The Electrical Design

of a

32,500 Kv-a. 150 R.P.M. Alternating Current Generator

by

Roy Anderson McCarty.

A Thesis Submitted for the Degree of
Bachelor of Science
Electrical Engineering Course

The University of Tennessee

1 9 3 0

I N D E X

Statement of Problem

It was required to determine the electrical and magnetic design dimensions of an alternating current generator having the following characteristics --

Output	32,500	Kva.
Power Factor	80	%
Speed	150	R.P.M.
Frequency in cycles per sec.	25	
Poles	20	
Phases - "Star" connected	3	
Voltage	12,000	
Amperes per terminal	1,565	

The guarantees were --

Efficiency at 26,700 Kv-a. 90% P.F. -	97.0
Max. temperature for which the insulation was adequate	150°C.

Selection of Stator Core Dimensions

While experience indicates that many factors enter into the selection of the stator magnetic core dimensions, it is generally of assistance to start with a knowledge of average output constants of similar machines. Similar constants may be calculated in other ways. The one used here is determined by the following equation:

$$K = \frac{D^2 \times L \times R.P.M.}{K.V.A. \times 10^4}$$

Where K = the output constant.
" D = outside diameter, in inches, of the stator magnetic core.
" L = the axial width, in inches, of the stator core, including the air ducts in core.
" RPM = Revolutions per minute
" KVA = Kilo-volt-ampere rating of machine.

Average values for this constant are given on the set of curves, Figure I.

For a 20-pole machine of about this rating, the curves, Figure I, indicate the output constant should be approximately 1.45. The constant calculated from the final dimensions selected is 1.56.

Knowing the approximate value of output constant needed, the next problem is to select tentative values for D and L. In general, for a given value of K, the cost of building such a machine will be decreased with decreasing values of D and increasing values of L. However, unless special means are used to ventilate the machine, difficulty is apt to be encountered if the peripheral speed is less than from 8000 to 10,000 ft. per minute. There is also generally very limited space for field copper on the rotor, if too small a diameter is chosen. Ordinarily, a number of values of diameter and length are selected, and the design partially completed, in order to determine the best proportions. The final selections in this case were 228" diameter of core, and 65" length of core.

Size and Number of Armature Core Vent Ducts

Experience has shown that little, if any, gain in temperature is obtained by making the vent ducts through the stator core greater than one-half inch in width. The gain in temperature in increasing the duct width from 3/8" to 1/2" is small. Therefore, 3/8" ducts were selected. Again, it has been found that, in order to get rid of the heat in the stator core and

Curve No.______ Signature______ Date______

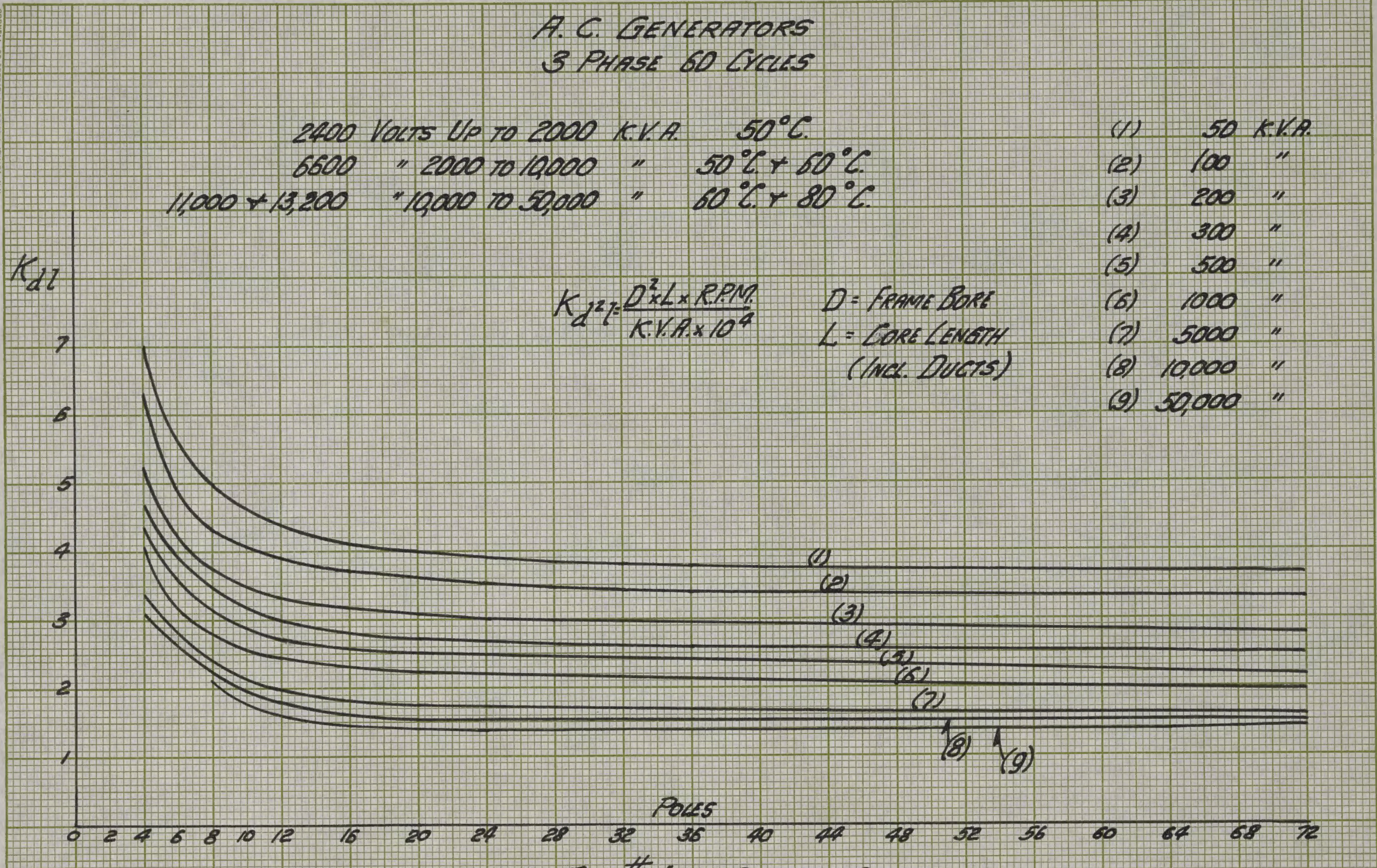


FIG. # 1. OUTPUT CONSTANTS

the embedded portion of the armature winding to best advantage, the thickness of the iron packages between ducts should be from 1.5" to 1.75". If they exceed 1.75", difficulty from excessive temperature may be encountered. For this reason, 30 vent ducts were selected, giving a thickness of iron package between ducts of 1.73".

Number and Size of Armature Slots

In general, a relatively large number of slots will improve the performance by giving greater radiating surface and consequently lower armature winding temperatures, less pulsation of flux in the air gap, and therefore less loss in the pole faces and the voltage wave, which will more nearly approximate a sinusoidal form. Against these desirable features must be balanced: less rigidity in coils, when of small cross-section; and increased cost of a large number of coils.

Since 9 slots per pole give a slot frequency component to the voltage wave which is apt to resonate with telephone diaphragms and cause trouble in adjacent communication lines, a large machine having a comparatively small number of poles, as in this case, should have not less than 12 slots per pole. The ultimate selection was 15.

The size of armature slot of course depends on the amount of copper required and the space needed for insulation. The current density in amperes per square inch, on such a machine, may vary over quite a large range, depending on the efficiency and temperature limits to be met. Ordinarily, the current density would lie somewhere between 1500 and 2500 amperes per square inch of copper in the coils. The value selected in this case was 2300 amperes per square inch.

The insulation used was largely mica and was proportioned to meet the Standards of the A.I.E.E. of twice rated voltage, plus 1000. The number of armature conductors (discussed later) of the required cross-section with the necessary space for insulation and the coil retaining wedge, fixed the size of the slot at .75" x 4.75".

Design of Armature Coil

Each armature coil contains three conductors with each conductor made up of 4 wires in parallel. Two sizes of wire were used, i.e. .129" x .274" and .204" x .258", arranged as shown in Figure 2.

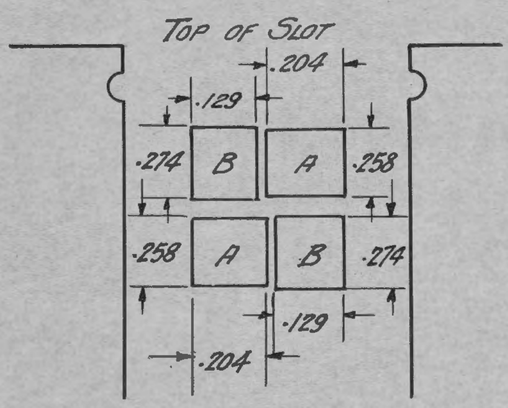


FIG. 2
CROSS SECTION OF
ARMATURE CONDUCTOR

It will be noted that wires "a" are somewhat greater in width than wires "b", and that they alternate in position in the conductor. Wires "b" have no individual insulating covering, while wires "a" are insulated with mica tape to a thickness of about .008" per side.

By this arrangement, separation is secured between wires "b" so that individual insulation on them is unnecessary. Since these four wires are electrically in parallel, insulating them from each other throughout the coil is only for the purpose of reducing the eddy current loss. The four wires are bound together and insulated as a group with mica tape to a thickness of about .020" per side. The three conductors were then assembled and given the overall insulation in the form of mica sheets applied under heat by machines to form a very solid homogeneous coil. The total space in slot required for insulation was roughly .372" in width and 1.126" in depth.

Air Gap

The radial distance between the rotor and stator, i.e. the air gap, is often affected by mechanical clearance considerations in an induction motor or generator having a large number of poles. In a machine such as this, electrical conditions will always dictate a large gap. In order to avoid excessive changes in voltage with changes in load and lack of stability under line fault conditions, it is necessary to use a gap sufficiently large so that the ampere turns of the field are equal to from 80 to 125 percent of the armature ampere turns. The final selection of air gap for this machine was .50" single gap.

The slots of the armature have the effect of increasing the gap from the standpoint of magnetic reluctance. Hence, the effective gap is the actual gap times a constant.

$$K = \frac{t_1}{Z_1 + X\delta}$$

Where K = the constant

" t_1 = the distance between the centers of two adjacent armature slots measured along the air gap surface.

" Z_1 = width of an armature tooth at the air gap.

" δ = the radial dimension of the actual single air gap.

" X = curve value corresponding to values of $\frac{t_1 - Z_1}{\delta}$ as given on Figure 3.

Determination of Magnetic Flux Form

In order to determine the flux form, draw two adjacent poles at any convenient scale, to obtain the proper relative position of the different points. (Figure 4)

Divide the pole pitch into 24 equal parts, and, using any convenient length as unity, draw one-half of one pole to this scale in such a way that the armature surface should be represented by a straight line. It is evident that, in this representation, the pole will have a distorted shape, but it can be readily constructed by using the relative points and proportions (referred to the armature surface), as obtained from the first pole sketch.

The distance between the armature surface and pole equals the effective (discussed later) single air gap drawn to scale.

Through the middle points of the 24 divisions draw curved lines between the armature and pole, representing lines of force- (center lines of magnetic tubes of force, limited by the division).

(It must be borne in mind that the distribution of flux is

Curve No.______ Signature______ Date______

FACTOR "K" FOR AIR GAP

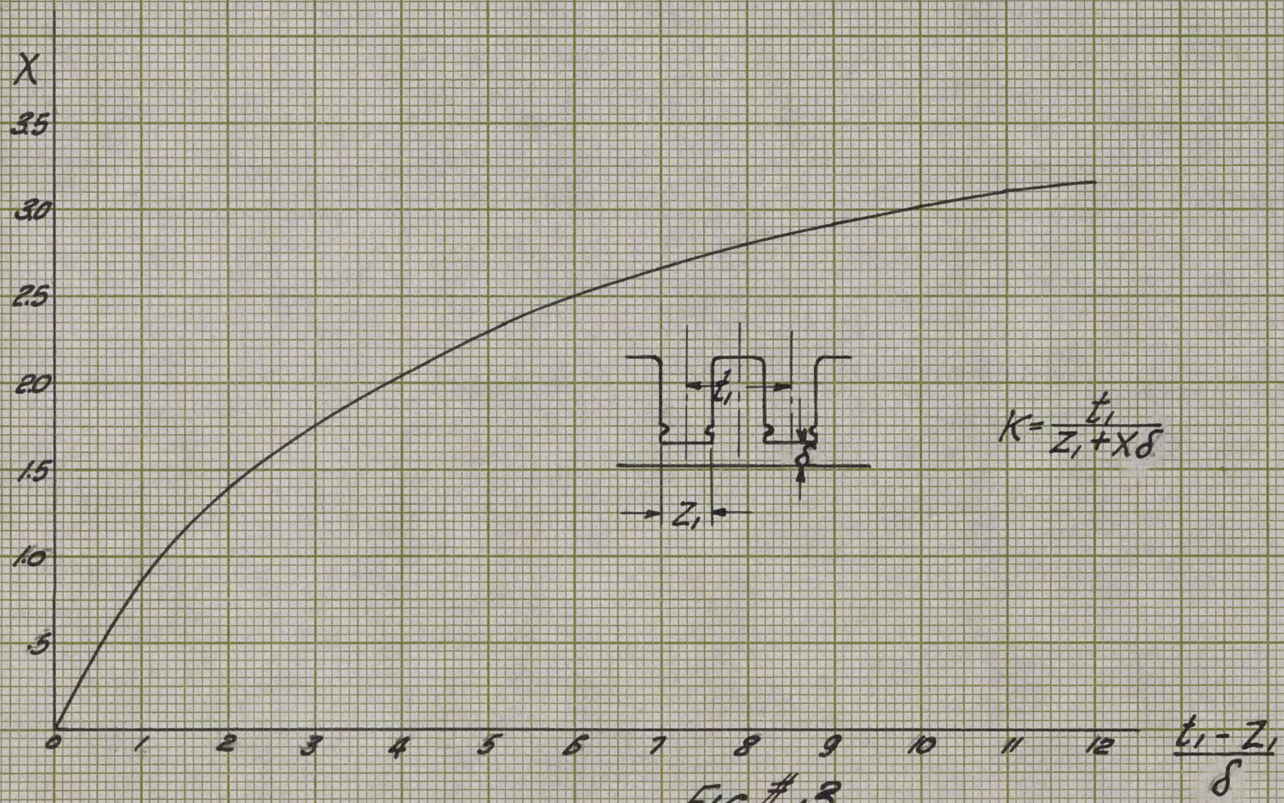


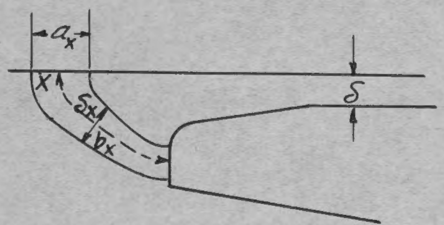
FIG. # 3

WESTINGHOUSE ELECTRIC AND MANUFACTURING CO. Curve No.______

always such that the magnetic field energy is a maximum; hence, that distribution comes nearest to the reality, which has the maximum magnetic conductivity).

As the permeability of the iron, as compared with that of the air, is very great, the lines of force will enter the iron under approximately 90 degrees. Hence, under the pole face, in the air gap, they should be drawn as straight lines in radial direction; on side of the pole they must have a curved form, making an angle of almost 90 degrees with both, the armature and pole surface, and being approximately parallel with each other.

Assuming the magnetic potential difference between the pole tip and the armature everywhere constant, the field density B + any point is proportional to the conductivity of the tube of force surrounding this point. If δ_x the length of axis and b_x the mean width of the tube, the conductivity is proportional to $\frac{b_x}{\delta_x}$



and the field density in X on the armature surface is proportional to $\frac{b_x}{a_x \delta_x}$. Denoting the maximum field density under the pole with 100 and using the length (corresponding to the equivalent air gap) as unity in measuring δ_x, the density at any point X is found, as $B_x = 100 \frac{1}{\delta_x} \frac{b_x}{a_x}$

Since in the most cases $\frac{b_x}{a_x} = 1$, it is sufficient to measure the length of the central line of force, in the units of δ , and make the field density b_x inversely proportional to this length.

Curve No.______ Signature______ Date______

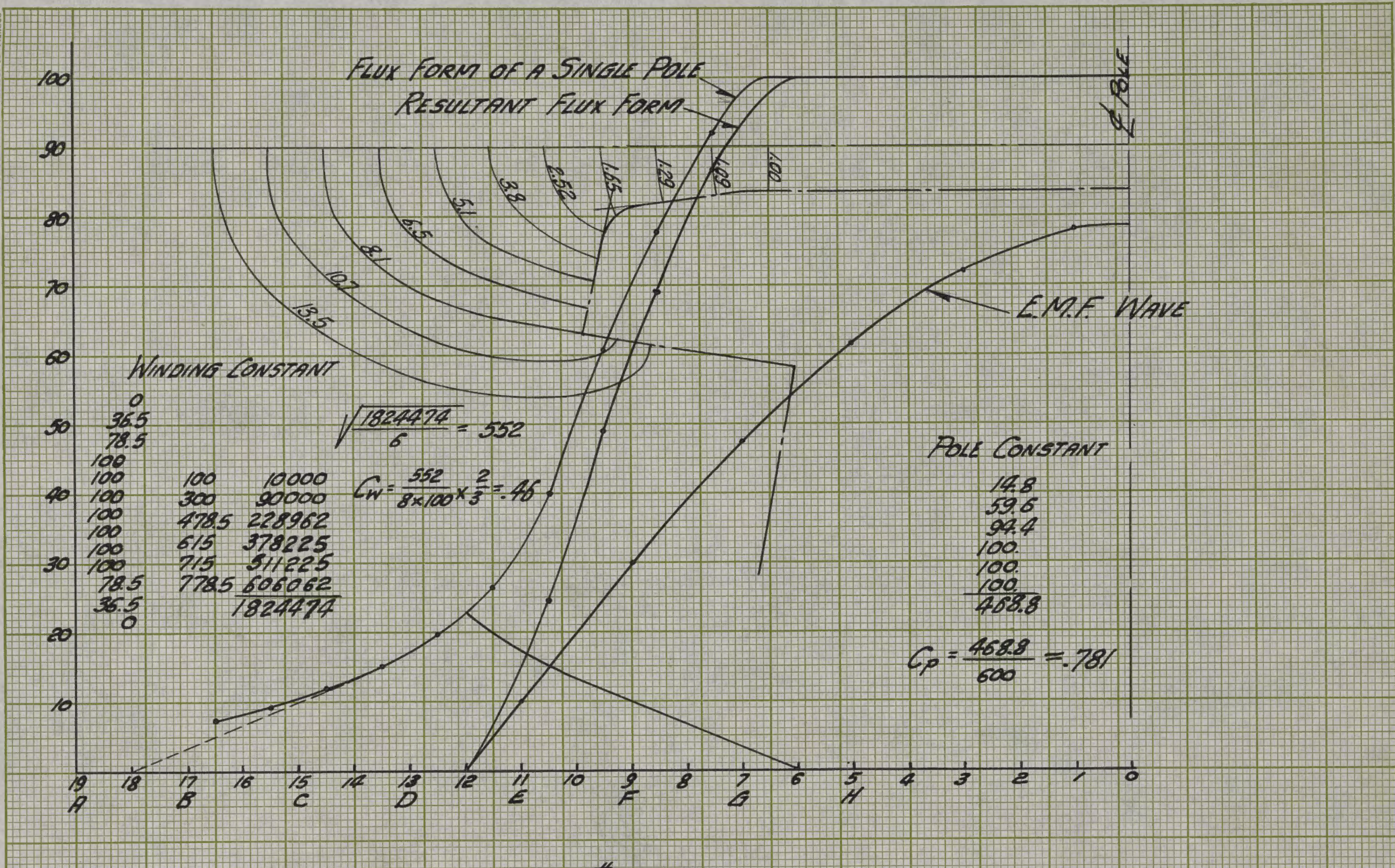


FIG. # 4. FIELD FLUX FORM

WESTINGHOUSE ELECTRIC AND MANUFACTURING CO. Curve No.______

P-10

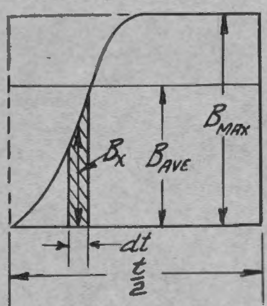
$$B_x = \frac{100}{\delta_x}$$

With this construction, we can plot the flux form of one pole's magnetic flux in percent of maximum density.

As the fluxes of adjacent poles of opposite polarity are superimposed on each other, the actual flux form is obtained by subtracting the field densities of the two poles where they overlap. From the twelfth division (center between two poles), draw the field curve backwards, and in order to obtain the actual field form of one pole, use as ordinates the differences between the upper and lower curve.

Since δ_x for one pole is nowhere equal ∞, the field intensity will never become zero; hence, with this construction, the actual flux form would never reach the maximum value 100 under the pole. As, however, in reality, the flux of one pole cannot penetrate into the air gap of the other, we modify our field in such a way that, shortly behind the bevelled part of the pole tip, the field reaches its relative maximum value 100.

Pole Constant



The pole constant C_p is the ratio of the area enclosed by the actual field form to the area of a rectangle having the same maximum value and base line. $C_p = \frac{\int_0^{\frac{t}{2}} B_x \, dt}{\left(\frac{t}{2}\right) B_{MAX.}}$

Since $\frac{2}{t}\int_0^{\frac{t}{2}} B_x \, dt = B_{AVE.}$, the pole constant can be also defined as the ratio of average flux density to the maximum.

$$C_p = \frac{B_{AVE.}}{B_{MAX.}}$$

In order to find C_p from the flux form, divide the plotted half into six equal parts, and add the middle (mean) ordinates of these strips (ordinates corresponding to the odd numbers of the twelve divisions.) Then -

$$b_1 + b_2 + b_3 + b_4 + b_5 + b_6 = 6\,B_{AVE.}$$

Hence - $B_{AVE} = \frac{\Sigma b}{6}$

And - $C_p = \frac{B_{AVE}}{100} = \frac{\Sigma b}{600}$

For a sine wave shaped flux, form C_p = .637

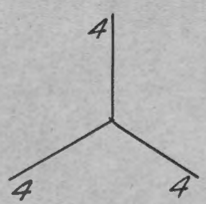
The E.M.F. Wave Form

The wave form of E.M.F. induced into the armature winding through the relative motion of the magnetic field and the armature conductors, can be constructed from the flux form. (Figure 4).

The E.M.F. wave induced in each single conductor is exactly of the same shape as the flux form itself. If more than one conductor - connected in series - are moving in the magnetic field, the resultant E.M.F. will be the sum of the instantaneous values; therefore, the resultant form of E.M.F. wave is dependent upon the number and distribution of conductors forming one group, or one winding element.

Assuming, for convenience, one conductor per slot, it can readily be shown that, if the number of slots per pole is 5 or more, the wave form practically becomes independent of the number of conductors; i.e. it may be constructed by using any convenient number. With fewer slots, the actual number must be used; if, however, there are 5 or more slots per pole, use any convenient number, say 12, which gives 4 slots per pole per phase, in 3 phase; and 6 slots per pole per phase, in 2 phase, windings. Hence, in the open type windings, the construction will be made with 12 conductors in the case of 3 phase, and with 6 conductors in the case of 2 phase machines. In single phase windings, the actual distribution of conductors should always be taken into consideration.

The fundamental idea of the construction is that we move the groups of conductors in the magnetic field, and determine the number of lines of force cut by each individual conductor. Adding these figures with the proper sign, we obtain a value which is proportional to the E.M.F. induced in the whole group of conductors. This value plotted at the proper abscissa at any arbitrary scale, gives one point of the E.M.F. wave.



Eight conductors, 1/12 of one pole pitch apart from each other, are connected in series, forming one group, and moving from left to the right. In the momentary position shown on the figure, the resultant E.M.F. will be zero, since the two halves (4-4) of the group move in symmetrical, but opposing, magnetic fields.

As the group moves through one division (1/12 pole pitch) further, each conductor cuts a number of magnetic lines, which is proportional to the area of the corresponding 1/12 field strip. The width of each of these strips being equal, the areas can be expressed by the middle (mean) ordinates.

As taken from the flux form, these middle ordinates are as follows:- (Corresponding to division numbers 12- 10- 8- 6- 4-

0- 36.5- 78.5- 100- 100- 100- 100- 100- 100- 100- 78.5- 36.5- 0

When the group of conductors moves thru one division from left to right, the number of magnetic lines cut by each individual conductor, expressed in terms of middle-ordinates, is as follows:

A	B	C	D	E	F	G	H
-100	-78.5	-36.5	0	+36.5	+78.5	+100	+100

the numbers being taken + in the field of one polarity, and - in the other.

As immediately seen, the resultant is + 100; i.e. the value of E.M.F. induced in the whole group is proportional to a relative value +100.

This was induced when the centerline of the group moved from div. #12, to div. #10; hence must be plotted on the ordinate of the middle div. #11.

By following up the movement of the group, we obtain the following tabulated values:

(This table must be understood as follows: conductor A moving from position 0 to 1 cuts a number of lines proportional to 100: from 1 - 2 ... 78.5 etc.)

$A_0 \quad B_0 \quad C_0 \quad D_0 \quad E_0 \quad F_0 \quad G_0 \quad H_0$

-100 -78.5 -36.5 0 36.5 78.5 100 100

$A_1 \quad B_1 \quad C_1 \quad D_1 \quad E_1 \quad F_1 \quad G_1 \quad H_1$

-78.5 -36.5 0 36.5 78.5 100 100 100

$A_2 \quad B_2 \quad C_2 \quad D_2 \quad E_2 \quad F_2 \quad G_2 \quad H_2$

-36.5 0 36.5 78.5 100 100 100 100

$A_3 \quad B_3 \quad C_3 \quad D_3 \quad E_3 \quad F_3 \quad G_3 \quad H_3$

0 36.5 78.5 100 100 100 100 100

$A_4 \quad B_4 \quad C_4 \quad D_4 \quad E_4 \quad F_4 \quad G_4 \quad H_4$

36.5 78.5 100 100 100 100 100 100

$A_5 \quad B_5 \quad C_5 \quad D_5 \quad E_5 \quad F_5 \quad G_5 \quad H_5$

78.5 100 100 100 100 100 100 100

$A_6 \quad B_6 \quad C_6 \quad D_6 \quad E_6 \quad F_6 \quad G_6 \quad H_6$

By adding these individual values, we obtain the instantaneous values of the resultant E.M.F.

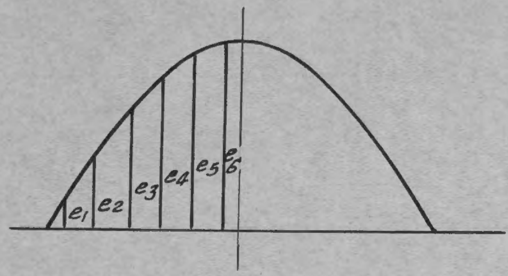
0 - 100 - 300 - 478.5 - 615 - 715 - 778.5 - (715 ...)

corresponding to abscissae:

#12 #11 #9 #7 #8 #3 #1 , forming thus one quarter 0- $\frac{\pi}{2}$ of the resultant E.M.F. wave.

<u>Winding Constant and Total Magnetic Flux</u>

The winding constant is a value used in calculating the magnetic flux. It takes into accouht the method of connecting the armature winding, the shape of the flux form, etc. It is determined as follows:



If e_1 e_2 e_3 e_4 e_5 e_6 the middle (mean) ordinates of six strips of the E.M.F. wave, the effective value is found by figuring:

$$e_{eff} = \sqrt{\frac{e_1^2 + e_2^2 + e_3^2 + e_4^2 + e_5^2 + e_6^2}{6}}$$

Referring to attached figure $e_{eff} = 552$, and hence

$$\frac{e_{eff}}{e_{max}} = \frac{552}{788} = .702$$

(for sine wave = .707)

If n=the number of conductors used in determining the wave form, the value of E.M.F. contributed by each individual conductor to the resultant is, evidently, $\mathcal{H} = \frac{e_{eff}}{n}$

In the present method of construction n = 8 for 3 phase and n = 6 for 2 phase.

As it follows from the construction e, e_2 ... e_6 are relative values only, and are expressed in terms of magnetic field density; hence $\mathcal{H}$ must represent the uniform flux density, distributed all over the armature entering from the air gap into the armature, which induces <u>in each single conductor</u> of the armature winding the same effective (continuous) E.M.F. of the machine, under the actual flux distribution, represented by the field form, and having a relative maximum density of 100.

If the maximum flux density is B_{MAX} (CGS units), the "conductor constant" will be: $\frac{B_{MAX}}{100} \times \mathcal{H} = B_{MAX} \frac{\mathcal{H}}{100} = B_{MAX} \mathcal{H},$

representing, as above, an ideal uniform flux distribution.

If a conductor of 1 cm. length moves in this field with a relative velocity of v cm per second, the E.M.F. induced in it will be: $e = B_{MAX}\, \mathcal{H}_1\, l\, v\, 10^{8}$ VOLTS

Let W = the total number of conductors in series on the armature (sum of all phases), then:

In 1 phase windings	W	conductors are connected in series between any two terminals; hence:
In 2 phase windings 1/2	W	
In 3 phase windings 2/3	W	

the effective terminal voltage of the machine is calculated as:

$$E = B_{MAX}\, \mathcal{H}_1\, \alpha\, W\, l\, v\, 10^{-8} \text{ VOLTS}$$

where $C_W = \mathcal{H}_1, \alpha$ is the winding constant ($\alpha = 1/2, 2/3$)

Substituting $v = D\pi \frac{RPM}{60}$ cm and $D\pi l = S\, cm^2$ surface of the armature, we get: $E = B_{MAX}\, C_W\, W S \frac{RPM}{60} 10^{-8}$ volts.

Taking into consideration that 1 K (kappline) = 930 C.G.S. units per cm^2 and that $1\square'' = 6.45\ cm^2$ we get:

$$E = K S^{\square''}\, C_W\, W \frac{RPM}{60}\, 6000\ 10^{-8} \text{ VOLTS}$$

or, finally: $E = KS\ C_W\ W\ RPM\ 10^{-6}$ VOLTS

and $$KT = KS = \frac{E\, 10^6}{C_W\, W\, RPM}$$

In this formula:

K T = the maximum flux density (K kappline per $\square''$) under the pole in the air gap times total surface of the armature in sq. in.

E = effective terminal voltage.

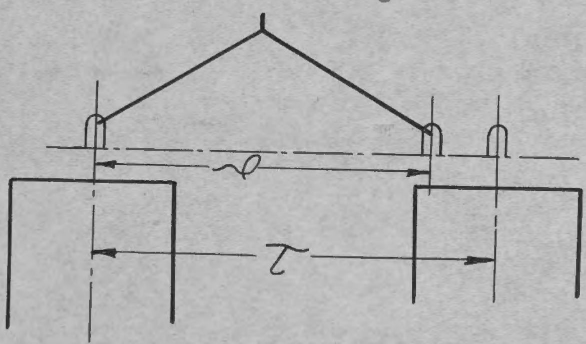
C_w = winding constant = $\frac{e_{eff}}{100} \quad \frac{\alpha}{n}$ (where α = 1 $\begin{cases} 1 \text{ ph.} \\ 1/2 \text{ ph.} \\ 2/3 \text{ ph.} \end{cases}$

W = total number of conductors in series.

For the generator under consideration, the pole constant and the winding constant C_p are worked out on Figure 5 and Figure 6, as .69 and .434 respectively.

Chord Factor

Where the span of the armature coil is less than the pole pitch, as shown in sketch, the total flux for a given terminal voltage has to be increased in the ratio of the sine of half the electrical degrees between the two sides of the coil.



The chord factor "X" is calculated as follows:

$$X = \sin\left[\frac{180\,(K-1)}{2n}\right]$$

Where N = number slots per pole

" K = No.of slots spanned by coil.

It being understood full pitch is from slot 1 to slot (N + 1) and the short pitch is from slot 1 to slot (K + 1).

Total Magnetic Flux

The total flux, then, is calculated by the following formula:

$$KT = \frac{E_x 10^6}{C_{W x} X_x W_x \text{ R.P.M.}}$$

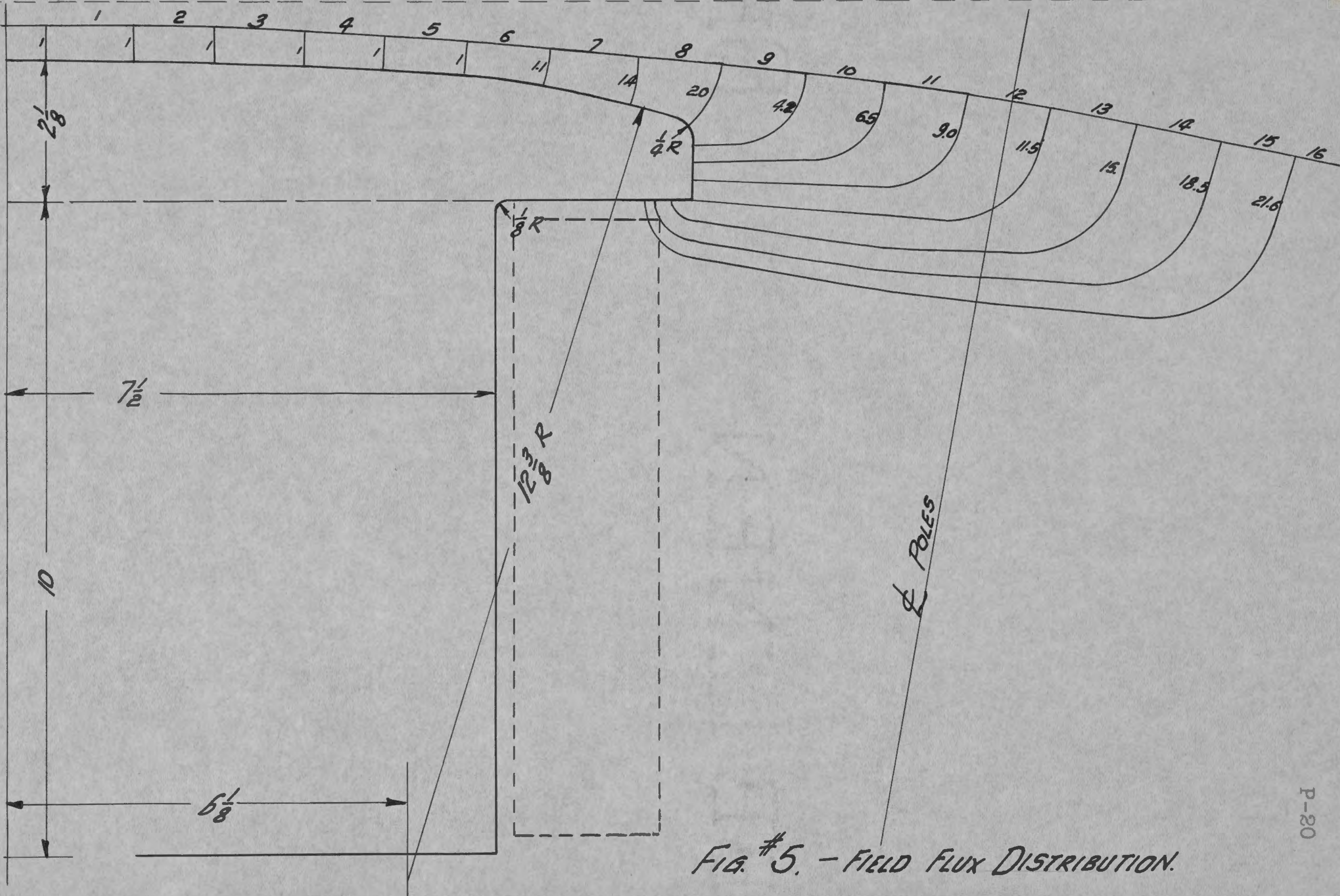


FIG. #5. - FIELD FLUX DISTRIBUTION.

A	B	C	D	E	F	G	H	
-96	-58	-13	0	13	58	96	100	= 100
-58	-13	0	13	58	96	100	100	= 296
-13	0	13	58	96	100	100	100	= 454
0	13	58	96	100	100	100	100	= 567
13	58	96	100	100	100	100	100	= 667
58	96	100	100	100	100	100	96	= 750

10000
87500
207000
321000
443000
562500
1631000

$$\sqrt{\frac{1631000}{6}} = 522$$

$$\frac{522 \times 2}{8 \times 3 \times 100} = .434 = C_W$$

6.5
30.
78.5
99.
100.
100.
414.0

$$\frac{414}{600} = .69 = C_p$$

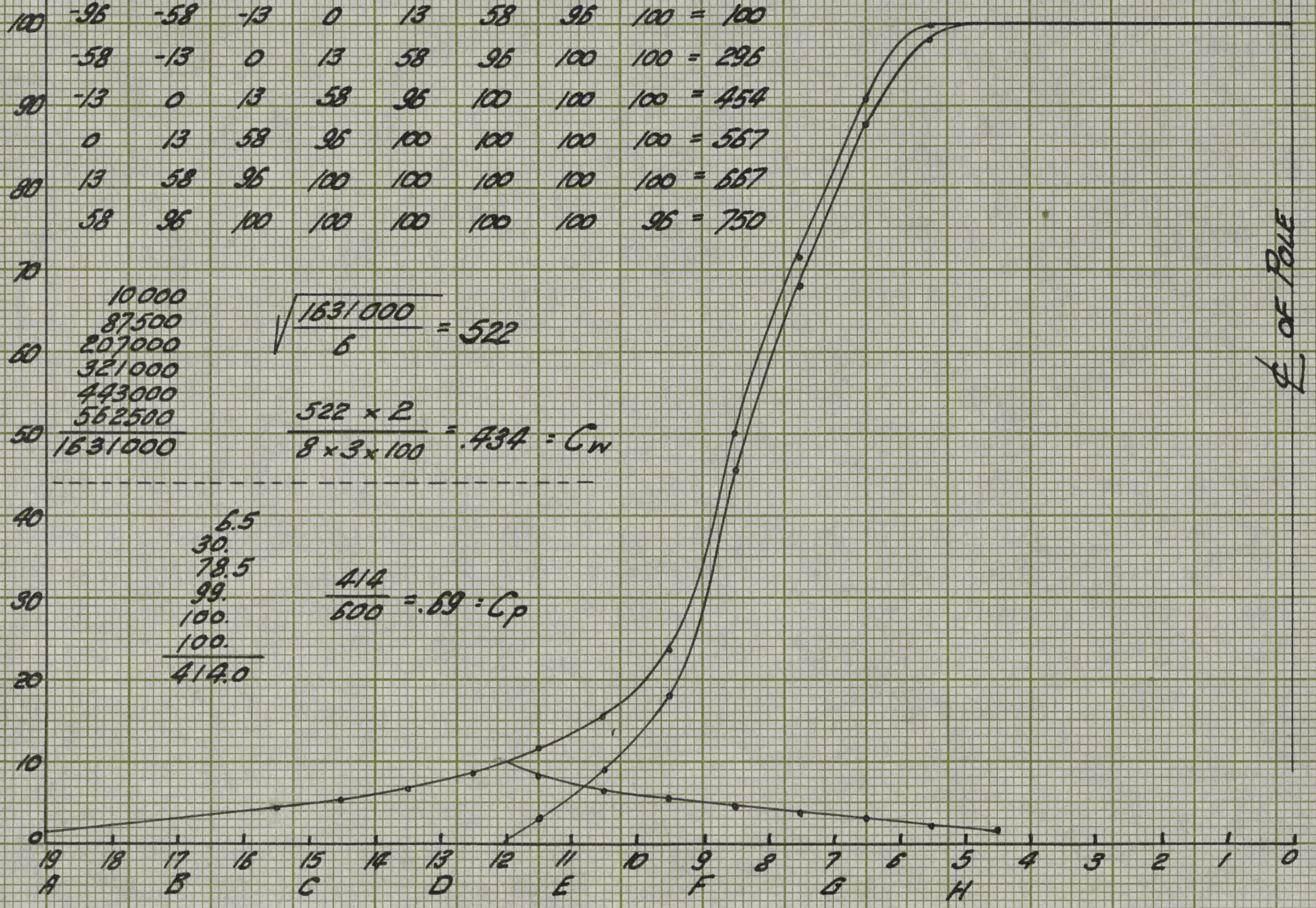


FIG. # 6. FIELD FLUX FORM

Where KT = total flux in air gap in kapplines(lines per sq. in. divided by 6000).
" E = terminal voltage.
" C_w = winding constant.
" X = chord factor.
" W = total conductors.
" R.P.M. = revolutions per minute.

The coil span selected for this machine was slot 1 to slot 14, and the chord factor, therefore, was .978.

It is apparent from the foregoing that changing the span of the coil is a convenient method of obtaining a fractional number of effective conductors in order to get the desired total flux. It is not considered economical to chord the winding to an extent that the chord factor becomes less than .866 (two-thirds full pitch).

The winding of this machine was connected in four parallel circuits which, with six conductors per slot, gave a total number of conductors W =450. Parallel circuits were used to avoid having excessively deep slots which might lead to heating difficulties; and it also reduces the eddy current loss by reducing the depth of each conductor. Also, it will be noted that the combination selected gives 1.5 effective conductors per slot, which would be otherwise impossible to obtain.

Using the above data, the total flux for this machine was calculated as 417500 kapplines.

Unit Flux Densities

The inside bore of the armature core was selected as 197". This was determined by the depth of slot and the necessary depth back of slots, to provide the required magnetic section. With the foregoing dimensions determined, and the total flux fixed, the unit flux densities are found as follows --

a) Air Gap

The air gap section S.G. = $\pi D_A L$

Where D_A = diameter of inside bore of armature core
" L = length of armature core.

For this unit, S.G. = 40250 sq. in.

Then the maximum flux density in the gap is -

$$\frac{KT}{SG} = \frac{417{,}500}{40{,}250} = 10.37 \text{ kapp lines per sq. in.}$$

The average flux density in gap is $\frac{K.T. \times C_p}{S.G.} = 7.17$ Kapp lines per square inch

The maximum density is used in calculating the ampere turns required to force the flux across the air gap.

b) Armature Teeth

The section of the armature teeth is found from -

S.T. = t x T x L_1 = 22050 sq. in.

Where S.T. = section in sq. in.
" t = width of tooth midway its height.
" L_1 = width of stator core less vent ducts.

(See Figure 7)

Then, the maximum flux density in teeth $\frac{KT}{ST}$. = 18.95.

The maximum density is used for calculating the saturation and iron losses.

c) Armature Core Back of Slots

The section back of the teeth S.B. $= \left(\frac{D_f - D_a}{2} - L_t\right) L_1 = 578$ Sq.In

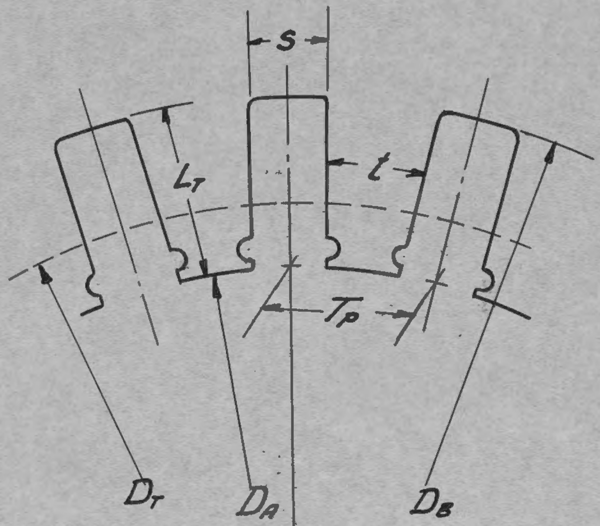


FIG. #7.
END VIEW OF STATOR SLOTS

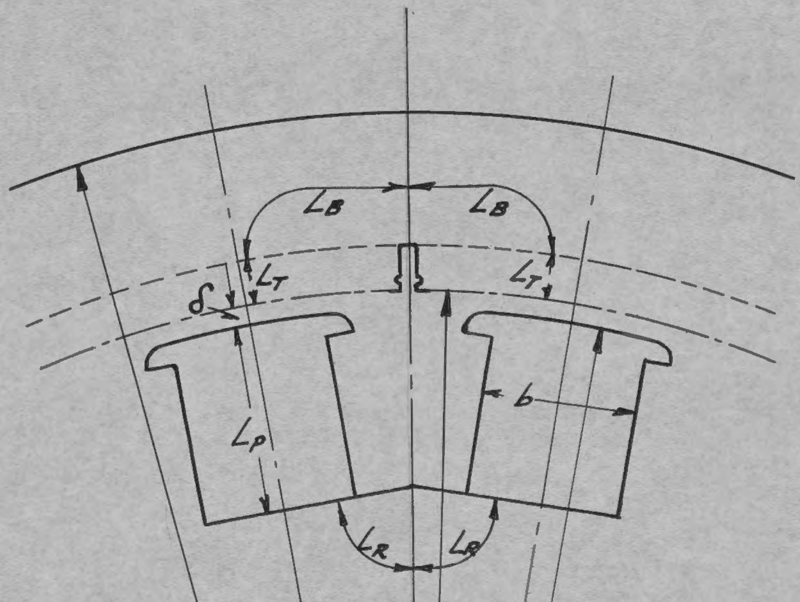


FIG. #8
END VIEW OF STATOR CORE &
ROTOR POLES.

Where SB = Cross-sectional area of stator core back of slots.
" D_f = Outside diameter of stator core
" D_a = Inside diameter of stator core
" L_t = Depth of slot
" L_1 = Length of core less the vent ducts

(See Figure 8)

The flux density back of slots is calculated using the average flux found as follows -

Average flux per pole K.P. = $\frac{K.T. \times C_p}{P}$ = 14400 Kapp lines

Where K.T. = total flux
" C_p = pole constant
"
" P = No. of poles

Then the density back of teeth K.B. = $\frac{K.P.}{Z \times S.B.}$ = 12.45 Kapp lines per sq. in.

WHERE K.P. = Average flux per pole
" S.B. = Section back of slots

Stator Core Loss

(a) Loss in Teeth: The iron loss in the teeth =

$$S.T. \times L_t \times W_t = 151 \text{ kw.}$$

Where S.T. = Section of teeth
" L_t = depth of slot
" W_t = Watts loss per cu. in.

For values of W_t at 25 cycles, see Figure 9.

(b) Loss back of Teeth: The iron loss back of teeth =

$$V \times W_t = 197 \text{ kw.}$$

Where V = the value of the stator iron back of the teeth expressed in cu.in.
" W_t =watts loss per cu.in.

(See Figure 9 for values of W_t at 25 cycles)

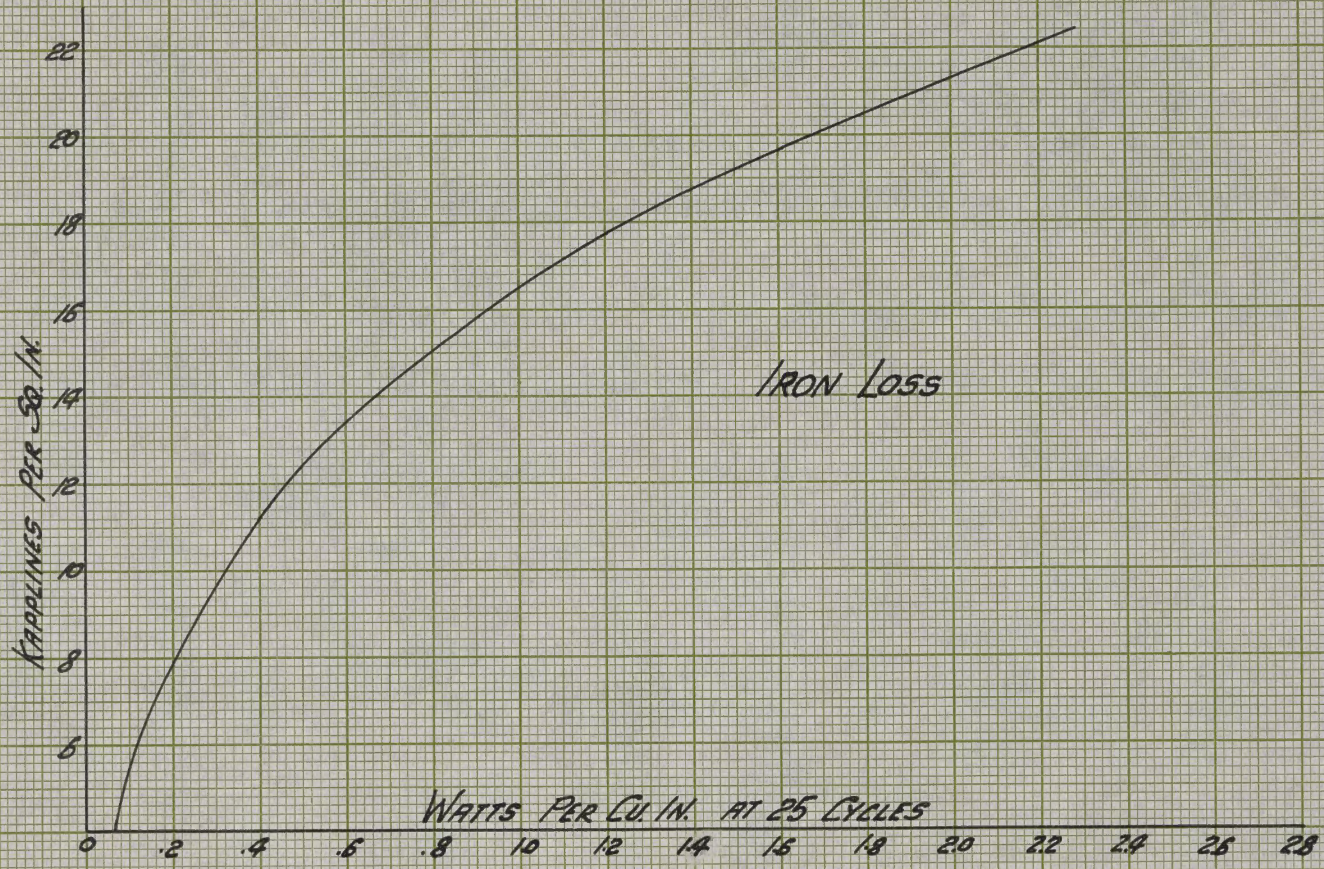

KAPPLINES PER SQ. IN.
22
20
18
16
14
12
10
8
6
IRON LOSS
WATTS PER CU. IN. AT 25 CYCLES
0
.2
.4
.6
.8
1.0
1.2
1.4
1.6
1.8
2.0
2.2
2.4
2.6
2.8

FIG. # 9.

Stator Armature Copper Loss

The resistance per phase of the armature winding may be calculated as follows:

$$R = \frac{\frac{MT}{Z} \times W}{1.21 \times 10^6 \times m \times g \times g} = .0234 \text{ ohms}$$

Where R = Ohms per leg of winding at 75°C.
" $\frac{MT}{Z}$ = Length in inches of the mean turn in an armature coil = 128.5".
" W = Total number of conductors in series in entire winding.
" m = Number of phases.
" g = Area in inches of each conductor.
" g = Number of parallels in winding.

Then the armature copper loss = $.0234 \times \overline{1565}^2 \times 3 = 172$ kw.

Eddy Current Loss

It is customary to calculate the eddy current loss in the armature coils in all large 60 cycle machines, and also in large 25 cycle units, if the conductors are deep and not well split into small parallel wires. Since the conductors in this generator are comparatively shallow and separated into four parallel wires, it is not considered necessary to make a detail calculation for the eddy current loss. A rough check indicated eddy current losses in the top and bottom coils of approximately 10 percent and 5 percent respectively of the I^2R loss. These values are very low.

Armature Coil Radiating Surface

The armature coil radiating surface is calculated in terms of square inches per watt of I^2R copper loss. The surface is taken at the bore copper using the top of the top coil in the slot, the bottom of the bottom coil in the slot, and the sides of both coils.

This value, based on the I^2R loss at 75°C, is calculated by the following method.

$$\square''/\text{watt} = \frac{P \times g \times 1.21 \times 10^6}{\left(\frac{t}{g}\right)^2 \times W}$$

Where P = Perimeter of the copper in the slot in inches.
" g = Area in sq.in. of each conductor.
" t = Number of parallels in armature winding.
" W = Total number of conductors in series.

For this generator, the radiating surface = 1.61 □"/watt. This value of surface may vary over quite a wide range, depending upon the conditioms of efficiency and temperature to be met. While there was no definite temperature guarantee in this case, the design was made with the expectation of not exceeding a rise of 80°C. By comparison with other units, this machine should operate well within that value.

(See Appendix "A" for test results).

Armature Winding Leakage Reactance

The self-induced fields set up by the alternating armature current cut the conductors and generate a counter E.M.F. which has to be compensated for by generating a higher voltage than that which appears at the terminals of the winding.

The method of calculating this quantity follows:

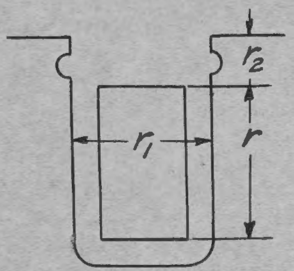
$$X = \frac{16 \times \nu \, W^2}{p \, q \, 10^8} \sum l_x \lambda_x$$

Where

x = armature reactance per phase expressed in ohms.
ν = number of cycles per second.
w = number of turns per phase in series.
p = number of pairs of poles.
q = number of slots per pole per phase.
$\Sigma l_x \lambda_x$ = conductivity of leakage.

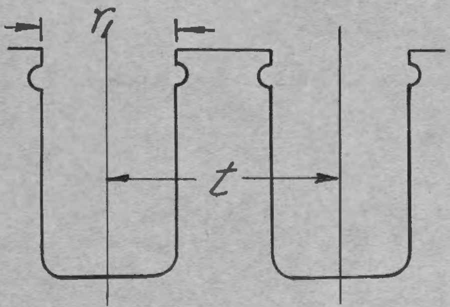
$$\Sigma l_x \lambda_x = 2.54 \left(l_i \lambda_n + l_i \lambda_k + l_s \lambda_s \right)$$

λ_n = conductivity of slot leakage.) for 1"
λ_k = conductivity of tooth tip leakage. } length of
λ_s = conductivity of end connection leakage.) winding.
l_i = width of armature core incl. air ducts { In
l_s = length at end connection = 1/2 MT $- l_i$ { Inches



$$\lambda_n = \frac{r}{3r_1} + \frac{r_2}{r_1}$$

TOOTH TIP LEAKAGE



τ = POLE PITCH

WHERE

$$q=1 \quad \lambda_k = .732 \left[\log \frac{\pi t}{r_1} \right]$$

$$q=2 \quad \lambda_k = .732 \left[\log \frac{\pi t}{r_1} + 2 \log \frac{\tau}{t} \right]$$

$$q=3 \quad \lambda_k = .732 \left[\log \frac{\pi t}{r_1} + .7 + 3 \log \frac{\tau}{2t} \right]$$

$$q=4 \quad \lambda_k = .732 \left[\log \frac{\pi t}{r_1} + 1.35 + 4 \log \frac{\tau}{3t} \right]$$

$$q=5 \quad \lambda_k = .732 \left[\log \frac{\pi t}{r_1} + 2.22 + 5 \log \frac{\tau}{4t} \right]$$

$$q=6 \quad \lambda_k = .732 \left[\log \frac{\pi t}{r_1} + 3.3 + 6 \log \frac{\tau}{5t} \right]$$

In case of two-coil per slot, chorded winding, where coils belonging to different phases lie in the same slot, λ_n and λ_k have to be multiplied by a chord factor which is < 1. (See Figure 16 for values). If in a three phase winding the pitch is 1-13, the chord 1-11, the percent of chording = 100 10/12 = 83.3 and chord factor = .875.

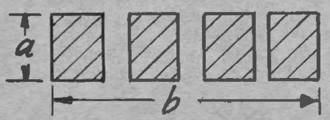
End Connection Leakage

$$\lambda_s = .366\, q_s \left[\log \frac{l_s}{d_s} - .2\right]$$

Where

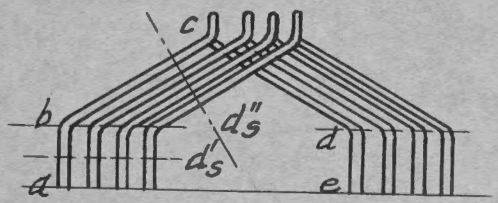
q_s = number of end connections laying side by side in one group p. phase.

d_s = diameter of a circle having the same circumference the q_s group of end connectors in vents included.



$$\lambda_s = \lambda_s' + \lambda_s''$$

$$d_s = \frac{2(a+b)}{\pi}$$



$$\lambda_s' = .366\, q_s \left[\log \frac{l_s'}{d_s'} - .2\right] \quad l_s' = ab + de$$

$$\lambda_s'' = .366\, q_s \left[\log \frac{l_s''}{d_s''} - .2\right] \quad l_s'' = bcd$$

The results calculated for this machine were as follows:

λ_n = 2.2
λ_K = 3.97
λ_s = 1.69
X = .582

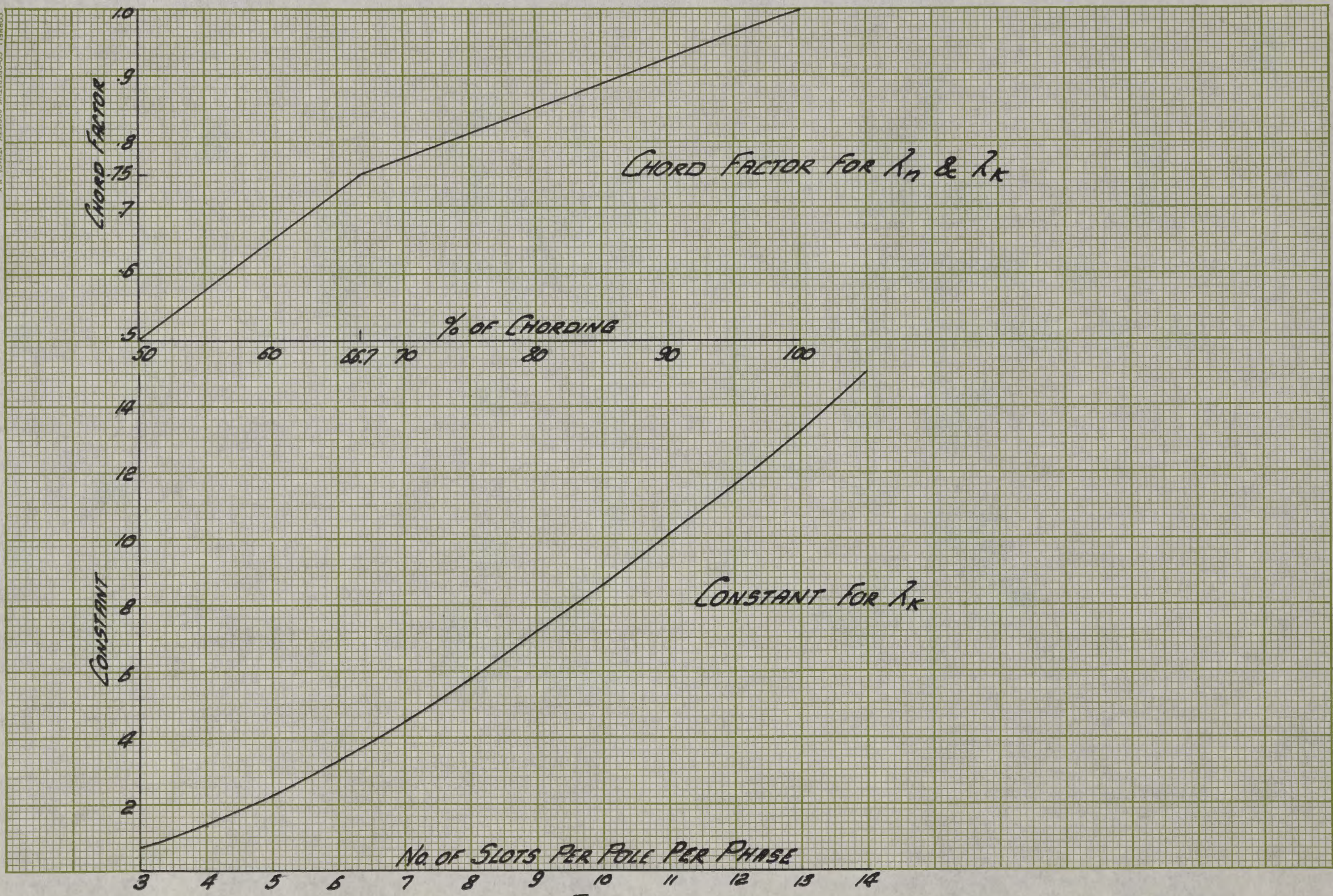

CHORD FACTOR
1.0
.9
.8
.75
.7
.6
.5
CHORD FACTOR FOR λ_n & λ_k
% OF CHORDING
50
60
66.7
70
80
90
100
CONSTANT
14
12
10
8
6
4
2
CONSTANT FOR λ_K
No. OF SLOTS PER POLE PER PHASE
3
4
5
6
7
8
9
10
11
12
13
14

FIG. 10.

Hence, the leakage reactance voltage = X (Amp.per phase)$\sqrt{3}$
=1575 volts

De-magnetizing Effect of Armature

The armature current sets up a flux counter to the flux from the field. This effect of the armature is generally termed the "armature de-magnetizing effect". It is calculated as follows:

$$AT_D = K_0 f_w W m J \sin\psi \, \chi$$

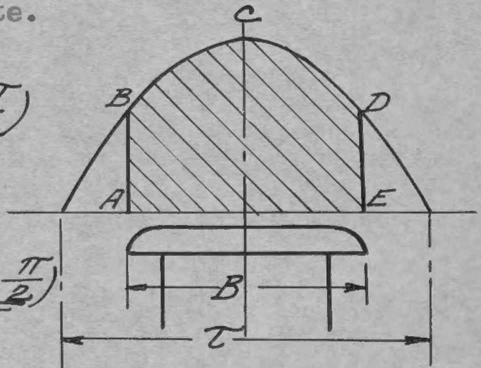
Where AT_D = de-magnetizing effect expressed in ampere turns
" K_0 = ampere turn-factor of the armature winding
" f_w = winding distribution factor
" W = number of turns per phase in series
" m = number of phases
" J = amperes per phase
" ψ = angle of total internal phase displacement (ordinary neglect)
" χ = coefficient of chording

Ampere Turn Factor

The ampere turn factor = .9 x mean ordinate of area (ABCDE).

Let $\alpha = \frac{B}{\tau}$ and the amplitude of the sine shaped magnetomotive force, then the mean ordinate.

$$\frac{1}{\alpha}\pi \int_{x=-\alpha\frac{\pi}{2}}^{x=+\alpha\frac{\pi}{2}} \cos x \, dx = \frac{\sin(\alpha\frac{\pi}{2})}{\alpha\frac{\pi}{2}}$$



$$K_0 = .9\,\frac{\sin(\alpha\frac{\pi}{2})}{\alpha\frac{\pi}{2}} = .9\,\frac{\sin(\frac{B}{\tau}\frac{\pi}{2})}{\frac{B}{\tau}\frac{\pi}{2}}$$

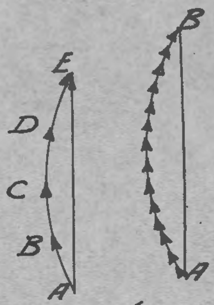
$\alpha = \frac{B}{\tau}$	1.	.9	.8	.75	.70	.65	.60	.55	.50	.45
K_0	.57	.63	.66	.70	.73	.75	.773	.79	.81	.83

Winding Factor

The winding factor f_W in the ratio at the geometric sum (resultant) of a group of vectors to their algebraic sum.

$$f_W = \frac{AE}{AB + BC + CD + DE}$$

In distributed windings the winding factor is the ratio of chord $\overline{AB}$ to arc $\widehat{AB}$.



Polyphase Windings

$$f_W = \frac{\sin\left(\frac{\pi}{2m}\right)}{q \sin\left(\frac{\pi}{2qm}\right)}$$

Distributed $f_W = \dfrac{\sin\left(\frac{a}{m}\frac{\pi}{2}\right)}{\frac{a}{m}\frac{\pi}{2}}$

Where m = No. of phases

q = No. of slots per pole, per phase.

m = No. of phases

a = 1 for not overlapping.

a = 2 for overlapping windings.

2 Phase

g =	2	3	4	5	6	Distributed $\frac{s}{\tau} = \frac{a}{m} = 1/2$
f_W =	.924	.910	.906	.904	.903	.901

3 Phase

g =	1.5	2	3	4	5	6	Distributed $\frac{s}{\tau} = \frac{a}{m} = \frac{1}{3}$	$\frac{s}{\tau} = \frac{a}{m} = \frac{2}{3}$
f_W =	.983	.966	.960	.958	.957	.957	.956	.830

For this generator the de-magnetizing ampere turns were calculated to be 244,500.

<u>No Load Saturation Curve</u>

The curve between no load voltage and field ampere turns is important since from it is the starting point in determining the full load field ampere turns and, consequently, the design of the field winding, the voltage regulation characteristics, etc. etc.

The method is as follows:

<u>Amp. Turns for the A.G.</u>

$$AT_{AG} = 1890 \times (K\delta) \times K.in.$$

Where

δ = actual single air gap in inches
K = air gap factor (increase due to slots (See Fig.3)
$K.in.$ = density in kapplines per sq. in. air gap at voltage E.

<u>Amp. turns for teeth</u>

$$AT_t = aT_t \times L_t$$

Where

aT_t = amp. turns per inch at density K.T. in teeth (See Fig. 11)
L_t = depth of slot.

<u>Amp. turns for armature back of teeth</u>

$$AT_b = at_b \times L_b$$

Where

aT_b = amp. turns per inch at density KB. back of teeth (See Fig. 12)
L_b = length of iron-path back of teeth as shown in figure.

<u>Amp. turns for pole</u>

$$AT_p = at_p \times L_b$$

Where

at_p = amp. turns per inch at density B in pole (see curves)
L_p = height of pole.

The pole tip can be considered as a part of the pole body, or entirely neglected- except when the pole face is slotted for a cage winding and thus the section reduced. In this case, figure density in the reduced section, and figure amp. turns separately for the length corresponding to the depth of slots.

<u>Amp. turns for rotor below poles</u>

$$AT_R = at_R \times L_R$$

where

at_R = amp. turns per inch at density. K.R.in rotor core.

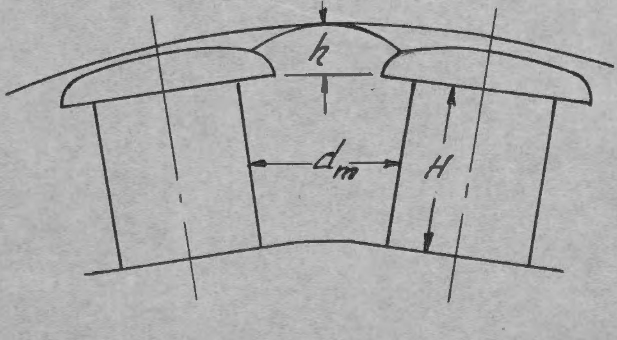
L_R = length of iron path in rotor as shown in figure.8.

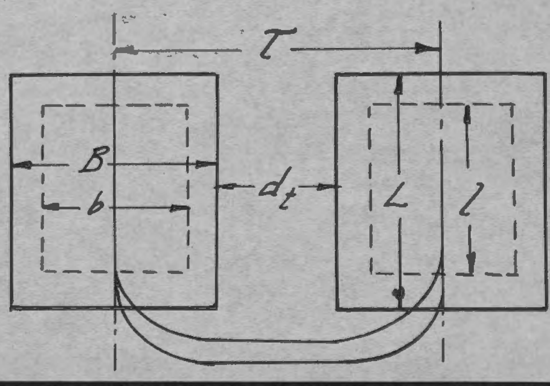
<u>Total amp. turns per pole at A.G. density K. in.</u>

$$A.T.P. = AT_{A.G.} + AT_T + AT_B + AT_P + AT_R$$

Pole Leakage Flux

The field flux which passes between poles of different polarity without cutting the armature coils and, therefore, does no useful work, is called the pole leakage flux. For machines having many poles and, consequently, a small pole pitch, this leakage flux may be quite an important item in determining the ampere turns required on the field. This leakage flux is determined as follows:





WESTINGHOUSE ELECTRIC AND MANUFACTURING CO. Curve No.

Curve No.

Signature

Date

CORNELL CO-OPERATIVE SOCIETY, ITHACA, N.Y.

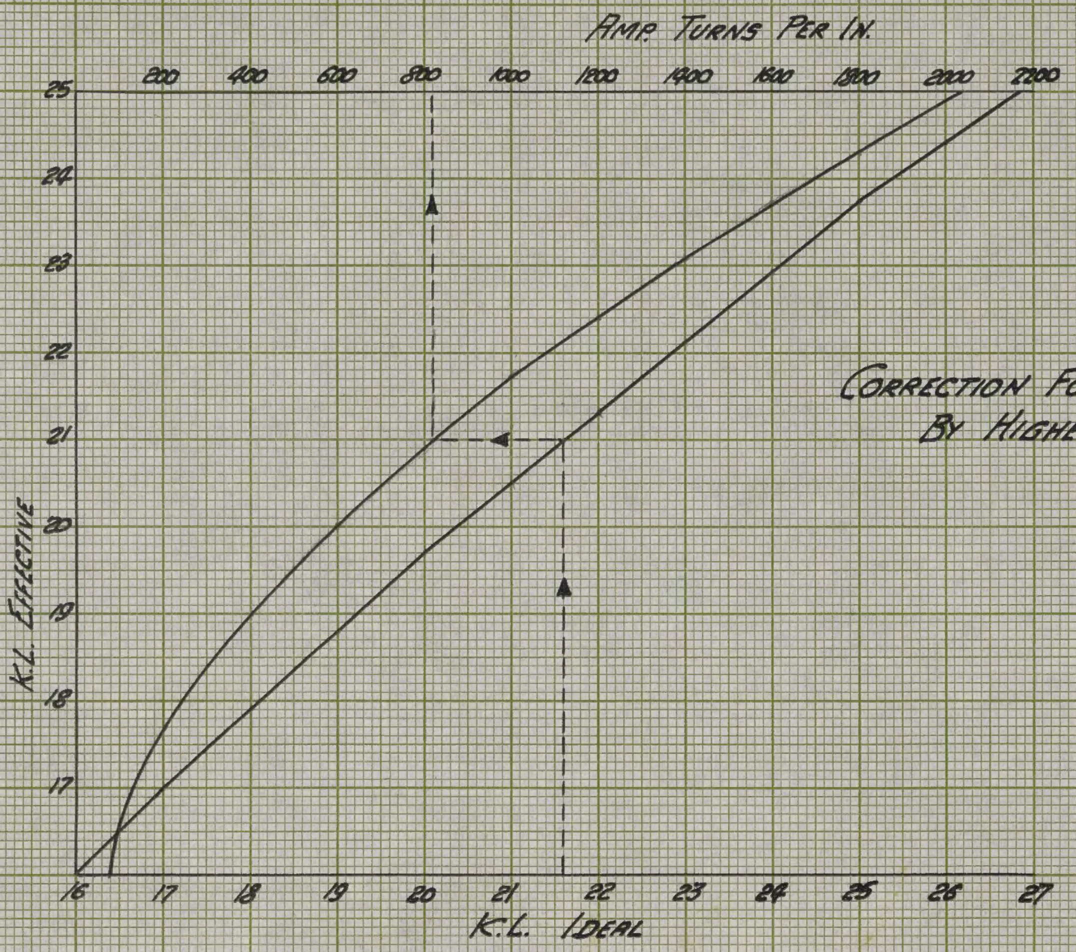


FIG.# 11.

P-35

Curve No.______ Signature______ Date______

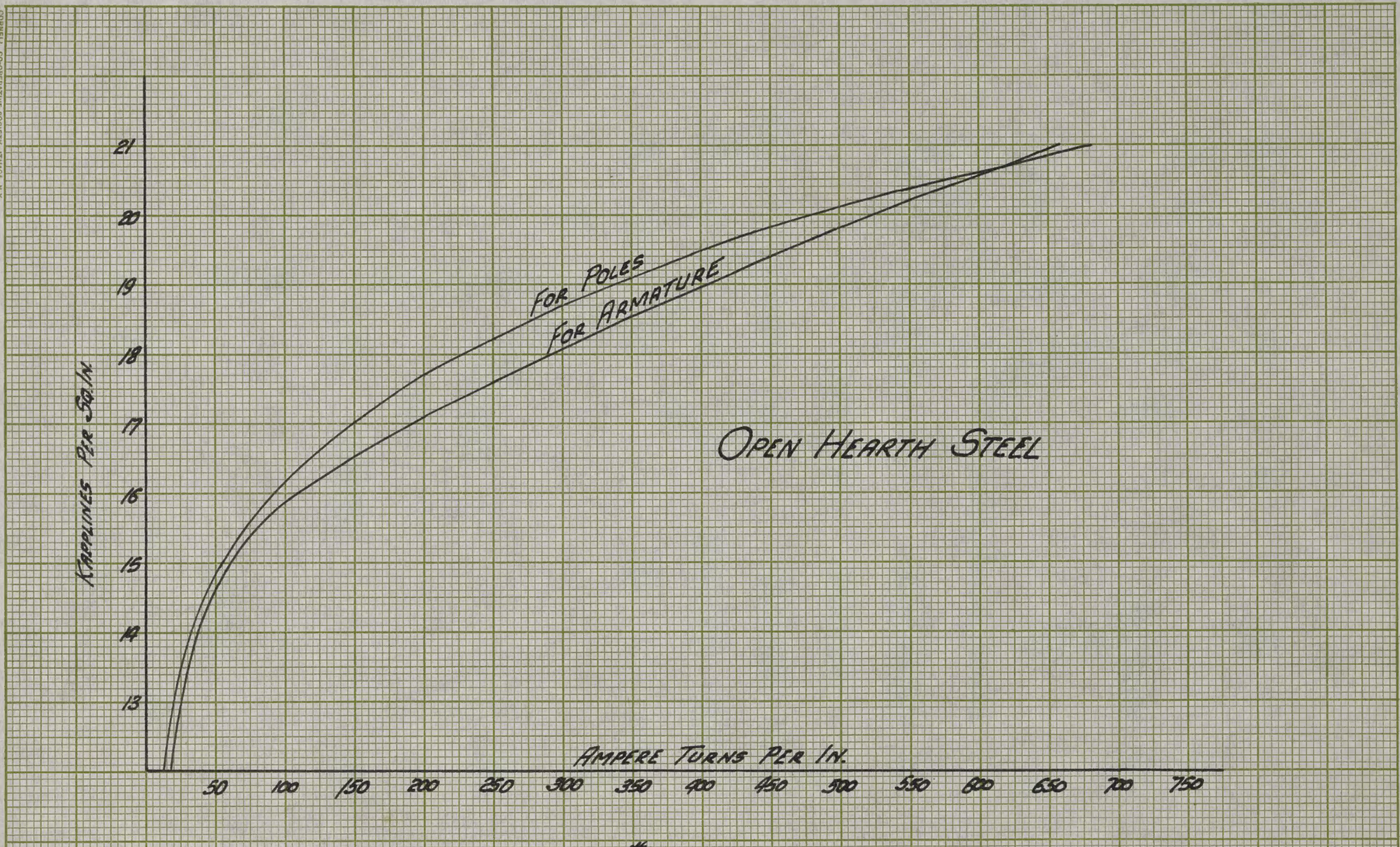


Fig. # 12 Magnetization

WESTINGHOUSE ELECTRIC AND MANUFACTURING CO. Curve No.______

P-36

The magnetic potential difference between the pole tips equals:

$$\Delta P = AT_G + AT_T + AT_A$$

AT_G = amp. turns for the air gap.
AT_T = amp. turns for the teeth
AT_A = amp. turns for the armature core

} For one pole

1. Pole-tip side leakage: $L_{Ts} = \frac{\Delta P}{1890} \frac{4hL}{d_t}$

2. Pole-tip end leakage: $L_{Te} = \frac{\Delta P}{1890} 5.86\, h \log\left[1 + \frac{\pi}{2} \frac{B}{d_t}\right]$

3. Pole side leakage: $L_{Ps} = \frac{\Delta P}{1890} \frac{2Hl}{dm}$

4. Pole and leakage $L_{Pe} = \frac{\Delta P}{1890} 2.93\, H \log\left[1 + \frac{\pi}{2} \frac{b}{\Delta m}\right]$

Total leakage flux per pole: $L = L_{Ts} + L_{Te} + L_{Ps} + L_{Pe}$

If all dimensions are measured in inches, L is obtained in kapplines.

If the pole pitch is very great, such as in 4 or 6 pole machines, it is more accurate to divide the height of the pole into several strips and determine the leakage flux for the strips separately. The total being the sum of the results for the strips.

For this generator, the flux per pole = $\frac{K.T.x\, C_p}{No.Poles}$ = 14,400 Kapplines

Following the above method, the leakage flux per pole at no load normal voltage was found to be 1285 kapplines.

Then the total flux per pole at no load is the sum of 14,400 + 1285 = 15,685 Kapplines.

Applying the method given for the determination of the no load saturation curve and correcting it for the pole leakage as just just explained, it was found for this generator that the field ampere turns at three voltages, were as follows:

Generator Voltage	Field Amp. Turns per Pole
12,000	13,974
13,500	19,360
15,000	27,966

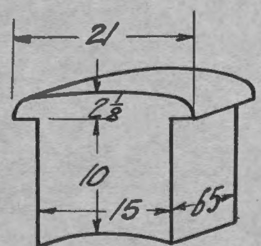
It was only considered necessary to calculate the saturation curve within narrow limits near the operating range. Fig. 13 shows the results in curve form.

For the no load normal voltage point, the magnetic densities and the ampere turns required were as follows:

Part	Density	Amp.Turns per Pole
Air gap	10.37	10,700
Teeth	18.95	1,710
Core back of teeth	12.45	324
Pole	16.15	1,100
Spider rim	9.30	140
	Total	13,974

Pole Dimensions

In the foregoing calculation, the pole dimensions used were as shown in sketch.



Short Circuit Ratio

The short circuit ratio is the ratio of the field ampere turns required to give no load normal voltage to the field ampere turns required to circulate rated current through the armature winding with the generator terminals short-circuited. This ratio may vary from about .6 to 2.0, but in a normally proportioned machine will usually be equal to unity. A generator having a low ratio will have large changes in voltage with changes in load or power factor, and will tend to be less stable in its performance under conditions of short circuits, grounds, voltage disturbances, etc., on the transmission line.

The field ampere turns required for no load normal voltage have already been determined. The field ampere turns required to circulate full load current with the terminals short-circuited, is the sum of the ampere turns required to balance the armature de-magnetizing ampere turns, previousl determined, and the ampere turns required to produce a voltage equal to the armature leakage reactance voltage. The ampere turns required for the latter may be read direct from the saturation curve, Fig. 13. They are found to be 1500 ampere turns per pole. These two values are added directly, since, at short circuit, the two readings are in phase.

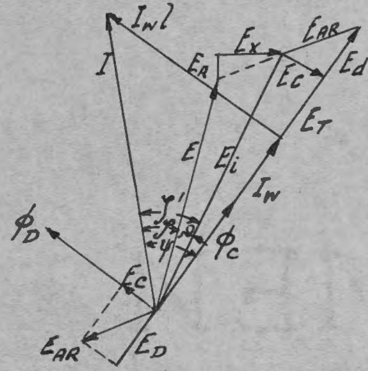
Then, for this generator, the short circuit ratio C =

$$\frac{13974}{1500 + \frac{244,500}{20}} = 1.02$$

Full Load Field Ampere Turns

The full load field ampere turns are determined by a graphical construction which is based on the following considerations:

The armature ampere turns in their reaction upon the field can be divided into two parts, viz., de-magnetizing A.T. and cross-magnetizing A.T. - If the current is in phase with the induced E.M.F., then there is no de-magnetizing effect at all, the field being merely distorted by the "cross" flux. If the current lags 90° behind the E.M.F., the de-magnetization reaches its maximum, since the total flux set up by the armature ampere turns, directly opposes the main flux of the poles, at any intermediate phase displacement ψ the de-magnetizing A.T. are proportional to $\sin\psi$, the cross magnetizing A.T. to $\cos\psi$ - or, since the total A.T. are produced by the armature current I, we may say that the de-magnetizing A.T. are proportional to the wattless component of the current, and the cross magnetizing A.T. to the watt component of the current.



$$AT_D = K_0 f_w w m J \sin\psi$$
$$AT_C = K_q f_w w m J \cos\psi$$

where the coefficients of proportionality K_0 and K_q, depending upon the magnetic reluctance, i.e., upon the field construction, are, in general, not equal.

Let ϕ_D equal the flux set up by the de-magnetizing A.T.$_D$ and ϕ_C the flux of the cross magnetizing A.T.$_C$. The former is in the direction of the wattless component J_{wl}; the latter in the direction of the watt component J_w.

The E.M.F.'s induced in the armature winding by these fluxes lag 90° behind them: ϕ_D induces E_D and ϕ_C induces E_C, which voltages, of course, must be compensated by the total voltage induced in the armature by the external field.

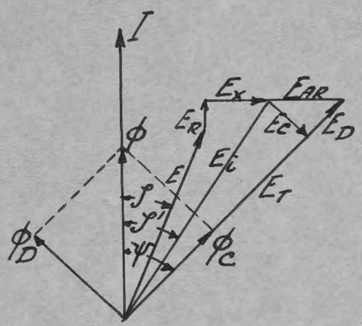
The complete vector-polygen of E.M.F.'s is made up, hence, as follows:

E = Terminal voltage, leading the current by the angle
E_r = Armature ohmic drop, parallel to the current vector
E_x = Armature reactance drop, perpendicular to the current vector.
E_i = Total induced voltage in the armature, corresponding to the actual flux in the armature.
E_c = Voltage component necessary to overcome the E.M.F. induced by the cross magnetizing flux, perpendicular to ϕ_C.
E_d = Voltage component necessary to overcome the E.M.F. induced by the de-magnetizing flux, perpendicular to ϕ_D.
E_t = The resultant of all voltage components, i.e. the voltage which would be induced at open circuit by the same field current, which is necessary to produce the terminal voltage E, when the machine is loaded with T amperes, at φ external phase displacement.

Let $tg\psi = \frac{J_{wl}}{J_w}$ since, as pointed out before, the magnetic reluctance for ϕ_D and ϕ_C is in general quite different $(K_o I K_q)\, tg\psi \lesseqgtr \frac{\phi_D}{\phi_C}$, and since E_D and E_C are proportional to these fluxes, $\frac{E_D}{E_C} \gtrless tg\psi$ which says that the resultant of $(E_D E_C = E_{AR}$ is not perpendicular to the current vector T.

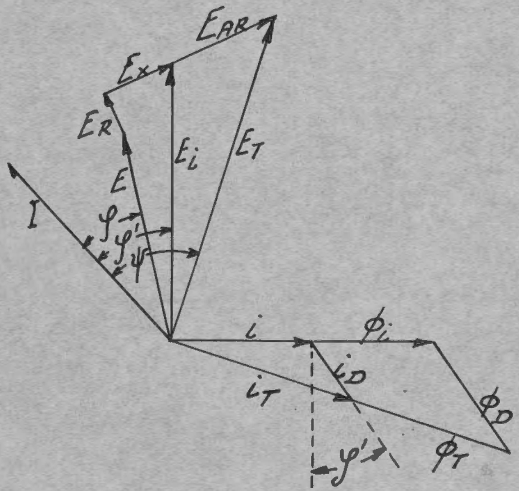
<u>Assuming, now, that the magnetic reluctance for ϕ_D and ϕ_C is the same</u>, we find:

$$tg\,\psi = \frac{J_w l}{J_w} = \frac{AT_D}{AT_C} = \frac{\phi_D}{\phi_C} = \frac{E_D}{E_C}$$



Thus, res. $(E_D E_C) = E_{AR}$ is perpendicular to J; it has the same direction as E_x. Evidently, when $\psi = 90^0$, $E_{AR} = E_D^{max}$: hence, if we figure the total voltage due to the de-magnetizing ampere turns at zero internal power factor, or sine $\phi = 1$, we may obtain the total voltage vector E_T of the loaded generator, by adding to the terminal voltage the ohmic drop E_R parallel to J, and $(E_x + E_D^{max})$ perpendicular to J.

It is, however, not necessary to convert the ampere turns into voltage first; we can make our construction with the ampere turns themselves, since, from the simplified voltage diagram, the diagram of ampere turns or amperes follow immediately.



Each E.M.F. is induced by a flux which is leading it by 90°C. In order to produce the voltage E_i, the field flux must have a component $\phi_i \perp E_i$: and in order to neutralize the armature flux, which would produce the voltage component E_{AR} the field must also have a component $\phi_D \perp E_{AR}$. Hence, the total resultant field flux will be ϕ_T, which, as evident from the construction, leads E_T by

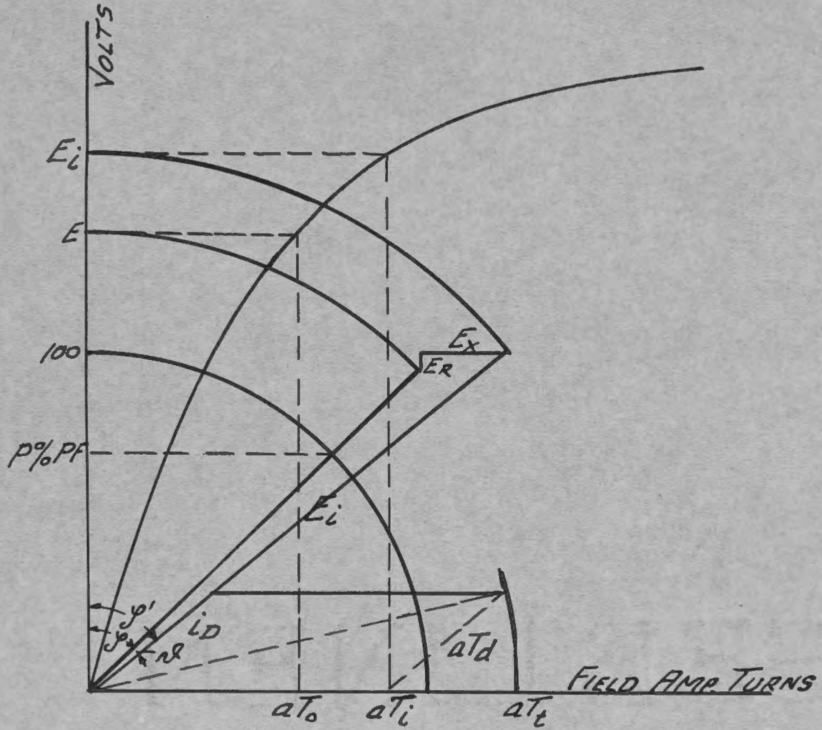
90^{o}. Now, since each flux is proportional to its magnetomotive force, and since they are produced by the same field amp. turns, we may reduce our flux-triangle $\Delta(\phi_i \phi_D \phi_T)$ to a similar triangle $\Delta(i i_D i_T)$ which gives the total (actual) field ampere turns as the resultant of two components i and i_D - i is the field A.turns which corresponds to the total induced volts E_1, and is determined in our method of constructing the vector polygon $RE_R E_x$ and using the N.L. saturation curve of the machine. $E_{AR} = E_D^{max}$, hence ϕ_D the flux corresponding to the maximum value of de-magnetizing ampere turns ($\sin\psi = 1$) and i_D the field A.turns which corresponds to this AT_D^{max} reduced to the field ampere turns.

$$i_D = \frac{K_o f_w W m J}{T_p \times P}$$

and which, as evident from the construction, must be added vectorially to i under an angle $90^{o} + \varphi'$, where $\varphi' = \varphi + \vartheta$ angle of internal phase displacement corresponding to the external φ and current J.

As pointed out, this construction is only, then, strictly correct when the reluctance of the magnetic path in which ϕ_D and ϕ_C are produced by AT_D and AT_C is the same. This is the case, when the ratio $\frac{B}{T} = \frac{\text{POLE ARC}}{\text{POLE PITCH}} = 1$, and thus $K_o = K_q$.
In any other case, the magnetic reluctance encountered by the cross flux is considerably greatér than that in the path of the de-magnetizing flux.

The foregoing is illustrated in the following sketch and explanation.



E = normal terminal voltage at no load.
E_r = armature drop due to ohmic resistance at T amp. p.phase.load.
E_x = armature drop due to reactance at T amp. p.phase.
E_i = total internal E.M.F. induced in the armature.
φ = angle corresponding to p% external power factor.
ϑ = angle of internal phase displacement.
$\varphi' = \varphi + \vartheta$ angle of phase displacement between E_i and the line current.
aT_o = field ampere turns at no load normal voltage.
aT_i = field ampere turns corresponding to E_i total induced volts.
aT_d = field ampere turns corresponding to de-magnetizing amp. turns at T load.
aT_t = total field ampere turns at E terminal volts. T amp. load, p% power factor.

The determination of the field ampere turns required under load has been worked out for this generator on Fig.13, for 26,700 kva. 90% power factor, and 32,500 kva. 80% power factor. The values are 22,800 and 27,000 ampere turns per

Curve No.______ Signature______ Date______

CORNELL CO-OPERATIVE SOCIETY, ITHACA, N.Y.

WESTINGHOUSE ELECTRIC AND MANUFACTURING CO. Curve No.______

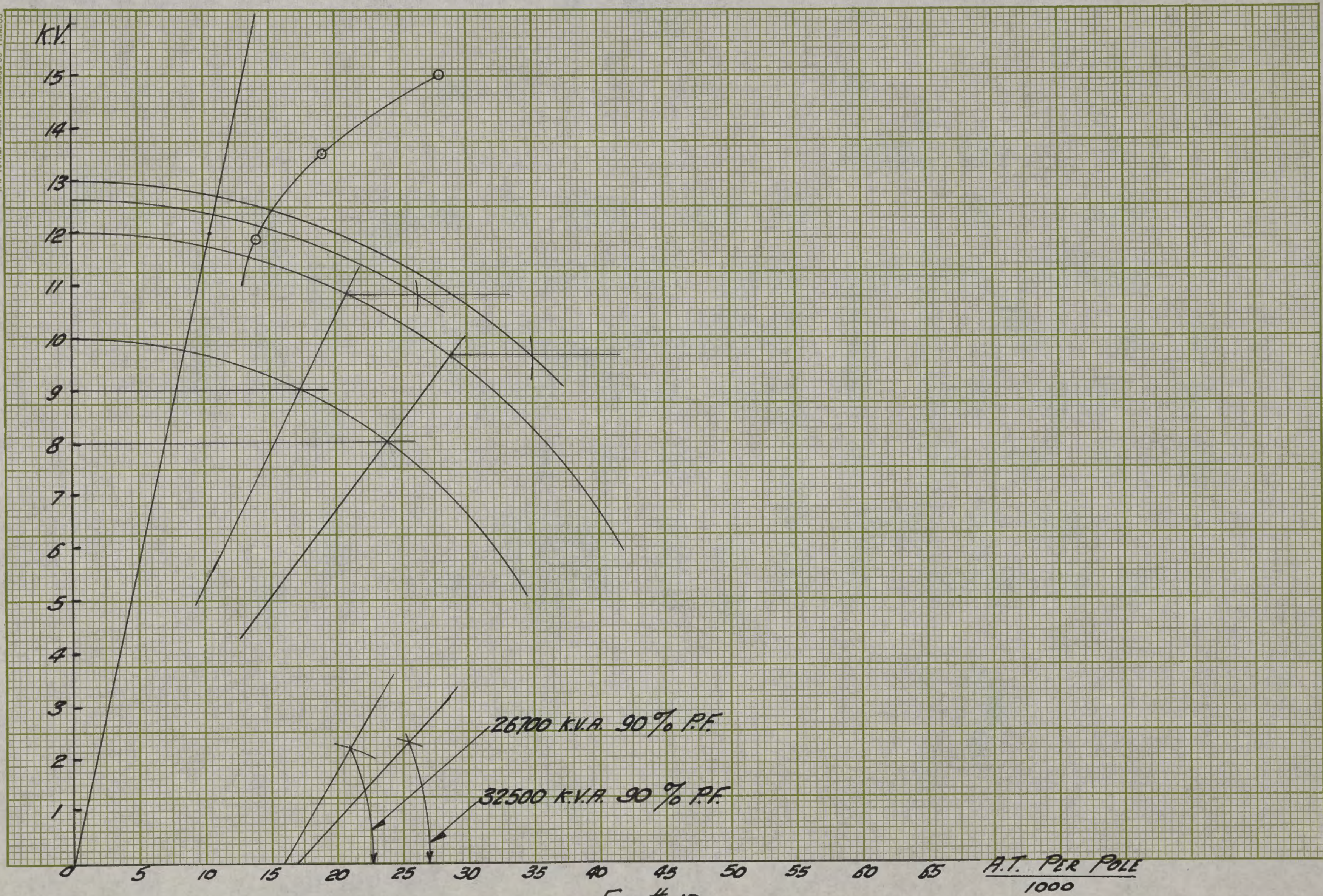


FIG. # 13.

P-44a

pole respectively.

Field Winding

Having determined the maximum required field ampere turns explained above, the field winding may be calculated as follows:

$$A = \frac{AT \times P \times MT \times K}{V \times 1.365 \times 10^6}$$

$$t = \frac{(AT)^2 \times P \times MT \times K}{a \times 1.365 \times 10^9 \times K.W.}$$

Where A = area in sq.in. of field conductor.
" A.T. = ampere turns per pole at maximum load.
" P = number of poles.
" M.T.= Length of mean turn in inches.
" K = Ratio of field resistance at maximum load to field resistance at 40°C.
" v = Allowable voltage drop across field at maximum load.
" t = Number of turns per pole.
" K.W.= Total permissible loss in field at maximum load.
" 1.365×10^6 = Conductivity (+'/resistance) of an inch cube of copper at 40°C.

Applying this method to the present case led to the selection of 42-1/2 turns (42 and 43 turns on alternate poles in order to bring out the leads in a convenient manner) of copper strap .203" thick by 2.25" wide, having a cross-sectional area of .449 ".

General Observation on Method of Design:

It is apparent from the above description that, in making the design of such a machine, it is not possible to determine the proportion in a "one, two, three" order, but, in general, assumptions must be made and the design worked out at least partially, in order to prove the suitability of the assumptions. In other words: it is rather a "trial and error" method. For this reason it is obvious that the design as a whole must be largely determined before, but few, if any, of the major features are final. However, in this discussion, the various points have been covered approximately in the order in which they are determined.

Tabulation of Results

For convenient reference, the principal design results are tabulated below:

Output Constant	1.56
Outside diameter of stator core	228"
Gross width of stator core	65"
No. of armature core vent ducts	30
Width of each vent duct	.375"
Net width of armature core	53.75"
Current density in armature copper	2300 amp./□"
Number of armature slots	300
Size of armature slots	.75" x 4.75"
Pole constant	.69
Winding constant	.434
Throw of armature coils	Slots 1 to 14
Chord factor of armature winding	.978
Total conductors	450
Total magnetic flux in air gap (max.)	417,500 kapplines
Inside bore of armature	197"
Section of air gap	40,250 □"
Air gap flux density (max.)	10.37
Tooth flux density (max.)	18.95
Section of teeth	22,050 □"
Flux density back of slots	12.45
Loss in teeth	151 kw.
Loss back of slots	197 "
Stator I^2R loss	172 "
Armature coil radiating surface	1.61 □"/watt
Leakage reactance	1575 volts
Total de-magnetizing ampere turns	244,500
No load normal voltage field amp.turns	13,974 per pole
Short circuit ratio	1.02
Full load field ampere turns	27,000 per pole
Turns per pole (average)	42.5

Acknowledgement

The writer acknowledges his indebtedness to the Westinghouse Electric and Manufacturing Company for the use of their data, records and facilities in preparing this thesis.

The general method of design followed was formulated by the late B.G. Lamme, formerly Chief Engineer of the Westinghouse Electric and Manufacturing Company.

Much of the supporting theory is covered in the writings of the late Professor E. Arnold.

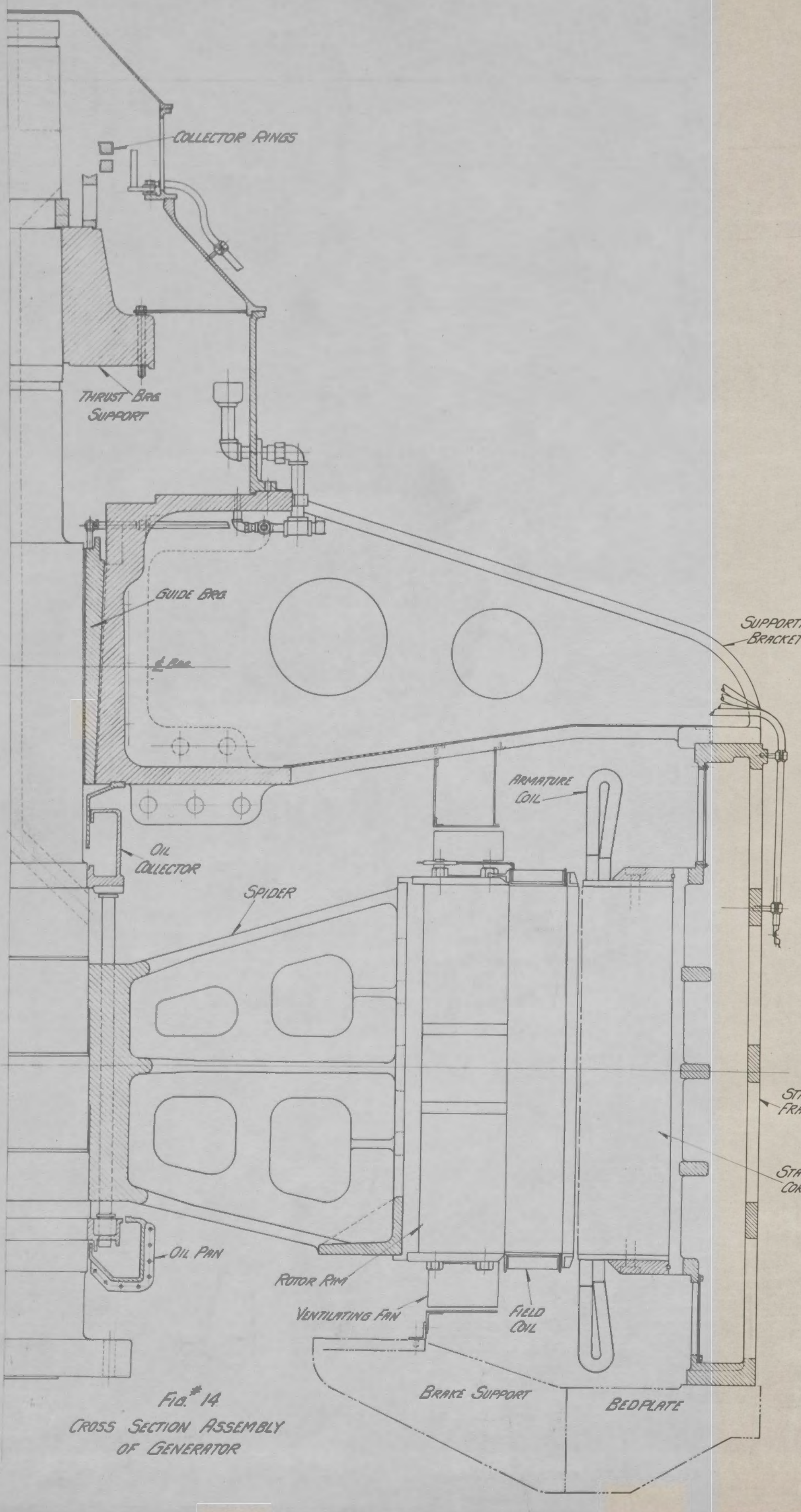


FIG. # 14
CROSS SECTION ASSEMBLY OF GENERATOR

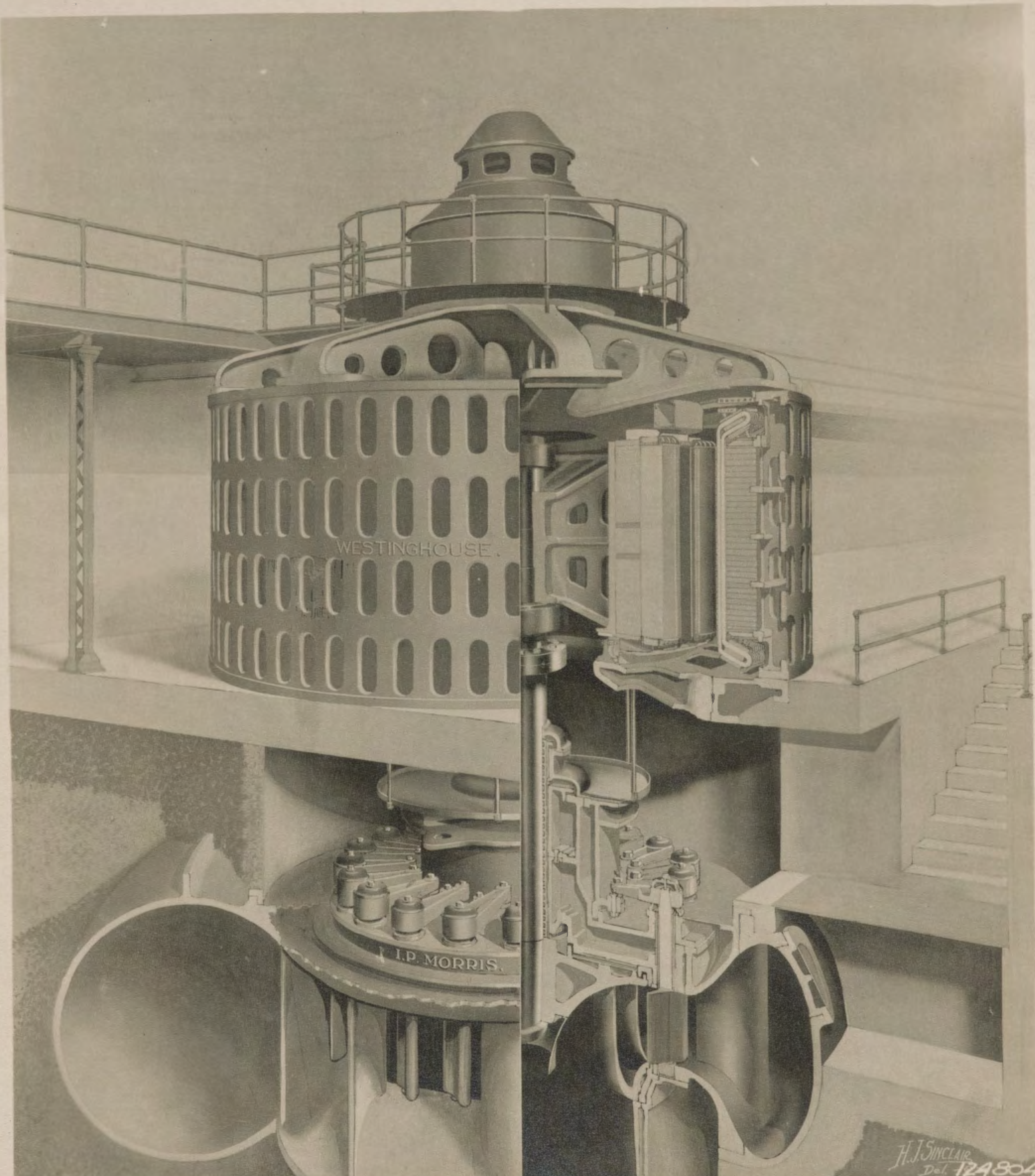


FIG. 15

DRAWING OF ASSEMBLED GENERATOR AND TURBINE
IN QUARTER SECTION

A P P E N D I X "A"

GENERATOR TESTS

On account of the size and weight of the assembled parts this generator was not fully erected in the Shop, and consequently factory tests were not obtained. Tests were made at the station of the Niagara Falls Power Company, and the results in comparison with the design are given below:

	Calculated	Test
No load field current, 12000 volts	329	325 amp.
No load field current, 13500 volts	457	445 "
No load field current, 15000 volts	658	650 "
Short circuit ratio	1.02	1.00
Stator winding resistance at 40° C.	.0415	.0423 ohms
Rotor winding resistance at 40° C.	.232	.241 "
26,750 Kv-a. 90% P.F.		
Stator copper loss	117	113 kw.
Stray load loss	46 *	66 "
Rotor copper loss	79	73.3"
Iron loss	348	390 "
Friction and windage	80	80 "
Efficiency	97.3	91.1%

The losses, other than the stator and field copper losses which admit of calculation, were measured by the deceleration (retardation) method.

The temperature results were obtained under station load conditions, and were as follows:

Voltage	13,000
Amperes per terminal	1377
Power Factor	90%
Field amperes	668
Stator core temp.rise	39.5°C. by thermom.
Stator copper temp.rise	56.0°C. " detector
Rotor copper temp. rise	71.0°C. " resist.
Temp.rise in cooling air	25.5°C. " thermom.

*Estimated from similar machine in the absence of an exact method of calculating loss.

APPENDIX "B"

Re-design of Generator

This generator was originally designed in 1918. During 1928, the stator core and winding were re-designed to take advantage of improvements in materials and design practice, to obtain higher efficiency.

The armature iron used was .014" thick compared to .017" in the original design. The silicon content was also appreciably higher, both of which reduced the loss. In addition to the change in grade and thickness of iron, the number of slots was increased and the current densities reduced. The principal changed design results were as follows:

Current density in armature copper	1675 Amp./□"
Size of armature slots	.77" x 5.8"
No. of armature slots	360
Throw of armature coils	slots 1 and 16
Chord factor of armature winding	.966
Total conductors	540
Total magnetic flux	355,000 kapplines
Flux density in air gap	8.33 "
Tooth flux density (max.)	18.4 " "
Section of teeth	19,350 □"
Flux density back of slots	11.7 kapplines
Total iron loss	159 kw.
Stator I^2R loss	143 kw.
Arm. coil radiating surface	2.73 □"/watt
Leakage reactance	2110 volts
Total de-magnetizing ampere turns	275,000

Comparison of armature copper and iron losses at 32,500 kva.:

	Original Design	Re-design
Arm. copper I^2R loss	172 kw.	143
" iron loss	348 "	159
	520 "	302

Reduction in losses = 218 kw.

APPROVAL:

The foregoing thesis is hereby approved as a creditable study of an engineering subject, carried out and presented in a manner sufficiently satisfactory to warrant its acceptance as a prerequisite to the degree for which it has been submitted. It is to be understood that by this approvel the undersigned does not necessarily endorse or approve any statement made, opinions expressed, or conclusions drawn therein, but approves the thesis only for the purpose for which it is submitted.

Name J. G. Tarboux

Title Prof. Elec. Eng.