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A Direct Method Of Measuring Stray Load Loss In DC Machines

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***Abstract*—Stray load loss in dc machines is defined as the difference between the actual loss under load and the well-recognized loss. This paper describes a simple, reliable, and direct method of measuring this loss by means of a proposed short-circuit test with a correction factor for uncompensated-type dc machines. It also describes the sources and components of stray load losses as they occur under short-circuit test.**

Introduction

STRAY LOAD LOSS is defined as the difference between the actual loss and the well-recognized loss. Since stray load loss is very difficult to measure accurately, for many years the standard practice in the calculation of conventional efficiency of dc machines in the United States [1] has been to include stray load losses as equal to 1 percent of the output. The Russian standard [2] takes 1 percent of the output for uncompensated generators and 0.5 percent of the output for compensated machines. An AIEE Committee on Rotating Machines, after investigating 243 stray loss tests on different motors ranging from 1/2 to 50 hp, reported [1] that the present practice should be continued until a new method is available which gives a direct measure of these losses.

It is now realized that the input–output method gives stray load loss results which are neither consistent nor accurate, while Blondel's opposition test [3] is complicated because it requires 1) correct setting of brushes in the neutral position so as to generate equal voltages for the same excitation, 2) another identical machine, 3) another driving motor to supply the mechanical power, and 4) a booster generator having unusual ratings.

This paper describes a simple, reliable, and direct method of measuring these losses for both compensated and uncompensated dc machines by means of a short-circuit test. Short-circuit test data, along with a correction factor, determine these losses accurately for the uncompensated dc machine.

Sources and Components

In this test the stray load losses are considered as being made up of a number of separate components:

1) a core loss component which consists of additional increase in hysteresis and eddy current losses in the armature teeth and core, resulting from the distortion of the air-gap flux by the armature magnetomotive force (this loss appears as a counter torque),

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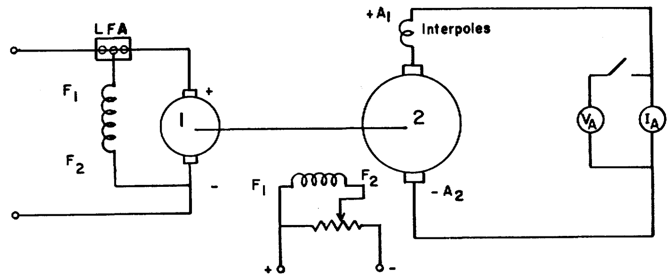

Fig. 1. Schematic connection diagram for short-circuit test. 1—Calibrated dc motor; 2—dc machine under test.

2) increased armature-circuit loss arising from skin effect and eddy currents in armature conductors, due to the alternating current flowing in the armature,

3) eddy current loss in the iron surrounding the armature conductors which results from the ac armature-current field (this appears as increased winding resistance),

4) hysteresis losses resulting from the ac field around armature conductors which appear as increased winding resistance, as in transformers,

5) additional brush contact loss due to imperfect commutation (when heavy load current is flowing in the armature), which is reflected in increased brush drop and hence increased resistance,

6) losses in metal parts and binding wires supporting the armature and coils.

The first component of stray load loss is termed the core loss component, while all the remaining components are put under the term increased resistance loss, since they result in increased effective winding resistance irrespective of their cause.

Proposed Short-Circuit Test

In this test the machine is driven at rated speed by a calibrated motor which is coupled mechanically to the machine as shown in Fig. 1. Its excitation is increased from zero until full-load current flows in the short-circuited armature. Under this condition the alternating current flowing in the armature winding consists of 1) a fundamental frequency component determined by rated speed and the number of poles of the machine, and 2) the high-frequency components due to the product of the number of armature slots and rated speed and/or number of commutator segments and speed. Since the armature is short-circuited, its output is zero, and the extra mechanical power supplied by the driving motor, after subtracting losses due to windage and friction, brush contact loss, and ohmic losses, gives the total short-circuit stray load loss. This should be equal to the full-load stray power loss in a machine having compensating winding.

During the short-circuit test, the core loss component in a small uncompensated machine will be greater than that occurring under load. In the presence of a weak main field (under

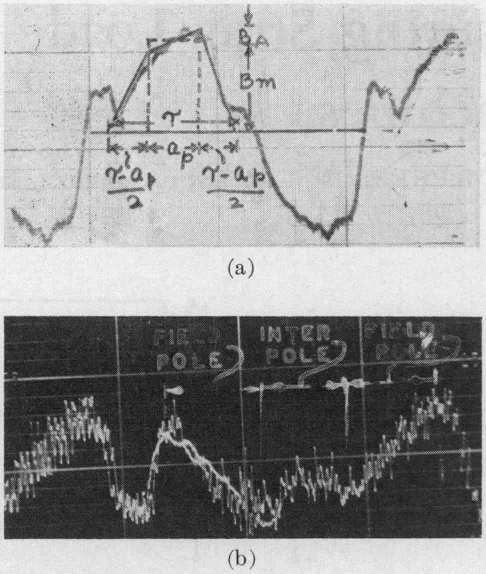

Fig. 2. Typical oscillograms. (a) Under full condition. (b) Under short-circuit condition (I_{sc} = 48.0 A).

short-circuit conditions, the exciting ampere turns required to circulate full-load current will be small) and strong cross-field due to armature magnetomotive force, the flux density will reverse under the pole as shown in Fig. 2. For a machine with compensating windings, this component is not of any significant importance. The increased resistance loss component, due to armature alternating current, is little affected by the magnitude of main field. Therefore, this test should measure stray load losses accurately for the compensated machine while for small, uncompensated machines, the results obtained must be corrected by a factor which will be described later.

Test Procedure

1) After operating under load to attain the temperature rise corresponding to the load in question, the armature is short-circuited through an ammeter, and the machine is driven at rated speed by a calibrated motor. With the residual magnetism reduced to zero, the field excitation is increased from zero until the required load current is obtained. The driving power and the voltage drop across the ammeter are measured. The power lost in the ammeter is subtracted from the driving power to get the net short-circuit power.

2) With the same field excitation and speed, but with the armature open-circuited, the open-circuit power required to drive the machine is measured.

3) Armature dc resistance is measured. This measurement should be made as quickly as possible so as to avoid heating of armature. Armature resistance, after being corrected for the corresponding temperature rise, is multiplied by the square of the load current to obtain the ohmic armature copper loss.

4) Brush contact loss is calculated by multiplying the corresponding current by two volts to conform with the IEEE Standard.

5) Short-circuit stray load loss is then obtained after subtracting losses due to 2), 3), and 4) from 1).

For a machine with compensating windings, the short-circuit stray load loss thus determined should closely approximate the stray load loss of the machine under load at the same armature current and speed used in 1) and 2) of the preceding test procedure. A correction must be applied to make the results useful for uncompensated machines.

Experimental Investigations

To investigate the practicability and accuracy of the short-circuit test as a means of determining stray load loss, a series of tests were carried out on a 12-kW, 250-V, 48-A, 1200-r/min, dc machine. An identical machine was available for pump-back and opposition tests. The stray load loss obtained by the short-circuit test was compared with that obtained by two other methods: 1) pump-back load test proposed by Caldwell [1] and 2) Blondel's opposition test. These are plotted in Fig. 3 as functions of load current. It is seen that the stray load loss obtained by short-circuit test is in close agreement with the average of the other two methods up to I_A = 40.0 A. At full load (I_A = 48.0 A), as expected, the loss obtained from short-circuit data is higher than that found by the other two methods. The major part of this difference is, as explained earlier, due to increased core loss. This increase in core loss at reduced excitation has been reported by Sieron and Grant [4].

Calculation based upon the work of Von Blittersdorff and Hughes (their work on this subject is summarized in Erdelyi [3]) confirms the major role of armature-tooth core loss in the variation in stray load loss between short-circuit and actual load conditions. A search coil was inserted in one armature slot of the test machine in order to obtain the flux-density waveforms necessary to apply these graphical methods. Typical oscillograms for load and short-circuit conditions are shown in Fig. 2. Von Blittersdorff's method assumes that the flux-density waveform under a pole becomes a trapezium under load as a result of armature reaction. On this basis the major iron loss may be computed. The process is shown in the Appendix. The Appendix also shows Hughes' method of computing the excess in tooth iron loss in uncompensated machines under short-circuit conditions. The results of these computations are shown in Table I.

In this table, W_{add}(total) is the core loss component of the stray load loss, according to Von Blittersdorff. Subtracting these values from the corresponding values of incremental iron losses in the teeth under short-circuit conditions, calculated by Hughes' method, gives the approximate number of watts by which the short-circuit stray load loss should exceed that under actual load. Table I shows that, at full load (48 A), the increase in tooth losses under short-circuit conditions is about 54 watts. This accounts for nearly all of the difference between the short-circuit stray load loss in Fig. 3 and that obtained by the other two methods.

Correction Factor

Since the stray load loss obtained by short-circuit test is higher than the actual value under load, the results obtained by short-circuit test may be multiplied by a factor to get the corrected stray load loss. From inspection of Fig. 3, it is seen that stray load loss is nearly proportional to the square of armature current. This is in agreement with the conclusion derived in [2].

Therefore,

actual stray load loss

$$= [\text{short-circuit stray load loss}] \times \left[1 - K_{sc}\left(\frac{I_A}{I_r}\right)^2\right]$$

TABLE I

Oscillogram Load Current (amperes)	W_{add} (hysteresis) Under Load, by Von Blittersdorff [3] (watts)	W_{add} (eddy) (watts)	W_{add} (total) (watts)	W_{tl} (incremental) Iron Losses in Teeth Under Short-Circuit, by Hughes, [3] (watts)	Excess of Short-Circuit Tooth Loss over Incremental Core Loss $W_{tl} - W_{add}$ (total) (watts)
48	41.4	7.4	48.8	102.5	53.7
34	23.0	2.18	25.18	49.4	24.2
24	7.8	0.254	8.054	13.6	5.5
12	4.0	0.0	4.0	5.8	1.8

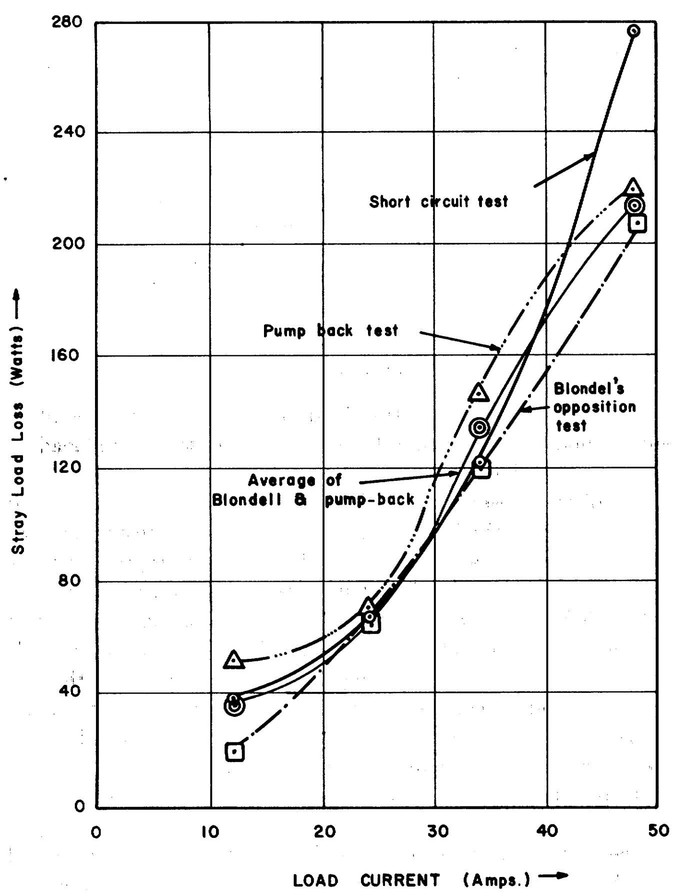

Fig. 3. Comparison of stray load loss by different methods.

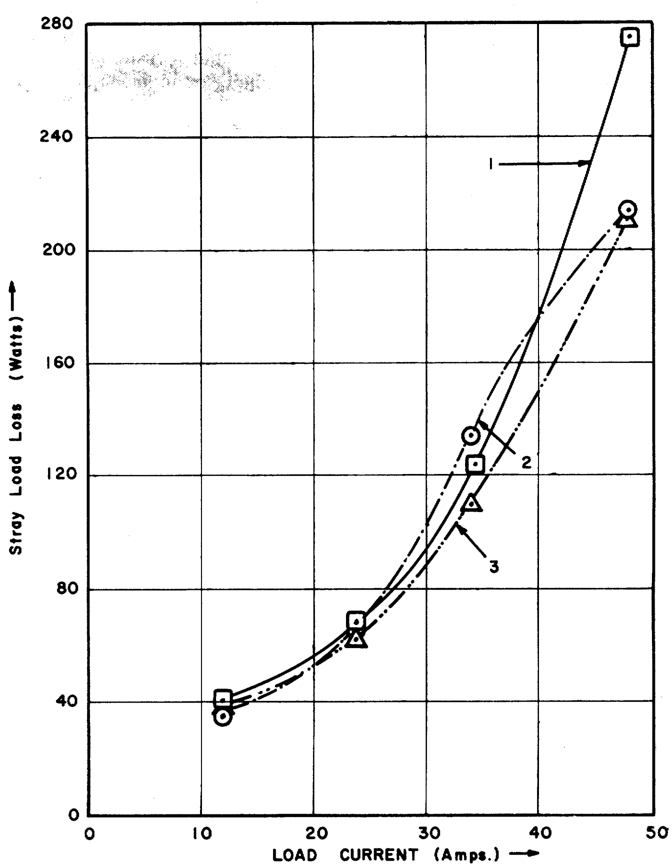

Fig. 4. Stray load loss with correction factor. 1—short-circuit test; 2—averaging the results of pump-back Blondel's opposition test; 3—after correction factor is applied.

where

I_A corresponding load current
I_r rated load current
K_{sc} correction factor (= 0.23 for this machine).

The factor K_{sc}, however, may vary with the design of the machine depending upon the distortion of the flux-density waveform caused by the armature magnetomotive force. In other words, it depends upon the ratio of field ampere-turns to armature ampere-turns. Since this is a second-order effect, linearity may be assumed with good overall accuracy, i.e.,

$$K_{sc} = K \times \left[\frac{\text{field ampere-turns}}{\text{armature ampere-turns}}\right].$$

Fig. 4 shows the stray load loss before and after the correction factor is used.

Measurement Techniques

Usually the pole flux-density waveform under load may be estimated with sufficient accuracy from design data, if such is available. Since nearly all of the difference between load and short-circuit stray load loss is attributable to incremental armature tooth losses, K_{sc} may be determined by application of the methods of Von Blittersdroff and Hughes to compute this incremental loss. Design data was not available for the machine used in the experimental investigation. For this reason a search coil of one turn was introduced into one armature slot to obtain

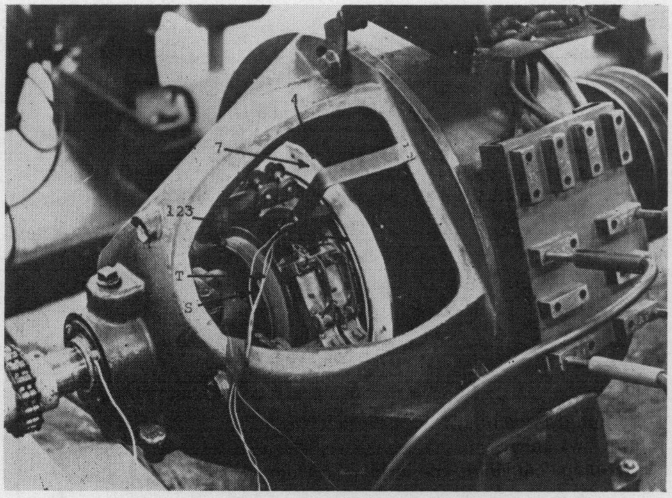

Fig. 5. Current collecting ring assembly.

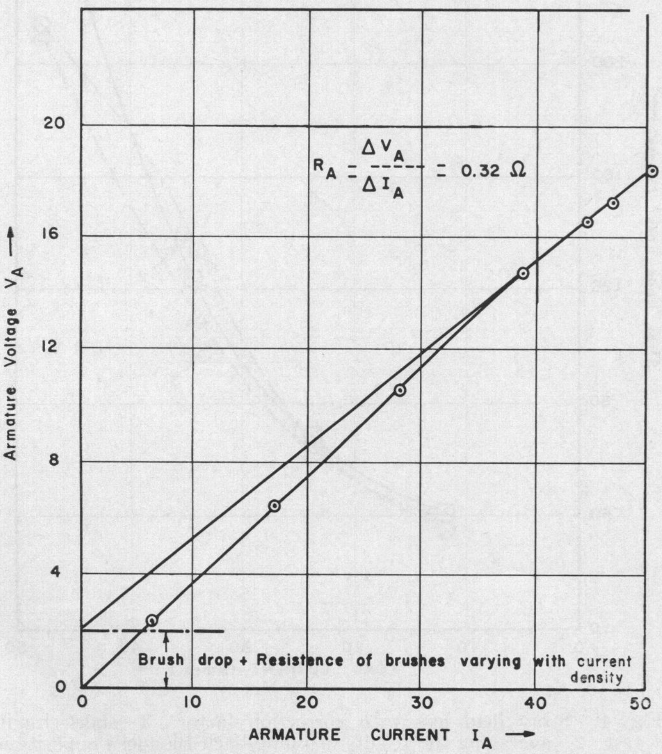

Fig. 6. Measurement of armature resistance.

oscillographs of the voltages generated in the armature at different loads and also during short-circuit test. This search coil is isolated electrically but is subject to the same flux as the armature winding. In this test only one side of the search coil was in the armature slot. The other side was grounded to the motor shaft and hence not included in the flux path. The ungrounded lead was taken via a brass collecting ring to a carbon brush and finally to the cathode-ray oscillograph.

To measure the temperature of the armature winding, a thermocouple was introduced in the armature slot and its two ends were connected to collector rings. Brushes making contact with these rings were connected to a potentiometer. The current collecting ring assembly, which rests on the commutator riser, is shown in Fig. 5.

Armature resistance was measured by a special method suggested by Usry [6]. In this test the field is not excited, armature current is supplied from a variable dc source, and data was taken for a plot of armature voltage drop at the terminals as a function of armature curent. During the measurement, the armature is rotated slowly to average out brush-contact resistance variations. Even at this slow speed, meter damping tends to average out resistance fluctuations resulting from random shorting of armature coils by the brushes. The slope of the linear portion of this curve back to the terminal-voltage axis produces an intercept equal to the brush drop. The process is illustrated in Fig. 6. Most machines, however, exhibit a more pronounced knee in the curve than does the one used in these tests.

A part of the difficulty in dealing with stray load loss lies in its very definition as the difference between total and specified conventional losses. Any small quantity defined as the difference between two large measured quantities is bound to be subject to substantially large errors. This is especially true if one of the large quantities is calculated on the basis of conventions which may not reflect accurately the actual situation. A case in point is the conventional brush-drop loss, which is taken to be $2I_A$. This is a good and reasonable standard until one tries to evaluate stray load loss in a machine having, say, 5 V brush drop. Fortunately, the machine used in these tests conformed to the standard, thus eliminating any ambiguity.

Conclusion

This paper shows that stray load loss obtained by three different methods is greater than 1 percent of the output at full load for uncompensated machines. It thus indicates a need for the revision of the present standards. The authors are well aware that this paper is not the first to suggest a short-circuit test as a means of determining stray load loss in dc machines, especially those with compensating windings (suggested in a discussion by Alger [3]). It is hoped, however, that the concept of a short-circuit test plus a correction factor may be developed into an acceptable standard method for evaluating the stray load loss of both compensated and uncompensated machines. Certainly it should prove more accurate than the present 1 percent standard. It seems probable that investigation would demonstrate the feasibility of setting up standard values of K_{sc} for various classes of dc machines.

The factor K_{sc} depends upon the flux-density waveform and may be determined accurately from tests made on machines for which design data are available. In a well-designed machine, there is a limit to which pole-flux distortion is allowed since it is reflected in commutation difficulties and other armature-reaction effects. Hence K_{sc} has an upper limit and in all probability varies over a rather narrow range for uncompensated machines. In heavy duty and larger capacity machines which may be subjected to heavy overloads, compensating windings are used to reduce this distortion to a minimum. Therefore, K_{sc} is approximately zero for such machines.

Appendix

Fig. 2 shows relative air-gap flux densities for the test machine under full-load and short-circuit conditions.

Von Blittersdorff's work allows one to calculate approximately the excess hysteresis and eddy current losses under load resulting alone from distortion of the pole flux by armature reaction:

$$W_{\text{add}}(\text{hysteresis}) = C_1(2B_mB_A + B_A^2). \qquad (1)$$

Let $B_A = \Delta B_m$, where B_m is the undistorted (no-load) flux density.

$$W_{add}(\text{hysteresis}) = C_1 B_m^2(2\Delta + \Delta^2)$$
$$= (\text{no-load hysteresis loss})\ (2\Delta + \Delta^2).$$

From the no-load test, no-load hysteresis loss = 132.4 watts. Then

$$\therefore W_{add}(\text{hysteresis}) = 132.4(2\Delta + \Delta^2). \quad (2)$$

Values computed from oscillograms taken for different load currents are listed in Table I.

Similarly, by Von Blittersdorff

$$W_{add}(\text{eddy}) = C_2\left[\frac{\tau}{ap} \times \frac{1}{\tau - ap}\right] B_A^2$$
$$= C_2 B_m^2 \left[\frac{\tau}{ap} \times \frac{1}{\tau - ap}\right] \Delta^2 \quad (3)$$
$$= (\text{no-load eddy current loss}) \times \left[\frac{\tau}{ap} \times \frac{1}{\tau - ap}\right] \Delta^2$$
$$= 148.8 \left[\frac{\tau}{ap} \times \frac{1}{\tau - ap}\right] \Delta^2 \quad (4)$$

for the test machine. Graphically determined values of W_{add} (eddy) are also listed in Table I. The sum of these incremental hysteresis and eddy current losses is listed as W_{add}(total) in Table I. W_{add}(total) is the core loss component of the stray load loss, due to pole flux distortion under load, according to Von Blittersdorff.

Hughes points out that this same core loss is greater under short-circuit conditions, because the flux under the pole actually reverses. He shows how to approximate the iron loss in the armature teeth under both load and short-circuit conditions:

tooth loss under load

$$W_{tl} = K_f^{1.5} V_t B_m^2 \quad (5)$$

tooth loss under short-circuit

$$W_{tl} = K_f^{1.5} V_t (B_m + B_m')^2 \quad (6)$$

where B_m' is the maximum flux density under short-circuit conditions.

For the test machine, using (5) and (6), it was found that the tooth loss at short-circuit was over twice what it was at full load. Values of short-circuit tooth loss, from (6), are shown in Table I.

At full load, the core loss component of stray load loss, from Table I, is 48.8 watts. At short-circuit, the tooth component of the stray load core loss itself is 102.5 watts. Thus, the core loss in the teeth, under short-circuit, accounts for the excess in stray load loss, as measured by a short-circuit test.

Acknowledgment

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Discussion

H. B. Hamilton (University of Pittsburgh, Pittsburgh, Pa.): A loss on the order of magnitude of 1 percent, even if the value chosen is in error by 100 percent or more, has practically a negligible effect on the value of calculated efficiency. Why then the interest in establishing a more accurate value? Because the stray load loss affects the temperature rise of the machine and the resulting heat either must be removed or it will cause shortened ultimate life of the machine (unless the rating is reduced). The authors have provided fresh insight into ways of measuring this loss which is extremely difficult, if not impossible, to calculate with any degree of accuracy.

For a comprehensive historical survey of past approaches to all facets of this loss and elaborate ways in which to do theoretical calculations, one can refer to the paper by Dr. Erdelyi [3].

It appears that the test procedure used by the authors is simpler and requires less equipment than the method referred to as Blondel's opposition test. However, the described complexity of that test should not include an unattainable exact neutral setting of the brushes nor exactly equal voltages in the identical machines. As Sieran and Grant [4] point out, these obstacles can be circumvented by taking two tests with "booster" generator current reversed in one of the tests. However, it does require three machines to test the fourth.

This paper confirms previous work, cited by the authors, that the 1 percent figure for stray load losses applied is virtually meaningless. I agree with them that the present standards should be revised to reflect the realistic situation, if possible. If, as they suggest, standard values of K_{sc} can be determined, the standards could be readily revised and the procedure they have suggested seems practical except in the determination of air-gap flux density using an imbedded search coil. Did the authors investigate the feasibility of using a Hall Effect transducer for measuring flux density? It may be feasible to cement several of these small devices across the field pole faces and to obtain an approximation to air-gap flux-density variations. This procedure, if workable, would eliminate the problem of embedding the search coil on the armature and the necessity of slip rings to bring out the resulting voltage. The Hall Effect device would measure flux density directly.

The authors are to be congratulated for their work and their presentation.

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J. S. Ewing (Reliance Electric Company, Cleveland, Ohio): As a member of the IEEE dc Subcommittee of Rotating Machinery, let me congratulate the authors for tackling a rather sticky subject matter that has been on our agenda for a number of years.

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Our experience has shown that it is extremely important to establish the correct resistance of the armature winding at the temperature of the test. In some of the tests we have made, we found that on larger motors, it is impossible to completely short-circuit the motor, and the resistance and associated losses of the conductor connecting the opposite polarities of the motor must be accounted for. In addition, it is quite educational to use potential probes riding on the commutator to measure the small terminal voltage used to overcome the brush drop and the short-circuiting resistance. It should be mentioned that C. Coho of the General Electric Company, has reported using a form of the short-circuit stray load test for measuring stray load and has found from experience that the actual stray load loss amounts to about 70 percent of the measured short-circuit loss. It is obvious that we need additional data relating K_{sc} to actual design of motors.

In conclusion, I would like to congratulate the authors for the practical results they have obtained.

E. P. Priebe (General Electric Company, Erie, Pa.): Attention is invited to IEEE standard 11 [7], section 8.4 (e), which states:

Stray-Load Losses for Direct-Current Machines

The stray-load losses shall be taken as a percentage of the test short-circuit loss in accordance with the following tabulation:

Ratio = Exciting Field Ampere-Turns / Armature Ampere-Turns Between Brush Centers	Stray-Load Losses as a Percent of Short-Circuit Losses
0	100
0.5	81
1.0	64
1.5	50
2.0	40
2.5	35

The type test for short-circuit losses shall be made with an ammeter connected with as little resistance as possible across the armature terminals of the machine. The desired values of rpm shall be obtained by driving the machine with a small motor of known efficiency and about twenty-five percent or less of the capacity of the machine being tested. Only enough field excitation shall be used to cause the desired values of armature current to flow through the short circuit. Interspersed readings of armature circuit resistance shall be taken. I^2R losses, friction and windage losses shall be subtracted point by point from the input to shaft of the machine, the short-circuit losses being the remainder.

It is unfortunate that this standard applying to rotating electric machinery on railway equipment is not better known in the industrial field.

As it turns out, this paper essentially agrees with IEEE standard 11, except that in the railway standard the discount factor giving load loss from short-circuit core loss is fully defined and does not distinguish between compensated and uncompensated machines.

The identification of components of stray load loss is interesting. There is no question that there are core loss components. Without belaboring the components which the authors list as increasing the effective resistance, attention is invited to the fact that such losses would reduce the speed of the machine. The way the short-circuit core loss test is run, all losses show up in torque. The common practice is to subtract all load loss from torque. It appears that the torque loss component predominates in actual service. One reason for this is that the no-load excitation (and hence core loss) provides for flux reversals at pole frequency; although these also show in short-circuit core loss, they must be discounted out of load loss to avoid duplication.

In practice the short-circuit core loss test is difficult to run and the data is frustrating to analyze. Finding residual loss by subtracting known losses from input gives the difference between large numbers. Winding temperature is a major problem because resistance loss is relatively large; even taking winding rise by resistance often and trying to reckon the actual resistances when points were read, short-circuit core-loss data is widely scattered.

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Short-circuit core loss data will be useful only when curves can be drawn to give a loss value at any operating point. This implies that some basic assumptions have been made about the phenomena involved to predict how loss should vary with the parameters that establish the operating point. The scatter of test results could lead to two different conclusions from two tests on the same machine.

In conclusion, it should be said that load loss calculations of the type proposed by the authors of this paper have been used in railway work by manufacturers following IEEE standard 11 for a number of years. It is felt that this procedure gives a more accurate estimate of load loss than a convention such as one percent of input. However, this has been in a product line of a limited number of machines, and considerable effort has gone into correlation to establish a form in which loss data may be used.

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K. R. Shah and **G. McPherson, Jr.**: The authors would like to thank the discussers for their interest in the paper and for pointing out the important references. There seems to be no disagreement with the conclusions derived in this paper.

1) The present IEEE standard of considering stray load loss equal to 1 percent of the output for both compensated and uncompensated dc machines is meaningless and hence needs revision.

2) The proposed test method due to its extreme simplicity can be developed into an acceptable standard by determining standard values for K_{sc}.

Prof. Hamilton's assertion about the exact neutral settings of brushes in Blondel's test is correct if one is interested only in a measurement of total stray load loss. We wished to separate the core loss from the resistance loss components and found that if the brushes were not arranged into exact no-load neutral position, we got erroneous information as to the presence of the core loss and resistance loss components to the extent that at full load, the increased resistance loss component was actually negative! It was this fact which forced us to separate stray load loss into its two components by using graphical analyses, as reported in this paper.

Obviously, some sort of experimental program would have to be established to determine typical values of K_{sc}. Such a program would involve tests on a wide variety of fully instrumented motors. The problems involved in this test program must be distinguished from those which would arise in short-circuit tests of "run of the mill" motors, once the new standards were established. One can hope that experience gained in the test program would lead to the specification of a standard test procedure which could be applied without undue difficulty. For example, once values of K_{sc} were established for a particular class of machine, it would be unnecessary to take air-gap flux-density patterns in a specific machine in that class in order to determine its stray load loss by short-circuit test.

We are grateful for Prof. Hamilton's suggestion that Hall-effect devices might be used to sample the pole-face flux density at various points. Such devices were not available to us at the time of our experiments. The application of such devices requires removal of the armature, as does the insertion of a search conductor into an armature slot. Whether the equipment required to excite and calibrate these devices would require more time and effort than our slip-ring assembly can only be decided by experience. We experienced the difficulties described by Mr. Priebe and Mr. Ewing with regard to armature temperatures and feel that it would be desirable during the initial test program to have thermocouples imbedded in the armature slots, as we had during our tests. This would make slip rings necessary, in any event.

We agree with Mr. Ewing that it is impossible to completely short-circuit the motor and since the resistance of the armature of a large

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machine is much smaller, one must include resistance loss of the connecting wires along with the internal resistance loss of the ammeter.

We may also point out that at full load, using $K_{sc} = 0.23$ for the machine tested, we obtain actual stray load equal to 77 percent of the measured short circuit loss which is comparable with the experience of C. Coho.

We are glad to note from Mr. Priebe's discussion, the important reference to IEEE standard 11, Section 8.4(e) [7]. This agrees with the findings of our paper that the actual stray load loss depends upon a K_{sc} defined to be proportional to the ratio of the field ampere-turns to armature ampere-turns. However, our paper makes the additional and fundamental point that the stray load loss is not a fixed percentage of short-circuit losses for a given design, but varies as the square of the armature current, and that K_{sc} is different for compensated and uncompensated machines. Also, we feel that using the proposed formula for deriving actual stray load loss has some advantage over the IEEE standard 11 because one can find the actual stray load loss for any value of the ratio of field ampere-turns to armature ampere-turns.

Mr. Priebe is no doubt correct that those components of stray load loss which effectively increase the armature resistance cause a slight decrease in speed of a motor below that predicted from dc armature-resistance measurements. Since these components represent a minor fraction of the stray load loss, which in itself is about 1 percent of the total power, the slight difference in speed is likely to be swamped out by other effects. These components appear as torque during the short-circuit test because all of the armature copper losses appear as torque during this test.

In conclusion, we hope that the future work of the dc subcommittee, in cooperation with industry and educational institutions, will lead to the revision of the present 1 percent standard and that they will find our suggestions helpful in devising more realistic standards for the determination of stray load loss from tests.

Solution of Load-Flow Problems by Partitioning Systems into Trees

BERNARD A. CARRÉ

Abstract—**This paper presents a method of calculating load flows by partitioning a system into subsystems of tree form, for which the node-voltage equations can be solved very efficiently by optimally ordered Gaussian elimination. The solution for the complete system is then obtained by coupling direct solutions for the trees by a block-iterative method. Results comparing the block method with the accelerated Gauss–Seidel method show that the block method is considerably faster.**

Introduction

IN SEVERAL recent papers [1]–[3] it has been demonstrated that the amount of computation involved in solving a sparse set of network node-voltage equations by an elimination method depends on the topology of the network, and on the order in which the eliminations are performed. It has also been shown that although it is difficult to determine an optimal ordering of the elimination procedure for a complete power system, in the particular case of a network of tree form an optimal ordering is easily found, and this leads to an extremely efficient one-term elimination scheme [4].

This paper describes a block-iterative method of solving load-flow problems which takes advantage of the simplicity of the optimal elimination procedure for tree networks. To obtain a solution for a complete power system, the system is first partitioned into a number of subsystems, each of tree form. The optimal ordering for each tree is then determined, and the set of node-voltage equations for each tree is factorized. The solution for the complete system is then obtained by a block-iterative method [5], [6] in which direct solutions are obtained for each tree in turn, repeatedly.

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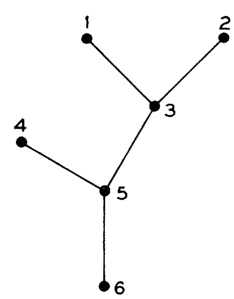

Fig. 1.

The computer storage requirements and the work involved per iteration are very little greater than for the "accelerated Gauss–Seidel" or successive overrelaxation (SOR) method, but convergence rates are considerably better, particularly for large systems for which the block method is several times faster.

Results comparing the performance of the block method and the SOR method are given for several IEEE standard test systems and for British systems of up to 515 busbars.

Optimal Elimination Scheme for a Tree Network

For any network whose line diagram is of tree form (Fig. 1) after removal of connections to reference nodes, the network node-voltage equations

$$YV = I \tag{1}$$