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CURRENT USAGE & SUGGESTED PRACTICES IN POWER SYSTEM STABILITY SIMULATIONS FOR SYNCHRONOUS MACHINES

— PREPARED BY THE TASK FORCE ON DEFINITIONS — AND PROCEDURES

IEEE JOINT WORKING GROUP ON DETERMINATION AND APPLICATION OF SYNCHRONOUS MACHINE MODELS FOR STABILITY STUDIES

JOINT WITH POWER SYSTEM ENGINEERING & ROTATING MACHINERY COMMITTEES
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A. INTRODUCTION

This paper is being produced as a follow-up to the Symposium on "Synchronous Machine Modelling for Power System Studies', (IEEE 83 TH0101-6-PWR). In the symposium publication, discussed at the IEEE-PES Winter Power Meeting in 1983, the Joint Working Group, through a series of individual papers, gave an overview of various approaches which have been used, or are currently being developed, to produce parameters for stability models. The complexities possible in model availability were covered only briefly. Furthermore, the limitations in parameters obtained from data, obtained using either "Standard" methods, or using newly proposed methods, were not investigated or delineated in any depth.

Another important factor, not given much treatment at the Symposium, was how saturation should be treated in stability studies. It has been customary to consider a "total" saturation during the initialization stage of stability studies, and also during subsequent step by step calculations in time domain simulations. However, the application of saturation factors to "Unsaturated Models" had not been clearly demonstrated or fully justified in any of the Symposium articles. Also the effects of incremental changes in permeability (or saturation) have been covered in very few publications. The consideration of such effects in small signal or linearized stability analyses has been given limited recognition or study.

The Joint Working Group feels that the above issues should be brought to the industries' attention, and we welcome comments from both "producers" of stability data, as well as from the many "users" of such data. It is the Joint Working Group's objective, in accordance with its scope, to eventually produce a recommended set of Guidelines for using various models in different types of stability studies, along with concordant procedures for obtaining data for such models.

85 WM 152-4 A paper recommended and approved by the IEEE Power System Engineering and Rotating Machinery Committees of the IEEE Power Engineering Society for presentation at the IEEE/PES 1985 Winter Meeting, New York, New York, February 3 - 8, 1985. Manuscript submitted October 19, 1984; made available for printing December 12, 1984.

As a prelude to discussing some of the issues of prime interest to those concerned with the appropriate use of stability models, the Task Force believes the principal areas of interest for generator electrical modelling can be categorized into four general areas (I) through (IV) noted below: (Only two of these areas, ie, II and III will be covered in detail in this current paper).

(I) Short circuit, faults, and relay application studies. Parameters are required to obtain initial R.M.S. current values or R.M.S. values of current after subtransient currents have decayed. More complex studies can be performed including determination of dc offset values for specifying breaker ratings more precisely.

(II) Stability Studies (Large Disturbances, Non-Linear). Such analysis can include various types of time domain stability studies. Included are such examples as:

. First angular swing, no exciters, no saliency (constant voltage behind a fixed reactance). This approach, once common, is now used infrequently for particular investigations of generating station stability limits. However, it is often used to approximate the transient time-angle response of machines or groups of machines electrically remote from the principal area of investigation. The inertial effects of such remote machines are important.

. First angular swing, with saliency represented, both in the steady state and transient state (constant field flux linkages in the direct axis). This is an extension of the more commonly used approach described above. It is seldom used, since the approximations made for the "constant voltage" approach provide acceptable results.

. Two or three time-angle rotor swings are often calculated principally of dominant machinery frequency, including subtransient effects in each axis. Exciters, with varying magnitudes in the value of their main time constants (> 0.1-1.0 seconds) are usually simulated. Saturation has been represented in the d axis. With the advent of digital computers, this type of simulation became popular, even for large scale studies where the stability of one power system, or large area, with respect to its interconnected neighbours, was of concern.

- Complete stability representation of many machines for multi-machine multi-swing cases, where accurate representation of all system damping is necessary. Separate d and q axis saturation can be used for studies of power system oscillations in the 0.1-10 Hz range.

Accurate values of rotor self and mutual inductances are important, especially with small time constant, (< 0.1 second) exciters, which require additional stabilizing signals. Up to 3 rotor windings can be represented in each axis. This type of investigation is used to determine inter-area power oscillations, as well as to more precisely determine the effect of excitation systems and power stabilizers in improving inter-system and intra-system damping.

(III) Stability Studies, (Small Signal-Linear)

In general, the same requirements apply for modelling as in the previous paragraph. The number of state variables to be considered will increase in going to larger power systems, and more detailed model representations. Saturation in linear analysis is important and has usually been handled as an incremental concept, about some initial operating point on the direct and quadrature axis saturation curves. Eigenvalues and eigenvectors are the outputs of solutions to the state space equations. From these eigenvalues and eigenvectors several types of useful information can be extracted. For example, time responses can be obtained on the effects of a stepped input to the error summing junction of an excitation system. Frequency responses, in which synchronous machine stator outputs such as voltage or the variations in speed of the rotor, both as a function of field voltage perturbations, can be plotted over a range of frequencies from, for example, 0.1 to 10 Hz. The eigenvalues can be used to plot small signal stability loci of constant damping for various megawatt outputs of a generating station. Reductions in such stability limits as a function of external system reactance, for fixed generator terminal voltages is a useful guide in determining stabilizer and excitation system gains and time constants.

(IV) Sub-Synchronous Resonance (SSR) Studies

A typical SSR study must deal with two different machine-system interactions. The first is the interaction between the electrical system and the machine-shaft torsional system. The second is the "induction motor" or "induction generator" action of the synchronous machine. In general, these phenomena are coupled and therefore must be treated together.

The range of frequencies of interest is fairly broad. Rotor frequencies due to series resonance in the system electrical network are the sum of the system frequency plus or minus the resonant frequencies. (A specific example might be 60 Hz up to ± 50 Hz that is, from 10 Hz up to 110 Hz). A knowledge of the rotor iron circuit and amortisseur responses as a function of frequency is vital in the above noted frequency ranges.

Only issues of concern relating to items (II) and (III) above will be dealt with here.

In spite of the wide range of situations which can arise in stability studies, there is a fairly limited set of machine model structures from which to choose. This is particularly true for the present variety of "Standard" large-size stability programs currently available. In such programs, the use of fixed parameters to describe the dynamic performance of nonlinear devices, such as synchronous machines, has generally been recognized. This statement applies to hydro machines, and much more to solid rotor turbine generators.

The Task Force, for the purposes of the current paper has decided to categorize their ensuing comments in four broad areas:

- <u>Practical ranges</u> in model availability (Section B).
- <u>Obtaining data</u> from which parameters may be derived for several of the models categorized above (Section C).
- <u>Limitations in data</u> obtained by test procedures or through analytical means (Section D).
- <u>Rationale</u> for recommended model selection, and saturation algorithms to be considered (Section E).

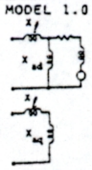
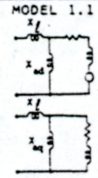
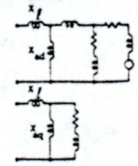
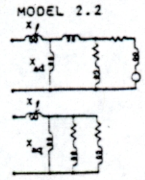
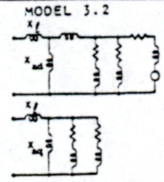
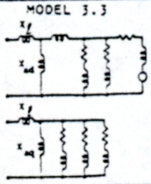
A fifth area in Section F deals briefly with various computer programming approaches to model structure representations, and the associated power system interfacing computational routines. However, a complete coverage of the two principal programming approaches either "time constants plus reactances", or utilization of Resistances and Inductances from models, would require as much space as the suggestions regarding model structures, or the associated parameter limitations or requirements. As a consequence, only a short outline of the two programming approaches will be noted, but IEEE technical paper references are given to aid those interested in further pursuing this aspect of stability simulations.

B. <u>PRACTICAL RANGES IN MODEL AVAILABILITY</u>

Consider the matrix shown as Table I, which can be conveniently chosen for describing models ranging from first order to third order in terms of the roots of the characteristic equations which describe their transient performance. In this complete matrix there are 12 possible combinations of direct <u>and</u> quadrature axis representations, plus one "constant flux linkage" d axis model. The most complex (model 3.3) would have a field winding and two equivalent rotor iron (damper) circuits in the direct axis, and three quadrature axis equivalent (damper) windings. Some combinations of d and q axis winding configurations are not considered in Table I, and equivalent circuit structures are not drawn or discussed. Based on the experience of the Task Force, as well as on general intuition, we believe there are seven model structures which could be serious candidates for inclusion in large system stability simulations. Six of these models are drawn in Table I, and the seventh is the constant flux linkage (or constant voltage back of transient reactance) model.

DEFINITION OF GENERATOR MODELS OF VARYING DEGREES OF COMPLEXITY

TABLE I

D-AXIS ↓ / Q-AXIS →	NO EQUIV. DAMPER CIRCUIT	ONE EQUIV. DAMPER CIRCUIT	TWO EQUIV. DAMPER CIRCUITS	THREE EQUIV. DAMPER CIRCUITS
CONSTANT FLUX LINKAGES	MODEL 0.0	—	—	—
FIELD CIRCUIT ONLY	MODEL 1.0 	MODEL 1.1 	1.2	1.3
FIELD CIRCUIT + ONE EQUIV. DAMPER CIRCUIT	2.0	MODEL 2.1 	MODEL 2.2 	2.3
FIELD CIRCUIT + TWO EQUIV. DAMPER CIRCUITS	3.0	3.1	MODEL 3.2 	MODEL 3.3 

The possibility exists, theoretically, in the direct axis, when discussing the most complex models, for considering two additional "differential leakage" reactances. These reactances represent fluxes which link the field and the equivalent rotor body paths, but do not link the stator windings. The structures are shown in Figure 1 for model 3.3. The nomenclature for the elements of the models in Figures 1 and 2 is a logical extension of the element descriptions in Figure 4. For the quadrature axis, of course, the subscript 'q' has replaced the subscript 'd'.

When discussing the order of a model one should also consider the electromechanical "swing" equations. However for the purposes of this section, which deals with the generator electrical aspects of stability models, No. 3.2 or No. 3.3 are third order models, and No. 2.1 or No. 2.2 are considered second order models.

In practice, as noted above it is often appropriate to consider just one differential leakage reactance in any of the models as noted in the direct axis representation of Figure 2. These reactances are intrinsically the differences between field to rotor mutual reactances and field to stator mutual reactances. Such mutual reactances are, in the reciprocal (X_{ad}) system of Rankin, relatively large and close to each other in value. Use of X_{f2d} or L_{f2d} implies that current and associated flux paths between the #1 and #2 direct axis equivalent rotor body circuits can be easily identified. This is not usually the case, so that the flux paths and the associated mutual and leakage reactances between these equivalent circuits and the field winding cannot be readily singled out. Furthermore, most model identification studies to date, based on fitting model 3.2 or 3.3 to direct axis operational inductance data, yield very small values for L_{f2d} or X_{f2d}.

Therefore, model 3.3, including the one differential leakage branch as shown in Figure 2 is the most complex which we feel needs to be coded for large size stability programs. The q axis representation in Figures 1 and 2 are identical. It should be noted that one differential leakage reactance could exist in models 2.1 and 2.2 in Table I, for the direct axis structures as well.

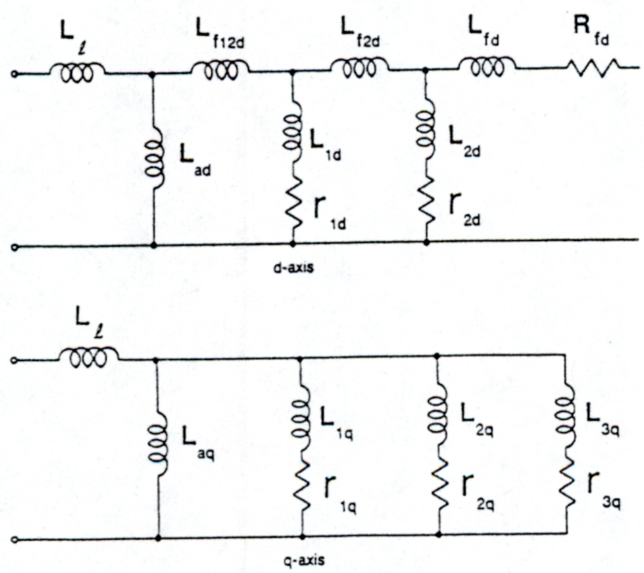


FIGURE 1
COMPLETE, THIRD ORDER REPRESENTATION BOTH AXES

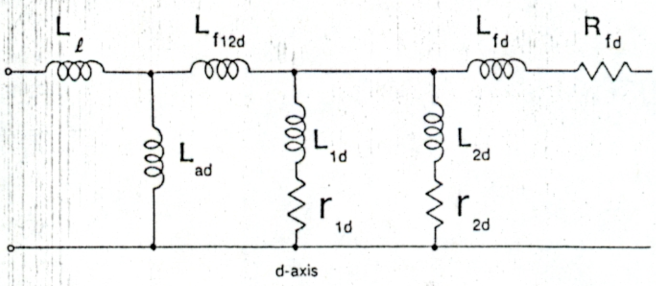


FIGURE 2
MODIFIED THIRD ORDER REPRESENTATION FOR D-AXIS

A popular and widely used structure in many current programs can be described, based on model 2.2, and which considers two windings in each axis, including the direct axis field winding. This model structure is a so-called "state of the art" version, for which data has been, by and large, supplied by manufacturers of synchronous machines, or has been obtained by tests described in IEEE Standard No. 115 (1983). Two time constants and three reactances (X_d, X'_d, X''_d) have been employed to describe the response of model 2.2 in each of the d and q axes. The model is shown here in Figure 3.

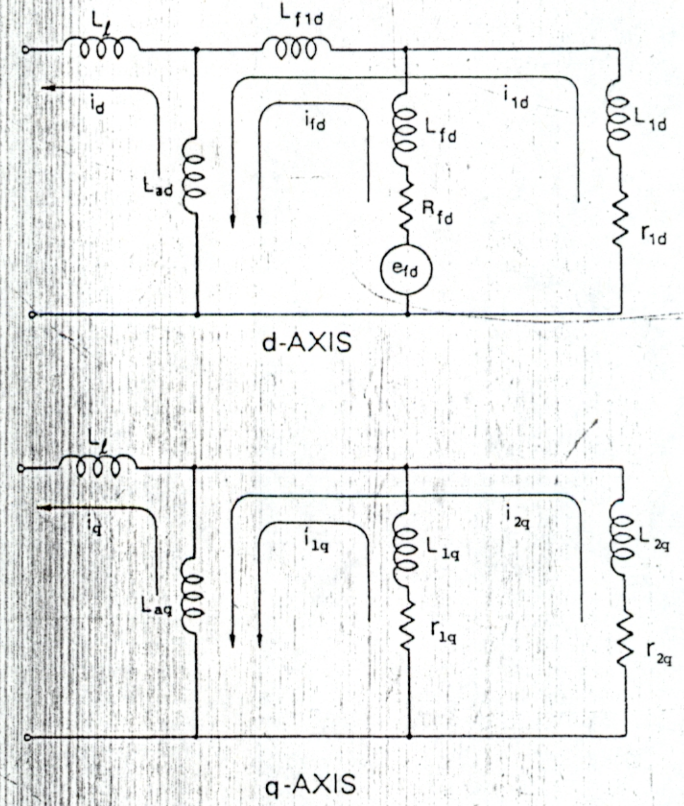


FIGURE 3
COMPLETE SECOND ORDER REPRESENTATION BOTH AXES

Methods of calculating or testing for data for model 2.2, for both d and q axes, are described in Section C.

Models 3.3 and 2.2 embrace the upper and lower limits of complexity in dealing in any degree of detail with turboalternators, although model 2.1 has sometimes been employed for such purposes. Models 2.1 and 1.1 are widely used in hydro generator stability studies. More discussion on model application follows in Section E.

Model 1.0 is the other model in popular use, where no second order or "subtransient" effects are considered, in either the d or q axis. Model 1.0 is often used in conjunction with some type of excitation system representation, where accounting for field flux linkage changes is required. When the so-called classical model, "voltage behind transient reactance" (E' = constant) is utilized, saliency is neglected. This is a variation of the constant flux linkage model No. 0.0, where E'_q is constant.

C. OBTAINING DATA FROM WHICH PARAMETERS MAY BE DERIVED FOR SEVERAL OF THE MODELS DESCRIBED IN SECTION B

C.1 General Testing

Since the most widely recognized model is that of 2.2, which requires parameters corresponding to a second order characteristic equation, discussions on the historical methods of obtaining test data for this "two rotor-winding" model are noted below. Some of the material in this section is a condensation of the IEEE publication 83TH0101-6-PWR- "Symposium on Synchronous Machine Modelling for Power System Studies". Two considerations exist here -- direct calculation of parameters, and as an alternative, performing tests to obtain data from which parameters are derived. (The limitations of either approach will follow in Section D.) Testing will be discussed first.

C.1.1 Testing for Data - Short Circuits

IEEE Standard No. 115 (1983) describes in some detail the short circuit tests which have formally been in place since 1945, commencing with AIEE Test Code No. 503, June 1945. This latter document was replaced by the Standard in 1965, which in turn was revised in 1983.

A typical test from the Standard would consider a three phase short circuit, applied to the terminals of a synchronous machine which is running at rated speed, on open circuit. The voltage on open circuit can be chosen at any value consistent with machine specifications. For reactance determination, the test generally is performed for several open circuit voltages, in a range typically up to about 0.5 to 0.6 pu of rated terminal voltage.

The changes in peak to peak armature current magnitudes are noted from some form of oscillograph record, as a function of time. These magnitudes are then usually plotted on semi-logarithmic paper, and generally two slopes can be identified. The projection of such slopes to zero time (the application of the short circuit) will then identify an initial magnitude of current, which, when divided into the voltage magnitude before the fault, gives an inductance or reactance. A second, decaying, linear component from the semi-logarithmic plot gives a second reactance, when projected back to zero time. The initial, smaller value is the subtransient reactance, and the second, larger value is the transient reactance.

These procedures noted in IEEE Standard No. 115, give also, based on the slopes of the semi-logarithmic plots, two "short circuit" time constants: - T'd, T"d.

The circuit as described in model 2.2 is chosen to represent the machine response in the direct axis. There is no description of similar test procedures in IEEE Standard No. 115 which will produce corresponding quadrature axis values. Measurements of d axis quantities can be further refined by noting changes in induced field current, after the application of the three phase fault. This has been commented on in a paper by Shackshaft.[4] Note that this basic model, derived from a short circuit test, is presumed to be acceptable for a broad range of system or machine transient situations.

C.1.2 Testing for Data by Decrements

A second approach to obtaining data for models such as 2.2 is the "decrement" approach described by Shackshaft and De Mello[1,2], and also described by Shackshaft and Poray[3] in IEE publications in Great Britain. The method in general involves the sudden interruption of armature current in a synchronous machine connected to the power system. In the De Mello approach, the machine currents must be interrupted under two initial operating conditions (i) $i_d = 0$, and (ii) $i_q = 0$. The conditions can be achieved by underexciting or overexciting the machine at some percentage of full load. Achieving an exact loading condition for either $i_d = 0$ or $i_q = 0$ appears to be unnecessary providing an accurate measurement of rotor angle is available.[5]

In Shackshaft's testing methods, in use at the English Central Electricity Generating Board, the machine is at or very close to zero MW load. This zero MW load condition can be achieved when the unit has just been synchronized to, or is about to be removed from, the power system. Field excitation is reduced to zero to obtain an initial value of i_d. For the tests involving the direct axis, with the field shorted, or at zero voltage, the synchronizing or reluctance torque is proportional to

$$\frac{E_b^2\ (X_d - X_q)\ \sin 2\delta}{2(X_d + X_e)\ (X_q + X_e)}$$

where δ is the angle from the quadrature axis to some external point Eb. When such a point is the low voltage side of the step-up transformer (ie, generator terminals), X_e would be neglected in the above equation.

This expression results in small values of torque near zero degrees internal angle, and the stator currents, corresponding to E_t/X_d, are then interrupted to obtain the decrement plots of terminal voltage and any desired field quantity changes. For tests involving the quadrature axis, with the field preferably open, steam conditions are adjusted to increase the internal angle. It is desirable to go beyond the peak MW electrical output which occurs (under zero field voltage conditions) at 45 degrees internal angle, up to a value of about 90 degrees internal angle. With this situation, stator currents corresponding to E_t/X_q are then interrupted to obtain quadrature axis decrement values.[3]

C.1.3 Testing for Frequency Response Data

A third approach to obtaining models by tests involves standstill or on-line Frequency Response Testing. Such testing yields a range of models from 1.1 through 3.3, depending upon the interpretation of the data. The details of the testing method are also covered in IEEE 83 TH101-6-PWR, as well as in an EPRI Report.[6]

Since this is a relatively new approach, some brief comments on standstill frequency response testing methods, and the interpretation of the results are first noted. The models so obtained are small signal models because of the magnitude of the measuring signals. The behaviour of the generator at standstill is most nearly described by the incremental permeability of the rotor iron. As such, the values of $L_{ad}(0)$ and $L_{aq}(0)$ obtained from the 'zero-frequency' intercepts of the operational inductance curves will be incremental values. For such conditions, the incremental permeability at zero biasing flux density is substantially coincident with the normal permeability at the toe of the normal B/H curve. The "patching in" of L_{adu} from the air gap line in the d axis model results in a relatively minor correction being made to $L_{ad}(0)$. The value of L_{adu} from the air gap line is substituted for $L_{ad}(0)$.

At the time of publication, this increase, based on test results from eight or nine machines, has amounted to somewhere between 8% to 18%, and the average is about 12%. These values of L_{adu} from the air gap line are subsequently corrected for steady state saturation in most stability programs in the initialization processes. This correction is a function of the generator MV.A loading, power factor, and terminal voltage. The same comments also apply to the values obtained for $L_{aq}(0)$. It is currently assumed that the correction factor for the quadrature axis is proportional to the $L_{adu}/L_{ad}(0)$ correction factor. This correction for variations in incremental permeability is also discussed in greater detail in IEEE Standard 115A-1984.

The actual derivation of the model elements values, from frequency response testing results, is discussed in limited detail in Section A.5 in the Appendix of IEEE Standard 115A-1984. That section in general deals with obtaining models from standstill test data. One approach used in Section A.5, but not the only one possible, starts with a choice of circuit form for both axes. Figure 1 or Figure 2 in this paper would be an example of this. L_ℓ is chosen and $L_{ad}(0)$ is then calculated. R_{fd} is calculated from the armature to field transfer impedance measured at standstill. More precisely R_{fd} is determined from the slope of sG(s) as 's' tends to a zero value. The remaining elements are calculated by assuming some starting value for them, and calculating the error between the frequency response of the resulting equivalent circuit and each measured test point. The value of each undetermined element is then changed by a small amount, and if the error between calculation and test is reduced, the process is continued until the error begins to increase again. The process is repeated for all other undetermined elements until the error at each test point between calculation and test cannot be reduced further.

The process of adjusting models based on OLFR testing has been reported from EPRI[6] and in the literature.[14] This work concerns two Ontario Hydro generators at Lambton and Nanticoke generating stations. A summary of these procedures is also contained in the Symposium proceedings referred to earlier. One conclusion arrived at so far is that the SSFR model is a rational starting place for verification of the OLFR test procedures. As reported in Reference 6, the SSFR model for Lambton required very little adjustment to match the OLFR test data for that generator and the SSFR model gave accurate simulations of line switching tests. Some significant adjustment was required for the other test generator at Nanticoke. As reported elsewhere the type of rotor construction and pole face configuration varied considerably between these two 500 MW units. It is felt that further investigation into this area is necessary before definite recommendations about OLFR, or open circuit, rated speed, frequency response test procedures can be made. These are currently underway.

C.2 Parameters Derived by Calculation

C.2.1 "Standard-Based" Parameters

Most manufacturers provide parameters for d and q axis models based on model 2.1 or 2.2. North American manufacturers of turbogenerators, (principally Westinghouse and the General Electric Company) provide calculated values of reactances and time constants which characteristically or traditionally have been called transient or subtransient constants (in addition to the "steady state" constants).

Such values of the direct axis reactances and time constants are based on tests which are described in Section 8 of IEEE Standard No. 115 (1983). These tests are conducted under open circuit or short circuit conditions, and the calculated values of these parameters provided by the manufacturers should duplicate, under computer simulations, the decay or decrement of voltages or currents after circuit conditions in the field or stator have been suddenly changed. Values so provided are also subclassified into 'rated voltage' or 'rated current' quantities.

Tests for quadrature axis transient and subtransient values similar to those in IEEE Standard No. 115 (1983) for the direct axis, are impractical to conduct, and are not even described in that Standard. It should be noted that both direct and quadrature axis open circuit, short circuit, subtransient and transient quantities are defined in IEEE Standard No. 100 (1984) (IEEE Dictionary). The definitions so listed do not necessarily lead to practical methods of conducting tests to determine specific values or quantities associated with the definitions.

For turbogenerators, manufacturers mentioned above derive or calculate a value of transient quadrature axis reactance based on an assumed excitation of the stator with the resultant armature magnetic (flux) axis aligned with the rotor interpolar space. The exciting frequency is somewhere between 0.6 Hz and 1.0 Hz, with an assumed 1.0 pu armature exciting current.

The impedance so derived yields values described in terms of X'_q and T'_q. Other values of quadrature axis 'open circuit' and short circuit time constants and reactances are derived from the above knowledge of X'_q and T'_q, and some designers assume as well that $X''_q = X''_d$. This manner of derivation, in relating the transient values to the subtransient values, and open circuit values to short circuit values, is analagous to that used in the direct axis, the latter formulations being described in Section 8 of IEEE Standard No. 115 (1983), as noted above.

Since in hydraulic machines there is often a well defined electrical path in the interpolar space, due to the presence of continuous metal amortisseur bars, the calculation of quadrature axis subtransient quantities can be more clearly accomplished. Due to the absence of any second electrical path in the interpolar space, the concept of transient quadrature axis quantities in hydraulic machines is often ignored. The direct axis hydro machine quantities are calculated based on the tests described in IEEE Standard No. 115 (1983) in more or less the same way as for turbinegenerators.

C.2.2 Calculations of Operational Impedances or Inductances

Calculated values of operational inductances can be used in the same manner as test data to provide the values of the elements in the various model structures.

EPRI project RP1288 showed that operational inductances can be calculated using finite-element magnetic field analysis of the generator. The operational inductances so calculated are essentially the same functions as would be measured in a frequency response test. This means that the generator circuit model constants can be derived from the analytical operational inductances using the same statistical fitting techniques presently used to process frequency response test data.

The calculation procedure uses the finite element formulation of the phasor form of magnetic diffusion equation. This computation includes induced currents in the rotor body and other parts, such as amortisseur circuits. From a set of such calculations at a series of frequencies the operational inductances are derived. This method is at present limited to linear problems and cannot represent saturation directly. It is used in a small signal perturbation sense; the operational inductances so calculated represent excursions around a particular operating point. The operating point is characterized by using, as input to the frequency response calculation, a set of permeabilities corresponding to the steady state condition of the generator magnetic circuit at that operating point. For small perturbations, these are called incremental permeabilities. The incremental permeabilities are assigned according to the local steady state flux density. The steady state flux density is obtained from a prior nonlinear magnetostatic calculation of the operating point.

Results of these calculations have been validated in detail by comparison with test data from one generator; while the agreement was satisfying in this case, it remains a single validation.

Advantages of this method are that the calculation can be performed for any operating point, including one at load; for special purposes, the rotor characteristics can be varied at will (such as adding an amortisseur circuit); and, as with any analysis, the generator need not be disturbed, or even exist.

Since the calculation procedure for determining the operational inductances is new, and requires sophisticated computer technology, these kinds of results are not now available on request. Eventual wide availability will depend partially on the evolution of and the demand for more accuracy in generator modelling.

D. LIMITATIONS IN EXISTING "STANDARD" TEST PROCEDURES OR IN PRESENT CALCULATION METHODS

D.1 Limitations in Interpreting Test Results

D.1.1 Short Circuit Testing, and Current Decrement Testing

In examining model 2.2 for both the direct and quadrature axes, the question arises as to how the results of short circuit current decrement tests can be correlated to these 'two winding' models. This has been discussed to some degree in a previous Working Group paper,[7] particularly in the Appendix of the paper.

With the machine represented (in the direct axis for example) by the circuit and elements as noted below, the actual open circuit response of the machine fluxes in the d axis will be characterized by two time constants, obtained from the roots of a second order characteristic equation. Referring to Figure 4, and noting that

$L_{ffd} = L_{fd} + L_{ad}$, and $L_{11d} = L_{1d} + L_{ad}$, this

expression is:

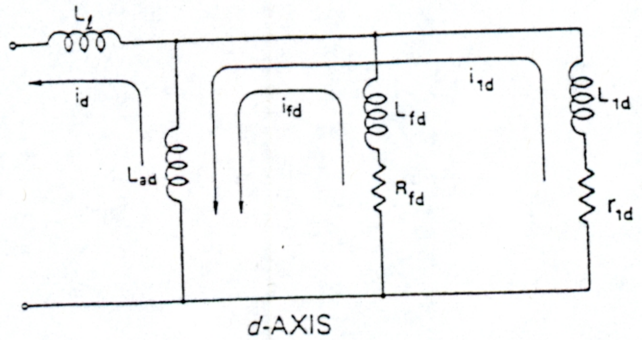
$$1 + s\left(\frac{L_{ffd}}{R_{fd}} + \frac{L_{11d}}{r_{1d}}\right) + s^2\left(\frac{L_{ad}(L_{fd} + L_{1d}) + L_{fd}L_{1d}}{R_{fd}r_{1d}}\right) = 0$$

It is shown by Shackshaft,[4] and in the Appendix of the Working Group paper[7] that these roots can be simplified by assumptions regarding the values of r_{1d} and R_{fd}, by assuming R_{fd} is zero during a "subtransient" or initial period, and further assuming r_{1d} is sufficiently large that the amortisseur or rotor body currents decay much faster than the field currents, and do not influence the transient during subsequent decrement periods. The inverse of these roots are, using the above approximations

(i) $\dfrac{L_{ffd}}{R_{fd}}$, (OR T'_{do})

(ii) $\dfrac{1}{r_{1d}}\left\{L_{1d} + \dfrac{L_{fd}\cdot L_{ad}}{L_{fd} + L_{ad}}\right\}$, (OR T''_{do})

These basic assumptions, as noted by authors such as Adkins[15] in England, and Concordia[16] in the United States, have been generally accepted as satisfactory for two winding models.



L_ℓ = STATOR LEAKAGE INDUCTANCE L_{fd} = FIELD LEAKAGE INDUCTANCE

L_{1d} = DIRECT AXIS ROTOR IRON EQUIVALENT LEAKAGE INDUCTANCE

L_{ad} = STATOR TO ROTOR AND FIELD MUTUAL INDUCTANCE

R_{fd} = FIELD RESISTANCE r_{1d} = d-AXIS ROTOR IRON EQUIVALENT RESISTANCE

FIGURE 4
"STANDARD" SECOND ORDER MODEL
- D AXIS

Existing second order programming approaches, referred to in Section F, such as used by DeMello,[2] account for the fact that a reasonably exact machine transient response is obtained in terms of defining the roots of the characteristic equation (listed above), on the basis of the assumptions noted in the above paragraph. An examination of the block diagram (found in Reference 2) would indicate that the equivalent time response or principal mode of the flux in the transient regime in the direct axis is governed by the expression

$$\frac{L_{ffd}}{R_{fd}} + \frac{L_{11d}}{r_{1d}}$$

Thus the time constant and reactance approach used in this way is quite valid, where the values of T'_{do} and T''_{do}, etc, are as defined above and on p 1626 of Reference 7.

The time constants in the quadrature axis, when defined in the same way as (for example) noted by Adkins and Concordia for the direct axis, are considerably different from the roots of the two element equivalent circuit. This happens because the values of r_{1q} and r_{2q} (for example in the quadrature axis representation of Figure 3) are much closer to each other in value. The block diagram in Reference 2 is based on the definition of the two time constants as noted in the preceeding paragraphs and is structured to produce the modes of response corresponding to the real roots of the equivalent d or q axis circuit. It is possible to <u>derive</u> the above defined time constants from knowledge of the roots, and use the time constant data to correctly represent the q axis. We believe it is important to know, when using the block diagram of Reference 2, whether data on q axis relates to the roots of the operational inductance or to the ratio of inductances to resistances of the equivalent circuit. If it is known that data on T'_{qo} and T''_{qo} correspond to the roots of the second order approximation of the operational inductance, it is suggested that adjustments to these values should be made when using this particular programming approach.

Other limitations in the short circuit tests are whether the reactances for the direct axis are either "saturated" (rated voltage) or "unsaturated" (rated current). This depends upon the voltage level on open circuit from which the short circuit tests are run, or the operating conditions under which decrement tests are carried out. IEEE Standard No. 100 (1977) suggests that for rated current values the open circuit voltage be adjusted, pre short circuit, to a value which will produce rated armature current at the instant of short circuit. The present IEEE Standard No. 115 (1983) tests also ignore the fact that vital information about the identity of the field winding can be obtained by using the values of induced field current to obtain, in effect, a stator to field transfer function.

De Mello's stator decrement test procedures[2] as well as Shackshaft,[4] also provide data for calculating values for the field structure since these tests involve measurements of the stator and field quantities. Ideally the test procedure requires that i_d and i_q are respectively equal to zero before interrupting the armature current. To obtain the d axis response, ie, with i_q equal to zero, the machine is absorbing purely reactive load. The steady state phasor diagram for a machine under these conditions shows that the armature voltage phasor lies along the q axis and the current phasor is on the d axis. In case rotor angle cannot be measured and the axes cannot be precisely defined, the transient change in i_{fd} will indicate the amount of i_d component in the stator current prior to interruption. Analysis for the quadrature axis could then account for the change in total flux Ψ due to changes in i_d to obtain Ψ_q from the armature voltage. A major disadvantage of these tests is that for machines in normal operation the voltage level cannot be reduced substantially so that saturation effects cannot be avoided. This fact requires making an analysis for a first estimate of machine parameters, and then running simulations adjusting these parameters until agreement is made with the measured data and simulations.

The process[2] is one of trial and error and the degree to which the model can duplicate test results depends on the model structure chosen, including treatment of saturation.

Model 2.2, shown in Figure 3, which is a traditional representation, seems to be adequate to duplicate flux transient response to abrupt changes in stator currents. However, to obtain accurate duplication of field current transients and response of flux to field voltage, a third equivalent winding seems to be needed in the d axis. This equivalent winding determined by trial and error is deemed to essentially duplicate the shielding effect of eddy currents in the iron at low frequencies[2,5].

Limitations described above do not apply to the Central Electricity Generating Board (CEGB) approach described by Shackshaft and Poray.[3] However, other limitations do occur when the machine terminal voltage, at zero MW output, cannot be lowered beyond that value available from the maximum high voltage to low voltage turns ratio in the main step-up transformers. In the CEGB, step-up transformers have under load taps available in addition or as an alternative to fixed no-load taps. Desirably, the terminal voltage should be lowered to the straight line (air gap line) portion of the saturation curve. (This usually occurs around 0.70 to 0.80 pu of normal voltage.) The method also requires a nearby generator on the system to supply lagging current to the test generator. For a 555 MV.A machine, with an X_q as high as 2.0 pu, this would require about 225 Mvar at 0.90 pu of normal terminal voltage, assuming the high side voltage was maintained at around 1.0 per unit. The same remarks apply to the underexcited portion of the DeMello tests.

Most of the tests described above usually require the machine to be at partial or zero load for various periods of time, with the possibility of increased energy production costs. The same remarks on increased production costs also apply to rated voltage, open circuit frequency response testing and to a lesser extent they apply to on-line frequency response (OLFR) testing. These tests are discussed in Section D.1.2.2. Some of the on-line frequency response tests may have to be performed at less than full load, if excitation system stabilizers are taken out of service for the duration of the OLFR tests.

D.1.2 Frequency Response Testing – Limitations and Other Considerations

Frequency response tests deliver data in the frequency domain rather than the time domain in contrast to the other test methods described. Also, they are small signal tests and hence, resulting models calculated directly from frequency response test data are inherently linearized about some operating point. Two types of frequency response test have been described in the literature on generator modelling, standstill and on-line. Although generically similar, the two methods are quite different in the required test conditions and in the type of data that they produce.

D.1.2.1 Standstill Frequency Response Tests

Standstill frequency response tests can provide data in as much detail and over as wide a frequency range as desired. They are done at low flux levels and with the generator rotor stationary. These conditions raise the following issues:

(1) The SSFR model element parameters calculated directly from the frequency response test data are applicable to small flux changes associated with incremental permeability. The normal practice is to increase $L_{ad}(0)$ and $L_{aq}(0)$ to a value that corresponds to the air gap line of the open circuit saturation curve, and then use the model in whatever manner is customary. The validity of this adjustment has been questioned; however, the evidence so far supports the approach.

(2) The generator is not saturated during standstill frequency response tests; in fact, the flux densities are below those on the more linear part of the permeability characteristic that is commonly referred to as "unsaturated". Accordingly, the applicability of SSFR-derived models to saturated conditions is often questioned. This is a valid point, but not one that is peculiar to SSFR models. Any generator model in the form of an equivalent circuit, with constant parameter values, is valid only for a single operating point and a single magnitude of disturbance. The problem of adjusting those models to different operating points and different signal levels in the context of a highly nonlinear iron characteristic is challenging but also general to all such models, whatever their origin.

(3) The fact that the rotor is stationary during the measurements means that rotational forces that act on the rotor components during normal operation are not present during standstill tests. Since contact resistances are highly nonlinear and functions of contact pressure, there is a valid concern that the resistance of the path through the rotor slot wedges may be different during standstill and running conditions. This effect is complicated by the fact that the contact resistances are nonlinear, and require a minimum voltage/contact to establish a low impedance current path. This means that slot wedge conduction depends on the magnitude of the end-to-end voltage induced on the rotor body reaching a certain threshold. This threshold might be the voltage needed to break down two contacts multiplied by the number of wedges per slot. In one machine with full length slot wedges, the combination of these two phenomena was important.

D.1.2.2 Rated Speed Frequency Response Tests

On-line and open-circuit frequency response tests, by virtue of the rated speed test condition, avoid the uncertainties regarding the rotational forces on rotor components. They are still small signal tests, although not as small as the standstill tests; the signal levels used are sufficient to produce slot wedge current in at least some machines. Any given rated speed test is also done at a single operating point (saturation level), although it is possible to repeat the tests at different operating points and measure the effect of different conditions on the resulting models. The measurements are usually made by perturbing the field voltage and measuring various transfer functions.

Both tests are limited to approximately 10 Hz at the upper end since beyond this, the inductance of the field winding makes it impossible to change the field current significantly. The on-line test is also limited at the low frequency end to approximately 0.05 Hz; below this, the signal to noise ratio is too low for accurate measurements of power and shaft speed. Useful open-circuit measurements can be made to quite low frequencies.

Both tests are capable of providing useful information from which to check the validity of SSFR models, principally in the direct axis, or to make any required corrections for rotational force/slot wedge conduction effects.[14]

D.2 LIMITATIONS IN PARAMETER CALCULATION METHODS

D.2.1 Data Calculated by Manufacturers

The limitations in parameter calculation methods, as utilized principally by manufacturers, do not seem to have been cause for concern for many stability applications. However, it should be pointed out that the direct-axis values of transient and subtransient reactances and time constants are basically derived from conditions which involve a sudden short circuit at the terminals of a synchronous machine. Such values of calculated reactances and time constants should reproduce, under computer simulations of a stator terminal 3-phase fault, accurate values of armature currents and their rates of decay, until steady state conditions are reached.

The fact that such time constants and reactances (principally the transient values) have been widely used over the years to obtain power angle relationships for transient stability studies, indicates that this heuristic approach has basically been satisfactory for many traditional applications. There are no corresponding standard test procedures available to obtain quadrature axis stability data or parameters. The existing procedures used to calculate quadrature axis stability parameters are therefore open to some question. The latter factor does not appear, in the past, to have been a serious hindrance for the simple and more basic types of analysis discussed in Section A.

However, the applications of high initial response (or 'zero' time constant) solid state exciters, and the need to carry stability simulations for extended periods of real time has focussed attention on the need for higher order models. The electrical damping produced by turboalternators during angular perturbations is also a function of the quadrature axis quantities. This question of damping representation in both axes has also been accentuated when performing small signal linearized stability analyses. It is the opinion of the Task Force on Definitions and Procedures that the present "calculating" methods described herein, as well as in IEEE Standard 115 (1983), and which are used for determining direct and quadrature axis time constants and reactances, for turbogenerators in particular, do have some noticeable limitations, particularly for acceptable quadrature axis information. This statement holds, especially when applying and studying advanced excitations controls to synchronous machines, and when using higher order models which involve a third time constant and reactance, or a "third winding" in each axis. Such additional parameters are extremely difficult to derive from stator or field decrement data, but are easily identified from operational parameters involving frequency response techniques.

D.2.2 Limitations in Producing Operational Data From Frequency Response Calculation Methods

As mentioned in Section C.2.2, finite-element frequency response calculations have been well validated by comparison with test data. However, this validation, having been made by only one contractor and on only one generator, is subject to the following qualifications:

1. The accuracy of the finite-element results is dependent on the use of a finite-element grid which has a large enough number of appropriately located elements to properly represent the magnetic characteristics of the generator. This is especially true in the frequency-response part of the calculation, where rotor skin effect is important. At higher frequencies, the grid must be sufficiently fine near the surface of the rotor to represent the large gradients in flux density that can occur. Even in the magnetostatic form of the solution, grid effects have been shown to affect the accuracy of the steady state load point representation. The prescription of a "sufficient" grid is not compactly stated and may require some experimentation for each machine modelled. The tendency to use a relatively coarse grid for economy in computation must be resisted.

2. The incremental permeabilities used in the above study were specially measured on a sample of rotor iron from the machine being studied. It is not known whether these are sufficiently universal to be used on other machines, although it is suspected that this would be the case. Manufacturers usually have available a normal B-H relation which is well enough characterized for the magnetostatic part of the calculations.

3. The frequency response part of the calculation is inherently based on the assumption of infinitesimal signals. Limited test data are available[6] (RP997) to show that signal strength affects the measured results, and the magnitude of the test measuring currents may affect the agreement between test and calculated results.

   No well developed analytical procedure is yet available for calculations where the a-c signal is large enough that the local permeability of the generator iron depends on the signal strength.

4. Many turbine generators are constructed with conducting rotor slot wedges; however, these wedges are not always continuous. Some assumption about the electrical continuity of these rotor "circuits" must be made for the analysis.

E. RATIONALE FOR SUGGESTING OR SELECTING MODELS FOR VARIOUS TYPES OF STABILITY ANALYSIS

E.1 Detailed Models

When considering the response of any model, or on checking its applicability to system stability studies, it should be noted that in large scale studies there are several oscillatory modes which can be encountered.

These modes are classified as follows:-

(a) Inter-system or inter-machine modes in the 0.1 to 2 Hz Range;

(b) Control modes, for exciter stabilizers (for example) in the 2-10 Hz Range.

The above remarks apply to both linear, small disturbance stability analyses, and to large disturbance studies on large interconnected system simulations. The latter types of disturbances, in large size system studies, are concerned mostly with low frequency oscillations and local modes noted in (a).

In general, the number of rotor circuits deemed adequate may ultimately depend on rotor construction, in the case of turbogenerators. If a detailed study is being made of the stability performance of such generators with relatively complex damper assemblies in both axes, the (most complex) model (3.3) is suggested for use, if parameters are available. Basically, the higher order the model, the better the results will be for special applications, provided that good values for the parameters or elements of the advanced models can be obtained!

For most turbogenerators, particularly those without damper windings (per se), model 2.2 is likely to be more than adequate for studies involving either linear perturbations, or for large disturbance (fault) type studies.

For excitation system stabilization studies, small signal analysis is recommended for determining the correct damping of the system, and preserving the identity of the field circuit is recommended and necessary. By field winding identity we imply a representation of the field such that field currents can be accurately calculated during system transient conditions. As a corollary, we are usually interested in just the overall effect of induced rotor body currents, as viewed from either the rotor or stator terminals.

This field circuit identification is also necessary for field forcing or high initial response studies, with thyristor exciters in particular or in diode excitation systems which have an appreciable voltage regulation. The effect of system fault currents on inducing currents in the field circuit is sometimes important, and retaining the identity of the field terminals under such study conditions is vital. The same remarks also apply to modulation of the field current when utilizing many forms of power system or excitation system stabilizers. In the above studies, a differential leakage reactance is appropriate and useful in model representation.

E.2 Reduced Order Models

Damper circuits, especially those in the quadrature axis provide much of the damping torque. This is particularly important in studies of small signal stability, where conditions are examined about some operating point.

The q axis amortisseur (damper) effects may be approximated be considering only one q axis rotor circuit, as in model 2.1. This model is also much in use for representing hydraulic generators, as there is, particularly, even with continuous waterwheel rotor amortisseurs, only one physically identifiable circuit in the area between the salient poles.

The "classical" model, which is a variation of model "0", assumes in essence a constant voltage behind transient reactance. It often offers a significant improvement in computational simplicity, particularly in large systems, where the voltage depression during, and subsequent to, faults are small. It is frequently used, as noted above in the introduction, along with the generator inertia constant, where the approximate response of electrically remote machines is considered adequate. This is often the case where the electrical "distance" from the study area is greater than 0.5 to 1.0 pu reactance on the study MV.A base. The user is not interested in local oscillatory control modes of these remote machines, and their dominant effect on the stability problem being examined can be principally reflected in their inertia.

The second order direct axis models of Figure 3 also includes a differential leakages reactance. In certain situations for second order models, the identity of the field winding is also considered important, in order to accurately determine field current transients. Alternatively, the field circuit topology can alter by the presence of an excitation system, with its associated nonlinear features. The inclusion of the differential leakage reactance permits, in programming algorithms, a precise retention of the field structure (R_{fd} and X_{fd}) in the direct axis. It follows that tests or analytical methods should be appropriate for determining the rotor elements, including stator to field transfer function data and field to stator operation impedance data.

If such data is not available, as is often the case, or if the identity of the field winding is not a precise and specific modelling requirement, the differential leakage reactance can be set arbitrarily to zero, and the remainder of the second order model parameters can then be determined either by stator winding derived data, or by adjusting all the direct axis model elements by a factor K, which includes the value of X_{f1d}. See Reference 8 by I.M. Canay.

E.3 Saturation Algorithms (Current and Proposed)

Since the inception of many large scale computer programs, the manner of accounting for saturation of the rotor and iron paths in generators has been to determine an operating point on the direct (and sometimes quadrature) axis open circuit saturation curves, and from this some form of saturation factor is derived.

It is sometimes customary to calculate a voltage 'back of leakage reactance'.

Thus $E_\ell = E_t + I\,(jX_\ell)$.

Then a saturation factor 'k' is determined by the ratio of the actual pu excitation on the curve to the excitation required to produce the same voltage on the air gap line. The value of k is used to adjust X_{ad}, thus giving a saturated value of synchronous reactance, where

$$X_{dsat} = X_{adsat} + X_\ell$$

The total saturation voltage is then determined, where

$$E_I = E_t + I(j\,X_{dsat})$$

Another approach, when considering the vector diagram of the machine, in the steady state, is determined from the relationship.

$$E_I = E'_q + i_d\,(X_d - X'_d) + \Delta E_I$$

The value of ΔE_I is calculated as the difference between the excitation calculated for an operating point (at E_ℓ the voltage back of leakage reactance) and the excitation required for the same voltage on the air gap line. The various methods all give values of E_I which lie fairly close together, particularly for hydro machines. Some of the above methods disagree with the IEEE Standard 115-1983 approach, particularly for calculating turbogenerator excitation requirements. Some of the recommended IEEE methods use the Potier Reactance. The Potier Reactance method has not had much application in large scale stability programs. Some of the advanced methods discussed below resulted in more accurate methods for calculating turbinegenerator excitations over a wide range of power factors and MW outputs, but their computational efficiency has not been fully assessed, compared to existing methods.

Recognition has been given by Shackshaft[9] to the fact that the direct axis flux and quadrature axis flux cannot be arbitrarily separated, for saturation calculations, particularly in turbogenerators. He has proposed a method of accounting for saturation differently in each axis and also recognizes a coupling between the axes. The requirement for different d and q axis saturation data is recognized in work performed under EPRI contracts RP997 and RP1288, and in IEEE Power Apparatus and Systems Transactions publications.[10] An important factor in the representation of saturation, in the quadrature axis, is that the accuracy of many stability simulations is influenced by the initial steady state internal angle of the synchronous machine. This internal angle of course is directly influenced by X_q and particularly the degree to which X_q saturates under loaded conditions.

A new approach to saturation algorithms has been developed under EPRI RP1288. In this approach, the basis for saturation is an array of reactances calculated from finite-element steady state, nonlinear load points covering the full operating range of the generator. Empirical saturation functions have been found which correlate the variation of the reactances with load point. Separate saturation functions are used for X_q and X_{ad}. X_d is found by adding a constant leakage reactance to X_{ad}. The leakage reactance has been found, from the finite element results, to be approximately constant. Each saturation function is related to a flux behind an empirically found internal reactance. The saturation function is the product of two functions. The first has the total flux as its argument and the second has the q axis component of that flux as its argument. This approach has been validated against test data from two, two-pole generators; close agreement between predicted and measured load angles and excitation has been shown.

All of the above approaches relate to the techniques used for obtaining a total saturation, either at the initialization stage of stability studies (ie, at t=0-) or <u>during the step by step calculating process</u>. It is the opinion of some analysts that during severe system disturbances, when generator currents are usually two to three times normal, that X_ℓ, the leakage reactance, should also be adjusted.[11]

In small signal studies,[12] several approaches have been used. The value of X_{dsat} used to obtain an operating point is performed in one of the ways described above. Then X_{dsat} is further adjusted for incremental (not total) permeability.

For the values of X_{dsat} and X_{qsat} used in many stability programs, to determine initial excitation and internal angle quantities, application of saturation factors would result in a reduction from the given unsaturated values. Typical reductions for hydraulic machines have been found to be as much as 25% (from 1.0 pu to 0.75 pu is typical). For turbogenerators the reduction could range from 15% to 25%, where a change from 1.90 pu down to 1.50 pu is typical. The latter comments apply to normal overexcited conditions.

Some investigations have indicated that X_{adsat} and X_{aqsat} values for <u>small signal applications</u>, could be 50% or less of the unsaturated values, when considering incremental saturation. (1.80 pu down to 0.90 pu for a <u>change</u> in X_{ad} could be considered typical).

F. FORMULATION OF MACHINE EQUATIONS FOR COMPUTATION

Two basic approaches can be cited in utilizing data or parameters corresponding to first, second, or third order models. One approach formulates the machine equations in terms of time constants and reactances. Reference 2 provides the basic equations and corresponding block diagram for this method.

The second approach, cited in Reference 13, formulates the equations in terms of the basic elements (Resistances and Reactances) of the equivalent circuits. Except for the differences in the way saturation is represented, either of the two approaches may be effectively used for implementing second order models into time domain stability programs, and should be expected to give similar results.

For a more detailed model, with three rotor circuits in each axis, or in one axis, including the effects of the differential leakage reactance in the direct axis, the second approach appears to be more flexible. Reference 13 also shows equations which demonstrate the basic concordance between the two approaches.

CONCLUDING REMARKS

The Task Force encourages discussion on their views, and we realize that some readers may disagree with them. Our long-term objective, in raising these issues, and pointing out some of the existing limitations in synchronous machine stability simulations, is to gather as much industry comment as possible. Eventually, we propose to publish, through the Joint Working Group, some type of a "recommended practices" or "guidelines" document for the general assistance of the power system analyst who is concerned with stability.

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Discussion

Charles Concordia (Consulting Engineer, Venice, Florida): This paper is useful as bringing together in one place an informative discussion of the aspects of the several synchronous-generator models now for use in power-system stability studies. However, I was at disappointed, since, from the title, I had expected to find in the "recommended practices", or "guidelines" for model selection, which I now find from the "Concluding Remarks", are still to come. In fact, there *is* a little discussion of the appropriateness of some of the models, but *in general* I felt the reader might be more confused, by so much unevaluated information, than guided. (Perhaps I am really disappointed at not finding any dogmatic recommendations with which I could disagree.) It may be of interest, and perhaps even helpful, to refer to a 1975 paper[1] which discusses more generally the detail of system representation appropriate for a range of study types.

Following are a few comments on specific points.

1. Regarding the statement in Section A - II (Stability Studies) that the "constant voltage behind a (single) fixed reactance—provides acceptable results," we have found this generally true only if the generator is *over*-excited," (i.e., if it is supplying reactive power to the network). However, if it is "*under*excited," (i.e., absorbing reactive power), then including "transient saliency" is necessary to provide acceptable results.

2. Section A - IV (SSR Studies) seems self-inconsistent, speaking first of "*two* different machine-system interactions" and then stating that they "must be treated together". It would be more nearly correct to admit that, in real life, with an excited machine there is *only one* interaction.

3. It was impossible for me to find any meaning in the statement (in A - IV) that, in large stability programs "the use of fixed parameters—has been *recognized*". What *does* it mean?

4. In Section B it would be more precise to discuss "constant *rotor-circuit* flux linkage" models rather than simply "constant flux linkage" models. This is not just a quibble, since a model with constant flux linkages in *all* circuits is often used in *short-circuit* calculations.

5. The statement that "Models 3.3 and 2.2 embrace the upper and lower limits of complexity in dealing with *any degree of* detail with turbo-... —" is misleading. It would be much clearer to omit the phrase "...egree of".

6. Regarding Section C.1.3 - "Frequency response tests", we should like to mention, again, that, about 45 years ago we found that very good results, corresponding to the incremental permeability as measured with a biasing d - c flux density, could be obtained by applying some direct current to the field winding during the standstill tests, and we herewith recommend considering such a test. (This comment applies also to the discussion in Section D.1.2)

7. In section C.2.1., as a comment on the assumption that $x''_q = x''_d$; we noted a few years ago, in an IEEE paper 80 SM 541 - 3, ratios of x''_q/x''_d of about 1.5. The following year we noted that paper 81 SM 428 - 2 reported $x''_q/x''_d = 1.0$, but checking with the authors of both of these papers resulted in confirming the first ratio of 1.5 but modifying the second to $x''_q/x''_d = 1.1$ to 1.2. Thus we question seriously the assumption $x''_q = x''_d$.

8. In Section C.2.2 it is stated that the EPRI finite-element approach "cannot represent saturation directly" and is "limited to linear problems". While both these statements may be strictly true they are very misleading, since in fact this approach represents saturation more directly than any other method so far considered, and can be used in nonlinear problems just as well as any other method so far considered.

9. Regarding Section D.1.1, we point out that the possibility of separation of roots discussed here was in fact suggested many years ago by Park, and even before that by Doherty and Nickle. Further, the equivalent direct-axis transient time constant shown is definitely *not* based on "the assumption of separation of roots", as implied in the paper, but is rather based on precisely the *opposite* approximation, namely lumping together two nearby roots, i.e., paralleling the field and *lowest* iron resistances.

10. It is stated that "for rated current values (of reactance) the open-circuit voltage should be adjusted to produce rated armature current at the *instant of short circuit*." I had understood that the adjustment was to give rated *transient* current (i.e., neglecting the subtransient component). Which is correct?

11. In Section E.2 (Reduced-order models) the "classical" model seems recommended for distant machines. However, very often the tie-line power swing damping is important so it becomes necessary to represent somehow the contribution to damping of even very distant machines, which may be just as, or even more, important than those in the so-called study area. Otherwise, it is possible to be fooled by study results which do not account correctly for all aspects of system performance, and real life may show a problem in an unexpected location. In fact, we know of cases where this has occurred.

12. In regard to Section E.3, we should like to mention that, in a 1938 AIEE paper[2], we found that, using resultant (total) air-gap *flux*, modified by field *current* (MMF), we could determine the corresponding air-gap (mmf) and the field excitation rather accurately. This is very similar to, but not exactly the same as, the "new (EPRI) approach" described. The rationale appears to be identical.

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1) "Appropriate Component Representation for the Simulation of Power System Dynamics" by Charles Concordis and Richard P. Schultz, in IEEE Publication 75CH0 970-4-PWR, "Adequacy and Philosophy of Modeling System Dynamic Performance"

2) "Stability Characteristics of Turbine Generators" by C. Concordia, S. B. Crary, and J. M. Lyons, AIEE Trans., Vol. 57, pp. 732-743, 1938.

Manuscript received February 19, 1985.

Claude R. Desrosiers (Hydro-Quebec, Montreal, Quebec, Canada): This paper is very well documented and, from my point of view, is directed towards "users" of stability data.

Being part of the "producers" of such stability data, I would like to bring out one specific point to the authors.

Thirty-four out of thirty-seven synchronous machines have been commissioned at our Hydro-Quebec La Grande hydroelectric complex in the last few years and the average measured value of Xd of these machines is greater than the calculated values announced by the suppliers. The test results were produced in accordance with the IEEE no 115 test code. Our technical specifications required that "the direct-axis synchronous reactance (Xd) should not exceed 1.0 p.u." In a particular powerhouse equipped with 12 units, one generator has a measured Xd value of 1.14 p.u. and the average measured value of Xd in this powerhouse is 1.11 p.u.

The parameters "Xd" in stability simulations is, I believe, mainly used for establishing the initial condition of a stability run. The IEEE Standard no. 115 does not specify any acceptable tolerance from a specified Xd. Why? Should there not be a definition of an acceptable tolerance between the supplier calculated values of the different reactances and time constants of a generator versus those measured on as installed units?

I would appreciate your comments on a "larger than calculated" Xd versus the stability of such a machine, electrically distant from the load center. Finally, what is being perceived as an "acceptable tolerance" for the electrical constants of a synchronous machine would be well received by a "producer" of stability data.

Manuscript received February 28, 1985.

P. Kunder and **G. J. Rogers** (Ontario Hydro, Toronto, Ontario, Canada): This paper provides a good summary of work done during the last ten years in North America and elsewhere on the modeling of synchronous machines. While the paper provides much information, we were a little disappointed to see that no specific recommendations were made regarding test procedures and model selection. In this discussion, we would like to give our experience and focus attention on some of the considerations in the selection of synchronous machine models for use in present-day stability programs. We also wish to bring out some of the basic issues which still need to be resolved.

1. *Ranges of Model and Model Selection*

In reality what we have is only one model, based on Park's transformation with an arbitrary number rotor circuits. For power system stability studies, it is sufficient to limit the number of rotor circuits in each axis to 3, as shown in Fig. 1 of the paper and identified as Model 3.3 in Table 1. All the other models are simplifications of this basic model obtained by either neglecting rotor winding branches or by making other simplifying assumptions such as constant flux linkages. In any study the complexity of the model used is governed by two factors:

- availability of data;
- effect of the particular machine being modelled on the problem being investigated.

Before the advent of digital computers, there was considerable incentive to minimize the number of differential equations. This is no longer necessary and, from a computational point of view, there is little justification for using some of the reduced order models. In fact a higher order with no dynamic (transient, sub-transient or sub-subtransient) saliency could be computationally more efficient than a lower order model with dynamic saliency.

It is our opinion that Model 2.2 should be used for representing most of the machines. Even if all the parameters are not available, it is more accurate to use estimated values than neglect them altogether [A]. With parameters chosen so that there is no subtransient saliency, such a model is also computationally efficient. Very remote and equivalent machines could be represented by the classical model (Model 0.0) with a suitable value of damping coefficient. Except for machines with unusual rotor construction modelled for use in special studies, it is unlikely that it will be necessary to use Model 3.3.

2. *Sources of Data*

Several test procedures to determine the model parameters are described in the paper. In our experience these have all had mixed success. The combination of SSFR to give a good initial model, followed by OLFR to refine the model parameters has been most successful. However, these tests are expensive to perform and difficult to justify in cases. Moreover, for system design and planning studies, the data is required before the generator is built and placed in service. Therefore, there is a real need for methods of deriving adequate models from design data. We believe that attempts should be made to refine methods of determining the "standard" data by the manufacturers rather than discard them as being inadequate. In fact, there have been several reports of satisfactory experience with models based on manufacturers design data [B, C, D].

We feel that there is sometimes a tendency to overemphasize the need for modelling synchronous machines very accurately. Except for some special protection and control design studies a very accurate generator model may not be essential. Unless we have models of comparable level of accuracy for other system elements, the results of studies with better generator models are not likely to be more accurate. For example, many utilities still use typical data for excitation systems which may not reflect actual settings, and our ability to model loads accurately is also very limited. There is a greater need to improve these areas of modelling than to overly refine generator models. We are not suggesting that further work on synchronous machine modelling should not continue; there is still a need for better understanding of some of the aspects of synchronous machine transient characteristics. What we find difficult to justify is the use of complex and expensive tests as a standard procedure for obtaining data. Such tests are likely to be justified only for systems which depend on special excitation controls for maintaining stability.

3. *Important Parameters*

For transient stability studies, with slow exciters, X_d and T_{do} have most significance. With high initial response exciters T'_{do} and exciter ceiling voltage are important.

T'_{do} determines the per unit field resistance which in turn affects the base field voltage. If this does not correspond to the voltage base assumed in the exciter model, the error introduced could be considerable, since both ceiling voltage and gain will be affected. The value of field resistance used should correspond, as closely as possible, to that under actual operating conditions. This should be appropriately reflected in the generator and exciter models.

For small signal stability the phase characteristic between field voltage and electrical torque over the normal range of oscillatory frequencies (0.1 to 2 Hz) is important. This determines the effect of the excitation system on the synchronizing and damping torques of the generator.

The phase characteristic is significantly affected by the amortisseur circuits of the generator. The damping introduced directly by the amortisseurs is usually small but their influence on the interaction between exciter and generator may be large.

The generator saturation characteristic is important in transient as well as small disturbance stability studies. There is a need for improved methods of saturation representation, particularly for q-axis. Thests suggested so far determine only the steady-state saturation characteristics and there is need for validating saturation representation under transient conditions.

The methods used for representing saturation are largely empirical and there is a need for a fundamental examination of the process of saturation in generators. One of the problems is that an accurate representation of saturation may conflict with the fundamental assumption of superposition assumed in the d,q axes model.

4. *Model Validation*

Validation of generator models has been mainly through comparison with the results of specially tests. Of necessity these tests have been designed to impose as little stress as possible on the power system. This restricts their value. Disturbance monitors are now available which can record the response of generators to actual system disturbances. Validation of generator and other system component models against such recordings should be encouraged.

In the previous section we discussed the importance of reflecting accurately the correct value of field resistance in the generator and exciter models and the need for checking the saturation representation under transient conditions. A simple test for validating these aspects of generator modelling is to force the field voltage of a loaded unit to its ceiling for about a second and monitor terminal voltage, active and reactive power, field voltage and current. At Ontario Hydro we are proposing such a test on a 270 MV.A unit due to be commissioned in March 1985.

5. *Definition of Model Parameters*

Standardization of model parameter definitions is also an important aspect of generator modelling. As implied in the working group paper, most currently used computer programs assume the simple relationships between the time constants and circuit parameters given in Section D (i) and (ii). In addition it is assumed that the time constants specified are based on unsaturated reactances. Values of time constants obtained from open or short circuit tests on the machine do not conform to this definition. Great care must be taken in their use to ensure consistency with the programmed models. Depending on the manufacturer, the supplied model data may correspond to the given definitions or to calculated test values. It is not a question of which definitions and using them consistently. The stability analyst should not be put in the position of having to query the origin of data to be used in studies. While this problem has been recognized for some time, it is disappointing that the Joint Working Group has not, so far, proposed a definitive standard.

In summary the following items remain to be addressed:

- The most suitable value of field resistence for the defintin of generator models and exciter models.
- A better understanding of the nature of saturation in generators and its influence on generator transient response.
- Standardization of model parameter definitions.

In addition the working group should work towards the development of a standard method by which adequate synchronous machine model can be derived from design data.

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[C] I. M. Canay, "Extended Synchronous Machine for the calculation of Transient Process and Stability", Electrical Machines & Electromechanics, 1:137-150, 1977.

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Manuscript received February 28, 1985.

K. Reichert and **E. Thaler** (Swiss Fed. Inst. of Technology, Zurich): The authors have presented an excellent paper on modeling and parameter identification of synchronous machines. It can be considered as a standard for future work in the area of power generation and transmission stability simulation.

With regard to the issues of interest, I would propose to add the design and evaluation studies required to determine the torques and stresses in generator-turbine sets resulting from the interaction between the electromagnetic and the mechanical systems following large (e.g. fault-clearing) and small disturbances (e.g. converter or control interaction). Even if the modeling of the mechanical system (turbine-generator-shaft) is beyond the scope of the task force, it is important that the modeling and simulation of the electrical part does result in realistic torques and currents.

The identification of the d-q-models (0.0 - 3.3) is a complex issue. It has to be based on the fact, that the identification of n model parameters requires at least n independant measurements or typical data (e.g. time

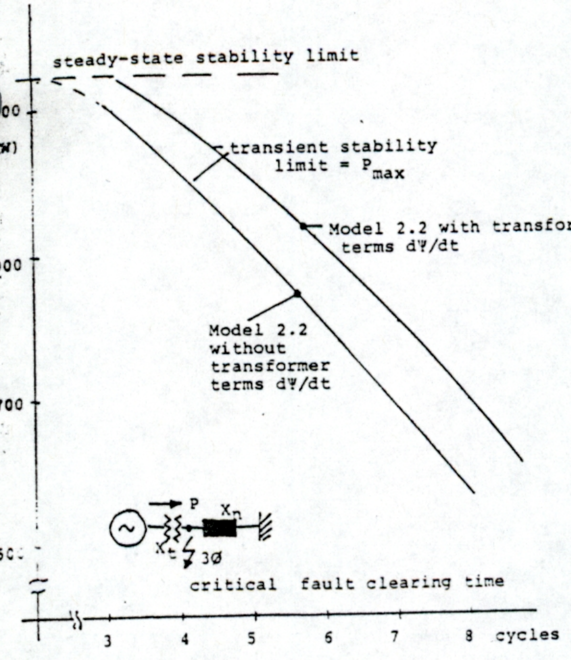

Fig. I: Effect of system equations on transient stability limit
$X_t = 0.15$ (machine base)
$X_n = 0.2$ prefault, $= 0.3$ postfault (500 MVA base)

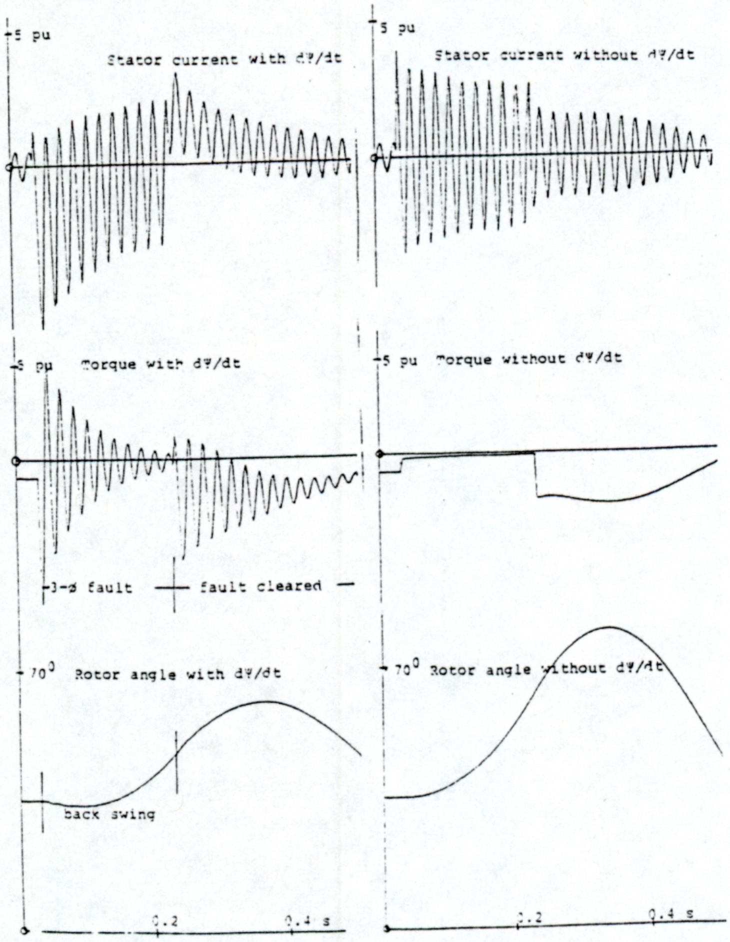

Fig. II. Simulation of fault application and clearing at a synchronous machine, with and without transformer terms $d\Psi_d/dt, d\Psi_q/dt$

tants and reactances). It might be possible to describe with some acy the response (e.g. short circuit current) of the second order d-model 2.2 (Fig. 3) by means of two time constants (T'_d, T'_d) and three reactances (X_d, X'_d, X''_d). It is yet impossible to determine the seven parameters of the d-axis model 2.2 from these five data without further assumptions or additional values. The popular and widely used approach to neglect L_{fld} and to provide a value for the stator leakage inductivity L_l is a solution to the problem as long as modelling of L_d (s) only and not of G (s) is of importance.

Our last comments concerns the formulation of machine equations for computations, e.g. for the simulation by means of numerical methods. Park's equations for the d- and q-axis stator quantities:

$$U_d = R_s i_d + \frac{d\Psi_d}{dt} - \omega\Psi_q$$

$$U_q = R_s i_q + \frac{d\Psi_q}{dt} + \omega\Psi_d$$

are consisting of two terms resulting from Faraday's law:

a) the rotational voltages $\omega\Psi_q$ and $\omega\Psi_d$ are responsible for the power transfer

b) the transformer voltages $d\Psi_d/dt$ and $d\Psi_q/dt$ are responsible for the electromagnetic transients i.e. the dc - components in the stator quantities.

It is common practice to disregard the transformer terms $d\Psi_d/dt$ and $d\Psi_q/dt$ in the stator equations and in the network for stability simulations. Detailed studies (1) have proven, that this approach gives more pessimistic transient stability limits (critical fault clearing time for a given power transfer) than the complete one as the braking torque due to the dc - components in the stator currents resulting in the backswing phenomena is neglected (see Fig. I).

It can be concluded, that for smaller generators with a large inertia, large fault clearing times and for remote faults the approximate simulation method provides sufficient information on the stability limits.

In cases where realistic values of generator torques and currents are of interest, such as fault application and clearing at large generators with small internal constants, SSR, asynchronous operation, false synchronization, etc. the detailed model 3.3 and the complete set of Park's equations (including the transformer terms) has to be used. Fig. II is provided to support this. It shows the behavior of a synchronons machine during fault application and clearing, simulated with and without transformer terms.

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[1] Reichert K. and Leon N. Calculation methods and models for stability simulations of large synchronons machines. Brown Boveri Review, P. 480 — 487, 1974.

Manuscript received March 1, 1985.

Baker, Hannett, Jones, Edmonds, Lee, Salon, Minnich, Umans and **Dandeno:** The Task Force thanks the discussers for their comments and the interest they have indicated in our mission and activities.

Mr. Concordia expresses some disappointment in our title — we had considered a stronger title such as "guidelines" or "recommended practices". However the Task Force felt at this stage that such a revised title could be used eventually when we approach the standards board with a revised document, which will also have been subjected to the criticisms of the members at large and the discussers in particular and which also spelled out some recommendations, rather than a few conclusions. We also note with thanks Mr. Concordia's reference to the 1975 paper "Appropriate component representation for the simulation of Power System Dynamics", authored by himself and R. P. Schulz. This symposium paper will be appropriately reviewed by the Task Force when preparing the revised document for Standards consideration.

We shall next consider Concordia's individual points, and comment on them where we feel it is necessary:

1. It is likely true that including transient saliency would be appropriate for stability investigations of underexcited generators. It has been our experience that if 'remote' machines are being studied, where the voltage depression is 0.10 pu or less during the initiating disturbance, a constant voltage behind a fixed reactance representation would be adequate regardless of whether the machine was underexcited or overexcited.
2. The Task Force feels that an alternative way of stating this concept raised by Concordia is "the use of fixed parameters in stability studies has been common practice, and has been widely accepted". What we were also trying to say in this instance was that an electrically non-linear device such as a turbogenerator with a solid iron rotor is usually treated in stability studies as a linear piece of equipment by the assumptions of fixed values of d and q axis parameters, with the exception perhaps of X_{ad}, which is often adjusted to account for iron saturation. This also is implied in the notion of $(1 + sT)$ functions in the numerator and denominator of the operational expressions describing the fit of the machine to some kind of data over a range of frequencies. This assumption would be opposite to using a function such as $(1 + s^{1/2}T)$. The latter expressions in the operational inductances would indicate that some eddy current dependence existed over part of the frequency range, but might yield a better fit to test data. The use or application of such a sophisticated approach would be extremely difficult, and inefficient, as well as impractical.
3. We agree that the concept of constant *rotor-circuit* flux linkages is a more precise way of describing the effects discussed here.
4. The Task Force agrees with this statement.
5. The subject of biasing flux density was also treated in EPRI reports EL1424, volumes 2 and 3, and is also discussed in EPRI Report EL3359, volume 2 (RP1288). The findings from the latter are summarized in [1]. According to the reasoning developed in [1], in *small-signal* tests the incremental permeability varies so slowly with biasing flux density that essentially the same response will be obtained for all biasing field currents along the airgap line including zero. It is the general feeling of the task force that DC biasing flux tests are more appropriate during running-open-circuit frequency response tests. Even here, the *small-signal* response is quite similar to that at standstill, except for depression at the low frequency end when the DC field bias is that for rated voltage. The incremental characteristics of the rotor iron are not so important as possible rotor amortisseur circuit changes in producing different characteristics between running and standstill.
6. All that the Task Force was trying to do here was reflect in a non-controversial way, some, (but not all) of the assumptions made by some (but not all) turbogenerator designers, when attempting to produce calculated values of subtransient reactances and time constants, particularly for values in the quadrature axis of a solid iron rotor turbinegenerator.
7. The use of the word "saturation" in the context that Mr. Concordia refers to was an unfortunate choice, in violation of the plea in [1] for specificity in wording. What was meant is that the present form of the finite-element diffusion equation formulation cannot represent "large-signal rotor response". However, Mr. Concordia's comments seem to refer to the finite-element magnetostatic, or steady-state formulation, which represents saturation quite accurately. The wording in the referenced paragraph seems clear enough in stating that it is the diffusion equation formulation being discussed.

   As discussed above, it is evident that the lumped parameter models used in transient stability analyses are linearized models. In addition the finite element analyses developed to obtained frequency response type data for the representation of turbogenerators are linearized representations. In each case saturation effects can be included in an approximate fashion; in the lumped parameter models, magnetizing reactances can be adjusted to account for saturation effects, while in the finite element analyses, incremental permeabilities are similarly adjusted.
8. The separation of roots 'question' is a vexatious one. It seems to those in the Task Force (who are not manufacturers' representatives) that the quoted value T'_{do} is not necessarily the lumping together of two nearby roots of the characteristic equation. This is something that will be discussed in some future user's guide. We only wished to highlight this issue in any case, and not "rule" on it. The question boils down to: "Can agreement on the "definition" of T'_{do} be reached"?
9. Task Force stands corrected. The statements in both IEEE Std. No. 100 and IEEE Std. No. 115 do state that it is the *transient* component of short circuit current that should be used when calculating the transient reactance of a synchronous machine for rated current conditions.
10. As discussed previously in our comments on section 1, 'remote' machines *do* play some part in the study of system stability reactions. The use of a damping factor is always some kind of approximation. If damping is considered to be a deciding or important factor, then the frequency (or speed) excursions, particularly post-fault, must be more than minimal, and the machines should then *not* be treated as remote (simplified) machines in the electrical sense.

We turn next to the discussions submitted by Messrs. Kundur and Rogers. They have provided the Task Force with many useful insights and comments, which should be of considerable help to us in transforming this present Transactions document from a 'Current Usage' concept into firmer, more rigid 'recommended practices', or users' guide.

In their section #1 (Ranges of Model and model selection), they state that there is only one model, with an arbitrary Number of rotor circuits. Specifically, if a lower order model is used in place of, for example, the "complete" third order model (#3.3 form Table I) its constants must be obtained by fitting frequency response data (for example) to the lower order model. We do not believe they mean to imply that one can go from the higher order to the lower order simply by arbitrarily removing or reducing (through paralleling the branches) the number of Resistance and Reactance elements representing the rotor iron body and amortisseur circuits. The lower order models, if they correspond closely in fitting procedures to on-site test data, have often been considered the simplest to use. As the discussers point out, the reduction in the model order (or in the number of differential equations) is today no longer justified on computational grounds. Kundur's and Roger's comment on this is certainly not valid when *both* model structure and the associated element constants are considered.

The same discussers also point out the computational advantage in assuming (for example) that $X'''_d = X'''_q$, or that $X''_d = X''_q$, etc. We do not believe they meant to imply that complete elimination of dynamic saliency was a practice to be recommended, ie, that X'_d would be assumed equal to X'_q. The assumption for $X'''_d = X'''_q$ is usually acceptable and would appear to have a minimal impact onthe magnitudes of the remaining parameters of a third order model, for example. The same comments also apply to some extent to the practice of assuming that $X''_d = X''_q$ in a second order model.

We agree that Model 3.3 would be principally used for machines with rotors which had d and/or q axis dampers, and where high ceiling, high initial response static type excitation systems are utilized. Model 2.2 could also be used for specialized excitation system studies provided the identity of the field circuit is retained. The above comments are also reflected in their section 2 (Sources of Data). It is agreed that, in the long term, improved methods of calculating stability parameters, probably based on some in some part on finite element methods, will be required for complex rotor body construction situations, along with the application of (zero time constant) static type excitation systems.

The discussers' section 3 (Important parameters) is extremely useful in highlighting some of the issues which we missed or which we were unable to include due to space limitations. A note of caution should be inserted here regarding the determination of R_{fd} (rotor field resistance).

We believe all will agree that it is consistent to use the value of R_{fd} given by measuring the field resistance, *or* by determining R_{fd} as noted in IEEE Std. No. 115A, and then correcting it to the "hot" value recommended in C50.10 Table I. It then must be converted to a per unit value for simulation purposes. The calculation of R_{fd} from a given value of X'_d and T'_{do}, according to the custom of some users, is not recommended. We also appreciate the remainder of their comments under their section 3, and concur with them.

The above comments regarding R_{fd} also apply to the question of definition of model parameters which they refer to in their section 5 (Definition of Model Parameters). It was difficult to reach a consensus on their concern about such definitions. They quote, in the middle of the first paragraph on their section 5, a sentence which reads as follows. "Depending on the manufacturer, the supplied model may correspond to the given definitions or to calculated test values". As noted again in our comments on Concordia's point #9, this is a vexatious question. We may still try to resolve it when approaching the standards board with some kind of recommended practices which would be directed to generation designers.

Some members of the Task Force feel that if the model parameters are given in terms of Resistances and Reactances, or Resistances and Inductances (all in per unit), for linear air-gap line conditions, along with the actual R_{fd}, then all the effective time constants are implied from the elements of the d and q axis models. In concluding our comments on the well-presented discussion of Kundur and Rogers, we agree with their listing of three items which must be addressed in any future standard guidelines, or recommended practices:

- A suitable value of R_{fd} which ought to be used.
- Better understanding on the nature, application and effects of saturation (incremental or total) in synchronous machines, relating in particular to dynamic analysis.

Mr. Desrosiers addresses the question of the unsaturated synchronous reactance, and refers to the fact that it can be higher than design values. What the effect of X_{du} or $(X_{adu} + X_{\ell})$ would be on the more important stability parameters, such as the transient reactance or the open circuit time constants is difficult to say (once again the question of parameter definitions arises.) It also depends on whether one is talking about rated current or rated voltage values. It does not appear to the Task Force that the values of the measured X_{du} which he quotes as being up to 14% higher than specified or quoted is serious when one considers the saturated values of X_{du} likely to be encountered under loaded conditions. Incidentally we would consider Mr. Desrosiers a "user" of such data rather than a "producer".

We wish to thank Konrad Reichert and his associates regarding the comments he makes on correct machine representation to be used during studies of machine torques and stresses under disturbances conditions. The subject is also one which should be addressed more fully than this Task Force could hope to do in the present document. Their point about retaining the concept of stator flux linkage changes ($p\Psi_d$, $p\Psi_q$) is well taken, and certainly will have an impact in stability studies concerned with critical switching times, or in the study of the corresponding sudden "shock" torques under (for example) three phase faults near or at the terminals of turbinegenerators. Such studies are often limited in scope, through representation of system effects by one or two impedances. We appreciate the information they have provided in reminding the industry of these important considerations.

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