DIFFERENTIAL GENERATOR PROTECTION WITH NON-RESTRAINT RELAYS

by

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ABSTRACT

The purpose of this work is to investigate the possibility of using non-restraint relays in a generator differential protection scheme.

The effects of both the a.c. and d.c. components of through-fault current on the performance of current transformers is examined. The design equations for the construction of a non-saturating current transformer are established. The technique of balancing current transformer burdens to prevent false differential relay operation on through faults is also examined. The relative effects of the a.c. and d.c. fault current on the differential protection system are then investigated on a reduced scale by experimentation.

A differential protective system is then designed, in light of the alternatives presented, to facilitate the use of a non-restraint relay in the protective system.

CHAPTER I

INTRODUCTION

Generators, the most costly single component of power generating systems, require reliable and sensitive relaying for their protection (Von Roeschlaub, 1958, p. 65). An internal generator fault generally develops as a ground fault in one phase and often spreads to involve more than one phase. Differential protection is by far the most effective known type of protection against such faults. Ideally, the current entering a winding is compared differentially to that leaving the winding, and the difference current is used operate a relay (Applied Protective Relaying, 1964, p. 4-1).

Current transformers step down thousands of primary amperes to a nominal level of five amperes for relaying. The current transformer serves a second, somewhat unrelated purpose in that it electrically isolates instruments and relays from the high-voltage primary circuits (Matsch, 1952, p. 1). The heart of the protective system is the current transformer. Under normal primary conditions, current transformers (C.T.) can be balanced or matched to a very close degree. There are, however, two types of errors inherent in the C.T. These are current ratio error and phase angle error. These errors can become extremely important under large-current transient conditions.

Under heavy through-fault conditions, the exciting currents of the C.T. may become quite unbalanced, and thereby produce undesirable relay operation. The current practice to avoid relay operation on heavy through faults is to use relay restraining elements whose restraint is proportional to the magnitude of fault current, thereby complicating the relay.

As far as detecting internal faults, nearly any type of C.T. arrangement is sufficient. It is the C.T. performance during through faults that is critical to the proper operation of the protective system (Applied Protective Relaying, 1964, p. 4-2).

The purpose of this paper is to investigate the possibilities of designing a reliable protective system using a simple non-restraint relay. The most attention will be devoted to C.T. design. A complete differential protective system must be designed in accordance with the current rating and armature short circuit time constant of a particular machine, and must function properly under all fault conditions at the generator terminals.

CHAPTER II

STEADY STATE ANALYSIS WITH NO INTERNAL FAULT

The equivalent circuit for a current transformer is shown in Figure 2.1. In this figure, the voltages, currents, and various parameters are as follows:

I_φ = the exciting current

Y_φ = the exciting admittance

E_2 = the secondary induced EMF

Z_S = secondary leakage impedance

N_1 = the number of primary turns

N_2 = the number of secondary turns

I_S = the secondary current

The equivalent circuit which will be used for the secondary circuit of the C.T. is shown in Figure 2.2 (Mathews, 1955, p. 6). The basic differential protection circuit is shown in Figure 2.3. Redrawing the circuit with the C.T. model, we arrive at Figure 2.4. In this figure, Z_{L1} and Z_{L2} are the impedances of the secondary wiring from each C.T. to the relay. From the circuit diagram we see that for C.T.'s of the same turns ratio, the relay current I_R is given by

$$I_R = I_{\varphi_1} - I_{\varphi_2} \qquad (2.1)$$

the difference in the two exciting currents.

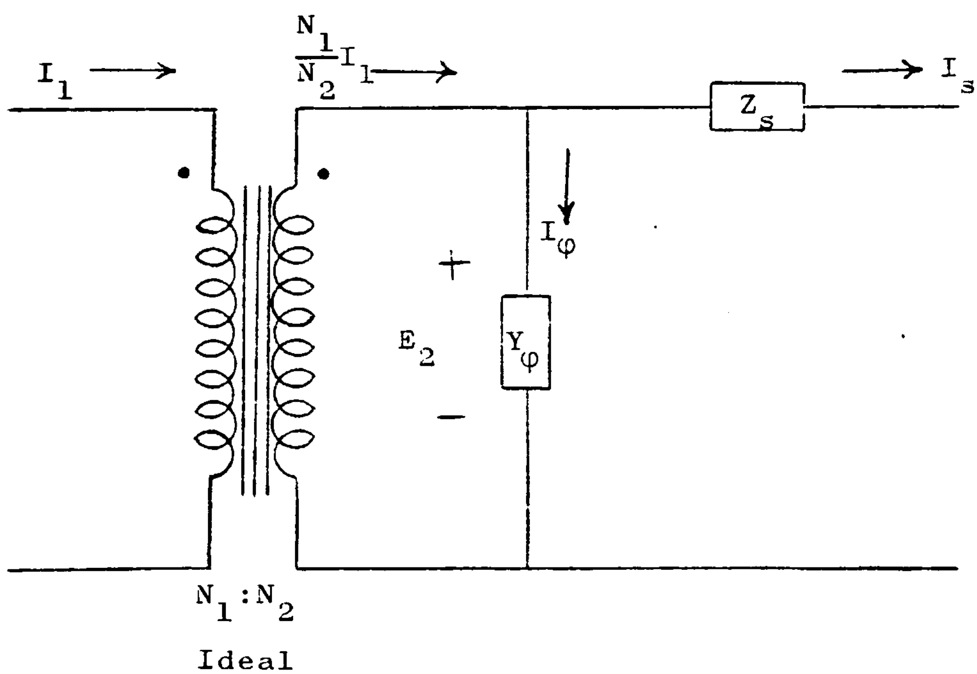

Figure 2.1. Current Transformer Equivalent Circuit

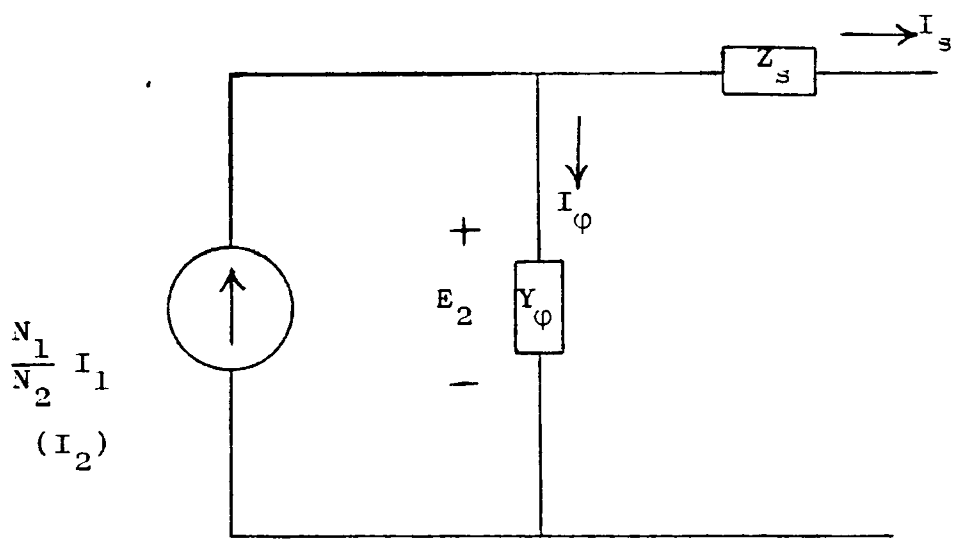

Figure 2.2. Current Transformer Equivalent Circuit with Ideal Transformer Replaced by Ideal Current Source

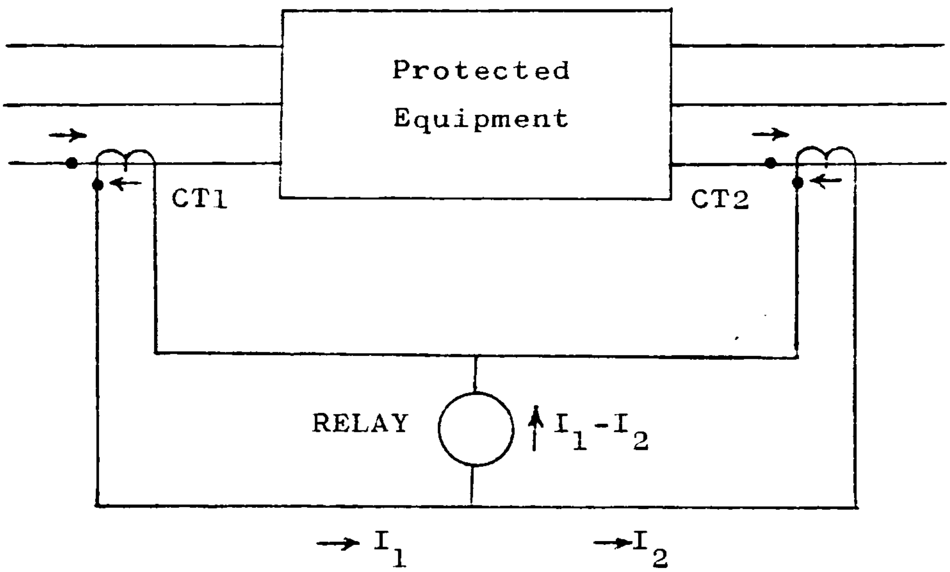

Figure 2.3. Basic Differential Protection Circuit (shown for one phase only)

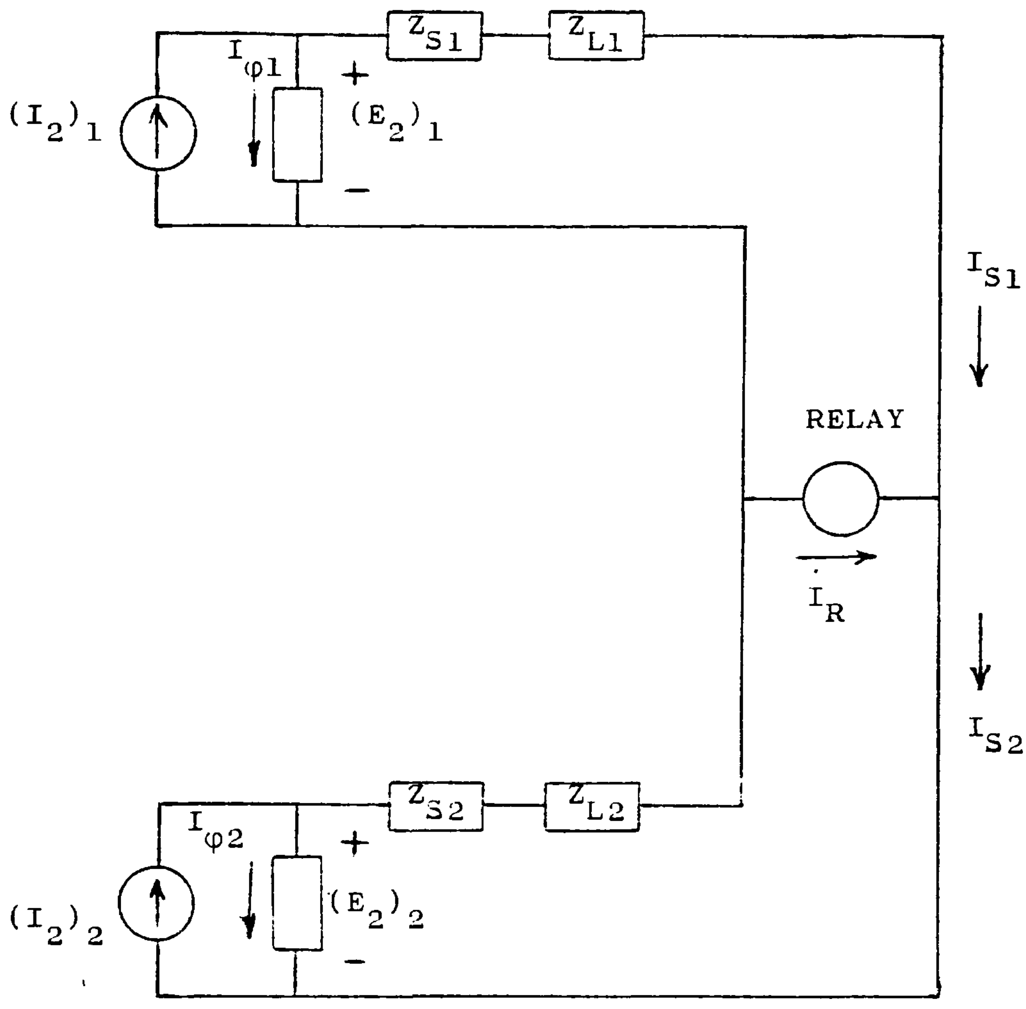

Figure 2.4. Differential Protection Circuit Showing Current Transformer Equivalent Circuit

For no relay operation, the relay current must be below the minimum relay pick-up current, and, therefore, the relay voltage will be correspondingly low. Then, for analysis of an external fault of zero average fault current, the relay may be replaced by a short circuit. This is a rather conservative approximation, for the current through a relay would be even less than that in the short circuit due to the relay impedance.

From Figure 2.4

$$(E_2)_1 = I_{S1}(Z_{S1} + Z_{L1})$$

and

$$(E_2)_2 = I_{S2}(Z_{S2} + Z_{L2}).$$

The relay current is given by

$$I_R = I_{\varphi_1} - I_{\varphi_2}$$

and for zero relay current, the criterion is

$$I_{\varphi_1} = I_{\varphi_2}.$$

The magnetizing current is a function of the secondary induced EMF of the current transformer. Saturation of C.T.'s may lead to false operation on through faults. The saturation point for the C.T. can be obtained from a magnetization curve. A curve for a particular C.T. is shown in Figure 2.5. If the iron is worked below saturation (below φ_1) there exists a linear relationship between

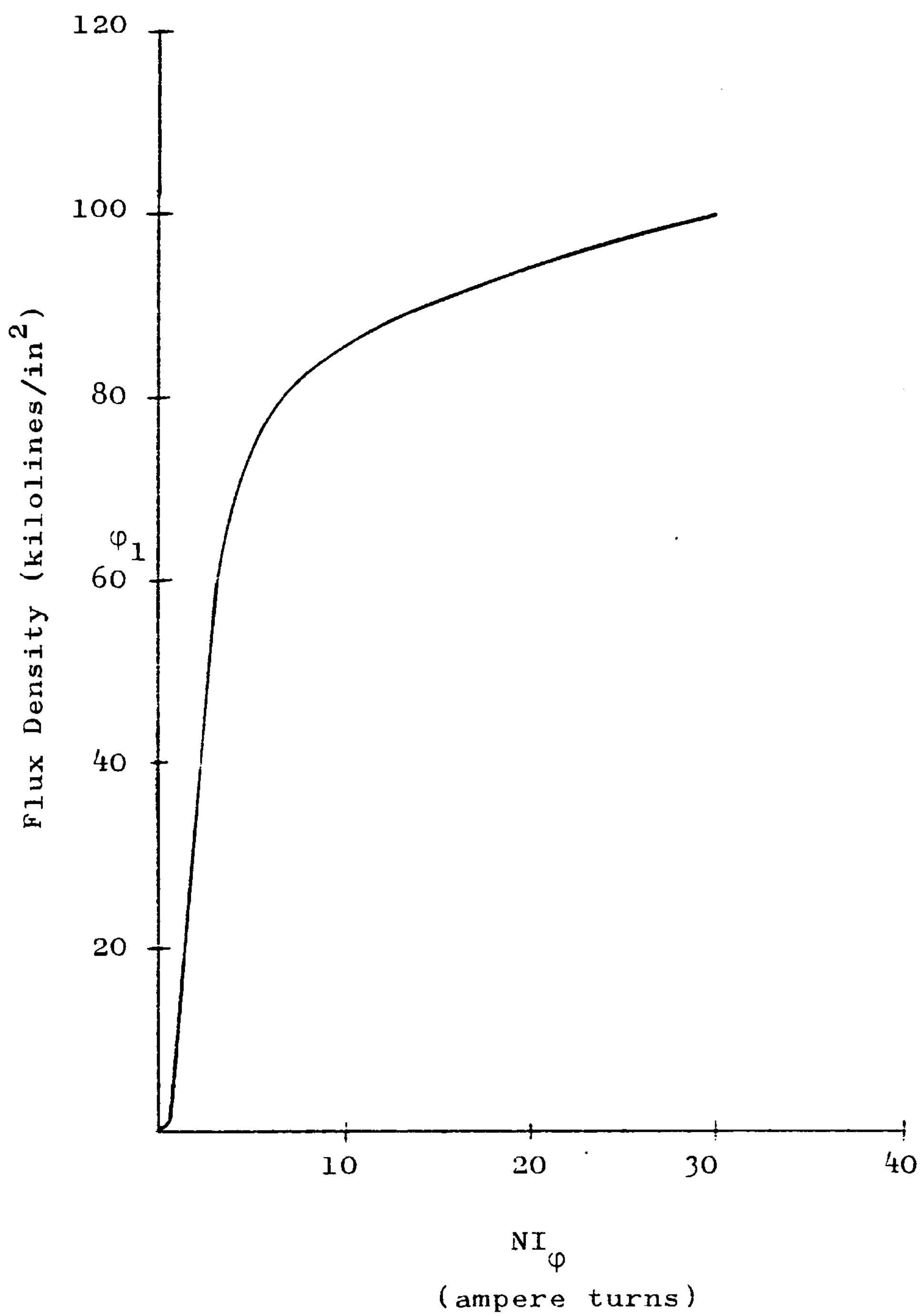

Figure 2.5 A.C. Magnetization Curve for Iron Core Current Transformer

the flux and the exciting current I_φ, i.e.,

$$\varphi = C_1 I_\varphi .$$

In addition the exciting current is small in this region. For large flux swings, however, saturation produces an extreme non-linearity in I_φ.

A sinusoidal impressed voltage produces a flux in the iron as shown in Fitzgerald and Kingsley (1961, p. 356) which is given as

$$\varphi_{max} = \left(\frac{1}{4.44f}\right)\left(\frac{V}{N}\right) \text{ webers/in}^2$$

where

V = applied r.m.s. voltage

f = frequency in hertz

N = number of turns

φ_{max} = maximum core flux.

For a given core size, frequency, and number of secondary turns, the flux density in the C.T. is

$$\varphi_{max} = C_2 V$$

where $C_2 = \frac{1}{4.44fN}$. Therefore,

$$C_1 I_\varphi = C_2 V$$

or

$$I_\varphi = KV$$

where $K = C_2/C_1$.

Now, let $I_{\varphi_1} = K_1(E_2)_1$ and $I_{\varphi_2} = K_2(E_2)_2$. For the imposed condition of equal magnetizing currents, we obtain

$$K_1(E_2)_1 = K_2(E_2)_2$$

and, therefore,

$$K_1[I_{S1}(Z_{S1} + Z_{L1})] = K_2[I_{S2}(Z_{S2} + Z_{L2})]$$

$$I_{S1}K_1(Z_{S1} + Z_{L1}) = I_{S2}K_2(Z_{S2} + Z_{L2}).$$

For zero relay current

$$(I_2)_1 = (I_2)_2$$

and

$$I_{\varphi_1} = I_{\varphi_2}$$

and

$$I_{S1} = I_{S2}.$$

Therefore,

$$K_1(Z_{S1} + Z_{L1}) = K_2(Z_{S2} + Z_{L2}). \qquad (2.2)$$

For a particular transformer, K_1, K_2, Z_{S1}, and Z_{S2} are fixed. The two line impedances will have a minimum value, but either can be increased to satisfy the above equality.

This design criterion holds under the assumption that the C.T.'s are linear and do not saturate. The only qualification, therefore, for the design equation is that the voltage applied to each transformer be kept below that voltage which would cause the iron to saturate.

Under careful inspection of a magnetization curve we see that the transformer is actually linear beyond the knee of the curve. In fact, the permeability beyond saturation eventually approaches that of air. The only real nonlinearity occurs at the knee of the saturation curve. This gives rise to the question of allowing the transformers to saturate. The only change in the derivation is that the flux is no longer linearly related to the exciting current. However, the relationship can be expressed as

$$\varphi = \Psi(V) I_{\varphi} \tag{2.3}$$

where $\Psi(V)$ is a function of induced voltage. The same derivation as before leads to the result that

$$\Psi_1(V)[Z_{S1} + Z_{L1}] = \Psi_2(V)[Z_{S2} + Z_{L2}]. \tag{2.4}$$

As long as both transformers saturate at the same rate, the values of Z_{L1} and Z_{L2} can be adjusted to satisfy the above equality. If the two C.T.'s are identical, then the secondary burdens must be identical to satisfy the equality. When the equality is satisfied, saturation of the two transformers will occur simultaneously and no relay operation will result.

CHAPTER III

TRANSIENT ANALYSIS WITH EXTERNAL FAULT

In dealing with the performance of C.T.'s during power system faults, one must look at the effects of through-fault currents on the C.T. In order to do this one must deal with the worst possible conditions imposed on the generator. For the case of a solidly grounded generator, a single-line-to-ground fault near the generator terminals creates the greatest possible external fault current in a winding of the generator. However, common practice is to ground a generator through an impedance such that the single-line-to-ground fault current does not exceed that of a three-phase short at the generator terminals. No other type of external fault produces more fault current in a particular winding of a generator than the three phase fault. Therefore, the worst possible fault on the generator is a three-phase fault near the generator terminals, and this condition is the one of interest in this paper.

Majmudar (1965, p. 493) has shown that the current under a three-phase fault condition in one winding of a synchronous generator is given by:

$$i_{ac} = \sqrt{2}E_a \{[\frac{1}{x_d} + (\frac{1}{x_{d'}} - \frac{1}{x_d}) e^{-t/T_{Q'}}] \cos(wt + \Psi)$$

$$- (\frac{x_{d'} + x_q}{Z_{x_{d'}, x_q}}) e^{-t/Ta} \cos(\Psi) - (\frac{x_q - x_{d'}}{Z_{x_{d'}, x_q}}) \cdot$$

$$e^{-t/Ta} \cos(2wt + \Psi)\}. \quad (3.1)$$

The condition which will produce the maximum current is that of a completely offset wave which will occur for

$$\Psi = 0.$$

Neglecting the decrement factor for the a.c. component of fault current as well as the second harmonic component of fault current as given by equation (3.1), a completely offset current wave may be expressed as:

$$i = \sqrt{2}\, I\, [e^{-t/Ta} - \cos wt] \quad (3.2)$$

where I = r.m.s. value of maximum a.c. current and the short circuit armature time constant $Ta = \frac{x_{d''} + x_{q''}}{2wr_a}$. Therefore, for the ideal transformer with a single turn primary,

$$i_2 = \frac{\sqrt{2}\, I}{N} (e^{-t/Ta} - \cos wt). \quad (3.3)$$

The induced secondary voltage is

$$e_2 = \frac{\sqrt{2}\, I}{N}[R(e^{-t/Ta} - \cos wt) + L\frac{d}{dt} (e^{-t/Ta} - \cos wt)] \quad (3.4)$$

where R = total resistive burden and L = total inductive burden. The flux in the C.T. is given by

$$\varphi = \frac{1}{N}\int_{t_1}^{t_2} e_2 dt.$$

For the worst condition, t_1 will be zero. Therefore,

$$\varphi = \frac{1}{N}\int_0^t \frac{\sqrt{2}\ I}{N}[R(e^{-t/Ta} - \cos wt) + L\frac{d}{dt}(e^{-t/Ta} - \cos wt)]dt$$

Integrating, we get

$$\varphi = \frac{\sqrt{2}\ I}{N^2}[R(-T_a e^{-t/t_a} + \frac{\sin wt}{w}) + L(e^{-t/T_a} - \cos wt)]_0^t \quad (3.5)$$

$$\varphi = \frac{\sqrt{2}\ I}{N^2}[R(\frac{\sin wt}{w} - T_a e^{-t/T_a} + T_a) + L(e^{-t/T_a} - 1) - L(\cos wt - 1)].$$

Now, as t becomes large

$$\varphi = \frac{\sqrt{2}\ I}{N^2}[R(T_a + \frac{\sin wt}{w}) - X_L(\frac{\cos wt}{w})]. \quad (3.6)$$

For sixty hertz operation,

$$\varphi = \frac{\sqrt{2}\ I}{N^2}[R(T_a + 0.00266 \sin wt) - X_L(0.00266 \cos wt)].$$

For modern power system generators, the value of T_a usually is in the range of one-tenth to one-half second. For a small generator with time constant of only one-tenth second, the a.c. component of flux in the C.T. is less than three per cent of the d.c. component of flux.

Therefore, for large generators, with a correspondingly larger time constant all a.c. components may be neglected without a serious loss in accuracy. With this approximation, the flux equation reduces to

$$\varphi \approx \frac{\sqrt{2}\ I}{N^2} [R\ T_a]. \qquad (3.7)$$

It is interesting to note that the inductive part of the C.T. burden has a negligible effect on the flux in the C.T. In the design of a non-saturating C.T., the flux given by the above equation is the maximum flux in the C.T.

CHAPTER IV

EXPERIMENTAL DETERMINATION OF RELATIVE EFFECTS OF A.C. AND D.C. CURRENTS

Tests were made to determine the relative effects of the d.c. and a.c. components of fault current on the performance of the protective system. The two C.T.'s used were sheet-steel wound ribbon type. The cross-section of each was one-half inch square and the mean radius of each was 1.75 inch. The turns ratio used was 50/10. The test circuits used are shown in Figures 4.1 and 4.2.

The switching in the d.c. test circuit was accomplished by 3 heavy duty relays with suitable logic for a make before break contact. This was necessary in order to ensure a complete circuit involving the inductor. The inductor used was the field winding of a 5-kw d.c. generator with a time constant of .33 sec. as indicated by Figure 4.3.

Energy was stored in the inductor and then discharged through the circuit containing the series C.T.'s. This simulated, on a much reduced scale, the d.c. component of a through-fault current which might flow in a synchronous generator on a power system. A 2.5-ohm shunt was alternately placed between points A and B, and also A and C on the secondary side of the C.T.'s. The primary current

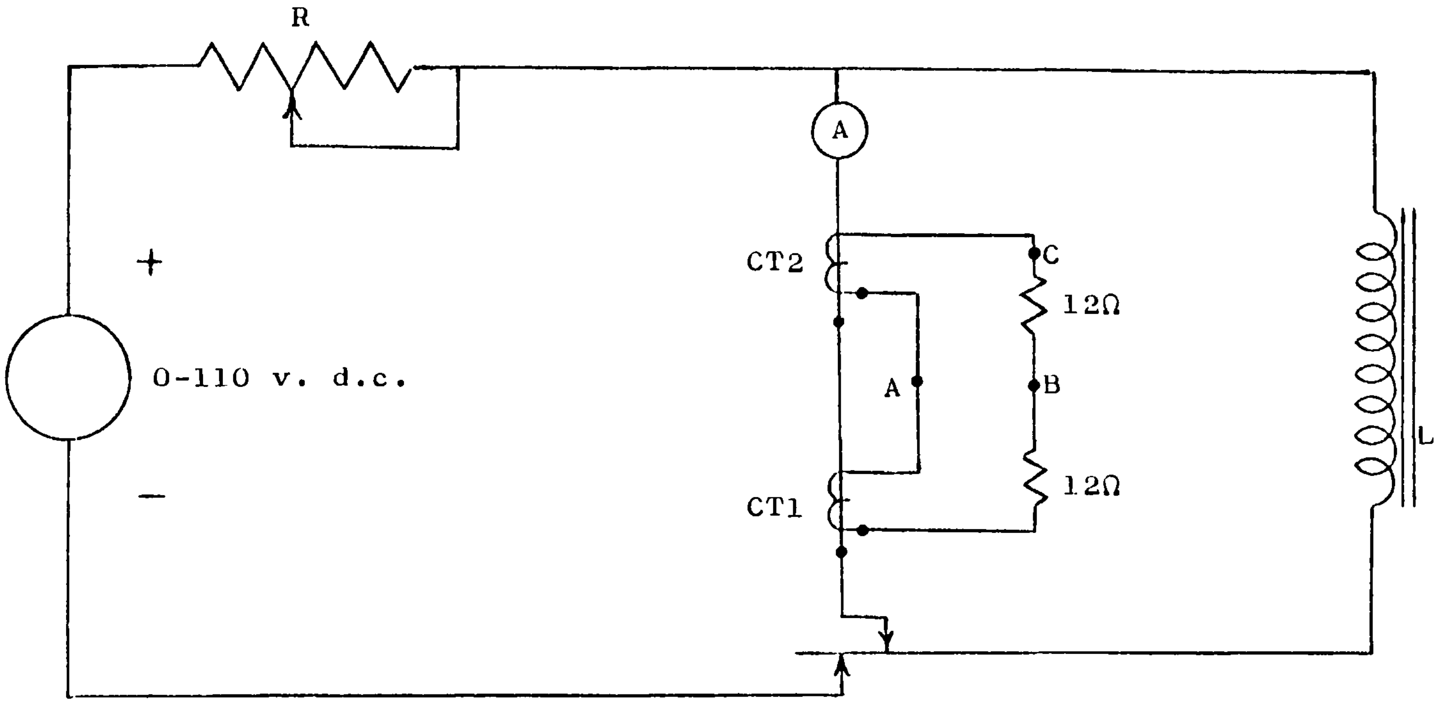

Figure 4.1. D.C. Test Circuit

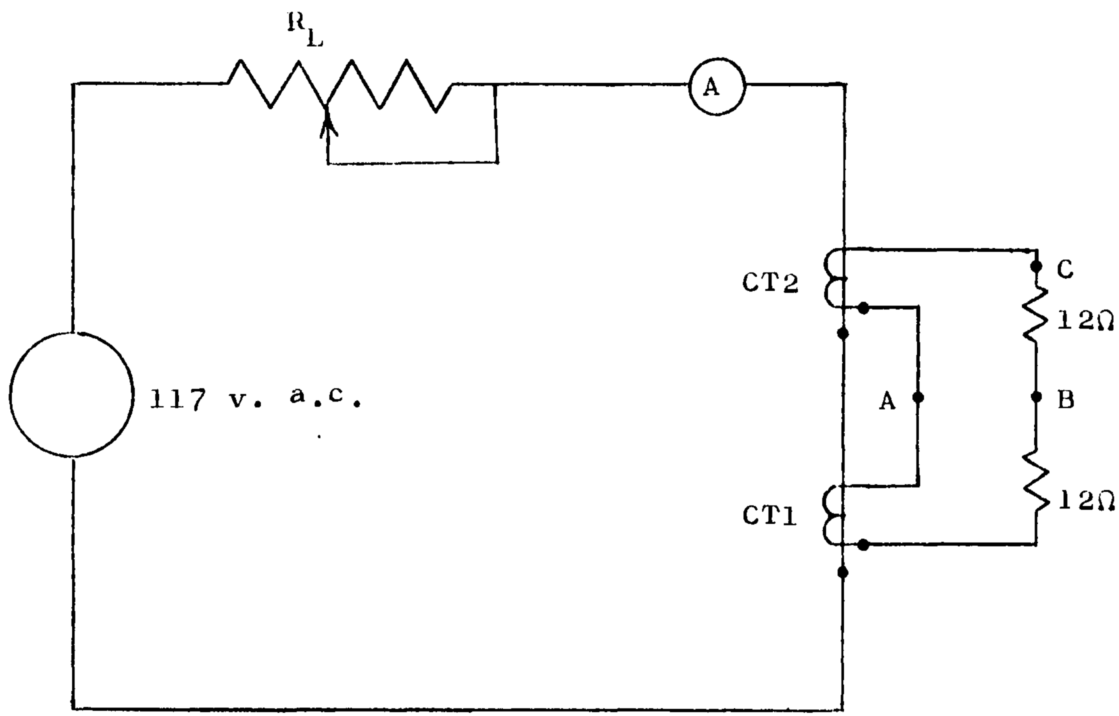

Figure 4.2. A.C. Test Circuit

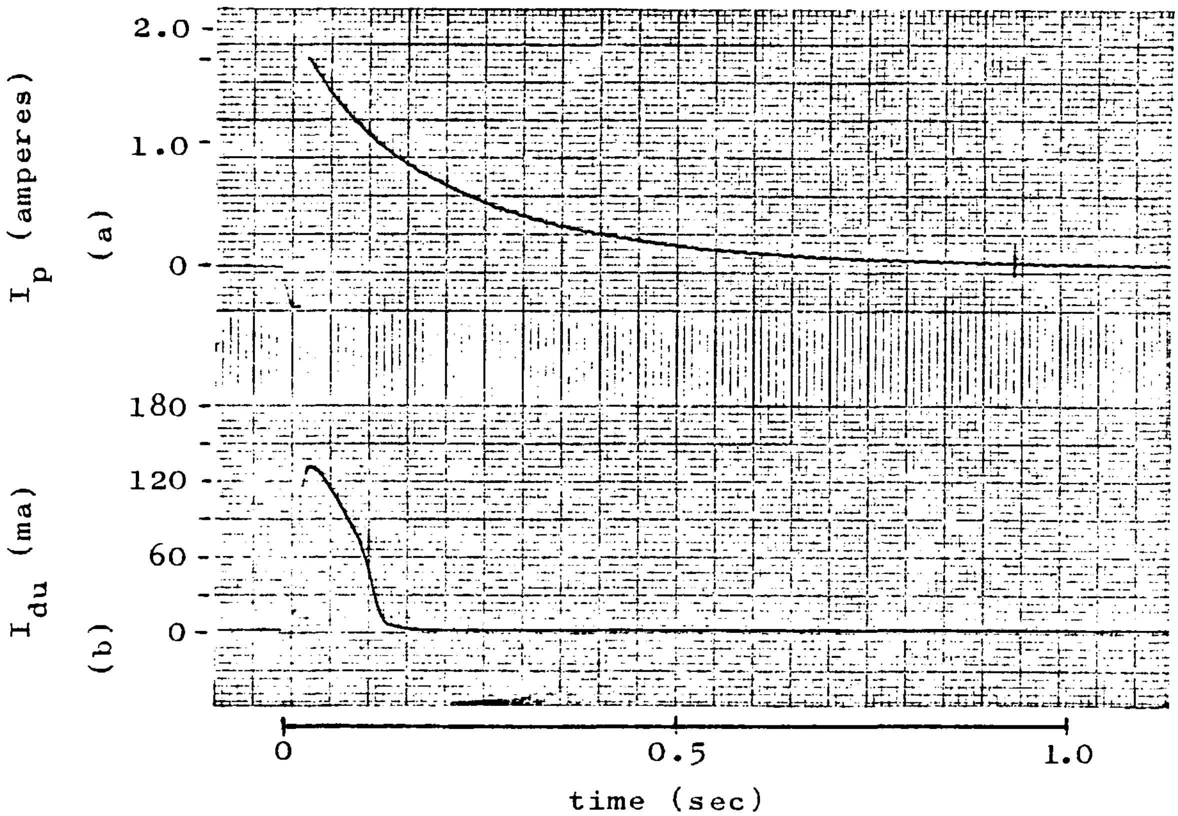

Figure 4.3. Sanborn Recorder (#003736) Strip Chart with Initial Primary Current of 1.8 Amperes Showing Differential Secondary Current with Unbalance Secondary C.T. Burdens -- (a) Primary current, (b) differential secondary current.

was observed by the measuring of the voltage drop across a 0.22-ohm resistor in the primary circuit. A trace of primary current and differential current through the 2.5-ohm shunt was recorded on a Sanborn recorder (Figure 4.3). The initial negative swing of primary current was caused by the make before break contacts associated with the relay. This transient, however, has very little effect on the C.T.'s when compared to the much longer d.c. transient.

The initial positive current in the shunt when connected from A to C is primarily the magnetizing current of CT1. This is due to the relatively heavy burden seen by CT1 as opposed to that seen by CT2. This current tends to level off as both C.T.'s saturate, and falls off when the primary current can no longer sustain this secondary current.

The current in the shunt when connected from A to B is the difference in the exciting currents when both C.T.'s have equal burdens and saturate simultaneously.

The results of the d.c. tests are given in Table 4.1. In this table I_p is the peak initial primary current, I_{db} is the peak differential current with balanced burdens, and I_{du} is the peak differential current with unbalanced burdens.

In the a.c. tests, a variable load was used to control the primary current of the C.T.'s. In these tests, however, an ammeter was used in the secondary circuit of

Table 4.1. D.C. Test Data

$\frac{N_1}{N_2} I_p$ (ma) (peak)	Balanced Burden Differential Current I_{db} (ma) (peak)	Unbalanced Burden Differential Current I_{du} (ma) (peak)
40	0	13.2
80	3	26.4
120	6	33.0
160	6	46.2
200	6	79.2
240	8	92.4
280	8	111.3
320	10	118.8
260	10	132.0

the C.T.'s to record the balanced and unbalanced differential currents. The test circuit is shown in Figure 4.2.

For the ammeter connection between A and B, the difference in the magnetizing currents for C.T.'s with equal burdens was measured. With the ammeter connected from A to C the difference in magnetizing currents for C.T.'s with unequal burdens was recorded. The results are given in Table 4.2 for the a.c. tests. In this table I_p is the r.m.s. primary current, I_{du} is the r.m.s. differential current with unbalanced burdens, and I_{db} is the r.m.s. differential current with balanced burdens.

Comparing the results of the two tests, it is evident that the d.c. current causes saturation at lower current levels than a.c. current. If, for instance, the maximum a.c. current for a through fault was 1 ampere a.c., there would be no problem in using C.T.'s with unbalanced burdens in a practical range. The relay pickup current could be set at slightly above 10 ma. This value of pickup current would represent slightly over 50 ma unbalance in the primary ckts., or about a 5% unbalance. However, for a through fault, the primary current may be completely offset. For this condition, with unbalanced burdens, the d.c. component of primary current alone requires that for correct relay operation, the pickup current must be in excess of 79.2 ma. This corresponds to approximately 400 ma unbalance in the primary circuit, or about a 40%

Table 4.2 A.C. Test Data

$\frac{N_1}{N_2} I_p$ (ma) (r.m.s.)	Balanced Burden Differential Current I_{db} (ma) (r.m.s.)	Unbalanced Burden Differential Current I_{du} (ma) (r.m.s.)
40	approximately = 5	approximately = 5
80	"	"
120	"	"
160	"	approximately = 10
200	"	"
250	"	15
300	"	26
350	"	41
400	"	55
500	"	84
630	"	100

unbalance. In addition to this the a.c. component will further saturate the C.T.'s, requiring the minimum pickup current to be increased still further. A relay set with this high a pickup current would probably not be sensitive enough to detect internal faults near the neutral.

The removal of this d.c. component of fault current could simplify the protective system design since this current produces most of the differential current in the protective system.

It can also be seen from the tests that the resulting differential current with balanced C.T. burdens was quite small in both a.c. and d.c. tests. This seems to indicate a relay with a pickup current in excess of 10 ma could be used with adequate sensitivity to primary unbalance without likelihood of false operation due to saturation of the C.T.'s.

These tests are in no way an attempt to obtain data which would be used in an actual power system for any relay settings. The Sanborn Recorder is not fast enough to give a true representation of the leading edge of the waveform of the d.c. transient differential current (see Appendix A). However, precise current measurements are not necessary to validate the conclusions. The tests show the d.c. transient does produce an appreciable amount of differential current, much more so than an a.c. current of the same magnitude. This phenomena is present in large power systems although the actual magnitudes of current are much larger.

CHAPTER V

PROTECTIVE SYSTEM DESIGN

The design of the following generator differential protection scheme is based on a Westinghouse, number 69P525 steam turbine generator belonging to Tucson Gas and Electric Company. The various generator constants are given in Table 5.1.

The maximum possible a.c. current for a through fault on this generator is given by

$$I_{a.c.} = \frac{1}{\sqrt{3}} \frac{E_{rated}}{x_d''} \qquad (5.1)$$

or

$$I_{a.c.} \cong 86{,}000 \text{ amperes/phase}$$

One approach to the design of the protective system is to design current transformers which will not saturate. The maximum flux density in electrical grade sheet steel is approximately 80,000 lines per square inch. However, to allow a margin of safety, the maximum flux density in the core will be limited to 40,000 lines per square inch. A typical C.T. in common use has a cross section of four square inches. The number of secondary turns required to keep the C.T. out of saturation is given by equation (3.7), which, rearranged, yields the following:

Table 5.1. Turbine Generator Constants

Rating: 203,882 K.V.A.; 18 K.V.; 6,540 amperes 3 phase; 60 Hertz; 3600 R.P.M.

Rated Power Factor: 85%

x_d = 167%	T'_{do} = 8.32 secs.
x'_d = 18%	T''_{do} = 0.056 secs.
x''_d = 12.1%	T''_d = 0.035 secs.
x'_{du} = 22.0%	T'_{qo} = 1.5 secs.
x_2 = 12.0%	T''_{qo} = 0.284 secs.
x_0 = 4.01%	T''_q = 0.035 secs.
x_q = 175.6%	T_a = 0.32 secs.
x'_q = 96.8%	r_a = 0.099%
x''_q = 11.9%	R_2 = 0.66 %
x_p = 20%	
R_g = 0.2784 ohms	

Date of tests: January 6, 1965

$$N = \left[\frac{\sqrt{2}\ I_{ac}}{\varphi_{max}} (RT_a) \right]^{1/2}. \qquad (5.2)$$

At this point we must assume a burden for the C.T. This burden must include the secondary impedance of the C.T. and the impedance of the wiring up to the relay, but not including the relay. For this design, a 4-ohm burden, including that of the secondary winding, is assumed. The maximum flux, determined by the core cross-sectional area, is

$$\varphi_{max} = BA$$

or

$$\varphi_{max} = 1.6 \times 10^{-3} \text{ webers.}$$

Therefore, N, the number of secondary turns is

$$N = 9{,}870 \text{ turns.}$$

The major objection to a C.T. with this high a turns ratio is that a relatively high voltage would be developed across the relay should an internal fault occur. Because of the low secondary currents in the C.T.'s, a high impedance relay must be used for the desired sensitivity. On a 5-ampere basis, a typical differential relay for generator protection has a pickup current of 0.20 amperes and an impedance of 58 ohms. A C.T. with 9,870 turns represents a secondary current base of 0.71 amperes. The relay used with these C.T.'s must have a correspondingly lower pickup current for the same sensitivity as that of the relay used with the 5 ampere C.T.'s. This is a 7.05:1 reduction in operating current and a 49.7:1 increase in relay impedance. The new relay would therefore have an impedance of approximately 3,000 ohms. For an internal fault near the generator terminals, the minimum primary current unbalance would be 86,000 amperes. This would produce a minimum of 26,000 volts across the relay terminals. In addition to the obvious insulation problems associated with this relay, voltages of this magnitude would present an unacceptable hazard.

Another approach is to design the cross sectional core area of the C.T. which will not saturate for a given number of secondary turns. The standard relaying transformer in use today is a 5 ampere secondary C.T. For the generator in question, the current ratio for the C.T. is 7500/5. The turns ratio is 1500/1. For this ratio, the core area required to prevent saturation, as given by equation (3.7), would be

$$A = 173 \text{ in}^2.$$

The space required for six of these transformers would seem to be excessive.

As demonstrated earlier, matched C.T.'s with matched burdens can be allowed to saturate with no adverse effects. One way to achieve a balanced condition is to physically locate the relay exactly half-way between the two C.T.'s. This, however, is hardly ever practical, for it is most desirable that the relay be located in the switchgear with the other protective relays.

A practical method of achieving balanced C.T. operation is that of using balancing resistors in the secondary circuit.

Figure 4.3 shows that the differential current due to d.c. primary current occurred only during the initial few milliseconds of the test. Therefore, a time delay relay could be used with the timer set to permit relay operation only after the initial transient period were over. This, however, would lead to a serious problem in the case of an internal fault. On this type of fault, the time delay would still be present, and this delay in the removal of the generator from the power system could cause extreme generator damage.

CHAPTER VI

SUMMARY

There are two alternatives that must be considered if a differential protective system using a non-restraint relay is to be used. The first alternative is that the two C.T.'s must be carefully matched, and their burdens matched either by physically locating the relay midway between the two C.T.'s or by adding secondary balancing resistors. The second alternative is that of designing non-saturating C.T.'s for the protective system. Increasing the secondary turns on a standard 5-ampere C.T. core in present use will produce hazardous voltages in the secondary circuit should an internal fault occur. Increasing the core area of a 5-ampere C.T. produces C.T.'s of excessive size. However, for the larger machines being installed at the present time and in the future, the use of non-saturating C.T.'s which are a compromise between increased core area and increased secondary turns merits further investigation.

APPENDIX A

DETERMINATION OF FREQUENCY RESPONSE OF SANBORN 150 STRIP RECORDER

An important consideration in the evaluation of the accuracy of the differential current waveshape produced by the Sanborn Strip Recorder is the speed of the recorder. A test was made to determine the frequency response of the Sanborn Recorder. The equipment used was as follows:

Oscillator--Hewlett Packard 652A (#000990)

Voltmeter--Hewlett Packard 3400A (#000978)

Oscilloscope--Tektronix Type RM31A (#000899)

Recorder--Sanborn 150 (#003736)

The test circuit is shown in Figure A.1. The output voltage of the socillator was adjusted such that the pen on the recorder achieved full scale deflection for the recorder attenuation factor set at two. The frequency of the oscillator was then raised and the recorder pen deflection was recorded holding the input voltage constant. The frequency of the oscillator was verified by the oscilloscope. A plot of the results of the test is shown in Figure A.2. From this figure it is seen that the cutoff frequency of the recorder is approximately 61 Hertz. The question now arises as to the adequacy of this response. From Figure 4.3, the period of the fundamental frequency of

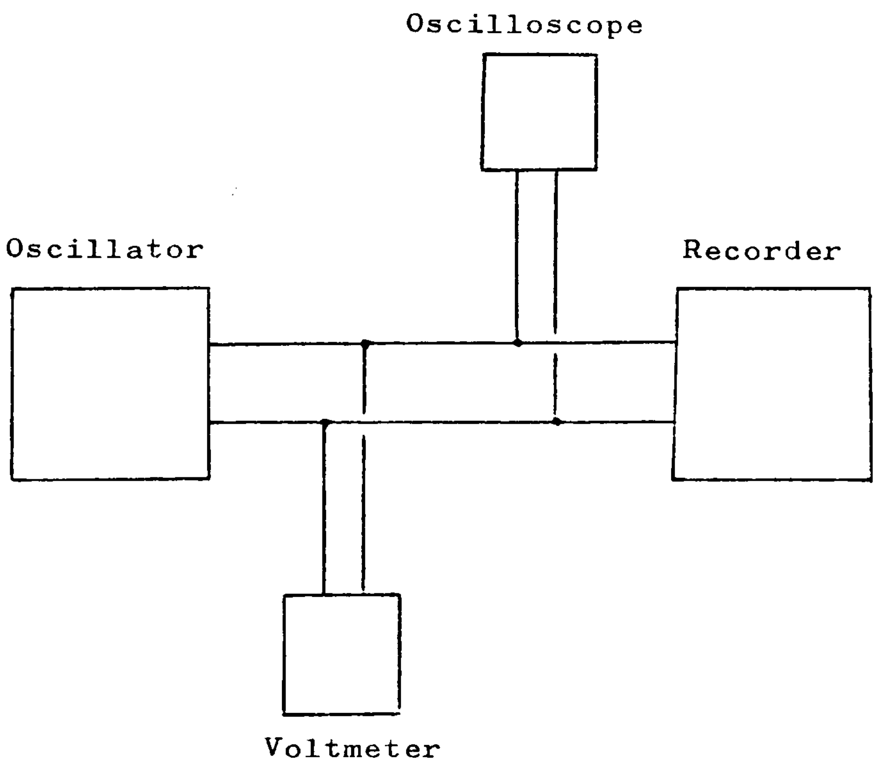

Figure A.1. Sanborn Recorder Frequency Response Test Circuit

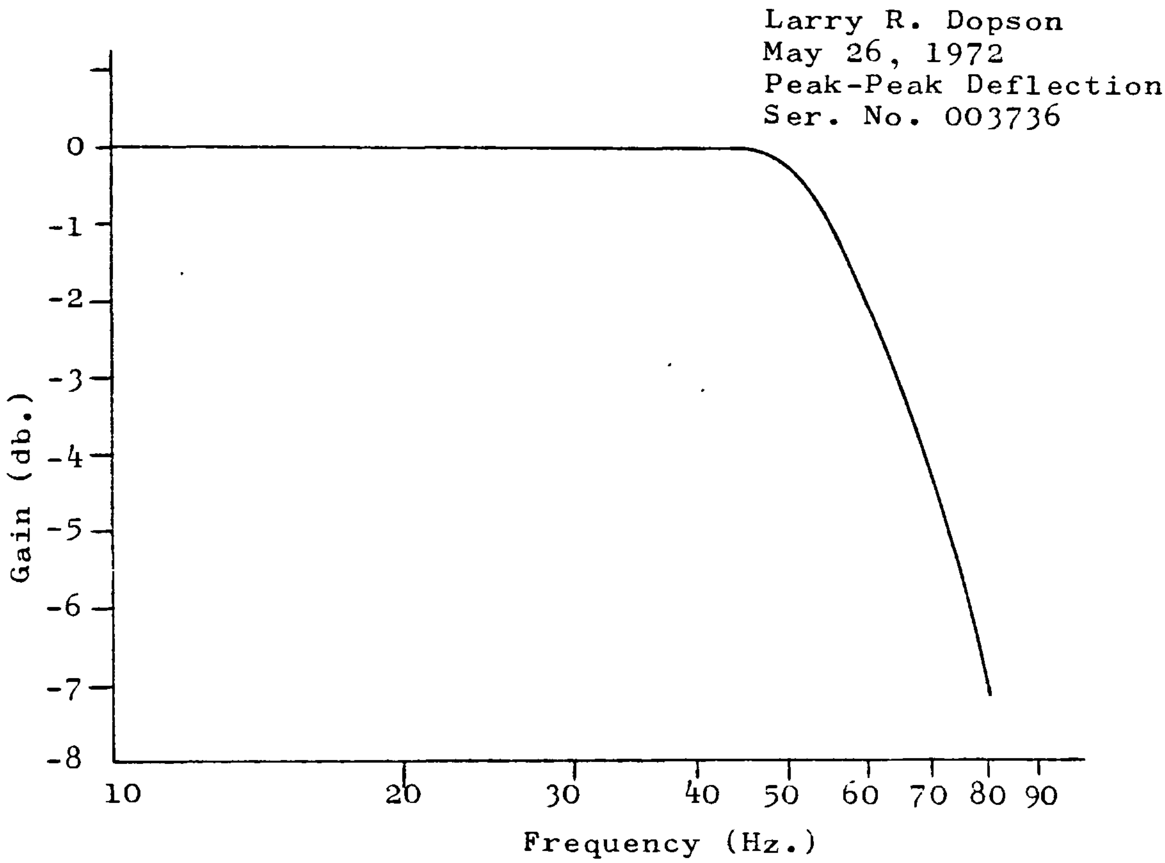

Figure A.2. Frequency Response of Sanborn Recorder the differential current is approximately 0.26 seconds. This corresponds to a fundamental frequency of 3.85 Hertz. The cutoff frequency of the recorder is higher than the fundamental frequency of the differential current by a factor of 15.8. By most standards used in industry, this cutoff frequency would be considered high enough to give an adequate representation of the differential current. However, the rise time of the leading edge of the differential current pulse is fast enough such that the recorded rise time could be limited by the recorder.

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