makes $C_1I_fN_f = C_{dl}F_a$ is F_{at}, the rotor m.m.f. equivalent of F_a. Then

$$F_{af} = I_fN_f = \frac{C_{d1}F_a}{C_1} \qquad (28.21)\Leftarrow$$

$$C_1 =\Leftarrow \frac{\text{Peak of fundamental flux wave}}{\text{Peak of actual flux wave}}$$

Putting in typical values of C_{d1}, and C_1

$$F_{af} = (1.08 \text{ to } 1.2)k_w \frac{IT_{ph}}{gp}(\text{A-t/pole}) \qquad (28.22)\Leftarrow$$

Figure 28.20 shows how F_{at} is combined with F_e to calculate F_f, the on-load ampere-turns per pole.

28.8 Reactances and time constants[11,83,84,86–91]

In order to evaluate the steady-state behaviour of a synchronous generator or its response to changes of load, excitation and system disturbances, a mathematical model of the machine is required. Reactances have been defined which, with winding resistances where significant, can form an appropriate equivalent circuit for which behaviour equations can he written; these equations can then be solved to determine the performance of the machine.

The reactances commonly employed are described below. They are associated with a two-axis model, represented on the d axis by an equivalent stator winding, the field winding and a damper winding, and on the q axis by a second equivalent stator winding and one damper winding. Circuit impedances can normally be taken as reactances because the resistances are comparatively small; however, the resistance values directly influence the time constants.

Voltages, currents and reactances are usually expressed in per-unit (p.u.) terms of rated voltage and current; other values are put in p.u. by dividing actual voltage and current by the rated values. The unit reactance is the ratio rated phase voltage/rated phase current. Thus a reactance having a voltage drop of 0.2 p.u. when carrying 0.5 p.u. current has a value of 0.4 p.u. Stator voltages, currents and reactances are all per-phase values.

Each reactance is associated with a particular component of flux produced by either the d or the q axis component of current in the stator winding. A d axis current produces a d axis flux: as shown in *Figure 28.7*, the conductors in which it flows are near the q axis but they form coils magnetising on the d axis. Similarly a q axis current produces a q axis flux. The numerical value of each reactance is then the fundamental frequency e.m.f. per phase generated by the associated flux, divided by the corresponding component of current. Usually reactances are defined with rated current in the d or q axis and are termed 'rated' or 'unsaturated' values. With the heavy currents occurring under short-circuit conditions, saturation reduces the flux per ampere, and the saturated reactance values are lower.

The reactance values can be derived from the machine geometry by calculating first the permeance of the associated flux path and then the inductance L, i.e. the flux linkage with the stator winding per ampere of stator current in the required axis. The reactance (in ohms) is $2\pi fL$. Difficulties arise in defining exactly the flux paths and permeances and in allowing for saturation at high flux densities, even though a large part of the leakage flux paths is in air.

28.8.1 Armature leakage reactance

The armature leakage reactance X_1 results from stator leakage flux that crosses the stator slots, flux that passes circumferentially from tooth to tooth round the airgap without entering the rotor and flux linking the stator endwinding. X_1 is a component of all the positive- and negative-sequence reactances. Since the flux paths are independent of the rotor, the reactance has the same value for both axes; and since the flux paths are the same for positive- and negative-sequence currents, X_1 has the same value for both.

28.8.2 Magnetisation (armature reaction) reactances

The magnetisation (armature-reaction) reactances are associated with the synchronously rotating flux set up by positive-sequence current in the stator winding, i.e. by balanced phase currents. Suppose the field winding is rotating synchronously but is open-circuited, and a three-phase e.m.f. (V) of correct phase sequence is applied to the stator winding. After the initial damper circuit currents have delayed, a steady stator current I flows. If the e.m.f. is phased so that I produces flux along the pole axis, i.e. $I = I_d$, then I_d has a value sufficient to establish an airgap flux that induces a stator e.m.f. $= V$ (neglecting the stator resistance and leakage reactance drops). V/I_d is the direct axis magnetising reactance X_{ad}. If the phasing is such that I produces only q axis flux, i.e. $I = I_q$, then V/I_q is the corresponding reactance X_{aq}. In most generators, X_{ad} lies between 1 and 2.2 p.u. In salient-pole machines, X_{aq} is typically 0.6 X_{ad}. In cylindrical-rotor machines the airgap is of uniform length, but the rotor slots slightly increase the q axis reluctance, so X_{aq} is usually approximately 0.9 X_{ad}.

28.8.3 Synchronous reactances

The synchronous reactances are the total reactances presented to the applied stator voltage when the rotor is running synchronously but unexcited:

$$X_d = X_{ad} + X_1 \text{ and } X_q = X_{aq} + X_1 \qquad (28.23)\Leftarrow$$

The magnetising and synchronous reactances are steady-state values applicable with balanced phase currents of constant r.m.s. value. They are defined by considering the machine to be excited by stator current only. The two axis reactances can be represented by the equivalent circuits in *Figure 28.8*.

28.8.4 Transient and subtransient reactances

The transient and subtransient reactances relate to conditions that arise when the m.m.f. on the magnetic circuit of the machine is suddenly changed. Consider the conditions

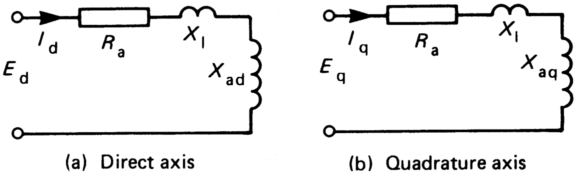

Figure 28.8 Equivalent circuits for d- and q-axis armature reaction reactances

following the sudden application of a three-phase supply of voltage V to the stator winding, with the rotor running synchronously and its field winding closed but unexcited. The three-phase stator currents develop an m.m.f. that rotates synchronously with the rotor. (There are direct-current (d.c.) components as well, but they are not relevant here.) Suppose that the instant of switching is such that the stator m.m.f. is impressed on the pole axis. The flux linkages of the stator winding must induce an e.m.f. that balances V (neglecting resistance). If there were no current paths on the rotor, the stator would immediately carry currents necessary to magnetise the machine to the required flux level, i.e. $I = V/X_d$. However, if there are closed rotor circuits available (i.e. the field winding, damper windings and solid iron poleshoes), currents are induced in them, inhibiting the rise of flux through the rotor poles and so forcing the flux into rotor leakage paths of high reluctance. Hence the initial stator current must be larger than V/X_d. The leakage paths are largely circumferential in the pole faces, from pole to pole in the gap between adjacent salient poles, and across the wedges and tooth tips in a turbogenerator rotor. This path adds only a small permeance to that of the stator leakage paths alone, so the effective reactance is not much more than X_1.

I^2R losses in the damper circuits cause these currents to decay rapidly, enabling the flux to penetrate past the dampers into the pole and field winding region. The permeance of the available flux path therefore increases, decreasing the stator current needed to maintain the required stator flux linkage. Thus the induced rotor currents, and the stator current, decrease in unison, at first rapidly as the damper currents decay, then for a time more slowly, until eventually the induced current in the field winding has disappeared, the flux is fully established along the main flux paths and the stator current is settled at its magnetising value $I = V/X_d$. The main decay time is called the transient period, and the brief initial decay period is the *subtransient*.

The machine as seen from the supply system can be represented by the equivalent circuit shown in *Figure 28.9*(*a*), where I_{kd} and I_f are the components of I_d needed to balance the induced damper and field currents respectively. The effective impedance increases progressively from its initial value of X_r in series with the other three circuits in parallel, through X_1 in series with X_{ad} and the field circuit in parallel when I_{kd} has reached zero, to $X_1 + X_{ad}$ in the steady state. Thus the transient reactance X'_d and subtransient reactance X''_d are

$$X'_d = X_1 + \frac{1}{1/X_{ad} + 1/X_f} \tag{28.24a}$$

and

$$X''_d = X_1 + \frac{1}{1/X_{ad} + 1/X_f + 1/X_{kd}} \tag{28.24b}$$

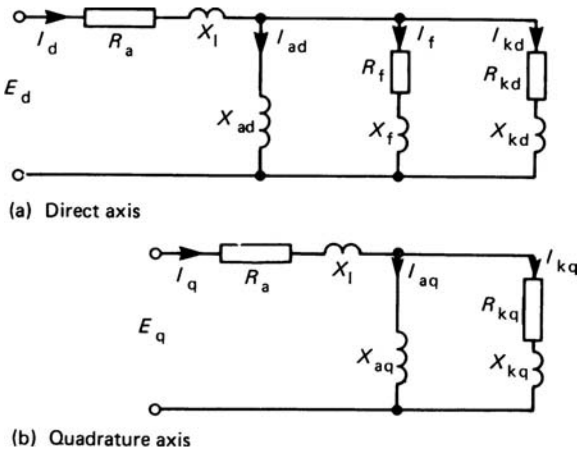

Figure 28.9 Equivalent circuits for d- and q-axis reactances

If the moment of switching is such that flux is established along the quadrature axis, similar arguments apply except that q axis flux has no net linkage with the field winding. There is, therefore, no q axis reactance corresponding to X_f on the d axis, and in this simple model the q axis circuit is as shown in *Figure 28.9*(*b*).

The q axis subtransient reactance is

$$X''_q = X_1 + \frac{X_{aq}X_{kq}}{X_{aq} + X_{kq}} \tag{28.25}$$

In practice, the induced current paths in the iron on the d and q axes change as the flux distributions change, so neither axis can be accurately represented by a single damper circuit, with fixed X and R and therefore one fixed time constant. This is especially true of solid cylindrical rotors, in which the tooth tops and slot wedges form a surface damper cage of relatively high resistance with a time constant typically less than 50 ms. As surface currents decay, lower resistance current paths in the poles and beneath the slots become effective, introducing higher reactances with time constants up to a few seconds. Hence the machine can be represented more closely by having two damper windings on each axis. Traditionally the direct axis reactances X'_d and X''_d and associated time constants have been deduced from oscillograms of a sudden symmetrical three-phase short-circuit test, assuming only one damper circuit. Values appropriate to different levels of magnetic saturation can be found by testing at a number of voltages (tests at or near full voltage are rarely done because they cause very heavy forces on the windings). The short-circuit test measures only d axis values; other tests for these, and for q axis quantities, are given in IEEE Publication 115: 1983 and IEC Publication 34–4 1985; IEEE 115A 1987, describes frequency response tests (see Section 28.8.1).

In a salient pole rotor with laminated poles and specific damper cages, the damper circuits are more clearly defined, and a model with one damper on each axis (as well as the d axis field) is accurate enough for many purposes. Such a model has in the past been used for turbogenerators too, but advances in design and test procedures, and in computer analysis, have encouraged the use of the more elaborate models.

28.8.5 Negative-sequence reactance

Unbalanced load or fault conditions are usually analysed by the method of symmetrical positive, negative and zero phase-sequence components (p.p.s, n.p.s. and z.p.s. components). Currents of n.p.s. in the stator produce an m.m.f. rotating at synchronous speed in a direction opposite to that of the rotor. This m.m f. acts upon the d and q axes in turn, inducing double-frequency currents in any available rotor circuit. The stator presents a low reactance to n.p.s. current, taken to be the mean of the subtransient d and q axis values, i.e.

$$X_2 = \frac{1}{2}(X''_{\mathrm{d}} + X''_{\mathrm{q}}) \tag{28.26}$$

28.8.6 Zero-sequence reactance

Zero-sequence currents in the three phases are equal and in time phase, and their combined effect is to produce a stationary field alternating at supply frequency, therefore inducing a stator e.m.f. of that frequency. The m.m.f. and therefore the flux are small compared with the p.p.s. and n.p.s. components, and they depend heavily on the coil-span, falling from a maximum value to zero as the span decreases from full pitch to 2/3 pitch. Accordingly X_0 is usually quite small.

28.8.7 Reactance values

Ranges of typical values of reactances and time-constants are given in *Table 28.4*. Since the ranges all depend on the details of the machine design, there will be exceptions to the following generalisations, which do however indicate normal trends.

For a given output and speed, the physically smaller machine will have a higher current loading, so all reactances will be higher than those of a larger, and therefore 'slacker', design. At a given speed, reactances tend to rise with increasing rated output, since higher electrical loading and more intensive cooling are needed to attain more output per unit volume of active material. For a given output, low-speed machines are physically larger, and tend to have higher reactances, than high-speed designs.

28.8.8 Reactances and time constants

The reactances and time constants are based on equivalent circuits, such as those in *Figure 28.9*. Time constants are given by inductance/resistance ratios, i.e. the ratio of $X/2\pi f$ to R. In the formulae below, $2\pi f$ is written as ω, the angular frequency. All time constants are in seconds if all X and R values are expressed consistently in per-unit or ohmic values.

28.8.8.1 Open-circuit

With the stator winding open-circuited, the leakage impedance (X_1 and R_a) has no influence and the transient behaviour is determined by the inductance and resistance of the field winding. The open-circuit transient time constant is

$$T'_{\mathrm{do}} = (X_{\mathrm{ad}} + X_{\mathrm{f}})/\omega R_{\mathrm{f}} \tag{28.27}$$

The subtransient duration depends primarily on damper-circuit currents: if we neglect the effect of R_f, the time constant is

$$T''_{\mathrm{do}} = \left[\left(X_{\mathrm{kd}} + \frac{X_{\mathrm{ad}}X_{\mathrm{f}}}{X_{\mathrm{ad}} + X_{\mathrm{f}}}\right)\right]\frac{1}{\omega R_{\mathrm{kd}}} \tag{28.28}$$

28.8.8.2 Short circuit

With the stator short circuited, current and flux changes are influenced by X_1, but R_a is usually negligible. The transient time constant is

Table 28.4 Synchronous generators: typical reactances (p.u.) and time constants (s)

Parameter	*Symbol*	*Turbogenerator*	*Salient-pole generator*		*Compensator*
			With dampers	*Without dampers*	
Synchronous reactance					
d axis	X_d	1.0–2.5	1.0–2.0		0.8–2.0
q axis	X_q	1.0–2.5	0.6–1.2		0.5–1.5
Armature leakage reactance	X_1	0.1–0.2	0.1–0.2		0.1–0.2
Transient reactance					
d axis	X'_d	0.2–0.35	0.2–0.45	0.2–0.45	0.2–0.35
q axis	X'_q	0.5–1.0	0.25–0.8	0.5–1.0	0.25–0.8
Subtransient reactance					
d axis	X''_d	0.1–0.25	0.15–0.25	0.15–0.3	0.1–0.25
q axis	X''_q	0.1–0.25	0.2–0.8	0.5–1.0	0.2–0.8
Negative-sequence reactance	X_2	0.1–0.25	0.15–0.6	0.25–0.65	0.15–0.5
Zero-sequence reactance	X_0	0.01–0.15	0.04–0.2		0.03–0.2
Time-constants					
Time-constants					
D.c.	T_a	0.1–0.2	0.1–0.2		0.1–0.2
Transient	T'_d	1.0–1.5	1.5–2.0		1.5–2.5
Subtransient	T''_d	0.03–0.1	0.03–0.1		0.03–0.1
Open-circuit transient	T'_{do}	4.5–13	3–8		5–8

$$T'_d = \left(X_f + \frac{X_1 X_{ad}}{X_1 + X_{ad}}\right)\frac{1}{\omega R_f} \tag{28.29}$$

The subtransient short circuit time constant depends primarily on damper-circuit parameters:

$$T''_d = \left(X_{kd} + \frac{X_{ad} X_f X_1}{X_{ad} X_f + X_f X_1 + X_1 X_{ad}}\right)\frac{1}{\omega R_{kd}} \tag{28.30}$$

The armature short circuit time constant relates to the rate of decay of d.c. components of stator current that occur from the beginning of a sudden short circuit:

$$T_a = \frac{X_2}{\omega R_a} = \frac{X''_d + X''_q}{2\omega R_a} \tag{28.31}$$

28.8.8.3 q Axis

If the machine is represented by the simple model with only one q axis damper circuit, only subtransient time constants occur. These are

$$T''_{qo} = (X_{aq} + X_{kq})/\omega R_{kq} \quad \text{open circuit} \tag{28.32}$$

$$T''_q = \left(X_{kq} + \frac{X_{aq} X_1}{X_{aq} + X_1}\right)\Big/\omega R_{kq} \quad \text{short circuit} \tag{28.33}$$

28.8.8.4 Other relations

From the foregoing, it is found that

$$T'_d / T'_{do} = X'_d / X_d \tag{28.34}$$

$$T''_q / T''_{qo} = X''_q / X_q \tag{28.35}$$

$$T''_d / T''_{do} = X''_d / X'_d \tag{28.36}$$

28.8.9 Potier reactance

The Potier reactance X_p is an estimate of armature leakage reactance deduced from the open-circuit and zero-power factor (z.p.f.) curves; it is used in one method of calculating the on-load field current allowing for saturation. X_p is slightly higher than the true X_1, especially for salient-pole machines where pole saturation is greater than on normal load because of the greater pole-to-pole leakage flux for the z.p.f. conditions. Hence using X_p somewhat overestimates the load excitation.

28.8.10 Frequency-response tests[87–89]

The d and q axis parameters can be measured by injecting one-phase current into the stator winding over a frequency range (typically 1 mHz to 1 kHz), the rotor being stationary with its d and q axes in turn aligned with the stator field. By fitting an expression in the Laplace form

$$X_d(s) = \frac{(1 + sT'_d)(1 + sT''_d)}{(1 + sT'_{do})(1 + sT''_{do})} \tag{28.37}$$

to the curve of d axis reactance against frequency, X_d and the time constants can be found, and the corresponding X'_d and X''_d values derived, appropriate to a machine model with one damper circuit on the d axis.

To fit the q axis reactance-frequency curve reasonably closely, it is necessary to assume two damper circuits, which give an expression similar to the d axis expression quoted, but with q axis quantities. Thus X'_q and T'_q values are deduced, as well as X''_q and T''_q, despite the absence of a field winding on the q axis.

More accurate fits to the measured frequency–response curves may be obtained by using an equivalent with more damper circuits and corresponding time-constants. Techniques have been developed for taking frequency–response curves on the machine in service in order to obtain values more appropriate to the load condition. There is an extensive literature in the *IEEE Journal* (*Power Apparatus and Systems*). A review of the subject, and some specific papers are contained in IEEE Publication 83TH0101-6-PWR, Symposium on Synchronous Machine Modelling for Power System Studies, February 1983.

ANSI/IEEE 115A—1995 describes 'Standard procedures for obtaining synchronous machine parameters by standstill frequency response testing'. It contains eight references, and an appendix showing how operational impedances transfer functions and the R and L values of the equivalent circuit can be deduced from the measurements.

Measurements of the behaviour of machines and systems when they are subjected to test disturbances have shown that for some stability calculations the simpler circuits, even with the subtransient effects neglected, are adequate. However, for calculating short-circuit torques, the subtransients must be included; and for detailed analysis of the effects of excitation control, more elaborate models are needed.

28.9 Steady-state operation

28.9.1 Open- and short-circuit characteristics

Prediction of the operation of a generator in the steady state is based on the open- and short-circuit characteristics shown in *Figure 28.10*.

With the stator winding on *open circuit*, the field current I_f produces a mutual flux linking the stator and rotor windings, plus a relatively small rotor leakage flux linking the rotor winding only. The corresponding stator winding flux

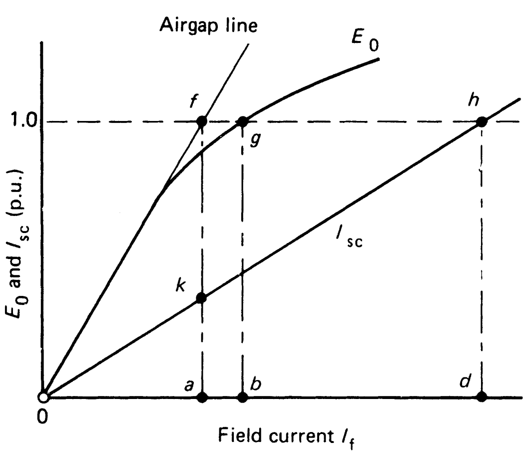

Figure 28.10 Open- and short-circuit characteristics

linkage per phase Ψ_0 generates the stator e.m.f. At rated speed, the value of I_f represented by 0b generates rated e.m.f. E_o represented by bg; 0a is the current needed to overcome the reluctance of the airgap, and ab is required for the iron parts of the magnetic circuit.

With the stator winding *short circuited*, field current 0d circulates rated stator current I_{sc}, represented by hd. Armature-reaction m.m.f. produced by I_{sc} is wholly demagnetising, and the difference between it and the field m.m.f. (0d) develops a mutual flux Φ_{sc}, sufficient to induce an e.m.f. E_{sc} equal to the stator leakage reactance drop $I_{sc}X_l$. This neglects the stator winding resistance R_a and any harmonic fluxes developed by the stator and rotor m.m.f.s.

The flux Φ_{sc} is too small to cause magnetic saturation; hence I_{sc} is proportional to I_f. As both E_{sc} and X_l are proportional to frequency, the short-circuit characteristic is almost independent of speed; nevertheless, it is usual to obtain it at rated speed.

Still neglecting saturation and armature resistance, a field current $I_f = 0a$ gives $E_o = af$ on open circuit and $I_{sc} = ak$ on short circuit. Thus on short circuit the stator appears to present a reactance $X_{du} = E_a/I_{sc} = af/ak$, a constant representing the *unsaturated* d axis synchronous reactance $X_{ad} + X_l \cdot X_{du}$ is usually defined in terms of I_f as the ratio 0d/0a in *Figure 28.10*. The short-circuit ratio is defined as 0b/0d which from the geometry is $(0b/0a)(l/X_{du})$. Here 0b/0a is a saturation factor, usually in the range 1.1–1.2.

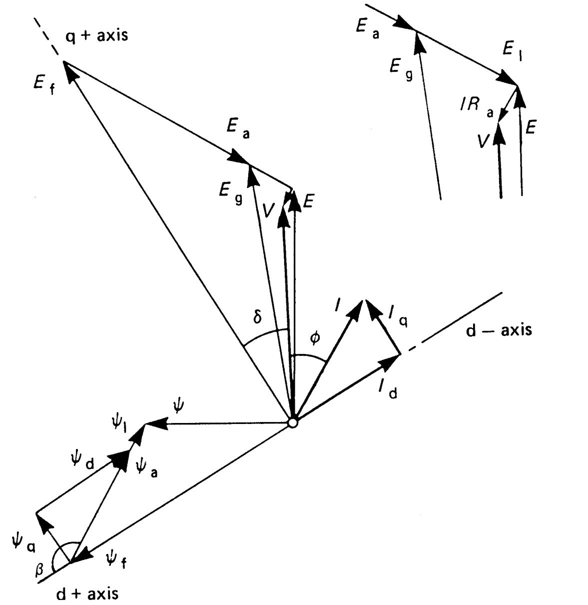

Figure 28.11 Phasor diagram for a cylindrical-rotor generator on load

28.9.2 Phasor diagram and power output

The resultant m.m.f. of the stator current I and the held current I_f develops an airgap flux Φ which induces the stator phase e.m.f. E_{ph}. The stator leakage flux Φ_l induces the e.m.f. E_l.

Detailed analysis allowing for local flux distribution and the variation of magnetic permeability in ferromagnetic parts of the magnetic circuit is necessary in design. However, the performance of a generator on load and under fault conditions can be examined conveniently (and, for many purposes, adequately) by combining the e.m.f.s considered to be produced by I_f alone and I alone, taken separately. Neglecting saturation, the machine can be represented by an equivalent circuit of constant reactances, and with e.m.f.s proportional to their respective currents. Usually the effect of stator-winding resistance can be neglected. Further, by assuming the airgap flux distribution to be sinusoidal, phasor diagrams can be employed. Finally, for a cylindrical-rotor machine the uniform gap length makes it permissible to assume that the d and q axes have equal reluctances. In practice, X_q is usually $0.85X_d$ to $0.95X_d$.

Adopting the conventions of IEC Publication 34–10, the basis is generator action, with power positive when it flows from generator to load. An induced e.m.f. is $e = -d\Psi/dt$, where Ψ is the linkage between flux and stator winding. This means that the e.m.f. phasor lags the flux (or linkage) phasor by 90° electrical. Then, since the flux linking a stator phase winding is in time phase with the current, the phasor diagram in *Figure 28.11* applies for a generator on inductive load, and *Figure 28.12* shows the corresponding equivalent circuit. Here Ψ_f is the stator linkage produced by the field current I_f alone; it would induce on open circuit the e.m.f. E_f. The load current I produces the linkage Ψ_a (the armature-reaction effect) reducing E_f to the airgap e.m.f. E_g. Stator leakage flux produces the linkage Ψ_l and the e.m.f. E_l, and the total induced e.m.f. is E. Subtraction of the volt drop IR_a gives the terminal voltage V. With the convention

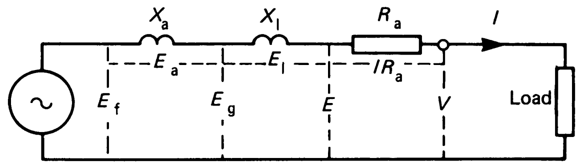

Figure 28.12 Equivalent circuit for a cylindrical-rotor generator

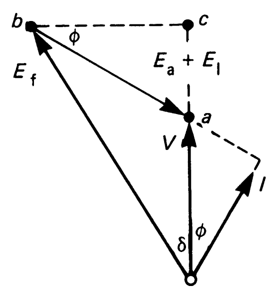

Figure 28.13 Simplified phasor diagram

that the inductive voltage drop leads the current by 90°; the two equivalent equations in phasor terms are

$$V = E_r + E_a - E_l - IR_a \quad \text{or} \quad V = E_f - I(X_a + X_l) - IR_a$$

If, as is usually permissible, R_a is neglected, then V and E coincide, simplifying the diagram to that in *Figure 28.13*. Here ab on a voltage scale represents IX_d. If the scale is divided by X_d then ab represents I. The angle abc is ϕ, the power-factor angle; bc represents the active-power component of I; but bc is also $E_f \sin\delta$, and hence the power per phase is $(VE_f/X_d)\sin\delta$.

At no load, V and E_f coincide. Hence δ is the *power* (or *load*) angle, i.e. the angle by which the rotor must be driven forward relative to the resultant flux (i.e. forward from its no-load position) to deliver active power. If V and E_f are fixed, then the active-power output P is proportional to sin δ, reaching a maximum for $\delta = 90°$.

For a *salient-pole* machine, account must be taken of the differing d and q axis reluctances. The q axis component current I_q has a lower flux-producing effect than in a cylindrical-rotor machine, i.e. X_q is smaller than X_d. *Figure 28.14* shows the phasor diagram for an output at lagging power factor, with Ψ_1 regarded as part of Ψ_a and E_1 as part of IX_d. The position of the q axis and the length $0e = E_f$ are found by dividing ab at c such that $ab/ac = X_d/X_q$. The voltage triangle abd is similar to the current triangle I, I_d, I_q, each side being the appropriate current multiplied by X_d. As $ac/ab = de/db = X_q/X_d$, it follows that $de = I_q X_q$, whence

$$0e = V + I_d X_d + I_q X_q = E_f$$

The triangle ade is similar to the flux linkage triangle $\Psi_{ad}\,\Psi_{aq}\,\Psi_q$.

Again δ is the load angle. It is less than that for a cylindrical-rotor machine of the same X_d delivering the same active power at the same voltage and excitation. From the geometry of the phasor diagram it can be shown that the active power output is

$$P = V E_f \frac{1}{X_d} \sin\delta + V^2 \frac{X_d - X_q}{2 X_d X_q} \sin 2\delta \qquad (28.38)$$

The second term is the power that is available with zero field excitation ($E_f = 0$) and which is developed as a reluctance torque and power that depend on the different axis reluctances X_d and X_q. *Figure 28.15* shows power–angle curves for a salient-pole machine with typical values of X_d and X_q and for different excitation levels. In a cylindrical-rotor machine X_q is approximately 0.9 X_d, so the reluctance torque ($E_f = 0$) is small, and the power–angle curve is not far from sinusoidal.

28.10 Synchronising

Almost all a.c. generators operate in parallel with others. This raises the problem of switching a machine safely into service ('synchronising') and ensuring that it subsequently remains in synchronism. It is here assumed that a generator is to be connected to a system large enough to fix its voltage and frequency regardless of changes in load and excitation on an individual generator.

28.10.1 Synchronising procedure

The following conditions must be satisfied by the incoming machine with respect to the network bus-bars: (I) the speed must be such that its frequency is close to that of the bus-bars (preferably about 0.2% high); (2) its r.m.s. voltage should equal the bus-bar voltage within ±5% and

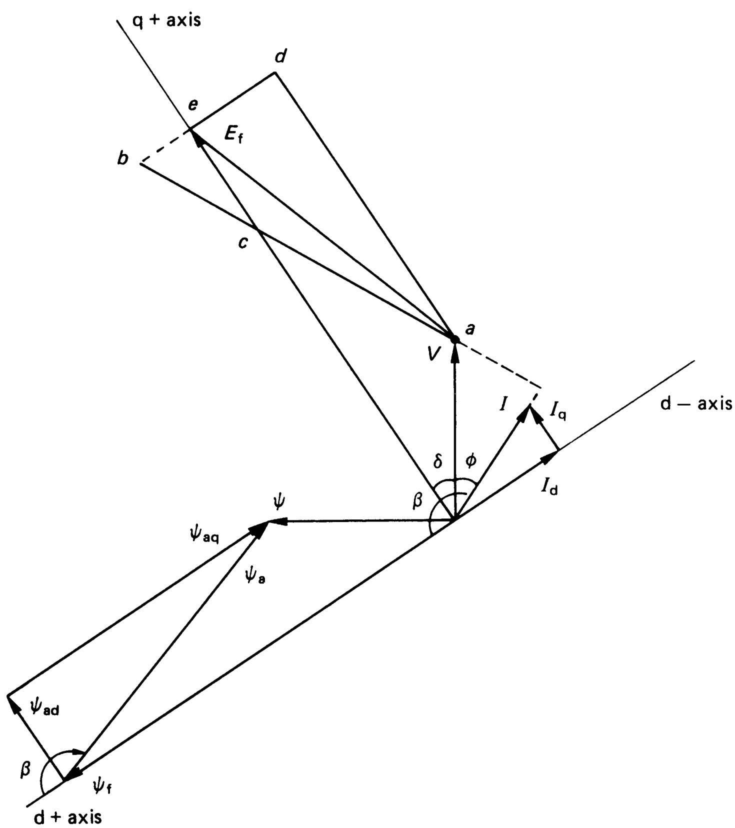

Figure 28.14 Phasor diagram for a salient-pole generator on load

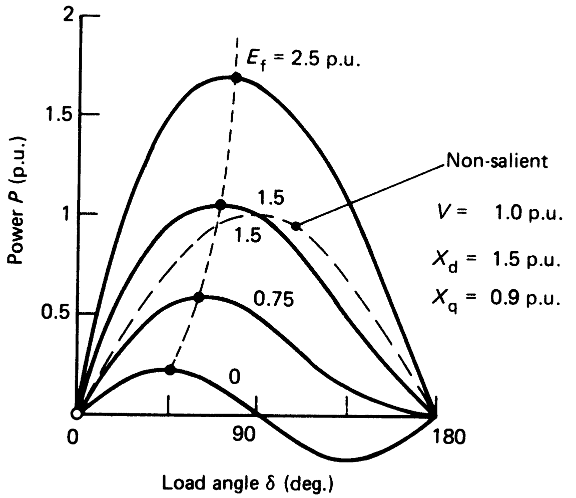

Figure 28.15 Power–angle relationships for a salient-pole generator on load

(3) the machine and bus-bar voltages must be momentarily in phase, or within ±5° of phase coincidence.

In manual synchronising, condition (3) involves the use of a synchroscope to indicate to the operator the relative phase positions. Allowance must be made by the operator for the 0.2–0.4 s time-lag between initiation of switch closure and the actual closure of the switch contacts.

Alternatively, the process may be carried out by automatic means which monitor the conditions and initiate switch closure at the proper instant.

28.10.2 Synchronising power and torque

If a generator running in parallel with others is disturbed from its steady state, for example by a small change in system voltage, the electromagnetic torque no longer balances the driving torque (presumed constant), and the rotor swings from load angle δ_1 to a new angle δ_2 at which the balance is restored. The equal-area graphical criterion for stability is useful to give a picture of the oscillation process with a single machine,[172,173] but is inadequate for accurate calculation. In *Figure 28.16* the initial operating point a moves to b as the terminal voltage drops from V_1 to V_2, reducing the electrical output. The rotor oscillates between δ_1 and δ_3 before settling at δ_2, again delivering output P_{e1} equal to the mechanical power input. Areas abc and cde represent the energy interchanged between rotor kinetic energy and electrical energy.

If P_{e1} is too close to the peak of the V_2 power curve, area cdf is less than abc, so the rotor can never be slowed to synchronous speed on its forward swing. It rises to a mean speed a little above synchronous, say up to 1%, delivering rather less than the initial load as an induction generator. As the rotor poles slip past the stator m.m.f. wave the speed fluctuates, and there are large swings in current, power, reactive and voltage. An e.m.f. at slip frequency is induced into the field winding, and may reach four or five times the excitation voltage needed at rated load. This forms one of the design criteria for the diodes or thyristors of the excitation system. In a turbine-generator, increased leakage flux in the end regions causes rapid heating of the core ends. E.m.f.s induced between the laminations have in a few machines initiated local damage to the core-plate insulation, and serious core faults have developed in subsequent service. Many machines have pole-slip protection to trip the machine if synchronous running is not restored very quickly.

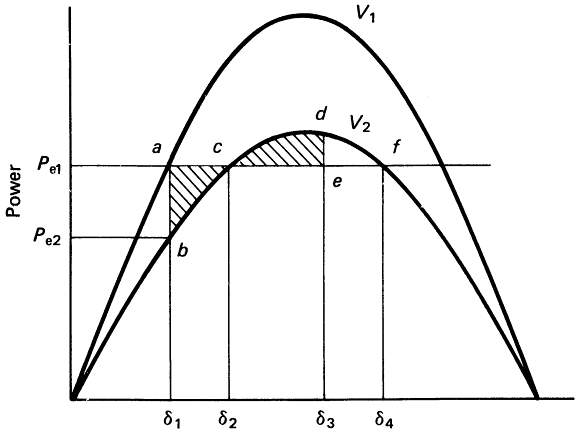

Figure 28.16 Equal-area criterion

If $\Delta\delta$ is a small deviation from δ_2, the accelerating, or retarding, torque ΔT is approximately $\Delta\delta$(slope of the T–δ curve at δ_2) $= \Delta\delta T_s$ where T_s is the synchronising torque coefficient. The corresponding synchronising power $\Delta P = \omega_m \Delta T = \Delta\delta P_s$ where ω_m is the synchronous speed (in mechanical rad/s $= 2\pi$ rev/s) and P_s is the synchronising coefficient.

If the rotor swing were very slow the appropriate P–δ curve would be the steady-state curve such as in *Figure 28.15* and

$$P_s = \frac{dP}{d\delta} = \frac{VE_f}{X_d}\cos\delta + \frac{V^2(X_d - X_q)}{X_d X_q}\cos 2\delta \qquad (28.39)$$

where P_s, the total for three phases, is in watts/electrical radian if V and E_f are line voltages (not phase) and reactances are in ohms/phase. T_s is then in newton-metres/electrical radian. Usually per unit (p.u.) values are more convenient, using rated apparent power and phase voltage and current as bases. Then P_s is in p.u. power/electrical radian, and T_s p.u. is numerically the same.

28.10.3 Rotor oscillation

The synchronising torque acting on the inertia of the coupled rotors constitutes an oscillatory system with a natural frequency

$$f_n = \frac{1}{2\pi}\sqrt{\frac{T_{spu}\omega_s}{2H}}\ \text{(hertz)} \qquad (28.40)$$

where $\omega_s = 2\pi f_s = 314$ for $f_s = 50$ Hz and 377 for 60 Hz.

This formula ignores the effect on the frequency of the damping torques and, more significantly, the additional synchronising torques developed by currents induced in damper cages and solid iron.

In practice too, the natural frequency for almost any machine and system lies in the range 1–3 Hz, within which the time of a half-cycle of oscillation is comparable with,

Table 28.5 Comparison of steady and transient states

Calculation Basis	E_f	E'	δ (°)	δ' (°)	P_m	P'_m	P_s	P'_s	f_n	f'_n
Steady state	1.58	—	66	—	1.05	—	0.43	—	0.46	—
Transient	—	0.97	—	14.3	—	3.38	—	3.76	—	1.3

or less than, the transient time constant. The field flux linkages do not have time to change significantly, and the effective reactances are the transient, not the synchronous, ones.

With this assumption a round-rotor machine can be represented simply as a constant e.m.f. E' behind the transient reactance X'_d. E' is determined by conditions before oscillation starts, and is assumed not to change as δ', the angle between E' and the terminal voltage V, changes.

Then the peak of the transient power curve is

$$P'_{max} = \frac{VE'}{X'_d} \tag{28.41}$$

and at angle δ',

$$P' = \frac{VE'}{X'_d} \sin\delta' \tag{28.42}$$

The synchronising power coefficient

$$P'_s = \frac{VE'}{X'_d} \cos\delta' \tag{28.43}$$

in p.u./electrical radian.

Calculations from the steady-state and transient representations are compared in *Table 28.5* for a generator set with $X_d = 1.5$, $X'_d = 0.25$ and $H = 8$ s, with an initial load of 1.0 p.u. at 0.95 p.f. leading.

In many investigations this approximate model may be good enough, at least as a starting point. For large swings, and to get more accurate results, an appropriate equivalent circuit representation is needed. The damping and induction torques can be included, as also can the influence of fast-acting excitation systems. Computer solution of the resulting equations is necessary. The effect of series reactance X_e between the machine and the fixed voltage bus is to reduce the synchronising torque and lower f_n. X_e is allowed for by adding it to the machine reactances.

28.11 Operating charts

Operating charts based on *Figure 28.11* or *28.14* define the operating limits imposed by the prime mover, excitation, load and stability. Saturation is ignored and the reactances are deemed constant.

28.11.1 Cylindrical-rotor generator

In *Figure 28.17*, 0ab is the synchronous reactance triangle. The fixed phase voltage V is represented by 0a to a scale of say v V/cm. Point b represents rated load, where ab is the rated stator current to a scale of v/X_d A/cm, at the power factor cos dab. ad and ag are the active and reactive components of ab.

0b represents E_f, the phase e.m.f. behind X_d. The corresponding value of I_f can be read off the airgap line of the open-circuit characteristic shown in *Figure 28.10*; the value of I_f is not seriously inaccurate at leading power factors where magnetic saturation is low.

To a scale of $(3Vv)/X_d$ V-A/cm, ab represents rated apparent power, ad the active power, and ag the reactive power.

a0 represents, at v/X_d A/cm, the magnetising current V/X_d drawn from the system at no load if I_f is reduced to zero, and also the corresponding vars V^2/X_d at the scale $(3Vv)/X_d$ V-A/cm.

The P and Q axes are usually marked in megawatts and reactive megavolt-amperes, respectively, or in p.u. terms where, if $V = 1$ p.u., ab = rated apparent power = 1 p.u., ad = active power, cos ϕ p.u. ag = reactive volt-amperes, sin ϕ p.u. and a0 = rated MVA/X_d = $(1/X_d)$ p.u.

Operation must be so controlled that the operating point is within the boundary set by (i) an arc of centre a and radius ab representing rated stator current, (ii) an arc bh of centre 0 representing rated field current, and (iii) the line bdb$_1$ representing the rated active power output of the prime mover.

The line 0m, corresponding to a load angle $\delta = 90°$, shows the theoretical maximum power (since the output falls for $\delta = 90°$), while 0b$_1$ is the lowest field current for which rated power can be delivered, the corresponding stator current then being ab$_1$.

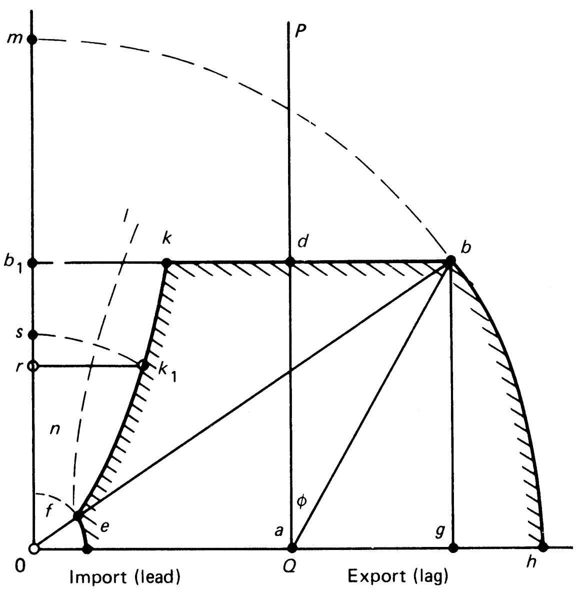

Figure 28.17 Operating chart for a cylindrical-rotor generator

Instability at all loads occurs when the generator (being underexcited) absorbs the reactive power 0a of value $1/X_d$ p.u. The line 0a to the current scale is the zero-power-factor component of stator current; considered as delivered to the power system, 0a leads the terminal voltage V.

Stable operation in practice is not possible up to the theoretical steady-state limit line 0m. It is usual to construct a practical stability line such as 0fk on which, from each point such as k_1, operation for a given excitation $0s = 0k_1$ is not permissible in the region rk_1. The load increment rs may be a fixed fraction of rated load or may change progressively between no load and full load. Often, a minimum acceptable field current is defined to avoid pole-slip at low load when a system voltage drop occurs (for the synchronising torque depends on VI_f). The minimum excitation is typically 20% of that needed on no load giving the limiting arc ef.

With a constant active-power output, the stator leakage flux in end-winding spaces increases as the power factor becomes more leading. This increases the loss, and the temperature in the end core packets and clamping plates will rise. Hence an end-heating limit line ln may be specified. Either the practical stability or the end-of-core temperature may set the limit on the reactive power that can be absorbed.

The operating chart can form the dial of a P–Q meter having a pointer moving parallel to each axis. Where the pointers cross indicates the load point, and the margins between the output and the several limits are readily observed.

The effect of reactance X_e (of, say, a transformer or power line) between the generator and the fixed-voltage bus-bar is allowed for by adding X_e to X_d and X_q. Then for a cylindrical rotor machine the steady-state stability limit is reached where $\delta_B = 90°$: the reactive power Q_L is $V_B^2/(X_d + X_e)$ and the maximum real power P_L is $V_B E_f/(X_d + X_e)$. At any stable load condition

$$P = \frac{V_B E_f \sin \delta_B}{X_d + X_e} \tag{28.44}$$

and

$$Q = \frac{V_B(E_f \cos \delta_B - V_B)}{X_d + X_e} \tag{28.45}$$

The phasor diagram in *Figure 28.18(b)* shows that the presence of X_e requires the generator to operate over a range of terminal voltage. Generators are usually designed to deliver rated megavolt-amperes at rated power factor over a voltage range of ±5%, and a frequency range of ±2% or so. As V_T and f move away from the rated (1 p.u.) values, the rotor or stator temperature rises will increase. If V_T or f goes outside the design range, load may have to be reduced to avoid unacceptably high temperatures. See, for example, IEC 34–3 (Section 28.21).

To provide a voltage range of, say, 10% rather than 5% adds significantly to the size and cost of a large machine. In service the p.u. reactances increase as the voltage decreases, and this reduces the stability margin. Hence many generator transformers have on-load tap-changers, to reduce the range of generator voltage needed.

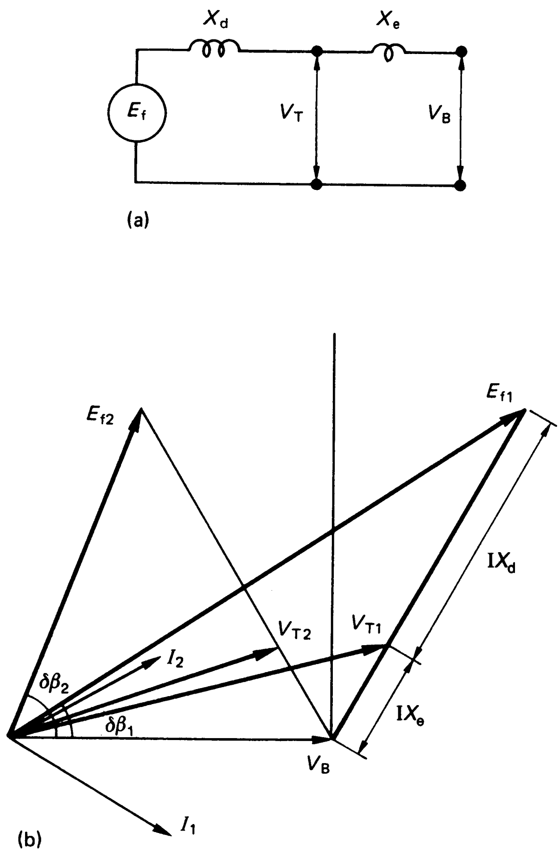

Figure 28.18 Effect of system reactance

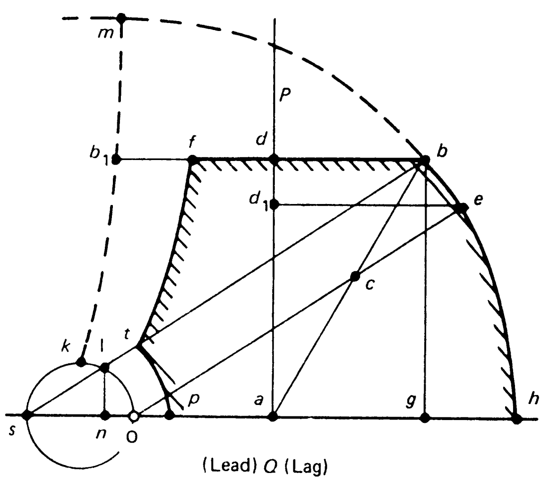

Figure 28.19 Operating chart for a salient-pole generator

28.11.2 Salient-pole generator

From the phasor diagram the P–Q chart for operation at fixed V_T of 1 p.u. can be drawn as in *Figure 28.19*; ab, ad and ag are the stator current or volt-ampere quantities, as before; $ac/ab = X_q/X_d$; $a0 = 1/X_d$; $as = 1/X_q$; be $\perp$ oe; slb||oe; and lb = oe. Hence ols = 90°; and the semi-circle constructed on diameter 0s represents a zero-excitation boundary. Angle bs0 = c0a = the load angle δ. At any load, E_f and I_f are represented by a length such as lb, on the line through s. (For any constant E_f and I_f, the locus of b is not quite a circular arc, but this becomes evident only with low excitations near the stability limit.)

ad_1 is the power contributed by E_f, and $d_1d = ln$ is the reluctance power derived from the difference between X_d and X_q.

The theoretical steady-state stability limit is the line skb_1m. One method of finding the line is to use the expression $dP/d\delta = VE_f \cos \delta/X_d + V^2(X_d - X_q) \cos 2\delta/X_dX_q$ to find consistent values of E_f and δ that make $dP/d\delta = 0$ (or see reference 8, article 6.5). A practical limit such as ptf can be constructed as for the round rotor generator, but with modern excitation controllers operation even beyond the theoretical limit, or with negative excitation, may be accepted, at least as a temporary condition.

With external reactance X_e, the intercepts of the zero-excitation circle lie at $a0 = V/(X_d + X_e)$, and $as = V/(X_q + X_c)$. For a given E_f the peak power at the stability limit is also reduced.

28.12 On-load excitation

Use of the constant unsaturated value of X_d leads to values of E_f higher than those that occur for a practical machine. Thus a cylindrical-rotor generator with $X_{du} = 2$ p.u. and carrying rated load at p.f. 0.85 lagging would have $E_f =$ 2.67 p.u. while a practical machine would saturate at around 1.5 p.u. If I_t were read from the airgap line for $E_f = 2.67$ p.u. it would be about 15% low. At leading power factor, for which saturation levels are low, the error would be small. However, a more accurate estimate of the field current I_f is needed in the design of the excitation system and its cooling, and to determine the open-circuit e.m.f. that would be reached if the automatic voltage regulator failed to limit the excitation on sudden load rejection.

Flux distributions can now be calculated in considerable detail using computer programs that contain information on the geometry of the magnetic circuit, m.m.f.s of stator and rotor currents and values of iron permeabilities appropriate to the local flux densities. Hence, open-circuit curves and excitation on load can be calculated without much labour from design data once a program has been proved. However, methods based on phasor diagrams and adjusted reactances, making separate allowance for saturation, are still of value if a suitable program or the detailed design information, is not available. They are also needed when calculating excitation from test results on built machines.

Such methods add the rotor m.m.f. phasor needed to generate the no-load voltage to that needed to balance armature reaction. They differ in the choice of the voltage and in the way allowance is made for the effects of saturation.

The open-circuit short-circuit and zero-power-factor characteristics (o.c.c., s.c.c. and z.p.f.c.) are required, either by test or from design calculations. All the methods should give the full-load excitation to within ±5%, or closer at leading power factor. We consider three methods here.

28.12.1 M.m.f. phasor diagram (*Figure 28.20*)

The e.m.f. E behind the leakage reactance X_l requires a field m.m.f. F_c, read from the calculated or tested o.c.c. F_{ar} is the armature-reaction m.m.f. in rotor terms, obtained by using the equations in Section 28.7 or (if X_l is known) by calculation from the tested s.c.c. as follows. To circulate stator current I on short circuit, excitation I_n is needed; to generate an e.m.f. to balance IX_l drop, I_{f2} is needed; hence the armature-reaction m.m.f. is $F_{af} = I_{f1} - I_{f2}$. E_f is the excitation m.m.f. required for the load current I at terminal voltage V and p.f. angle ϕ. *Figure 28.20(c)* shows the diagram for a salient-pole machine with a leading p.f. load. As in *Figure 28.14*, ab is divided at c such that $ac/ab = X_q/X_d$ to find point *f*.

28.12.2 The ANSI Potier reactance method (*Figure 28.21*)

ANSI Potier reactance method (ANSI/IEEE Publication 115, Section 4) requires the tested o.c. and z.p.f. characteristics, and is limited to machines that can be loaded for a z.p.f. test. In *Figure 28.21(a)*, A is the rated-current short-circuit point and D is the rated-current rated-voltage point: to reach D the field current exceeds the rated-load excitation level. Draw DC = A0, and draw CF parallel to the airgap line 0BH. Drop FL perpendicular to DC, and draw triangle 0BAM similar to CFDL. Then FL is the Potier voltage drop IX_p, and DL is the armature-reaction m.m.f.

The argument is that for a given stator current the armature-reaction and leakage-reactance voltage drops are constant, but the latter requires more excitation at the higher levels of saturation.

0G, 0A and FH in *Figure 28.20(b)* are excitation currents as in (*a*), with 0G for rated voltage on the *airgap* line. GH is the total excitation required.

The z.p.f. test may have to be performed at less than rated stator current; the z.p.f.c. is then closer to the o.c.c., and DL and FL are smaller. Nevertheless, the Potier reactance is still considered to be $X_p = FL/I$ up to rated value.

Given only the o.c.c. and s.c.c., and no facility for adequately loading the machine, one procedure is to measure the d axis subtransient reactance from a sudden-short-circuit test (or by the IEEE method below) and to use it as X_p.

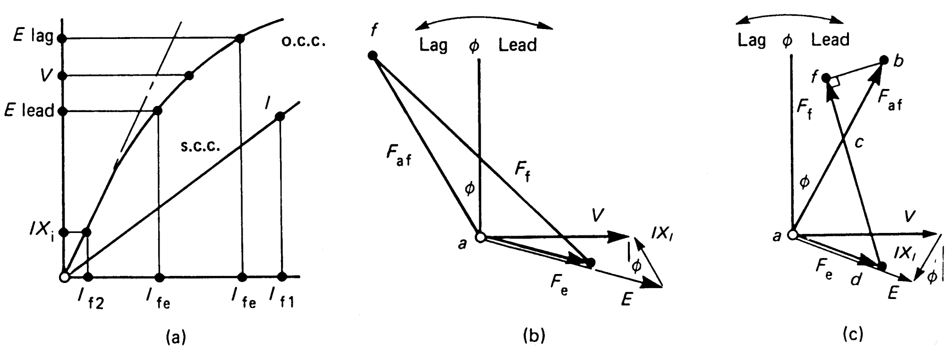

Figure 28.20 M.m.f excitation diagrams: (a) open- and short-circuit characteristics; (b) cylindrical-rotor, power factor lagging; (c) salient pole, power factor leading

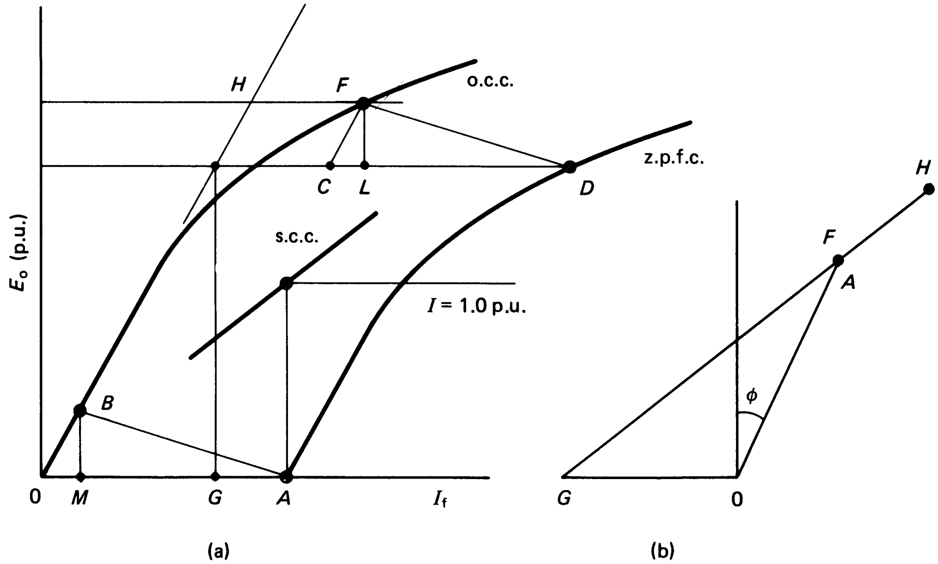

Figure 28.21 Potier reactance excitation diagram

The test method for X''_d in ANSI/IEEE 115, Clause 7.30.25, is to apply a voltage E of normal frequency to each pair of stator terminals in turn and to observe the current I with the rotor stationary. Let the three quotients of E/I be A, B and C. Then with E and I in per-unit values of rated phase voltage and current, $X''_d = (A+B+C)/6$ to an approximation. To avoid rotor overheating, the duration of the test should not exceed the maker's recommendations (e.g. 0.2 p.u. for a time sufficient to read the meters).

28.12.3 Use of design calculation (*Figure 28.22*)

Methods can be refined by allowing for the rotor pole-to-pole leakage. From a knowledge of the airgap flux, the m.m.f.s required for the gap, stator teeth and stator core are calculated. The m.m.f. for the rotor pole and body are calculated from the gap flux and pole leakage. The component phasors are added as in the diagram, where the total rotor m.m.f. per pole bc is obtained from 0a (armature-reaction), 0b (gap, teeth and core) and ac (pole and body). Saliency can be allowed for by dividing 0a at d such that $0d/0a = X_q/X_d$. Then the total rotor m.m.f. is bc′. This differs very little from bc, but the load angle δ' is more accurate.

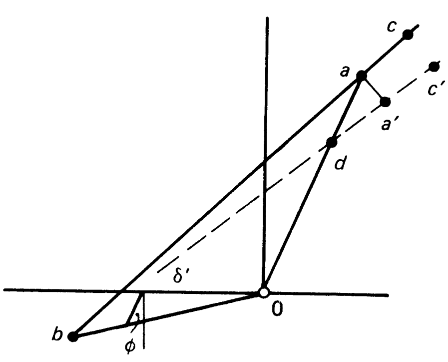

Figure 28.22 M.m.f. excitation diagram from design calculations

28.13 Sudden three-phase short circuit[6,7,11,19]

If a three-phase generator is initially excited to a phase e.m.f. E_o on open circuit, and then the three phases are suddenly short circuited together, the stator winding carries balanced three-phase currents of up to several times full-load value depending on the magnitude of E_o. These currents produce a m.m.f. that rotates synchronously with the rotor, with its axis along the main pole axis, tending to reduce the mutual flux from its initial value. Change of flux linkage in a closed circuit induces therein a current opposing the change. Hence large direct currents are induced in the rotor damper circuits and field winding.

The combined effect of the large and opposing stator and rotor currents is to produce large leakage fluxes around the stator winding, the damper circuits and the field winding while the mutual flux along the main flux axis decreases correspondingly, so that the *total* flux linkage with each winding remains momentarily unchanged. I^2R losses rapidly dissipate stored magnetic energy and the damper currents rapidly decay. Typically, the induced field current reaches its peak a period or two after the short-circuit instant and then decays relatively slowly (*Figure 28.23*).

In the stator, each phase current is asymmetric to an extent depending on how near the phase voltage was to zero at the instant of short circuit; zero instantaneous voltage produces full asymmetry. Thus each phase carries a d.c. component which, at the instant of short circuit, is equal and opposite to the instantaneous a.c. component. These d.c. components produce a stationary m.m.f. sufficient to hold the stator flux linkage momentarily unchanged, i.e. fixed relative to the stator in the position the stator flux linkage occupied at the short-circuit instant. The d.c. rapidly decays, and with it the stationary field; while it persists, however, the rotation of the rotor within it induces rotational-frequency currents in the rotor damper and field circuits. The a.c. in the field is clearly seen in *Figure 28.23*; it is the greater, the less effective the damper circuits are. With no damper at all, the induced d.c. and the zero-to-peak amplitude of the a.c. would initially be equal.

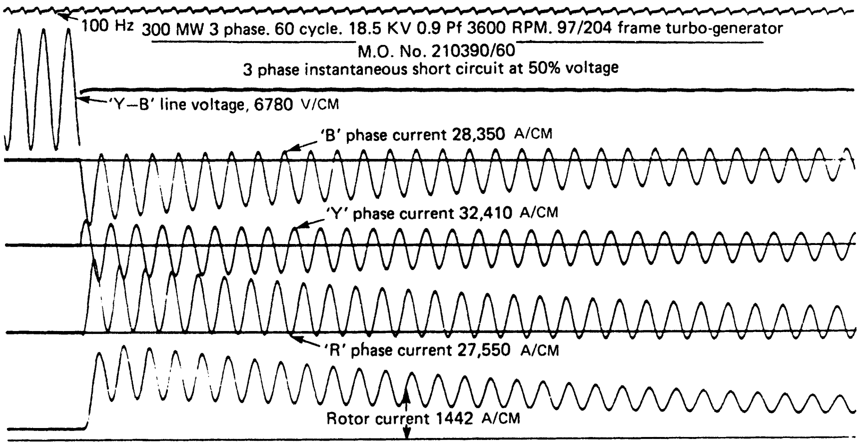

Figure 28.23 Short-circuit oscillogram

The path taken in the rotor by the stationary airgap flux has a greater permeance when the d axis coincides with the stationary flux axis than when the q axis coincides. Hence the flux fluctuates at twice fundamental frequency, and double-frequency current is induced in the stator winding. This current and the a.c. in the rotor decay as the stator d.c. component decays. The magnitude of the second-harmonic stator current depends on the difference between X''_d and X''_q; it is small in turbogenerators and in salient-pole machines with good interconnected damper windings.

In summary, the a.c. components in the stator give rise to the d.c. components in the rotor circuits; after the subtransient period, stator a.c. and field d.c. decay together with the transient short-circuit time constant, T'_d. The stator d.c. components produce the rotor a.c., and these decay together with the armature short-circuit time constant T_a.

Direct-axis reactances and time constants are derived from the oscillograms of a three-phase short circuit as follows (see *Figure 28.24* and Section 28.22: BS EN 60034-4:1995 and ANSI/IEEE Std 115-1995). The oscillograms should record for not less than 0.5 s; I_d can be measured by instruments or by taking a second oscillogram after the steady state has been reached. Speed and field current should be constant throughout. E_o *is* the open circuit phase e.m.f. corresponding to the rotor excitation.

The modern testing technique includes also digitally recording voltages and currents, and using computer programs to analyse the results and to present values of reactances and time constants. The principles are the same as for the analysis of the oscillograms described below.

(1) Draw the envelopes abc and a′b′c′ of one phase-current oscillogram. Then aa′ is the double-amplitude of the prospective current at the instant $t = 0$ of short circuit. (The first current peak is slightly less than ½ aa′ because of the rapid subtransient decrement.) Taking aa′ *as scaled in per-unit terms*, the r.m.s. current is $I''_d = \mathrm{aa}'/(2\sqrt{2})$ and

$$X''_d = E_o/I''_d$$

(2) Project the envelopes in the transient region, cb and c′b′ back respectively to d and d′, ignoring the initial rapid subtransient decrement (this cannot be done with great accuracy). Then $I'_d = \mathrm{dd}'/(2\sqrt{2})$ and $X'_d = E_o/I'_d$.

(3) Repeat steps (l) and (2) for the other two phases and derive the mean values of X''_d and X'_d.

(4) For a closer estimate, the equation relating the r.m.s. value of the a.c. short-circuit current I_t to time may be used:

$$I_t = I_d + (I'_d - I_d)\exp(-t/T'_d) + (I''_d - I'_d)\exp(-t/T''_d) \quad (28.46)$$

where $I_d = \mathrm{ee}'/(2\sqrt{2})$ is the sustained steady-state short-circuit current, and I'_d and I''_d are the transient and subtransient r.m s. currents respectively, corresponding to dd′ and aa′, respectively, at $t = 0$.

(5) Measure (e.g. in centimetres) the double-amplitude between the envelopes at each current peak and subtract ee′ from each. Plot the results as ordinates on a logarithmic scale, to a linear base of time. This gives the curve abc in *Figure 28.24*(*b*).

(6) Project cb by a straight line to d at $t = 0$. Then

$$[\mathrm{d\ (cm)}/(2\sqrt{2})] \times \text{current scale of oscillogram} + I_d = I'_d$$

whence

$$X'_d = E_d/I'_d$$

[If bc is not linear, the decay is not exponential and I'_d is not a constant. I'_d and T'_d can be estimated from a straight line drawn through chosen points on the curve bc where the transient current components are b and 0.368b (see IEC 34–4: 1985).]

(7) The subtransient r.m.s. current at $t = 0$ is $I''_d = I'_d +$ the rapidly decaying component represented by da. Thus

$$I''_d = I'_d + \frac{\text{Intercept da}}{2\sqrt{2}}\ \mathrm{(cm)} \times \text{Current scale}$$

and

$$X''_d = E_o/I''_d$$

The intercepts between ab and db are drawn to extended current and time scales in the lower part of *Figure 28.24*(*b*). Point f on the ordinate scale corresponds to da.

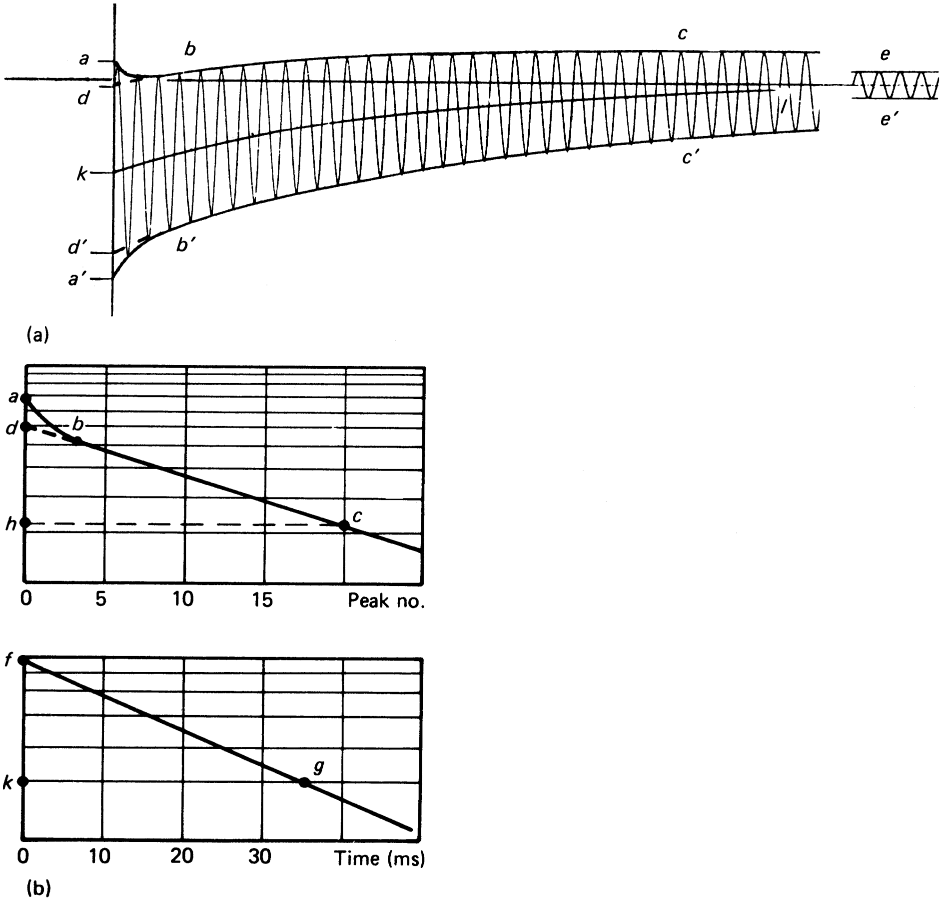

Figure 28.24 Analysis of a short-circuit current oscillogram: (a) current envelope; (b) logarithmic plot

(8) Time constants are obtained from the slopes of the current–time plots. At c on line dc the transient component at time t is represented by h on the logarithmic ordinate (centimetre) scale and by hc on the time scale. It is related to I'_d at $t = 0$ by

$$I_t - I_d = (I'_d - I_d)\exp(-t/T'_d) \tag{28.47}$$

i.e. $h = d\exp(-t/T'_d)$, consequently

$$T'_d = (hc)/\ln(d/h) \tag{28.48}$$

where hc is in seconds and d and h are in centimetres. T''_d is obtained similarly from the extended-scale plot in *Figure 28.24*(*b*). The $T''_d = kg/(\ln f - \ln k)$.

(9) The procedure (5)–(8) is repeated for the other two phases and the mean values are obtained.

(10) The reactance values decrease with increasing short-circuit current (and therefore with increasing open-circuit voltage E_o). Rated-current values are obtained when at $t = 0$ the transient current I'_d is equal to rated current. A range of short-circuit tests spanning the expected X'_d will give a plot of reactance to a base of transient current I'_d from which the appropriate value can be found. A rated-voltage value, if required, is obtained by testing at 1 p.u. voltage for a small machine without a transformer, but the electromechanical forces on the stator endwindings would be excessive in a large generator. Tests up to 0.7 p.u. voltage simulate a fault on the h.v. side of the transformer in a generator–transformer unit and are more relevant to the service conditions of a large machine.

(11) The armature short-circuit time-constant T_a is determined from the decay of the d.c. components of stator current or from the decay of the a.c. component of induced field current. The latter method is simple: it requires only a log-linear plot of the a.c. component to a base of time, similar to the plot in *Figure 28.24*(*b*). The stator d.c. component is represented by the median line kl of the current envelopes in *Figure 28.24*(*a*). However, if there are significant even-order harmonics then the median is displaced and a waveform analysis is necessary to find the harmonic effect. This will occur in a salient pole machine that has no effective damper winding, and so is not common.

28.14 Excitation systems

A synchronous machine requires an excitation system to provide the field current for magnetising the machine to the desired voltage and, when it is running in parallel with others, determining the lagging reactive power generated or received. It is customary for each generator to have its own self-contained excitation system, which provides the power required to supply the I^2R loss in the field circuit. This varies between about 10 kW/MVA for small machines and 5 kW/MVA for very large units.

Excitation voltage and currents are chosen: (i) to give field winding conductors that are mechanically robust in small machines and not too massive in large ones, (ii) to suit the ratings of available diodes or thyristors, and (iii) to give convenient designs of exciter, and also of slip-rings where these are used. Values range from a few score amperes and volts on very small machines up to say 8 kA at 600 V on the largest turbogenerators. At no load or with leading power factor, control of the exciting current is needed down to about one-third of the value for rated load.

The excitation system must respond to applied signals quickly enough to have the desired effect on the generator flux. Its duties can be broadly classified as:

(1) to control the generator voltage accurately as slow changes of power and reactive loading occur;
(2) to limit the fluctuations of voltage when loads are suddenly imposed or removed;
(3) to maintain steady-state stability; and
(4) to maintain transient stability.

(See references 168, 172 and 173.) These duties require different characteristics of the excitation system: these must be reconciled to provide proper control.

The performance of an excitation system is represented by its response ratio, or its response time and ceiling voltage. BS EN 60034-1:1998 gives definitions, and ANSI/IEEE Standard 421.1 gives definitions and methods of test for these and for other characteristics. In *Figure 28.25*, abdm is the voltage–time curve obtained, starting from the voltage V_e needed on the generator field winding at rated load, when the control is suddenly changed to cause ceiling voltage V_c to be reached as quickly as possible. Definitions are, where area abdf equals area agf:

Characteristic	*ANSI/ IEEE 421.1*	*BS EN 60034-1:1998*
Response time (s)	t_2	—
Response ratio measured over 0.5 s	$\dfrac{2\text{gf in volts}}{V_e}$	$\dfrac{2\text{gf in volts}}{V_e}$
Initial response/second	—	$\dfrac{1}{V_e}\left(\begin{array}{l}\text{Slope of } V-t \text{ curve}\\ \text{at } t=0 \text{ } V/\text{s}\end{array}\right)^*$
High initial response system	A curve such as ak	—

*The initial response is $(V_c - V_e)/(t_1 V_e)$ per second if curve abd is exponential.

The measurements are conveniently made with the exciter on open circuit, but for analysing the generator behaviour values are needed with the exciter supplying the rotor winding. Where such testing is impractical, the on-load values must be calculated using known parameters of the machine(s).

Any system with a response time of 0.1 s or less is called a 'high initial response system'. A thyristor system[166,171] supplied from a transformer (or from an exciter machine that runs continuously at ceiling voltage) is inherently a high initial response system. A brushless exciter system[165,167] can be given high initial response by forcing the exciter field current with a pilot exciter voltage that may be 5–10 times that needed for rated generator output. The exciter field must be designed to have a short time constant; the whole magnetic circuit must be laminated, and damper windings, both deliberate and incidental, must be avoided. For example, clamping bolts and plates should not form closed loops linking flux. The exciter output voltage can be forced to a ceiling value of, say, 2–3 times V_e in less than 0.1 s. The output current rises more slowly, depending on the effective time constant of the generator field circuit. The controller may limit the exciter field current to say 2½ times the rated load value. In service such forcing would usually occur only for a few periods of up to about 0.5 s each as the rotor swings and returns to synchronism after a system fault.

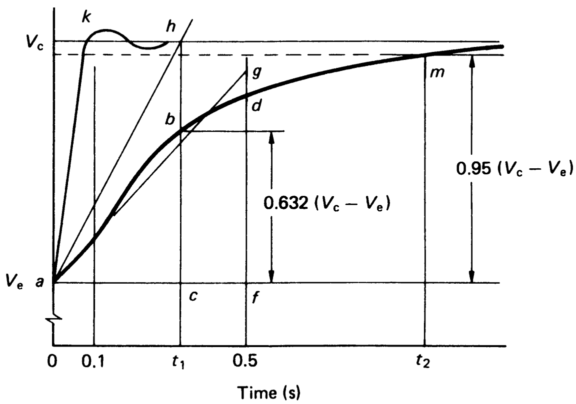

Figure 28.25 Excitation response definitions

A heavily forced main exciter with V_c about $2V_e$ may provide the required *initial* response more cheaply than would a bigger exciter with yet higher V_c but a longer time constant.

Whatever the performance of the excitation system, the change in generator flux is delayed by eddy currents induced in its field winding and any available damper circuits. It is not practicable to constrain the design to avoid these: for mechanical reasons a turbogenerator needs a solid forged rotor (except very small ones), whilst a hydrogenerator with laminated poles needs pole-face dampers. Hence the natural generator time constants have to be accepted, and the excitation system designed to suit them. The extra cost of excitation systems to achieve more and more rapid response may become unjustifiably great in relation to the control actually achieved on the generator.

For the duties (1) to (4) listed above, the characteristics of the excitation system need to be as follows.

(1) To hold the generator voltage within specified limits, which may be between ±3%, and ±0.5% of the set voltage, the excitation system needs a high d.c. gain, but a moderate ceiling voltage is enough to supply the small and slow changes of excitation needed.
(2) To limit the fluctuation of voltage when load is suddenly applied or removed, a large, rapid and well damped response is needed. However a high initial response system, as defined above is often not necessary. For example, starting a large motor demands first a large reactive output from the generator, then increasing real power as the motor runs up. On a small generator or generator group, to avoid the voltage dip being too great and too prolonged, the generator needs a low X'_d and the excitation must be kept high

for perhaps several seconds. A high ceiling voltage and a response time of say 0.2 s is more useful than a high initial response system with a lower ceiling.

(3) Following a fault and its clearance on a high voltage power system, or a serious sudden loss of generation, a large and rapid response is needed to maintain transient stability. i.e. to restore the voltage and synchronising power flow to hold the several generators in step. Whether or not a high initial response system is essential depends on the particular circumstances, including the inertias of the generator sets and the post-fault reactances of the system. Very low frequency swinging, at 0.5 Hz or less, can occur between generating areas, requiring higher than rated excitation currents to be maintained for several seconds.

(4) Steady-state stability can be improved, in the sense that the generator can run safely close to, or even beyond, the fixed excitation stability limit, if the controller has no dead band, and is designed to have the desired speed of response without introducing a phase shift that causes positive feedback. Such feedback encourages oscillations of rotor angle, etc., which can become intolerable. This is more likely with high-reactance power lines. Long lines with series capacitance inserted to compensate for the line inductance introduce the possible hazard of subsynchronous resonance.[128–133] A minor disturbance, for example normal switching of lines, can cause transient current to flow at the line natural frequency f_n (hertz), often in the range 20–40 Hz. Torque is developed on a generator rotor at system frequency f_s–f_n (hertz). If this is close to a natural frequency of torsional oscillation of some part of a turbogenerator shaft system, the oscillation may increase, sustaining the subsynchronous current. Fatigue damage has occurred on a few turbogenerator sets in this way.

Where stability problems are judged likely, the excitation controller is supplemented by a power system stabiliser. This acts in response to input signals such as voltage, power, rotor angle, or derivatives of these. They cause the controller to adjust the excitation so that torque is developed in the correct sense to damp the oscillations.

Because of its inherently fast response, and because of the mechanical advantages noted in Section 28.17 the self-excited thyristor excitation system is almost always used for hydrogenerators on long lines.

28.14.1 D.c. exciters

These have been superseded by brushless or static thyristor a.c. systems. The exciter was a d.c. generator coupled to the shaft of the synchronous machine, feeding its output to the main field through slip-rings. For high-speed generators of more than about 50 MW, the exciter had to be driven at a lower speed (typically 1000 or 750 rev/min) through gears, or separately by a motor, in order to avoid difficulties of construction and of commutation. On very-low-speed hydrogenerators a directly coupled exciter would be excessively large, so a higher-speed exciter, driven by a motor or perhaps a small water turbine, was used.

For small ratings, the exciter was shunt excited; however, most were separately excited from a directly coupled shunt-excited pilot exciter. Control of the generator excitation was provided by controlling the field current of the main exciter.

28.14.2 A.c. exciters with static rectifiers

Satisfactory service experience with brushless and static thyristor systems has made these early (diode) systems obsolescent. Many remain in service, but many have been replaced by modern equipment. The advent of solid-state rectifiers made it possible to avoid commutators by using an a.c. exciter, directly coupled to the generator and feeding its output via floor-mounted rectifiers to the generator held winding through slip-rings. The exciter can operate at any economically convenient frequency, usually between 50 and 250 Hz, and the system is suitable for generators up to the largest ratings.

The diode cubicles may be cooled by natural convection or forced air flow. Alternatively, especially for large ratings. the diodes may be mounted on water-cooled bus-bars; this greatly reduces the size of the cubicles so that they can, if desired, be mounted on the sides of the main exciter frame, thus avoiding the need for long runs of a.c. and d.c. bus-bars or cables.

The main exciter field is supplied by an a.c. pilot exciter, often a permanent-magnet generator. The excitation of the main generator is controlled by controlling the main exciter field current via the automatic voltage regulator.

The main exciter is usually three-phase, and the diodes are connected in the six-arm bridge circuit, usually with a fuse in series with each diode to interrupt the fault current should a diode break down (which almost always causes it to conduct in both directions, i.e. to act as a short circuit). Diodes are available with current ratings up to 1000 A mean d.c., and peak inverse voltage up to 5 kV (but not both together in one diode). For other than small ratings, each bridge arm has several diodes in parallel; all the diodes are fused and have sufficient current margin to enable the bridge to carry full-load excitation continuously with one or more of the diodes failed and isolated by their fuses. Hence the generator can remain in service until maintenance can be carried out conveniently. Some installations on large generators had a.c. and d.c. isolators to allow parts of the bridge to be worked on without taking the generator out of service.

The diodes must be able to withstand induced transient currents and voltages resulting from system short circuits, asynchronous running, pole-slip and faulty synchronising, as well as from faults in the excitation system itself. Their continuous duty rating must leave some margin for imperfect sharing between parallel paths and for the possible loss of one or more paths, as noted above.

The fuse characteristic is co-ordinated with that of the diode so that fuses should not blow unless a diode fails or there is a short circuit on the d.c. output. The fuses must clear the fault current under the most onerous condition, which is usually that of a failure with the exciter at ceiling voltage. Fuse blowing is easily indicated by a microswitch operated by a striker pin that is ejected from an indicator fuse in parallel with the main fuse.

28.14.3 Brushless excitation

The a.c. exciter has a rotating armature with three or more phases and a stationary field system. It usually is designed for a frequency of 0–5 times the power system frequency. The pilot exciter, when there is one, is usually a permanent magnet generator operating at around 6–8 times system frequency. The diodes and fuses are mounted on the rotor, and the rectified output is led directly to the generator field winding without need of brushes and slip-rings. The diodes are mounted on well-ventilated heat sinks, and special designs of fuse are used to withstand the centrifugal force on the fusible link.

On units small enough to require only one diode and fuse per arm, failure of one diode or fuse leaves the exciter with one phase unloaded; exciters are usually designed to supply full-load excitation in this condition without damage, so that the generator can remain in service until the fault can be repaired conveniently. However, experience shows that the failure rate of diodes is extremely low and that more often fuse links fail mechanically. Hence some makers supply salient-pole generators, up to say 25 MW, with no fuses at all, but use generously rated diodes to provide a large margin. These generators would use up to three diodes in parallel per bridge arm. For turbogenerators up to about 70 MW, some designs use two diodes in series—each of full duty, with one, two or more series pairs in parallel per bridge arm—and no fuses. On large units, redundant parallel paths, individually fused, are provided as in static equipments.

For units that use fuses, the striker-pin indicator type can still be used, the pin being observed by causing it to interrupt a light beam falling on to a photoelectric cell, or it can be observed visually with a stroboscope. Alternatively a neon lamp is connected across the fuse and glows when the fuse blows.

When diodes are in parallel, whether fused or not, if one becomes open circuit the system will continue to function apparently normally unless the remaining diodes are overloaded and eventually fail also. If an unfused diode fails by short circuiting, the short-circuit current in the exciter armature induces fundamental (exciter) frequency current in the exciter field winding. This can be detected and used to trip the set before serious damage is done. Another method of detection is to use stationary pick-up coils to see whether the diode connections are carrying current as they should as they pass the coils.

More elaborate indication, perhaps coupled with measurements of current and voltage and indication of earth fault, can be arranged by telemetry, but the telemetry may be less reliable than the diodes. Frequently instrument slip-rings are used, with solenoid-operated brushes that make contact only when readings are required.

The diodes, and fuses too if they are used, must be rated for the normal duty, including field forcing, and to withstand the abnormal conditions noted in Section 28.14.2.

28.14.4 Thyristor excitation

Direct control of the field current of the synchronous machine by thyristors gives quicker response than can be obtained by controlling the exciter field current, because the time delay in the exciter is eliminated and the machine field current can be forced down by using the thyristors to reverse the machine field voltage. (By contrast, with a diode bridge, the machine field voltage can only be reduced to zero by reversing the exciter field voltage.) This is valuable for generators and synchronous compensators in certain power-system situations: for example, to minimise the voltage dip caused by large and possibly frequent load changes; to maintain transient stability of a generator under short-circuit conditions on the power system; to enable a synchronous compensator to maintain close control of the system voltage by rapid change in its reactive load, to minimise the voltage rise following sudden load rejection; to reduce more quickly the current resulting from a fault between the generator and its nearest protective circuit-breaker when field suppression is the only means available.

The synchronous machine requires slip-rings and brushes, and this is a disadvantage, especially for large machines for which brushgear maintenance may become a significant inconvenience.

The excitation power may be supplied by direct coupled main and pilot exciters, the main exciter working continuously at ceiling voltage. This makes the power supply independent of voltage fluctuations on the power system. Usually though the excitation is supplied from the generator terminals through a step down transformer. This is usually designed to provide the required ceiling voltage when its primary voltage is reduced to about 60% of normal. This ensures that some field forcing can be done even when the power system voltage is depressed by a fault. It does subject the generator field winding to a rather high peak voltage when the system voltage is normal. A lower ceiling is adequate if power-rated current transformers are added in order to derive some excitation from the machine output current, so boosting excitation during the fault. The set is shortened by the absence of the exciter, and this may save costs on foundations and building. For very-low-speed generators the scheme may well be cheaper than a direct-coupled exciter and diodes.

Some excitation systems use diodes and thyristors in combination, e.g. in a full-wave half-controlled bridge circuit. One patented scheme uses a full-wave diode bridge with thyristor 'trimmer' control fed from a special excitation winding on the generator stator and from compounding current transformers.

Rotating thyristor systems have not yet been developed commercially, mainly because of technical difficulties in transferring control signals from the stationary equipment and problems concerning the reliability of rotating control circuitry.

28.14.5 Excitation systems circuits

Typical systems are shown in *Figure 28.26*.

(*a*) *Self-excitation* provides a simple and inexpensive scheme for generators up to about 3 MVA, using a one-phase thyristor output stage. With a three-phase thyristor bridge the scheme is applied for the highest ratings. The bridge rectifier may be half-controlled, with thyristors and diodes in combination. Another variant has a diode bridge that provides more exciting current than is demanded and thyristors to divert part of this current from the field winding.
(*b*) *Self-excitation through an exciter* is convenient for brushless sets where the diodes are mounted on the generator-exciter shaft, and where the cost or mechanical complication of a pilot exciter is undesirable. A typical rating limit is 10 MVA.
(*c*) *Separate excitation* provides excitation power independent of the generator output. It is commonly used for generators rated at 10 MVA up to the maximum.

Scheme (*a*) is capable of the most rapid response. In (*b*) and (*c*) some delay is introduced by the exciter time-constant; consequently a high exciter ceiling voltage and a large output from the pilot exciter are needed to obtain a more rapid response.

28.14.6 Excitation control

When a generator operates alone the excitation is controlled to maintain the steady-state voltage within the necessary limits, and to prevent unacceptable variations of voltage when large and sudden changes of load occur. Generators running in parallel may need additional control signals to

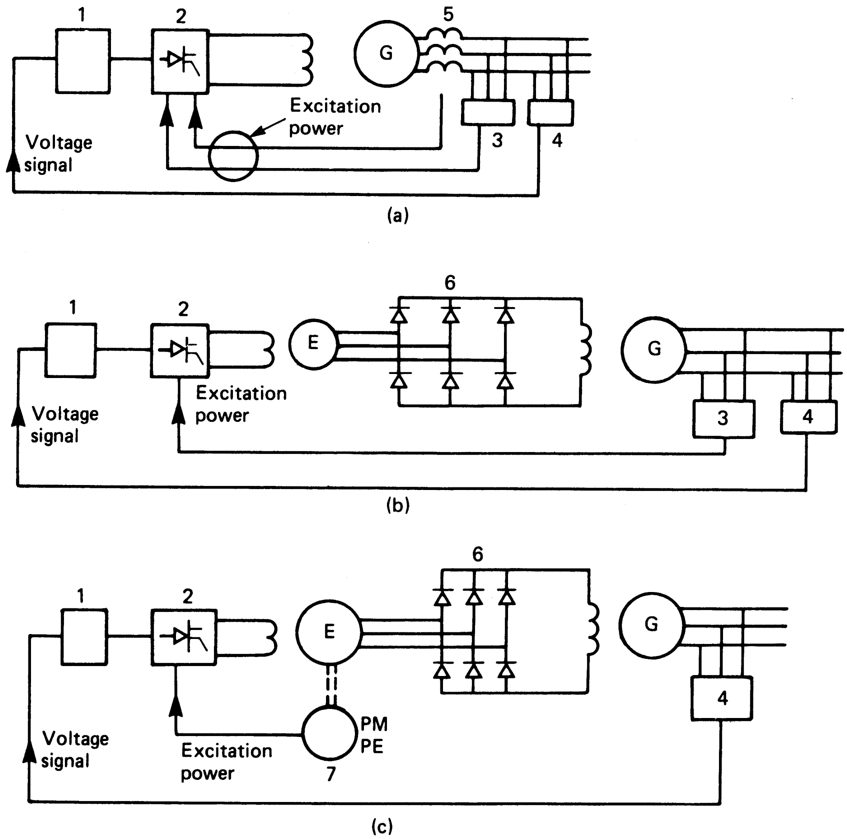

Figure 28.26 Excitation systems: (a) direct self-excitation; (b) self-excitation through an exciter; (c) separate excitation. 1, Control circuits; 2, power-output stage (a.v.r.); 3, excitation power transformer; 4, voltage transformer; 5, current transformers for excitation power; 6, diode rectifier (static or rotating); 7, permanent-magnet pilot exciter

share the total reactive load correctly between them. In an interconnected system, control of steady state and transient stability is a vital duty. Manual control of the excitation is inadequate, and automatic control is provided.

Electromechanical voltage regulators, in use only in old installations, may be of the carbon-pile, vibratory-contact (Tirrill) or rolling-sector (Brown Boveri) type. These have been superseded, initially by magnetic amplifiers, with or without amplidynes, and now by solid-state control systems using transistor amplifiers at low power levels, with thyristors for field-circuit power. The new systems are continuously acting (i.e. have no dead band) and can be arranged to respond to many control signals besides that from the terminal voltage, so the term 'automatic excitation controller' is more logical than 'automatic voltage regulator' (a.v.r.). Power supply for the control circuits is derived from the machine terminals or the pilot exciter.

28.14.7 Basic principles of voltage control

A direct voltage proportional to the generator average terminal voltage is derived via voltage transformers and a diode rectifier circuit. This voltage is compared with a stable reference voltage generated within the regulator. Any difference (the 'error voltage') is amplified and used to control the firing of a thyristor circuit which supplies the excitation, either to the field of the synchronous machine or to its main exciter-field winding. Thus the excitation is raised or lowered to restore the machine voltage to the desired level and the error voltage returns to near zero. The set level is obtained by adjusting the proportion of the machine voltage that is compared with the reference voltage or by adjusting the reference voltage itself. The basic circuit (*Figure 28.27*) is incorporated in *Figures 28.28* and *28.29*.

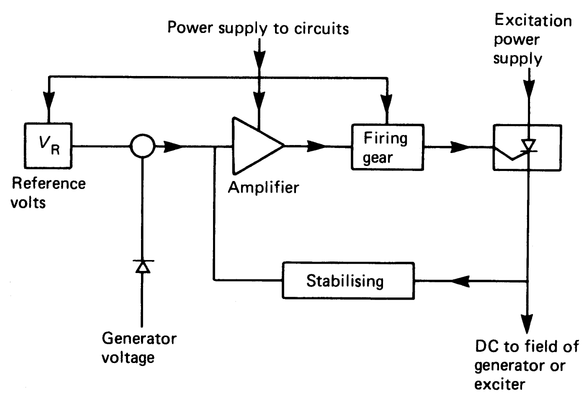

Figure 28.27 Basic circuit of an a.v.r.

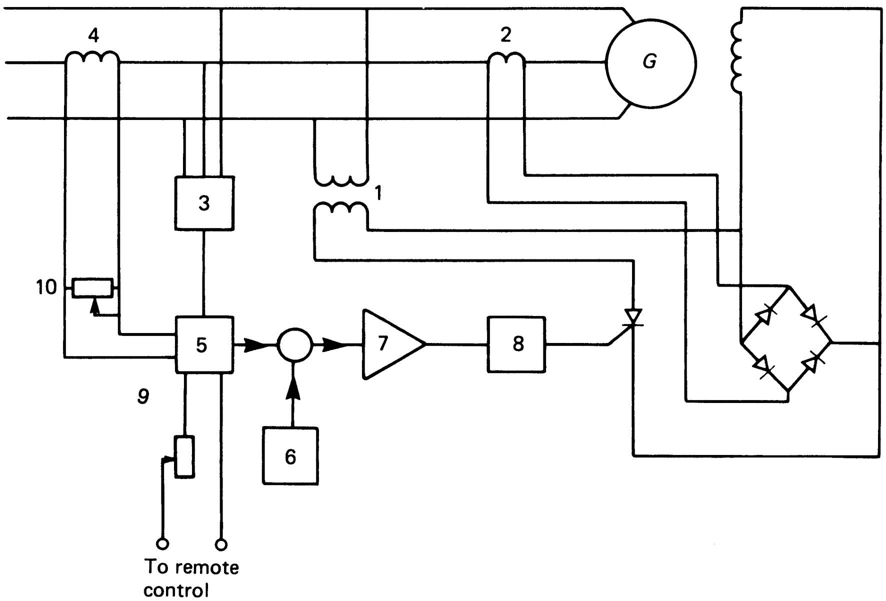

Figure 28.28 Self-excitation with a one-phase thyristor; 1, Excitation transformer; 2, excitation current transformer; 3, voltage transformer; 4, compounding current transformer; 5, voltage-measuring circuit; 6, reference voltage; 7, amplifier; 8, firing gear; 9, voltage-setting rheostat; 10, compounding adjustment

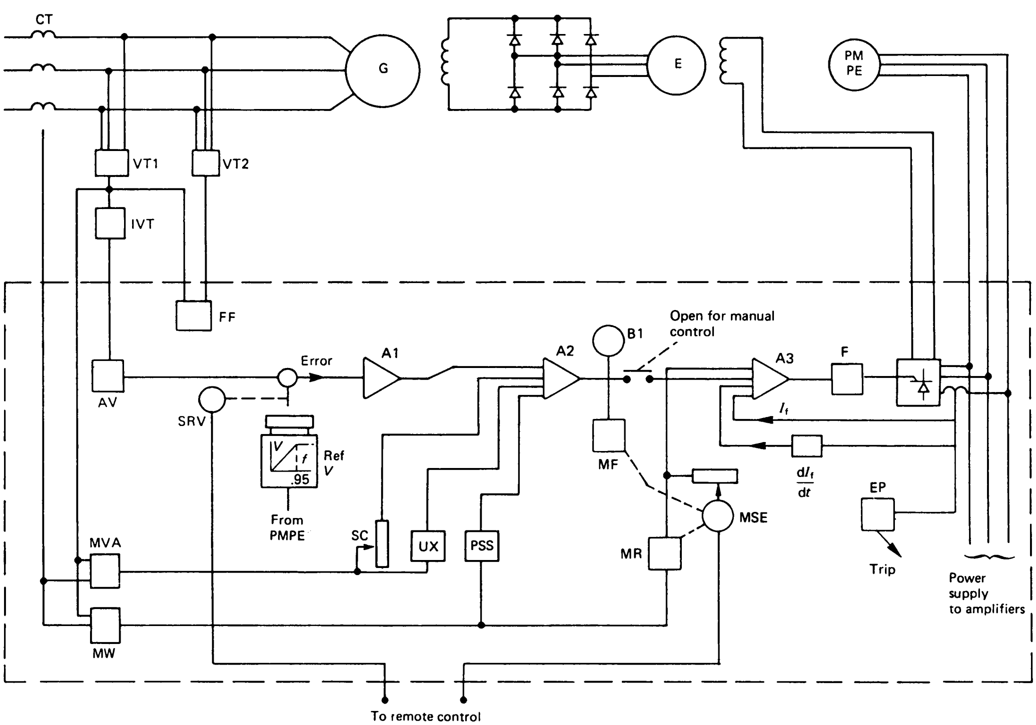

Figure 28.29 Automatic voltage regulator with additional control features: A1, A2, A3, amplifiers; AV, average-voltage circuit; B1, balance indicator; CT, current transformers; E, exciter; EP, excitation protection circuit (operates on high or low excitation current); F, thyristor firing gear; FF, voltage-transformer fuse failure detector and alarm; G, generator; IVT, isolating voltage transformer; MF, manual follow-up circuit (adjusts MSE); MR, manual restrictive circuit; MSE, manual set-excitation control; MVA, MW, circuits providing signals proportional to MVA and MW; PMPE, permanent-magnet pilot exciter; PSS, power-system stabiliser control; Ref V, reference voltage; SC, set-current compounding control; SRV, set-reference voltage control; UX, underexcitation (var limit) control; V_{T1}, V_{T2}, voltage transformers

28.14.7.1 Control range

Generators are usually designed to deliver any load from zero to rated output over a voltage range of ±5%, at any power factor between rated (usually 0.8–0.9 lag) and say 0.95–0.9 lead. The a.v.r. setting controls must provide the corresponding range of excitation, and also provide say 85% of rated voltage on no load. Accuracy of control is usually within ±2.5 or ±1.0% of the set value over the load range.

28.14.7.2 Manual control

Manual control is usually provided for use if the automatic control fails or for convenience when the set is being commissioned. In small units the manually controlled system may be entirely independent of the automatic one, but (especially for economy on large units) it uses the thyristor output stage and the associated firing circuits of the regulator.

Some regulators, when in auto control, drive the manual control so that, if it were in use, it would give the same excitation as the auto circuit. However, this follow-up must be prevented from driving the manual control down to excitation levels that are stable with continuously acting control but unstable with fixed excitation.

Some systems use the manual circuits continuously to control the steady-state excitation. The auto circuits continuously trim this to suit minor fluctuations of load, voltage, etc.; larger disturbances will cause rapid automatic changes of excitation voltage, up to full boost or full buck if necessary. If changed conditions persist for more than a few seconds, the follow-up circuit adjusts the manual control to the new steady state and the auto-circuit output falls to its usual low level.

28.14.7.3 Manual-to-auto change-over

Whichever system is in control, it is necessary to adjust the other automatically or manually, so that a change-over can be made without causing a significant change in excitation. A balance meter is provided so that the outputs of the two systems can be matched before making the change. The manual rheostat and the voltage-setting rheostat are often motorised for control from a remote control room, and the balance-meter reading must be repeated there too, unless automatic matching is provided.

28.14.8 Additional control features

28.14.8.1 Parallel operation

To ensure satisfactory sharing of reactive load between generators paralleled at their terminals, the a.v.r. can arrange for the terminal voltage to fall with increasing reactive load, usually by 2.5–4.0% at full load. For generators paralleled on the h.v. side of step-up transformers, the a.v.r. can either add to or partly compensate for the transformer impedance drop, as desired.

28.14.8.2 Excitation limits

Fault conditions on the power system will cause the excitation to rise to ceiling value to try to maintain normal voltage. An adjustable timer is used to return to normal excitation after several seconds in order to avoid overheating the machines if the fault persists.

A reactive-power-limiting circuit can be used to prevent the excitation falling so low that the generator will not remain in step with the system. The reactive power (under-excited) at which this circuit operates is automatically varied in response to machine voltage and power output to maintain an adequate stability margin.

28.14.8.3 Overfluxing protection

It is operationally desirable to be able to leave the a.v.r. in control when the generator is shut down or run up. To avoid overfluxing the machine and its associated transformer (if any) the reference voltage is arranged to decrease in proportion to frequency at speeds below about 95% of normal. This 'constant volts-per-cycle' control is needed also for generators that have to operate over a speed range, e.g. for ship propulsion.

28.14.8.4 A.v.r. fault protection

Failure of a.v.r. components, or of other components in the excitation system, may cause excessive or insufficient excitation for safe operation. Either condition trips the a.v r. to manual control and alerts the operator.

Voltage-transformer fuse failure is detected by comparing the voltages from two voltage transformers. If the a.v.r. voltage transformer fails, the system trips to manual control; if the comparison circuit fails, an alarm shows that fuse failure protection is no longer working.

28.14.8.5 Double-channel a.v.r.

To enhance reliability of operation, large or vital generators frequently use regulators in which the automatic and manual control circuits are duplicated; often the thyristor output stage supplying the exciter field is duplicated too. Occasionally the much larger thyristor bridge that feeds the generator field directly may be duplicated. Each channel is able to perform the full excitation duty; the two channels may operate in parallel or in main and stand-by mode. If one channel fails, the other maintains the excitation unchanged. If the second channel fails subsequently, it trips to manual control. Alarms indicate the abnormal conditions.

28.14.9 Overall voltage response

The overall voltage response is defined in terms of steady-state and transient behaviour with the generator on open circuit and on no load, and under the control of the excitation system. For a generator operating *alone*, the following conditions may be relevant, their importance depending on the duty required of the generator:

(1) *Steady state*: accuracy of voltage control over the range of load and power factor.
(2) *Transient*: 1. Response of the open-circuit generator voltage to a step change in reference voltage; 2. Voltage response when a sudden increase or decrease of load occurs.

Conditions of importance under transient conditions are the voltage rise and recovery time of the generator voltage when load is suddenly removed, and the voltage dip and recovery time when a large motor is switched on to the generator terminals. For a generator running alone, BS 4999-140:1987, 'Specification for voltage regulation and parallel operation of a.c. synchronous generators', specifies various grades of voltage regulation, for steady state and transient conditions. Accuracy of voltage control may conform to ±1% or ±2.5% or ±5%. Voltage dip must not exceed 0.15 p.u. when a current of 0.35 or 0.6 or 1.0 p.u. of rated generator current is suddenly demanded. The voltage is required to recover to 0.94 or 0.97 p.u. within 1.5 or 1.0 or 0.5 s. Values of the temporary voltage rise that occur when rated load at p.f. 0.8 is thrown off are specified with a range 0.35–0.15 p.u. The more severe the conditions the more powerful the a.v.r. control must be. Also, the lower must be the generator X''_d and X'_d in order to reduce the immediate fall or rise in voltage that the a.v.r. cannot affect. There is a consequential increase in the short-circuit current and in the generator frame size; both these increases raise the cost of the generator and, perhaps, of its switchgear. The response under transient conditions is a convenient way of expressing the overall performance and of testing it during commissioning. The terms used are

V_1	initial voltage
V_2	final voltage
$V_1 - V_2$	voltage step
v	voltage overshoot beyond V_2
t_1	rise time, that in which V_2 is first reached (and passed)
t_2	settling time, the shortest time after which the voltage remains within say ±0.5% of a steady value (which should be V_2). *Figure 28.30* illustrates a step up of voltage.

Normally, either V_1 or V_2 is the rated voltage V_r, depending on whether a 'step-up' or a 'step-down' change is being tested. The voltage–time curve is a well-damped transient, settling at V_2 after a few oscillations. Typical values of the quantities defined above are

$V_2 - V_1$	0.1 p.u. of V_r	v	not more than $(V_2 - V_1)/2$
t_1	0.2–0.6 s	t_2	1–5 s

For a given step change $(V_2 - V_1)$, t_1 is reduced by increasing the ceiling voltage V_c, by increasing the excitation system gain, and/or by reducing the system time constants. The parameters of the generator and exciter cannot be changed once the machines are made, but the a.v.r. parameters are designed to be adjustable. Changes that reduce t_1 will increase the overshoot v and the settling time t_2; hence settings of a.v.r. gain, time constants and feedback signals (if any, e.g. exciter voltage or exciter field current) are calculated to achieve the desired compromise, and performance is checked over a range of values of step change during commissioning (say steps of 1%, 5%, 10% and 20% of V_r).

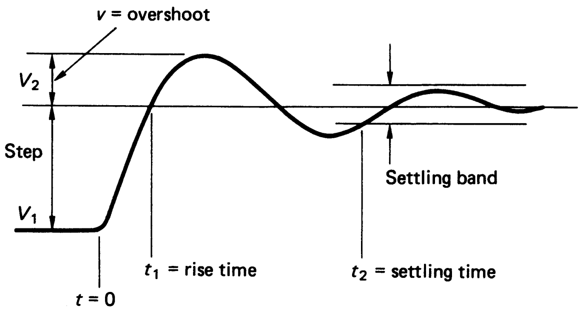

Figure 28.30 Voltage step change

When generators operate in parallel, as most do, the excitation systems must be designed and adjusted to achieve the best compromise between highly accurate voltage control, steady-state stability, and transient stability following a system disturbance. Thus some step-change tests, or tests by injecting low-frequency sinusoidal voltage into the reference circuit, are desirable, to confirm the calculated performance.

See IEEE Standard 421A, 'Guide for identification, testing and evaluation of the dynamic performance of excitation control systems'.

28.14.10 Digital control[170]

Increasing use is being made of a dedicated microprocessor to replace the analogue control system. The input signals (voltage, current, MW, MVAR, etc.) are converted to digital form, and the processor is programmed to respond to these to provide an analogue signal to the firing circuits. The characteristics needed in the excitation controller are reproduced digitally in the processor, and limits of relevant quantities are introduced. The whole can be set up and tested in the works, so that site commissioning is simpler and quicker. In service, settings do not drift or suffer from poor rheostat contacts, but can be readily changed, even while the set is in service, to suit changed operating conditions. If a component does fail, a new card can be inserted without repeating the commissioning tests.

There is a delay of up to 10 ms while the microprocessor scans the input signals and readjusts its output signal, if necessary, but this is very small compared with the machine time constants.

The thyristor firing pulses may also be generated digitally. Development is proceeding of adaptive controllers that will automatically tune their characteristics to suit the operating conditions.[169]

28.15 Turbogenerators[92–137]

The characteristic features of turbogenerators are their high speed to meet steam-turbine requirements and their large outputs to provide economy of capital and operating costs for the power station. Most are two-pole units running at 3000 or 3600 rev/min, but four-pole generators at 1500 or 1800 rev/min have become common for large outputs (1000 MW or more) from nuclear reactors of the boiling-water or pressurised-water type. These reactors deliver large volumes of steam at temperatures and pressures that are lower than those provided by fossil-fired boilers or some gas-cooled reactors. The low-speed turbine may handle these conditions with a greater efficiency that is sufficient to offset its higher capital cost compared with a high-speed unit. Design constraints are less exacting in the low-speed turbine and generator.

28.15.1 Main dimensions

The output coefficient C ranges typically from about 0.5 MVA s/m^3 for a rating of 20 MVA to about 2.0 MVA s/m^3 for a 1000 MVA unit. For 3000 rev/min machines these figures correspond to D^2L of 0.8 and 10 m^3, respectively. Economic diameters for these outputs range from approximately 0.75 m to 1.3 m, the latter being a limit set by centrifugal stresses in the endrings and in the rotor teeth. Hence outputs range from approximately 17 to about 170 MVA per metre of core length. The higher values of output coefficient are made possible by enhanced cooling techniques. Typical dimensions for a 660 MW two-pole hydrogen-cooled machine may be: rotor diameter, 1.15 m; core length, 6.8 m; overall shaft length, 13.5 m; core outside diameter, 2.7 m; outer casing, 4.8 m in diameter and 10.3 m long; total weight, 480 t.

28.15.2 Rotor body

The output available from a turbogenerator is largely determined by the excitation m.m.f. that can be carried on the rotor with acceptable winding temperatures. The high centrifugal stresses make cylindrical (i.e. non-salient-pole) construction essential.[92,93] Within the chosen diameter the number, shape, size and spacing of the winding slots have to be optimised to obtain the maximum m.m.f. capability with acceptable stresses in the teeth and slot wedges, with adequate insulation, with acceptable magnetic flux densities and with ducts for ventilation that enable temperature guarantees to be met. For air-cooled machines of medium output the manufacturing simplicity afforded by parallel-sided slots and solid copper conductors of rectangular cross-section may outweigh the loss of optimum performance and provide the cheapest design. For larger ratings tapered slots are used to accommodate more copper, while giving approximately constant mechanical stress and magnetic flux density along the radial length of the teeth.

A rotor is forged from a single steel ingot, the largest of which approach 500 t in weight; this would produce a rotor weighing 250 t, enough for a four-pole machine of about 1250 MW at 1500 rev/min. The forgings contain the alloying elements nickel, chromium, molybdenum and vanadium; according to size and speed, ultimate tensile strengths of the forgings range from 650 to 800 MN/m^2, while their 0.2% proof stresses range from 550 to 700 MN/m^2. The forgings are inspected with ultrasonics and magnetic-particle ink before use. Many generator makers now rely on these examinations and do not bore the forging axially along its centre line, except for large forgings or if ultrasonics reveals defects that can be removed by boring.

The endwindings (the parts projecting beyond the ends of the slots) must be supported against centrifugal forces by endrings (retaining rings) from which they are insulated by, for example, resin bonded fibreglass or aramid paper (Nomex) or combinations of synthetic insulating sheets. The endrings are steel forgings, usually shrunk on to the ends of the rotor body. In some older designs they were shrunk on to discs that were shrunk on to the shaft outboard of the windings, and they were not tight on the rotor body.

From the 1950s until 1982, the endrings[106,107] were of austenitic steel (18% Mn, 4–5% Cr, 0.3% C) warm worked to give high strength, up to 1100 MN/m^2 proof stress and 1220 MN/m^2 ultimate in the highest grade. This alloy is very susceptible to stress corrosion if it gets wet, e.g. by condensation from moist air or leakage of cooler water. In 1982 an austenitic alloy became available with 18% Mn, 18% Cr;[108,109] this is not subject to stress corrosion under any likely operating conditions, and has mechanical properties up to 1200 MN/m^2 proof stress (0.2% strain) and 1300 MN/m^2 ultimate strength. Endrings large enough for 1500 MVA two-pole or four-pole generators can be obtained, and higher strengths have been developed. Many endrings of the older alloy have been replaced, after some years in service, using the 18–18 material.[106,116]

Rotor vibration at running speed must be low—typically about 50 μm peak-to-peak measured on the shaft near the bearings, though up to twice this is commercially acceptable. Hence balance weights must be carefully positioned, axially as well as circumferentially, and the design of the rotor, its bearings and its supports must ensure that its critical speeds are sufficiently far from rated speed. Small 3000 rev/min rotors will have one critical speed below 3000 rev/min, say about 1700–2000 rev/min, but as ratings (and therefore the bearing span) increase, two or even three criticals will occur below 3000 rev/min (typically around 650, 1750 and 2500 rev/min). When the rotor is coupled to the turbine, the critical speeds are usually raised slightly, so that behaviour in both the coupled and the uncoupled condition must be acceptable, for site running and works testing (without the turbine) respectively.

Electrical faults on the power system or on the machine itself produce abnormally high oscillatory torques on the rotor, at system frequency and often at twice this frequency also. Where series capacitance is used in long lines to compensate for inductive reactance drop, electrical oscillation at the natural frequency f_n may cause torques on generator rotors at system frequency minus f_n. These torques may resonate with torsional natural frequencies of the shafts and produce unacceptable fatigue stresses. From all these causes, complex torsional oscillations develop in the shaft system, with components determined by the inertias and stiffnesses of the several shafts of the turbine, generator and exciter. The shaft dimensions must be chosen to avoid a serious loss of fatigue life during such incidents as well as to satisfy the critical speed criteria mentioned above. There are many articles on the subject; references 128–135 are a few of these.

Bearings are of the white-metalled cylindrical type, with forced oil lubrication and, except on small sets, high-pressure oil jacking also. Jacking allows the set to run slowly (typically 3–20 rev/min) on the turning gear to cool off the turbine and generator rotors before the unit is finally stopped. Without this, the rotors would bend because temperature gradients would occur across the diameter of each rotor, and vibration would occur on the next run-up.

All turbogenerator rotors bend under their own weight, and with two-pole rotors for outputs more than about 30 MW the amount of bend would be significantly more when the pole axis is horizontal than when it is vertical. Hence a vibration would occur at a frequency corresponding to twice the running speed; it is caused by the changing stiffness of the rotor in the vertical plane, and therefore cannot be removed by mechanical balancing. The stiffnesses about the polar (direct) axis and the slot (quadrature) axis must be made as nearly equal as possible under running conditions (when centrifugal force on the windings increases the stiffness in the plane of the quadrature axis). This is done either by cutting axial slots along the pole areas (and filling them with magnetic steel if necessary to avoid magnetic saturation) or by cutting narrow arcuate grooves circumferentially across the poles, sufficient grooves being spaced down the length of the rotor to reduce the stiffness to match that in the quadrature plane.[29,93]

28.15.3 Rotor winding

Excitation currents range from say 400 A at 200 V for a 30 MW generator to 5.7 kA at 640 V for a 1000 MW two-pole machine and up to around 7 kA at 650 V for a 1500 MVA machine. For rotors using indirect cooling each coil is wound with a continuous length of copper strap, bent on edge at the four corners. The copper contains about 0.1% silver and is hard drawn to increase its strength and so avoid the coil-shortening effect that occurred with plain soft copper as a result of heating while part of the copper was prevented (by centrifugal force) from expanding axially.

Directly cooled coils are usually made of larger section conductors of silver-bearing copper containing grooves and holes to provide gas passages. Half-turns are brazed together in the end regions after they have been positioned in the rotor slots. The slots may be parallel-sided, or tapered to contain more copper without increased tooth stress.

Insulation between turns is usually provided by interleaves of resin-bonded glass fabric or some other synthetic material. The coils are insulated from the rotor body by U- or L-shaped troughs of resin-bonded fibreglass, Nomex or melamine, or combinations of such materials. Similar insulating strips insulate the top conductor from the slot wedge; in direct-cooled rotors the strips must have through-holes to allow the cooling gas to escape from the rotor; they must be thick enough to provide adequate creepage distance to withstand the specified h.v. tests.

The end-windings are packed, partly or wholly, with blocks of insulating material to avoid distortion and the consequent risk of short circuits between turns.

28.15.4 Stator core

The stator core is built up of segments of electrical sheet steel, usually 2–3% silicon, 0.35 mm thick, cold rolled and non-oriented. To minimise weight, the core is worked at the highest flux density consistent with reasonable losses. In a two-pole machine the magnetic force across the airgap subjects the core to an elliptical distortion that rotates with the rotor, so producing a double-frequency ($2f$) vibration. The core depth must be chosen so that its natural frequency of vibration in this elliptical mode is well away from $2f$; usually $3f$ or more is practicable without excessive depth of core. Grain-oriented steel has better permeability and lower losses than non-oriented steel, but as it has a lower modulus of elasticity its other advantages cannot be realised without accepting higher vibration. This, and its higher cost, severely limit its use in turbogenerators.

28.15.5 Stator casing

Air-cooled machines may have bearing pedestals on a bedplate; the stator frame then merely supports the core and forms the ventilation enclosure. Alternatively, it may be a more rigid box frame with the rotor bearings carried in end-brackets.

A hydrogen-cooled machine must have a totally enclosed and gas-tight construction; the end-bracket bearing arrangement is adopted to minimise the bearing span and to raise the critical speeds. Hence the frame must be rigid enough to provide proper support for the bearings and to contain the gas pressure that might occur in the unlikely event of a hydrogen–air explosion inside the frame. This could produce pressures up to about 1400 kN/m^2; therefore this pressure, rather than the continuous working pressure of hydrogen, becomes the design criterion.

In large two-pole machines (which are invariably hydrogen cooled) some form of flexible mounting is needed between the core and the casing, and the casing should not have any natural frequency near to $2f$. This is to avoid excessive magnetic noise and the risk of unacceptable vibration on the casing, coolers or pipework.

Where transport facilities are inadequate for handling the complete stator, the core and windings must be made separately from the outer casing, separately transported, and assembled on site before the rotor is inserted. By contrast, smaller machines can be transported complete to some sites, with the rotor clamped in temporary supports; this arrangement facilitates erection.

28.15.6 Stator winding

For small machines the voltage is usually fixed at a standard network voltage (e.g. 6.6 or 11 kV), but for large machines, where a generator-transformer is used, the designer has a free choice. A high voltage avoids difficulties due to high currents, but valuable space in the slots has to be sacrificed to insulation; a compromise is thus about 15 kV for 100 MW and 200 MW machines, and up to 22 or 25 kV for the larger sets. Even so, generators of more than about 50 MW rating will have two circuits in parallel per phase; for more than 1000 MW it may be necessary to use special winding arrangements to have four parallel paths in a two-pole machine. In four-pole generators, four circuits occur naturally and can be in parallel or in series-parallel.

The winding is of the two-layer basket type, almost always with integral slots per phase per pole, as described in Sections 28.3 and 28.4. In the slots[27] and in the endwindings[101] the coils must be supported to resist electromagnetic forces.[93] These are, on normal load, continuous though fairly low vibratory forces at twice supply frequency. When a short circuit occurs close to the generator, transient oscillatory forces occur, 50–100 or more times greater than those on load.

In the slots the forces are radial, directed towards the bottom of the slot, except where different phases occupy the same slot; there the force on the top conductor is towards the wedge for part of each cycle. To support the coils along the whole core length, conformable packing strips of, for example, resin impregnated polyester fleece are placed beneath and between the coil sides. Slot wedges are fitted that apply a known radial load to the coils, greater than the electromagnetic forces. Packing may be fitted down the sides of the coils, and there may be a fibreglass ripple spring between the wedge and the top coil side to take up any small shrinkage in service.[27,29,93]

The endwindings are secured to a strong structure of insulating materials,[27,29,93,100] fibreglass rings carried on brackets of resin-bonded wood laminate have been used very widely. For larger ratings a solid cone of filament-wound resin bonded fibreglass is used for greater strength and long-term rigidity. The coils are bedded to the structure with conformable packing material. Usually the slot and endwinding packings are cured while the coils are held by temporary wedges and clamps, which are then replaced by the permanent ones. The complete structure is bolted to the end of the core; some axial movement may be allowed, to accommodate expansion of the coils relative to the core.

28.15.7 Cooling

Efficiencies are between 96.5% and 99%, increasing with the rated output. However, the losses will be 0.5–15 MW, appearing as heat that must be removed by circulation of an appropriate cooling medium: oil for removing bearing-friction losses, and air, hydrogen or water for other losses. Details of the cooling media used for stator and rotor windings are given in *Table 28.6*. The heat transfer coefficients

Table 28.6 Properties of cooling media

Medium heat	*Absolute pressure* (bar)	*Specific heat* (kJ/(kg K))	*Density* (kg/m^3)	*Relative volume flow*	*Relative thermal capacity*	*Relative heat transfer coefficient*
Air	1.0	1.0	1.1	1.0	1.0	1.0
Hydrogen	1.0	14.3	0.076	1.0	0.99	1.45
	2.0	14.3	0.152	1.0	1.98	2.52
	4.0	14.3	0.304	1.0	3.96	4.38
Water	1.0	4.2	1000	0.01	38	60

are typical, but depend considerably on velocity and duct size.

Traditionally, indirect air cooling was adopted for outputs up to 70 MVA, and indirect hydrogen cooling for outputs above about 50 MVA. Direct hydrogen cooling of rotors developed rapidly for ratings from about 100 MVA up to the largest, about 1500 MVA, that have been built. Some manufacturers used hydrogen cooled stator coils up to about 650 MVA: others adopted water cooling above about 150 MVA where indirect cooling was becoming difficult. In recent years cheap and simple designs have been developed up to 200 MVA rating using air cooling, direct in the rotor winding and indirect for the stator.

28.15.7.1 Indirect cooling

The cooling medium (air or hydrogen) is blown along the airgap, through ducts in the core and over the surface of the windings. Thus heat generated in the winding passes through the main insulation to the rotor and stator teeth, respectively, and is picked up by the cooling gas mainly from the iron surfaces.

With air cooling, closed-circuit ventilation is universal except in the very smallest sizes, and coolers are separately mounted—usually in a basement beneath the generator, but occasionally above or at the side of the machine. With hydrogen cooling, however, there is no alternative but to build coolers within the gas-tight explosion-proof structure of the machine itself. *Figure 28.31* shows a simplified diagram of a hydrogen-cooled machine with its gas system. It has directly cooled windings.

The cooling gas is usually circulated by a fan at each end of the rotor, though many air-cooled generators (30–60 MW or so) had motor-driven fans mounted in the basement with the air coolers. The rotor fans may be of the centrifugal or the aerofoil (axial-flow) type. Their main purpose is to establish the gas flow through the stator frame, core and coolers; the flow through the rotor results mainly from its own rotation. Most indirectly cooled rotors have axial ventilation slots in the teeth that are closed by wedges

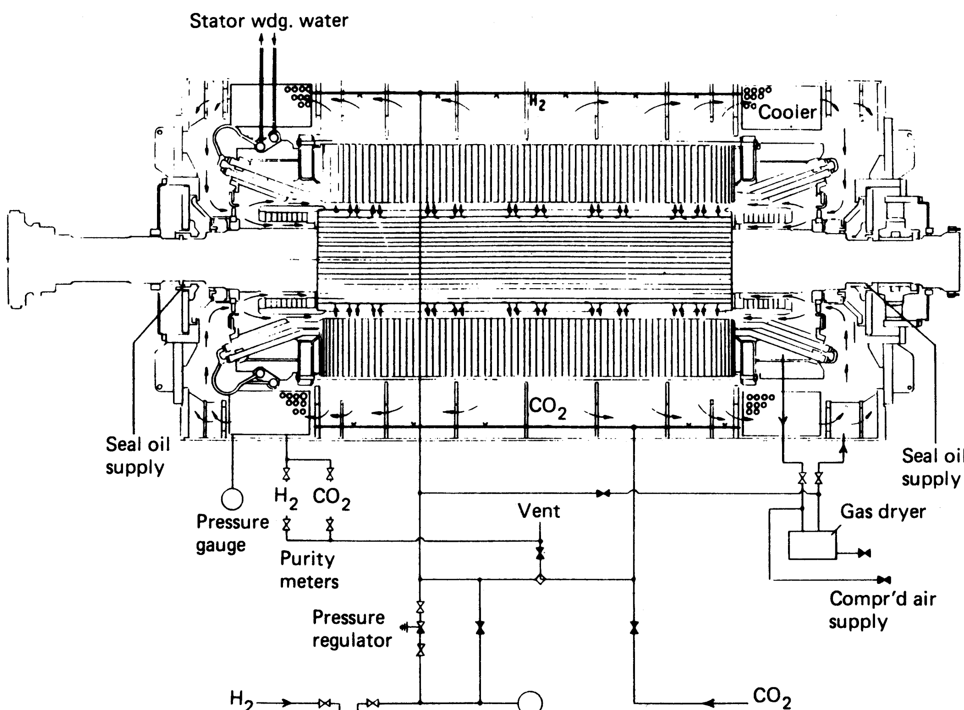

Figure 28.31 Section of a turbogenerator with simplified hydrogen cooling and water-cooled stator winding

except near the middle of the rotor body where the flows from each end emerge into the gap and then pass through the radial ducts in the stator core to the back of the frame.

Pure hydrogen has a density approximately 1/14 that of air, while its specific heat is 14 times that of air; it has a higher heat transfer coefficient and much better thermal conductivity. In service there may be about 1% impurity consisting of air and carbon dioxide; this increases the density by about 13%, but has no significant effect on the cooling properties listed in *Table 28.6*. Windage losses are proportional to density, but even at an operating pressure of 5 bar (absolute) they are still only 40% of what they would be in air at atmospheric pressure.

Early hydrogen-cooled machines were designed to operate at just above atmospheric pressure, but raising the pressure to 2 bar (absolute) and then to 3 bar raised the output available from a given frame size by approximately 15% and then by a further 10% respectively. No worthwhile improvement occurs above 4 bar (absolute) because the temperature gradient across the winding insulation is a large part of the permissible temperature rise.

The auxiliary equipment for hydrogen-cooled generators divides into two main groups: gas control and seal-oil treatment.

The gas control system provides means for filling and emptying the casing without risk of forming an explosive hydrogen–air mixture. Carbon dioxide is used as a buffer, and mixtures containing more than 5% hydrogen in air, or more than 5% air in hydrogen, are avoided. In service the rate of hydrogen loss, though small, is sufficient for the purity to settle at 98–99% hydrogen as make-up is added to maintain the desired operating pressure.

The shaft seals, which prevent leakage of hydrogen along the shaft to the bearings, are supplied with oil maintained at a pressure above the gas pressure. Ring seals which encircle the shaft are simple and allow free axial expansion of the shaft; however, they also allow a significant rate of oil flow towards the hydrogen side of the seal, and rather more to the air side. The gas-side flow absorbs hydrogen, and would release air or moisture into the machine if it contained these in solution. To avoid the consequent pollution, and loss, of the hydrogen, the oil is vacuum treated before being fed to the seals, and the hydrogen-side oil passes through detraining tanks to allow entrained hydrogen to return to the frame before the oil is vacuum treated and recirculated.

The face, or thrust, type of seal is a ring, usually of white metalled steel, which operates against the radial face of a collar on the shaft. The hydrogen-side oil flow is insignificant, so vacuum treating is unnecessary. The extremely thin hydrogen-side oil film (say 60 μm) makes the seal rather vulnerable to dirt particles, and if the ring does not slide freely to follow shaft expansion it will leave the collar and allow leakage or will suffer excessive face pressure and damage to the white metal. Both types of seal are in satisfactory operation. A doubly fed ring-type seal offers the advantages of both types, provided that the pressures of the two systems are kept accurately balanced so that the hydrogen- and air-side flows are kept separated.

A wide range of indicators fitted with audible and visible alarms is necessary to indicate any departure from normal operation of the various parts of the gas and oil systems.

28.15.7.2 Direct cooling[27,29,123–127]

Hydrogen The winding conductors are much more effectively cooled by passing the coolant through them in direct, or almost direct, contact with the copper. Hence much higher current density is possible with an acceptable temperature rise, and the output per unit volume of active material can be greatly increased. Furthermore, significant improvement in performance with hydrogen cooling is obtainable up to operating pressures of 5 or even 6 bar (absolute) (500–600 kN/m^2).

In the rotor, several flow arrangements are used:

(1) *Axial flow* The gas enters tubular, or axially grooved, conductors in the end-winding region and leaves radially through a group of holes through the conductors and wedges at the mid-length of the rotor.
(2) *Axial flow* This is as for (1), but for longer rotors. The middle quarter (approximately) is fed from subslots, while the two end-portions are fed as in (1).
(3) *Radial* The gas enters each end of a subslot cut beneath the winding slot and flows radially outwards through holes punched through the flat copper strips and insulation, distributed throughout the rotor length.
(4) *Axial–radial* This is a combination of (1) and (3) using axially grooved conductors in which the radial exit holes are displaced axially from those that feed gas from the subslots.
(5) *Gap pick-up* Specially shaped holes in the wedges ‘scoop up’ gas from the gap, and others eject it after it has passed through cooling ducts formed in the copper by punched holes or transverse grooves.

Types (1) and (2) require high-pressure axial-flow blowers, which may have three to seven stages of blades; (3) and (4) rely almost wholly on the self-ventilating action of the rotor; (5) may be applied by dividing the rotor into axially adjacent inlet and outlet zones, which may be co-ordinated with the stator ventilation zones. All these schemes are used for hydrogen-cooled machines up to the largest ratings. Type (3) is now common for air-cooled machines with totally enclosed air circuits.

Directly hydrogen-cooled stator coils are used by some makers up to ratings of 600 MW or more. The gas flows down thin-walled bronze or stainless steel tubes that are bonded among the conductor strips and lightly insulated from them. Entry and exit has to be at the ends of the coils, so a moderately high pressure differential is needed. The system co-ordinates well with rotor ventilation of type (1) or (2).

Water The high heat-removal capacity of water and its low viscosity allow it to be used in tubular subconductors that are still small enough to keep the eddy-current losses low.

At each end of the conductor the subconductors (tubes and strips, or all tubes) are brazed into a water box. The coil-to-coil current-carrying connection may be tubular to carry the water also, or separate connections may be made. The water boxes are connected to the inlet and outlet manifolds by insulating pipes of polytetrafluoroethylene (PTFE) or some other synthetic material. Water taken from the boiler make-up system is circulated by pumps around a closed pipework system containing the winding, coolers, filters, control valves and monitoring instruments. Water conductivity is easily maintained by using a demineraliser unit, usually of the resin-bead type; less than 10 μS-cm is easily attained, and the leakage currents down the water columns are insignificant.

The temperature difference between copper and water is only about 2°C, and the water temperature rise between inlet and outlet is usually about 25–30°C. The inlet water pressure is kept below the hydrogen pressure; if leakage does occur, it is leakage of hydrogen into the water (easily detected), and water does not enter the machine.

Water-cooling of the rotor winding increases the output available from given frame size; the slots can be smaller, leaving more room for magnetic flux, so the machine can be shorter provided the stator design is adjusted to suit. Constructional problems occur because of the need to convey water to and from the rotor, to accommodate water manifolds on the shaft, to support the water-pipes against centrifugal force, and to design them and the winding to withstand internal pressures (produced by rotation) up to around 15 MN/m^2. Nevertheless, such rotors are in successful service though they have not yet been widely adopted.

Direct cooling allows the specific electric loading to be increased without exceeding the permitted limits of temperature rise. A smaller frame size can be used for a given output, but the leakage reactances are increased by the increase of leakage flux compared with main airgap flux. The values of X_d and short-circuit ratio can be maintained by lengthening the airgap, until the increased excitation on load pushes the rotor winding temperature rise to its limit.

28.16 Generator–transformer connection

For small ratings up to 3 MW, single-core cables are used. For ratings above this where the number of cables needed would be excessive or cannot be accommodated, solid copper or aluminium bus-bars are used. The bus-bars are supported on insulating cleats or ceramic post insulators and are enclosed in a surrounding duct which provides mechanical protection and sealing. The bars are spaced and supported to suit the operating voltage, the current to be carried (taking into account skin and proximity effects which raises the a.c. resistance and leads to extra heating) and the electromagnetic forces produced by short-circuit currents. The phases are sometimes segregated by insulating barriers to achieve the creepage and clearance distances required. For higher current ratings, each phase conductor usually consisted of two angle- or channel-section bars mounted face to face to form an open diamond or box-shaped section. These sections and arrangements give lower skin-effect losses than flat rectangular bars of the same weight, and natural air cooling may be adequate up to at least 200 MW (with a corresponding current of the order of 10 kA).

To avoid the possibility of phase-to-phase faults, however, phase-isolated bus-bars were adopted for ratings above about 200 MW, and now above 60 MW. Each line connection consists of two angle- or channel-section bars supported by post insulators inside an aluminium tube which is physically and electrically continuous along its length. The tubes of the three phases are connected together electrically at each end of the run and are joined mechanically to the generator and transformer frames. Thus only phase-to-earth faults are possible. Eddy currents induced in the aluminium tubes cause additional losses, but they confine the magnetic field largely within the tube, so that heating of the foundation steelwork or other structures is avoided.

For the higher ratings, say above 12 kA, each conductor bar may be of semi-hexagon or semi-octagon shape, so that the complete conductor approximates to the circular cross-section that gives minimum skin effect and minimum loss.

Table 28.7 Typical dimensions of generator–transformer connections

	Cooling		
	Natural air	*Forced air*	*Pumped water*
Conductor shape	[ ]	[ ]	○⇐
Diameter of circumscribing circle (m)	0.9	0.5	0.13
Diameter of isolating trunking (m)	1.5	1.1	0.75
Total loss in conductor and trunking, for 3 phases (kW/m)	2.0	4.5	7.5

Natural air cooling is practicable up to about 20 kA, but a significant reduction in cross-section or an increase in current rating is made possible by forced-air cooling. A maximum conductor temperature rise of 55°C above the ambient air is usual. With this, and a continuous rating of 19.5 kA (660 MW at 23 kV, 0.85 p.f.), typical dimensions are as given in *Table 28.7*.

For forced-air cooling, the flow is usually from one end of the run to the other along one phase, half the flow returning along each of the other two phases. The air circuit is totally enclosed, with an air-to-water heat-exchanger extracting the loss. If the air circulation fails, the naturally cooled rating is about 60% of the forced-flow rating.

The use of water-cooled stator windings led to the development of water-cooled connections in solid tube or cable form. The water-cooling circuit is similar to, but usually separate from, that for the generator stator. The smaller dimensions are an advantage where space is limited, and the greater loss is not usually economically unacceptable. The system has not been widely adopted, however, because there is usually room for air-cooled connections, while the extra water-cooling auxiliaries introduce additional maintenance and extra complications of duplication and control to guard against shut-down of the generator if an auxiliary item fails.

28.17 Hydrogenerators

28.17.1 Introduction[26–28,30,53,138–156]

The design of hydrogenerators is determined mainly by mechanical considerations. Outputs range from less than 1 MVA to over 800 MVA, and speeds from 50 to 1000 rev/min, depending on the water head available and the type and size of the turbine. The low speeds require the generators to be physically large, and it is often necessary to transport them to site in sections. The inertia required in the set is determined by turbine governing or speed regulation requirements, or by the transient stability of the associated power system. The turbine contributes little flywheel effect, so the generator inertia must often be more than that of a design that satisfies the electrical specification in the least expensive way. The diameter must be increased, or in extreme cases a separate flywheel coupled to the shaft. This is often the best arrangement in fairly small horizontal shaft units.

A water turbine runs up to a high overspeed when load is suddenly removed, because the flow of water cannot be suddenly stopped without causing a high and probably damaging rise of pressure at the turbine gates or valves. The ratio of overspeed to normal speed is, approximately, for impulse turbines (Pelton) 1.7 to 1.9, for reaction (Francis) 1.8 to 2.1, and for propeller (Kaplan) 2 to 2.2. If the governor should fail during load rejectien, a Kaplan turbine could run up to 3 times normal speed.

The rotors must be designed to be safe at overspeed, where the factor of safety on the proof stress of the material used is normally not less than 1.5. A figure closer to 1.1 is acceptable for the very rare runaway condition of a Kaplan unit.

The first critical speed is required to be above the overspeed.

28.17.2 Construction

28.17.2.1 General arrangements[8,26,30]

Horizontal and vertical shaft arrangements are used, the former usually for impulse turbines and small reaction turbines, the latter for large reaction and propeller turbines. Nevertheless, the vertical arrangement has been used up to 36 MVA and 1000 rev/min, and the horizontal shaft up to more than 100 MVA at 428 or 600 rev/min.

Horizontal generators are similar to those made for diesel engine or geared turbine drive, except that they must accept the higher overspeed, may carry a flywheel, and need a thrust bearing to carry any unbalanced hydraulic thrust. A turbine may be overhung at each end, or at one end only.

With a vertical shaft, five bearing arrangements are possible. These are shown in *Figure 28.32*. The umbrella arrangements of (*d*) and (*e*) are suitable for low-speed sets in which the ratio of core length to stator bore diameter does not exceed say 1/4. With a top guide bearing added as at (*c*), higher speeds and an L/D of 1/3 are practicable. The top thrust bearing arrangement of (*a*) and (*b*) is used, where necessary, for higher L/D ratios and speeds usually above 400 rev/min. The top bracket and the stator frame must be rigid enough to carry the thrust load, and so are more expensive than with a bottom bracket assembly. However, as the bearing is on a smaller diameter part of the shaft, its losses are less; if the contract places a high value on losses (as with pumped storage for example) the capitalised value of the lifetime losses may be enough to pay for the top-thrust layout. If the bracket is supported directly from the pit walls, the stator frame is relieved of the thrust load, and some problems of differential expansion and electromagnetic vibration are avoided.

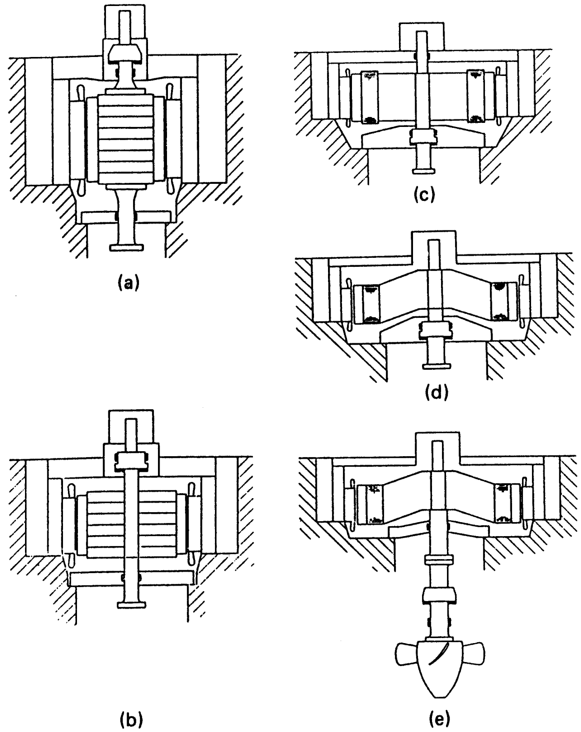

Figure 28.32 Hydroelectric generator bearing arrangements: (a) thrust bearing above rotor, two guide bearings—upper guide bearing separate from and below thrust bearing; (b) thrust bearing above rotor, two guide bearings—upper guide bearing combined with thrust bearing; (c) thrust bearing below rotor, two guide bearings (semi-umbrella machine); (d) thrust bearing and single guide bearing below rotor (umbrella machine in English, American and Continental literature); (e) thrust bearing mounted on turbine casing, single guide bearing below generator rotor (umbrella machine in German literature). (Reproduced, with permission, from Anscombe[30])

28.17.2.2 Thrust and guide bearings

In large vertical machines the dead weight plus hydraulic thrust is several hundred tonnes. The thrust collar rests on segmental pads (usually of steel) faced with white metal. They are supported on a thrust ring at the bottom of the annular oil chamber that surrounds the shaft. The pads and the lower part of the collar are submerged, and careful design and assembly are needed to avoid leakage of oil and vapour.

The pads are supported so that they can tilt slightly to develop hydrodynamic lubrication. The Kingsbury pad is supported on a single spherical pivot: the Michel pad pivots on a radial ridge. Both supports are offset from the centre line of the pad if rotation is to be in only one direction: a pivot on the centre line is needed for a reversing pumped storage set. Load sharing among the pads depends critically on the accuracy of dimensions or adjustment of the pad heights. This difficulty is much reduced if the pad sits on a mattress of closely spaced helical springs instead of a solid pivot. The specific bearing pressure can be quite high, up to 3.5 MPa average over the pad surface. Transient conditions on a pump/turbine unit may increase this at some times by a factor of about 1½. The peak oil pressure on the pad is about l.5 times the average. The oil film thickness is usually between 0.1 and 0.05 mm. On such highly loaded bearings a high pressure oil supply is often fed through one or more small holes in the surface of the pad to provide oil jacking and hydrostatic lubrication at rest. This is especially needed to reduce the starting torque required when the unit is being motored for pumping duty.

The pumping action of the bearing is often enough to circulate the oil through water-cooled heat exchangers placed in the oil pot. If the pot is too small for this, or to improve accessibility to the pads, the coolers may be mounted separately below the bearing. The oil is then circulated by main (a.c.) and stand-by (d.c.) motor driven pumps.

With high bearing pressures and surface speeds cooling water may be circulated through channels in the pads themselves.

Vertical shaft machines have friction brakes operated by air pressure to bring the set to rest without protracted running at low speeds, when the lubrication of the thrust pads may become inadequate. To reduce wear on the brake pads they are not applied at or near the full speed except in an emergency. Dynamic braking is achieved by circulating up to full-load current through the stator winding short-circuited by a braking switch. The friction brakes are then applied at 15–20% of full speed.

The brakes can also be operated by oil pressure when the machine is stationary, to lift the thrust collar off the pads, either to flood the surfaces with oil after prolonged standing, or for maintenance such as inspection or removal of the thrust pads. If the shaft is to remain lifted for a long time, the jacks are locked mechanically to allow the oil pressure to be removed.

Guide bearings are usually of the pivoted-pad type, running on the outside of the thrust collar or a smaller diameter of the thrust block. Where the guide is remote from the thrust bearing, it runs on the shaft in its own oil enclosure.

If there is a bearing above the rotor, it needs to be insulated to prevent the flow of shaft currents caused by some asymmetry in the core flux, e.g. differences in reluctances at the joints in the core. With a top thrust bearing the thrust face, which is normally a renewable ring bolted to the collar, can be insulated from the collar.

28.17.2.3 Rotor

For large machines the cheapest construction is to secure a laminated rim[143,149] to a fabricated spider that is shrunk on the shaft. The rim must carry its own hoop stress plus the load of the poles and coils. Laminations of rolled steel plate commonly have 0.2% proof stress of 450 MPa, suitable for rim speeds up to about 170 m/s at overspeed. Proof stress up to 700 MPa can be obtained, if necessary.

Sheet can be obtained of quality and width suitable for rims up to approximately 1.5 m diameter, so these can be built of complete rings, shrunk and keyed to the spider with enough interference to maintain contact at overspeed. Larger diameter rims have to be segmental; they will come free at just above running speed, but are kept true to the spider by keys that allow radial growth but not tangential movement. Some designs rely upon this key location, and do not have a shrink fit.

Two kinds of rim are used. The so-called *chain rim* uses segments about 5 mm thick spanning two or more pole pitches, clamped axially by close-fitting bolts. By suitably overlapping the segments and distributing the bolts, the rim can have a hoop strength up to 75% of that of a solid rim. The *friction rim* is being increasingly used. It has segments about 2 mm thick, to increase the number of surfaces in contact. They are clamped with high pressure by high tensile bolts in holes with some clearance. To build a good rim of either type the segments must be flat and of uniform thickness, and accurately aligned to ensure accurate dimensions of the keyways and the axial T-slots for the pole fixings.

Smaller rotors for 600 rev/min or more, especially horizontal ones, are built of discs up to 150 mm thick, shrunk on to a shaft, or spigotted together and held by through bolts, with bolted-on stub shafts. The diameter is limited by the size of plate available of suitable thickness and quality.

Poles are now most frequently built up of laminations about 2 mm thick. They, like the rim, carry unidirectional flux, so tensile strength, flatness, uniform thickness and magnetic permeability are important. It is not usually thought necessary to insulate the laminations, though this would reduce the surface losses caused by harmonic fluxes in the airgap. The laminations are clamped tightly between heavy steel endplates, cast, forged or fabricated depending on the load imposed by their own centrifugal force and the end turns of the field coil.

Poles are fixed to the rim by dovetail, or more often by T-head, projections. These are secured in corresponding axial slots in the rim by taper keys driven in from each end. These pull the base of the pole against the rim. Large rotors may use as many as nine T-heads per pole; detailed analysis of the stress distribution is needed, e.g. by a finite element technique, to ensure that they share the total centrifugal force reasonably equally.

Copper damper bars, usually uninsulated, are fitted reasonably tightly in semi-closed axial slots in the pole face. They are brazed at each end to a copper segment to form a closed grid. Currents induced in them help to reduce the oscillations of load angle when a disturbance occurs on the power system, and to counteract the negative sequence stator current of unbalanced loads. Pole-to-pole links are sometimes fitted to make the grids into a complete cage, but in large machines centrifugal force and fatigue stresses can cause mechanical failures. If interconnection is needed, it is usually sufficient to braze the bars to a few thick copper laminations next to the end clamps of the poles. These provide satisfactory contact to the rim, which completes the circuit between poles.

Field coils on very small machines have several layers wound with round or roughly square-section copper, enamelled and wrapped with resin impregnated insulating fibre or braid. Larger rotors have coils wound with bare copper strip, bent on edge, and larger rotors still[143] use coils fabricated from straight lengths of rectangular section copper strip, brazed together at the corners. The cooling surface is increased by making some strips wider than the others, and placing these, singly or say in pairs, every few turns. With continuously wound strip-on-edge coils the finned effect can be produced conveniently on the sides or the ends of the coil, but not on both.

Insulation between turns consists of two layers of polyimide or aramid paper, or woven glass, with a thermosetting resin. After the coil has been consolidated by baking it under pressure, the interturn insulation is approximately 0.3 mm thick.

Wire-wound coils may be wound directly on to the insulated pole. Strip-on-edge coils may go directly on to the insulated pole, or on to a separate insulated spool which is then secured to the pole. Thick washers of resin–synthetic paper or resin–glass insulate the coil from the rotor rim and from the pole shoe, unless the spool has insulated flanges that do this.

Coil-to-coil connections are secured to the rim, the connection to the top turn (i.e. the outermost one) of a strip-on-edge coil may be brought down inside the coil, between it and the pole, then out beneath the bottom turn. Connections should preferably be arranged to carry the field current equally clockwise and anticlockwise round the shaft. Otherwise it will be magnetised axially, and the bearing surfaces may be damaged by induced currents.

Centrifugal force on the sides of the coil has a component tending to bow the turns, i.e. to bend them on edge away from the pole. To resist this one or several V-blocks (depending on the axial length) are secured to the rim between the poles. Pieces of insulating board of course are fitted between the bare copper and the metal V-block. It is often required that poles may be removed without removing the rotor from the stator bore, and V-blocks make this difficult to arrange. Therefore in some designs, the coils are held by clamps that are secured to the pole itself; the two parts of each clamp are tightened together after the coil and its insulation have been fitted. Then the pole, coil and clamps can be withdrawn as a unit after the pole keys have been released.

28.17.2.4 Stator frame and core[26,143]

The frame is a fabricated assembly of rings of steel plate connected by axial members of tubular, angle or channel section. It is often necessary to split the frame and core of a large vertical shaft machine into several segments for transport to site. These must be bolted together accurately to form a true circle, with the joint faces of the core fitting extremely closely together.

The core joints are avoided if the core is built into the assembled frame sections on site.

The lower endplate of the frame is bolted to the foundations; if the machine has a top thrust bearing the frame may have to support the thrust load as well as the core and windings.

The wrapper plate round the outside of the frame will usually have openings to receive the air-to-water heat exchangers.

The core is built of cold-rolled silicon steel, usually 0.35 or 0.5 mm thick. The reduction of loss that could be obtained with oriented-grain sheet is small, and rarely enough to justify the higher cost of it. The coreplates are assembled with spacers to form radial ventilating ducts, and are clamped between strong endplates.

28.17.2.5 Stator winding (see Section 28.4)[26,53,143]

This is usually of the two-layer, fractional-slot type, using diamond coils with either lap or wave connections. The number of slots is often chosen to permit two or more parallel paths per phase, depending on the number of poles. Coils may be pulled, formed or made as single bars, depending on size. In large high-voltage machines they must be firmly secured in the slots and endwindings, especially if they are to suffer much thermal cycling, e.g. in pumped-storage units. Proper earthing in the slots and stress grading for a distance beyond the slot ends are essential to avoid surface discharges. Large and important machines now often have permanent instruments to give warning of any increase in electrical discharges in service.

28.17.3 Cooling

Except for small machines, closed-circuit air cooling is used. Most hydrogenerators rotate in one direction only, and radial-flow or axial-flow fans mounted on the shaft are commonly used. The heat is removed by air-to-water heat exchangers mounted on the back of the stator frame. About 4 m^3/s of air is required per kilowatt of loss removed. In cold climates, some of the hot air may be taken from the machine and used for heating the station.

Reversing sets for pumped storage would have to use radially bladed fans, which have poor performance and efficiency. For these machines, and for highly rated machines needing carefully controlled ventilation, several motor driven fans are used.

Hydrogen cooling has not been applied to hydrogenerators, mainly because of the cost and practical difficulties of making an explosion-proof casing. However, when inertia is not a controlling factor, a useful reduction in physical size can be achieved by water cooling the stator or rotor windings, or both.[26] Water cooling of only the stator winding introduces a multiplicity of joints which must not leak, but greatly increases the current capacity, or conversely allows the slot size to be reduced for a given rating; this slightly reduces the outside diameter of the core and significantly reduces the leakage reactance.

Water cooling of the rotor winding introduces more constructional difficulties and affects the design more profoundly. The excitation capability is increased without risk of exceeding the specified temperature limits, so a higher short-circuit ratio (longer airgap) is possible, improving the underexcited (line charging) capability. Alternatively a smaller machine with higher electric loading is possible with the stator design suitably adjusted. However, it may be necessary to adopt a size larger than the smallest determined from purely thermal considerations. This larger size may be needed to attain the desired inertia constant, or the lower capitalised cost of its lower losses may be enough to offset the lower first cost of the smaller frame. In some circumstances, water-cooled stator and rotor windings may be economically justified at ratings as low as 150 MVA; conversely, water-cooled stator windings with some form of improved air cooling for the rotor may be preferred on grounds of adequacy and simplicity for ratings as high as 700 MW.

28.17.4 Excitation

Vertical-shaft generators may use main and pilot a.c. exciters mounted above the generator, or separate motor-driven exciters. These often have a flywheel to maintain the exciter speed and output during momentary interruptions or reduced voltage of the motor supply.

Static thyristor equipment is now more often used, because it simplifies the mechanical arrangement of the unit, reduces the height, and avoids the possibility of an unacceptable run-out at the top of a tall shaft assembly. Thyristors also have the advantage of inherently high response, which is valuable when the generator feeds along transmission lines.

Slip-rings and brushgear are rarely troublesome, as the peripheral speed can be fairly low; 40 to 50 m/s is a usual limit.

Horizontal-shaft generators may use brushless exciters, overhung or two bearing depending on size and speed. Again static thyristors simplify the mechanical layout, especially if the generator has a turbine at each end.

28.17.5 Pumped storage units

Pumped storage units were originally installed as peak-levelling units, running as generators at times of high system load, and as motors pumping water up to the top reservoir during light-load periods. A unidirectional set has separate pump and turbine, whereas a reversing set uses the same hydraulic machine either as a pump or as a turbine, depending on the direction of rotation.

The original purpose has been extended to provide spinning reserve and to deliver power into the system at a 'few seconds' notice to assist in maintaining stability if other generation, or a system interconnection, is suddenly lost. This introduces particular problems of thermal cycling and mechanical fatigue, especially with reversing sets, which may be required to go from full-speed pumping to reversed full-speed generating within seconds, and to do this perhaps several times a day. For reversing units, separately driven fans are usually provided because rotor-mounted fans designed for both directions of rotation have low efficiency. Water cooling may be applied, as for generators, and may be particularly valuable for the damper cage if this is used for 'induction-motor' starting.

Figure 28.33 shows a cross-section of one half of the motor/generator of a reversible pumped-storage unit rated at 330 MVA, 18 kV, 0.95 p.f., 500 rev/min. The design at this output and speed approaches the limit achievable with present materials and air cooling. The outside diameter of the stator core is 6.2 m, the rotor diameter 4.5 m and the active core length 3.6 m. The mechanical design of the rotor is dominated by centrifugal stresses and the fatigue effects of reversals. The rotating parts weigh about 440 t, and the hydraulic thrust raises the load on the thrust bearing to almost 600 t.

28.17.5.1 Starting[26,146]

Methods available for starting and run-up of a machine in the pumping mode are: (i) by a direct-coupled auxiliary starting turbine or pony motor, (ii) by back-to-back connection with another machine driven by its own turbine and acting as a generator; (iii) from the power network through a step-down transformer, using the rotor solid pole-shoes or the damper winding as a cage for an 'induction' start; and (iv) from the power network through a variable-frequency thyristor converter controlled to give an output over the range of a few hertz up to normal system frequency. In (iii) the pole-shoes or damping windings must be designed to carry the induced currents without excessive rise in temperature. In (iv) the machine moves from rest by induction torque, but at a low frequency it synchronises with the converter and thereafter remains in synchronism up to normal frequency, to be then synchronised with the power network. The starting equipment is expensive, but run-up is more readily supervised, and the damping cage (or pole-shoe) design is not constrained. The method is preferred for large units.

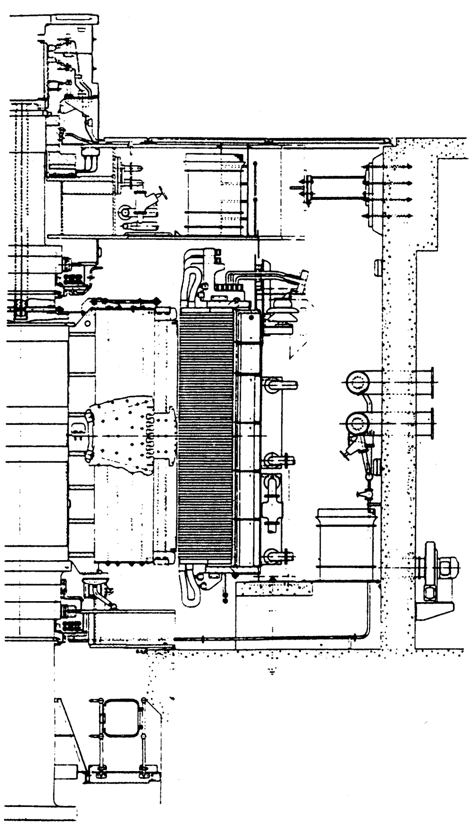

Figure 28.33 Section of a pumped-storage motor/generator

28.18 Salient-pole generators other than hydrogenerators

These are made for synchronous speeds ranging from less than 100 rev/min to 1500 rev/min for 50 Hz and 1800 rev/min for 60 Hz supplies: hence they have from 4 to more than 72 poles. Small 2-pole generators are also made in large numbers. The outputs for salient pole generators range from a few kilovolt-amperes up to about 60 MVA. Prime movers range from internal combustion engines burning petrol, gas (either natural gas or methane obtained from land-fill sites or biomass schemes) or diesel fuel and steam or gas turbines. Direct drive by an internal combustion engine is practicable over the whole range, though at 1500 and 1800 rev/min the output is limited by the maximum engine available power of about 5 MW. Typical ratings for low-speed two-stroke diesel sets are 15–60 MVA at 150–100 rev/min. Diesels, often four-stroke, in the speed range 428–1200 rev/min (14–6 poles) are particularly common, with outputs of say 10–45 MW. Four-pole generators with outputs up to about 60 MVA are driven via a suitable gearbox by high speed (up to 15 000 rev/min) steam or gas turbines.

28.18.1 Applications

Salient-pole generators are used both for stand-by applications and to continuously supply power. They can therefore be connected to public power systems, incorporated in marine installations (ships and oil rigs) or in a great variety of industrial plants. Where a *public system* requires unit ratings up to about 40 MVA, high speed, medium speed or low speed diesel sets can be installed more cheaply and more quickly than a gas or steam turbine set with its boiler and auxiliaries. The internal combustion engine set can deliver full load within a few minutes of starting, which can be done remotely if necessary; it will respond rapidly to changing load demand, and will have better efficiency at part load than the turbine. The slow two-stroke diesel is particularly suitable for the larger outputs (15–60 MVA), running economically on low grade fuel and with low maintenance costs. The recent focus of attention on emissions has had a marked effect on the choice of type of i.c. engine to be used. Engines burning gas are able to achieve lower level emissions of nitrous and sulphur oxides and are therefore proving very popular for new installations. In contrast, achieving acceptable levels of emissions with 2 stroke diesel engines burning low grade fuel is more difficult and costly.

Combined cycle installations are being increasingly used to attain higher thermal efficiency. Exhaust heat from a gas turbine that drives one generator is used to supply steam to a turbine that drives another. The turbines are high speed, geared down to, usually, the four-pole speed. For outputs greater than the gearbox limit of about 60 MW, two-pole cylindrical rotor generators are directly coupled to the turbines.

For *marine applications*, high-speed or medium-speed generators are used. These can either be driven by dedicated diesel engines, shaft driven from the engine used for propulsion or, in larger installations by gas turbines.

For *ship propulsion*, medium-speed diesel or gas turbine driven generators supply d.c. through rectifiers, or variable frequency a.c. to motors coupled to the propellers. The domestic load may be supplied from the same generators, but often separate generators are used—this has the advantage of isolating the domestic supply from the converter harmonics.

Oil rigs can either use internal combustion engine-driven generator sets or gas-turbine-driven generators for their power depending on their size. The generators are required to supply the large drilling motors and mud-pump motors, and the domestic supply. The number, rating and reactances of the generators are chosen to avoid excessive voltage dip when a large motor drive is started, so that other loads are not badly affected. Rated outputs can be around 15 MW where gear-driven generators running at 1800 rev/min would be used or up to 25 MW directly coupled to the power turbine at 3600 rev/min.

Industrial power stations commonly use steam turbines if there is need for process steam, which is delivered through a back-pressure or pass-out turbine; or if waste heat is available to raise steam for generating electricity.

Gas and diesel engines are often used, alone or in combined cycle installations. Gas turbines are expensive to run, and are used only if rapid response to load is essential, or the fuel is locally not so expensive, e.g. in an oil refinery.

28.18.2 Construction

The *rotor construction* for these salient pole machines is determined by the rating of the unit and its peripheral speed. High-speed rotors with four or six poles and ratings up to about 30 MW can have integral poles which are made up of a series of laminations. These can be punched from sheet material using a die or a nibbling type press; alternatively, they can be manufactured using a laser cutting machine. The thickness of punchings is normally limited to less than 3 mm if one of these processes is used, which has the advantage of limiting the pole-shoe losses. Thicker plates up to 100 mm thick can also be used, in which case the pole profile is cut using a numerically controlled flame cutting machine. With this type of construction, due consideration must then be given to the extra surface losses which will occur in the pole-shoes.

For larger machines, i.e. above 30 MW, with bigger diameter rotors, the peripheral speeds will result in rotational stresses in excess of the safe limits for the laminated type of rotor materials. In this case, forged solid rotors with pole pieces integral with the shaft are used. Solid forged pole-shoes are secured by high tensile strength bolts after the coils have been put on.

For the lower peripheral speeds associated with multipole rotors, separate laminated pole pieces may be secured by dovetails or T heads in slots in the square or hexagonal middle portion of the forged shaft. Alternatively, the laminated poles may be bolted or dovetailed to the wide cylindrical rim of a flywheel on the shaft; or they can be fixed by dovetails or T-head slots in a laminated rim.

The laminated-pole construction is cheaper than a forged rotor, and reduces the cost of the stator winding too, because fewer, wider, stator slots can be used with acceptably low eddy current losses in the pole faces.

Generators may have one or two *bearings*. Many diesel driven generators have only an outboard bearing, the driving end of the shaft being solidly coupled to the engine crankshaft flange. This shortens the set and saves the cost of a drive end bearing and the flexible coupling. It also allows the generator to be partly supported on a Society of Automobile Engineers (SAE) flange on the engine crankcase. The crankshaft bearing must carry half the weight of the generator rotor. A gearbox bearing can rarely do this, so gear-driven generators normally have two bearings.

The generator may be supported on sole plates grouted into the foundations, or may be bolted directly to the engine base plate, where this has been designed for the purpose. If transport and site lifting facilities permit, the generator can be delivered complete on its own bed plate; this is to be preferred as it saves erection time on site.

Often it is desirable to limit the vibration transmitted to the foundations, especially when the driver is a diesel engine. A common method is to put flexible mountings beneath the combined bed plate. These need to be chosen, or tuned, to isolate the vibration and not to permit resonance at any of the disturbing frequencies.

Class F *insulation* is always used on high voltage stator and some rotor windings, although class B temperature limits are often specified to extend the life of the machine and give some margin for an overload capability. Class H systems are common on low voltage generators and also on some medium voltage machines. Often class F temperature rises will still be specified even for machines with class H insulation systems in order to achieve an extended life.

Stator coils are usually of the pulled diamond type (see Section 28.4.1). Rotor coils are generally similar to those of hydrogenerators, but as they are smaller they are more often of the wound on edge construction using rectangular strip copper rather than fabricated. Where the rotor has integral poles, the field copper is wound directly onto the pole pieces by rotating the complete rotor or the laminated rotor core-pack about its longitudinal axis.

28.18.3 Ventilation and cooling

These generators are always air-cooled. If an adequate supply of clean air is available, open ventilation with an appropriate class of protection is used. If the air is only slightly dirty, it may be ducted to and from the machine, with filters on the inlet side. In a short machine the air is drawn through from one end to the other by a single centrifugal shaft-mounted fan. Longer machines have radial ducts in the stator core; the air enters the stator bore at both ends and is expelled radially through the ducts to the back of the core. In this case, shaft mounted axial-flow fans are used to drive the air through the machine.

In dirtier surroundings a totally enclosed machine is used. The primary (internal) air is circulated by shaft fans through an air to air heat exchanger (c.a.c.a. arrangement). The secondary (external, ambient air) is driven through the heat exchanger by one or more separate motor driven fans. Alternatively a water-cooled heat exchanger is used (c.a.c.w. arrangement). The materials used in the heat exchanger must be chosen to suit the quality of the water, e.g. tubes of aluminium brass or cupro-nickel if the water is salty. The limits of temperature rise may be adjusted in accordance with the maximum temperature of the secondary coolant. (see Section 28.5.)

28.18.4 Particular design requirements

28.18.4.1 Machines for operation in hazardous atmospheres

As a result of a vast increase in power generation for the oil industry, and a few catastrophic accidents, generators are required to be designed to operate safely in potentially hazardous atmospheres. BS EN 60079-10:1996 defines the different levels of hazard. Generators, especially when driven by gas turbines, are considered to operate in a zone-2 area. They must be designed and made to prevent any gas that is present being ignited, and compliance with the requirements must be certified by a type-N certificate granted by one of the nominated inspecting authorities. The requirements are specified in BS 5000-16:1997. They are many and complicated, but the major ones can be summarised thus. For a particular group of gases, the surface temperatures of all bare live parts must not exceed an ignition temperature defined for that group. Mechanical clearances on fans, including separate motor-driven fan units, must be greater than usual to prevent any contact between the stationary and the rotating parts, to avoid sparking or excessive local temperatures. Auxiliary electrical devices, including terminals, heaters, fan motors, etc., must all be of certified origin, with certificates of conformity to demonstrate that they comply with the requirements.

28.18.4.2 Reactances

X_d, X'_d and X''_d are those that have most influence on the design and the operation of the generator. A maximum X_d or minimum short-circuit ratio is usually specified. A minimum value of X''_d or a maximum X'_d are either specified or implied by specifying the permissible fault megavolt-amperes or the grade of voltage regulation required. For example, BS 4999-140:1987 specifies voltage regulation grades in terms of

(1) accuracy of voltage control under steady load;
(2) voltage dip when specified loads are suddenly applied; and
(3) voltage rise when full load is suddenly removed.

This performance is with the automatic voltage regulator in control of course, but implies lower X'_d to achieve the smaller voltage dips and rises. As implied in, Section 28.6.1.4 it becomes expensive to specify a closer grade of voltage regulation than is really needed. The grade may be unattainable if X''_d is high to limit the fault megavolt-amperes.

28.18.4.3 Generators driven by internal combustion engines

ISO standard 8528 (equivalent to BS 7698:1993) entitled 'Reciprocating internal combustion engine driven a.c. generating sets' is applicable to generators used in these applications. Most of the standard is concerned with the engines, controls, etc., but Parts 3, 5 and 9 contain requirements for the generator, including definitions and limit values of parameters to do with voltage control.

The mechanical design of such sets must consider the transverse vibration and critical speeds of the shaft assembly, and also the possible torsional modes. BS 5000-3:1980 and ISO 8528 place the responsibility for seeing that the torsional behaviour is investigated with the supplier of the set, assisted by the makers of the engine and the generator. Some cyclic irregularity of the generator speed is inevitable: the rotor inertia may be enough to keep it acceptably small, but an extra flywheel may be needed.

Cyclic irregularity of torque will occur at frequencies determined by the number of firing impulses per second. In low-speed sets (100–150 rev/min) one of these harmonic frequencies may lie near to 10 Hz, and around this frequency the eye is very sensitive to light flicker caused by fluctuation of voltage as small as 0.5%. The fluctuation may affect the operation of electronic equipment, mains ripple control systems, etc. If it is impracticable, or too expensive, to reduce the speed fluctuation by adding more inertia, the voltage swing can be reduced with a high response excitation system, phase-controlled to swing the generator flux in opposition to the speed.

If the cylinder torques are not all equal, there will also be torque fluctuations at rotational frequency or low multiples of it, i.e. in the frequency range 1 ½ to 4 ¼ Hz. For most sets the natural frequency of electromechanical oscillation relative to the power system lies in this range. The resonant, or near-resonant, swings of load angle, power, voltage, etc., may nevertheless be tolerably small if the generator has a sufficiently effective damper winding.

28.19 Synchronous compensators

Synchronous compensators are synchronous motors running without mechanical load; they are used to generate or absorb reactive power, in order to control the voltage of a power system. Hence they are usually installed near a load, or part way down a long transmission line to support the voltage at the intermediate point.

At times of heavy load, compensators run overexcited to supply the magnetising power demanded by the load (transformers and induction motors) or the inductive I^2X losses of the line. At light load they must run underexcited to take reactive power from the line to offset the capacitive line-charging current and so avoid excessive voltage rise. In many h.v. systems (200 kV and above), the line capacitive power exceeds the load magnetising power, even at times of heavy load.

Static inductors and capacitors, switched to suit the system conditions, can be used for the same purpose, but the synchronous compensator has the advantage of providing continuously variable control, and with thyristor excitation it can have a response fast enough for many contingencies.

Thyristor-controlled static compensators give rapid and continuously variable control, but require filters to limit harmonic generation in the power system. Lower maintenance and running costs give them an advantage over rotating machines in most new installations.

Synchronous compensators up to 300 MVAR are in service. Air cooling has been used up to ratings of 40 MVAR, but hydrogen cooling is now normal to reduce the size and the light-load losses, the latter by reducing windage. As the shaft-end need not emerge from the hydrogen-tight casing, no shaft seals are needed. Losses at full load (overexcited) are in the range 0.01–0.016 MW per MVAR of rating.

The underexcited capability is usually about half the overexcited rating; for this a short-circuit ratio of about 0.75 is desirable, to ensure that at the underexcited capability the rotor has sufficient positive excitation to maintain stability. A short-circuit ratio of 1.3–1.5 will provide an underexcited capability level equal to the overexcited level, but the machine is larger and has higher losses than the design with the lower short-circuit ratio. Water cooling has been used for stator and/or rotor windings at ratings of 200 MVAR or more.

The compensator may be run up to speed as an induction motor through a step-down transformer, by means of a direct-coupled pony motor, or by using a variable-frequency inverter.

28.20 Induction generators[157–164]

If an induction motor is driven above synchronous speed it will deliver power to the system, with a slip of about −0.05 at full load. It has the advantage of simple construction, and needs no excitation, speed governing, or synchronising. This makes it cheaper than a synchronous machine and operationally more convenient, e.g. for unattended hydrostations or wind-driven generators. The disadvantage is that it must draw from the power system magnetising power of 0.5–0.75 of its rated active power output, and this has limited the size to about 5 MW.

Research in the USA has shown that, by using static var compensators to supply the reactive power, it should be practicable to run induction generators of a few hundred megawatts output either in parallel with synchronous generators or even as a separate supply system. Speed control would then be essential to fix the frequency of the separate system.

Connection to the power network can be made merely by closing the breaker when the machine is up to synchronous speed. To reduce the current surge in large machines, the machine can be allowed to build up to normal voltage by first connecting a capacitor and then synchronising in the usual way. By suitable design of the machine and the static compensator, efficiency and stability can be comparable to those of a synchronous machine.

The most suitable locations appear to be where transmission by h.v. cable is required, for the cable capacitance will contribute to the reactive-power requirement, and at points in the system where substantial var support is installed anyway, the generator being run when active power is also required.

To maintain stability following system faults requires considerable reactive-power capacity beyond that needed for steady full-load operation. Where the system is strong enough, however, this is not too costly, and the total cost of the induction generator installation can be less than that of a synchronous unit.

28.21 Standards

A selection of standards relevant to generators published by ANSI, BSI, Cenelec, IEC, IEEE and NEMA is listed below. Revisions are made every few years, so care must be taken to use the most recent issue, or an earlier one if that is relevant. Member countries of the European Economic Community are required not to have national standards that conflict with Cenelec Euronorms or Harmonised Documents. Some of these documents are technically equivalent to, or closely similar to, IEC standards that were adopted as the bases for harmonisation. British Standards are technically equivalent to relevant Cenelec documents, or to IEC standards if a corresponding Cenelec standard has not been published. BS EN 60034 parts 1 to 22 are numbered consistently with IEC 60034–1 to 60034–22. For a full comparison, see BS 4999: Part 0. The parts of BS 5000 relate to machines of particular types or for particular applications, and call for parts of BS 4999 where appropriate.

No.	*Title*	*Corresponding*	
		IEC 60034	*CENELEC 34HD*
BS 4999			
Part 102	Methods of determining losses and efficiency from tests	Part 2 and 2A	—
Part 104	Methods of test for determining synchronous machine quantities	Part 4	—
Part 144	Specification for the insulation of bars and coils of h.v. machines	—	345
BS EN 60034			
Part 1	Rating and performance	Part 1	
Part 3	Specification for turbine type synchronous machines	Part 3	—
Part 4	Methods for determining synchronous machine quantities from tests	Part 4	—
Part 16	Excitation systems for synchronous machines	Part 16	
Part 22	A.c. generators for reciprocating internal combustion (RIC) engine driven generators	Part 22	
ANSI C50			
Part 10	General requirements for synchronous machines		
Part 12	Requirements for salient-pole synchronous generators and generator/motors for hydraulic turbine applications		
Part 13	Requirements for cylindrical-rotor synchronous generators		
Part 14	Requirements for combustion gas turbine–driven cylindrical-rotor generators		
Part 15	Requirements for hydrogen-cooled combustion gas turbine-driven cylindrical-rotor generators		
ANSI/IEEE			
115	Test procedures for synchronous machines		
115A	Standard procedures for obtaining synchronous machine parameters by standstill frequency response testing		
421.1	Definitions for excitation systems for synchronous machines		

Cont'd

421A	Identification, testing and evaluation of dynamic performance of excitation control systems
NEMA MGI	
Part 32	Synchronous generators
Part 33	Definite purpose synchronous generators for generating set applications

Copies of standards can be obtained through the addresses shown for the standards authorities in Chapter 49; BSI, ANSI and IEEE documents are available directly from, respectively:

BSI Sales, Linford Wood, Milton Keynes MK14 6LE, UK

ANSI, 1I West 42nd Street, New York, NY 10036, USA

NEMA 1300 North 17th Street, Suite 1847, Rosslyn, VA 22209, USA

IEEE Service Centre, 445 Hoes Lane, Piscataway, NY 08854, USA

Most IEC standards are available from BSI Sales.

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IEEE for information from standard Standard 421.1 used in Section 28.14.

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