

pending on operating conditions would be about 0,75 to 1,5 times the values estimated by equation 68./ for the contribution of the settling compartment only. Therefore the total loss is approximated by the expression

$$\int_{Me}^{(tot)} = \int_{Me}^{sc} + (0,75 \text{ to } 1,5) \int_{Me}^{sc} \quad 71./$$

with a reasonable average value of 1,0 for the multiplier.

d./ Deviations from ideality. The fore-going analysis represents undoubtedly idealised conditions since it was performed by assuming uniform temperature, that is viscosity distribution in the entire slag bed. However, in connection with the discussion of the probable temperature distribution in the molten slag bath it has been demonstrated previously, how in a working unit the suggested heat flow pattern would affect the distribution of viscosity. Figures IV-7 and IV-8.) show the estimated viscosity distribution in vertical and horizontal direction between the top of the bath and the level of the electrode tip in the vicinity of an electrode or in the plane of temperature separation between the electrode and the furnace wall. The viscosity-terminal velocity relationship as represented by Stokes law would facilitate the estimation of the change in terminal velocity of the matte particles in the same directions as a function of temperature and, consequently, viscosity distribution in the slag. As for examples, figures IV-25 and 26.) drawn after the above-mentioned graphs indicate the settling velocity profile of matte particles of 50 micron size in the region between the two constant temperature faces, i.e. electrode tip and the top of the molten bath, while figure IV-27 represents the estimated settling velocity distribution in the entire depth of slag bed in vertical direction between the top of the melt and the slag-matte interface. Obviously the same applies to particles of any size in the entire size spectrum of the matte prills. Pertinent data are collected in Table IV-6.

The approximative nature of the estimated losses becomes truly apparent in considering the furnace after figure IV-22a.) as a series of continuously stirred reactors. In each reactor along the side of the electrode due to the ascending movement of the hot slag a portion of the particles will be lifted to the surface of the melt and carried toward the periphery of the active electrode area. On approaching the periphery the cooling melt starts descending and the prills being carried downwards, will be deposited (partly at least) in the bulk of the matte bed. This ascending-descending movement will facilitate greatly the coalescence of the particles and because of/...

and because of the substantially enhanced settling velocity, Stokes' law will have an ever-decreasing validity as the bulk flow of the slag gets closer and closer to the centre of the furnace. In addition, in the overlapping region of the active areas between electrodes and, especially, in the path of the current flow, the pattern of slag movement and with it the settling of particles becomes unpredictable. The extent of the overlapping areas of the active zones of the electrodes is very substantial, accounting to some 50 to 80 per cent of the total mixed area depending on electrode position and furnace operating characteristics. - These facts may further illustrate the extremely complicated nature of matte settling in an electric furnace and the difficulties involved in correct settler dimensioning. As a reasonable first approximation the discussed method based on average slag temperatures will be applied later in this study for the comparison of rectangular and circular furnace symmetries.

In an other attempt to correlate viscosity and hindered settling conditions for estimating matte losses the relationship proposed by Guth and Gold (29) was tested. Based on the original Einstein equation these authors produced in a rather lengthy calculation an expression applicable to the solutions of high molecular substances with spherical shape. Accordingly

$$\eta = \eta_0 (1 - 2,5v/V - 14,1 v^2/V^2) \quad 72./$$

$$\text{or} \quad \eta = \frac{\eta_0}{1 - 2,5v - 14,4v^2} \quad 73./$$

The application of equations 72 and 73.) was attempted in the context of the conditions outlined above, that is up to the range of about 0,5 per cent matte entrapment in slag with an assumed range of viscosity of 3,0-3,5 poise. The results obtained were completely meaningless. Usable results could be arrived at only when the loss figures approached 5 per cent and were preferably even higher. As a consequence, these equations could under no circumstances be recommended for loss estimation purposes in connection with the operation of matte smelting electric furnaces.

4./ Results of measurements on flow characteristics conducted with radioactive tracers.

A more realistic picture on the nature of the movement of material in a working unit can be gained by the application of radioactive tracers in the flow media proper. The method has been used by Themelis and Spira (15) in reverberatory furnaces and more recently by the National Institute for Metallurgy in South Africa (1, 30). The work of NIM is of special interest/ ...

interest since it was performed on a unit geometrically identical with the reference furnaces of this study. As it was found the total flow in the furnace is made up of backmix and plug flow plus a certain degree of stagnant or dead-flow. From the results of tracer tests it would appear that under the particular operating conditions at the time of the experiments, the flow could be characterized as consisting of about 64-65% backmix, 1% plug flow and 34-35% dead flow. The high ratio of backmix to plug flow is interesting and would indicate good flow continuity in the upper region of the bed. The mixing is a good deal more intensive along the entire length and the character of the flow is considerably different from that in a fuel-fired reverberatory furnace with theoretically estimated dead-flow regions of up to 86 per cent (15).

The extent of the dead-flow region found by tracer tests in electric furnaces of the matte smelting type may naturally be subject to rather extensive periodic variations depending on various factors like rate of flow, number and position of tapholes used, hydrostatic head above the taphole and, to a great extent on the attendance by the operator. Furthermore, it may also vary according to the position (depth of immersion) of the electrodes and according to the change in the level of the slag and matte. From the standpoint of uniform matte losses it is therefore important to keep the level of the slag, that is its height above the taphole as constant as possible.

Some discussion on the flow velocity of the slag bed will appear in Appendix II B.

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SECTION V.

ON THE NATURE OF METAL LOSSES TO THE SLAG AND THE POSSIBILITY OF SLAG
OPTIMISATION.

I./ On the nature of metal losses when smelting concentrates.

From the foregoing discussion of the physico-chemical characteristics of the slags as well as the character of the heat and material flow in the furnace it is apparent that the viscosity of the slag plays a decisive role in slag-matte separation when smelting copper-nickel concentrates either in reverberatory or electric furnaces. From a sound furnace design point of view it would be of great importance to have an adequate knowledge of the nature and the cause of metal losses to secure the best economy of the furnace operation.

As to the part played by the viscosity it is obvious that at lower slag viscosities the matte prills move more easily through the molten slag layer and the danger of matte carry-over through the taphole is thereby diminished and the economy of the operation improved. This point has already been handled in detail. However, the problem of metal losses to the slag is rather complex and cannot be equated simply with slag-matte separation based solely on viscosity considerations. It has long been the subject of research as to what extent the metallics present in the slag were true solutions or merely entrained as metal or matte particles. Literature data on the subject relate mostly to reverberatory furnace treatment of copper concentrates vastly different in composition from those handled in the RPM electric furnaces. Hardly any reference is encountered in the literature relating to simultaneous losses of copper and nickel, not to mention the case when these losses occur in electric furnace processing of concentrate.. Investigations on this line centred mostly around the dissolution of sulphidic copper, and in a very few cases nickel, primarily in fayalite-type slags produced in converter processing of mattes.

Copper losses in reverberatory furnace slags can be attributed to two main reasons viz. mechanically entrapped sulphide and an inseparable, non - sulphide phase of indeterminate nature which also might be termed "chemical, or electrochemical loss". Irrespective of the reason, the volume of the losses is rather substantial. As Altman and Kellogg(1) have pointed out, primary smelters in the USA lose every year more than 20,000 tons of copper in reverberatory smelter slags. From the results of recent investigations

it would appear/...

it would appear that a large proportion of unrecovered copper is in true solution in the slag melt. This may be so but the mode of approach to studying the nature and the true extent of the losses has an important bearing on how the experimental data could be related to, or would be representative of the losses occurring in practical furnace operation. For example from the work of Ruddle, Taylor and Bates (2), Toguri and Santander (3), Altman and Kellog (1) and Bailey and Garner (4) it is obvious that the oxygen potential of the atmosphere above the melt is a decisive factor in determining metallic losses to the slag. Indeed, these authors are in excellent agreement when their results are expressed in terms of the $\text{CuO}_{0.5}$ activity. On the other hand Sehnalek and Imris (5) developed the concept of sulphidic dissolution of copper in sulphur-containing slag and regarded the total dissolved copper as being the sum of sulphidic and oxidic dissolution. Themelis and Spira conducted research exclusively on commercial slags containing lime, alumina and other constituents (6) and the solubility data were presented in correlation with matte grade. The interesting point in their research was that the great number of tests were conducted under identical experimental conditions. This is an important fact since, as Nagamori (7) pointed out, meaningful comparison of copper loss among various smelters can be made only, if for example temperature, per cent copper in matte, per cent SiO_2 in slag and magnetite activity are well-defined parameters. Further to this comes that all other commercially common slag constituents such as lime, alumina, should also be identical for each case. If not, then additional parameters must be specified to define the system. As a consequence, the comparison among different smelters becomes rather difficult and may even lead to conflicting results.

From the standpoint of thermodynamics Nagamori's argument is entirely correct. If the research were restricted to matte losses occurring in converter treatment of matte with a fayalite-type slag, then basically the system to be handled would be a quinary system consisting of the constituents Cu, Fe, S, O, and SiO_2 . Because of the character of the converter operation, irrespective of the grade of matte to be treated, the necessary flux addition will be such that the composition of the slag produced will closely approach that of the pure fayalite. Then with the three co-existing phases in the converter, (matte, slag and gas) and with the compounds mentioned, the number of degrees of freedom for the system will be $F = 5 + 2 - 3 = 4$. Hence the necessity of the four, well-defined parameters mentioned above. Naturally in practice, to the five constituents (i.e. Cu, Fe, S, O, SiO_2) will have to be added Al_2O_3 , MgO and also CaO. Furthermore, slags and mattes in this system are produced together, exist together and are immiscible

liquids/...

liquids. As Brittingham noted (8) they should therefore be expected to conform with the behaviour of conjugate solutions which means that when the composition of one of the pair of conjugates varies, the composition of the other will also vary according to the phase rule relationship. Curiously enough this fact is not even acknowledged in a number of studies of metal solubility in slags.

1./ Laboratory studies on the dissolution of copper and nickel in the slag.

1.1./ Scope for study. Due to the complexity of metallic losses occurring in matte smelting operation the approach of Themelis and Spira, that is keeping identical experimental conditions appears to be the only feasible way to obtain meaningful results from laboratory (and also industrial) tests on the solubility of matte in the molten slag. The projection of laboratory test results onto practical operation may become even with these efforts difficult and the question of how far experimental data represent actual conditions existing in operating units may only partly be answered. It would be important to know what portion of the metallic losses is attributable to carry-over of matte in the slag stream and what could be regarded as true chemical loss as a result of dissolution of matte in the slag.

The study of the effect of slag composition on metal losses could be a useful approach to this problem. As mentioned, very little is found in the literature on this subject in general and even less in straight relation to reverberatory furnace operation. With regard to matte smelting in an electric furnace, data are virtually non-existent. As to the general nature of losses a paper published long time ago by Vanjukoff (9) is of considerable interest, containing data on relationship between slag composition and copper content of slag. However, the results obtained should be viewed with some caution because of a./ the extreme range of slag compositions and temperature used by the author and b./ the strongly reducing atmosphere in which the investigations were carried out. On account of the latter the results could hardly be related to reverberatory smelting and may be more relevant to electric furnace operation.

In regard to obtaining representative data from laboratory test work one seems to be in a more advantageous position when the smelting of concentrate is carried out in an electric furnace instead of a reverberatory furnace. The reasons are as follows:

- a./ The atmosphere is fairly uniform from furnace to furnace with a relatively low oxygen partial pressure which, to some degree, may simplify...

simplify the problem encountered in reverberatory furnace operation with an oxidising gas phase above the melt. As a result the magnetite activity will depend to a large extent only on the converter return slag.

- b./ The grade of electric furnace matte is relatively low, the total copper and nickel content probably varying from 20 to 30 per cent. Thus the effect of matte grade is diminished.
- c./ The silica content of the slag is high and can be anticipated to vary from 35 to 45 per cent.

On the other hand, from the point of view of representative results the common problem for electric and reverb-type smelting is that the silica and iron oxide contents are not near to SiO_2 or Fe-saturation. Nevertheless a reasonable possibility for simulation of laboratory tests with electric furnace operation is offered by keeping the oxygen potential of the atmosphere above the slag melt low, preferably similar to that existing in the plant. As to the ever-changing pattern of temperature and equilibrium conditions in a working unit the best compromise will be a constant temperature representing the high average of plant slags and a reasonable time-length for slag-matte equilibration.

1.2./ Experimental

The tests were carried out with the main objective to obtain information on the role of the various slag-forming constituents in metal losses with particular reference to the types of slag encountered in the operation of furnaces smelting copper and nickel-containing concentrates. In order to simplify the task the composition of the slags was restricted to the relatively narrow range applied in the study of electrical conductivity and viscosity. As a further simplification only one matte grade was used with all the slags investigated which is justifiable because of the limited variations in the grade of matte produced in electric furnace smelting of low-grade concentrates.

Altogether three series of tests were performed. The various series differed only in the oxidation state of the iron oxide constituents. Slags from the first test series originated from the electrical conductivity tests. After the completion of the conductivity runs, the platinum crucibles containing the reduced slags (Fe^{2+} content minimum 88%) were placed in a box furnace operating with silicon carbide heating elements and were heated to 1400°C in air. Then the hot slags were poured out from the crucibles unto
a large/...

a large, cooled iron plate so that the shock-cooling arrested further oxidation. The glassy materials were finally crushed to 80% minus 200 mesh size.

The materials used in the other two test series were made up from calcined oxides and ferrous oxide of minimum 90 per cent Fe^{2+} content and premelted in platinum crucibles in the tube furnace used for viscosity and conductivity work. In the third series iron powder was added to the FeO component in order to increase the Fe^{2+} content of the compound. In this instance it was assumed that the oxide above 86 per cent FeO was mostly in the form of magnetite and the metallic iron addition was calculated according to this stoichiometric relationship. The hot melts were poured into water and after cooling, the solidified materials were crushed to the fineness mentioned above.

Next the slags were intimately mixed with powdered electric furnace matte containing 9,27% Cu and 16,03% Ni. The ratio of slag to matte followed that employed in operating units, but for the study of the effect of matte quantity the investigations were extended to ratios 2:1 and 1:1 using only one particular slag (No 6) for this purpose. The total weight of mixture in each run was 20 grams.

Because of the presence of matte, platinum was unsuitable for crucible material in the high temperature tests. Instead of platinum, Haldenwanger Alsint 2/A-type sintered alumina crucibles were used. The crucible material constituted undoubtedly a weak point in these investigations since the highly corrosive slag attacked the crucible material and increased thereby the alumina content of the slag which resulted in a proportional decrease of all other slag-forming constituents. As analytical results indicated, the original 5% Al_2O_3 that was intended to be kept constant has increased in some cases on the completion of the tests to as high as 10 per cent, varying on an average between 7 and 8 per cent.

The crucibles were seated in a graphite holder which, in turn, was placed on the firebrick crucible support and raised in the hot zone of the furnace tube. To maintain the required low partial pressure of oxygen in the furnace atmosphere, spectrographic argon, or alternately nitrogen was used as a purge gas at a flow rate 1,2 to 1,5 litre per minute. The oxygen content of these gases was guaranteed to be 10^{-6} atm. or less. In one test series hydrogen was added at a low flow rate of 20 to 30 ml per minute to

the nitrogen stream/...

the nitrogen stream in order to decrease the oxidation potential of the furnace gases.

After heating to 1400°C the slag-matte mixture was kept for a minimum of 2 hrs. at this temperature. The average layer thickness of the molten slag + matte was about 15 - 20 mm in the crucible. A quick calculation will then indicate that matte particles released at the top of the melt in a minimum of 2 hrs retention at 1400°C had to have a terminal velocity of $2,0/2 \times 3600 = 2,8 \times 10^{-4} \text{ cm. sec}^{-1}$ to reach the bottom of the melt. Because $1/5$ of the total melt volume was occupied by the matte, the actual length of travel reduced to about 16 mm = 1,6 cm and thus the settling velocity $2,1 \times 10^{-4} \text{ cm. sec}^{-1}$. The viscosity of the slag investigated varied from 1,5 to 4 poise at 1400°C which, on the basis of Stokes' law would set the limiting particle size at the density difference of slag and matte to 15 (max) microns.

After the stipulated holding time at temperature the crucibles were quickly removed from the furnace and with due care taken to avoid stirring up the settled matte they were placed in cold water. Complete solidification of the melts took place within seconds and the matte separated clearly from the slag in form of a butt that could easily be removed from the cracked, glassy slag matrix. The slag was crushed to a fine powder and subjected to XRF analysis for Cu and Ni content while the divalent iron content was determined according to a method suggested by Jackson (10). The basic principle of this method is that after dissolution in 40% HF acid to which standard $n/10 \text{ NaVO}_3$ solution is added, the NaVO_3 oxidises the Fe^{2+} to Fe^{3+} while the excess vanadic acid is back-titrated with standard $n/100 \text{ FeSO}_4$ solution. The reproducibility and accuracy of this procedure is excellent, results could be reproduced with, or better than 1,5 per cent relative error.

1.3./ Test results and discussion.

For easy comparison the slags were grouped in the same way as in the conductivity and viscosity tests. The results obtained on the dissolution of copper and nickel upon the effect of the various slag-forming constituents are given in figures 1A to 5A and 1B to 5B.

A. series. Oxidised slags.

- 1./ In Group I (figure V-1A) the copper and nickel solubility increases with the increase of the basicity ratio and the decrease of the SiO_2 content of the slag. Basically, the increase of the CaO (at constant MgO) content is instrumental in

solubility/...

Figure V-1 A

EFFECT OF BASICITY ON THE METAL VALUES IN THE SLAG

FeO, MgO const
CaO, SiO₂ varied

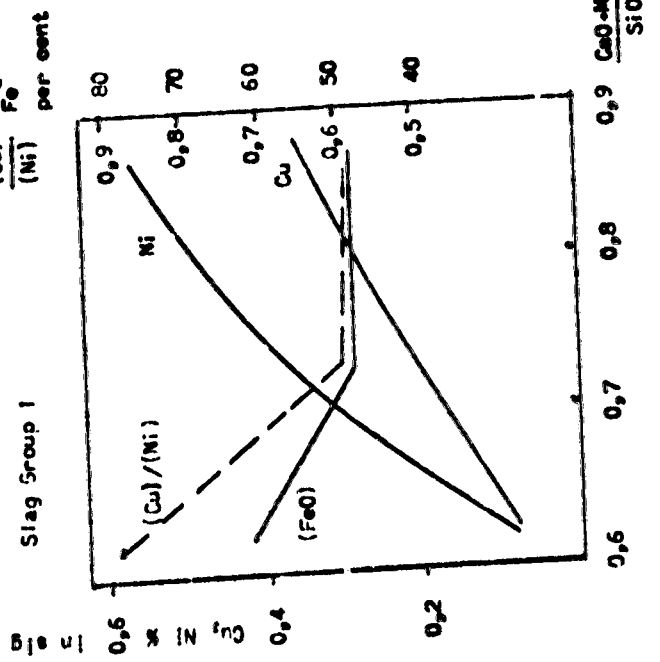


Figure V-2 A

RELATIONS: IP BETWEEN BASICITY AND METAL VALUES IN THE SLAG

FeO, CuO constant
MgO, SiO₂ varied

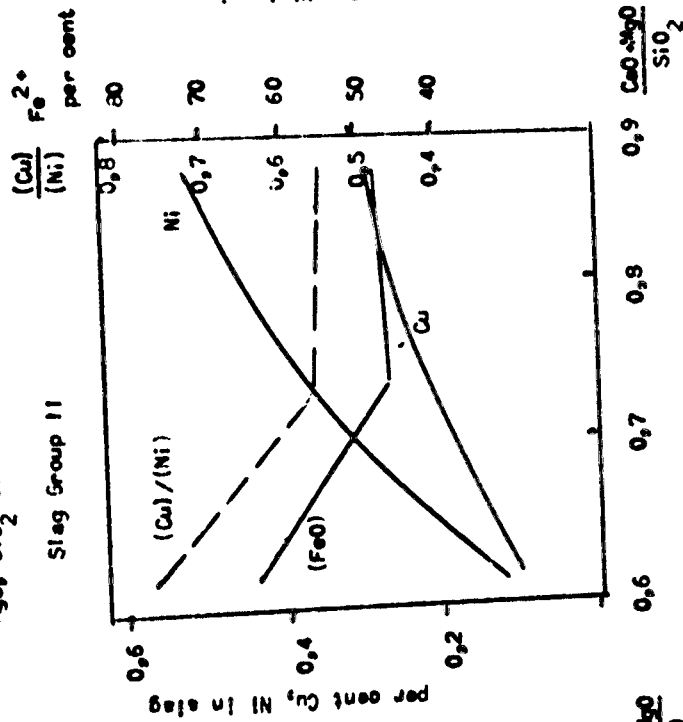


Figure V-3 A

RELATIONSHIP BETWEEN BASICITY AND METAL VALUES IN THE SLAG

FeO, CaO/MgO constant
CaO, MgO, SiO₂ varied

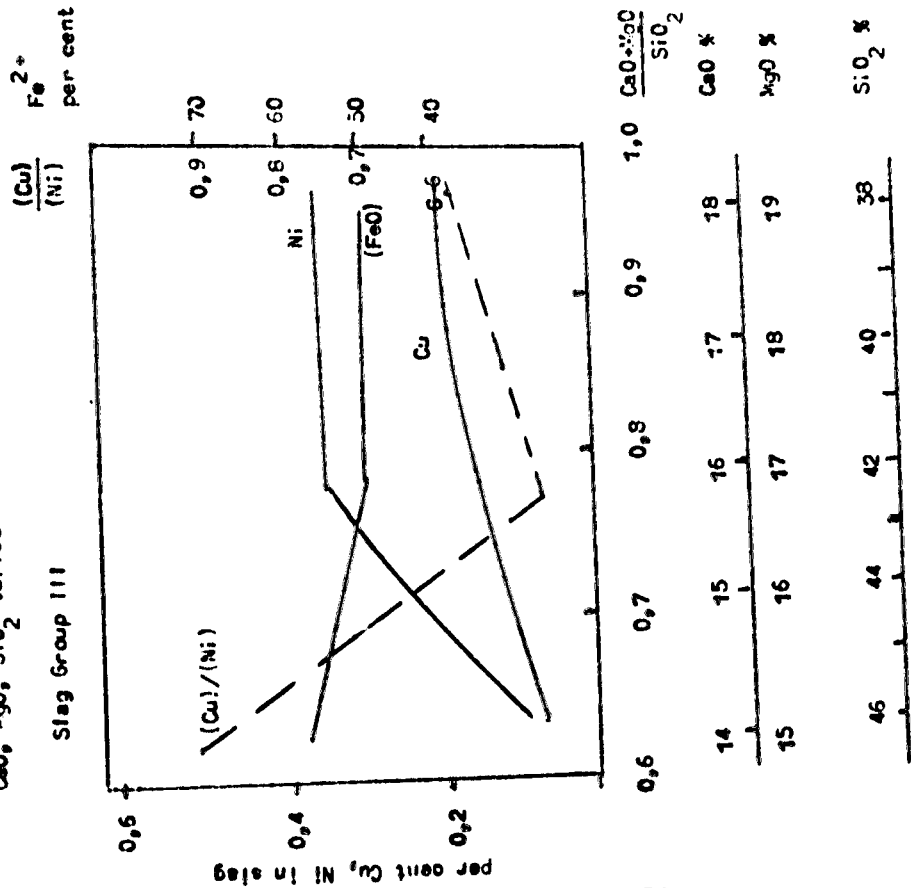


Figure V-4 A

EFFECT OF THE TOTAL IRON CONTENT ON THE DISSOLUTION OF Cu AND Ni IN THE SLAG

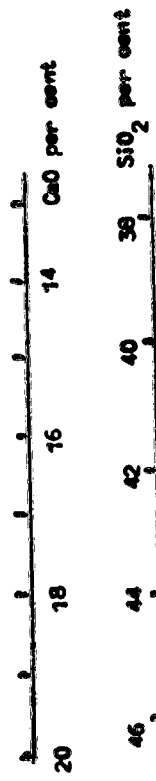
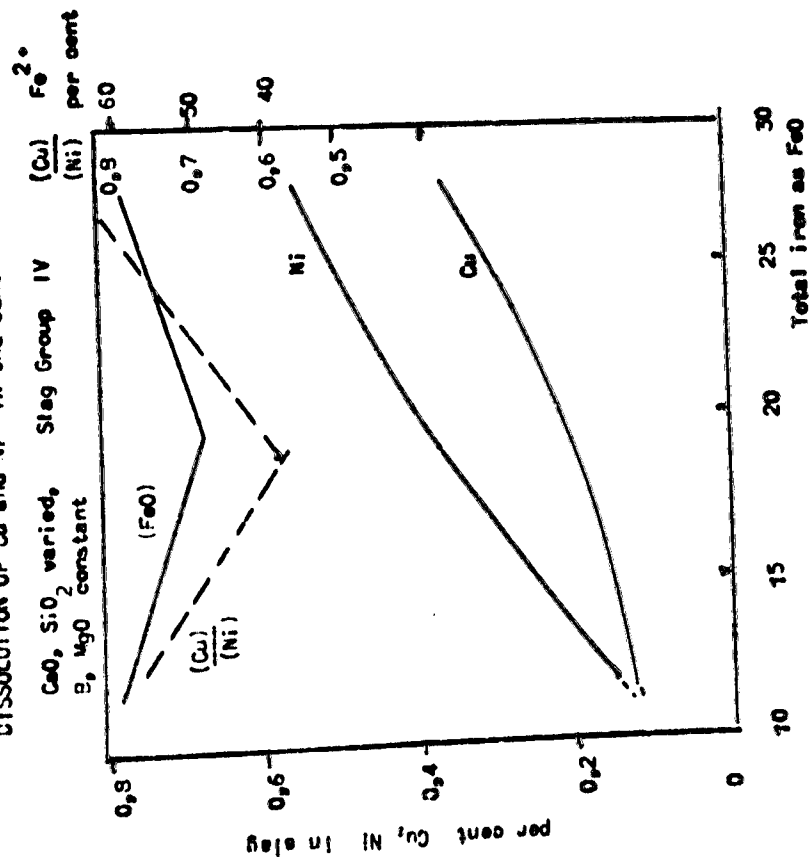
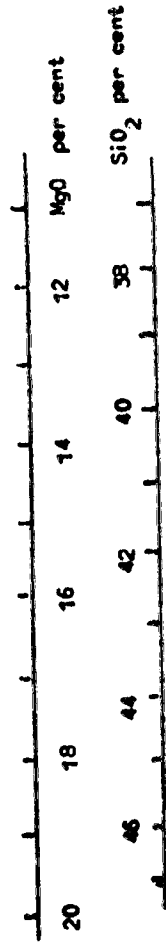
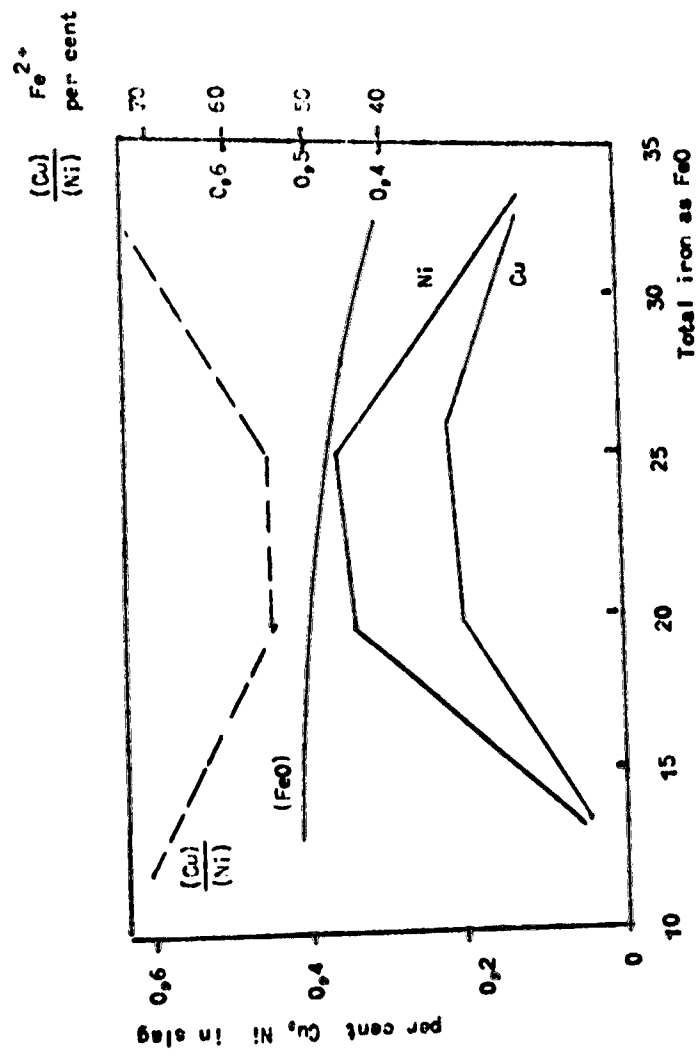
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Figure V-5 A

EFFECT OF THE TOTAL IRON CONTENT ON THE DISSOLUTION OF Cu AND Ni IN THE SLAG

MgO, SiO ₂ varied B, CaO constant	Slag Group V
100	100
90	90
80	80
70	70
60	60
50	50
40	40
30	30
20	20
10	10
0	0



solubility of both metals

- 2./ The pattern in slags of Group II (figure V - 2A) is similar to that shown in graph V-1A but the addition of MgO appears to result in an increased loss of nickel.
- 3./ In the slags in group III (fig. V-3A) the CaO/MgO ratio is kept constant but the amounts of each oxide are increased, the net effect being a substantial reduction in the loss of both copper and nickel as compared with the results shown in figures V-1A and 2A. Comparing figures V-2A and 3A shows that the viscosities of slags in Group III are slightly lower than those in graphs V-1A and 2A. The differences are such however that, as will be shown subsequently, it is difficult to accept that this is the only factor responsible for the reduced losses of Cu and Ni, and interfacial tension factors may also play a part.
- 4./ Increase in the iron content of the slags as in Group IV (figure V-4A) gives losses of copper and nickel comparable with those already found for Groups I and II (figs 1A and 2A). In slags in Group V (figure V-5A) an increase in the iron content appears to result in rather lower losses of copper and nickel giving values similar to those in Group III (fig. V-3A.)

On account of the reduced partial pressure of oxygen in the furnace atmosphere the ferrous oxide content of the slag is around 95% with occasional drops to 88-90%. Consequently, the use of slags of rather high trivalent iron contents was not done with the aim to simulate conditions prevailing in the electric furnace. The purpose was rather to indicate the general trend in copper and nickel losses that can be expected with the concentration variation of the slag-forming constituents. This was anticipated to be more pronounced with the higher oxidation potential of the slag. In general, the effect of the change in the concentration of the slag constituents upon metal losses corresponds with that observed by Vanjukoff despite the entirely different experimental conditions and techniques applied. In Vanjukoff's work only the loss of copper was investigated. He also noticed a reduction in solubility when substituting lime for magnesia in slags the iron and silica contents of which were held reasonably constant at 15 and 45 per cent respectively as against 15,5 and 40 to 46 per cent of this study. Vanjukoff investigated the effect of alumina on copper solubility and found it to be negligible. This observation is of considerable significance to the present study because of the increase of Al_2O_3 contents of the slags due to dissolution of the crucible material.

The striking feature/...

Figure V - 6. CHANGE OF Cu and Ni CONTENTS COMPARED WITH THE CHANGE OF VISCOSITY AS A FUNCTION OF BASICITY AND CaO (1), MgO (2) CaO-MgO (3) CONTENT OF OXIDISED SLAGS. ("A" series of tests)

Slag Groups I, II and III.

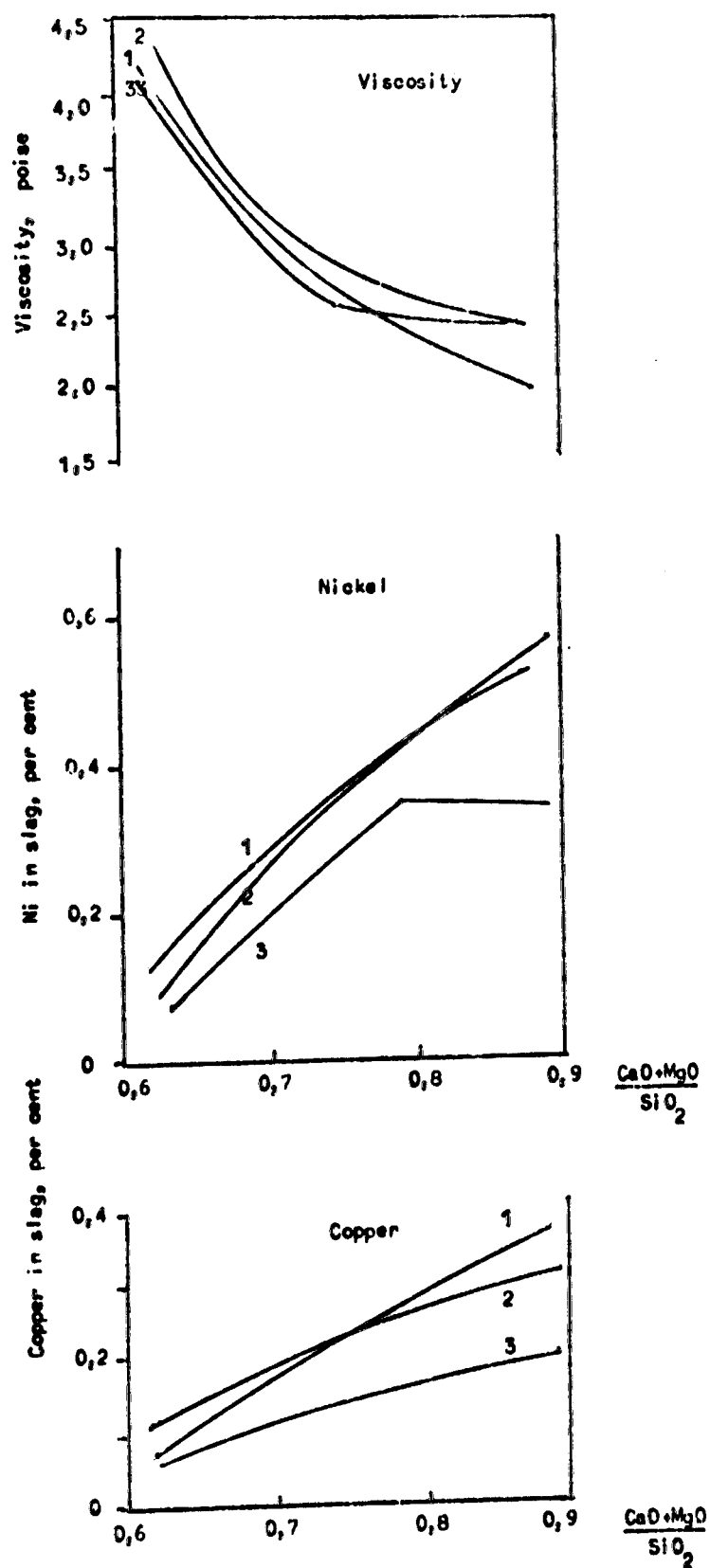
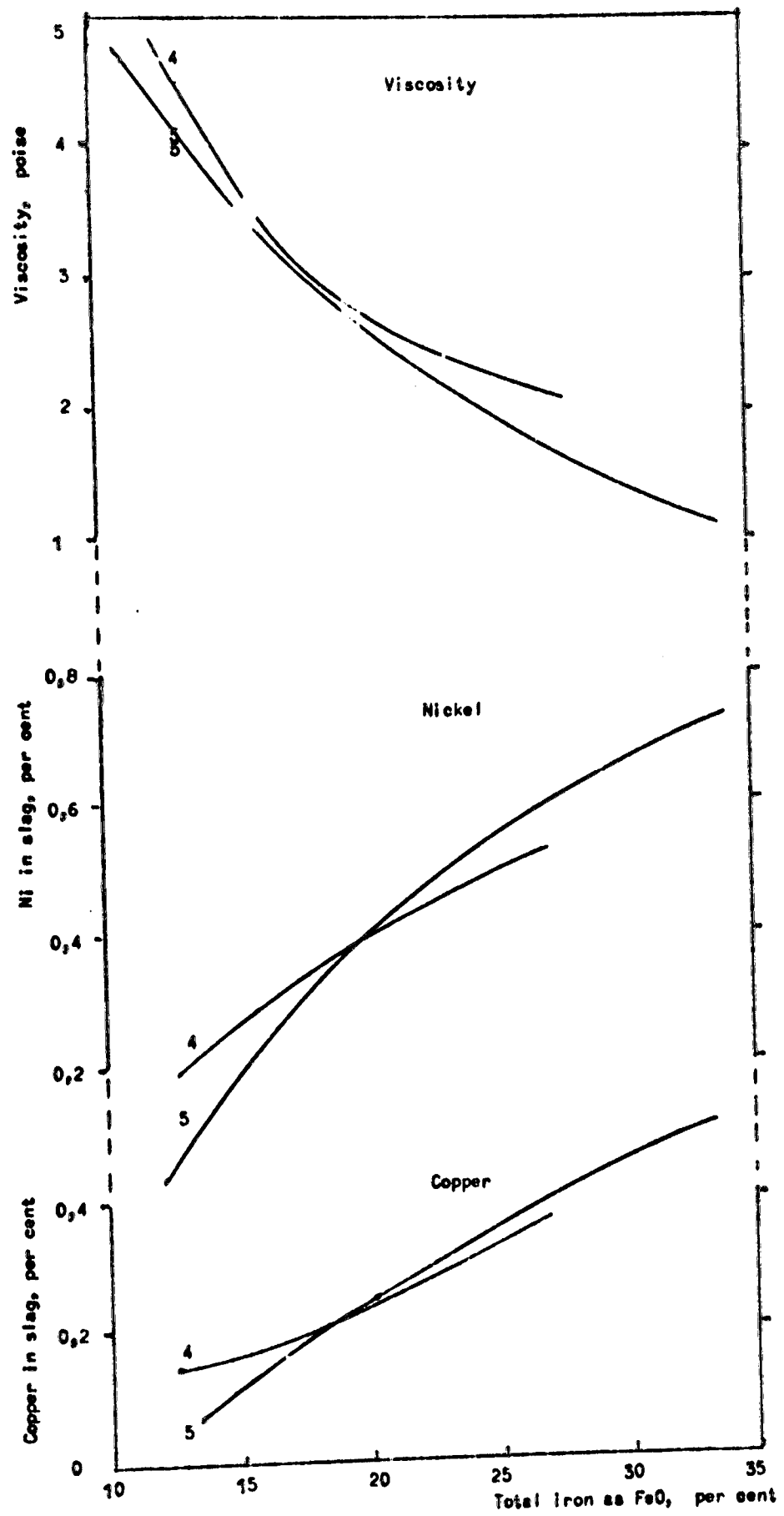


Figure V-7 CHANGE OF Cu and Ni CONTENTS WITH THE CHANGE OF VISCOSITY
AS A FUNCTION OF THE TOTAL IRON CONTENT OF THE SLAG,
(“A” series of tests), Slag Groups IV and V



The striking feature of the results is the increased loss of the metals as compared with their actual dissolution in working slags in the plant, and the rate of increase of the metal loss with a relatively small change in lime and magnesia contents of the synthetic slags. This applies to a certain degree also to the effect of the change of the iron content. However, literature data on the effect of slag composition upon Ni losses in the slag seem to substantiate these findings. In the work conducted by Volskiy et al. (11) on similar quinary systems a slag containing originally 40,8% SiO_2 , 15,6% FeO , 26,3% CaO was used. An increase of the CaO content to 30% and the decrease of the FeO to 12-13% reduced the nickel losses from 0,2 to 0,05%. A combined effect of basicity and FeO was demonstrated by reducing the CaO to 20% with the increase of SiO_2 to 45% or, reducing the SiO_2 to 38% and increasing the FeO above 16%. The result was an increase of nickel losses to the slag from 0,2 to 0,35-0,50%. The most favourable slag composition was found to be: 41-43% SiO_2 , 13-15% FeO , 26-28% CaO , 8-10% MgO and 5-6% Al_2O_3 .

It has to be pointed out that, although the trend of metal losses with the change of the various slag-forming constituents compares well with that established in the present study, a quantitative comparison is hampered by the lack of certain informations in the Russian paper, such as type of atmosphere above the slag, test duration, oxidation grade of slag, etc.

That the significant increase in metal dissolution is in no way the result of viscosity effects only, becomes immediately apparent when the change in loss values is plotted together with the viscosities on the same graph as functions of the slag composition. (figures V-6 and 7.). These graphs clearly indicate that while the loss of copper and nickel increases with the increase of the relevant slag-forming constituents in all five slag groups, the viscosity follows always the opposite trend. It is therefore obvious that the rise in metal losses is not due primarily to physical reasons i.e. - entrapment of matte particles brought about by viscosity effects.

* * *

B series. The behaviour of slags of low oxygen potential.

Test series 2 and 3, called together B series, were performed with a view to simulate conditions somewhat closer to those existing in operating furnaces with low partial pressure of oxygen in the gas phase above the melt. As the test results have shown, there was, apart from a few cases, very little difference in the extent of copper and nickel losses in the two series. Thus the addition of metallic iron to the charge in order to reduce further the oxidation state/...

Figure V -3 B

RELATIONSHIP BETWEEN BASICITY
AND METAL VALUES IN THE SLAG

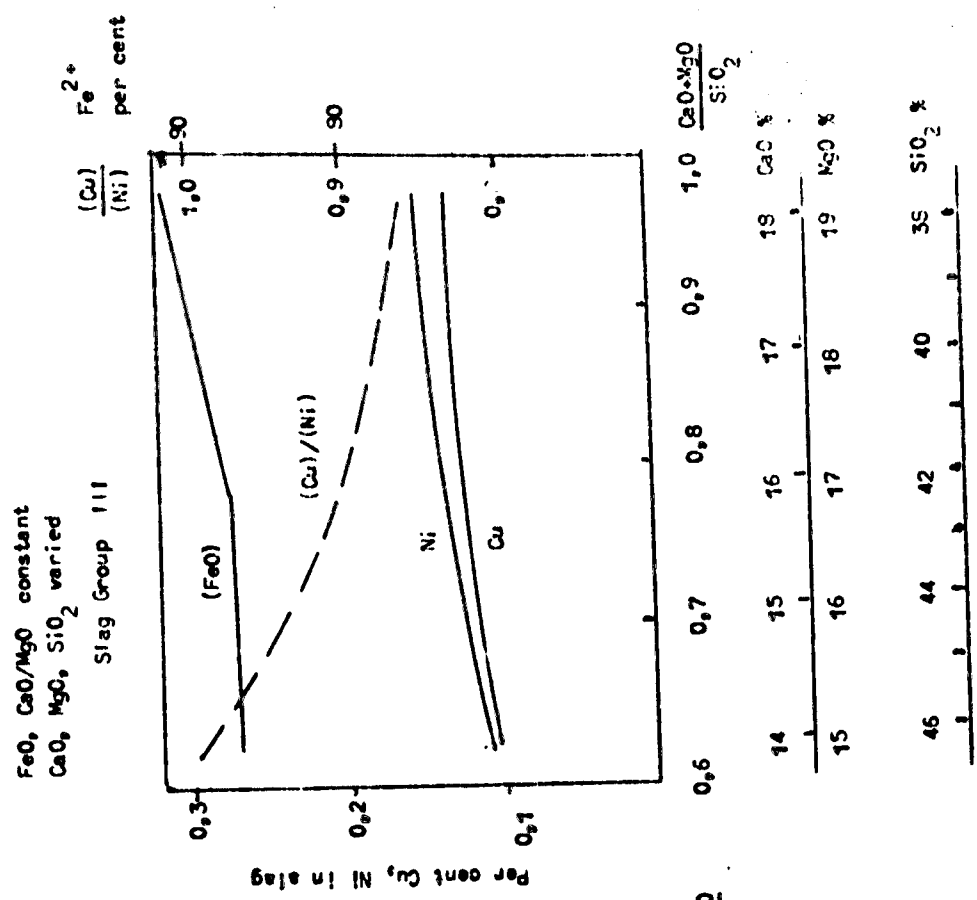


Figure V -2 B

RELATIONSHIP BETWEEN BASICITY
AND METAL VALUES IN THE SLAG

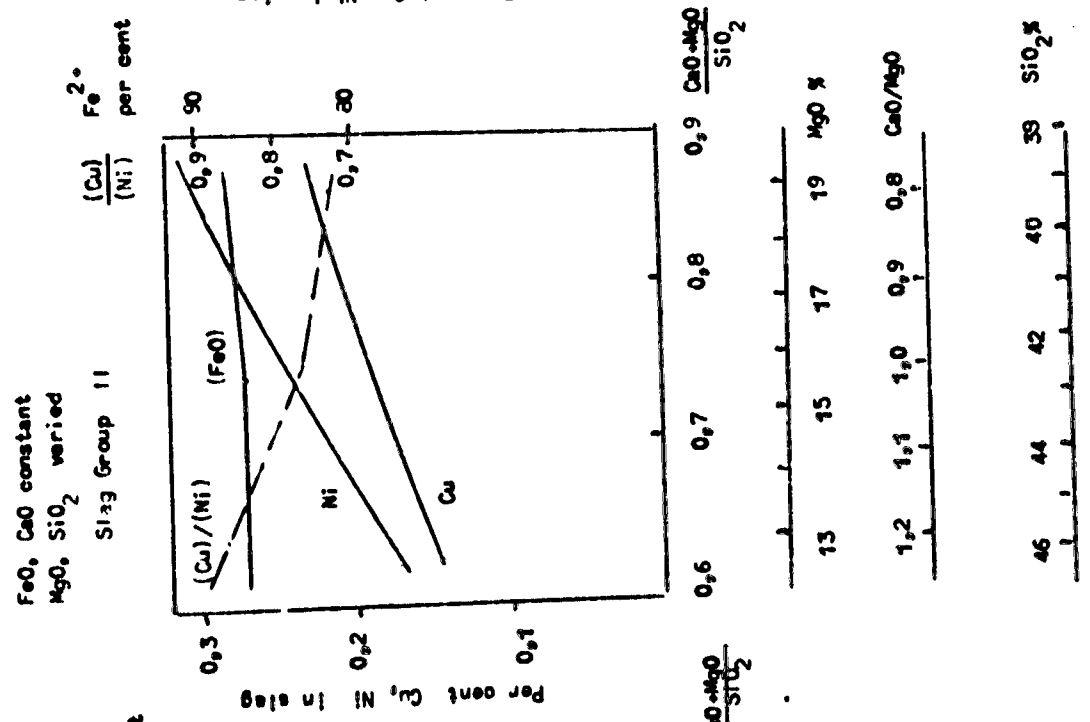


Figure V -1 B

EFFECT OF BASICITY ON THE
METAL VALUES IN THE SLAG

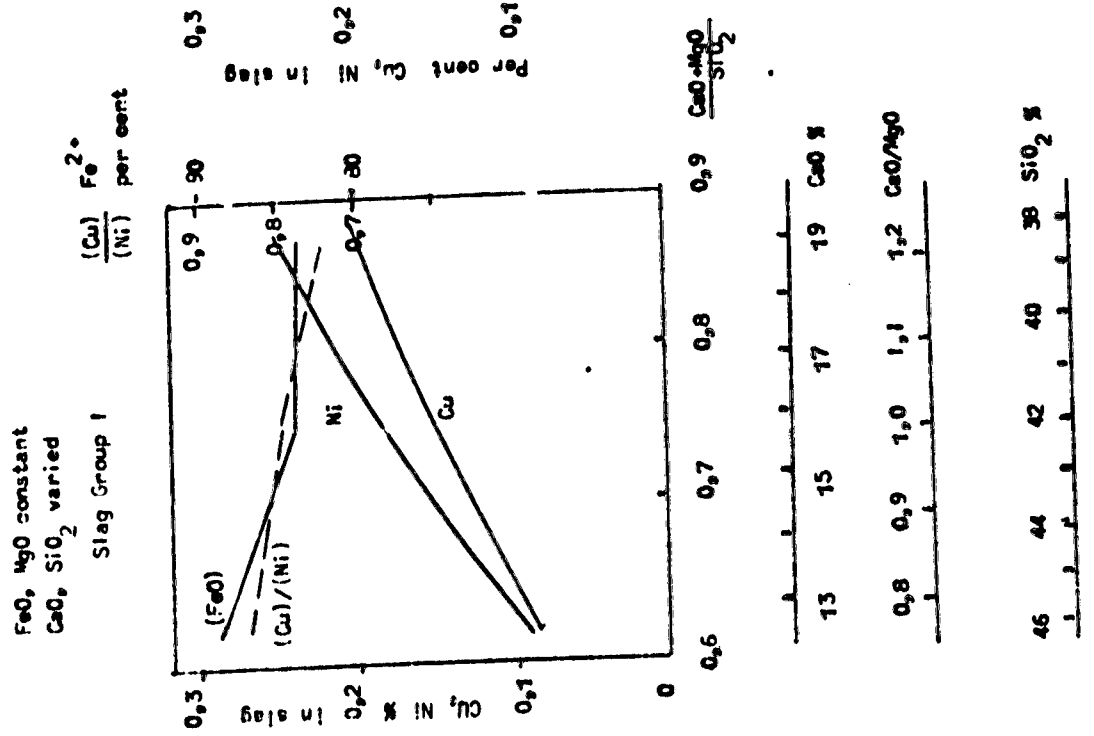


Figure V -4 B

EFFECT OF THE TOTAL IRON CONTENT ON THE DISSOLUTION OF Cu and Ni IN THE SLAG

CaO, SiO₂ varied
9, MgO constant
Slag Group IV
 $\frac{(Cu)}{(Ni)}$ Fe²⁺
per cent

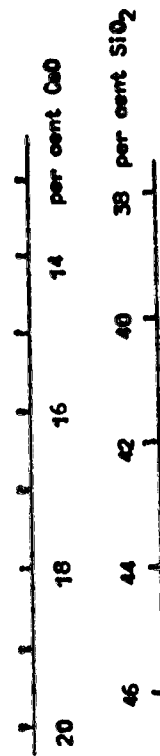
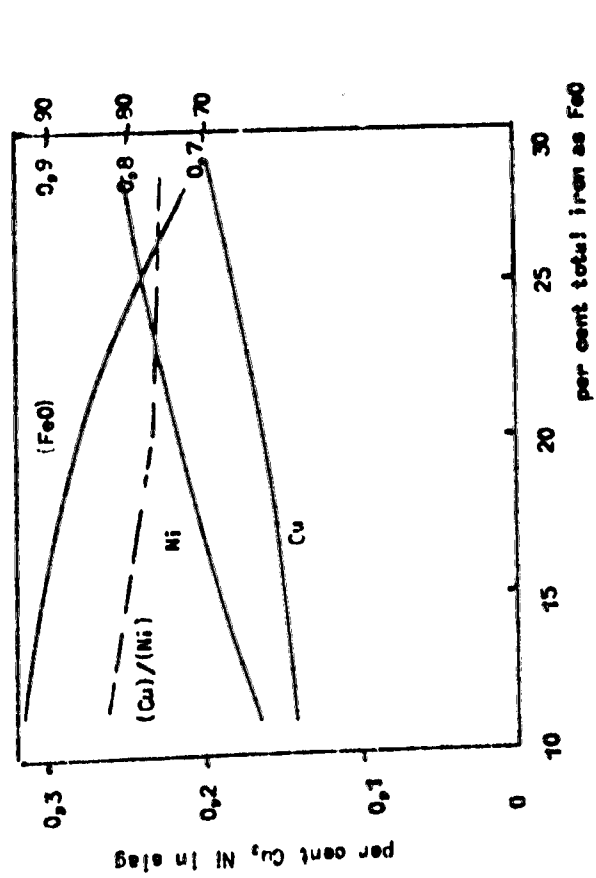
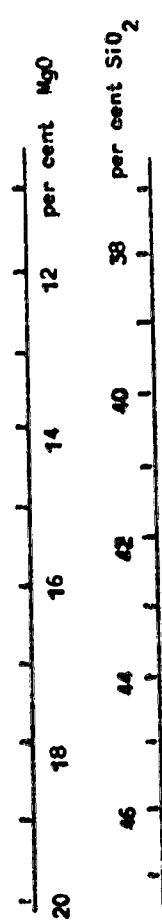
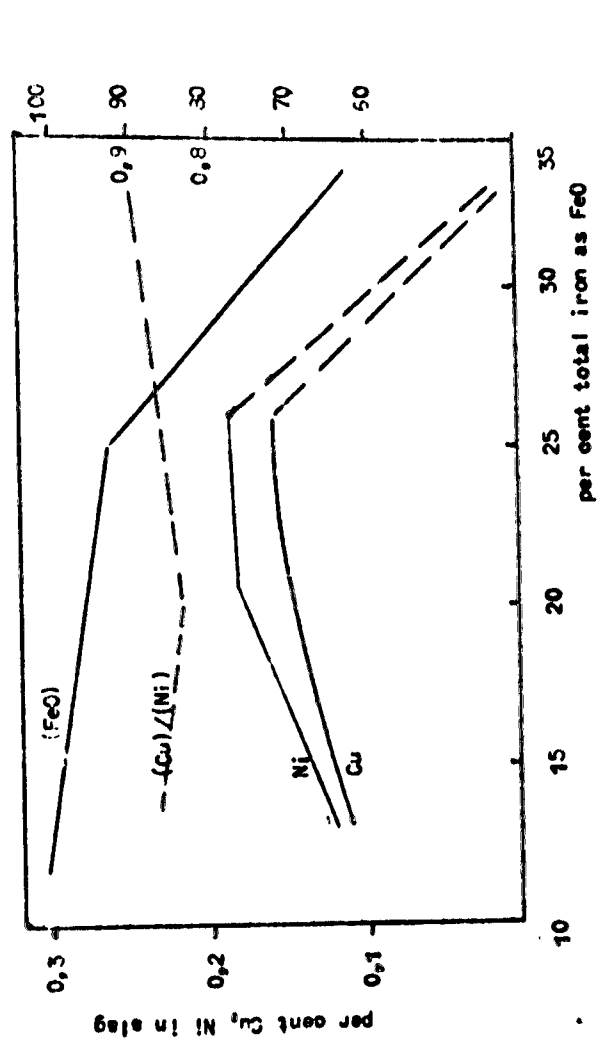


Figure V -5 B

EFFECT OF THE TOTAL IRON CONTENT ON THE DISSOLUTION OF Cu and Ni IN THE SLAG

MgO, SiO₂ varied
CaO constant
Slag Group V
 $\frac{(Cu)}{(Ni)}$ Fe²⁺
per cent



oxidation state of the slag became effective only in that it probably lead to some magnetite formation instead of the pure divalent oxide.

The results are presented in figures 1B to 5B in the order of the five slag groups. A comparison with the A series reveals a close similarity between the effects of the various compounds upon the Cu and Ni losses in both types of slag. However it appears that with diminished oxidation potential the change of metal solubility with the change of the concentration of the components becomes considerably less noticeable. A further difference is in the reversed role of CaO and MgO (figures V-2A and 2B), as in the reduced slags MgO appears to be the more effective in increasing metal losses when the total iron content is kept constant in both cases.

Slag Groups IV and V in both series would indicate that the increase of the total iron content would encourage metal dissolution despite the decrease in basicity ratios, CaO and MgO contents, the MgO being more effective in suppressing the activity of the FeO. A striking feature in slag Group V is that irrespective of the oxidation grade of the slag, in case of high (26%) total iron contents the metal loss drops very sharply (figures V-5A and 5B). Some decrease in Cu and Ni solubility could of course be expected by virtue of the drop in basicity ratios and MgO contents in conformity with the behaviour seen in slag Group II. Nevertheless the part played by the increased FeO content could not be overlooked and will be discussed later.

The Fe^{2+} content of the final slags with the exception of two cases was higher than 85 per cent in these test series. Generally, higher iron contents were associated with decreasing $\text{Fe}^{2+}/\text{Fe}^{3+}$ ratios. Thus in slags 10 and 12 (20% total iron as Fe) the Fe^{2+} content was 78 and 75% respectively, whereas in slag No-15 (26% total iron) the value dropped to 65%

On the other hand, the increase of the divalent iron content up to as high as 100% in slags 9 and 11 failed to bring the metal values in synthetic slags anywhere close to the level prevailing in working electric furnace slags. On an average the laboratory tests indicated about 30 per cent higher metallic losses than those observed in practical plant operation.

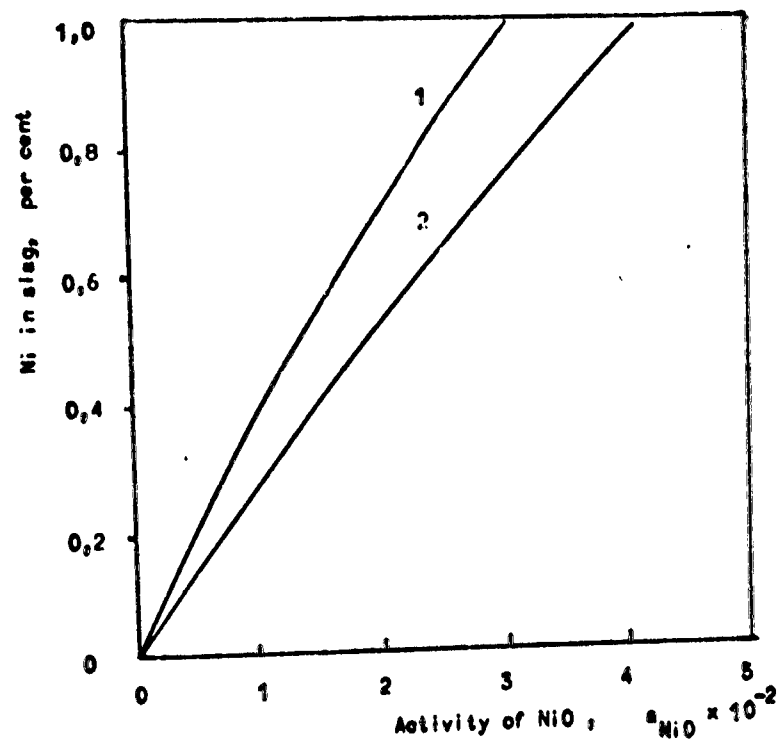
* * * *

The results obtained on copper-nickel solubilities with slags of high oxygen potential in test series A would correspond by and large with those, one could expect in commercial reverberatory slags under oxidising conditions. Altman and Kellogg (1) have found that the oxidation state of the slag in/...

Figure V -8

SOLUBILITY OF NI IN FAYALITE SLAGS AS A FUNCTION OF THE ACTIVITY OF NIO

- 1.) Nagamori, 1300°C, Fe/SiO₂ constant, (1,51, 1,97)
- 2.) Santander-Toguri, 1350°C, Fe/SiO₂ variable



the slag in copper smelting is much closer to magnetite than to iron saturation. Therefore it is useful to know the activity of magnetite and its possible effects in the system. Experiment conducted by these authors showed that primary magnetite crystals can contain significant quantities of dissolved copper. Pertaining to the effect of magnetite on the dissolution of nickel in fayalite-type slags Nagamori (7) has established that the solubility of nickel is very sensitive to the magnetite content of the slag. While for example the magnetite content increased from 6,8 to 12,9 per cent, the nickel content increased almost ten times from 0,43 to 3,97 per cent. On the other hand the solubility of NiO in the slag depends both on the silica and magnetite contents.

The solubility of nickel in silica-saturated fayalite slags was measured by Santander and Toguri (12) in experiments conducted at 1250° and 1350°C. For the relationship between Ni solubility and activity they formulated the following expression:

$$\%Ni = 9,6 a_{NiO}^{2/3} \quad 1./$$

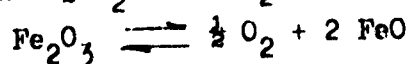
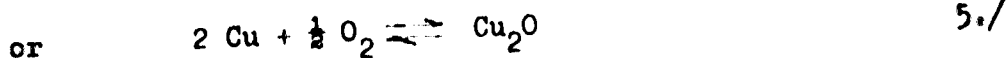
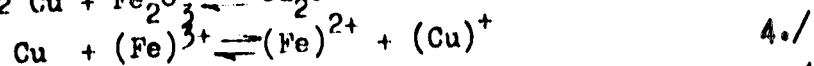
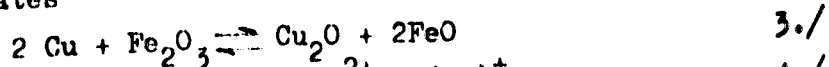
As a good approximation the equation can also be used for silica unsaturated slags. From detailed investigations related to 1200° and 1300°C Nagamori arrived at the formula

$$\%Ni = a_{NiO} \left[(53,6 - 18,0 Fe/SiO_2) + (-20,9 + 18,0 Fe/SiO_2)t + a_{NiO}^2 (-371,6 + 204,0 Fe/SiO_2) + (356,8 - 204,0 Fe/SiO_2)t \right]$$

$$\text{here } t = (T^\circ(K) - 1473)/100 \quad \text{and } 1,4 < Fe/SiO_2 < 2,1 \quad 2./$$

Fe/SiO_2 = ratio of the two components in the slag.

As calculations indicated, the solubility becomes less and less dependent on the iron-silica ratio as the temperature increases above 1250°C. The relationship between NiO activity and per cent Ni dissolved in the slag based on the above expressions is depicted in figure V-8. As will be obvious from the graph, the solubility of Ni approaches zero when the activity of the oxide approaches zero from which Nagamori concluded that nickel can be dissolved by the slag only after it has been oxidised. Incidentally by the argument of Bailey and Garner (4, 13) the same principle holds true for the dissolution of copper. Therefore the reaction suggested by Ruddle, Taylor and Bates



do not stand/...

Figure V-9 SOLUBILITY OF NICKEL IN FAYALITE SLAG
AT 1300°C, AS A FUNCTION OF THE ACTIVITIES
OF FeO and Fe₃O₄. (after Nagamori)

$$\frac{\% \text{ Cu}}{\% \text{ Ni}} (\text{matte}) = 0.45$$

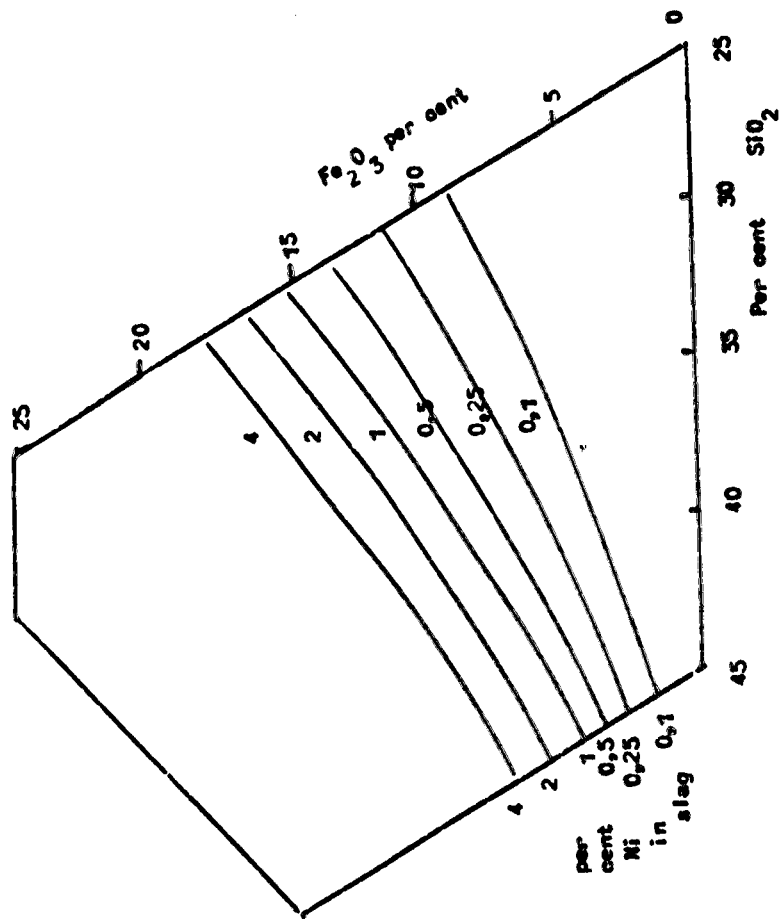
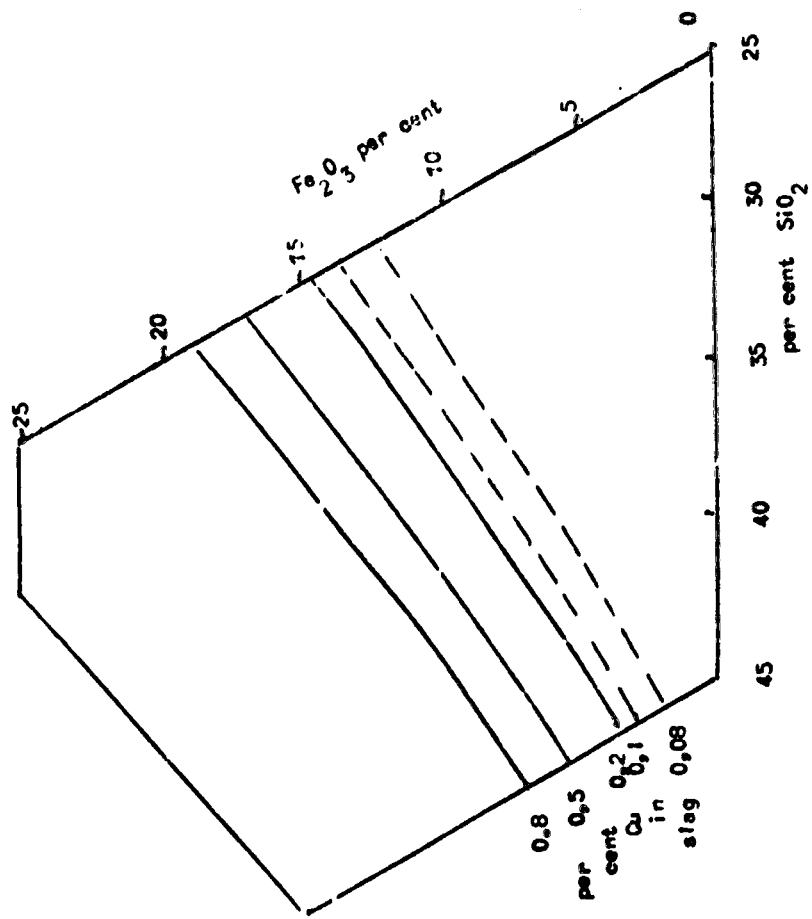


Figure V-10.

SOLUBILITY OF COPPER IN FAYALITE SLAG at 1300°C
(after Nagamori)
Extrapolated values based on the work of Korakas for
activities and solubility limits of FeO and Fe₃O₄

$$\frac{\% \text{ Cu}}{\% \text{ Ni}} (\text{matte}) = 0.45$$



do not stand for the mechanism for copper dissolution but indicate the oxidation of Cu with ferric oxide supplying the necessary oxygen. Therefore they represent the oxidation of copper prior to dissolution.

The effect of the silica and magnetite contents of a fayalite slag on the copper and nickel losses are shown in figures V-9 and 10. The data were obtained from the work of Nagamori based on the solubility limits and the activities of FeO and Fe_3O_4 of Korakas (13), extrapolated to higher SiO_2 and lower NiO contents in conformity with the concentration ranges handled in the present study. The combined effect of SiO_2 and magnetite on metal losses is obvious from the graphs. Taking for example the 0,1% NiO level in the slag (graph V-9) the increase in SiO_2 say up to 35 per cent will not affect the nickel dissolution as long as the magnetite content remains unchanged. If at the 35% SiO_2 level the magnetite content were increased from 12 to say 20 per cent, this would raise the nickel content of the slag from 0,1% to 2%. Thus the effect of the silica on both copper and nickel losses can be correctly defined only when the activity of magnetite is specified.

The graphs relate to fayalite slags and, therefore, to conditions where the presence of other slag-forming constituents, like CaO , MgO and Al_2O_3 do not exert any influence on the metal losses. In this way they do not fully represent relationships prevailing in electric furnace slags. When investigating the dissolution of copper in fayalite slags for a given matte grade Nagamori has found that the experimental data were about 2,5 times higher than those for commercial slags under oxidising conditions. Nevertheless the graphs give an indication both on a qualitative and quantitative basis of the trend of the changes that can be expected also in reverberatory or electric furnace smelting where the combined effect of the silica and magnetite content of the slag is an equally important factor in governing metal dissolution.

Although, due to the relative constancy of the grade of matte produced in the electric furnaces examined, changes in the matte grade were of no immediate concern to the present study. Its role, however is extremely important in metal losses and should briefly be discussed here. Even more so since Themelis and Spira (6) did not find any relationship, as against the previously discussed observations, between magnetite content and dissolved copper in the slag. Instead, they noticed that under industrial smelting conditions the dissolved copper increased with the magnetite content of the matte. The maximum solubility of oxygen in matte is at the highest oxygen potential at which the slag-matte system can exist, this being controlled by the reaction/...

Figure V-11

COPPER DISSOLUTION IN SLAG AS A FUNCTION
OF MATTE GRADE AND OXYGEN CONTENT OF MATTE

After Spire and Themelis

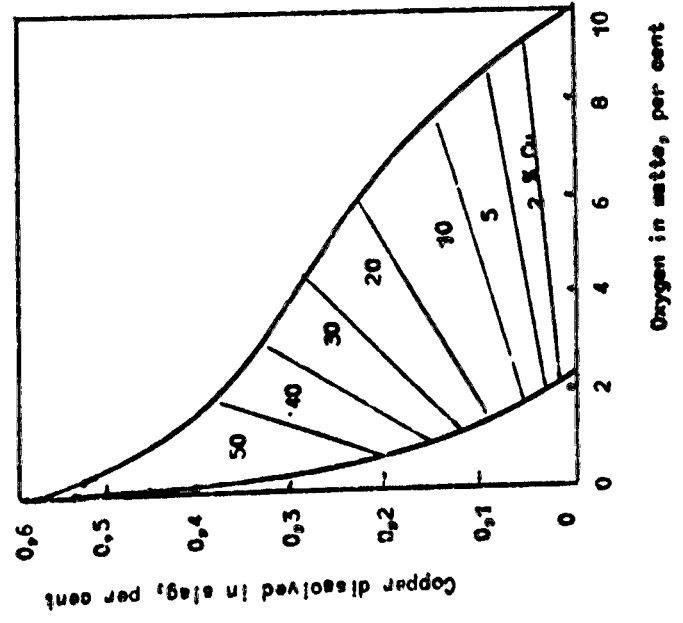


Figure V-11/a

DISSOLVED AND SUSPENDED COPPER CONTENT OF REGENERATORY
SLAGS. After Spire and Themelis

○ dissolved
△ suspended

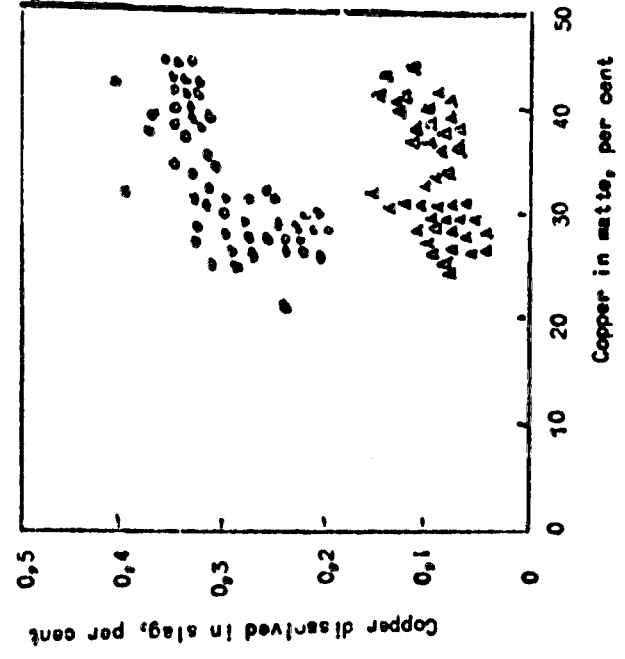


Figure V-11.

SULPHIDIC AND OXIDIC DISSOLUTION OF COPPER
IN COMMERCIAL REVERBERATORY SLAG AT 1300°C
AS A FUNCTION OF MATTE GRADE. (LOW GRADE MATTE)

After Nagamori
1./ Total dissolution
2./ sulphidic dissolution
3./ oxide dissolution
Dotted line by
Toguri and Santander

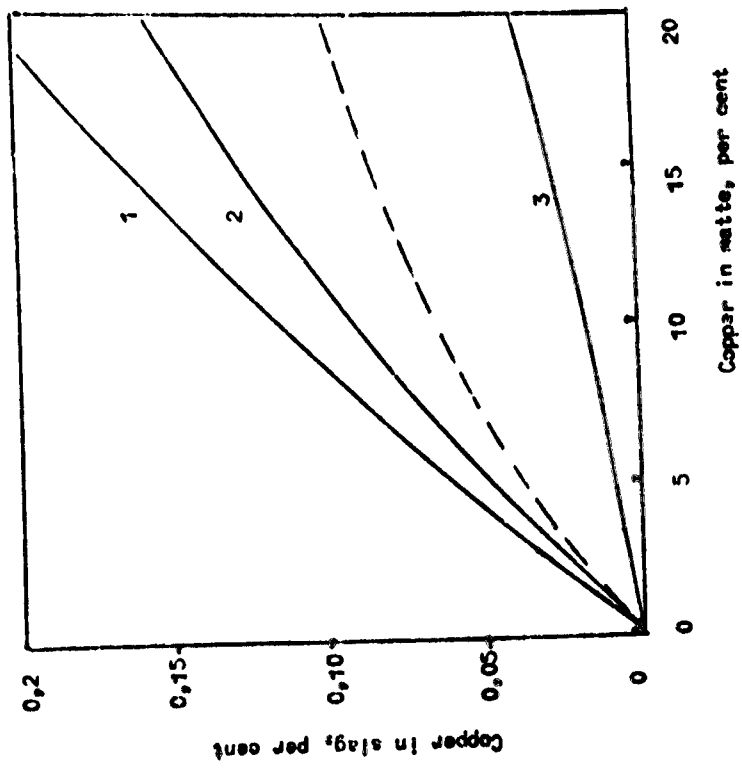
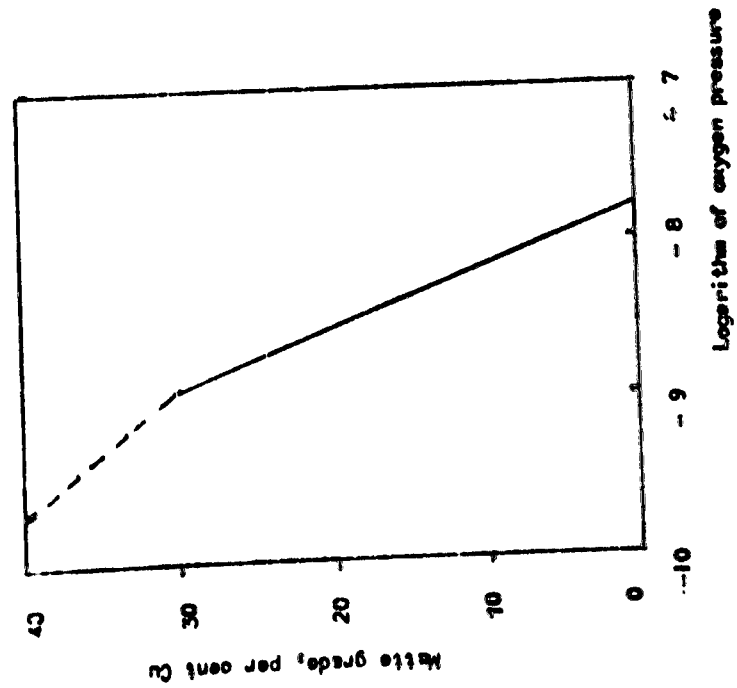


Figure V-12.

EQUILIBRIUM IN THE MATTE - SLAG SYSTEM
SHOWING LOW-GRADE MATTE PORTION OF DIAGRAM.

After Brown



the reaction $\text{FeS} + 3 \text{Fe}_3\text{O}_4 \rightleftharpoons 10 \text{FeO} + \text{SO}_2$ 7./

The matte grade increases as the activity of the magnetite decreases in accord with Ruddle and co-workers. The solubility of copper as a function of matte grade and oxygen content of matte after Spira and Themelis is given in fig. V-11. High oxygen pressures therefore are not conducive to reducing the equilibrium copper content of the slag and these oxygen pressures lead to the break-down of matte phase that is to the prevalence of non-equilibrium conditions. From an equilibrium point of view the higher the grade of matte to be smelted the lower the oxygen partial pressure should be to prevent a break-down of matte and, consequently, the occurrence of high metal losses. In this context relevant to the present study is the work of Brown and associates (14, 15) in slag-matte relationship, although their investigations were concerned also with silica-saturated iron silicate slags resulting from converter operation. It was found that for any particular matte grade there was an oxygen partial pressure below which the matte was stable but above which the matte broke down with the evolution of sulphur dioxide and the precipitation of a sulphur-rich phase. The results are summarised in figure V-12 indicating the minimum oxygen pressure for any matte grade needed for conversion to take place.

In case of copper smelting, with high grade mattes the Cu content of silica-saturated slags varied linearly with the matte grade up to about 60 per cent Cu in the matte (15). Concerning this point Japanese investigators developed an equation related to 1300°C according to which

$$\text{Per cent Cu in slag} = 0,00533 \times (\% \text{Cu in matte}) \quad 8./$$

With regard to lower grade mattes the data supplied by Nagamori (7) can be regarded as conclusive, especially because they relate to commercial reverb. slags. These are given in figure 13. The range of copper content in the electric furnace matte at RPM may vary between 9 and 12 per cent which, according to the graph would correspond to an equilibrium copper percentage of 0,10 to 0,14% in the slag (total dissolution). Practical operating data tend to support these findings based on experimental evidence.

Figure V-14 shows the activities of FeS , FeO , $\text{CuO}_{0,5}$ and CuS in iron and silica-saturated matte. Then with these data the per cent copper in sulphidic dissolution can be expressed by

$$(\% \text{Cu}) = (0,39\% \text{S}) a_{\text{CuS}_{0,5}} \quad 9./$$

$$\text{or} \quad (\% \text{Cu}) = (0,69\% \text{S}) a_{\text{Cu}_2\text{S}} \quad 10./$$

The sulphur content/...

Figure V -15 SULPHUR IN COMMERCIAL REVERB SLAGS vs MATTE GRADE

Copper content of slag as a function of matte grade.:

After Nagasawa: • 1 experimental data
 2 commercial plant data

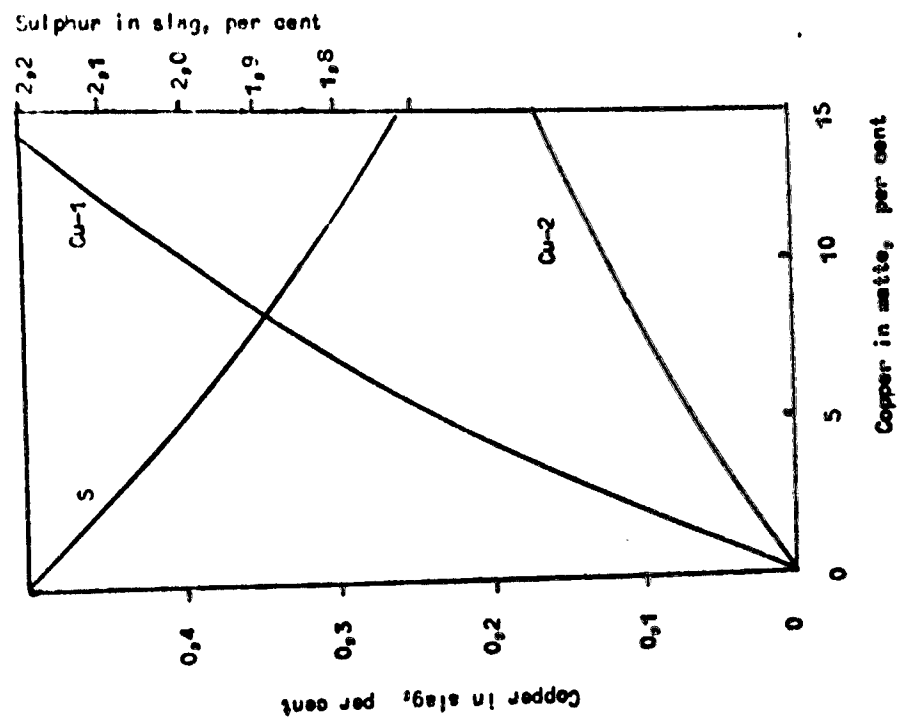
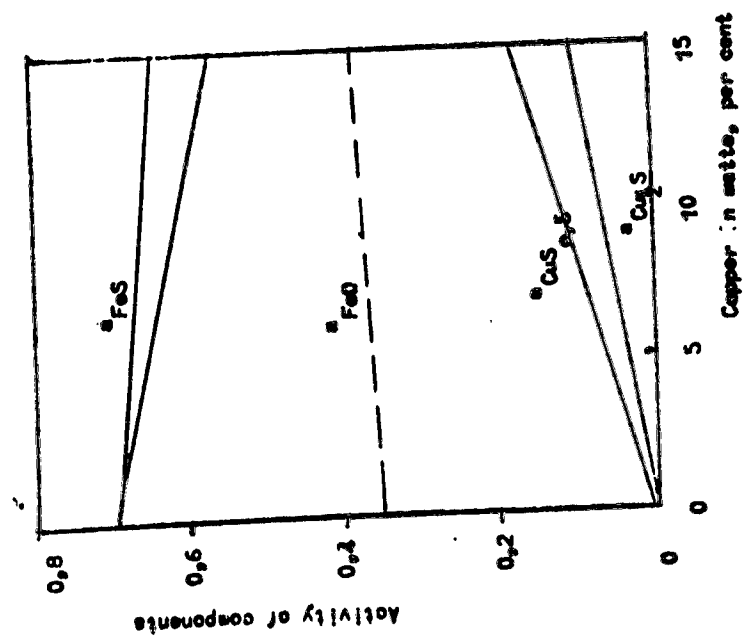


Figure V -14.

ACTIVITIES OF FeS and CuS IN IRON
AND SILICA SATURATED MATTE



The sulphur content of commercial reverberatory slags as a function of matte grade from the data of various plants representing low-grade matte is shown in figure V-15. The equation of the curve may be expressed for the sulphur content of reverb slags as

$$\%S = 2,2 - 3,75X + 1,25X^2 \quad 11./$$

The oxide solubility of copper based on the results of Ruddle et al. (2) and Altman et al. (1) is given by

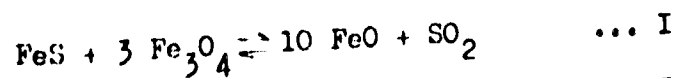
$$(\% Cu) = 35,9 a_{CuO_{0,5}} \quad 12./$$

With these equations for any particular matte the copper loss can be estimated as it is given in figure V-13, where the total dissolved copper is the sum of sulphidic and oxidic dissolution:

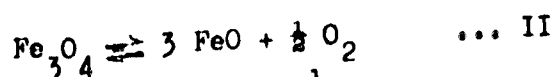
$$(\% Cu_{tot}) = (\% Cu)_S + (\% Cu)_{ox} \quad 13./$$

Involved in this line of thinking is the practical observation that the magnetite content in commercial reverberatory slags does not exceed 5 to 10 per cent. In electric furnace slags the magnetite level is even lower. Since by the oxidic-sulphidic dissolution concept of Nagamori half of the copper dissolved in the slag from lower grade mattes is due to sulphidic dissolution, in order to reduce copper loss especially in case of these lower grade mattes, the sulphur solubility in the slag must be lowered. Similarly, lower oxygen potentials would be advantageous to keep the oxide losses low. Under average electric smelting conditions, however, the sulphidic dissolution, or solubility of sulphur in slag appears to be the most important factor governing the metal losses.

The partial pressure of SO_2 in the furnace gases that could be expected from slag-matte reactions may be estimated as a function of matte grade with the data of figure V-14 and from the reactions



$$K_1 = (a_{FeO}^{10} \cdot p_{SO_2}) / (a_{FeS} \cdot a_{Fe_3O_4}^3)$$



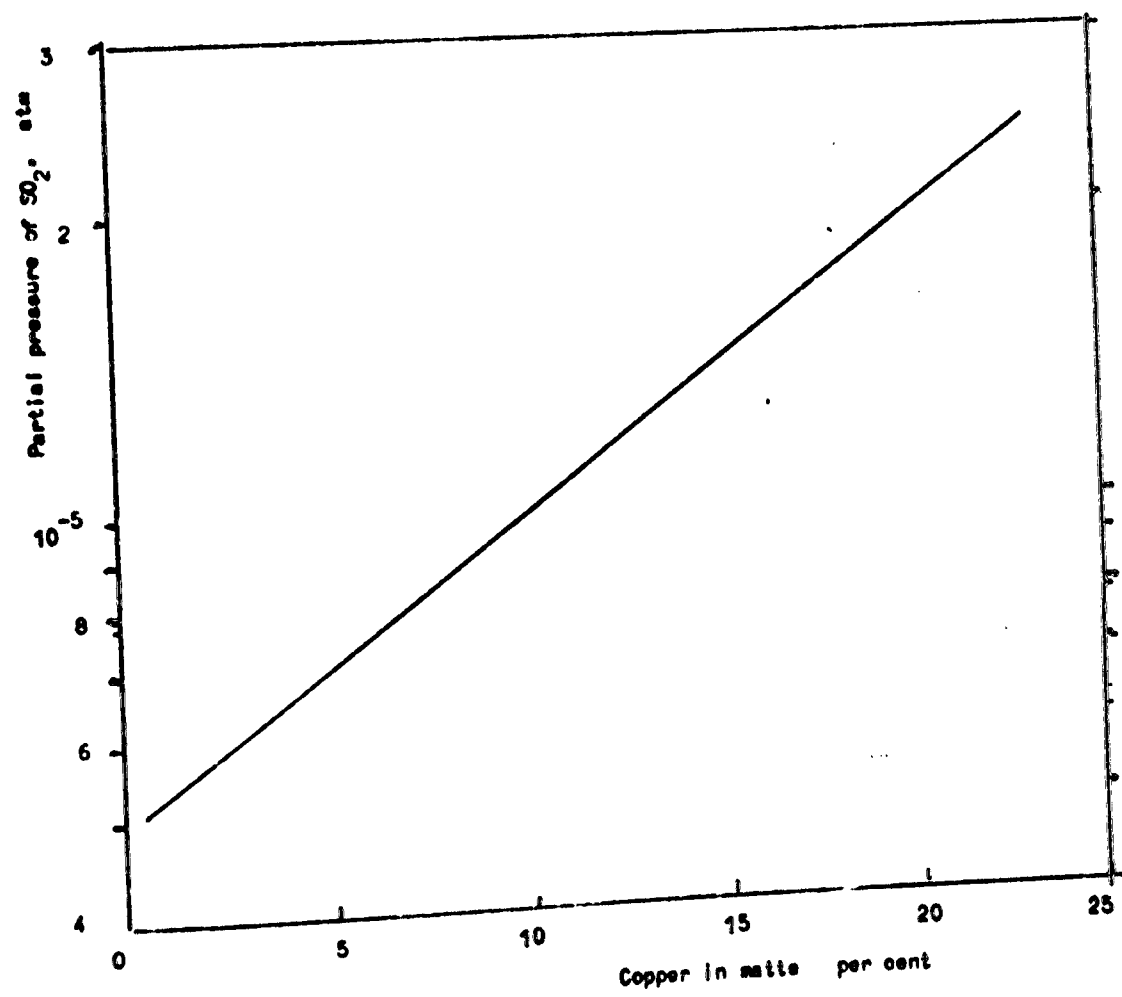
and

$$K_2 = (a_{FeO}^3 \cdot p_{O_2}^{1/2}) / (a_{Fe_3O_4})$$

The equilibrium constants of the formation of the various compounds in the above reactions is found in relevant texts (see e.g.: Barin I - Knacke O : Thermochemical Properties of Inorganic Substances, Springer, Berlin, 1973), then in the knowledge of the constants K_1 and K_2 eventually p_{SO_2} will be given as shown/...

Figure V -16.

PARTIAL PRESSURE OF SO_2 DUE TO SLAG - MATTE REACTION AS A
FUNCTION OF MATTE GRADE

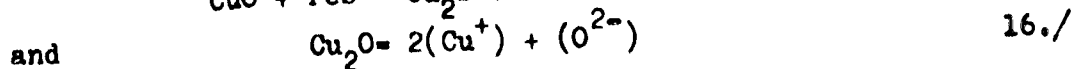


given as shown in figure V-16 for matte grades from 0 to 20% Cu at 1400°C. These are very low values indicating that the sulphur content of the furnace gases is in no way the result of slag-matte reactions. Rather, the P_{SO_2} part of the total pressure of the gases (these reach on an average 2 to 4 per cent) is due to the oxidation of concentrate above the molten slag-matte bath.

As Nagamori himself noted, the distinction between sulphidic and oxidic metal dissolution is only imaginary because the copper in the slag is assumed to be in the form of copper ions in both cases. The concept was introduced solely with the aim to facilitate the understanding of metal losses to the slag. A similar concept was advanced by Sehnalek and Imris (5) who, however, made a true distinction between sulphidic and oxidic losses and derived the expression

$$(\%Cu)_{loss} = A \left[(N_{Cu_2S}) / (1 - N_{Cu_2S}) + B(N_{Cu_2S}) \right] \quad 14./$$

where Cu_2S stands for the copper tenor of the matte and $A = 0,046$ and $B = 0,3368$ at 1400°C. The first term of the expression represents copper losses arising from Cu_2S solubility which, at constant temperature and silica saturation is only a function of matte grade and FeO activity in the slag. The second term represents Cu lost to the slag due to Cu_2S dissolution which, under given conditions, depends on the matte grade only. The idea of simultaneous dissolution as sulphide and oxide became strongly debated and as a modified expression suggested by Da.ey (19) could probably be regarded as a more general approach based on one and not two separate species, that is either Cu_2O or Cu_2S . Then with the equilibrium constants of the reactions



$$K_1 = \frac{a_{FeO} \cdot a_{Cu_2S}}{a_{Cu_2O} \cdot a_{FeS}} \quad 17./$$

$$K_2 = \frac{a_{Cu^+}^2 \cdot a_{O^{2-}}}{a_{Cu_2O}} \quad 18./$$

$$where \quad a_{Cu_2O} = K_2 (\%Cu)^2 \quad 19./$$

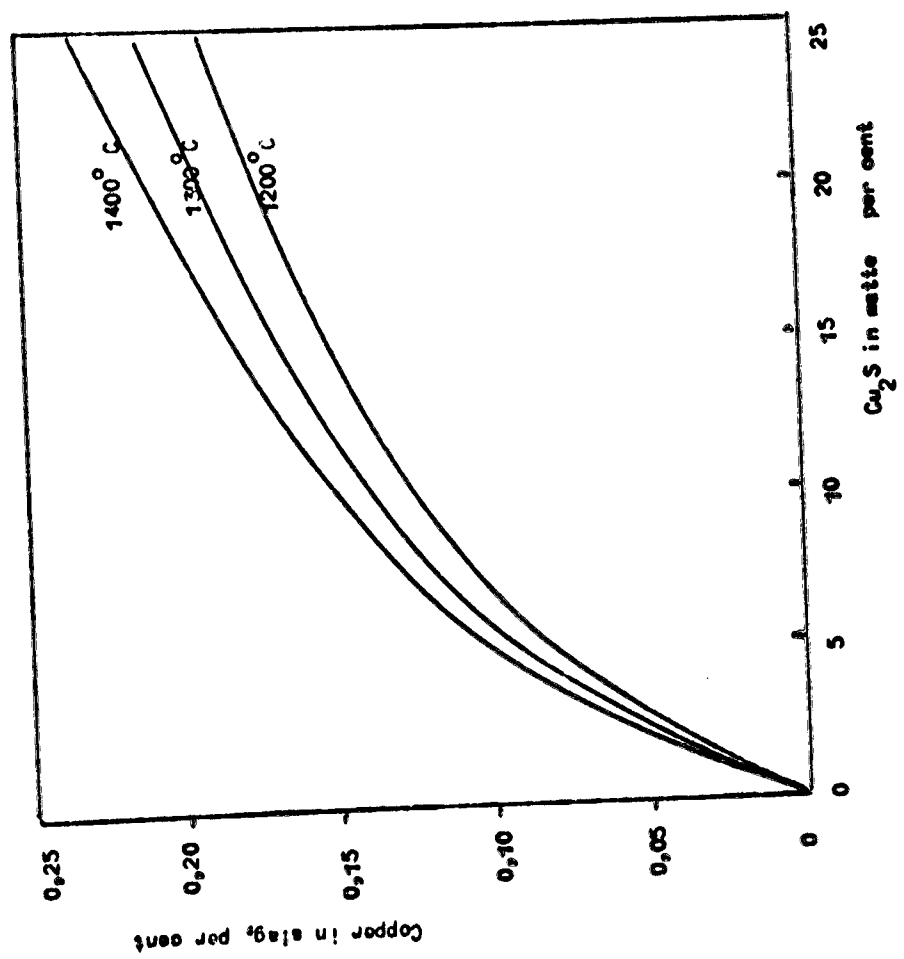
$$Then \ the \ per \ cent \ copper \ in \ the \ slag \ is \quad (\%Cu)_{sl} = A \sqrt{\frac{N_{Cu_2S}}{1 - N_{Cu_2S}}} \quad 20./$$

$$and \quad A = \sqrt{\frac{a_{FeO}}{K_1} \cdot \frac{1}{K_2}} \quad 21./$$

With the data/...

Figure V-17 THE EFFECT OF MATTE GRADE ON THE DISSOLUTION OF COPPER
IN SILICA-SATURATED SLAGS .

After Schmaltek and Ivaris



With the data supplied by Sehnalek and Imris the copper dissolution at three different temperatures was calculated as shown in graph V-17. The difference between the results obtained by expressions V-14 and V-20 is not significant.

A comparison of figure V-17 (equ 20) with figure V-13 (equation 12 of Nagamori) reveals a very good agreement between the two sets of results. For example in case of matte grade produced at RPM with copper content of 9 to 11 per cent the estimated copper losses at 1300°C would be 0,14 - 0,16 and 0,12 - 0,14 per cent respectively.

The activity of FeO in silica-saturated fayalite slags was found as $a_{\text{FeO}} = 0,39$ by Toguri and Santander (3). With this the activity of CuO and the copper dissolution could be calculated for various matte grades as shown by the dotted line in fig. V-13. Under the operating conditions of the reference electric furnaces with the high 40-42% SiO_2 contents of the slags, the produced slags are probably close to silica saturation while the magnetite content is apparently lower than that required for magnetite saturation. Therefore the results of investigation on silica and magnetite - saturated converter slags cannot be applied to them without due regard to the effect of the sizeable quantities of other important slag-forming constituents like CaO and MgO . Nevertheless, with regard to copper losses the indication is that the copper losses estimated by the method of Santander and Toguri would represent by and large average losses in the electric furnace slags at RPM. On the other hand, the results of the solubility tests performed with slags of high oxygen potential would correspond to those that can be estimated by the relationships deduced by Nagamori and also by Sehnalek and Imris. Furthermore, calculations performed with the equation proposed by Santander and Toguri would suggest that the soluble copper represents approximately 50 per cent of the total losses, the rest being due to entrainment losses.

With regard to reverberatory smelting it was also found that in a slag containing about 0,4 % copper half of the metal was lost because of dissolution (17), while according to Vanyukov and Zaytsev (18) the loss by dissolution amounted only to 20 to 30 per cent, and the rest was due to entrainment losses. (18, 19, 20).

Pertaining to nickel losses the effect of matte grade was studied with the aid of EMF measurements by Vatolin et al. (21). Mattes containing about 10% Ni were associated with 0,05 to 0,1% loss, those with 21,3% Ni content 0,08 to 0,24% Ni losses to the slag at various slag compositions.

The loss by/...

The loss by chemical dissolution was estimated as 30 to 50 % of the total.

In case of lower grade mattes, however, it will have to be pointed out here that the relationships derived from operating practice do not necessarily follow those outlined above on the basis of theoretical considerations. Thus for example when practical results of the equilibrium of the lower grade mattes and slags were examined it was found that no simple relationship existed between the copper tenor of a slag and the grade of the matte with which it was in contact. (22).

2./ Slag composition and slag-matte separation.

Practically, the composition of the converter slag has an important bearing on the electric furnace slag. From the converter the slag is returned molten to the electric furnace and joins the slags formed by direct smelting. The ferrous oxide originating from converter slags is necessary for suitable final slag conditioning. Experience at Rustenburg has indicated that the minimum of ferrous oxide that can be considered for successful furnace operation, i.e. good slag-matte separation, should be about 11 to 12 per cent. (23). This may partly be due to the facts that 1./ with the decrease of the FeO content the liquidus temperature of the slag becomes more sensitive to changes in temperature (see Section II) and 2./ the viscosities become more temperature sensitive, (ibid.). The results obtained in the present work indicate that the incorporation of ferrous oxide from converter slag, that is the increase of FeO, makes the slag more tolerant to composition and temperature changes.

Laboratory test work, results and discussion.

On account of the considerable practical implications of effective slag-matte separation, for basic orientation purposes as regards the effect of FeO and also of basicity ratio, a series of tests were conducted using various slag compositions outside the range of those studied so far. In choosing the new slag compositions the objective was to keep a./ the MgO/CaO and the basicity ratio constant and alter the ferrous oxide content; b./ keep the MgO/CaO ratio and the FeO content of the slag constant and alter the basicity ratio.

The mode of slag preparation was identical to that described earlier. Here too the slags were premelted in platinum crucibles then, after cooling, crushed and mixed with pulverised matte in the ratio of 3 to 1. Heating of the mixtures was effected in the same vertical tube furnace for a/...

Table V - 1. Results of slag-matte separation tests

Test No	S l a g c o m p o s i t i o n					CaO MgO	SiO ₂	FeO	Al ₂ O ₃	CaO+MgO SiO ₂	CaO 1.60	Weight loss on total weight of sample, %	Weight of matte button g	% of total
I. Constant basicity ratios, varying iron oxide contents														
1	22	19	52	2	5	0.79	0.86	1.6	3.25	65				
2	22	19	50	4	5	0.82	0.86	2.0	3.50	70				
3	21	18	49	6	5	0.80	0.85	2.2	3.62	72				
4	20	17	48	9	5	0.77	0.85	2.3	4.00	80				
5	19	16	47	12	5	0.74	0.84	2.4	4.65	93				
6	17	14	40	24	5	0.77	0.82	3.0	4.75	95				
7	17	14	40	28	5	0.77	0.82	3.1	4.75	95				
II. Varying basicity ratios, constant FeO content														
8	30	24	25	16	5	2.10	0.80	2.4	0	0				
9	27	22	30	16	5	1.67	0.81	2.4	2.50	50				
10	25	19	35	16	5	1.30	0.80	2.5	3.85	77				
11	22	17	40	16	5	0.98	0.78	2.6	4.50	90				

furnace for a duration of minimum 2 hours at a temperature of 1400°C . After the completion of the runs the crucibles were placed in cold water and the samples shock-cooled, then dried and their weight loss recorded. Finally the matte butts formed in the process were carefully separated from the cracked slag matrix and weighed. Due to the general character and aim of these investigations the weights were recorded only for two digits after the decimal point. Slag compositions and test results are compiled in Table V - 1. The weight of matte butt here refers only to that representing the coalesced matte found in the bottom of the solidified melt as a distinctly separate, coalesced phase, while the rest of the matte remained dispersed in the slag in well-noticeable smaller globules which were hard to separate from the matrix. From the results it would appear that at the basicity ratios investigated, even at low FeO contents clear separation between slag and matte occurred, but part of the matte, about $1/3$, remained dispersed or suspended without coalescing in the slag. Above 9 per cent FeO content the separation of the two phases rapidly improved. The weight loss of the samples increased with the increase of the FeO content.

These results would indicate that the role of the iron oxide in causing difficulties in phase separation is supported by an other, probably equally important factor in case of the particular slags concerned, namely by the basicity ratio. The indication is that even with FeO contents in the slag that could be regarded as normal from a furnace operation point of view, high basicity ratios with increased CaO and MgO values could lead to conditions at which no separation of the matte from the slag would occur. The reason for this is most likely the rise in slag viscosity and liquidus temperature brought about by the enhanced CaC and MgO and the reduced SiO_2 contents of the slag. Especially in the period of start-up or re-starting the furnaces when conditions with regard to burden composition are still unbalanced, the likelihood for high basicity ratios always prevail while, in the meantime, the FeO might drop to a low level. Therefore the decreased FeO level could easily be regarded as the sole cause for the difficulties encountered in slag - matte separation.

Other test series conducted with the same objective but using different experimental techniques lead to the same conclusions. (24). Furthermore it was found that for effective separation about half of the lime flux could be replaced by silica addition. Practically this means that an increase in the SiO_2 content of the concentrate would be beneficial in reducing the flux and thereby the gangue quantity which would lead to a reduction in slag volume i.e. to a decreased ratio of slag to matte produced. In general/...

In general, the return converter slag assists in lowering the melting point and viscosity of the final discard slag. As mentioned earlier, "soluble" or "non-soluble" copper losses are mainly functions of the oxygen potential of the slag (2, 14, 15), and as a consequence, because of their high FeO content and resulting higher oxygen potential, converter slags are probably the dominant factors in non-sulphide, i.e. chemical or electro-chemical copper losses. If, as in the case of the RHM smelters, the amount of FeO and other iron oxides entering the discard slag is relatively constant, then the chemically lost or dissolved copper should also be a constant factor. See in this instance L.K. Verney's reference to reverberatory furnaces in (25).

II./ Aspects of slag optimisation.

1./ Surface tension and interfacial tension in matte settling: their role in slag optimisation.

Apart from viscosity, the rate of settling could be markedly affected by the interfacial tension between the molten matte and slag. When the interfacial tension is low, the matte is wetted by the molten slag and the settling velocity is reduced. (On the effect of surface activity see ref.26.). The tendency for coalescence should increase with increasing interfacial tension but the ability of the particles to coalesce when in contact, will also depend on their viscosity. The lower the viscosity the better is the tendency to coalesce.

The tendency for particle coalescence is governed also by the physical character of the concentrate to be smelted. In this instance the observation made by Johannsen and Wiese on particle coalescence has already been referred to in the previous section. King (27) stated that a high value of interfacial tension, i.e. the difference between the surface tension of the slag and matte, will cause a stabilising effect on the matte droplets and render them less liable to break up, thus giving a cleaner separation between the two phases at the interface. The highest surface tension in molten silicates accompany the strongest binding and lie between the ionic and covalent type bonding. This property can be calculated by use of surface tension factors of oxides and the ionic potentials of cations ($\sigma = Z/r$ where Z the valence of cation and r is its radius). Thus Boni and Derge (28, 29) using experimental data obtained for the system CaO-FeO- Al_2O_3 - SiO_2 were able to derive an expression for estimating the surface

tension of/...

Figure V-18

VISCOSITY, ELECTRICAL CONDUCTIVITY, SURFACE TENSION
AND SLAG COMPOSITION RELATIONSHIP AT 1400°C

Slag Groups I, II, III

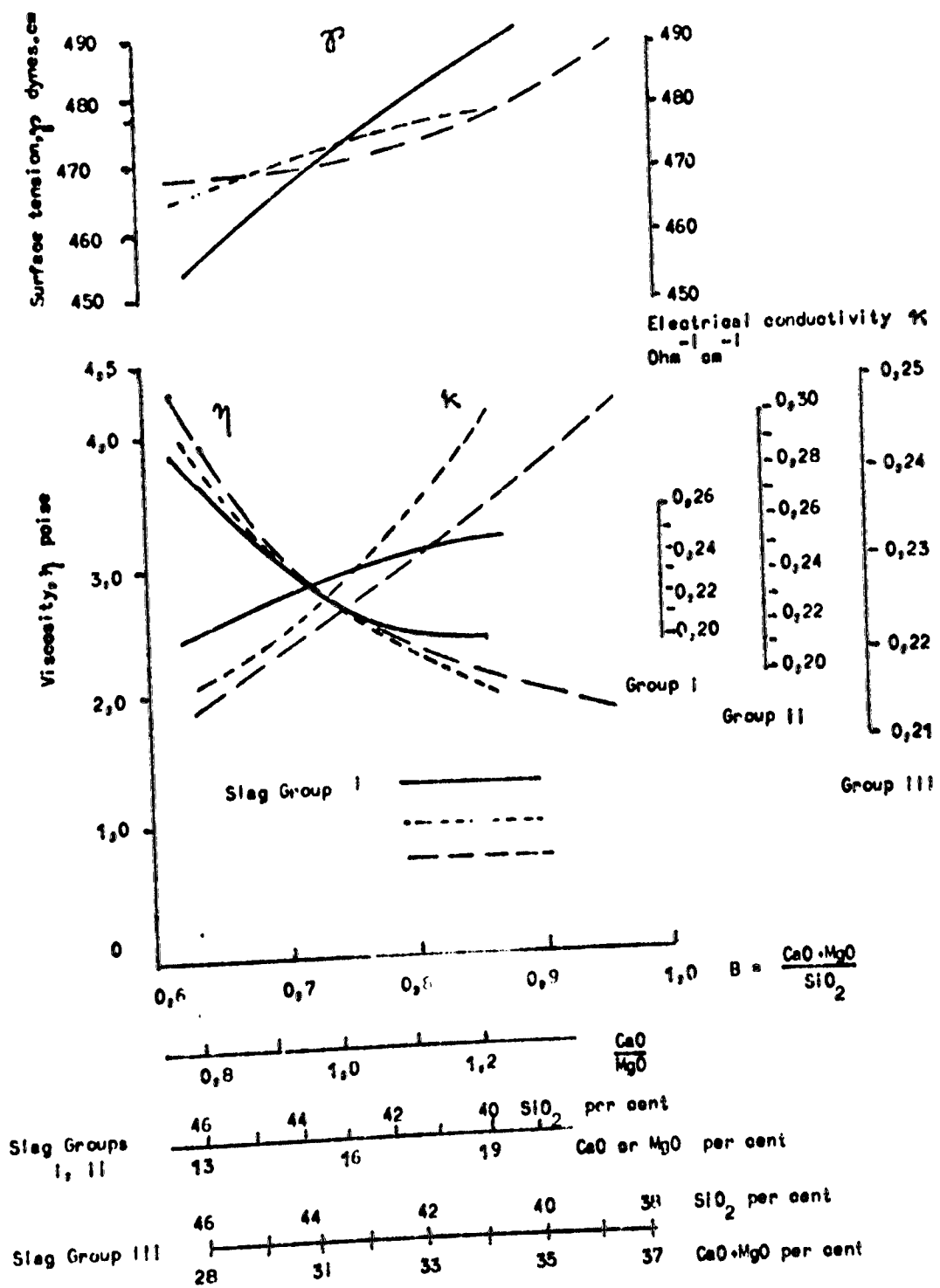
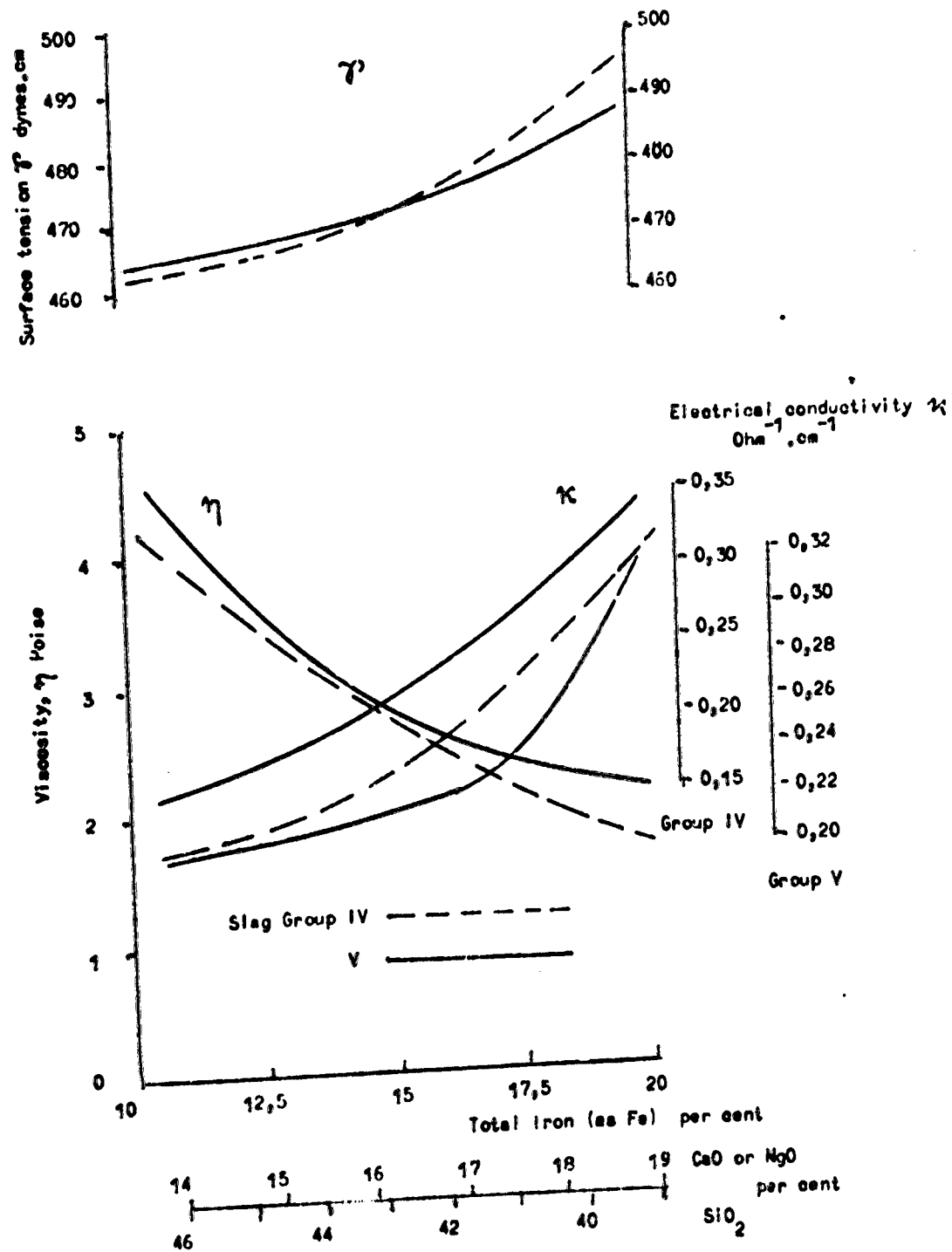


Figure V - 19

VISCOSITY, ELECTRICAL CONDUCTIVITY, SURFACE TENSION
AND SLAG COMPOSITION RELATIONSHIP AT 1400°C

Slag Groups IV, V.



tension of various slag-forming oxide constituents. Related to a temperature of 1400°C their expression is:

$$\gamma_{1400^{\circ}} = (\text{FeO}) \times 570 + (\text{SiO}_2) \times 285 + (\text{Al}_2\text{O}_3) \times 640 + (\text{CaO}) \times 608 + (\text{MgO}) \times 512$$

dynes.cm 24./

in which all constituents are given in mole fractions. This equation was now applied to all the synthetic and plant slags investigated in this study and the results of calculations for synthetic slags are given in figures V-18 and 19. As will be apparent from the graphs the magnitude of the surface tension in the slags is between 450 and 500 dynes.cm. and tends to increase with increasing total iron, CaO, MgO content and basicity ratio, and to decrease with increasing silica content, when κ , η and γ are shown in a simultaneous representation as being functions of the slag composition at a temperature of 1400°C . It is interesting to note that in slag Groups III, IV and V this increase up to approximately the optimum slag composition range is moderate then it becomes more rapid with the change in chemical composition.

The surface tension of Ni_3S_2 -FeS and Cu_2S -FeS systems was more recently investigated by Zaytsev and co-workers. (19). According to their results the surface tension of a matte containing the same range of Ni and Cu sulphides as the RHM electric furnace matte is approximately 360 dyn.cm. This rather high value would ensure the necessary stabilising effect through the entire composition range of the synthetic slags and also of the plant slags for effective separation. Therefore the interfacial tension aspect does not seem to play a decisive part in these slags. Obviously this refers only to the equilibrium system of the final slag in the settling zone of the furnace.

In connection with the considerations outlined above a brief mention should be made here of the effect of SO_2 bubbles upon matte carry-over by the slag. Sulphur dioxide gas is formed by the reaction



Investigations on transport and flotation behaviour in matte-slag systems indicated (30, 31) that

- a./ the flotation of droplets is favoured in all matte-slag systems
- b./ silica saturation in the slag tends to inhibit film stability and reduces matte transport into the slag
- c./ the replacement of FeO by CaO in the slag tends to encourage the formation of a stable matte film. Furthermore, as con -

firmed by the work/...

Figure V - 20 OPTIMUM SLAG COMPOSITION FROM VISCOSITY AND ELECTRICAL CONDUCTIVITY MEASUREMENTS FOR SLAG GROUP II

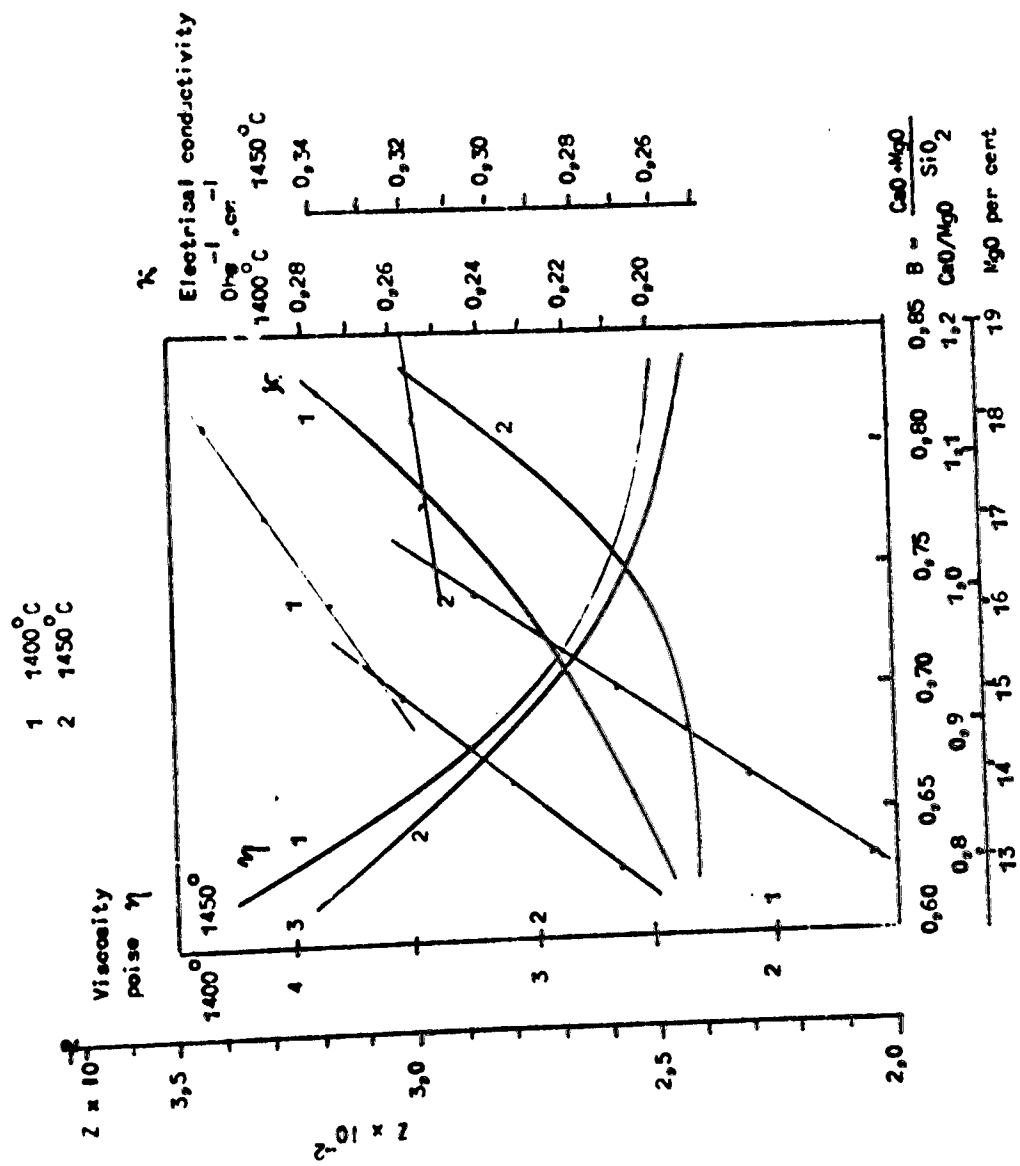


Figure V - 21
OPTIMUM SLAG COMPOSITION FROM VISCOSITY AND ELECTRICAL CONDUCTIVITY
MEASUREMENTS FOR SLAG GROUP IV.

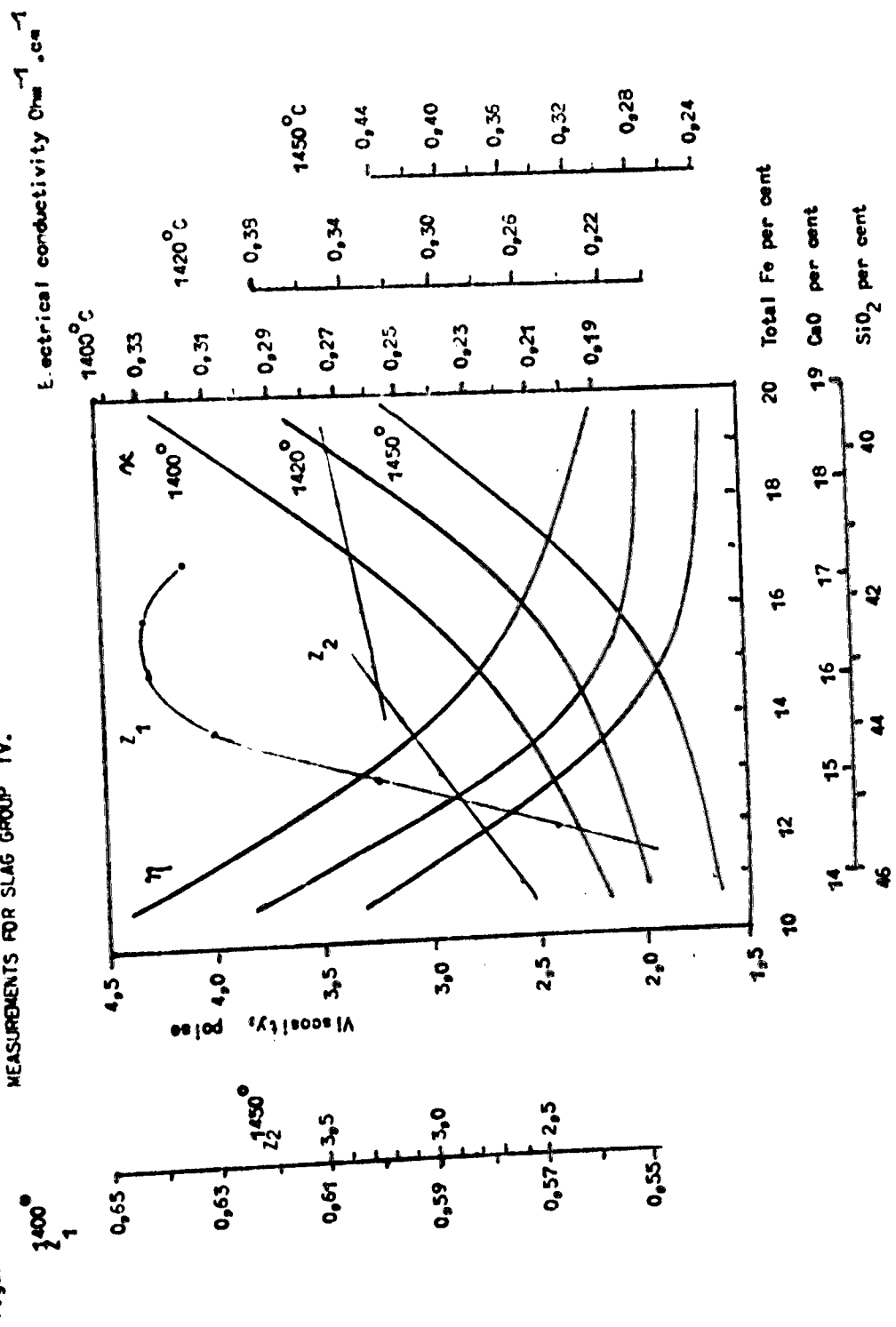


Figure V-22 OPTIMUM SLAG COMPOSITION FROM VISCOSITY AND ELECTRICAL CONDUCTIVITY MEASUREMENTS FOR SLAG GROUP V.

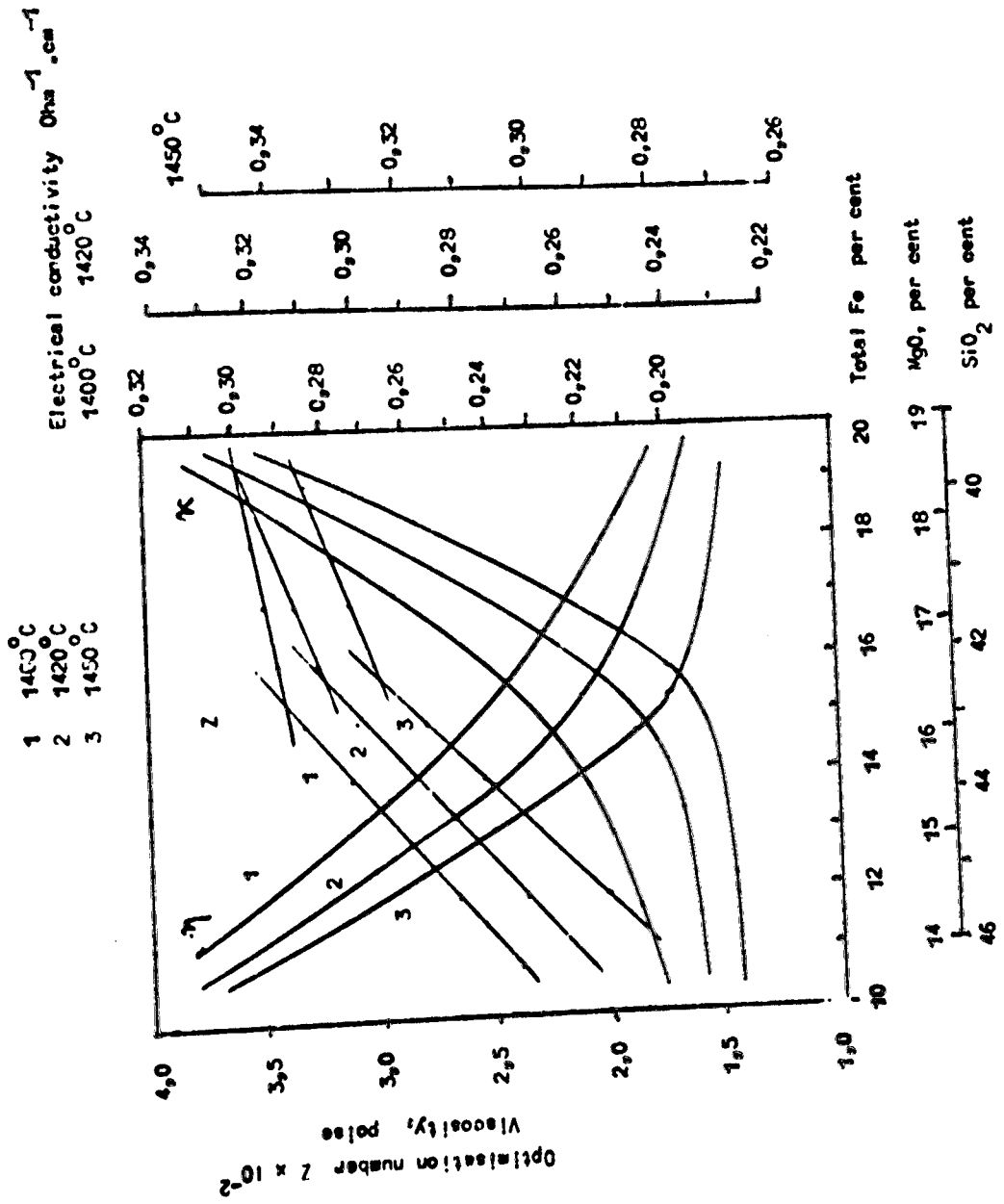


Table V - 2.

Optimum slag compositions estimated from viscosity and electrical conductivity data of synthetic slags.									
Slag Group	Range of slag constituents at optimum composition				Viscosity & conductivity at optimum slag composition			Temperature range	
	CaO	MgO	FeO	SiO ₂	Basicity Ratio	$\frac{\text{CaO}}{\text{MgO}}$	Viscosity poise		Conductivity $\frac{\text{ohm}^{-1} \cdot \text{cm}}{^{\circ}\text{C}}$
I	15,0-15,3	Const. 16,0	Const. 20,0	42,5-43,0	0,72-0,74	0,95	2,80	0,230	1400
II	Const.	15,5-16,5	Const. 20,0	-	0,73-0,76	0,95-1,30	2,90-1,90	0,225-0,270	1400-1450
III	15,0-15,2	16,5	Const. 20,0	43,0-43,6	0,73-0,75	Const. 0,77	2,65-1,75	0,224-0,260	1400-1450
IV	15,7-16,1	Const. 16,0	18,7-20,0	42,5-43,5	Const.	Const. 0,76	2,43-1,68	0,220-0,250	1400-1450
V	Const.	16,6-16,8	19,8-20,2	42,3-43,5	Const.	Const.	2,43-1,68	0,235-0,276	1400-1450)
Plant Slag	16,0-18,0	13,0-15,0	16,0-21,0	40,0-42,0	0,71-0,77		3,40-3,10 3,05-2,50	0,217-0,230	1400 1450

firmed by the work of Poné and Borge (28), an increasing ratio of CaO/FeO at constant SiO₂ increases the $\gamma_{\text{slag/gas}}$ (interfacial tension between slag and gas) whereas increasing temperature increases $\gamma_{\text{matte/slag}}$ but decreases $\gamma_{\text{slag/gas}}$.

Although the relevance of these aspects to the operation of the electric furnaces is obvious, from considerations made earlier in connection with the partial pressure of SO₂ in the electric furnace gases, the extent of interaction should be rather limited. Without large amounts of magnetite in the slag the presence of SO₂, as it was noted, should be mainly the result of the oxidation of concentrate on the top of the melt (the furnace is run under a slight negative pressure) rather than of chemical reactions occurring in the bath proper.

2./ Mode of slag optimisation based on the viscosities and electrical conductivities of the synthetic slags investigated.

Since viscosity and electrical conductivity exhibit two opposite trends on the change of composition in a given slag system, their interaction as a function of slag composition might serve as a basis for the best type of slag for optimising electric furnace performance. One way of approach toward this goal is to utilise the graphical representation of the viscosity-conductivity curves whereby the point of intersection of the two, in most cases parabolic, curves projected to the composition axis may be regarded as the most desirable slag composition. Graphs 20, 21 and 22 have been drawn in this way. Table V-2 gives the suggested optimum slag compositions read from the graphs in the temperature range shown in the figures together with corresponding viscosity and conductivity values. For comparison it contains also relevant data on slags of the type presently encountered in the operating furnaces. The salient points that emerge from the data of Table V-2 are:

- a./ The narrow range of composition, viscosities and electrical conductivities within and also among the various slag groups. This could be expected because of the nature of the method used for optimisation. The trends detectable with the optimum values would indicate some difference in viscosities between Groups IV and V when CaO is replaced by MgO.
- b./ From the comparison between optimum and actual slag composition it would appear that the FeO and CaO contents of the plant slags are in the optimum range (CaO probably somewhat higher), while MgO is lower by about 2 to 3 per cent and the SiO₂ by about 1,5 to 2,0 per cent/...

Figure V - 23 EFFECT OF TEMPERATURE ON THE OPTIMUM SLAG COMPOSITION

A Slag Group IV
B Slag Group V

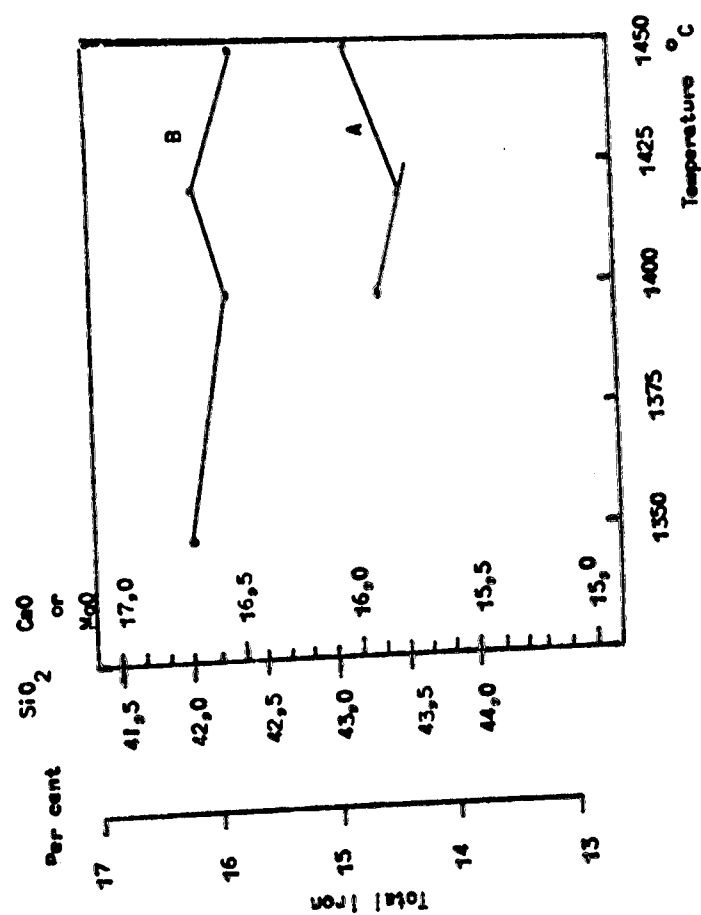
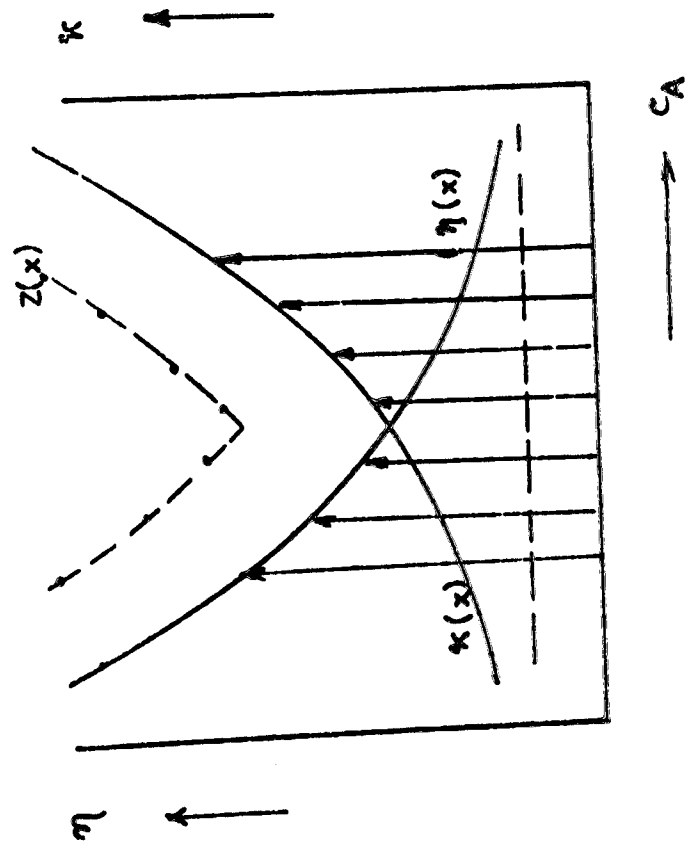


Figure V - 24 ILLUSTRATION OF THE METHOD OF FINDING OPTIMUM
SLAG COMPOSITION FROM VISCOSITY-CONDUCTIVITY PLOTS.



per cent. From a practical point of view the increase of the MgO content of the plant slags from the present level to that of the optimum would probably somewhat lower the viscosity and increase the electrical conductivity, but might also increase the liquidus temperature, although this does not seem to be substantiated by results obtained on the behaviour of MgO in slag Group II.

- c./ The viscosity range of plant slags tapped from the furnaces is noticeably higher than that indicated by optimisation calculations. On the other hand the difference between relevant conductivity values is not significant but tends to be lower than that of the other groups in Table V-2.
- d./ The effect of temperature on the optimum compositions is negligible in all five groups, fig. V-23 showing this in case of Groups IV and V.

* * * *

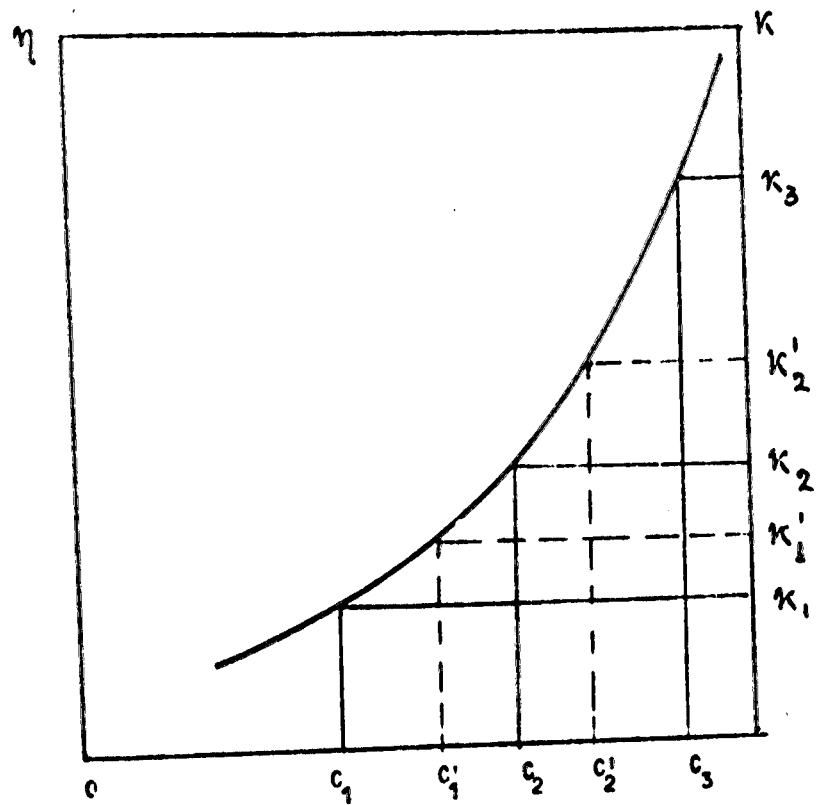
With a view to control purposes it was thought to be of some interest to follow up the viscosity-conductivity-slag composition relationship by the formulation of some suitable mathematical functions. The objective here is that the expression should incorporate at least two of the most influential slag components the importance of which is being judged on the basis of relations outlined in figures V-20 to 22, then either K or η .

The described method used for finding the optimum slag composition from viscosity and electrical conductivity plots is based on the general principle of optimisation from the intersection of two curves. Let for example the conductivity and viscosity be functions of say the concentration of component A. In its simple form this is an unconstrained optimisation problem because, within a reasonable wide range, one is at liberty to choose any value for the slag component. Then finding the optimum value by the intersection of the κ - η plots may be performed the way as illustrated in graph V-24. The optimisation curve $Z(x)$ is obtained by adding up the corresponding (equally spaced) distances below the curvilinear plots of κ and η . Naturally in case of two intersecting parabolic curves or a straight line intersecting a parabolic curve, the minimum of the resulting plot will always lie above the point of intersection of the κ - η plots. Only in case if κ were represented by a parabolic and η by a hyperbolic plot or else, if with the parabolic curves one would change sign and drop below the x axis would a shift in the minimum of the optimisation curve occur with respect to the point of intersection. However, due to the general trend of viscosities and electrical conductivities with the change of slag composition, these conditions would virtually never materialise.

When optimising/...

Figure V - 25

POINT GENERATION ON A PLOT BY THE INCREMENT METHOD



$$d\kappa = (\kappa'' - c^n) \Delta c$$

Figure V - 26

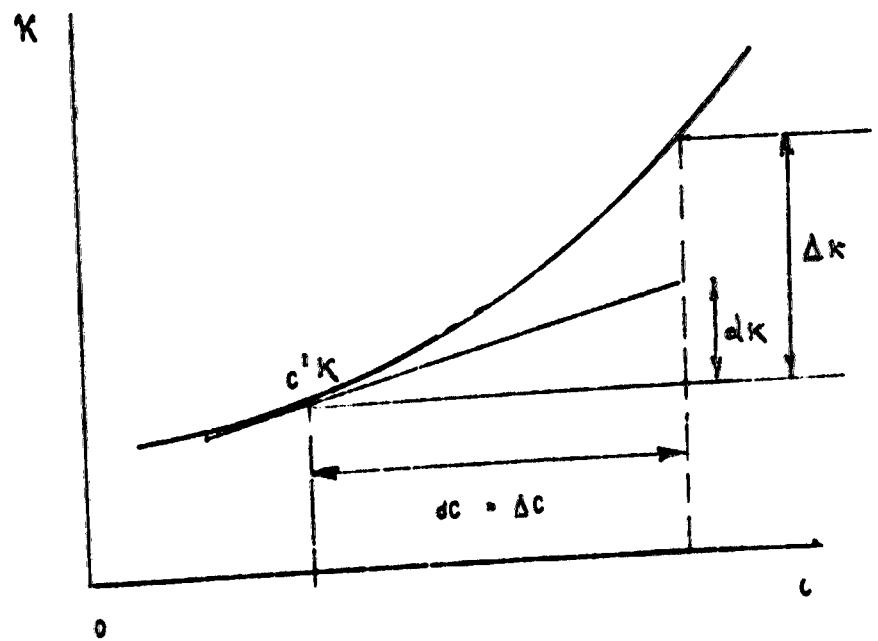


Table V - 3. Results of laboratory measurements on viscosities and electrical conductivities used for the computation of the Z function.

Slag Group	Slag number	Specific resistivity Ohm.cm	Conductivity Ohm ⁻¹ ,cm ⁻¹	Slag constituents. per cent		
				Fe _{tot}	CaO	MgO
IV	9	3,45	0,190	11,0	14,1	16,0
	2	2,85	0,234	15,3	16,2	16,0
	10	1,95	0,336	20,0	19,0	16,0
V	11	3,20	0,203	11,0	16,0	14,1
	2	2,80	0,234	15,3	16,0	16,2
	12	1,95	0,234	20,0	16,0	19,0

Table V - 4. Tabulation of calculated values involving C_{Fe} and C_{CaO} as independent variables. Slag Group IV.

Slag constituent referred to per cent	κ	$2\sqrt{\kappa}$	$\frac{C_{Fe}}{2\sqrt{\kappa}}$	$\text{erf } x_1$	$\frac{C_{CaO}}{2\sqrt{\kappa}}$	$\text{erf } x_2$	$Z(2) \times 10^{-2}$
Fe _{tot} CaO							
11 14,00	0,190	0,0870	0,126	0,141	0,162	0,183	2,56
12 14,55	0,198	0,0890	0,135	0,150	0,164	0,183	2,76
13 15,10	0,208	0,0914	0,142	0,159	0,166	0,186	2,95
14 15,65	0,218	0,0934	0,150	0,168	0,167	0,187	3,14
15 16,20	0,233	0,0964	0,156	0,173	0,167	0,187	3,24
16 16,75	0,250	0,1000	0,160	0,179	0,166	0,186	3,30
17 17,30	0,270	0,104	0,164	0,183	0,164	0,183	3,34
18 17,85	0,290	0,108	0,167	0,187	0,162	0,182	3,40
19 18,40	0,311	0,111	0,171	0,191	0,161	0,181	3,46
20 19,00	0,330	0,117	0,172	0,192	0,159	0,178	3,42

Table V - 5. Tabulation of calculated values involving C_{Fe} and C_{MgO} as independent variables. Slag Group V.

Slag constituent referred to per cent	κ	$\frac{C_{Fe}}{2\sqrt{\kappa}}$	$\text{erf } x_1$	$\frac{C_{MgO}}{2\sqrt{\kappa}}$	$\text{erf } x_2$	$Z_{(2)} \times 10^{-2}$
Fe_{tot}	MgO					
11	14,00	0,205	0,122	0,137	0,175	9
12	14,55	0,208	0,131	0,147	0,179	2,63
13	15,10	0,213	0,140	0,157	0,182	2,86
14	15,65	0,220	0,149	0,163	0,186	3,12
15	16,20	0,230	0,156	0,175	0,188	3,29
16	16,75	0,242	0,163	0,182	0,189	3,43
17	17,30	0,259	0,166	0,186	0,187	3,44
18	17,85	0,277	0,171	0,191	0,186	3,54
19	18,40	0,300	0,173	0,193	0,185	3,57
20	19,00	0,320	0,177	0,198	0,185	3,64

Table V - 6. Tabulation of calculated values involving C_{Fe} , C_{CaO} and C_{SiO_2} as independent variables. Slag Group IV.

Slag constituents referred to per cent		$1-X_{SiO_2}$	$1-X_{SiO_2} \times (3 \times 10^{-1})$	$\frac{C_{SiO_2}}{2\sqrt{k}}$	$2\sqrt{k}$	erf x	$Z(3) \times 10^{-2}$
Fe_{tot}	SiO_2						
11	46,0	0,540	0,162	0,176	0,0870	0,207	0,530
12	45,2	0,548	0,164	0,184	0,0890	0,205	0,567
13	44,4	0,556	0,167	0,183	0,0914	0,204	0,602
14	43,6	0,564	0,169	0,181	0,0934	0,202	0,630
15	42,8	0,572	0,171	0,177	0,0967	0,198	0,640
16	42,0	0,580	0,174	0,174	0,100	0,195	0,644
17	41,2	0,588	0,176	0,169	0,104	0,190	0,635
18	40,4	0,596	0,178	0,165	0,108	0,185	0,630
19	39,6	0,604	0,181	0,163	0,111	0,182	0,620
20	38,8	0,612	0,184	0,156	0,117	0,175	0,602

When optimising slag composition with regard to electrical conductivity κ and the concentration of slag components c_A and c_B it was found that the equation given below will represent the relationship among the selected variables:

$$Z = \operatorname{erf} \left(\frac{c_A}{2\sqrt{\kappa}} \right) \operatorname{erf} \left(\frac{c_B}{2\sqrt{\kappa}} \right) \quad 25./$$

The practical application of the function will be illustrated on slag groups IV and V, involving electrical conductivities only as dependent variables. The basic data used for the computations are compiled in Table V-3. In order to complete the conductivity plots and ensure their adequate continuity, from the available data points further values were generated. This was accomplished by the increment method on the basic principles shown in figures V-25 and 26, giving the solution of $\kappa = \kappa(c)$ for given conductivity and concentration (c) values, in the range of say $\kappa_1 < \kappa_1' < \kappa_2$ etc. Naturally, the increment Δc was adjusted according to the slope of the curves: when this became larger, the value of increment was decreased.

The results relating to the mentioned slag groups are given in Tables V-4 and V-5. For the purpose of remaining in the adequate region of the $\operatorname{erf}(x)$ values, for computational purposes the concentration of the reference slag component (given as weight per cent) was divided by 10^3 , that is expressed as $\text{wt}\% \times 10^{-3}$ and was evaluated as such from the table of error functions.

Since the test results indicated that in all slag groups under investigation the SiO_2 content and thus the basicity ratio defined as $(\text{CaO} + \text{MgO})/\text{SiO}_2$ had a strong influence on both viscosity and electrical conductivity of the slags, it was attempted to involve SiO_2 into the mode of estimation of the Z function. However, SiO_2 decreases with the increase of κ , therefore the values of $1 - X_{\text{SiO}_2}$ were used instead of X_{SiO_2} . The values obtained in this way were large, so they had to be multiplied by an arbitrary multiplication factor, in this case 0,3, in order to remain again in the convenient region of $\operatorname{erf}(x)$ set for c_{CaO} and c_{Fe} . Taking slag Group IV as an example with experimental results obtained at 1400°C Table V-6 gives the results of computation in the composition range of the slag investigated.

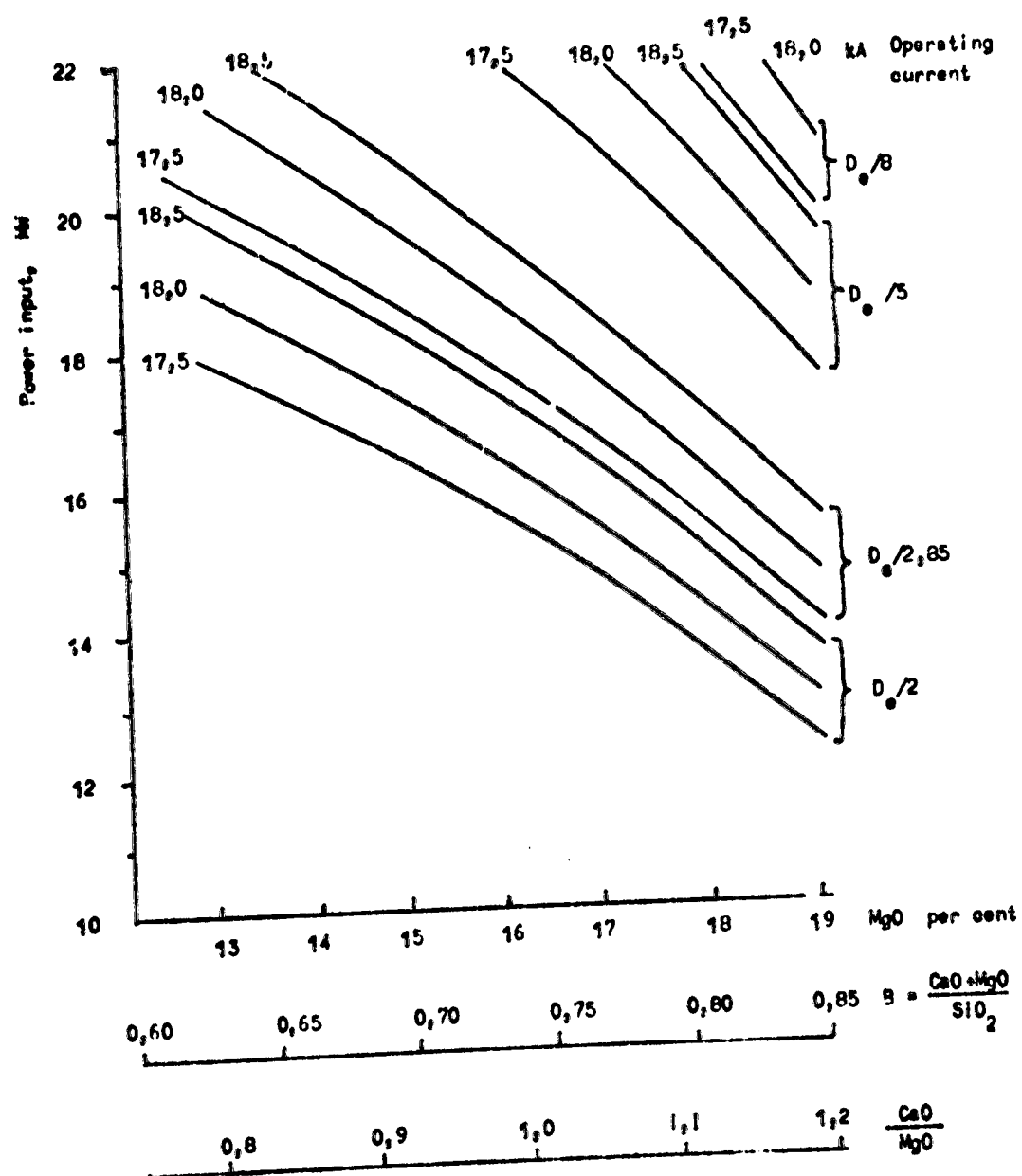
The results shown in these tables are plotted also in figures V-20 to 22. As will be seen, at the optimum value of the pertinent slag constituents the plots exhibit in all cases a marked inflexion or "kink" which becomes more noticeable as the curvature of the plot increases.

Naturally it should be/...

Figure V - 27

EFFECT OF SLAG COMPOSITION ON FURNACE OPERATION CHARACTERISTICS.

Slag Group II, Temperature 1400°C



Naturally, it should be emphasized here that the equation used for the estimation of Z (equ. V-25) is, in a true sense, under no conditions an optimisation function. The basic equation it is derived from corresponds to transient heat conditions or diffusion in a two-dimensional domain.

3./ Practical expectancy of the suggested mode of optimisation.

The practical application of the suggested technique of optimising slag composition on the basis of electrical conductivity and viscosity measurements will be discussed in the following on two examples which may serve as illustrations of the merits and shortcomings of the method. The first one deals with the possibility of predicting the relative rate of furnace production. The second one concerns itself with the question of optimum slag composition contours, whether it is possible at all to delineate such contours in order to find the optimum operating range of an electric, or in fact any kind of smelting furnace.

3.1./ Slag composition and relative rate of production of furnace.

Since the composition of the slag, on account of its effect upon viscosity and electrical conductivity is one of the prime factors that stipulates the production rate of the furnace, it is of great importance that the composition of the charge (concentrate) then the quantity of flux and of the return slag should, as far as possible, remain constant throughout the operation in order to secure a slag of constant composition. As the results of the laboratory tests indicated, even with the narrow range of slag compositions very substantial, 30 to 50 per cent variations in viscosities and electrical conductivities were associated. The effect of these variations upon the electrical operating parameters of the furnace, as it is apparent from figures III-4 and 5, is also very significant. It is in this fact that the conversion of the calculated data into actual furnace productivity on the basis of the measured electrical conductivities is made rather difficult.

For the illustration of this point let slag Group II serve as an example. The plot relating to 1400°C in fig. V-20 shows an optimum slag composition at 15,5 per cent MgO . The conductivity of the slag at this point is $0,225 \text{ ohm}^{-1} \cdot \text{cm}^{-1}$. With the decrease of the MgO content to 13 per cent it decreases to $0,200 \text{ ohm}^{-1} \cdot \text{cm}^{-1}$, whereas at the maximum of 19,0 per cent MgO it increases to $0,273 \text{ ohm}^{-1} \cdot \text{cm}^{-1}$. From fig. V-27 (which is the corresponding graph showing furnace operating parameters), at the optimum slag composition/...

Figure V - 28 LIMITING MATTE PRILL SIZE IN PARTICLE CARRY - OVER
AS A FUNCTION OF SLAG LEVEL ABOVE THE TAPHOLE.

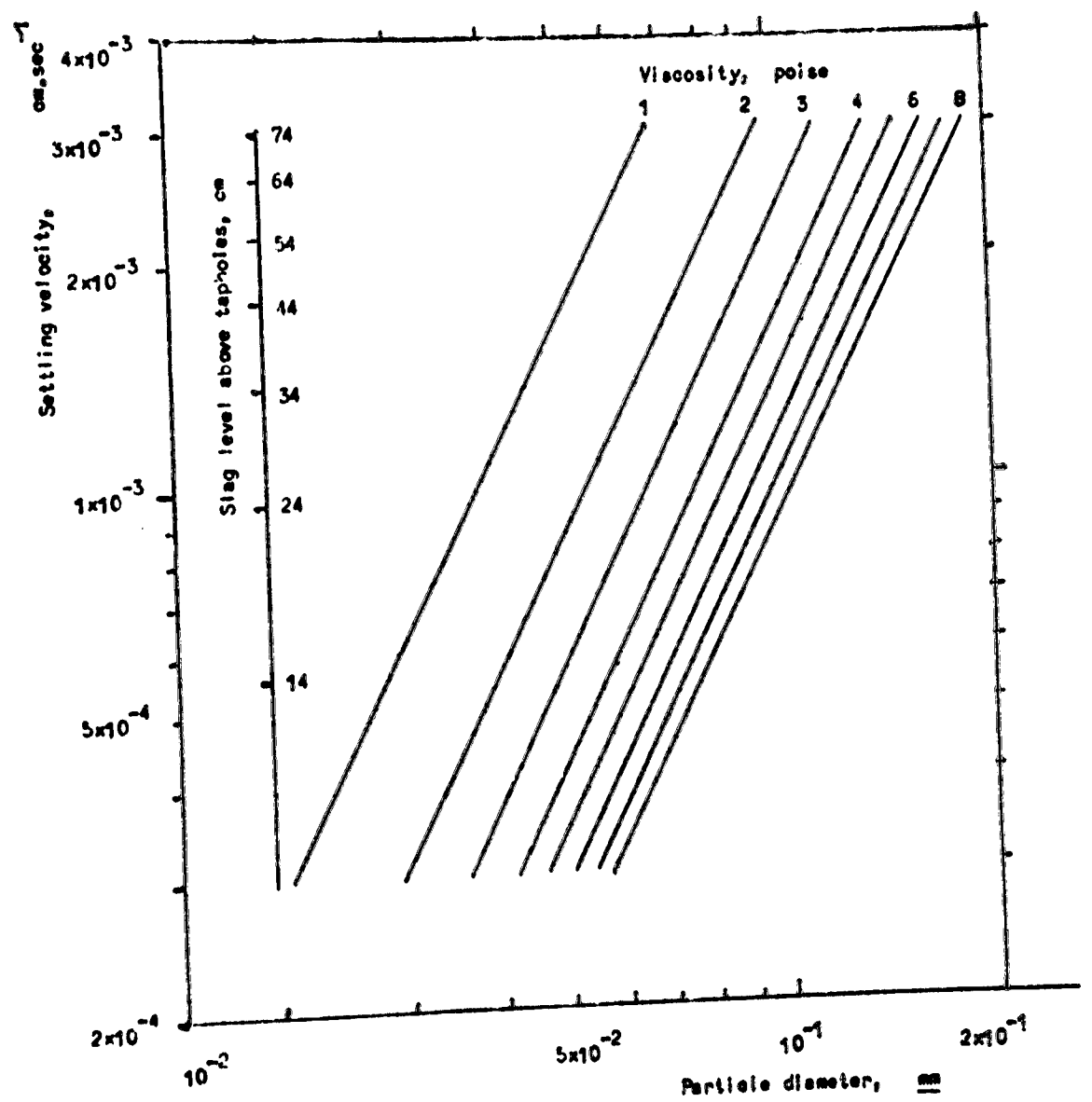
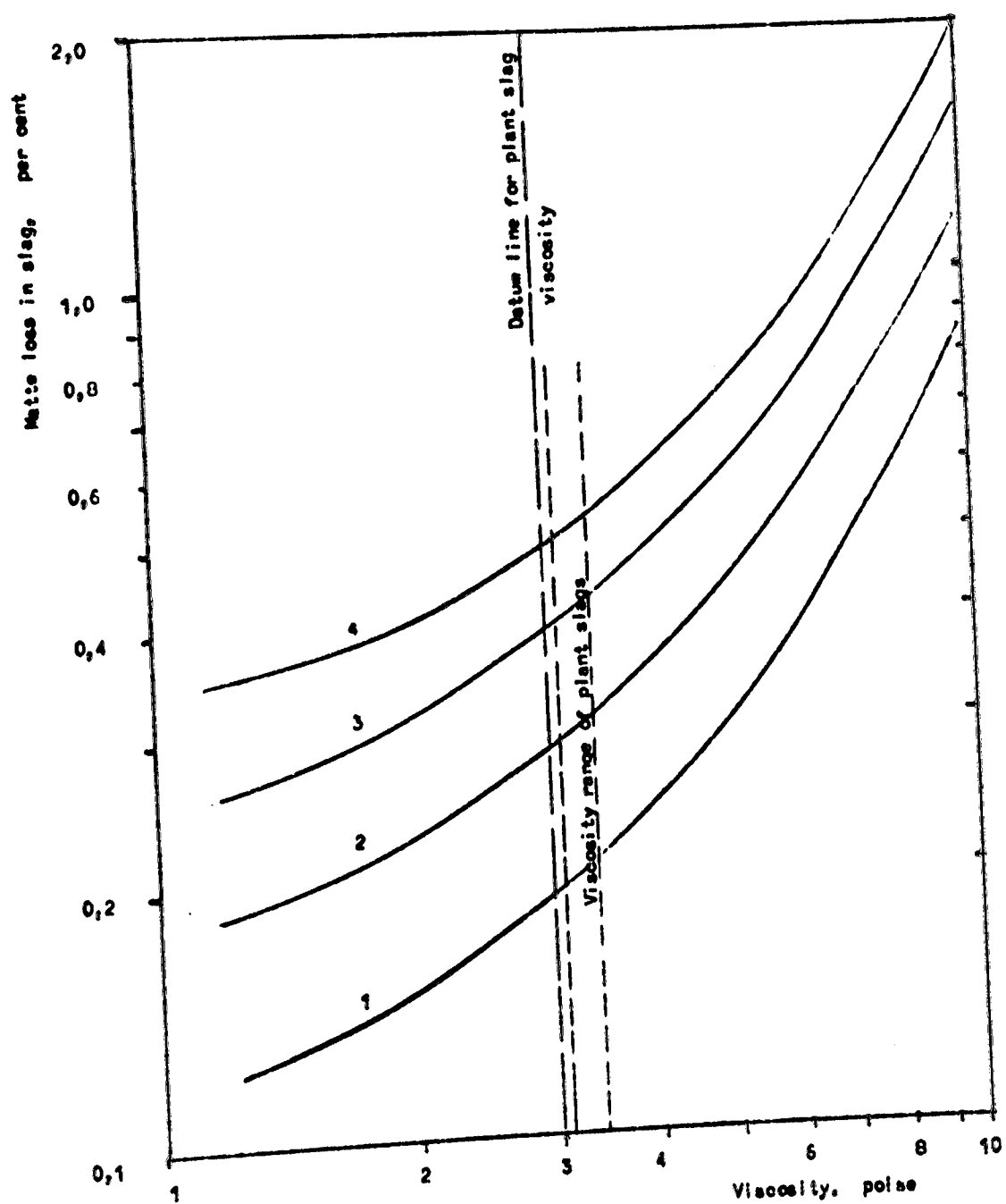


Figure V - 29 MATTE LOSS IN SLAG AS A FUNCTION OF SLAG VISCOSITY.

Legend		Matte content of slag	
1	0,2	per cent	
2	0,3	" "	
3	0,4	" "	
4	0,5	" "	



slag composition in the operation range of the furnace the current drawn would be 18,5 kA at an electrode depth of immersion $D_e/2$ ($\approx 0,525$ m) or 17,5 kA at a $D_e/3$ depth of immersion. By the graph 18,5 kA is equivalent with 18 MW power input which, upon the decrease to 13 per cent MgO content in the slag would rise to 19,9 MW, i.e. beyond the rated transformer capacity of the furnace. On the other hand, the increase of conductivity to $0,273 \text{ ohm}^{-1} \cdot \text{cm}^{-1}$ would result in the decrease of the power input (at a constant 18,5 kA) to 13,7 MW, which, of course could be balanced out by the appropriate alteration of the tapchanger position on the furnace transformer. At constant current this would mean that the decrease of conductivity in the above proportions (i.e. from 0,225 to $0,200 \text{ ohm}^{-1} \cdot \text{cm}^{-1}$) would bring about a proportionate increase in throughput of $/1,0 - (18,0/19,9)/ \times 100 = 11,05$ per cent, whereas the increase of conductivity from 0,225 to $0,273 \text{ ohm}^{-1} \cdot \text{cm}^{-1}$ would be equivalent to a decrease in throughput of $/1,0 - (13,7/18,0)/ \times 100 = 23,8$ per cent.

Obviously these large variations in throughput would never materialise in practice primarily because of the already discussed great difference in the variation of the components between synthetic slags and plant slags. Therefore the graphs representing furnace operating parameters should also be viewed in this context.

The practical implications of slag composition change versus viscosity and their effect of changes in productivity are somewhat simpler and may be estimated with reasonable certainty. On taking again slag Group II in fig. V-20 as an example, at optimum slag composition the viscosity is 2,9 poise at 1400°C . Let the average Ni and Cu content in the slag be 0,09 to 0,1 per cent and 0,06 to 0,08 per cent. With an average Ni content of 17 to 18 per cent in the matte (i.e. roughly 1/5 of its weight) the matte contained in the slag would come to approx. 0,5 per cent. About one half to 2/3 of this amount can be regarded as being entrapped at an average slag viscosity of around 3 poise at 1400°C . Based on these data the percentage loss in matte due to increase in viscosity was estimated with the use of fig. V-28 for four different levels, viz. 0,2, 0,3, 0,4 and 0,5 per cent matte contents in the slag. The results are summarised in graph V-29. With the chosen range of 0,2 to 0,5 per cent it is by no means construed that at 1400°C and at a reference slag viscosity of 3 poise four matte levels can coexist when both temperature and viscosity are being fixed by the composition of the slag which was regarded as being unchanged. With the selection of the above loss figures it was rather intended to illustrate the effect of temperature when the slag cools from say an average $1390 - 1400^\circ\text{C}$ in the production zone to the temperature of tapping, say 1350°C .
Such a drop/...

Such a drop in temperature may change the viscosity from 3 to over 5 poise. Now if the losses due to entrapment are taken as a maximum of 0,5 per cent (which in the operating practice of the reference furnaces would be an exaggerated value for matte losses), then from fig. V-29 the quoted 2,9 poise would represent approximately 0,49 per cent loss. An increase of the MgO content from the optimum 15,5 per cent to 19,0 per cent (the highest limit investigated) will lead to the decrease of the viscosity to 2,5 poise (see graph V-20). Now with the aid of fig. V-29 the upward shift on the line drawn for 0,5 per cent matte content of the slag $\eta = 4,1$ poise will correspond to a new matte loss of 0,6 per cent, while the shift downward to 2,5 poise to a loss of 0,45 per cent. From this the proportionate decrease of loss due to viscosity effect will be $(1,0 - (0,49/0,60)) \times 100 = 18,3$ per cent and, with an increase on the opposite end it will come to $(1,0 - (0,45/0,60)) \times 100 = 8,2$ per cent. These are relative percentages on the originally present 0,5 per cent matte content of the slag. From the hourly production data of matte (4,5 tons) and slag (21 tons) (see Ref. 32) the actual matte loss in the slag would be $(21,0/4,5) \times 0,5 \sim 2,4$ per cent, thus the absolute volume of increase and decrease in matte production upon the effect of viscosity would amount to 0,43 per cent and 0,20 per cent respectively. This calculation when extended to the rest of the slag groups resulted in similar matte gain and loss figures.

3.2./ The question of "optimum slag composition contours"

With the available experimental data on the physico-chemical characteristics of slags and with the suggested mode of optimisation the question might reasonably arise whether it is justified at all to speak about "optimum slag composition contours" in a ternary or quaternary phase diagram involving the most influential constituents in the operation of the furnace. This question appears to be well-founded by the findings of the present study which would indicate that from an optimisation point of view there exists only one specific composition range that would secure best results for the operation in the context of the special conditions obtaining at RPM. Even a limited deviation from this optimum could bring about quite substantial changes in furnace operating parameters and might lead to a noticeable deterioration of the economy of smelting.

As shown above the effect of the changes of the physico-chemical characteristics of slags appeared to be considerably different depending on whether the viscosity or electrical conductivity was chosen as a basic parameter. These calculations were extended subsequently to the entire composition range/...

Figure V - 30. RELATIVE EFFECT OF VISCOSITY AND CONDUCTIVITY ON THE MATERIAL THROUGHPUT IN THE ELECTRIC FURNACE.

Slag Groups II and V. Temperature 1400°C

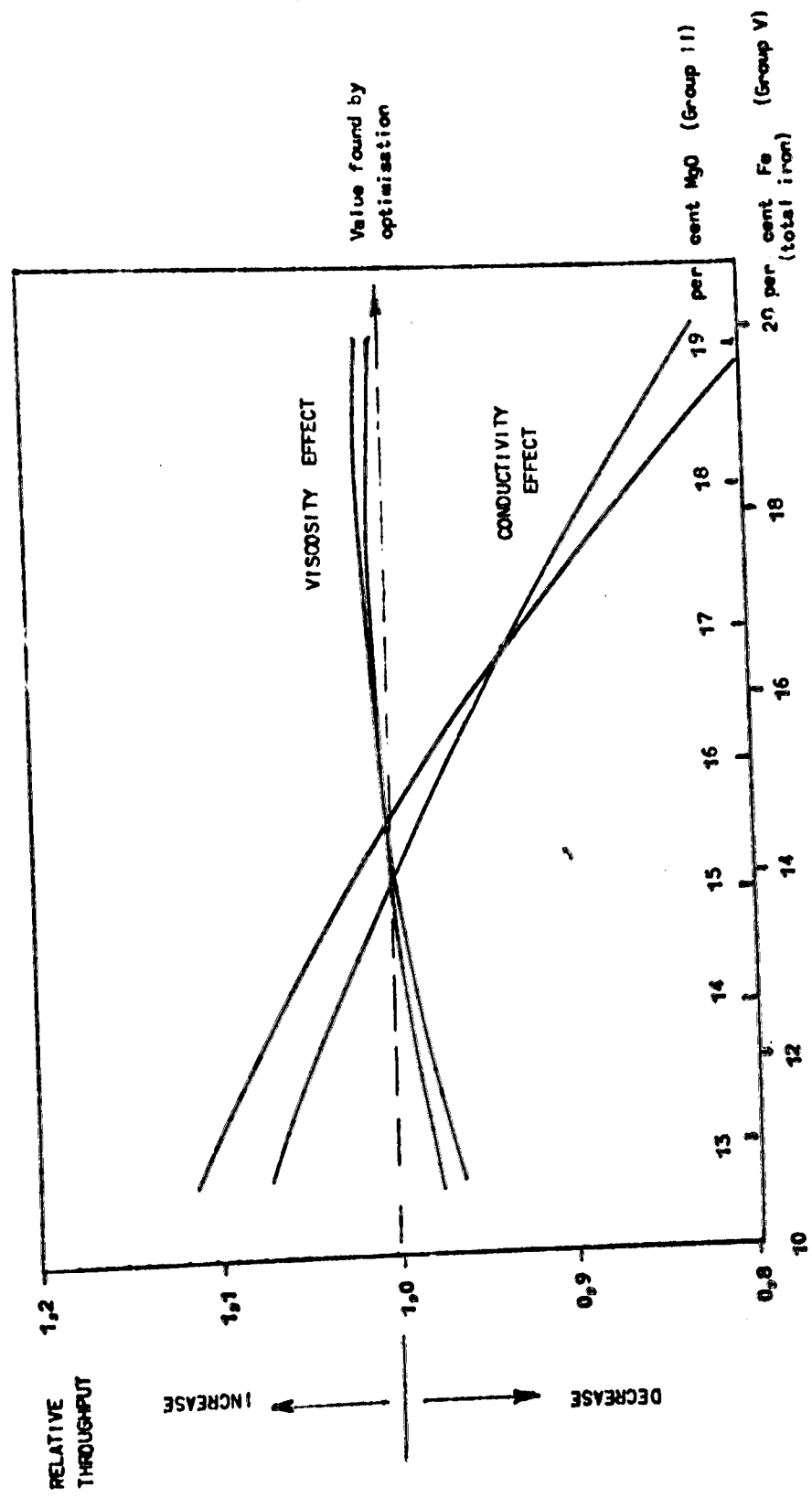
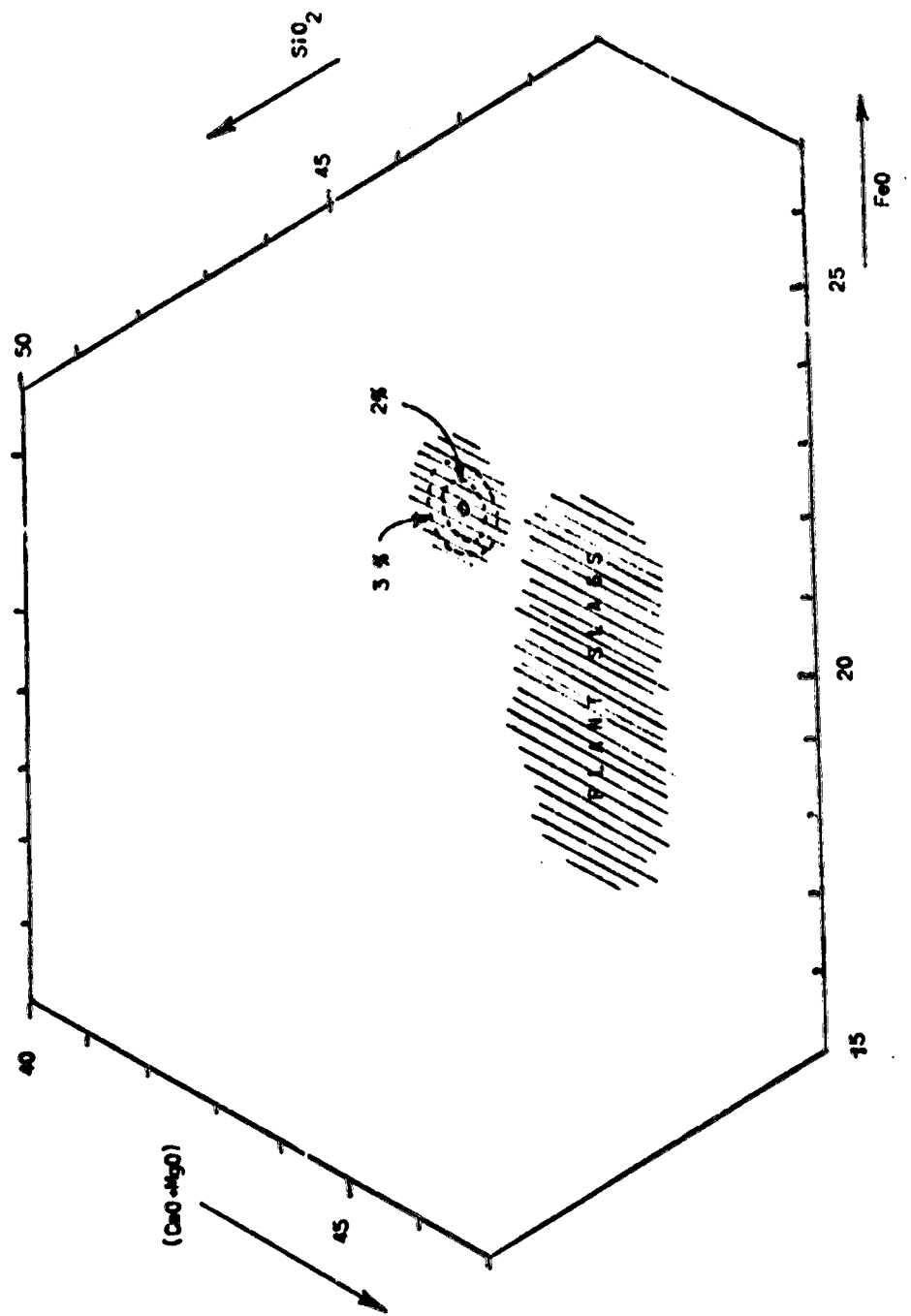


Figure V - 31. COMPOSITION RANGE OF PLANT SLAGS COMPARED WITH OPTIMUM SLAG COMPOSITION CONTOURES IN THE PSEUDO- TERNARY $\text{FeO} - (\text{CaO} \cdot \text{MgO}) - \text{SiO}_2$ SYSTEM CONTAINING 5 per cent Al_2O_3



composition range of the synthetic slags investigated and the effect of the change in viscosities and electrical conductivities upon the change of slag composition was projected to the relative throughput in the electric furnace. The results of these calculations related to slag groups IV and V are shown in figure V-30.

The difficulty of defining the contours of optimum slag composition with synthetic slags becomes immediately evident from the graph. It was noted earlier that the estimated large variations in electrical parameters (which eventually determine the rate of material throughput) caused by changes in slag composition would hardly come about in practice because A./ the sizeable difference in the change of components between synthetic and plant slags and B./ the large number of variables involved. Nevertheless, the outcome of these investigations indicates the poor reliability of attempts aiming at the definite demarcation of composition ranges on the basis of laboratory viscosity and electrical conductivity measurements, thought to be best suited to bring about maximum efficiency in furnace operation. In practice the relative indifference of working units toward variations in the two most influential slag components, MgO and FeO, beyond those limits that could be tolerated from optimisation considerations adds further doubt to the quantitative merit of these attempts. For purely qualitative orientation they may serve a more useful purpose.

The foregoing considerations can be further illustrated in the pseudo-ternary diagram of the slag system $\text{FeO}-(\text{CaO}+\text{MgO})-\text{SiO}_2$ with a constant 5% Al_2O_3 content. In figure V-31 the range of optimum slag composition, as obtained by the described mode of optimisation, and the range of plant slag composition is illustrated. Inside the broader range and around the point of optimum slag composition the dotted boundaries, representing slag Groups II and V, are associated with respectively 2 and 3 per cent decrease in material throughput in the furnace with the change of slag composition as estimated from fig. V-30. However, as it was stressed already, these results were obtained with laboratory slags and the change of the various slag-forming constituents was carefully controlled so as to yield information specifically on the effect of the component in question. In plant slags, on the other hand, the variation of the constituents follows a random pattern inasmuch that the conflicting effects may cancel out. As a result, the variation in slag composition will be much less noticeable in the viscosity-electrical conductivity relationship and, consequently, in the change of electrical parameters. In this way the material throughput/...

material throughput may undergo modest or even hardly noticeable variations.

In the light of the above facts the exact delineation of optimum composition contours in a ternary or quaternary phase diagram could not be justified in the context of the electric furnace slags investigated and the specialities of the operation. Similarly, the slight difference between the composition range of the conventional plant slags and that found by the technique of optimisation would discourage a categorical definition of optimum slag composition.

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SECTION VI.

DESIGN CONSIDERATIONS BASED ON THE OUTLINED MATERIAL AND HEAT FLOW CONCEPTS.

- 1./ The concept of the principle of similarity as an aid in furnace design.

Although the extent of disruptive forces acting on the outlined heat and material flow pattern is by no means negligible and the system is far from operating under steady-state conditions, the preceding considerations can still be used as reasonably sound starting points for furnace design when the concept of similarity is taken into account.

The principle of similarity defines the relationships existing between systems of different size and it has proved most useful in scaling up of physical and chemical processes, as it is demonstrated in the excellent study of Johnstone and Thring. (1). According to the basic concept of the principle the configuration of a system in space and time can be defined by ratios of any magnitude within the system and does not depend on the size or nature of the units of measurements.

The similarity states of importance to the present study can be summed up as follows:

a./ Geometric similarity. In the present case geometric similarity is maintained by treating the material in vessels of the same rectangular shape equipped with electrodes arranged in the same 6-in-line geometry. An important point is that the furnace loading for the same energy distribution per unit surface area or unit volume is kept reasonably constant.

b./ Dynamic similarity. This involves the forces affecting the movement of mass in a dynamic system and also entails kinematic similarity, as the motion of both feed material then slag and matte phases are functions of the forces applied to the system. Dynamic similarity in the flow of feed material is ensured by controlling the energy input to the furnace so that the Reynolds number is of the same order of magnitude in the flow for different size furnaces. Furthermore, for the moving bed of burden the particle sizing is the same and the energy-loading per unit volume or surface area of the furnace is the same.

c./ Chemical similarity. Chemical similarity is probably the most important similarity state in designing the furnaces and comparing their melting ability. The importance of the

physico-chemical/...

physico-chemical similarity of slags produced has already been demonstrated and emphasized in the foregoing and has to be pointed out repeatedly. It may be possible to regard the electrode as the main central parameter for design purposes around which the furnace with its ancillary equipment will be built, being thus the size of the electrode by virtue of its current carrying capacity the factor that will stipulate electrode spacing, furnace width, transformer size, etc. Yet it is the very character of the feed material that eventually determines the furnace load and thereby the melting rate of the furnace. Therefore the chemical composition of the starting material as well as slag produced should be similar in order to secure similar heat generation, electrical conductivities and viscosities to the slag with similar Reynolds numbers for the flow.

d./ Thermal similarity. The liquidus temperatures of slags have to be comparable which are basically the functions of slag composition pointing thus to the importance of chemical similarity. Comparable liquidus temperatures and slag resistivities will assure comparable smelting rates and, therefore, energy consumption.

Similar rates of heat transfer can be obtained by controlling the Reynolds number and also the Grashof number of the flow system to the same order of magnitude for different furnaces. This will also ensure an approximately equal heat flux to the charge to be melted.

In summing up, the similarity principle facilitates the design of furnaces provided the differences in the characteristics of the raw materials to be processed are not too great. Since in this case it can be safely assumed that the various disrupting effects on the discussed heat and material flow pattern will be by and large the same, as a first approximation, they will be left out of consideration in the following discussion.

2./ Design principles.

Considerable amount of work has been carried out on the operation of submerged arc furnaces used primarily in the electric steelmaking and ferroalloy industry. The various design equations proposed to describe and simulate the operation of the units cover fairly well the various aspects in conjunction with ferroalloy production, but as was referred to earlier, with the possible exception of Androae's basic formula, none of them can be regarded to be of general validity. This is quite understandable in considering the great varieties in burden properties the furnaces have to

cope with/...

cope with. The efforts to account for the properties of the feed materials are well represented by the work of Kjølsæth, Morkramer, Nilsen, Downing and Urban and Persson already referred to in Section III. This work centered on the exact determination of the k factor proposed by Andreae. Back in the 1940's together with Kelly they determined a large number of k values from operating plants. Eventually Kelly presented a typical range of k factors for different submerged arc operation (2). A comprehensive review of these efforts was given by Urquhart. (3).

In spite of their fundamental and also practical aspects, none of the investigations conducted could strictly be related to the operational and symmetry aspects of the rectangular-shaped matte-smelting electric furnaces with 6-in-line electrode arrangement. Only very recently did Persson and Treilhard, as it was shown, extend toward this direction the line of thought on furnace resistance, although Russian literature gave a comprehensive account on similar work long before this. Since, however, the interest of this study is focussed primarily on conditions given by the treatment of copper and nickel containing ores of South African origin, the symmetrical relationships established for a single furnace or a series of furnaces may differ from those outlined in the literature.

Pertaining to the simulation of conditions prevailing in a given furnace the method to be followed is based on some of the principles of heat and material transfer delineated in Section IV. Therefore as a most obvious choice, the electrode will be the basic parameter around which the most significant variables determining furnace geometry are going to be built. The reason for this choice will be immediately apparent from the expression of the specific hearth resistivity based on Andreae's concept (4)

$$\rho_h = R \pi D_e = (U_h / I_e) \pi D_e \quad 1./$$

The hearth resistivity, the characteristic value for various processes carried out in an electric furnace, is a constant value given by the hearth voltage U_h , the electrode current I_e , and the diameter of the electrode D_e . Thus in the knowledge of D_e and the measured resistivity of the slag (this latter being characteristic to the particular material processed in the furnace) the most important operating parameters, i.e. current and voltage, hence the size of transformer and the rate of power input to the furnace are also given. Recalling furthermore the relationship postulated by Andreae between the diameter of the electrode, the active zone around it and the extent of power input (set by the active area), the electrode as a central

parameter enables the determination of virtually all the other important design parameters. From a design point of view there is nothing new about regarding the electrode as a main design parameter. It has been used as will be discussed subsequently, by many distinguished workers in this field for quite some time. However its application for delineating furnace geometries with 6-in-line electrode arrangement is thought to be novel in many respects and the findings could serve as explanations for the choice of existing furnace geometries. It may also entail suggestions to be made to existing furnace design in order to improve performance.

In respect to model considerations it need not be emphasized that there is a striking difference between rectangular and circular furnaces. Thus while the basic concept of spherical geometry for modelling, as advanced by Schwarz von Bergkampff (4) may be successfully applied to electric arc furnaces of circular cross section with three electrodes, the same principle would be certainly met with considerable difficulty in the context of rectangular units.

In the previous discussion of heat and material flow, the working units at RPM were regarded as reference or model furnaces. This method will be retained and adapted to the procedure of scaling up furnace symmetries with the size of electrode as the main design parameter.

3./ Estimation of the active area of the electrode (d_A) and furnace width as functions of electrode diameter.

In the discussion of heat flow in horizontal direction across the molten slag bath reference was made earlier to the concept of Andreae according to which the flow of energy from the electrode comes about the radius of concentric circles. In this way the active zone around an electrode is circular having an area proportional to the diameter of the electrode. Therefore the radius of the active area varies with the size of the electrode.

Andreae's argument concerning the relationship between electrode diameter and active area served as a starting point for the estimation of the working zone of an electrode. Essentially it boils down to the calculation of radial heat distribution around the electrode with specified boundary conditions given by the characteristics of the slag. The general approach was/...

Table VI-1. Mode of calculation of $\sqrt{G}$ for various electrode diameters.

D_e (m)	0,4	0,6	0,8	1,0	1,2	1,4	1,6
δ_e "	0,112	0,168	0,224	0,280	0,336	0,392	0,444
k	0,055	0,082	0,110	0,137	0,165	0,192	0,216
σ	1,82	1,220	0,907	0,729	0,605	0,520	0,462
$\sqrt{\sigma}$	1,35	1,105	0,950	0,852	0,778	0,720	0,680

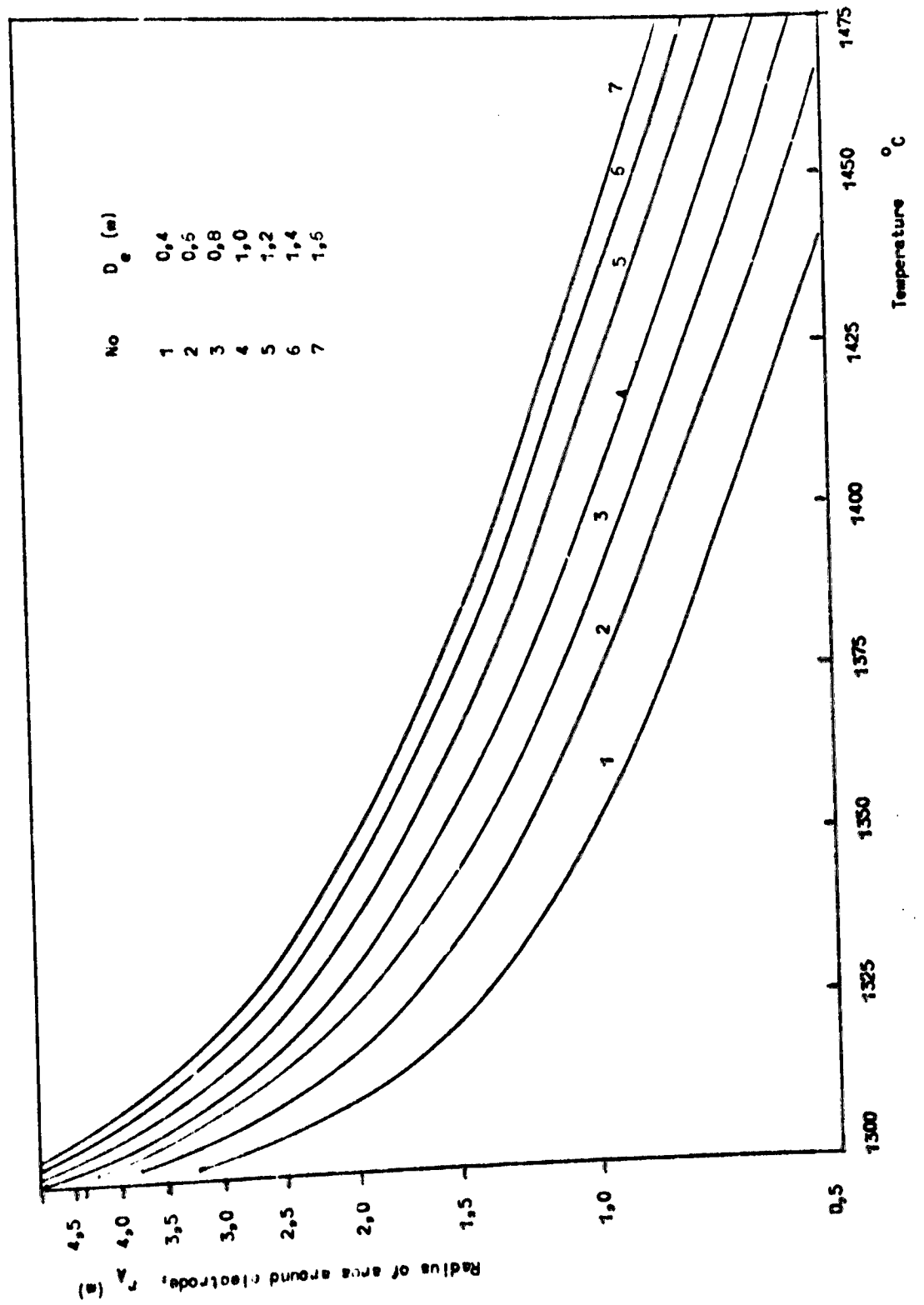
Table VI-2. Values of $r\sqrt{\sigma}$ for various electrode diameters

[illegible]

Table VI-3. Values of $K_0(x)$ and estimated temperatures at various distances (r = radius of active area) and electrode diameters, ($T_a = 1300^\circ\text{C}$). $1./K_0(x)$, $2./^\circ\text{C}$.

r (m)	$D_e =$		$0,6$		$0,8$		$1,0$		$1,2$		$1,4$		$1,6$	
	$1./$	$2./$	$1.$	2	1	2	1	2	1	2	1	2	1	2
0,3	1,01	1502												
0,4	0,86	1472	1,06	1512										
0,5	0,70	1440	0,85	1470	0,99	1498	1,09	1518	0,85	1470	0,92	1484	0,98	1496
0,7	0,45	1390	0,39	1418	0,71	1442	0,74	1454	0,58	1416	0,64	1428	0,68	1436
1,0	0,26	1352	0,36	1372	0,45	1390	0,52	1402	0,32	1364	0,37	1374	0,42	1384
1,5	0,11	1322	0,17	1334	0,24	1348	0,28	1356	0,20	1340	0,23	1346	0,26	1352
2,0	0,05	1310	0,09	1318	0,13	1326	0,16	1336	0,11	1322	0,14	1328	0,16	1332
2,5	0,02	1304	0,05	1310	0,07	1314	0,10	1320	0,08	1316	0,09	1318	0,10	1320
3,0			0,02	1304	0,04	1308	0,06	1312	0,05	1310	0,06	1312	0,07	1314
3,5					0,02	1304	0,04	1308	0,04	1308	0,04	1308	0,05	1310
4,0							0,03	1306	0,04	1308	0,04	1308		

Figure VI - 1. ACTIVE AREA OF ELECTRODE AS A FUNCTION OF ELECTRODE DIAMETER - TEMPERATURE DISTRIBUTION AROUND ELECTRODES OF VARIOUS DIAMETER ESTIMATED WITH BESSEL FUNCTION METHOD.



approach was outlined in the relevant section on heat conduction. Based on operating experience, for the optimum depth of electrode immersion a value of $D_e/2,5$ (expressed in terms of electrode diameter, $D_e=1,25$ m) will be used in the present context with the particular types of slag produced. In the reference furnaces, as it was shown, the depth of the slag layer of "constant" temperature (δ) varied between 30 and 35 cm at the quoted depth of electrode penetration into the slag, i.e. $p = 50$ cm, which from practical observation was found to be optimum. (actually 48 to 50 cm). Let us express this optimum in terms of half electrode diameter, that is in case of the reference furnaces

$$p_{opt.} = 50/62,5 = 0,8 D_e/2 \quad 2./$$

The ratio between the depth of constant temperature zone (δ) and p_{opt} is

$$\delta/p_{opt.} = 35/50 = 0,70$$

$$\text{therefore} \quad \delta = (0,7)(0,8 D_e/2) = 0,56 D_e/2 \quad 3./$$

Putting this into the expression of σ in equ. 19a of Section IV

$$\sigma = \frac{h}{k_{th} \cdot 0,56(D_e/2)} \quad 4./$$

where h has a value of around 1,0 (0,9 to 1,1) and the thermal conductivity of the slag is on an average $k_{th}=4,9$ cal/hr.m²⁰C/m. With these data $K_0(x)$, where $x = r\sqrt{\sigma}$, can be estimated to give the horizontal temperature profile in the slag according to equ.14 of Section IV. Table VI-1 gives the various steps for the calculation of $r\sqrt{\sigma}$ for electrode diameters of 0,4 to 1,6 meter. In Table VI-2 the data obtained on $r\sqrt{\sigma}$ for various values of r are compiled while Table VI-3 summarises the values of $K_0(x)$ estimated from tables of modified Bessel functions. The calculated results are plotted in figure VI-1 which enables finally the estimation of the diameter of the reaction zone with the change of electrode diameter. For the calculation of d_A the average liquidus temperature of the plant slag (1325°C) can be used.

The electrode diameter - working area relationship given in fig.VI-1 can be extended further to estimate the actual width of the furnace when smelting concentrates originating from the Merensky Reef ore deposit or concentrates of different composition but yielding slags of similar electrical conductivities to those produced in the present operation. For this purpose the lower limiting temperature was chosen as 1310°C at which the average plant slag is still fluid enough for practical flow purposes and also for limited matte settling, while below this temperature the rapid

increase of/...

Figure VI - 2. HORIZONTAL TEMPERATURE GRADIENT IN THE SLAG BED FROM
FIRST ORDER RITZ PROFILE. $D_0 = 1,25$ m

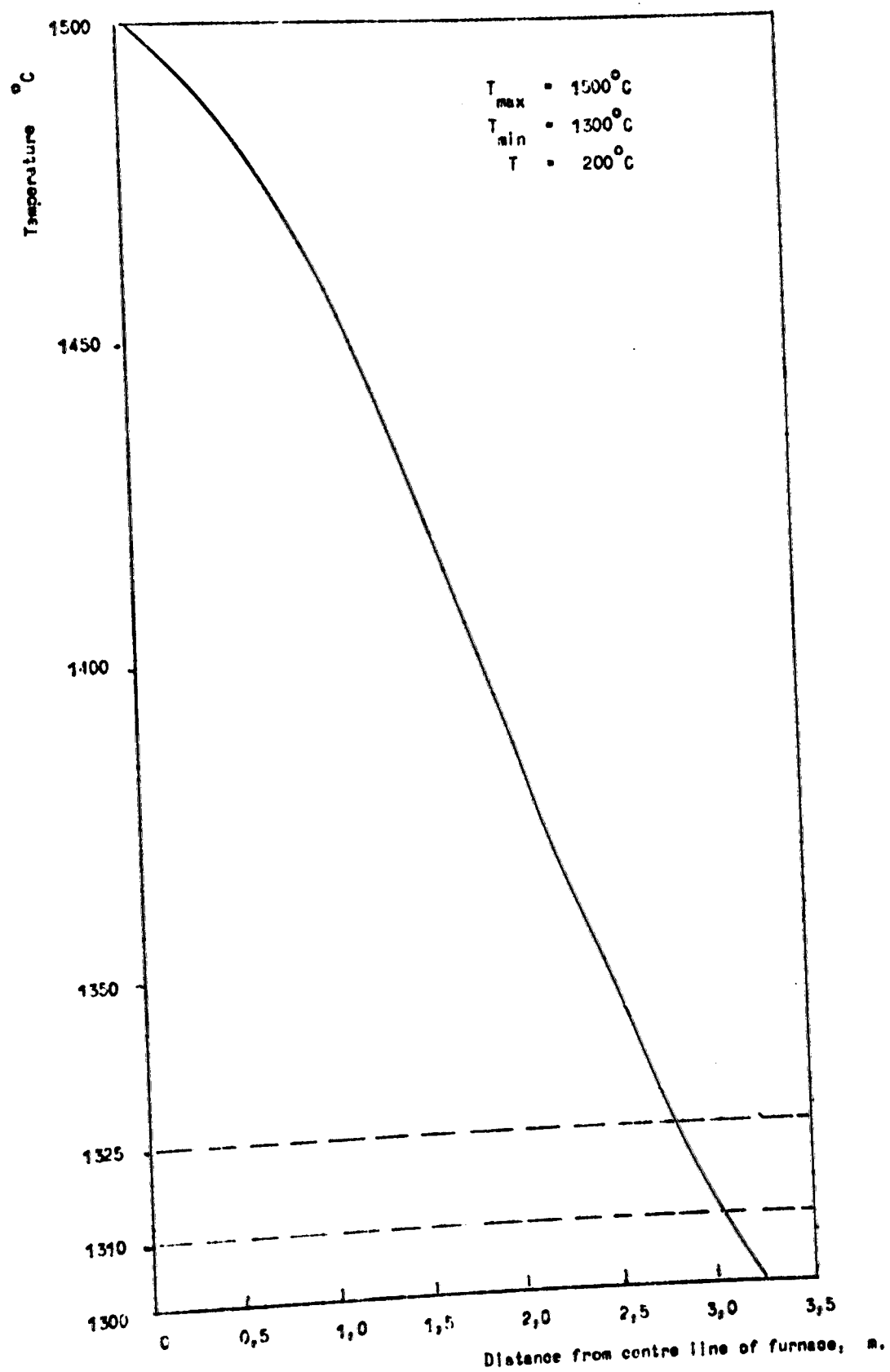


Figure VI - 3. EFFECT OF CHANGE IN ESTIMATED TEMPERATURE UNDER THE TIP OF THE ELECTRODE ON THE AREA OF TEMPERATURE DISTRIBUTION WITH ELECTRODES OF 0,6 and 1,2 METER DIAMETER.

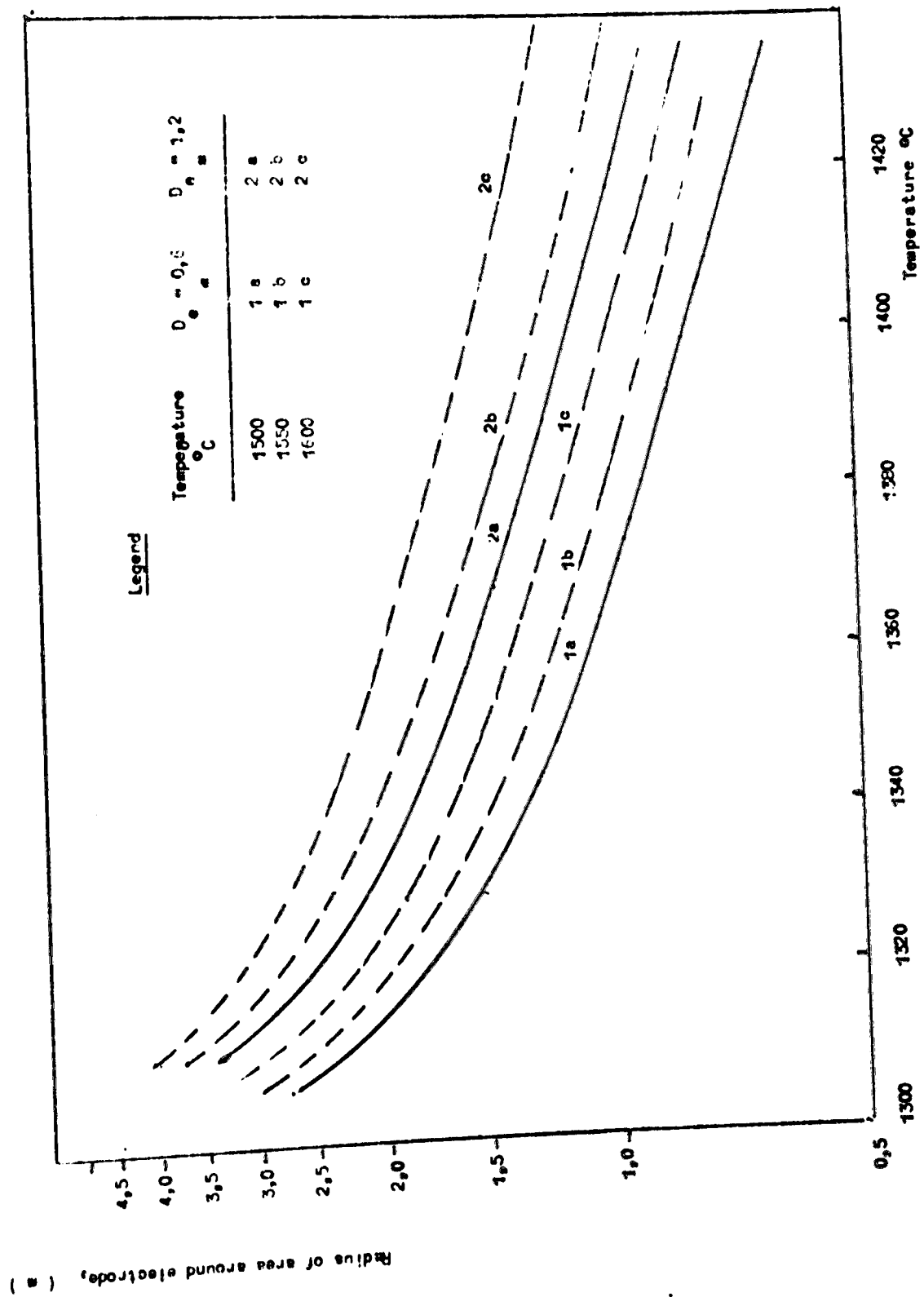


Table VI-4. Furnace width and active area versus D_e

D_e (m)	0,4	0,6	0,8	1,0	1,2	1,4	1,6
$d_A^{1325^\circ\text{C}}$ (m)	2,9	3,7	4,1	4,6	5,1	5,6	6,0
$d_w^{1310^\circ}$ (m)	4,0	5,0	5,7	6,3	7,0	7,5	8,0

Table VI-5 Active area of electrode as a function of the depth of electrode immersion into the slag (p).

$$D_e = 1,25\text{m}, \quad T_{\text{ref}} = 1325^\circ\text{C}$$

Depth of immersion in terms of D_e	$D_e/2$	$D_e/3$	$D_e/4$	$D_e/4$
in meters	0,625	0,417	0,313	0,250
d_A (m)	3,40	3,64	4,30	5,34

Table VI-6 Active electrode area as a function of the depth of slag layer of constant temperature (δ)

$$D_e = 1,25\text{ m}, \quad P_{\text{tot}} = 0,5\text{ m}, \quad T_{\text{ref}} = 1325^\circ\text{C}$$

δ (m)	0,1	0,2	0,3	0,4
Per cent of P_{tot} .	20	40	60	80
d_A (m)	2,8	4,0	5,0	5,9

increase of slag viscosity will create stagnant conditions. Table VI-4 displays the estimated relationships regarding both d_A and l_w for various electrode diameters.

The horizontal temperature gradient evaluated from the two-dimensional Ritz profile (see Section IV) can also be used for the estimation of d_A and l_w . For comparison this is shown in figure VI-2 for the electrode diameter $D_e = 1,25\text{m}$. From the plot with $T_{\text{max}} = 1500^\circ\text{C}$ the intersection at 1325°C gives a distance of 2,7 m for the radius of the active electrode area therefore $d_A = 5,4\text{ m}$. This would correspond to an electrode diameter of $D_e = 1,3\text{ m}$, in reasonably good agreement with the data obtained by Bessel function estimate.

4./ Effect of limiting maximum temperature.

From the extrapolation of results obtained from temperature measurements carried out in the close vicinity of a working electrode, 1500°C was chosen as a most probable temperature prevailing under the electrode tip and representing T_{max} . With identical slag compositions the main factors affecting this temperature are the operating current density on the surface of the electrode tip and, to a lesser degree, the depth of electrode immersion into the molten slag by virtue of the amount of heat distributed in a unit volume of melt as discussed previously. If, on account of various reasons like change of slag resistivity brought about by smelting effects with the change in the flow rates of the feed material to the electrodes, or intentional increase of operating current to increase production, the temperature changed from 1500° to say 1600°C , this would bring about a change also in the calculated active areas as indicated in figure VI-3 for electrodes of 0,6 and 1,2 m diameter respectively. As will be apparent from the graph the change in the temperature of the hot zone under the electrode will influence considerably the size of the reaction zone. These temperature variations could be especially pronounced during the process of charging converter return slag into the electric furnace or with a sudden change of the quantity of burden admitted to the electrode.

The operation of the electric furnaces, because of the complexity of the processes carried out in them is necessarily prone to cyclic fluctuations which, on a temperature basis, could easily involve a 3 to 3,5% change (i.e. about 50°C) in the hot zone temperature. As a consequence, the reaction zone of the electrode is also subject to periodic contractions and expansions, a well-known fact from operating practice. An exact determination of the extent/...

Figure VI - 4. EFFECT OF THE DEPTH OF ELECTRODE IMMERSION INTO THE MOLTEN SLAG ON THE TEMPERATURE DISTRIBUTION AROUND THE ELECTRODE.

$D_e = 1,25 \text{ m}$, $d_f = 0,35 \text{ m}$

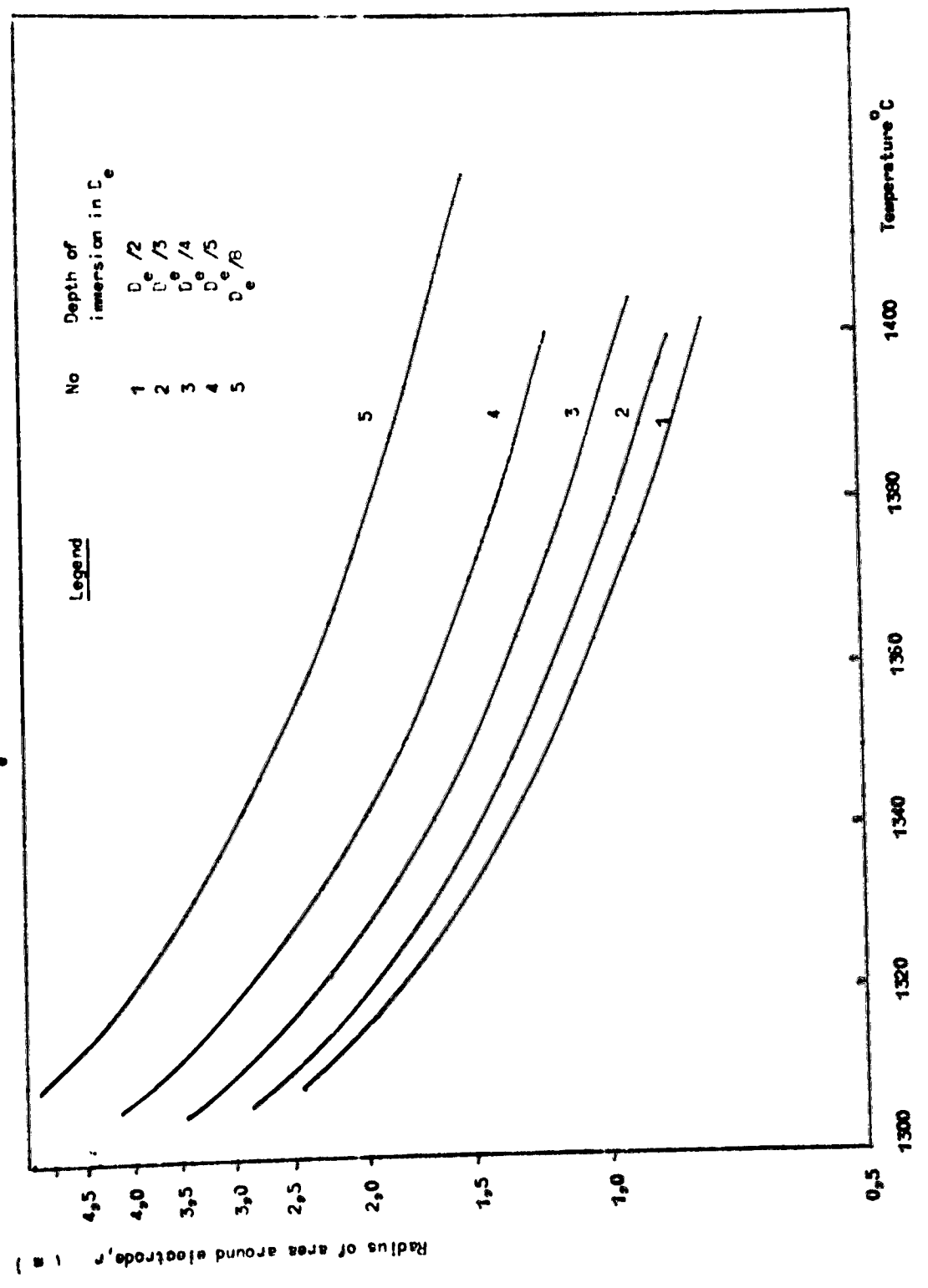
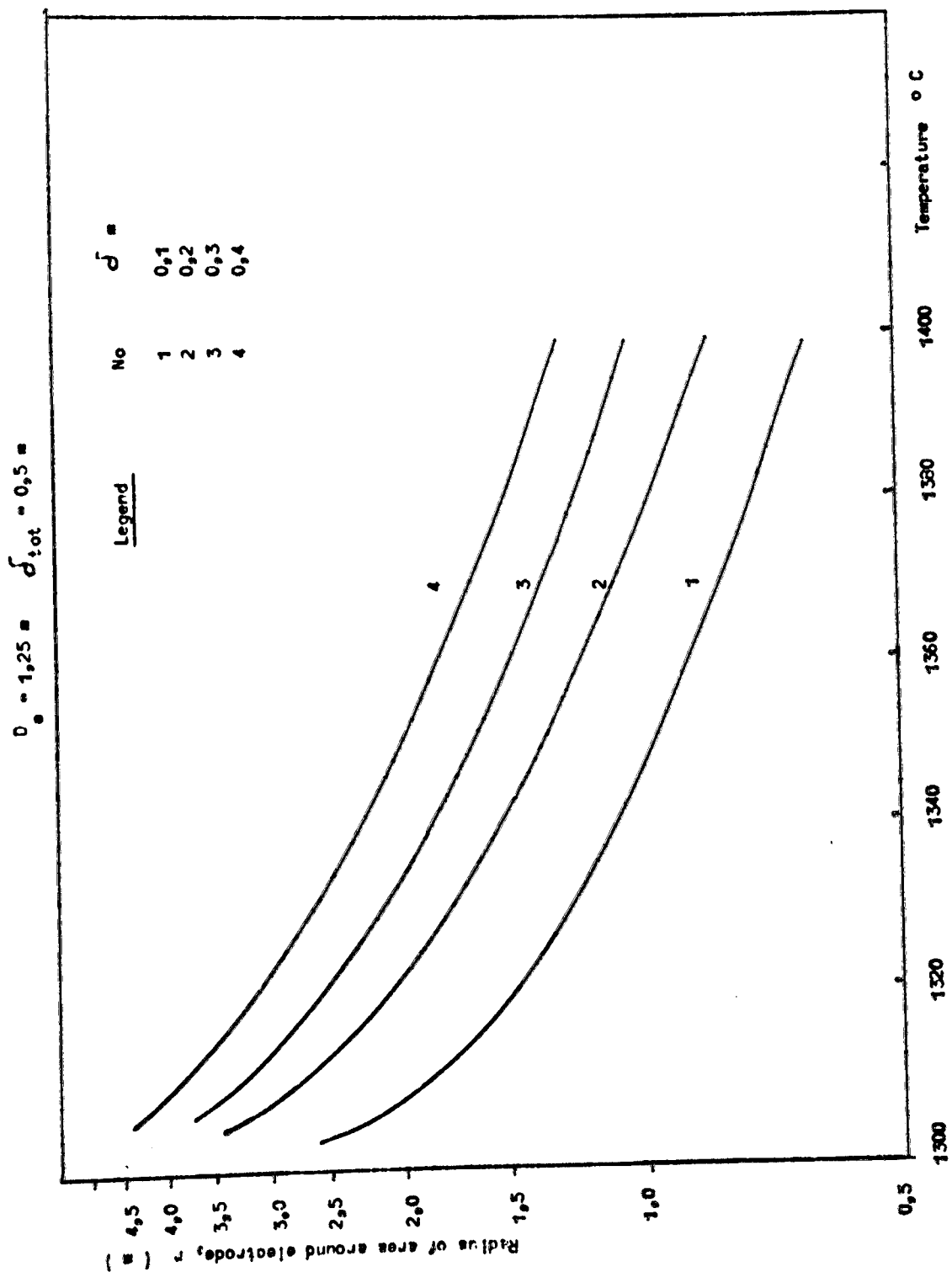


Figure VI - 5. EFFECT OF THE DEPTH OF CONSTANT TEMPERATURE LAYER IN THE SLUG UPON THE AREA OF TEMPERATURE DISTRIBUTION AROUND THE ELECTRODE.



of the extent of this zone would only be possible under steady state, therefore the present estimates can be regarded merely as reasonably good approximations of actually existing conditions.

5./ Effect of the depth of electrode immersion (p) in the slag.

Up till now the depth of electrode immersion was considered to be at its optimum i.e. about $p = D_e/2,5$ based on operating practice of the reference furnaces. Let us assume now that the composition and, as a result, the resistivity of the slag varies in such a way that this will give rise to variations in electrode position between $D_e/2$ and $D_e/5$ in terms of electrode diameter. Following the sequence of calculations outlined in Tables VI-2 and VI-3 and using equations 2 and 3 for optimum depth of immersion (p_{opt}) and constant temperature zone (J), the results shown in figure VI-4 and Table VI-5 can be obtained. The inverse relationship between active area and depth of electrode penetration is obvious and becomes more noticeable with the decrease of p.

In this context it has to be noted here that the decrease of the depth of electrode penetration into the slag will necessarily alter also the depth of the slag layer assumed to be at a constant temperature. The thickness of this zone as that of p depends on slag characteristics and at constant k_{th} , h, η and κ as well as operating current it will remain unchanged. A change in the electrical conductivity of the slag will alter the position of the electrode with the results described above. In this way the effect of the change in the depth of the constant temperature layer upon the active area will be synonymous with that imposed by the change of the depth of electrode immersion, followed by an inverse proportionality in slag heating. Results of calculations performed for $\alpha = 0,1, 0,2, 0,3$ and $0,4$ m with $D_e = 1,25$ m are given in Figures VI-5 and Table VI-6.

6./ Electrode current and power input.

According to Schwarz von Bergkempf (5) the electrical load on the surface of an object of circular cross-section is a constant value and depends on the character of the material. At identical surface temperatures it is given by

$$n_a = \text{const.} = \frac{I^2 \rho}{D^3 \pi^2 / 4} \quad 5./$$

from which he suggested the theoretical model relationship for the permissible load of cylindrical conductor at constant external surface temperature as

6./ ...

$$I\sqrt{\rho} = kD^{1,6} \quad 6./$$

where D is the diameter of the conductor in meters and ρ is its specific resistivity in ohm.cm. From practical plant data in case of Soederberg electrodes the following expression was suggested for a limiting current

$$I = (0,5 \times 10^4) D_e^{1,6} \quad 7./$$

where D_e is expressed in meters and the current I in amps. With the electrode of the reference furnace $D_e = 1,25$ m, the area of the flat-end surface $A_e = 1,23 \times 10^4 \text{ cm}^2$, from which the current density Δ_1

$$\Delta_1 = (5,0 \times 10^4)(1,25^{1,6}) / 1,23 \times 10^4 = 7,3 \text{ A/cm}^2$$

Based on observations conducted on a great number of electric furnaces Morkramer (6) established that the maximum current load on self-baking Soederberg electrode-types is a function of the electrode diameter according to

$$I_{\max} = 20,0 D_e^{1,7} \quad 8./$$

when D_e is in centimeters and I in amps.

Basically the current load on an electrode as this was already shown by Andreae is defined by the maximum permissible temperature to which the electrode surface above the melt can be exposed. When the energy generated above the charge as $I^2 R$ is in equilibrium with the energy dissipated along the surface, the following equation expresses the conditions obtaining (4):

$$k(t_e - t_a) \pi D_e L = \frac{4 \rho L}{\pi D_e^2} I^2 \quad 9./$$

here k = heat transfer coefficient to the atmosphere in watts/in²°C
 t_e = temperature of electrode surface, °C
 t_a = temperature of air, °C
 L = length of section of electrode above charge
 ρ = resistivity of electrode, ohm.in/in²
 I = current in amps

That is, above a certain temperature the electrode will burn in contact with air so t_e cannot exceed a certain limit. For long electrodes, such as the ones in closed furnaces the value of t_e must be chosen low, otherwise the temperature of the tip of the electrode becomes abnormally high. Andreae has found that for example a 76 cm dia. electrode which under normal conditions will carry 30,000 amperes could be loaded only with 19,000 amperes in a furnace where the electrode extended around 3 meters under the roof.

Besides these researchers a number of others, such as Kelly,

Pashkis and/...

Figure VI - 6. MAXIMUM LOAD ON SELF-BAKING SOEDENBERG ELECTRODES.
(after Mortensen)

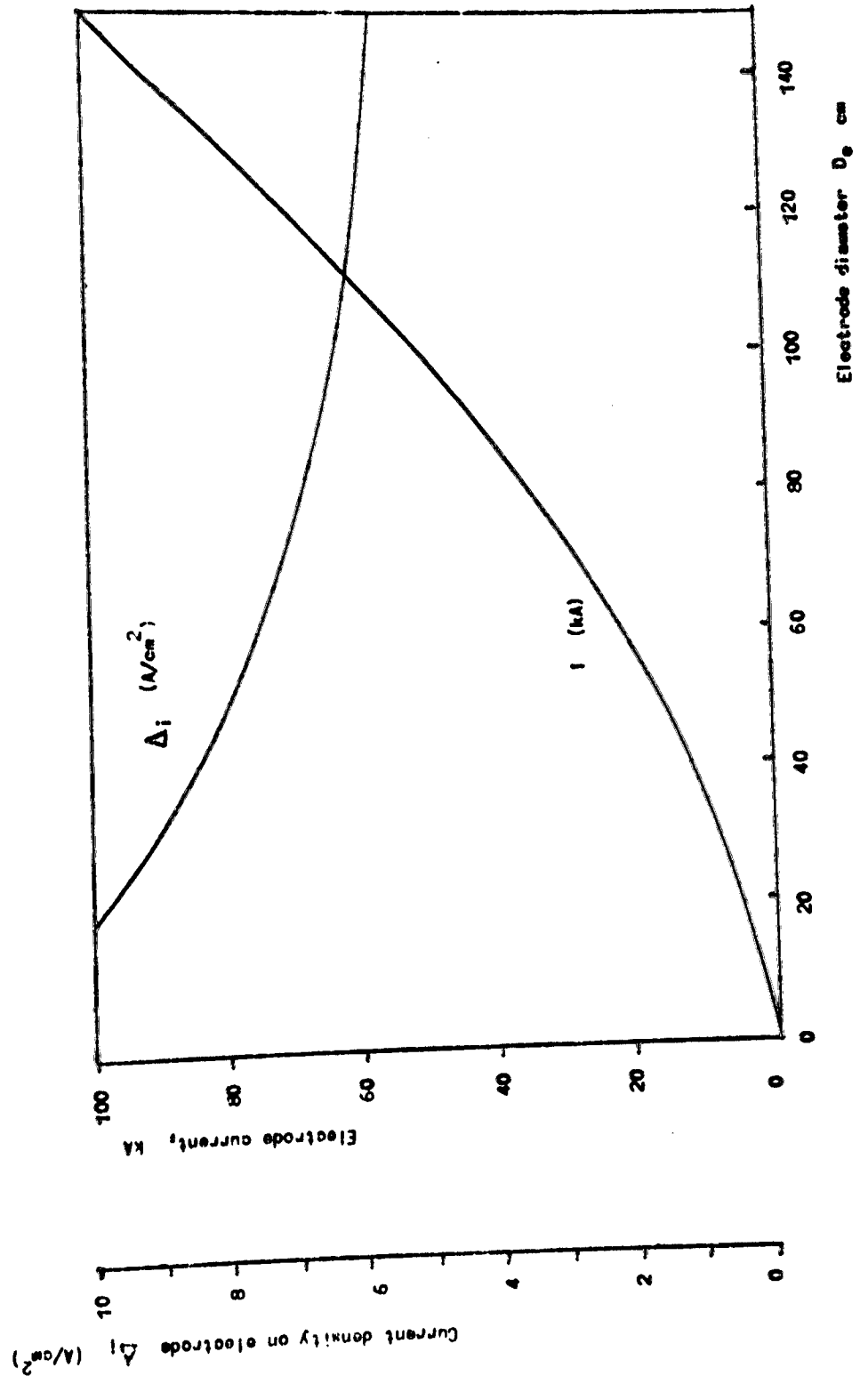


Table VI-7 Variation of power input with electrode size

D_e (m)	0,4	0,6	0,8	1,0	1,2	1,4	1,6
Electrode current kA	12	22	35	50	68	90	114
Power input MW							
maximum	7,3	15,8	21,8	31	42	55	71
operating	3,4	6,1	9,8	14	19	25	32

Table VI-8 Power input, operating current, and cell constant values as functions of electrode diameter.

D_e (m)	0,4	0,6	0,8	1,0	1,2	1,4	1,6
$P_w(\max)$ MW	3,35	6,1	9,8	14	18,7	24,7	32,0
I_f kA	4,4	7,3	10,6	14,2	18,5	22,5	27,2
I_f/D_e	11,0	8,2	7,6	7,0	6,5	6,2	5,9
G Sm^{-1}	6,51	4,34	3,36	2,6	2,17	1,85	1,63

Pashkis and Persson, Persson have proposed formulas for the electrode size-current carrying capacity relationship as will be discussed later. However, their equations refer specifically to the production of certain groups of ferroalloys. The expression advanced by Morkramer proved to be of more general validity, therefore in the present study the calculation of electrode current was based first of all on equ. VI-8. The relationship found by this author for various electrode diameters, electrode currents and current densities are given in graph VI-6. On the basis of the graph the electrodes of the reference furnaces with $2,65 \text{ A/cm}^2$ current density (7) and a maximum of $19,3 \text{ kA}$ operating current appear to be rather underloaded. From equ. VI-8 the maximum current density on electrodes of this size ($D_e = 1,25 \text{ m}$) would be about 6 A/cm^2 as against Bergkamp's $7,3 \text{ A/cm}^2$. In matte smelting furnaces in practice with electrodes of $D_e = 1,2-1,3 \text{ m}$ the current density may vary between $2,5$ and $3,0 \text{ A/cm}^2$ under optimum conditions with the particular slags used. Then with Morkramer's data on electrode current the maximum permissible power input and the operating power (MW_{max} and MW_{mean}) will vary as shown in Table VI-7. MW_{mean} was estimated on the basis of the range of current density quoted above for $D_e = 1,2$ to $1,3 \text{ m}$ at an average depth of electrode immersion of $p = D_e/2,5$.

In a further elaboration on operating current, in a recent paper Watley (8) gave the results of a detailed study on power input, electrode diameter and operating current with reference to ferroalloy producing furnaces. He showed that a constant relationship (C_A) exists among these parameters which, according to various workers can be expressed as follows:

$$C_A = \frac{P_w \cdot D_e}{I^2} \text{ (Andreae)} = \frac{P_w}{I \cdot D_e} \text{ (Persson)} = \frac{P_w}{I^{1,5}} \text{ (Westley)} \quad 9./$$

These relationships may be used for the estimation of electrode current in general, thus in the context of matte smelting furnaces when using Andreae's formula with say 19 MW and $18,5 \text{ kA}$ for C_A we get

$$C_A = [(19,0 \times 10^6)(1,25 \times 10^2)] / (1,85 \times 10^4)^2 = 6,95$$

while Westley's formula would yield $C_A = 7,47$. Then with the data of Table VI-7 relating to the estimated mean MW values for example in case of $D_e = 0,4 \text{ m}$ Andreae's constant will give

$$6,95 = \frac{(6,35 \times 10^6)(0,4 \times 10^2)}{I^2}$$

that is $I = 4,4 \text{ kA}$, while Westley's constant would give $4,23 \text{ kA}$.

As for a practical example, the slag resistivity in the present study was measured as $4,18$ to $4,67 \text{ ohm.cm}$ at 1400°C , with an average value for plant slags/...

Figure VI-7. SHIFT IN THE DIAMETER OF THE WORKING AREA OF THE ELECTRODE WITH THE ELECTRODE DIAMETER.

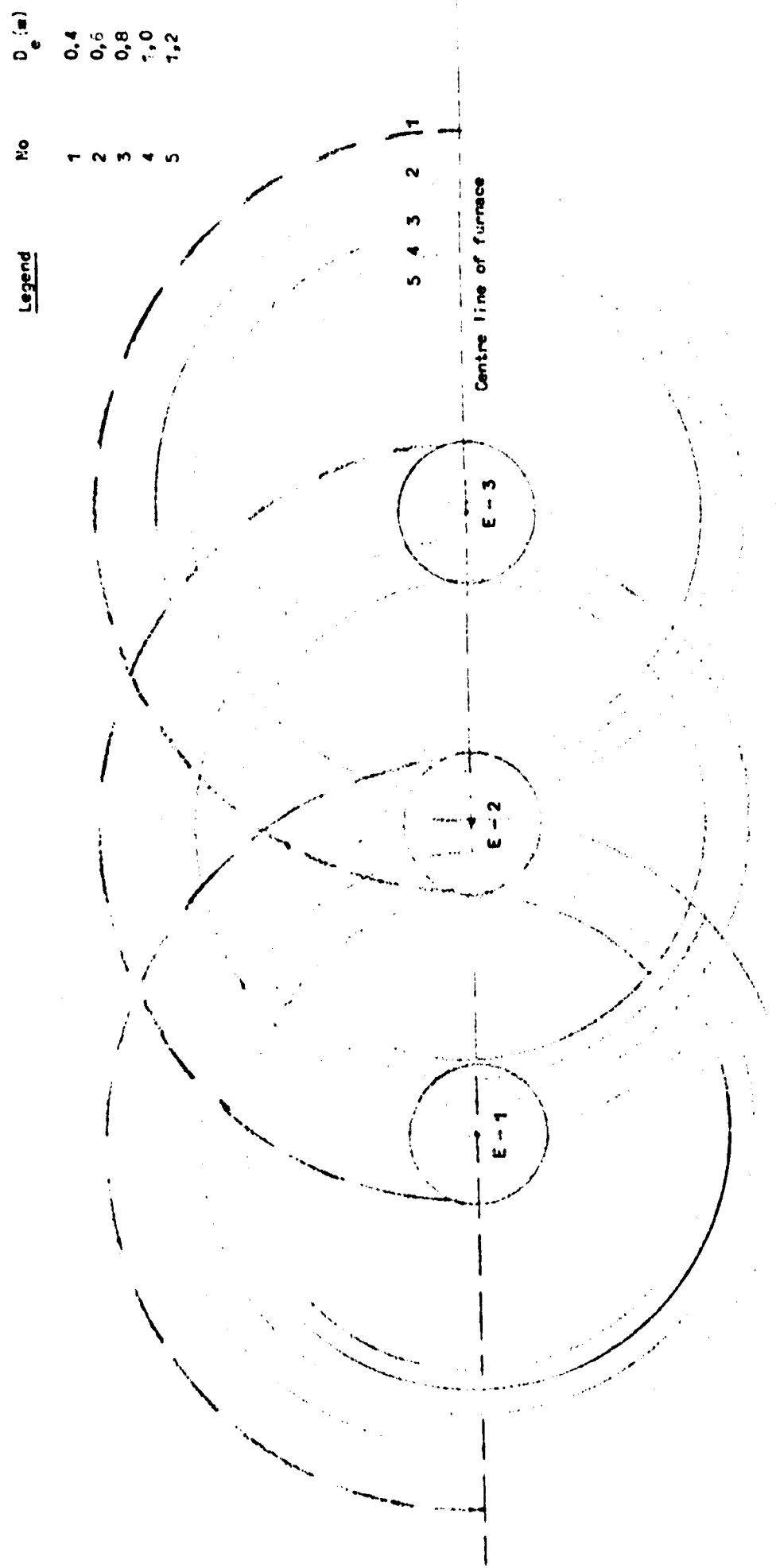


Figure VI - 8. CHANGE OF THE RATIOS i_r/D_0 AND d_A/D_0 WITH THE DIAMETER OF THE ELECTRODE.
RELATIONSHIP BETWEEN ELECTRODE DIAMETER AND HEARTH VOLTAGE.

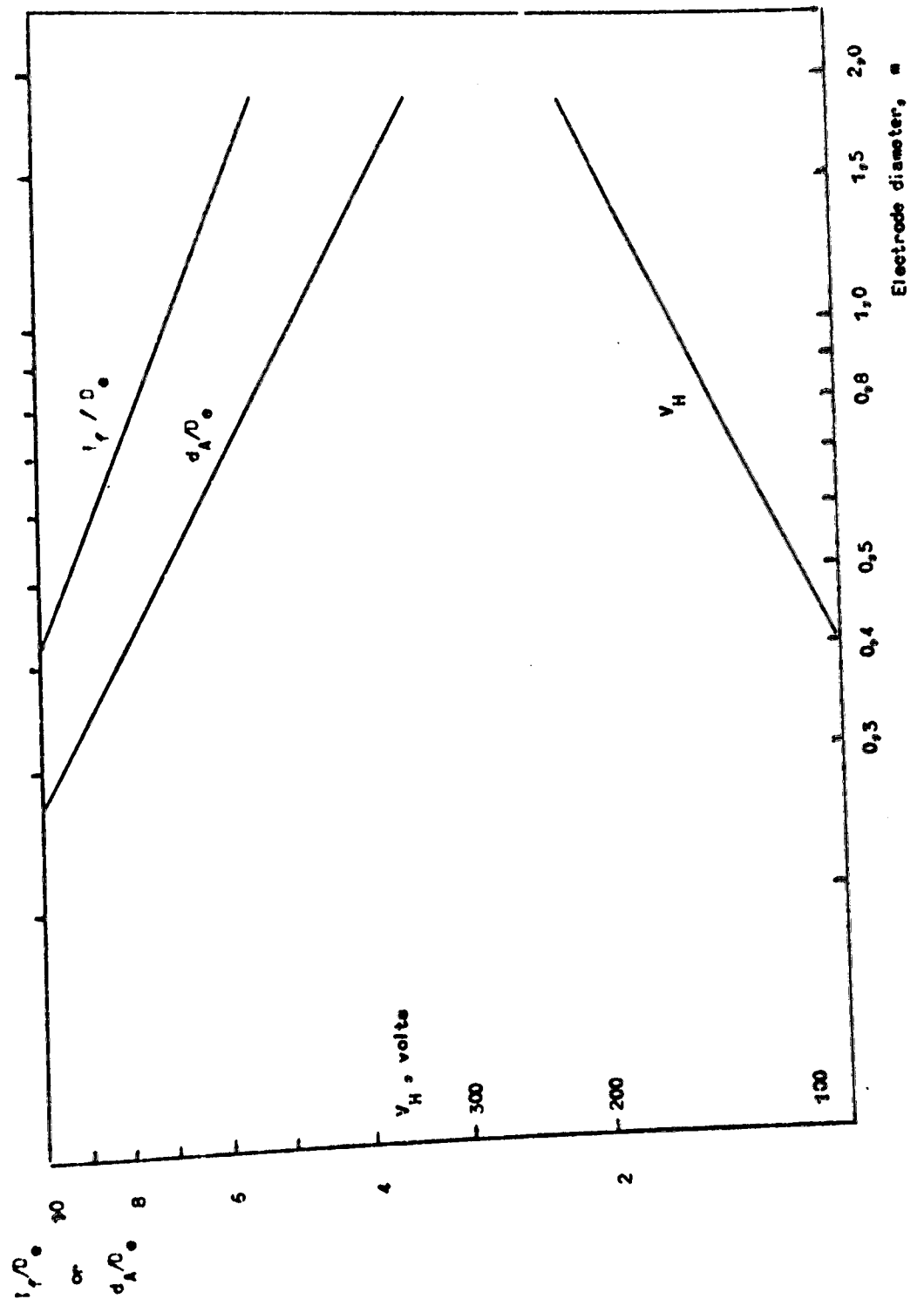
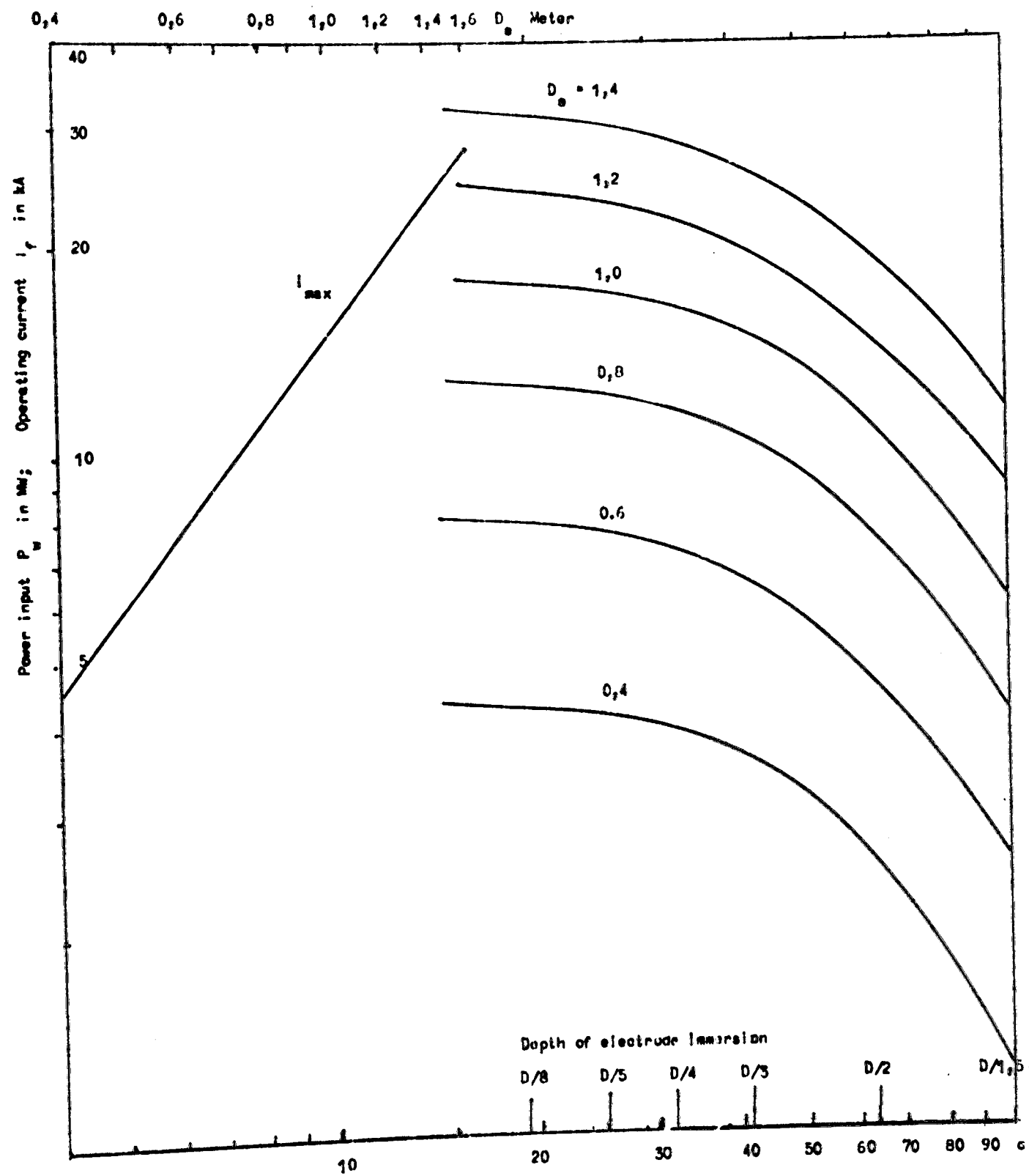


Figure VI - 9.

POWER INPUT AND MAXIMUM OPERATING CURRENT AT VARIOUS ELECTRODE DIAMETERS AND DEPTHS OF ELECTRODE IMMERSION INTO THE SLAG.



for plant slags of about 4,4 ohm.cm. Since $R_{sl} = kG$ and G the cell constant was calculated as $2,15 \text{ Sm}^{-1}$ at a depth of electrode immersion of $p = D_e/2,5$, then $R_{sl} = 4,4 \times 2,15 = 9,5 \text{ ohm.cm}$, that is 19,0 ohm.cm per phase in the context of the reference furnaces. Then with six electrodes $R = 3 \times 19 = 57 \text{ mohm}$ which would yield a power input of $P_w = I^2 R = (3,24 \times 10^8)(5,7 \times 10^{-2}) = 18,5 \text{ MW}$. In this way the cell constants, operating currents and furnace loads for various size electrodes can be estimated provided (that) the character of the slag produced is the same in all units. The results of calculations are compiled in Table VI-8. The values of G refer to an average electrode penetration of $p_{avg} = D_e/2,5$. (see also fig. VI-12).

The ratio I_f/D_e that is the relationship between operating current and electrode diameter derived from Andreae's considerations on maximum permissible current on the free section of the electrode exposed to the atmosphere above the charge (this decreasing with the decrease of furnace size) has a significant bearing on active electrode area - electrode diameter relationship, and accounts for the increase of the ratio d_A/D_e with the decrease of D_e . The extent of this shift is depicted in figure VI-7 showing three adjacent electrodes in the diameter range of 0,4 - 1,2 meter with their corresponding active areas. The ratio D_e/S that is electrode diameter and spacing (centre to centre distance) was kept constant in all cases. Figure VI-8 in a log-log representation shows the dependence of the ratio I_f/D_e and d_A/D_e respectively on D_e .

Thus the power input to the furnace is affected by electrode current, electrode diameter and depth of electrode immersion. The complex relationship between power input and depth of electrode penetration into the melt at maximum current (I_{max}) with the diameter of the electrode is summarised in figure VI-9.

7./ Hearth voltage, cell constant and electrode size

At a constant power input, with the automatic control of the electrode position the operating current will be raised or lowered following the course of change in slag resistivity. In this way the voltage between electrode tip and the surface of the matte should necessarily remain constant in practical operation for a given electrode size, that is

$$V (\text{electrode to hearth}) = K_v \quad 10./$$

Persson and Treilhard (9) described the relationship between hearth voltage and electrode diameter/...

and electrode diameter as $V_H = \sqrt{D_e}$ 11./

or $V_H = K_v D_e^{1/2}$ 12./

In case of the reference furnaces with a maximum phase voltage of 202 V (transformer secondary U-350V max.) the constant can be evaluated for the particular process and material used so that equ.12 will be written

$$V_{ph(max)} = (1,8 \times 10^2) \cdot D_e^{1/2} \quad 13./$$

when D_e is expressed in meters.

Further, according to Bockman (10) the relationship between furnace resistance, slag resistivity and electrode size is constant

$$R_f D_e / \rho = \text{const.} \quad 14./$$

or in an other form $P_w \rho / D_e V^2 = \text{const.}$ 15./

By virtue of $V^2 / P_w = R_f$ 16./

equations 14 and 15 are identical and solving 15 for V we get

$$V = \text{const.} (P_w / D_e)^{1/2} \quad 17./$$

so the electrode current will become

$$I = \text{const.} (P_w D_e)^{1/2} \quad 18./$$

From earlier considerations of this section the furnace power is expressed as

$$P_w = K_p D_e^{1,6} \quad 19./$$

At a given feed composition K_p depends on the depth of electrode immersion into the slag. In the range of electrode penetration of $D_e/2$ to $D_e/2,5$ the value of the constant varies between 12 to 22 and in average operating conditions K is about 16. Then for the present case equ.17 can be written in the form

$$V_H = c. (K_p D_e^{1,6})^{1/2} \quad 20./$$

that is the maximum hearth voltage, when D_e is expressed in cm is given by

$$V_H = 0,7 (K_p D_e^{1,6})^{1/2} \quad 21./$$

With raw materials having similar physico-chemical characteristics to those fed to the reference furnaces the maximum secondary voltage of the transformer at the highest tap position will be

$$U_{(max)} = 1,4 (K_p D_e^{1,6})^{1/2} \quad 22./$$

Further to the aspects of furnace symmetry Persson has shown (11) that the electrode-to-hearth voltage is inversely proportional to the square root of the electrode and, as indicated by equ.12, the relationship

is constant/...

is constant for a given material handled or produced in the furnace

$$V_H/D_e^{1/2} = \text{const.} \quad 23./$$

where V_H is the phase component of the electrode to hearth voltage. The constant involves the k factor and power density (watts per unit surface area) parameters. For example for copper matte production Persson gave the value : $V_H/D_e^{1/2} = 17,1$. As to the operating furnaces discussed in this study $U = 350V$ and $D_e^{1/2} = 11,2$ cm, then $(U/\sqrt{3})/D_e^{1/2} = 202/11,2 = 18,0$ for copper-nickel mattes as against the close value quoted for copper matte only.

In Section III equations 19 and 20 correlated the cell constant with power density and electrode diameter pertinent to slag resistance heated furnaces of rectangular shape having 6-in-line electrode arrangement. The basic concept is in accord with the foregoing proposal of Persson. In an other form the expression for cell constant was given as

$$k = \frac{(H - p)K_{ch}/\pi}{\frac{1}{4}\sqrt{\ln\left[\frac{S}{D_e} + \sqrt{\left(\frac{S}{D_e}\right)^2 - 1}\right]} + C_t} \quad 24./$$

where K_{ch} is the charge-dependent constant. Equ. 24 can be written in the following form:

$$\left[\frac{k(H - p)K_{ch}}{4\pi}\right]^2 = \ln\left[\frac{S}{D_e} + \sqrt{\left(\frac{S}{D_e}\right)^2 - 1}\right] + C_t \quad 25./$$

Let now $S/D_e = L_s$ for a given procedure (as defined by material, throughput rate, etc.) be constant, representing the length relations in the geometry as

$$\ln(L_s + \sqrt{L_s^2 - 1}) + C_t = \left[\frac{k(H - p)K_{ch}}{4\pi}\right]^2 \quad 26./$$

With the particular in-line electrode arrangement in rectangular furnaces where $L_s = 2,72$ and thus $\sqrt{L_s^2 - 1} = 2,53$ we can write $L_s + \sqrt{L_s^2 - 1} = 1 + 2,53/2,72 = 1,93 L_s$, therefore the left hand side of equation 26 becomes $\ln(1,93 L_s) + C_t$. The second capacitance term C_t , accounting for the action between electrode tips is also a constant value for the particular geometry and it was estimated before as being $C_t = 1,22 D_e$. Since $S = 2,72 D_e$, that is $S/D_e = 2,72 = L_s$, then $2,72/1,22 = 2,2$, and thus $2,2/2,72 = 0,82 L_s$. Then with these results equation 24. for the cell constant becomes

$$k = \frac{K_{ch}(H - p)}{\pi} \left[\frac{1}{4} \sqrt{0,82 L_s + \ln(1,93 L_s)} \right]^{-1} \quad 27./$$

where $L_s = S/D_e$, giving thus the relationship of the following parameters:
electrode/...

electrode diameter, electrode spacing, depth of electrode immersion into the slag and the total depth of the slag bed.

In a simpler formulation following the proposal of von Bergkamp(5) by which the furnace resistance

$$R_f = \rho \frac{K_{ch} \cdot L}{\pi D_e^2 / 4} \quad 28./$$

(see Section III), L being the length of electrode submerged in the melt and K_{ch} the charge-dependent constant, the cell constant may be expressed as

$$k = (H - p) \frac{K_{ch}}{\pi D_e^2 / 4} \quad 29./$$

K_{ch} in average plant operation and with average slag composition has the value of 2,5 to 2,7. The expression in its originally proposed form does not account for inter-electrode conduction and assumes that all current flows via electrode-mette-electrode. It is the constant K_{ch} based on experimental evidence then compared with the estimated resistance of the operating units, together with the (H-p) term that make the necessary corrections and render thereby equ. 29 reasonably accurate for the assessment of furnace resistance.

8./ Relationship between furnace resistance and operating current.

When investigating the relationship between furnace resistance and operating current in FeSi furnaces having various electrode diameters (0,9 to 1,55m) and run on burdens of varying composition, for minimum power consumption and best operating range Westley (8) arrived at the expression

$$R_p^{1/3} = \text{const.} \quad \text{or} \quad I/P_w^{2/3} = \text{const.} \quad 30./$$

The formulas were tested in the context of matte smelting furnaces working with slags of resistivities in the range of $R_{sl} = 4,2$ to $4,7$ ohm.cm at 1400°C . Using the data of Table VI-8 and the calculated resistivities, the relationship between operating current and furnace resistance in terms of electrode diameter as related to matte smelting furnaces can be expressed as

$$C_{D_e} = P_w^{0,6} \cdot R \quad 31./$$

In a similar way for optimum performance the constant was found to be

$$C_{D_e} = \frac{I}{P_w^{0,8}} \quad 32./$$

9./ General scale-up/...

Figure VI -- 10. ELECTRODE DIAMETER vs. ACTIVE AREA (d_A), FURNACE WIDTH (l_w), and ELECTRODE SPACING (S)

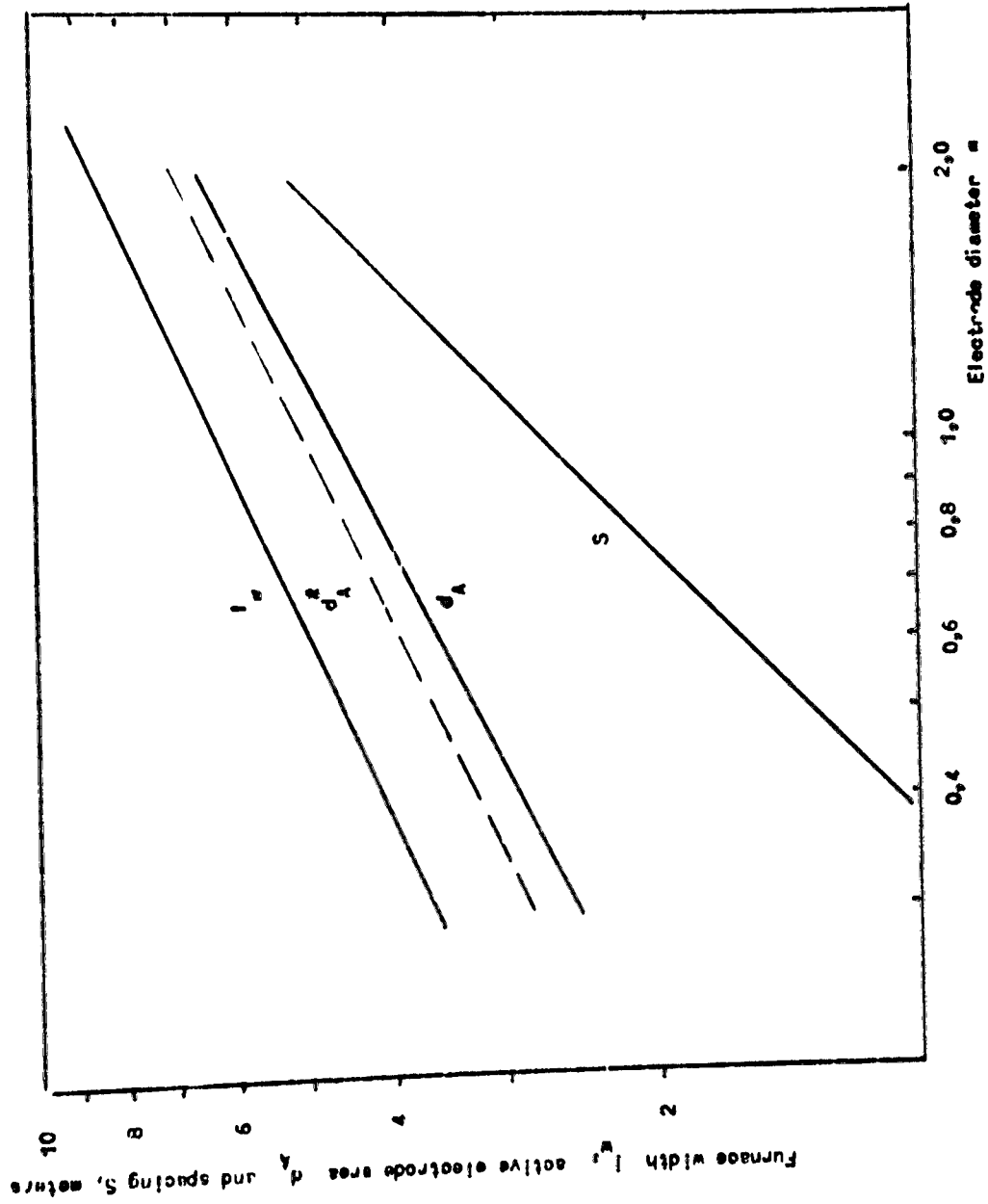


Figure VI - 11. POWER INPUT AS A FUNCTION OF ELECTRODE DIAMETER,
 DEPTH OF ELECTRODE IMMERSION AS A PARAMETER.
 Furnace current I_f and I_K shown for comparison

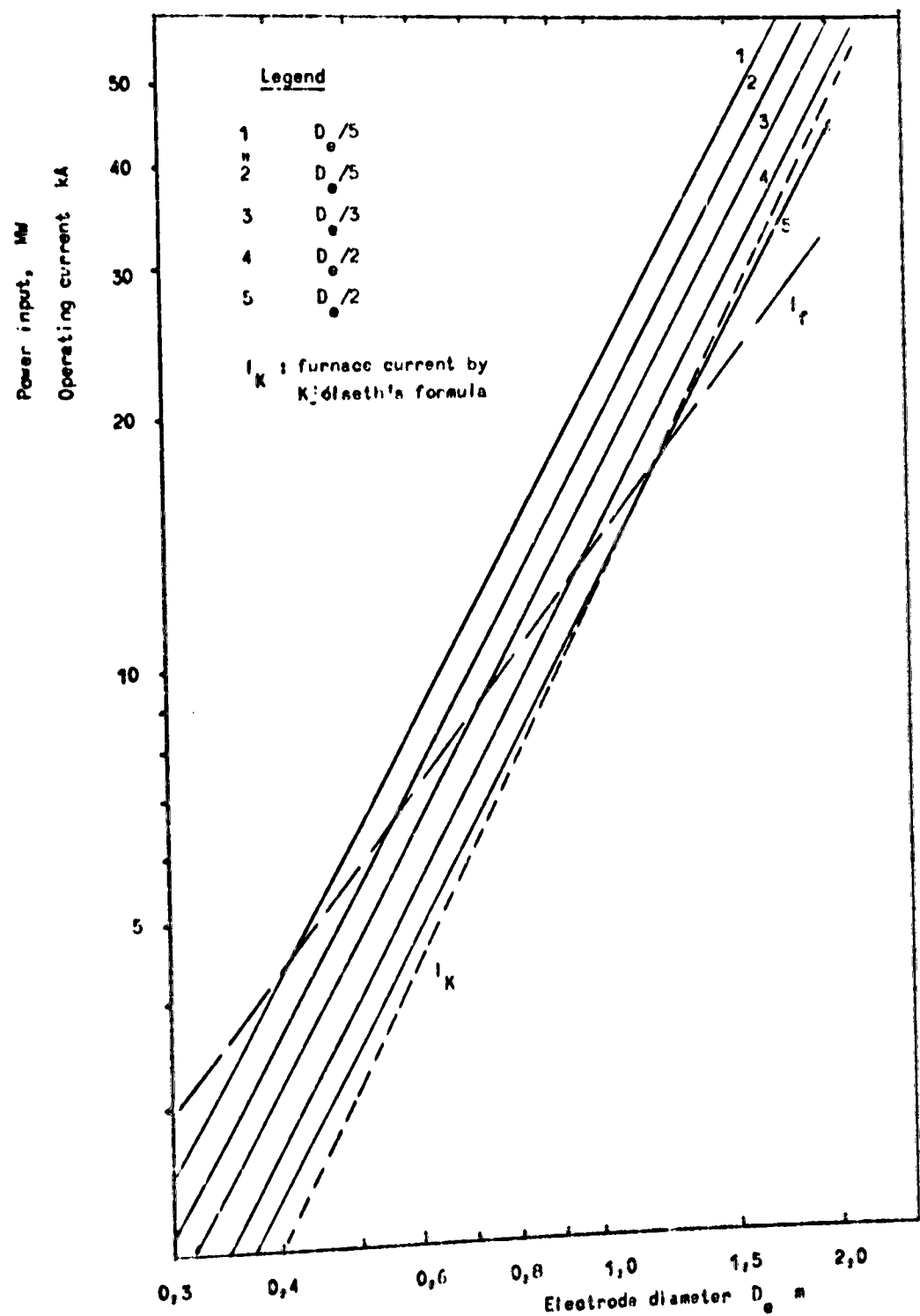


Figure VI - 12. OPERATING CURRENT, CELL CONSTANT AND ELECTRODE DIAMETER RELATIONSHIP.

Depth of electrode immersion $p = \text{const.} = D_e/2,5$

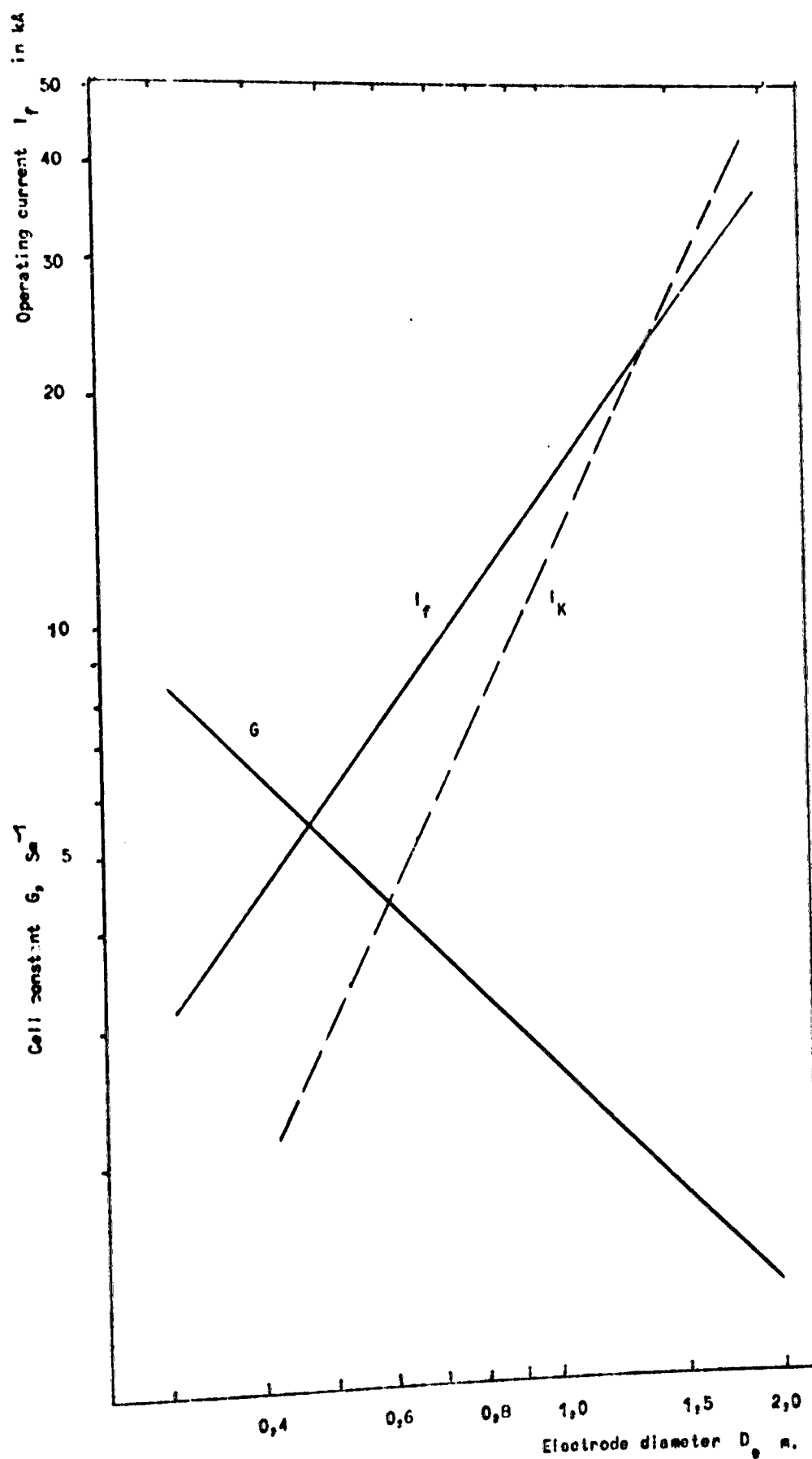


Figure VI - 13. ELECTRODE CURRENT DENSITY AS A FUNCTION OF ELECTRODE DIAMETER.

Estimated by the formulas of

- 1 Morkeamer
- 2 Kelly
- 3 Modified expression of Kelly for the condition of the model furnaces at PPM, from equ. 25.)

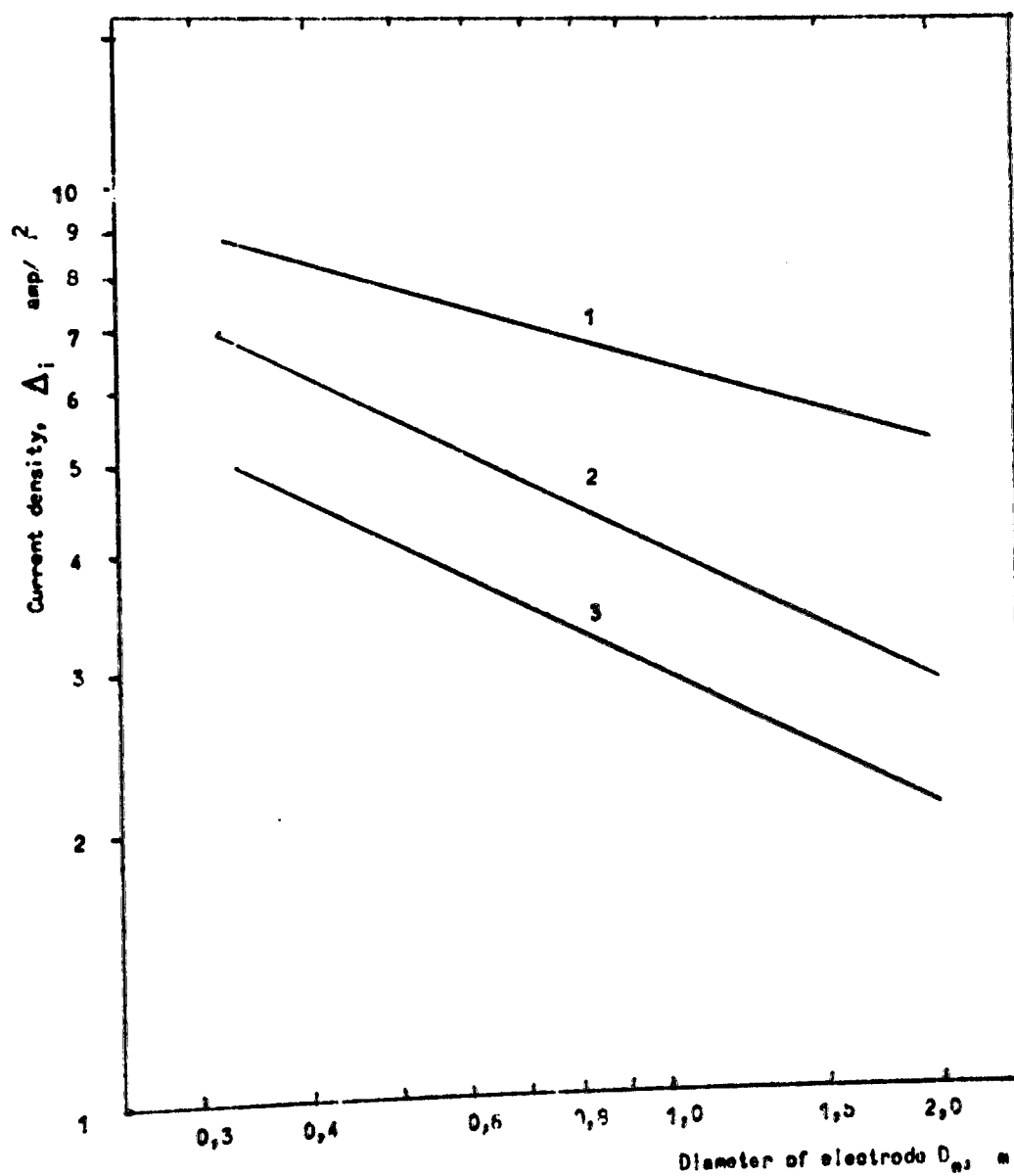


Figure VI - 13. ELECTRODE CURRENT DENSITY AS A FUNCTION OF ELECTRODE DIAMETER.

Estimated by the formulas of

- 1 Morkeamer
- 2 Kelly
- 3 Modified expression of Kelly for the condition of the model furnaces at RPM, from equ. 25.)

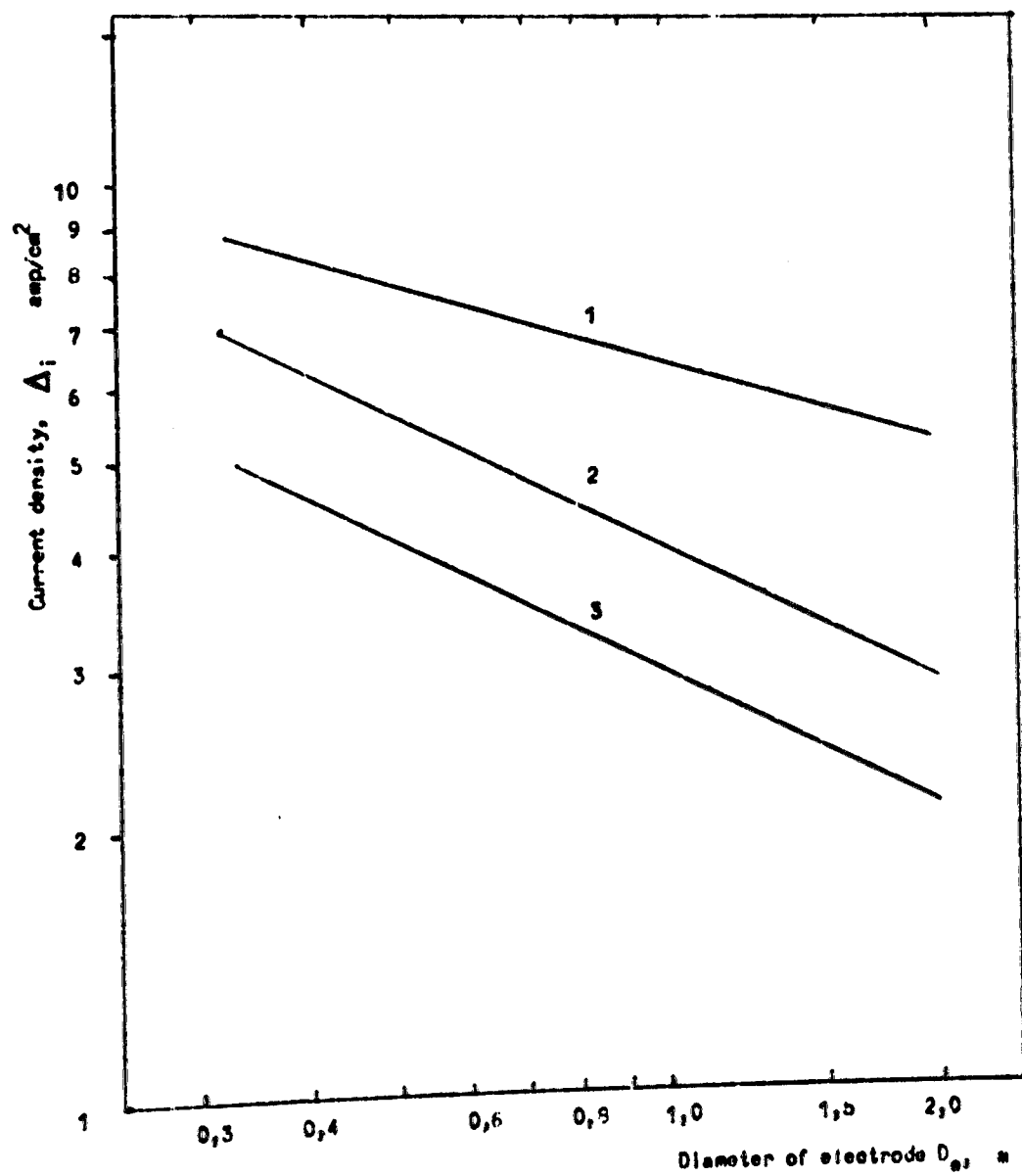


Table VI-9. Some basic equations in the form $y = K(x)^n$ representing furnace symmetry and operating variable relationships.

Parameters related		Equations
1	2	
l_w	D_e	$l_w = 6,3 D_e^{0,47}$
i_A	D_e	$d_A = 5,0 D_e^{0,49}$ to $0,53$
S	D_e	$S = 2,7 D_e$
S	d_A	$d_A = 2,9 S^{0,82}$
H	D_e	$H = 1,02 D_e^{0,98}$
h	D_e	$h = 0,66 D_e$
p	D_e	$p = 0,375 D_e^{1,1}$
$P_w (MW)$	D_e	$P_w = KD_e^{1,6}$
	$p = D_e/2$	$P_w = 12 - 14 D_e^{1,58}$
	$p = D_e/3$	$P_w = 16 D_e^{1,67}$
	$p = D_e/5$	$P_w = 19 - 22 D_e^{1,58}$
G	D_e	$G = 2,6 D_e^{-0,98}$
$I_f (kA)$	D_e	$e^{1,3} \quad K = 13 \text{ to } 15$

9./ General scale-up and design parameters related to the size of the electrode.

From the foregoing discussion it is obvious that with the size of the electrode all the other operating variables and factors determining furnace geometry will change according to a certain proportion. When the relationships are plotted on log-log paper, a simple expression of the form $Y = Kx^n$ can be obtained where both K and n are constants with K representing the character of the material handled in the furnace and n the symmetry relationships or scale-up factors. The method was extended to further variables and the results of calculations in a log-log representation are given in figures VI-10 to 12, and the calculated formulas in Table VI-9.

10./ Comparison with other works.

Kelly (2) made further elaboration on the k factor concept of Andreae and arrived at useful relationships pertinent to furnace design parameters, primarily in the field of ferroalloy producing submerged arc furnaces. The electrode current density Δ_1 was defined on the basis of a large number of furnace data as

$$\Delta_1 = \frac{250}{\sqrt{D_e}} \text{ amp/in}^2 \quad 33./$$

or in square centimeters $\Delta_1 = D_e^{-0.5} (0.4 \times 10^2) \text{ amp/cm}^2 \quad 34./$

Δ_1 vs D_e is shown in fig. VI-13 plot 2.)

The maximum current density that can be carried by a Söderberg electrode as determined by Morkramer was illustrated in fig. VI-6. The limiting values given by him in fig. VI-43, plot 1.) are considerably higher than those of Kelly's. With the size of electrodes of the R&M furnaces the "high edge range" (as Kelly termed the upper values) of current density would correspond to 3,54 amp/cm².

For practical considerations, in matte smelting furnaces plot 3.) represents the relationship between current density and electrode diameter for operating conditions similar to those at R&M. However, current densities in the range between plots 1.) and 2.) may safely be used in these type of units especially in case of larger electrodes. The equations of the plots in fig. VI-13 can be given as

1./ Morkramer $\Delta_1 = 6,35 D_e^{-0,28} \text{ amp/cm}^2 \quad 35./$

2./ Kelly $\Delta_1 = 4,0 D_e^{-0,48} \text{ " } \quad 36./$

c./ matte/...

Figure VI -14.

COMPARISON OF RESULTS OBTAINED BY USING KELLY'S FORMULAS
AND THE BESSEL FUNCTION METHOD FOR THE ESTIMATION OF REACTION
ZONE DIAMETER AND POWER INPUT TO THE FURNACE.

Legend

- 1 maximum
- 2 optimum reaction zone by Kelly
- 3 minimum
- K : Kelly's formula
- B : Bessel function method

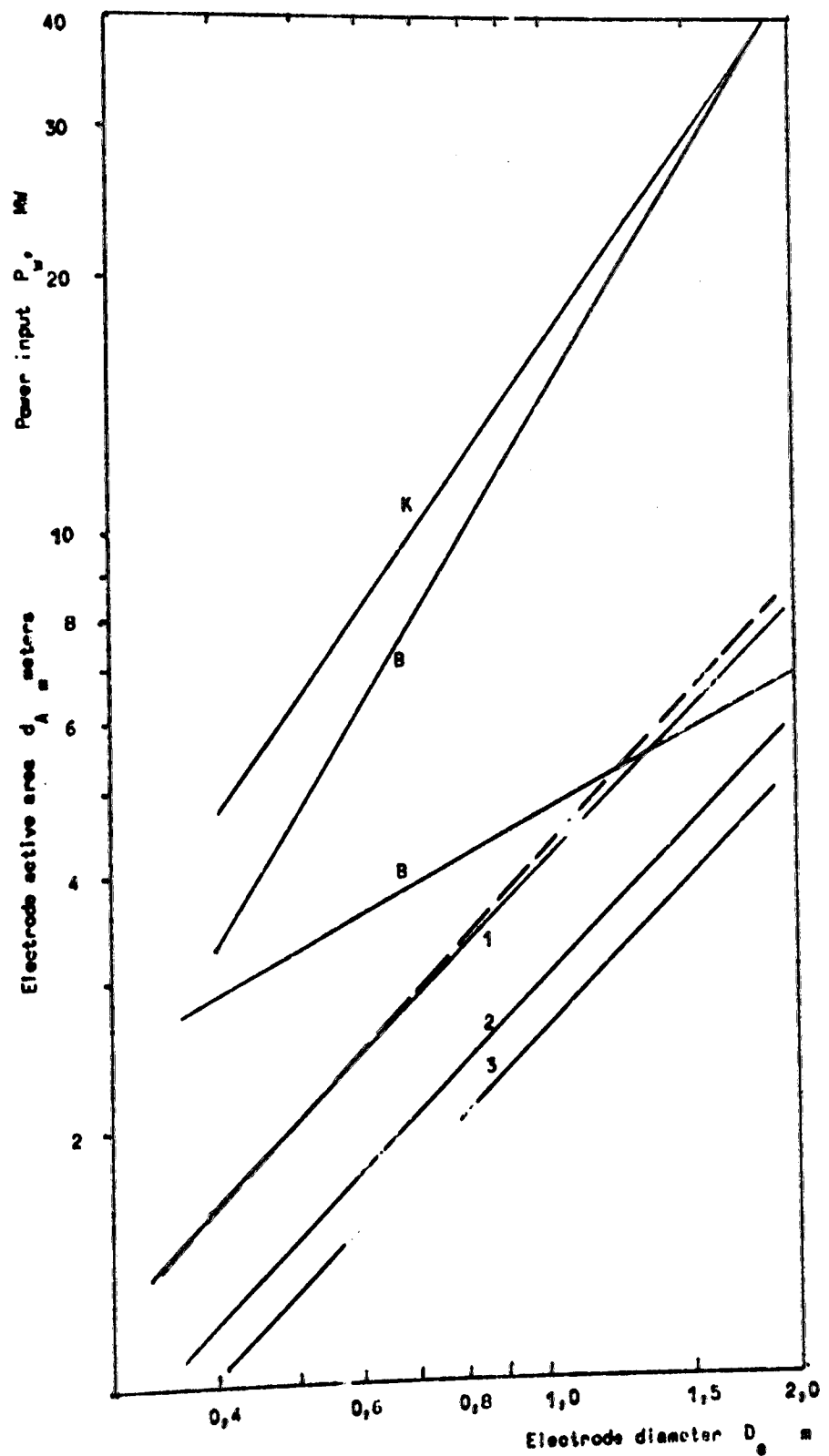
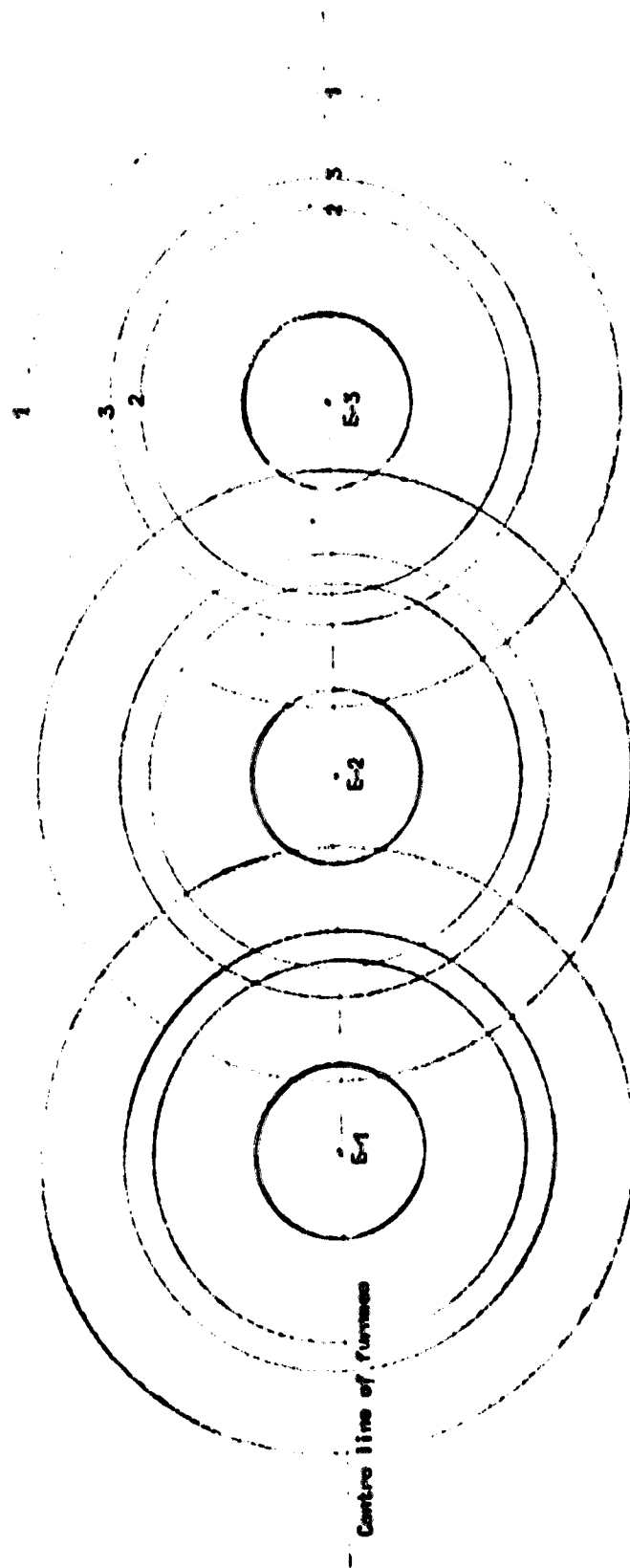


Figure VI - 15. MAXIMUM (1), MINIMUM (2), AND OPTIMUM (3) REACTION ZONE (d) CALCULATED BY KELLY'S FORMULAS AND PROJECTED ONTO THE CONDITIONS OF A FURNACE HAVING 6 ELECTRODES IN -LINE ARRANGEMENT.
(only 3 electrodes are shown in the graph)



3./ Operating range of matte smelting furnaces $\Delta_1 = 2,95 D_e^{-0,53}$ 37./

Equation 37 representing the average matte smelting operating range was obtained by applying Kelly's formula to the particular conditions of matte smelting. The expression can also be written as

$$\Delta_1 = D_e^{-0,53} (0,3 \times 10^2) \text{ amp/cm}^2 \quad 38./$$

As for a different and more specific formulation, in the present study it was found that the relationship between current density, operating current (from Table VI-8) and electrode diameter in the special case of matte smelting furnaces with the particular type of slags handled can be well-approximated by the following formula

$$D_e = 2,65 \times 10^{-2} \sqrt{\frac{I_f}{\pi \Delta_1}} \text{ meter} \quad 39./$$

Then with this expression the formula for current density becomes

$$\Delta_1 = (7,0 \times 10^{-4}) I_f (\pi D_e^2)^{-1} \text{ A/cm}^2 \quad 40./$$

That is in case of say 18,0 kA

$$\Delta_1 = (7,0 \times 10^{-4}) (1,8 \times 10^4) (3,14 \times 1,25^2)^{-1} = 2,58 \text{ A/cm}^2$$

In estimating the active area, Kelly regarded the electrode spacing i.e. the distance between the centre lines of the electrodes instead of their diameter as a reference parameter and defined maximum, minimum and optimum reaction zones for furnaces of circular cross section. The proposed formulas for active electrode areas and furnace diameters were as follows:

	Maximum	Minimum	Optimum
d_A	$2,0 S - D_e$	$1,0 S$	$1,16 S$
D_f	$2,3 S - D_e$	$2,16 S$	$2,3 S$

In terms of electrode diameters only, these equations can be expressed as

d_A	$4,1 D_e$	$2,56 D_e$	$2,96 D_e$
D_f	$7,07 D_e$	$5,53 D_e$	$5,89 D_e$

The estimated reaction zones in the range of electrode diameters investigated would then be as shown in fig. VI-14, with B denoting the results calculated by Bessel function method. If the diameters of the reaction zones obtained by Kelly's formulas were drawn in relation to the 6-in-line electrode configuration of the rectangular units, the symmetry conditions shown in fig. VI-15 would result. Apparently, for larger electrodes ($D_e \geq 1,0m$) there is a reasonable/...

a reasonable comparison between the active areas in circular vs rectangular furnace symmetry relationship when the average melting point of the plant slag is around $1320 - 1330^{\circ}\text{C}$ and Kelly's formula relating to maximum reaction zone is used. Below $D_e = 1,0$ m the dimensions tend to deviate more and more from each other.

As regards to the change of the maximum reaction zone ($d_{A(\max)}$) with electrode diameter the relationship can be closely approached by multiplying the active area estimated by Bessel function ($d_{A(B)}$) with the square root of the ratio between the diameter of the reference electrode and the diameter of the electrode in question, that is

$$d'_{A(\max)} = d_{A(B)} (D_{e(\text{ref})}/D_e)^{\frac{1}{2}} \quad 41./$$

where $D_{e(\text{ref})} = 1,25$ m and D_e denotes any electrode size within the range of 0,4 to 2,0 m. The plot is shown in fig.VI-14 by dotted lines and its equation will be

$$d'_A = 4,2 D_e^{0,94} \quad 42./$$

which is very close to that given in the table above for $d_{A(\max)}$.

By the original concept of Kelly the relationship between reaction zone and furnace diameter is expressed as $D_f = 2 d_A$ and the electrode spacing as $S = \sqrt{3}/(2 d_A)$. In this way the diameter of the active area would be proportional to the square root of the power input, which in case of matte smelting furnaces is clearly not the case. One of the main shortcomings of Kelly's analysis, as Coetze pointed out (12) was that the designer still had the problem to find a value for d_A on an existing furnace working on a raw material to be used in a new furnace as far as the problem of simulation was concerned. The exact determination of the working area of the electrode, apart from the electrode area itself, will depend largely on the character of the burden, such as for example porosity, this being an especially important factor in the production of ferroalloys and silicon carbide. Further, it may also be related to the electrode current density, although the inverse proportionality between the two variables is not much of a help in design and may even lead to confusion.

Pertaining to the power input - electrode diameter relation Kelly suggested that, as it was mentioned above, the power input is proportional to the square root of the active area of the electrode, that is

$$P_w \propto d_A^2 \quad 43./$$

Kjølseth (13) proposed the formula / . . .

Kjølseth proposed the formula (13)

$$P_w = K D_e^2 \quad 44./$$

and Nilsen added his own: (14) $P_w = D_e^{2,79} \quad 45./$

From these proposals Kjølseth's expression appears to be the closest to those relating to matte smelting furnaces with the exponent of D_e varying from 1,75 to 2,0 and K from 12 to 22 with a change of the depth of electrode immersion between $D_e/2$ to $D_e/5$.

The value of K in equ.44 when applied to the conditions of the RPM furnaces with a power input of 19 MW was $K = 12,15$. The corresponding plot of MW vs D_e is shown in figure VI-11, with dotted lines. As it is indicated, the trend for electrodes larger than 0,6 m dia. is similar or virtually identical with that obtained from Morkramer's formula relating to increased depth of electrode penetration and lower range of operating current. The equation of the line is

$$P_w = 12,15 D_e^{2,05} \sim 12,2 D_e^2 \quad 46./$$

By the principle of similarity Kelly's argument, relating to electrode spacing and power input rate, can be applied to the estimation of the electrical load of any desired size furnace in the knowledge of the two parameters in one particular unit (15). Let S_1 and S_2 be electrode spacings with P_{w-1} and P_{w-2} the respective electrical loads, then

$$\frac{S_2^2}{S_1^2} = \frac{P_{w-2}}{P_{w-1}} \quad \text{and} \quad S_2 = S_1 \left(\frac{P_{w-2}}{P_{w-1}} \right)^{\frac{1}{2}} \quad \text{or} \quad P_{w-2} = \left(\frac{S_2}{S_1} \right)^2 P_{w-1} \quad 47./$$

Similarly, if D_{f-1} and D_{f-2} are the respective furnace diameters then

$$\frac{D_{f-2}}{D_{f-1}} = \left(\frac{P_{w-2}}{P_{w-1}} \right)^{\frac{1}{2}} \quad \text{and} \quad D_{f-2} = D_{f-1} \left(\frac{P_{w-2}}{P_{w-1}} \right)^{\frac{1}{2}} \quad 48./$$

A comparison between power input rates with the change of electrode size obtained with the use of these equations and those estimated by the writer of this study is given in fig. VI-14 by plots K and H. As will be seen, with the increase of the electrode diameter above $D_e = 1$ m the difference between the power input rates estimated by the two methods becomes less and less significant.

Furnace width. Pertinent to furnace width as applied specifically to units of rectangular shape Persson and Treilhard (9) suggested a formula representing relations between width and area (therefore also length) with the inclusion of the number of electrodes as

$$l_w = \sqrt{\frac{2A}{n+1}}$$

49./

which, in the context of the model furnaces of RPM would yield

$$l_w = \sqrt{\frac{2 \times 26 \times 7}{6+1}} = 7,2 \text{ m}$$

from which the length $L_f = A/l_w = 182/7,2 = 25,3 \text{ m}$

The expression was arrived at from practical considerations mainly as the result of expected maximum permissible drop in product level (matte or metal) and electrode travel aspects. From the point of view of the present investigations it could hardly be regarded as a true design equation, since it lacks the main reference parameter, i.e., D_e and contains simultaneously two unknown variables, width and area, the inter-dependence of which is ill-defined without the knowledge of the electrode diameter.

In the present investigation it was shown how the active area and the width depend on the electrode size. The relationship was found to be non-linear. As a result, the electrode diameter - active area - furnace width - length - area relationship was defined separately for smaller furnaces, these being regarded as having electrodes with $D_e < 0,8 \text{ m}$ and larger furnaces with $D_e > 0,8 \text{ m}$ as follows (all in meters):

Smaller furnaces $D_e < 0,8 \text{ m}$ $l_w = d_A + 1$ 50./

$L_f = 4 d_A + 1$ 51./

$A_f = L_f \times l_w = 4 d_A^2 + 5 d_A + 1$ 52./

Larger furnaces $D_e > 0,8$

$l_w = d_A + 1,8$ 53./

$L_f = 5 d_A = 21 D_e$ 54./

or in terms of electrode spacing since $S = 2,7 D_e$

$L_f = 7,8 S$ 55./

$A_f = L_f \times l_w = 5 d_A (d_A + 1,8) = 5 d_A^2 + 9 d_A$ 56./

where $d_A = 4,2 D_e$ or $r_{d_A} = 2,1 D_e$

Then in the symmetry relationships of the model furnaces we have

$$A_f = (5 \times 5,2)^2 + (9 \times 5,2) = 182 \text{ m}^2$$

and the area of active reaction zone A_{rz}

$$A_{rz} = (2,1 D_e)^2 \pi = 21,7 \text{ m}^2$$

and $A_f = 182/21,7 = 8,4 A_{rz}$ 57./

It is interesting to/...

Table VI-10. Furnace geometries and electrical load of various matte smelting furnaces of rectangular shape with 6 electrodes in-line arrangement.

Plant	D _e	l _w	L _f	P _w	P _w /m ²
		in	meters	MW	hearth area
Impala Platinum Mines, S. Africa	0,89	6,0(7,2)	20,4	7,5	-
Boliden, Sweden	1,20	6,0	25	12	85
Thompson Smelter, Canada	1,22	6,7	27,4	18,5	107
Rustenburg Platinum Mines, S.Afr.	1,25	7,0	26	19	93-95
Zambia (Copper Belt)	1,50	10,4	31,4	36	-
Russian Plants 1.	1,05	5,5	21,0	12	100
2.	1,20	6,0	23,6	18	92

It is interesting to compare the outlined symmetry relationships with Russian conventions. Among the Russian designers it was Diomidovskii the first (16) to apply the concept of active, or working area of the electrodes. For 3 and 6 electrode rectangular furnaces he gave the following dimensional data (17)

	6 electrodes	3 electrodes
Electrode spacing	3 D_e	3 D_e
Internal width of furnace	6 D_e	6 D_e
Length of furnace	21 D_e	12 D_e
Active area of electrode	-	5 D_e

Apparently these measures are close to identical with those used in Western practice for the design of rectangular furnaces with the electrode spacing being somewhat larger in Russian convention.

Since furnace symmetries and operating parameters are based on the character and behaviour of the raw material to be treated in the process of smelting, design parameters might vary somewhat from process to process. This is illustrated in Table VI-10 compiled from available literature data on operating units.

11./ Depth of the molten slag bath (H) and electrode penetration (p) vs D_e .

A further important parameter forming part of the furnace geometry is the depth of the molten slag bath (H). This is determined primarily by a.) the amount of gangue contained in the furnace feed and b.) the resistivity of the slag which, on the other hand, determines the depth of electrode immersion (p), i.e. the distance between the tip of the electrode and the matte (or metal) surface. In dimensional relationship Pashkis and Persson (18) suggested a constant ratio between the distance h, the diameter of the electrode and the resistivity of the slag equally applicable to both circular and rectangular furnaces as

$$\frac{hD_e}{R_{sl}} = \text{const.} \quad 58./$$

or
$$hD_e = R_{sl} = \text{const.}$$

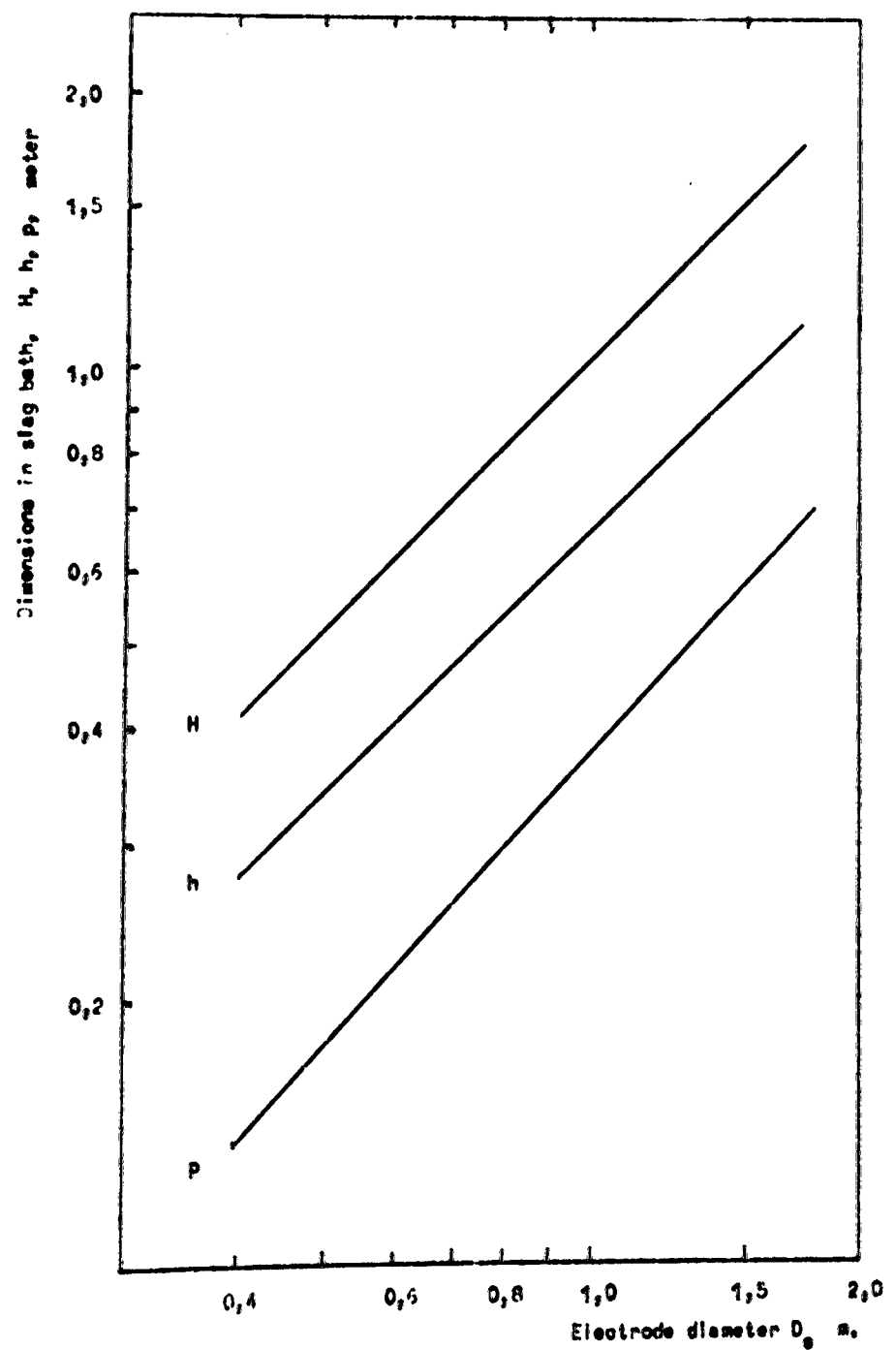
In other words the product of phase resistance and electrode diameter is a constant value for a particular process. Nothing is new about this relationship since it is an other form to express Andreae's electrode periphery resistance concept. Obviously the value of the constant will depend first of all on the character of the slag produced. With slag resistivities similar to those at RPM (4,2 to 4,7 ohm.cm, avg 4,4 ohm.cm) the value of the constant was calculated as $C = 2,33 \times 10^3 \text{ cm.ohm}^{-1} \cdot \text{cm}^{-1}$ for the average resistivity/...

Figure VI - 16.

ESTIMATED OPTIMUM BETWEEN ELECTRODE SIZE AND SLAG BATH DIMENSIONS
WHEN PROCESSING BURDEN OF IDENTICAL OR SIMILAR CHARACTER TO THAT
USED IN THE MODEL FURNACES.

Legend

- H total depth of slag bed
- h distance between electrode tip and slag-matte interface
- p depth of electrode penetration into the slag
- (all dimensions in meter)



resistivity and $2,2 \times 10^3$ to $2,45 \times 10^3$ cm.ohm⁻¹cm⁻¹ for the quoted range of slag resistivities. Based on optimum operating conditions the estimated dimensional relationship between electrode diameter and total slag depth, then electrode to matte distance and depth of electrode penetration are plotted in figure VI-16, the characteristic equations being

$$\begin{aligned} H &= 1,02 D_e^{0,98} & 59./ \\ h &= 0,66 D_e & 60./ \\ p &= 0,375 D_e^{1,10} & 61./ \end{aligned}$$

Obviously the relation between D_e and H , p , h may be subject to considerable variations due to operating practice, material produced, furnace availability etc. The main objection that can be raised against the formulas is the linear variation of h with D_e which would hardly materialise since both the change of operating current and the power input were based on the formula of Morkramer by which the ratio between current and power input and electrode diameter decreases with the increase of D_e . Consequently, the extent of the change of p and h should be in the same order, which is not the case. Therefore the equations are more of qualitative than of quantitative value.

1 ./ Production capacity of furnace versus operating current, power input, furnace dimensions (width and length) and electrode diameter.

In the possession of the available data it is possible now to assess the production capacity of the furnace as a function of its dimensional characteristics and operating variables pertinent to current and power used; or vice, with the capacity as the independent, that is the set variable, to determine these parameters required to achieve the desired rate of production. The starting point for these series of calculations is the experience gained from furnace operating practice correlated with the electrode size - operating current relationship. For optimum operation with uniform feed quality and availability (i.e. large stocks of feed and flux material) the current will vary only in a relatively narrow range in practice say 15 to 20% deviations from the maximum that can be delivered by the transformer. The minimum is indicated by dotted lines in figure VI-17. In between the two (the shaded) areas represent economic throughput rate for the size of the unit in question. These estimates relate first of all to the type of concentrate and burden feed used in the prototype furnaces.

In terms of/...

Figure VI - 17. SUMMARISING DATA ON THE RELATIONSHIP BETWEEN FURNACE CAPACITY AND ELECTRODE SIZE, POWER INPUT, OPERATING CURRENT, FURNACE WIDTH,

Legend

- 1 l_w (width)
- 2 $0,8 l_w$
- 3 D_e
- 4 $0,8 D_e$
- 5 I_f
- 6 P_w

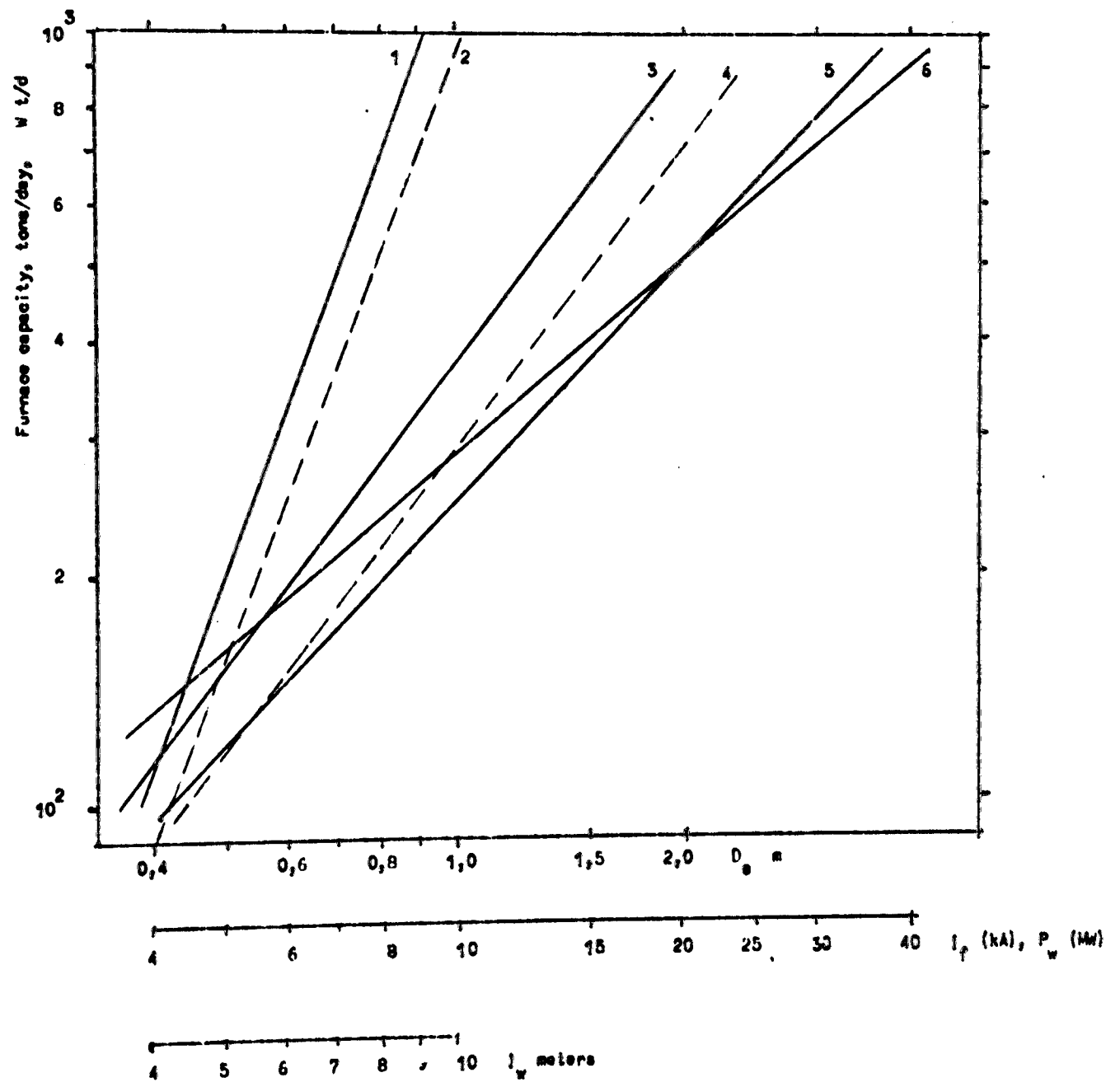
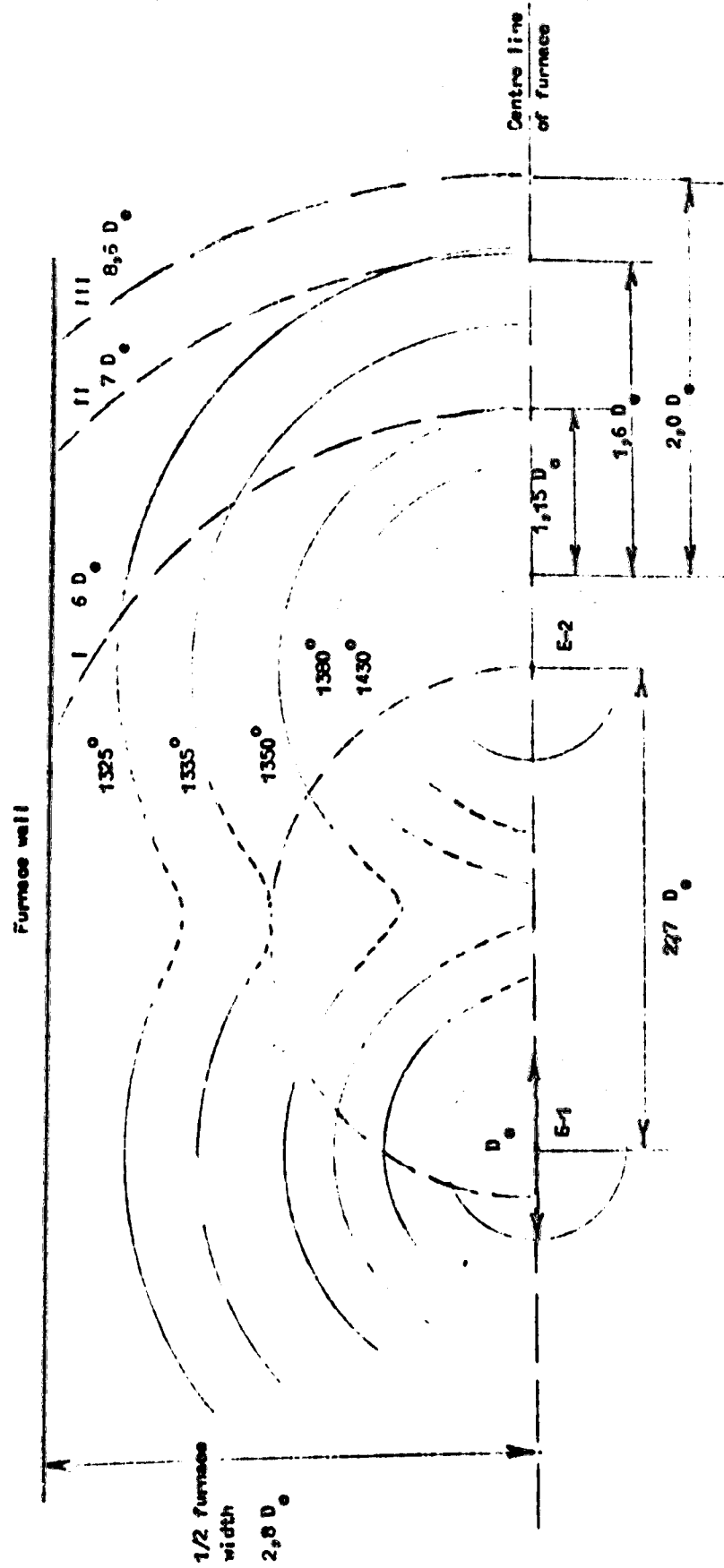


Figure VI - 18. COMPARISON BETWEEN RADIAL HEAT DISTRIBUTIONS IN FURNACES OF RECTANGULAR AND CIRCULAR SYMMETRIES HAVING 6 ELECTRODES IN-LINE AND 3 ELECTRODES IN EQUILATERAL TRIANGLE ARRANGEMENTS.



In terms of the various parameters the weight of feed processed in 24 hours operation is expressed by the following equations:

1./	furnace width	$W_1(t/d) = 2,70 l_w^{2,68}$	62./
2./	D_e	$W_D(t/d) = (3,7 \times 10^2) D_e^{1,36}$	63./
3./	Current $I_f(kA)$	$W_{I_f}(t/d) = 23,8 I_f$	64./
4./	Power P_w	$W_P(t/d) = 42,5 P_w^{0,84}$	65./

* * * *

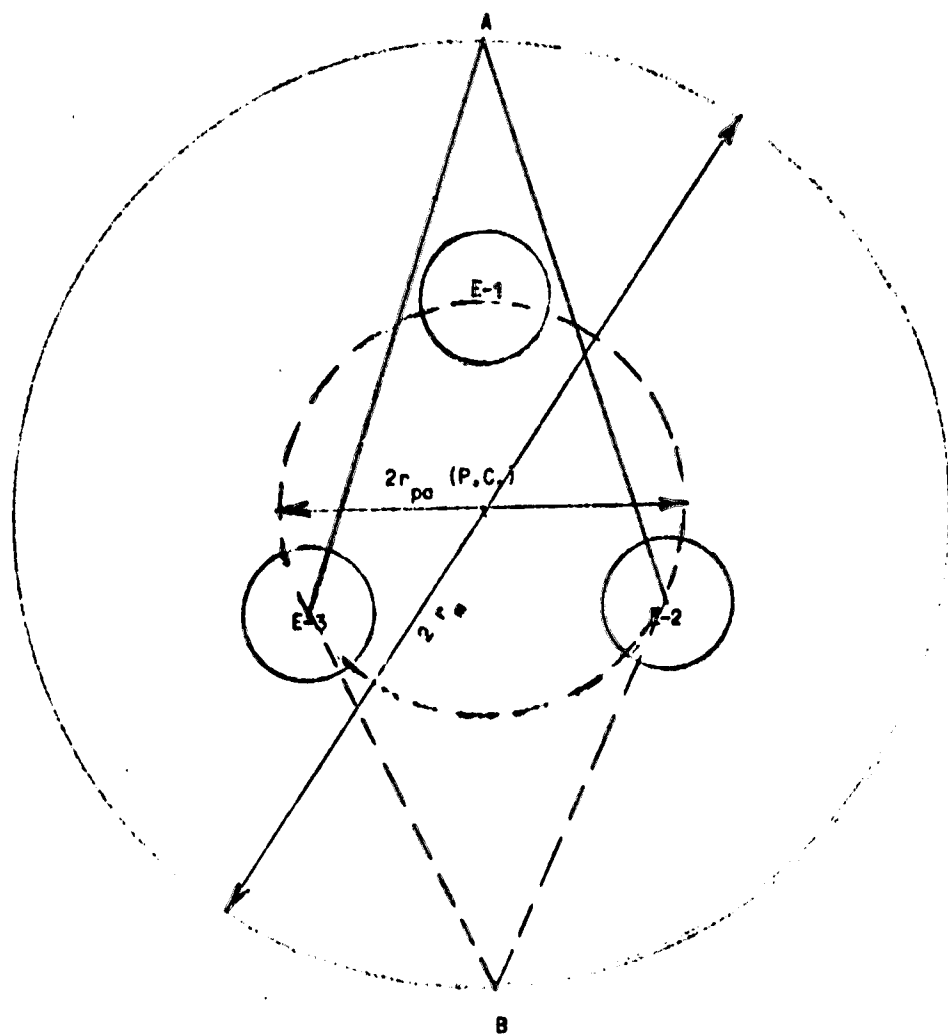
B./ 1./ Rectangular versus circular furnace symmetry

The foregoing considerations may permit a brief comparison between the symmetry requirements of three electrodes in triangle, circular cross section and six electrodes in-line rectangular electric furnaces. As a basis for this comparison the smelting capacity of the units will be taken as identical with those presently in operation at RPM.

As it was shown, from heat conduction considerations the effective smelting area of an electrode of 1,25 m dia. is about 4,0 to 4,2 electrode diameter which, due to turbulence and convective heat transfer, might extend beyond $5 D_e$ as measured from the surface of the electrode. By model experiments a circular furnace, equipped with electrodes of the same size, would have a pitch circle diameter of $3 D_e$ and an overall diameter of about $6 D_e$. The thermal conditions representative of the rectangular and round furnaces could on this basis be compared.

Figure VI-18 drawn to scale, shows the estimated radial temperature distribution around electrode E-1 in the reference furnace. The figure indicates also the dimensions of the electrically equivalent 3-electrode circular furnace, E-2 constituting its second electrode with reference to E-1. P.C. is the pitch circle (diameter $3 D_e$) while dotted line I indicates the distance of the wall from the electrode at a furnace diameter of $6 D_e$. The distance is exactly $1 D_e$ and, as will be seen in the graph, with a radial temperature profile equivalent, or similar to that of the 6-in-line furnace, the temperature at this point would be around $1350^\circ C$. Further two dotted lines denote the diameter of the round furnace if its wall temperature were to be kept the same as in a rectangular furnace. Line II corresponds with the average liquidus, $1325^\circ C$ at a distance of about $1,6 D_e$ from the electrode and represents an internal diameter of $7,7 D_e$, while line III gives the diameter of a circular furnace in which the temperature of the slag at the wall would be/...

Figure VI-19. OUTLINE OF THE PRINCIPLE OF THERMAL LOAD DETERMINATION.



Maximum thermal load at point A, nearest to the electrode;
 minimum at B, between two electrodes.

wall would be by and large the same as in the rectangular unit. Thus for the diameter of the thermally equivalent circular furnace $D_{f(c)}$ we can write

$$D_{f(c)}^{(t.e.)} = 8,6 D_e \quad 66./$$

Since the electrode spacing $S = 2,7 D_e$, then

$$D_{f(c)}^{(t.e.)} = 3,2 S \quad 67./$$

On the other hand, with electrodes of identical size the symmetry relations in a rectangular furnace with regard to width and electrode spacing are

$$l_w \sim 5,6 D_e \sim 2,1 S \quad 68./$$

These considerations are important from the point of view of the life of the furnace wall which depends to a great extent on the thermal load to which the surface of the wall is exposed. For the three circular furnace diameters discussed above this can be estimated by a method based on the ratio between the pitch circle and the diameter of the molten slag bath. The method was originally proposed by the Russian designer Bauera (20) and has been widely used in Russian furnace design convention. The principle is illustrated in figure VI-19. If r_m is the radius of the melt (molten bath) and r_{pc} that of the pitch circle, then the maximum and minimum loads per electrode, i.e. per phase are given as

$$K_{\max} = \frac{P_w}{3} \left[\frac{1}{(r_m - r_{pc})^2} + \frac{2}{(r_m)^2 + r_m \cdot r_{pc} + (r_{pc})^2} \right] \times 10^{-2} \text{ kVA/m}^2 \quad 69./$$

$$\text{and } K_{\min} = \frac{P_w}{3} \left[\frac{1}{(r_m + r_{pc})^2} + \frac{2}{(r_m)^2 - r_m \cdot r_{pc} + (r_{pc})^2} \right] \times 10^{-2} \text{ kVA/m}^2 \quad 70./$$

For a correct design $K_{\min}/K_{\max}$ must be equal or greater than 0,9. Now let us consider the three furnace diameters discussed above. The maximum power in each case would be 19,3 MW then with the following additional data we get

	Case I	Case II	Case III
D_f m	$6D_e = 7,5m$	$7,7D_e = 9,6m$	$8,6D_e = 10,7m$
r_m m	3,75	4,8	5,4
r_{pc} m	1,88	1,88	1,88
$K_{\max}$ kVA/m ²	0,665	1,40	0,835
$K_{\min}$ "	3,82	1,27	0,795
$K_{\min}/K_{\max}$	(5,75)	0,91	0,95

In support of the findings obtained from thermal equivalency considerations pertaining to matte smelting circular furnaces the results presented in the table/...

table clearly indicate that in case II and III the heating of the wall will be comparatively even and the design adequate while in case I, from a heat economy point of view the design of the furnace would not be satisfactory. The ratio of pitch circle to furnace diameter for circular furnaces envisaged for matte smelting would be

$$\frac{D_f}{d_{pc}} = \frac{8,6 D_e}{d_{pc}} = 2,9 \quad 71./$$

In the design of ferroalloy furnaces the electrode spacing is given usually as $S = 2,15 D_e$. When comparing the two furnace symmetries this low value would serve no useful purpose since the electrode current in a three electrode furnace is $\sqrt{3}$ times that for the 6-in-line furnace. The indicated increased electrode spacing of $2,7 D_e$ in fig. VI-18 would correct for the difference in current.

As it has already been referred to, Schwarz von Bergkamp from a model point of view (5) considered the three-electrodes-in-triangle electric furnace as being geometrically a spherical unit in which the bath would then correspond to a hemi-sphere. The internal diameter of the bath, d_b on this basis was estimated as

$$d_b = 5,1 D_e \quad 72./$$

and the total external diameter as

$$D_{f(tot)} = 7,2 D_e^{0,9} \quad 73./$$

On taking about 35-40 cm thickness for the walls in rectangular furnaces, this latter expression representing conditions for ferroalloy production in round furnaces would give a value close to the width of the rectangular units. However, as was mentioned earlier, due to the entirely different character of the melt and, as a consequence, resistivities and heat transfer in rectangular furnaces, the suggested dimensions would be prohibitive if furnaces of circular cross-section were considered for matte smelting.

The expression for maximum performance for three electrode round furnaces was formulated by the same author as

$$P_{w(max)} = 20,2 D_e^2 \text{ MW} \quad 74./$$

For rectangular, 6-in-line matte smelting furnaces in case of optimum depth of electrode penetration in the slag of similar character to that produced at RPM equation 74 would become

$$P_{w(max)} = 12,5 D_e^2 \quad D_e > 1m \quad 75./$$

$$\text{and } P_{w(max)} = 15,2 \text{ to } 21,0 D_e^2 \quad D_e < 1m \quad 76./$$

Only with/...

Only with electrode diameters of 0,6m or smaller would the formula of von Bergkampf be approached.

As to the maximum power density (w/cm^2) on the surface of the electrode the expressions suggested by von Bergkampf (5) for electrode current, highest transformer tap and efficiency factor relationship appear to be of general validity, then

$$I = 5 \times 10^4 D_e^{1.6}$$

and the voltage from the highest transformer tap

$$U = 260 D_e^{0.3} \quad 77./$$

$$\text{and} \quad \cos \varphi = 0,90 D_e^{0.1} \quad 78./$$

For 6-in-line electrode configuration dispensing with spherical and assuming flat electrode tips, the maximum power density in case of $D_e = 1m$ would be

$$\frac{(5 \times 10^4 D_e^{1.6} \cdot 260 D_e^{0.3} \cdot 0,9 D_e^{0.1})}{\pi^2 \times 10^4} = 252 \text{ watt/cm}^2/\text{phase}$$

In an other form the specific power density (or max. power density) can be expressed by the suggestion of Morkramer as

$$E = \frac{2 P_w}{3 \pi D_e^2} K_d \quad 79./$$

where the constant K_d depends on furnace dimensions and also on the characteristics of the slag produced. With an average furnace resistance of 10 mohm, operating current of say 18 kA in the range of 17,5 to 19 MW the value of K_d would be 6,5 to 6,9 and the specific power density in case of the reference furnaces ($D_e = 125 \text{ cm}$) would vary between 237 and 258 w/cm^2 .

B-2./ The problem of matte settling.

The next point in circular versus rectangular furnace relationship is the distance available for the settling of the matte particles. As was shown this distance depends to a great extent on the location of the tapholes. On circular furnaces they may be located either opposite to the electrodes or in between two adjacent electrodes when these are located in the corners of an equilateral triangle. If it were possible to release the bulk of the matte particles at the surface of the electrode, the distance available for settling would be given by the distance between this point and the furnace wall. For the quoted three furnace diameters these were calculated as $I = 1,15 D_e$, $II = 1,6 D_e$ and $III = 2,0 D_e$. Obviously, under these conditions the positioning of the tapholes on circular units may

become somewhat/...

Figure VI - 20. ESTIMATED ACTIVE AREAS AROUND ELECTRODES IN EQUILATERAL TRIANGULAR ARRANGEMENT.

Furnace diameters	I	6,0 D ₀	(7,5 m)
	II	7,7 D ₀	(9,5 m)
	III	8,6 D ₀	(10,7 m)

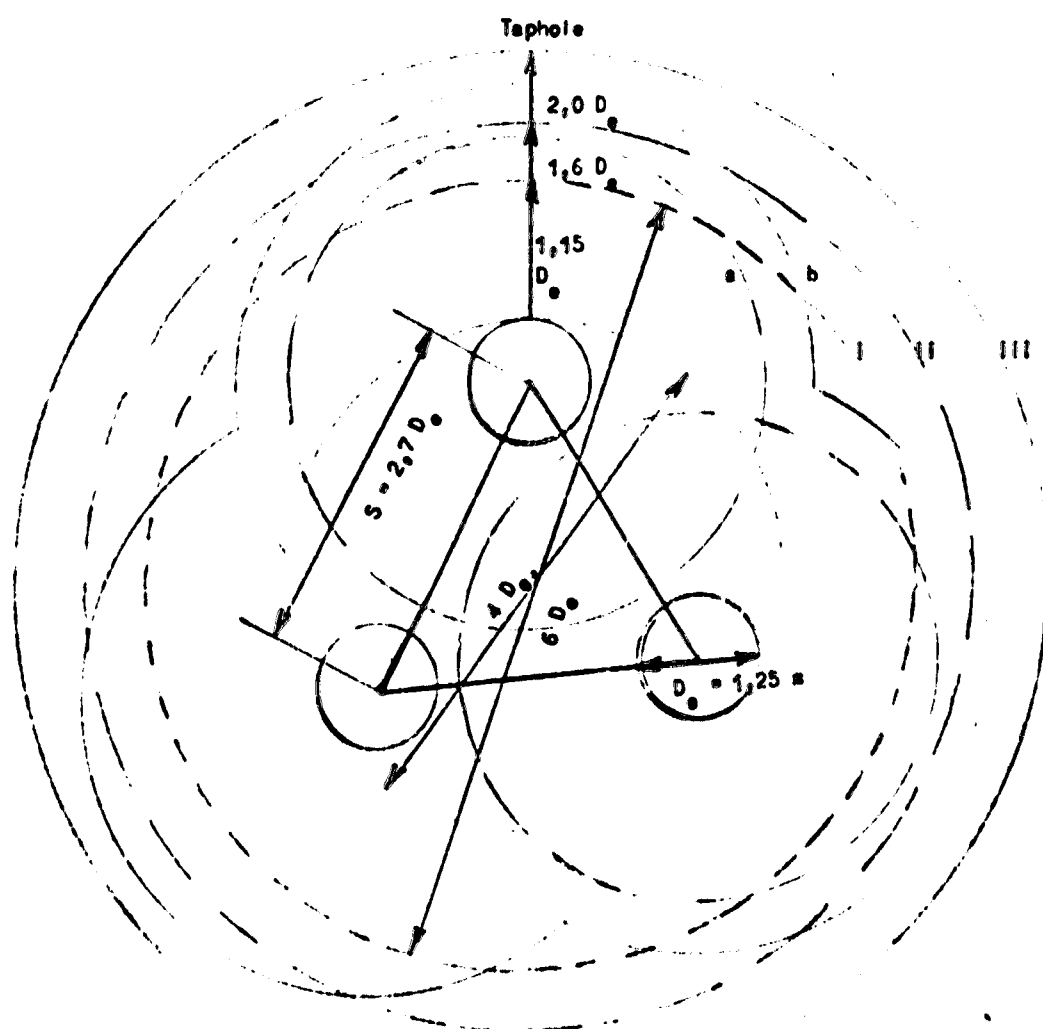


Table VI-11 Settling rate of matte particles in circular furnaces of various diameter as compared with their settling in a rectangular furnace at identical depth of slag beds. (=1,4m).

Furnace diameter	D_e m	6,0	7,7	8,6	Rectangular
Settling distance from electrode to taphole, m		7,5	9,5	10,7	7
Location of taphole given as H/h	5	10	10	2,5	5
in m, below slag level	0,28	1,4	0,28	1,4	0,28
					1,4

Per cent of total settled in the given size fraction

Particle size microns	3	7	4	9	6	11	12	23
20	8	16	11	22	13	27	28	60
30	13	28	19	39	24	50	49	100
40	20	43	28	59	35	75	60	100
50	28	58	40	81	50	100	100	100
60	50	100	70	100	100	100	100	100
80								
Per cent decrease in settling in relation to rectangular furnace								
20	75	61	64	61	50	56		
30	72	73	60	64	53	55		
40	73	72	60	61	50	50		
50	67	57	53	41	42	25		
60	72	42	60	19	50	0		
80	50	0	30	0	13	0		
100	20	0	0	0	0	0		

Table VI-12. Residence time of material and matte losses in circular furnaces of various diameter as compared to the rectangular furnace.

Depth of slag bed 1,4 m, Electrode diameter 1,25m.

Furnace diameter D_e m	Circular		Rectangular	
	6,0	7,7	8,6	7,0
Slag volume at equal bed depths m^3	7,5	9,5	10,7	7,0
	56	99	126	273
Retention time Throughput rate Matte loss	Circular		Rectangular	
	relative magnitudes		relative magnitudes	
	0,205	0,363	0,462	1,0
Throughput rate	4,87	2,75	2,16	1,0
	12,2	5,5	3,1	1,0

becomes somewhat difficult. In case of continuous slag tapping the length of travel of matte particles, as the calculations previously indicated, is rather short in radial direction. No settling could be expected in the area having the radius of a circle drawn around the outer surface of the electrodes concentric to the surface. Being equivalent to about $4D_e$ diameter, this leaves the estimated distance for settling in the mentioned order of $1,15D_e$, $1,6D_e$ and $2,0D_e$ respectively. The amount of particles settled out from the slag at identical feed rates of concentrate to the furnace can be estimated by the method outlined in Section IV. For three circular furnace diameters in question the values are given in Table VI-11. For comparison the table incorporates also relevant settling data computed for a rectangular furnace of the size in use at RPM. The height of the slag layer was taken in all cases uniformly $1,4 \text{ m}$ ($=1,12D_e$).

These calculations were performed with the assumption that the velocity of slag flow in the circular and rectangular units is identical. Percentagewise the data would indicate that even in the largest, $8,6D_e$ diameter circular furnace with particles smaller than 80 micron more than 100 per cent increase in matte losses could be anticipated when the location of the taphole is 28 cm below the slag surface, that is close to the position of the second taphole on operating rectangular units.

The distance between electrode tip and matte surface is set by the resistivity, that is the composition of the slag. As long as this is kept constant, no significant modifications can be made to the symmetry of the system. The depth of the slag bed cannot be increased substantially beyond say $1,6 \text{ m}$ without endangering the smelting efficiency. However, the retention time in the circular furnace, on account of the volume differences at identical depths of the slag bed will be less, that is at the same smelting power higher throughput rates can be expected. At higher feed rates more matte will be produced and thus proportionally more has to be settled with an inevitable increase in matte losses according to some factorial relationship. For various furnace diameters these aspects are delineated in Table VI-12 with the inclusion of data obtained for a rectangular unit as a comparison. The matte loss was estimated by multiplying the relative throughput rates with the factors respectively 2,5, 2,0 and 1,44 ($=2,0D_e$, $1,6D_e$ and $1,15D_e$) relating to the furnace diameters of $6 D_e$, $7,7 D_e$ and $8,6 D_e$ on the basis of figure VI-20. The depth of slag layer was uniformly $1,4 \text{ m}$ and the location of the tapholes opposite to the electrodes about 60 cm below the top of the molten slag. Conditions were regarded as being close to steady state similar to those prevailing in part of the settling zone of the rectangular furnace. Because of this and the
unknown/...

unknown factor of increased turbulence in circular units the calculated values can be regarded as highly conservative.

In an alternative arrangement the tapholes on the round furnace could be positioned in between the electrodes. This may be of some advantage because, as it will be obvious from figure VI-20, the tapholes would be beyond the active area of the electrode and, consequently, the distance available for settling would be greater. However, by the indication of the graph this increase, about one electrode diameter is far from being adequate to bring about a substantial improvement in matte settling. In addition, from more involved symmetry considerations, the hydrodynamic motion of the slag bath with circulating electric field in a round furnace is more intensive than in a rectangular unit and the active area would take up the form of a circle instead of a clover leaf shape.

Reverting now to the relevant equations in Section IV, the expression for the quantity of matte settled can be formulated in terms of furnace diameter. Taking the coalescence factor f_0 as constant in the range of the diameter investigated and denoting the slag flow velocity - matte settling velocity ratio by $u_{M/sl}$, the length of travel of the particle from its point of release by L , the ratio of total slag depth to the location of the taphole by $r_{H/h}$ and the furnace diameter by D_f while w_M^{tot} is left for the total weight of matte to be settled out in the unit, the settled matte weight w_M^s is given in per cent by

$$w_M^s = u_{M/sl} \cdot L \cdot r_{H/h} \cdot D_f^n \cdot w_M^{tot} \quad 80./$$

Since $r_{H/h}$ is constant in all cases and at equal throughputs, on account of the identical depths of slag bed, $u_{M/sl}$ is also constant, the settled matte weight is primarily a function of the point of release, i.e. the length of travel of the particle, L , and the diameter of the furnace D_f , thus

$$w_M^s \propto (D_f)^n \cdot L \quad 81./$$

In other words, at a given electrode diameter the settled matte weight varies according to a certain power of the diameter of the round furnace. From the data of Table VI-12 by extrapolation it can be estimated that a furnace diameter of approximately 12,5m would be equivalent in settling efficiency at the given arrangement of the three electrodes with the efficiency of a 6-in-line rectangular furnace. From a heat economy point of view this diameter cannot be regarded as an entirely sound proposition.

Pertaining to power input to a three-electrode circular furnace and a six-electrode rectangular furnace further calculations indicated that for an equal, 19,3 MW maximum power input the size of the electrodes in the/...

in the former one should be 1,6 m dia. (21). Since its specific hearth resistance is given as

$$\rho_H = R \pi D_e = \frac{U}{IV} \pi D_e \text{ ohm.cm} \quad 82./$$

therefore the current for the three-electrode circular furnace will be $\sqrt{3}$ times that of the 6-in-line unit. This would be counterbalanced by increased electrode spacing in the proportion as discussed before. In this way an overall furnace diameter of 13,8m was estimated as being capable of holding the same depth of slag as a rectangular unit. Then the slag volume in the 6-in-line furnace is $27 \times 7 \times 1,4 = 255 \text{ m}^3$ while in the three-electrode circular furnace $D_f^2 \pi / 4 \times h = (13,8)^2 \times 3,14 / 4 \times 1,4 = 209 \text{ m}^3$. With the same power input and quantity of feed material the same amount of slag will be produced but the retention time for the circular furnace will be

$$\theta_{\text{circ}} = \theta_{\text{rect.}} (209/255) = 0,82 \theta_{\text{rect.}} \quad 83./$$

that is about 20% less.

The settling distance in the settling zone is defined by the radius of the smelting zone of the electrode, which, in a rectangular furnace would reach approximately half the length of the settling zone, leaving around $2 D_e$ length for a more quiescent settling of the matte particles. Because of the increased flow velocity of the slag during the addition of converter return, the shape of the active area (i.e. the agitated region) around the last electrode at the slag tapping end might become distorted and assume an elongated parabolic shape in the direction of the flow, reducing thereby further the available $2 D_e$ settling length. Even so, the distance available for settling, as shown in figure VI-20 will not be quite equivalent to this in a circular furnace.

An alternative way of improving the settling of matte in a circular furnace could be offered by intermittent slag tapping. However, the main disadvantage of discontinuous slag removal is the increased fluctuation of the slag level leading to disturbances in the process of smelting, in the control of the electrode position, voltage and smelting efficiency. From a standpoint of modern trends in operating practice, the method would in general appear as a retrograde approach.

B-3./ Comparison of thermal efficiencies.

In comparing thermal efficiencies between circular and rectangular furnace geometries the data supplied by Barth (22) on heat needed by an electric furnace producing copper-nickel mattes might be considered for a rough estimate. Unfortunately the data are old and by no means truly representative/...

representative for the presently discussed units. Nevertheless they may give an approximate idea on the nature of heat requirements in the process of matte smelting as shown in the following table:

Source of heat loss	Per cent loss
Water vapour	5,3
Endothermic reactions	8,8
Heat in matte	14,1
Heat in slag	29,8
Heat in waste gas	25,4
Electrical losses	5,2
Radiation losses	11,4

From the table it would appear that in case of treating the same amount of concentrate and producing the same quantity of matte and slag, the only possibility for improving the economy in a circular furnace is offered by the decrease in radiation losses due mostly to shorter residence time of slag and matte in the furnace.

As to the change of heat losses (denoted as H_2 and H_1) with furnace size Pashkis and Persson (18) gave the formula as

$$H_2/H_1 = \beta \quad 84./$$

for round furnaces having three electrodes and used for ferroalloy production. However, with rectangular furnaces the direct proportionality of heat losses claimed in the above equation is not so obvious when the relationships displayed in fig.VI-18 are taken into account. From the graph the relation between furnace width and power input can be expressed as

$$l_w = 2,76 P_w^{0,312} \quad 85./$$

where l_w as a symmetry parameter is fairly representative of the size of the furnace. It can be anticipated with reasonable certainty that the heat loss will vary in a close to straight proportion with P_w from which it follows that with l_w the heat loss will vary also according to P_w , that is

$$l_w = 2,8 H_r^{0,3} \quad 86./$$

will actually be the expression for heat losses with the variation of the size of the units, where H_r represents heat losses in rectangular furnaces.

In rectangular versus circular symmetry relationships equ. 83 can be used as a basis for comparison. In comparing two units of equal performance in the various symmetries, after the fashion of the previous

paragraph/...

paragraph the bed depths were taken as identical. Then from the proportion of surface areas and residence time of materials the following empirical equation was found to reflect approximately the difference in heat losses in the present context of the mode of operation and the material handled:

$$H_c = H_r \left[\frac{A_c}{A_r} + k \left(1 - \frac{\theta_c}{\theta_r} \right) \right] \quad 87./$$

where H_c , H_r , A_c , A_r , and θ_c , θ_r denote heat losses, areas of operating units and residence times respectively in circular and rectangular furnace symmetries. The value of k depends on the character of material treated and it was estimated as $k = 0.45$ to 0.50 in this study.

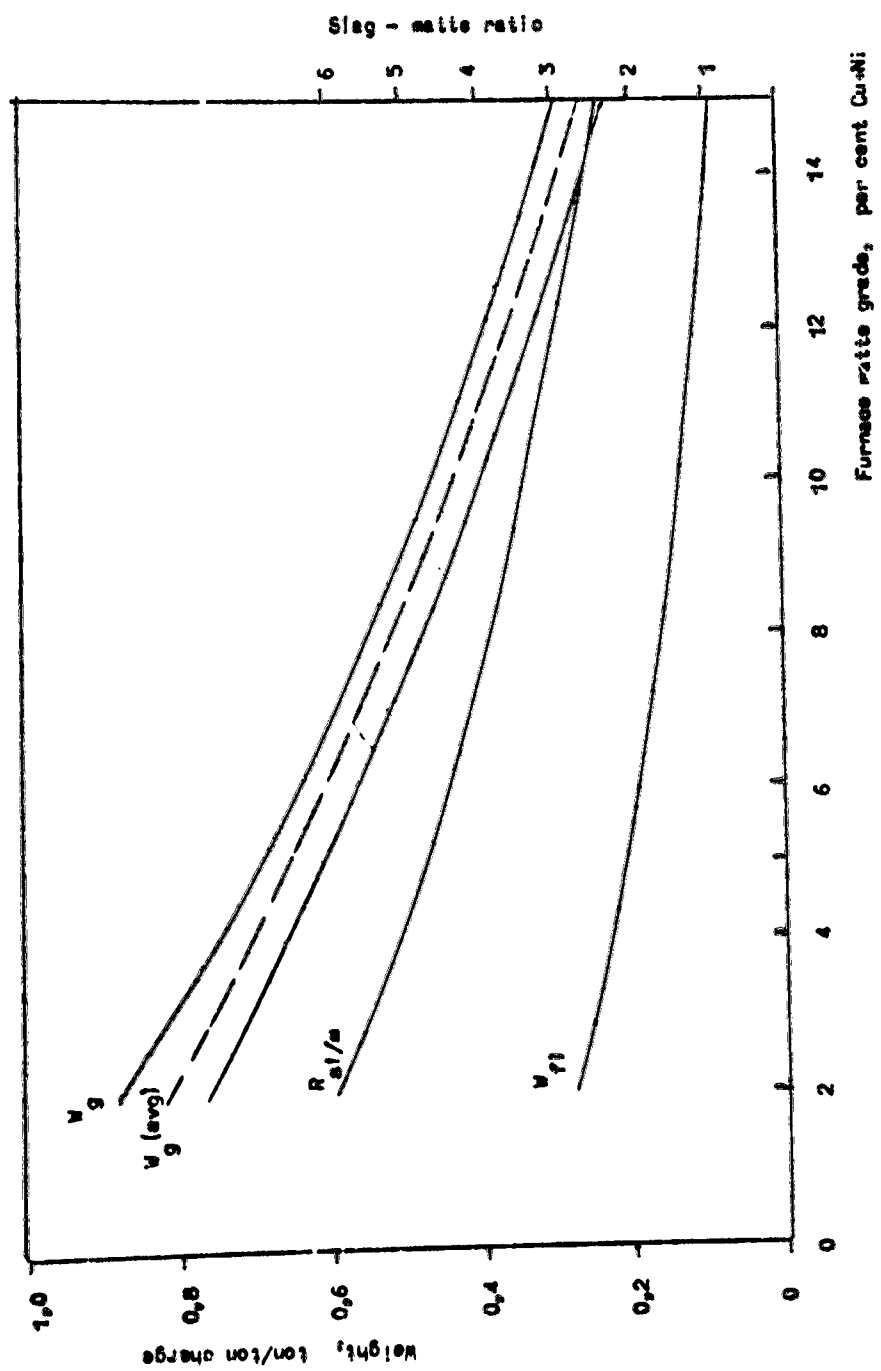
C./ The effect of the grade of concentrate processed in the furnace.

Finally, in the scope of this section a brief reference should be made to the effect of the grade of concentrate used in the process of smelting, upon certain operational parameters. The earlier estimate concerning the material throughput in the furnace assumed a feed of uniform composition. This assumption related particularly to the gangue material content of the concentrate which, in fact, in the operation of the reference furnaces at RPM is kept within a reasonably narrow range.

However, the grade of concentrate set by the gangue material content has an important bearing on the slag-matte ratios of any type of smelting process be it represented by electrothermal or reverberatory practices. The level of gangue material is prone to excessive fluctuations from one ore deposit to the other, both from a qualitative and quantitative point of view. It is primarily the qualitative aspect, that is the change in the general composition of the gangue that makes predictions pertinent to the quantitative effect, that is the actual amount of gangue present, difficult. The change ⁱⁿ its composition will alter the composition of the slag; as a result the physical characteristics of the slag will also alter and this may bring about altogether different conditions necessary for a successful smelting practice. Since in the present study the physical characteristics have been determined for a particular range of slag composition relevant to a given range of gangue quality and quantity, any serious deviation from this line would render predictions as to the effect of the quantity of gangue meaningless.

In the following/...

Figure VI-21. GANGUE MATERIAL CONTENT OF CONCENTRATE, FLUX (LIMESTONE) REQUIREMENT, FURNACE MATTE GRADE AND SLAG/MATTE RATIO RELATIONSHIP.



In the following simplified approach it will be suggested that with the change of the quality of gangue material in the concentrate the chemical composition of the gangue alters only in such a way that its effect upon the composition of the slag produced will not be significant so as to necessitate sizeable alterations to the operating parameters of the furnace. In other words, the characteristics of the slag will vary at the most within the range investigated in this study. This assumption obviously entails that the quantity of the limestone flux required for efficient smelting varies proportionately with the quantity of concentrate treated. The amount of moisture to be evaporated varies also proportionately with the weight of the concentrate.

Conditions pertinent to the grade of furnace matte, quantities of gangue and flux and slag - matte ratios are summarised in figure VI-21. To estimate the values shown in the figure the following relationships based on practical operation were considered:

$$\begin{aligned} \text{Standard weight of gangue} & W_{g(st)} = 0,60 \text{ to } 0,65) \cdot W_c \\ \text{Weight of concentrate} & W_c = 1,0 \\ \text{Weight of flux} & W_{fl} = 0,2 W_c \\ \text{also: } W_{fl} & = (1,0/0,6 \text{ to } 0,65) \times 0,2 = (1,43 \text{ to } 1,54) \cdot 0,2 W_c \\ & \text{then } W_{fl} = (0,3 \text{ to } 0,33) W_g \end{aligned}$$

The energy needed to melt the concentrate of various gangue material content can be estimated by using as a first approximation the latent heat of melting of the sulphide components, Q_{MeS}^L and that of the gangue Q_g^L . The gangue, as it was mentioned in Section I is a multi-component system of complicated minerals of which both the heat of formation and free-energy of formation values are relatively low as compared to the latent heat of fusion. Thus the energy requirement is given primarily as latent heat of fusion of concentrate + flux + decomposition heat of flux ($CaCO_3$) + heat needed to evaporate moisture. For the concentrate Q_c^L

$$Q_c^L = Q_{\Sigma MeS}^L + Q_g^L \quad 88./$$

The total heat required for smelting is then

$$Q_{sm} = Q_{\Sigma MeS}^L + Q_g^L + Q_{fl}^L + Q_{evap}^L + Q_H \quad 89./$$

where subscripts to latent heats Q^L refer to: MeS = metal sulphide, g = gangue, sm = smelting, fl = flux, evap = evaporation of moisture. Q_H is the heat quantity needed to raise the temperature of the charge to an average/...

average 1400°C of the melt system expressed in the form

$$Q_H = C_p \cdot W_{ch} \cdot \Delta T \quad 90./$$

where $\Delta T = T_r - T_a$, the difference between the reaction temperature and ambient.

In the present case the heat requirement will be assessed from the thermal balance of an operating unit. The thermal balance set up by Barth (22) as was referred to earlier in this section relates to the operation of a moderately sized, or by today's standard small, and technologically somewhat obsolete furnace that had been in operation some time ago at Imatra in Finland. Therefore it does not really represent conditions obtaining in the reference furnaces at RPM. Data relevant to these units were recently given by Mostert and Roberts (7) by which the heat output of the furnace, or absorbed heat is as follows:

Furnace slag	49 per cent
Endothermic reactions	18 " "
Furnace matte	7 " "
Radiation and electrical losses	16 " "
Furnace gases	10 " "

On the basis of the table the quantity of heat actually utilised for smelting (Q_{sm}) as related to the total heat added to the furnace Q_{el} will be

$$Q_{sm} = 0,74 Q_{el} \quad 91./$$

The energy consumption of the furnace based on long-range operating practice was found to be 897 kwh per tonne of concentrate and 689 kwh per tonne of charge, i.e. 0,689 kwh per kg as related to equivalent weights of concentrate and total charge. Therefore $Q_{el} = 0,689 \times 860 = 592 \text{ kcal/kg}$, thus the total useful smelting heat $\Sigma Q_e = 692 \times 0,74 = 439 \text{ kcal/kg}$ and the corresponding power $P_e = 0,510 \text{ kwh/kg}$. This amount of heat includes the raising of the temperature of the material from ambient to the average (1400°) temperature of the slag. With a specific heat value of 0,192 kcal/kg charge the heat needed to increase its temperature to 1400°C will be $1400 \times 0,192 = 268 \text{ kcal}$ and the evaporation of 2 per cent moisture contained in the charge represents $489 \times 0,02 = 10 \text{ kcal}$. Then for the actual smelting of the concentrate will be needed $592 - 278 = 214 \text{ kcal/kg}$, that is in electrical energy $P_{sm(ch)} = 0,25 \text{ kwh/kg}$.

The C_p value of the charge at various gangue levels can be calculated from the increase of the metal sulphide contents. As a first approximation the specific heats of the pure oxide and sulphide components were taken into account (together with that of the limestone flux) for

average gangue /...

Figure VI-22. POWER REQUIREMENTS TO SMELT ONE TON OF CHARGE OR CONCENTRATE AS FUNCTIONS OF THE GANGUE MATERIAL CONTENT.

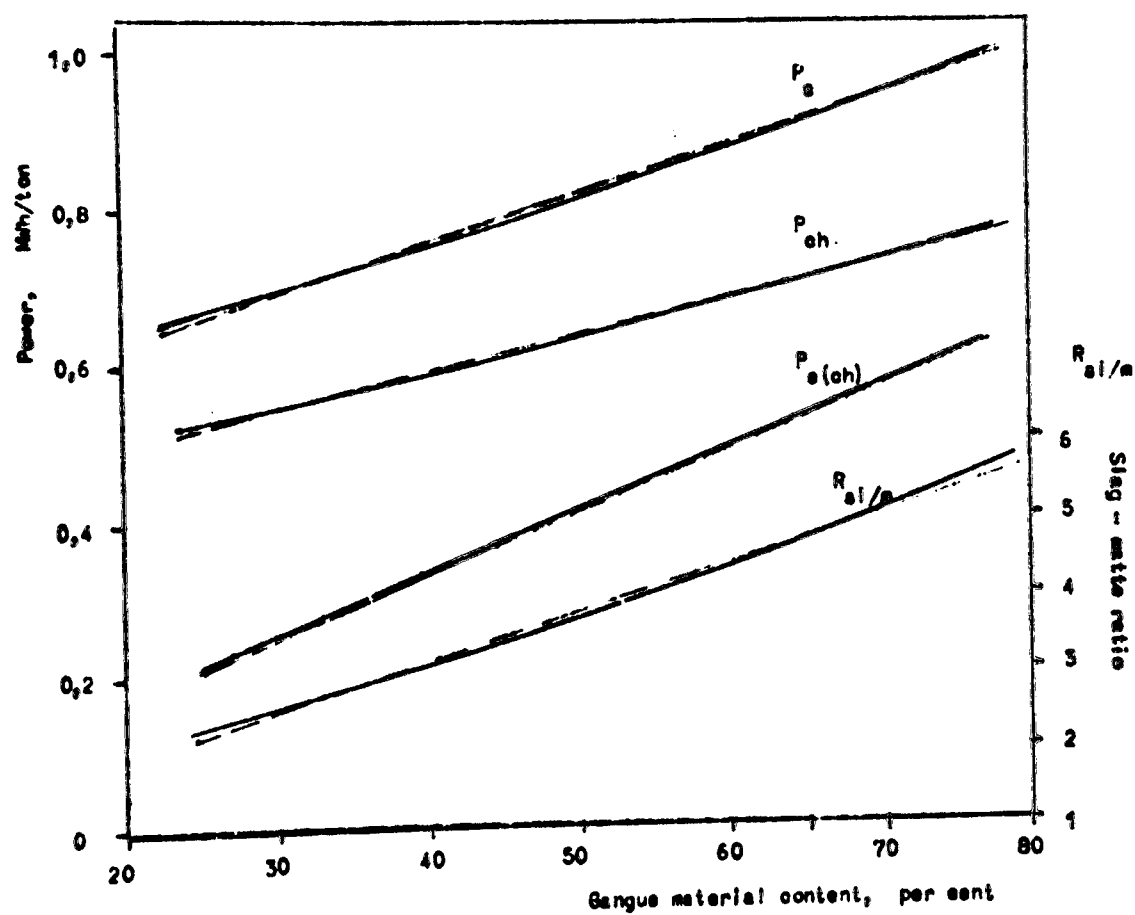
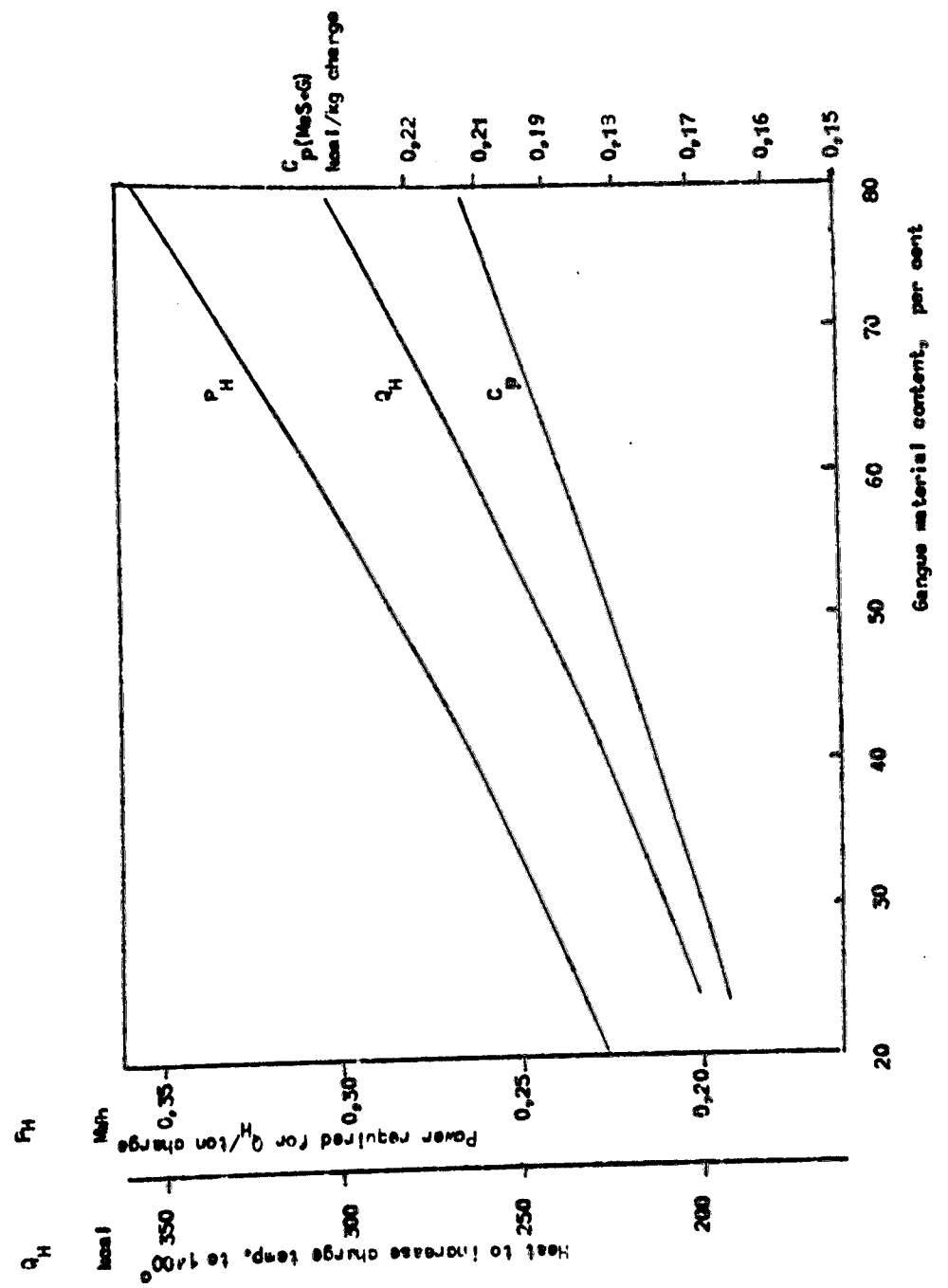


Figure VI-23. SPECIFIC HEAT OF CHARGE, HEAT AND POWER REQUIRED TO INCREASE THE TEMPERATURE OF THE CHARGE TO AVERAGE 1400°C, AS FUNCTIONS OF THE GANGUE MATERIAL CONTENT.



average gangue and metal sulphide compositions. The estimated change of the C_p values of the charge with the change of the gangue mineral content is shown in fig. VI-22, which includes in addition the heat and energy requirements to bring up the charge temperature to 1400°C . The trend of the plot representing the specific heat reflects the lower heat capacity of the sulphide constituents with their increase in the charge. Naturally the heat needed for melting the charge decreases with the rise of the metal sulphide content. The total effect of the simultaneous decrease of the gangue content and the increase of the metal sulphides, or vice versa, is hard to evaluate without the true knowledge of the degree of participation of the various minerals in the concentrate. As was mentioned above, the present estimate is based on the thermochemical behaviour of the pure oxide and sulphide components of the charge. As a consequence, it can be regarded as being more a qualitative than quantitative indication pertaining to the effect of the gangue mineral content upon the energy requirements of smelting. Graph VI-23 based on the mode of calculation delineated above represents the estimated variations in power requirements with the change of the gangue material content of the charge. It would indicate that sizeable savings in smelting power could occur when the grade of the concentrate is increased say from 5 to 10 per cent. For simplicity the relationship between the two parameters can be regarded as linear and expressed in the following forms:

$$\begin{aligned} P_{w(c)} &= 0,63 W_g(t/t \text{ conc}) + 0,5 \quad \text{MWh/t concentr.} \quad 92./ \\ \text{and} \quad P_{w(ch)} &= 0,47 W_g(t/t \text{ charge}) + 0,4 \quad \text{MWh/t charge} \quad 93./ \end{aligned}$$

Figure VI-23 includes also the effect of the gangue content on the slag - matte mass ratio in the furnace expressed by the formula

$$R_{sl/m} = 0,068 W_g(\%) + 0,35 \quad 94./$$

A combination of equations VI-92 and 93 with those given earlier for power and furnace size (the latter expressed in terms of electrode diameter) pertinent to furnace capacity will then give the effect of the gangue material content of the feed upon these parameters. With other conditions being equal the weight of charge that can be smelted is a function of the grade of concentrate C_g and the size of the furnace represented by the electrode diameter:

$$W_{ch} = f(D_e, C_g) \quad 95./$$

As was shown before, the relationship between power requirement and the maximum weight of concentrate that can be processed in one day's (24 hrs) operation in the reference furnaces is given by the formula

$$W_o(t/day)/...$$

$$\begin{aligned} W_c(t/day) &= (0,425 \times 10^2) P_w^{0,84} & 96./ \\ \text{that is in tons/hr} \quad W_c(t/hr) &= 1,77 P_w^{0,84} & 97./ \end{aligned}$$

These expressions give the average high value of 0,897 MWh power required per tonne of concentrate treated, that is in terms of the operating power P_w (MWh)

$$P_w/\text{ton conc.} = \frac{F_w(\text{MWh})}{1,77 P_w^{0,84}} \quad 98./$$

This relates to a concentrate having an average 62 to 65 per cent gangue content. Then with equ.92 the effect of the gangue content on the smelting power will be

$$0,63.W_g(t/) + 0,5 = \frac{P_w(\text{MWh})}{1,77 P_w^{0,84}}$$

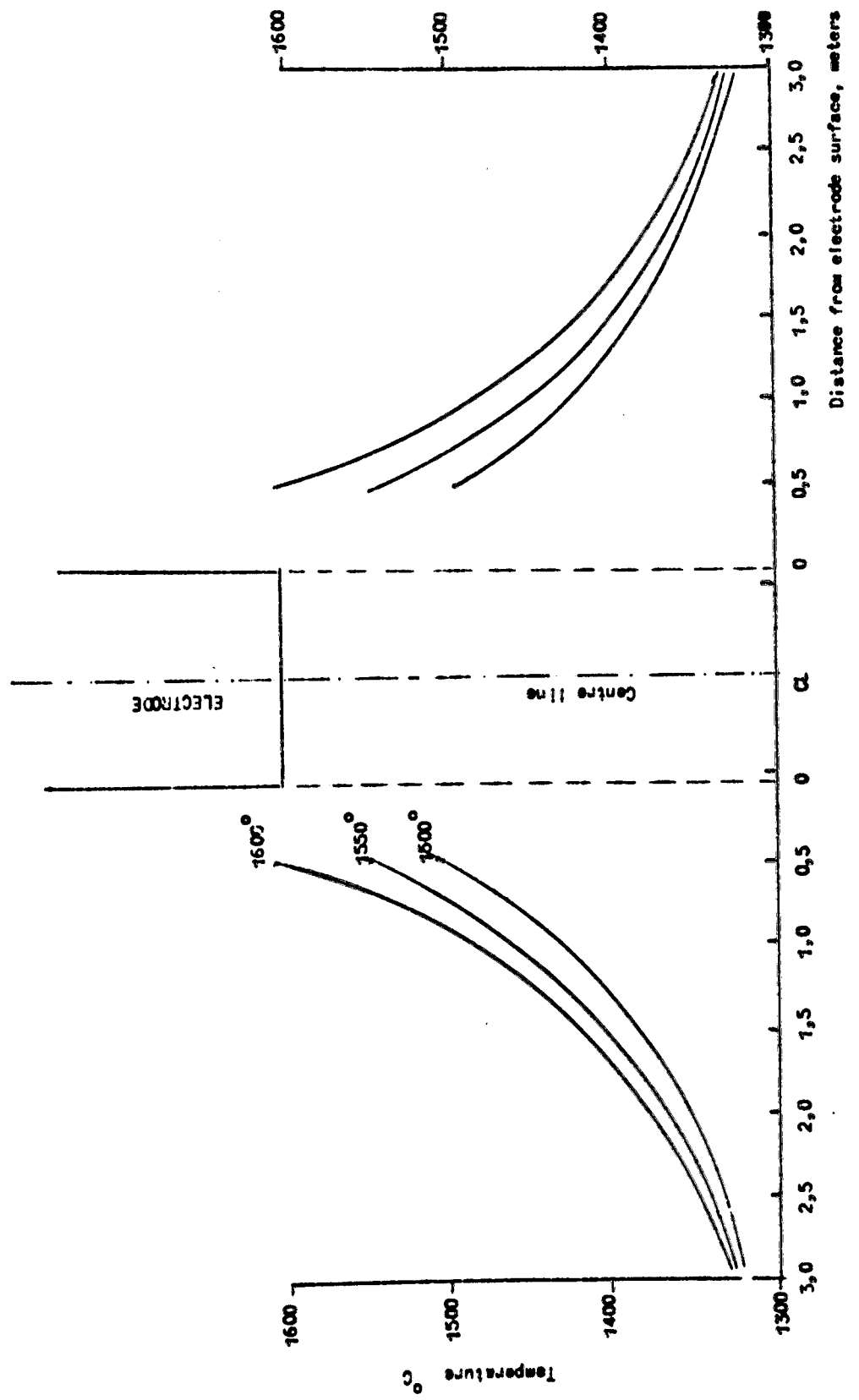
$$\text{or} \quad P_w(\text{MWh}) = 1,77 P_w^{0,84} (0,63.W_g(t/t) + 0,5) \quad 99./$$

* * *

Closing remarks to Section VI.

In the design of electric smelting furnaces the foregoing considerations clearly indicate the importance of the character of the material to be processed irrespective whether the aim is to produce say ferroalloys in three-electrode, submerged-arc furnaces of circular cross section or to treat Cu and Ni-bearing concentrates in six-in-line electrode, rectangular units, with a view to obtaining mattes for further treatment by conventional converter practice. In this latter case the actual composition of the concentrate is of little importance provided the feed to the furnace is in a suitable physical form. The physico-chemical characteristics of the slag, such as liquidus temperatures, viscosities and electrical conductivities are however extremely important. The vastly different nature of these parameters for example in the production of low- or high-carbon ferromanganese, ferrochromium and ferrosilicon necessitated intensive investigations into the geometry of the furnace leading to careful delineation of the k factor for the best economics in both furnace design and operation. From the results of the large number of investigations it is now apparent that, as has been emphasized already, apart from a very few truly basic formulas, no general relationships whatsoever could be devised that would be representative e.g. with regard/...

Figure VI - 24. ESTIMATED PATTERN OF HEAT DISTRIBUTION IN THE SLAG ALONG THE CROSS SECTION OF THE FURNACE.



e.g. with regard to furnace resistance and cell geometries of a whole range of products without the use of various empirical constants and correction factors to take account of the behaviour of both feed and product.

In this section it was demonstrated, how in the light of the knowledge of the characteristics of furnace products the basic geometrical relationships of a rectangular furnace can be delineated and how with the aid of the similarity principle these considerations can be extended toward the scaling-up of furnaces. To simplify the approach, the results obtained with slags of rather narrow composition range were used, therefore the validity of the proposed formulae is restricted primarily to the particular characteristics of these slags or to slags of different composition but of similar physico-chemical nature. The geometry of the furnace was then based on the character of the slag which actually defines the working area for an electrode of given size and from this the dimensions of the furnace. In this way the fundamental factor for design becomes the property of the material to be processed. Due to this approach, most of the proposed formulae are empirical in nature.

The basic shortcoming of the method used is undoubtedly in its reliance upon a limited composition range of material processed and slag produced which makes extrapolation to other ranges of composition and characteristics rather difficult. Let the point be illustrated by the example of heat distribution around an electrode of the same size as used in the reference furnaces. Three different slags, derived from various concentrates, are envisaged to be produced with which the high temperature zone under the electrode will be 1500° , 1550° and 1600°C respectively. By the method of calculating radial heat distribution in the molten slag around the electrode (see Section IV) the conditions shown in Fig. VI-24 will be obtained. In order to facilitate the delineation of comparable working electrode areas, all three temperatures are assumed to come about at the same electrode position.

The variation of the active area with electrode diameter was described earlier in this section by the formula

$$d_A = K\sqrt{D_e} \quad K \sim 5,0$$

when the temperature under the electrode and around its tip was assumed to be uniformly 1500°C . Thus the above equation relates to one temperature only, whereas with the change of T in the expression

$$T'_x = /...$$

$$T'_x = \Delta T K_o(x)$$

(where $\Delta T = T - T_a$, i.e. the difference between the upper and lower boundary temperatures, see Section IV) the extent of the area will also be prone to variations as illustrated in graph VI-24. Added to this one has to take into further consideration two important parameters affecting d_A , viz the depth of electrode immersion (p) and the depth of slag layer of assumed relatively constant temperature (δ) (see Tables VI-5 and VI-6). They define the area of overlap between the active zones of adjacent electrodes which, in turn, affect the energy distribution, material flow and matte settling characteristics in the furnace. Ultimately, the active area will set the electrode spacing and thereby the entire geometry of the units. In relation to submerged arc furnaces of circular cross section having three electrodes in an equilateral triangular configuration Kelly, as was shown earlier, went into great detail to define the extent of this zone being considered as optimum for electrode spacing, i.e. from a furnace design point of view. Appendix III contains additional notes on the energy and symmetry relations in relevance to electrode diameter and active area.

From the above line of thought it is apparent that the exact determination of the working area of an electrode and with this also the geometry of the furnace becomes an extremely complex problem when slags of different characteristics are taken into account for design purposes. In this instance the present investigations would indicate that for an adequate furnace design it is necessary to have a good knowledge of the physico-chemical characteristics (i.e. first of all viscosity, electrical conductivity and liquid temperature) of the slags to be produced. In the knowledge of the liquidus temperature in case of submerged electrode, slag resistance heated furnaces the active area of the electrodes may then be estimated in the outlined fashion based on simplified heat flow considerations and in the determination of settling areas needed for efficient slag-matte separation.

As for a general conclusion, in adequate furnace design the necessary electrical parameters should be derived from the character of the material; a principle which is summarised in probably the only generally valid correlation in electric furnace design. In Andreae's electrode periphery resistance concept.

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APPENDIX I.

Derivation of the expression (equ.IV-7) for radial heat conduction from the electrode in the slag.

Consider the close to constant temperature region of the slag layer of uniform thickness δ with inner radius r_1 and outer radius r_o as shown in figure IV-5. The temperature on the electrode surface (r_1) is T and at the end of the outer radius T_a . By the three energy arrows in the figure the energy balance becomes

$$q|_r = q|_{r+dr} + q_c \quad 1./$$

the appropriate rate equations are

$$q|_r = -\left[k\delta 2r\pi \frac{dT}{dr}\right]_r \quad 2./$$

$$q|_{r+dr} = -\left[k\delta 2r\pi \frac{dT}{dr}\right]_{r+dr} \quad 3./$$

$$q_c = 2h(2r\pi dr)(T-T_a) \quad 4./$$

The area $2r\pi\delta$ is a function of r and the multiplier 2 in equ.4./ for q_c accounts for heat losses from both sides of the temperature zone. Substituting the rate equations into the energy balance we get

$$-\left[k\delta 2r\pi \frac{dT}{dr}\right]_r = -\left[k\delta 2r\pi \frac{dT}{dr}\right]_{r+dr} + 4r\pi h(T-T_a) \quad 5./$$

Combining the terms r and $r+dr$ and dividing by dr

$$\frac{\left[k\delta 2r\pi \frac{dT}{dr}\right]_{r+dr} - \left[k\delta 2r\pi \frac{dT}{dr}\right]_r}{dr} = 4r\pi h(T-T_a) \quad 6./$$

$$\text{Then with limes } dr \rightarrow 0 \quad \frac{d}{dr} \left[k\delta 2r\pi \frac{dT}{dr} \right] = 4r\pi h(T-T_a) \quad 7./$$

Because of constant thermal conductivity and layer thickness, $k\delta 2\pi$ can be removed from differentiation. Upon dividing by $k\delta 2\pi$ the equation becomes

$$\frac{d}{dr} \left[r \frac{dT}{dr} \right] = \frac{2h}{k\delta} r(T-T_a) \quad 8./$$

Performing the differentiation (treating here rdT/dr as a product) then rearranging and dividing by r gives

$$\frac{d^2 T}{dr^2} + \frac{1}{r} \frac{dT}{dr} - \frac{2h(T-T_a)}{k\delta} = 0 \quad 9./$$

The boundary conditions are $T = T_1$ at $r = r_1$
and $\frac{dT}{dr} = 0$ at $r = r_0$

In a non-dimensional form we can define

$$\bar{T} = \frac{T-T_a}{T_1-T_a} \quad \text{and} \quad \bar{r} = \frac{r}{r_0}$$

The expression 9./ will assume the form

$$\frac{d^2 y}{dr^2} + \frac{1}{r} \frac{dy}{dr} - M^2 y = 0 \quad 10./$$

where $M^2 = 2hr_0^2/k\delta$

APPENDIX II.

To the aspect of coalescence in the settling of matte when smelting Cu-Ni sulphide concentrates.

Due to its substantial impact on the settling of matte, particle coalescence plays an important role in the dimensioning of furnaces used for smelting Cu-Ni sulphide concentrates. This aspect will be investigated in the following in the light of literature data coupled with the results obtained in this study by microscopic testwork pertinent to industrial matte settling conditions.

1./ Pertinent literature data.

With regard to coagulation in polydispersed systems, close to 50 years ago Tuorila (1,2) conducted fundamental studies the results of which are of particular relevance to certain features of matte settling in molten slag and can be applied successfully to the investigation of particle coalescence in the slag-matte system of smelting furnaces. The concept is based on the theory of fast coagulation developed by Müller (3) with a strong emphasis on particle interaction, whereby the larger spheres act as collectors upon the smaller ones in the process of settling. This action is envisaged to take place in a hollow cylinder. If the average mass of the small particles between their point of release from the concentrate and length of travel h_0 at time θ is $\sum m_h$, then with the starting mass being $\sum m_0$

$$\sum m_h = \frac{\sum m_0}{\theta x} (1 - e^{-\theta x}) \quad 1./$$

gives the number of smaller particles in the layer of the bed from which the larger grains have already been separated. The constant x in equ.1./ can be evaluated by setting $x\theta = y$, so the expression becomes

$$\frac{\sum m_h}{\sum m_0} = \frac{1}{y} (1 - e^{-y}) \quad 2./$$

Russian investigators (4, 5, 6, 7) carried out detailed studies on the various aspects of coalescence with particular reference to the behaviour of matte particles in the slag-matte system of smelting furnaces related primarily to the problem of copper production. The composition of slag investigated in this work was 44,7% FeO, 44,8% SiO₂, 10,5% CaO while the low-grade matte contained 19,4% Cu and 31,8% S. In the laboratory tests the system was kept at 1300°C under an atmosphere of argon and at various time-intervals samples were taken and subjected to chemical, microscopic and X-Ray analysis for the size and copper content of the matte particles. The degree of coalescence was estimated by Tuorila's formula with the

results shown/...

Figure A- 1.) VARIATION OF PARTICLE COALESCENCE AND INTERFACIAL TENSION WITH THE CaO CONTENT OF THE SLAG.
(after Yanjukov and Zaitsev)

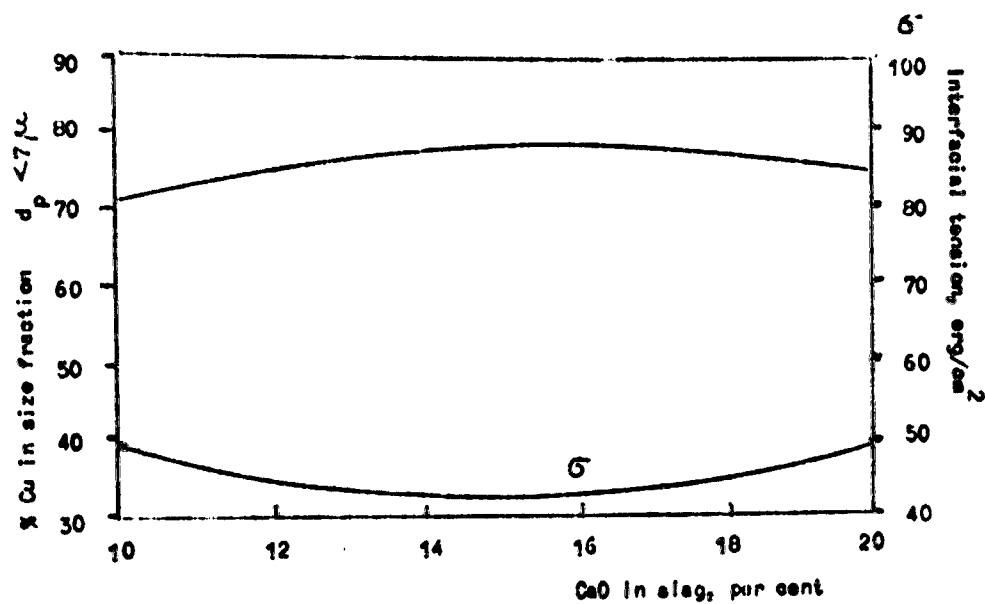


Figure A- 2.) VARIATION OF PARTICLE COALESCENCE AND INTERFACIAL TENSION WITH THE SiO_2 CONTENT OF THE SLAG.
(after Vanyukov and Zaitsev)

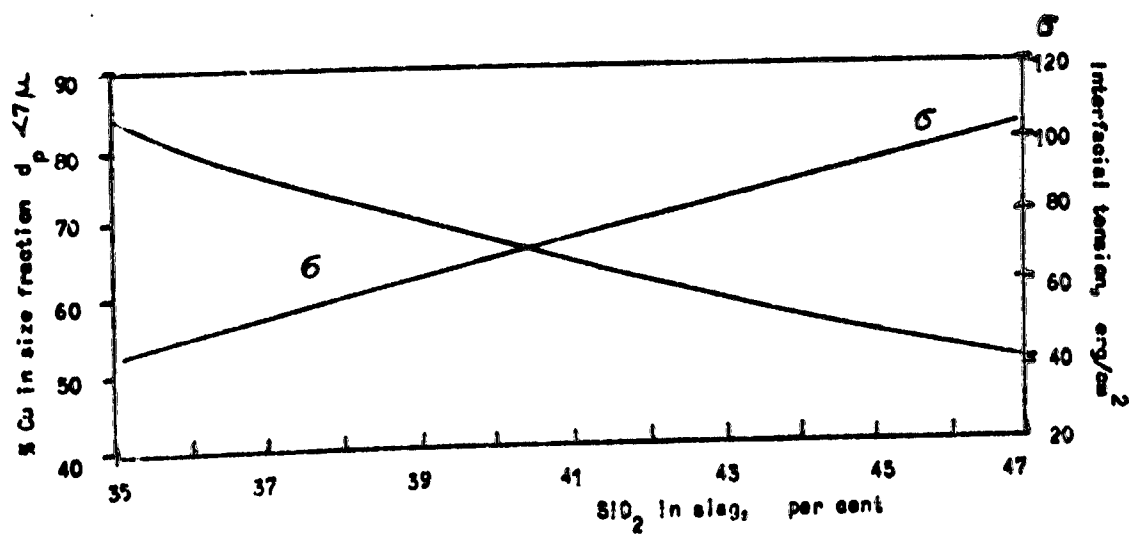
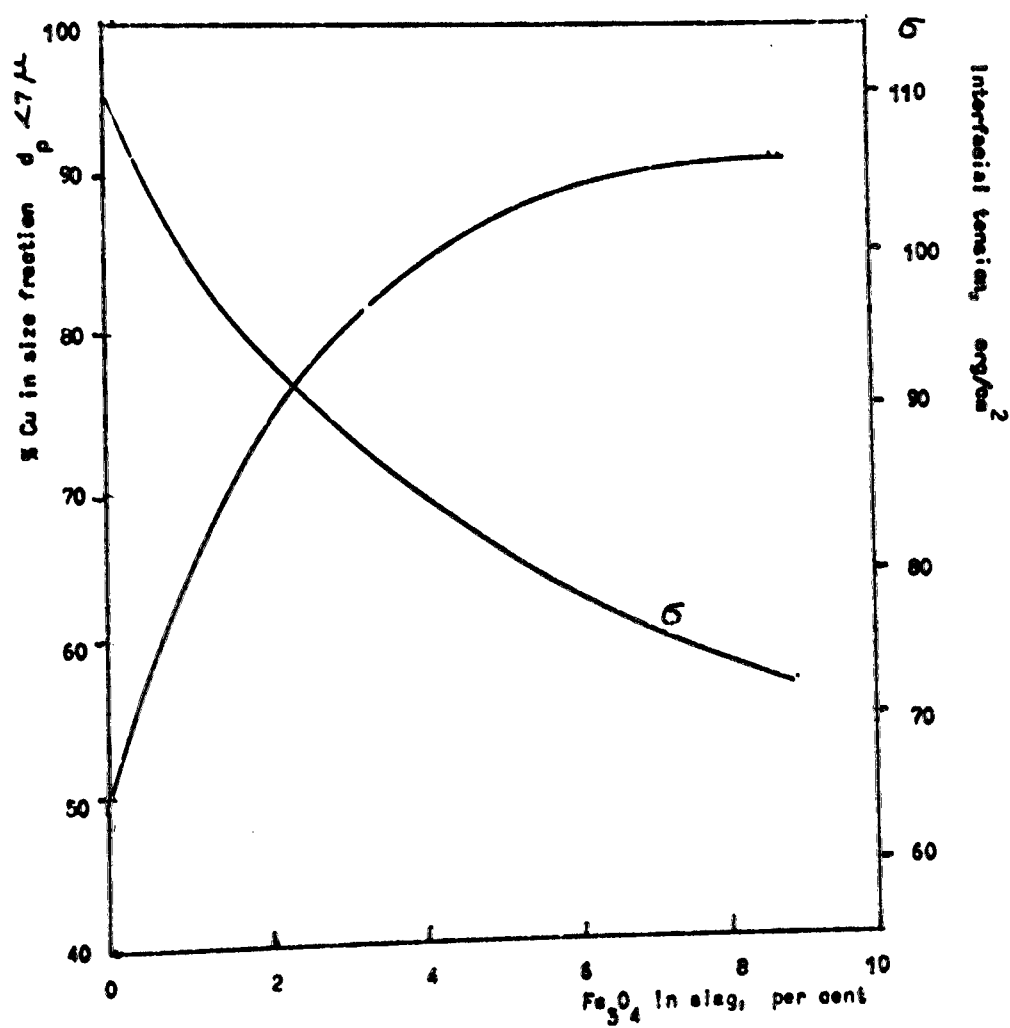


Figure A- 3.) VARIATION OF PARTICLE COALESCENCE AND INTERFACIAL TENSION WITH THE MAGNETITE CONTENT OF THE SLAG.
(after Vanyukov and Zaitsev.)



results shown in the following table:

Table A-II-1. Copper distribution in various matte size fractions as a function of the settling time of matte particles.

Settl. time min.	Copper content in slag %	Fraction of non-coalesced particles by Tuorila's formula	0,07	Size Fraction, mm					
				0,007-0,0105	0,0105-0,021	0,021-0,035	0,035-0,049	0,049-0,070	0,070-0,105
				Distribution of Cu, per cent.					
10	0,226	100	80,0	12,3	7,7	-	-	-	-
20	0,245	71,5	68,7	8,4	12,6	10,3	-	-	-
60	0,236	63,5	59,5	6,6	10,8	10,3	8,5	4,3	-
90	0,245	56,9	48,8	15,2	6,1	5,8	5,5	8,9	9,3

For the calculation of the non-coalesced fractions equations 1 and 2./ were used. Vanjukov and Zaitsev (6, 7) evaluated the interfacial energy for the quoted slag composition using the equation proposed by Sapiro (8)

$$dF = \sigma_{1,2} ds \quad 3./$$

in which dF: change of the free energy of the system; σ interfacial tension between slag and matte, ds = change of surface division between phases. As to the effect of the variation of the CaO, SiO₂ and Fe₃O₄ contents of the slag they arrived at the relationships shown in figures A-II 1 to 3. From these graphs it appears that

- a./ the increase of the CaO content (up to 16 per cent) increases the fraction of the non-coalesced particles. Higher interfacial energy is associated with a higher degree of coalescence;
- b./ the increase of the SiO₂ increases linearly the interfacial energy and simultaneously also the degree of coalescence;
- c./ with the increase of the magnetite content of the slag from 0 to about 3 per cent the interfacial energy becomes roughly halved leading to a close to 50% decrease in the extent of coalescence.

2./ Applications to the slag-matte system investigated in this study.

Although no practical work has been performed in the scope of the present study, the considerations and data displayed above may permit to draw some general conclusions pertinent to the degree of coalescence that could be expected in the slag-matte systems under investigation.

The composition of the slag quoted by the Russian researchers is altogether different from any of those of the present study. The high FeO content/...

Figure A- 4.) SURFACE TENSION AND SLAG COMPOSITION RELATIONSHIP AT 1400°C.
Slag groups I, II, III.

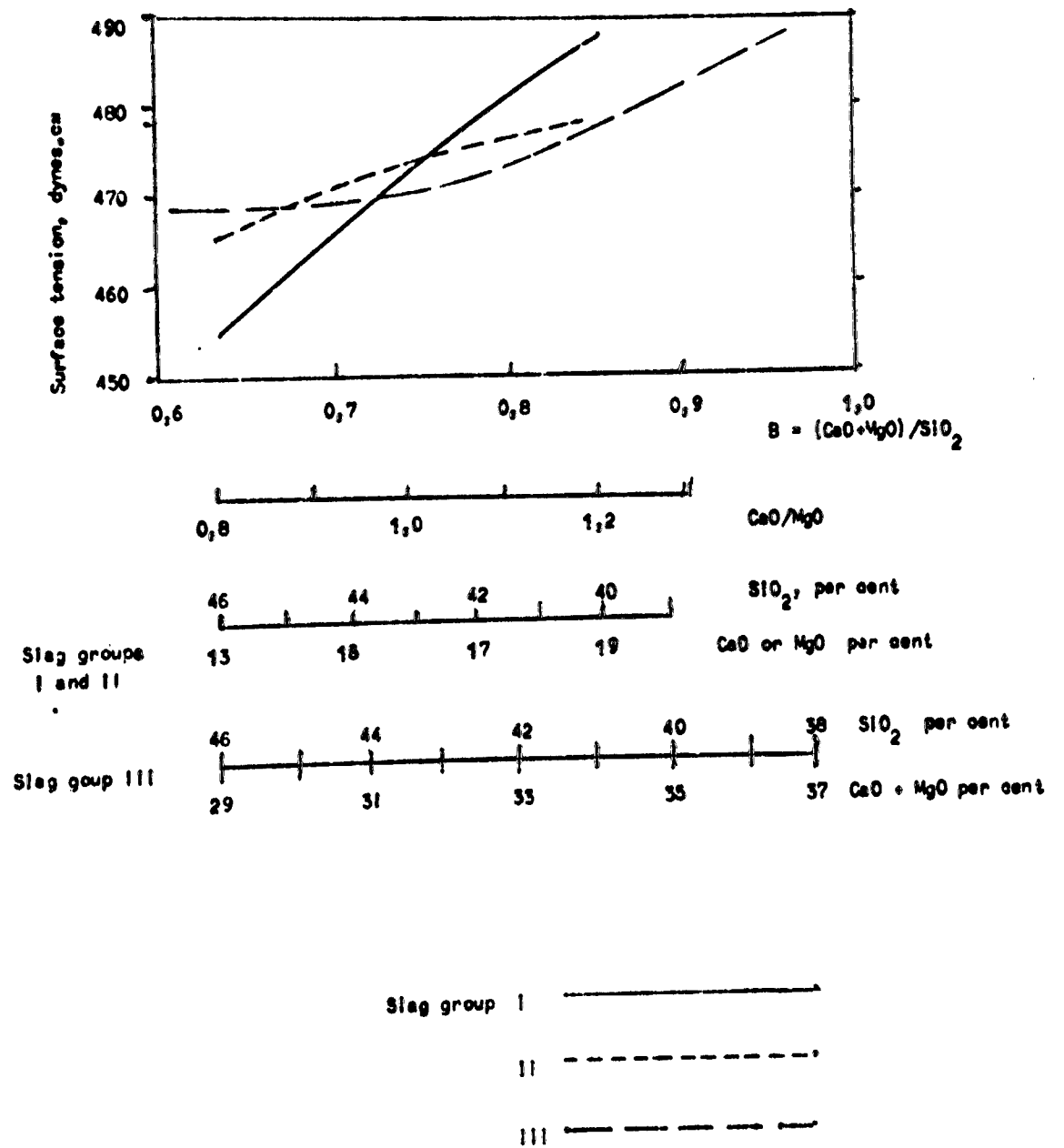


Figure A- 5.) SURFACE TENSION AND SLAG COMPOSITION RELATIONSHIP AT 1400°C
Slag groups IV and V.

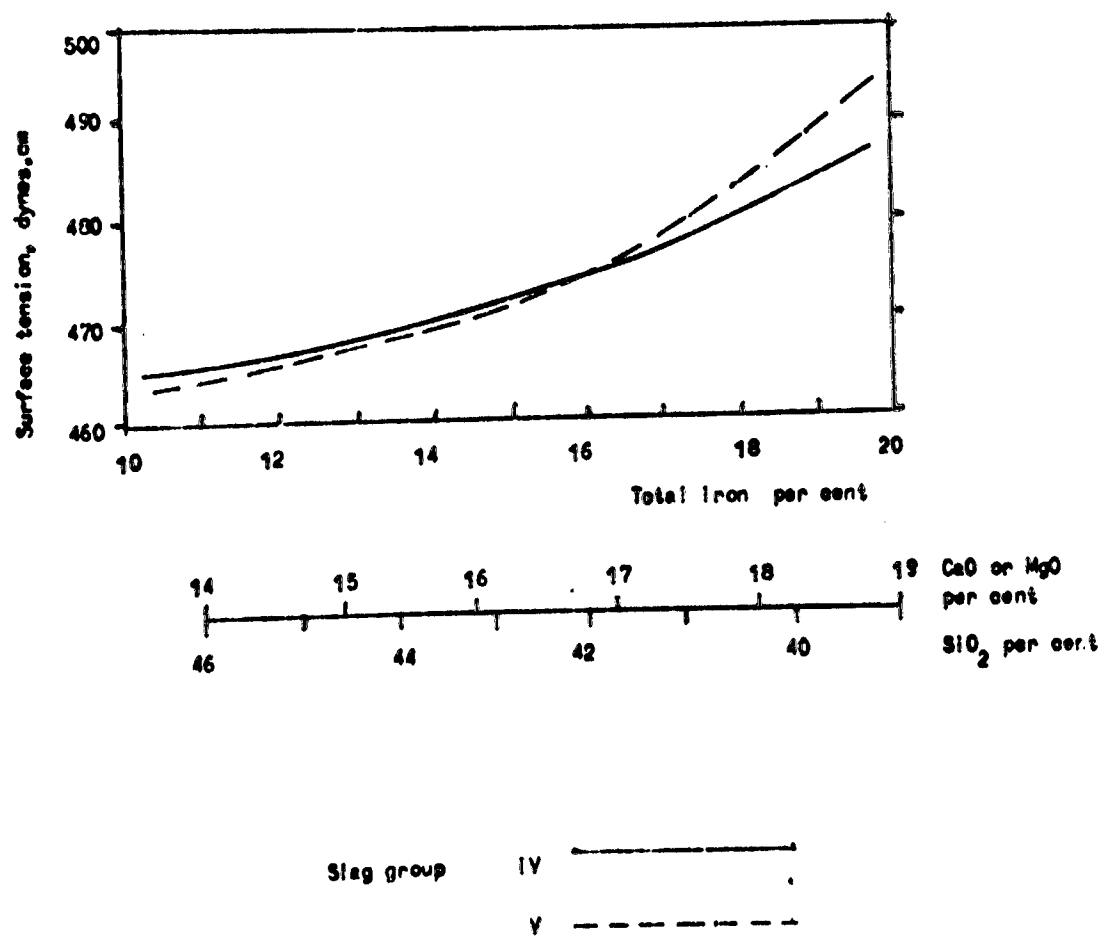
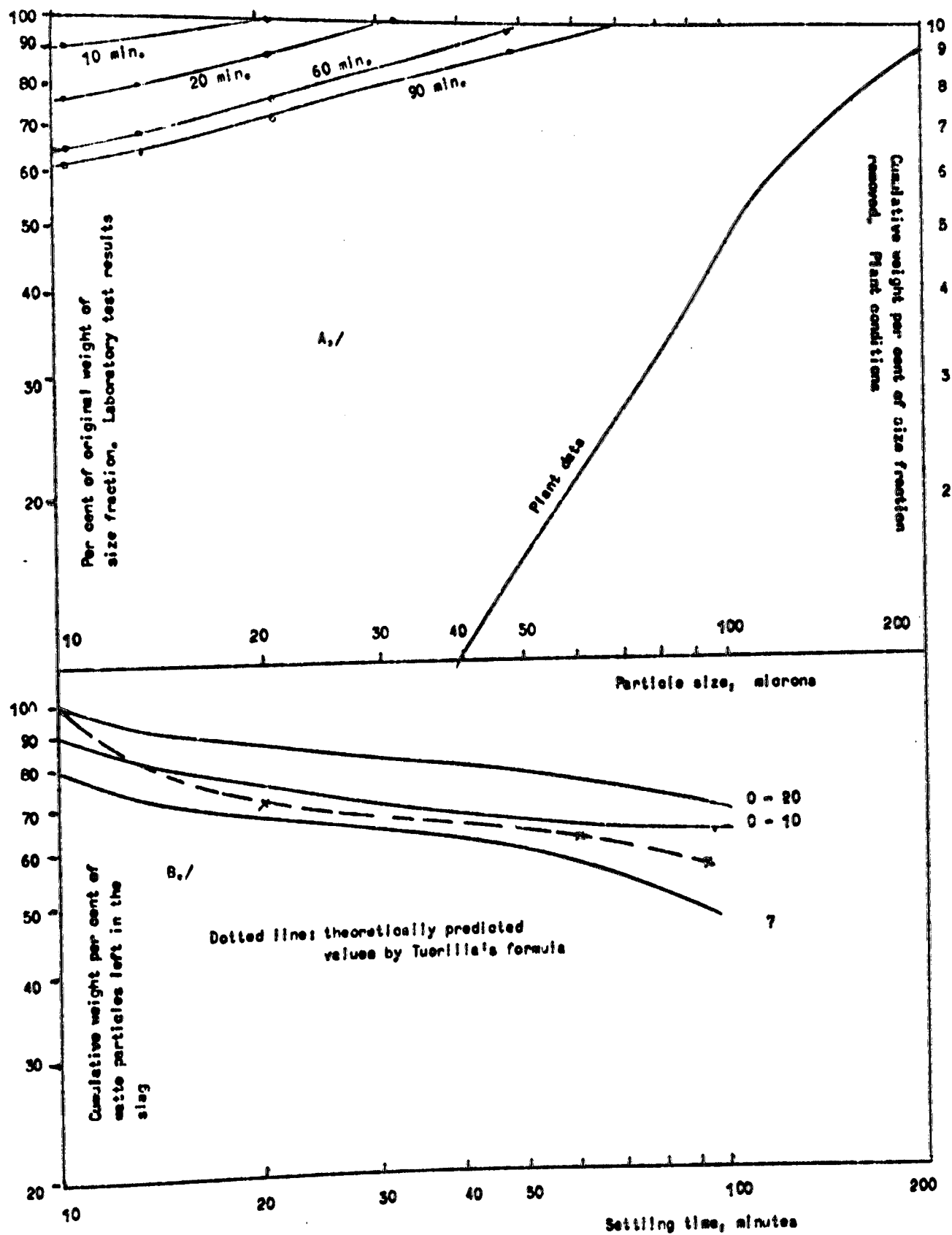


Figure A-V/L 6.)

EFFECT OF PARTICLE COALESCENCE, - WEIGHT PER CENT COPPER DISTRIBUTION
IN VARIOUS SIZE FRACTIONS OF MATTE AFTER 10, 20, 60 and 90 MINUTES
SETTLING TIME.

Laboratory test results after Vanyukov and Zaitsev.

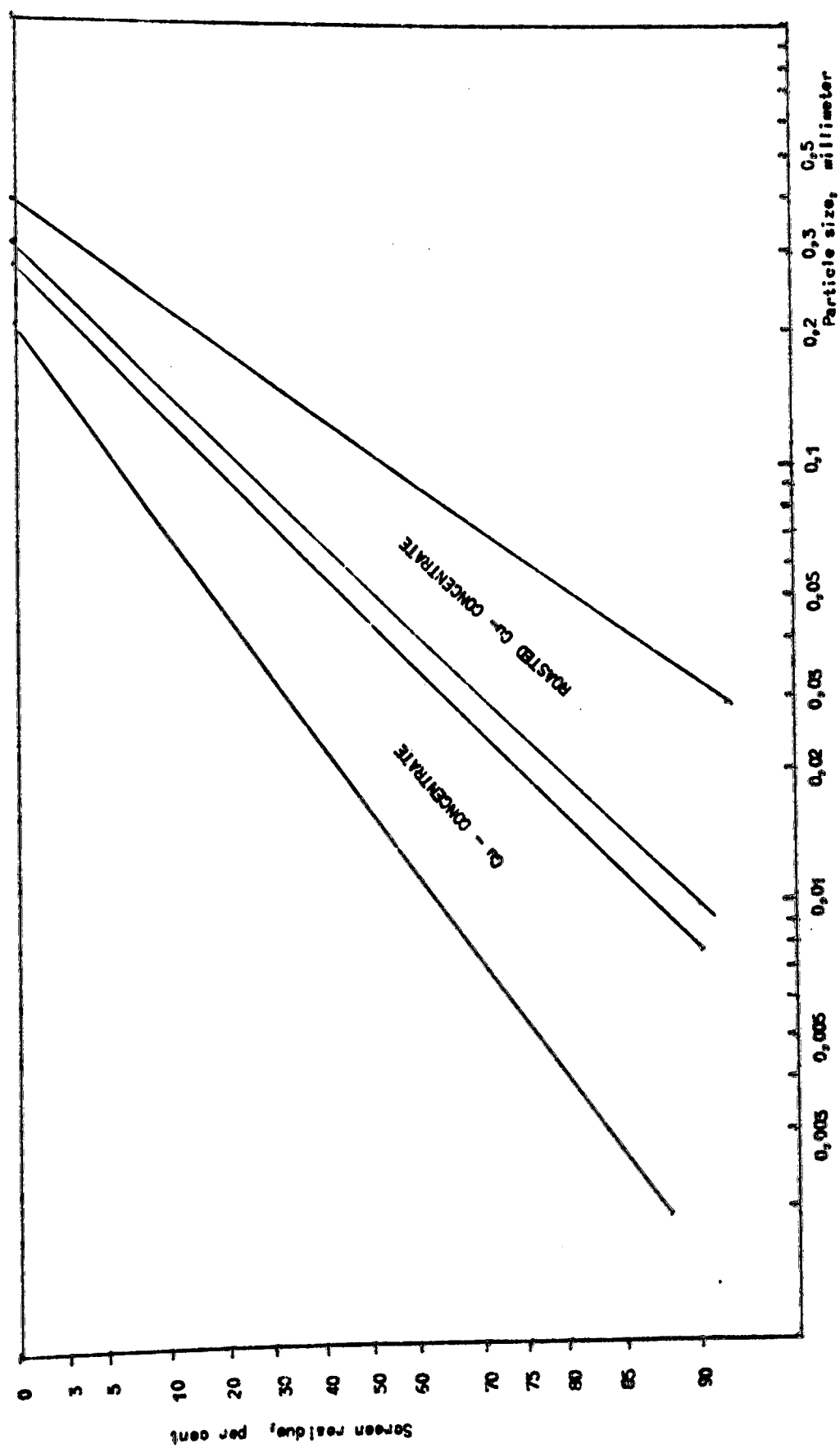


content and the complete lack of MgO and Al_2O_3 , though with a comparable low range of CaO suggests considerably reduced liquidus temperatures and viscosities, which is also apparent from the low, $1300^{\circ}C$ temperature of the tests. However, due to the higher reference temperatures ($1400^{\circ}C$) of this study the viscosities of the two types of slag, i.e. one at $1300^{\circ}C$ and the other at $1400^{\circ}C$ may become comparable. With this simplifying approach the viscosity effect should be similar in the two systems.

The change of the surface tension with slag composition in case of slags used in the present work was estimated with the aid of the formula proposed by Boni and Berge and this is summarised in figures A-II 4 and 5.). Data on the interfacial tension between fayalite slag and nickel matte with various $FeO-Ni_3S_2$ ratios in the matte is also available in the literature (9) according to which the change in both surface tension and interfacial tension in the range of a matte grade of 15 to 23% Ni_3S_2 is not very significant and follows by and large a parallel trend. Using this information as a first approximation for Cu-Ni mattes with a total metal content of 25 to 28 per cent, it is possible to judge the effect of the variation of slag composition upon the extent of the change in interfacial energy and, in the knowledge of this, on the degree of matte particle coalescence in the present system of slags. In comparing figures 4 and 5 with 1 and 2 it would appear that the increase of the CaO and MgO contents of the slag within the range of plant slag composition would exert only a very moderate effect on the particle coalescence, probably not more than 5 to 10 per cent. An increase of the SiO_2 content from 38 to 46 per cent, on the other hand, would be far more significant. However, a simultaneous reduction in the CaO and MgO constituents may, to a certain degree, counterbalance the effect of silica so that the net result would be far less spectacular than that represented by figure 2.). Likewise, the effect of the magnetite content of the slag may be considerably diminished by the fact that in electric furnace smelting of Cu-Ni bearing concentrates the level of the magnetite content in the slag is usually above the 2 per cent mark. Then by graph 3.) its variation above say 3 per cent may be equivalent to a modest 5 to 10 per cent change in particle coalescence.

Obviously, this comparison gives a semi-quantitative picture at the best of the conditions pertinent to the coalescence of matte particles in a working electric furnace. The difficulty of comparing laboratory test results with data obtained from the plant will be apparent from figure A-II/6 in which the data of Table A-II-1 obtained in laboratory tests are shown as compared with the results of this study on matte particle distribution found in plant/...

Fig. 9 A-V/1 7.) SIZE RANGE OF THE SCREEN ANALYSIS OF COPPER CONCENTRATES. (after Remmler)



in plant slags. Apart from the great difference in retention times of the matte in the slag in laboratory tests and plant runs (1½ hrs of the longest laboratory test as against some 35-36 hrs in case of the plant slag) other factors like stirring effects and, due to extensive temperature gradients in a working unit, viscosity effects which cannot be simulated properly in an experimental set-up, will render the effort of drawing quantitative conclusions rather doubtful. Notwithstanding the fact that by the indication of figure 6 b.) there appears to be a good agreement between the theoretically predicted (dotted line) and experimentally found rates of matte particle coalescence up to a size range of 10 microns.

On the other hand, the comparison of experimental and plant data clearly indicates the important role played in matte settling. Mention was made earlier of the dependance of matte particle size released during smelting upon the fineness of grinding of the concentrate. The range of the screen analysis of concentrates after Rammeler (10) is shown in figure A-II.7.) for unroasted and roasted material. This graph can be used also in the context of Cu-Ni concentrates. The grinding fineness of the ore in the practice of the Rustenburg Platinum Mines is minimum 50% -200 mesh, (i.e. 75 microns) and about half of the flotation concentrate is probably below the particle size of 40 microns. Under these conditions the diameter of the most frequent particles by the data of Rammeler is between 4 and 20 micron. On the basis of figure 7.) the range of the most frequent particle size in the RPM electric furnace slag is shifted to between about 15 to 40 micron which, in practice, represents only 1,2 per cent by weight of the total matte actually present as floating, undissolved material in the slag. It should be noted however that the samples used in the microscopic investigations were taken partly from the matte sounding hole of the furnace away from the point where the slag is being tapped.

The weight-size distribution of matte particles left after various settling times floating in the slag as shown in figure 6a.) was estimated after Vanjukov and Zaitsev on the basis of the metallic contents of these fractions. (see Table A-II-1.). Therefore the data of the table are also indicative to the number of particles in the various size ranges which for example in the 10 micron size range must be quite considerable at the start in order to account for close to 50% copper content even after 1½ hrs of settling. If the reduction in the number of the very small particles of matte could be attributed primarily to coalescence, then more than 90 per cent of the size range from the smallest size up to about 15 micron should be removed by coalescence with the larger grains in order to reach the low weight fraction/...

weight fraction estimated in this study. This would indicate a very extensive coalescence of the smallest size particles in the furnace.

By an approximate estimate, the contribution of sulphide losses in form of particle carry-over to the total losses in the slag systems of the electric smelting operation investigated in the present study would be around 50 per cent.

References to Appendix II.

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APPENDIX II B.

Calculation of the extent of stagnant flow region in the slag bed.

As the foregoing considerations outlined in Section IV would suggest, due to the action of the electrodes no uniform flow velocity exists in the molten bed of slag. From mass balance it is possible however to assign an average flow rate to the slag bulk and with some approximations calculate the extent of the stagnant region prevailing in the bath. Unfortunately, this kind of effort as the related work of Themelis and Spira (15, Sec. IV) on the flow velocities obtaining in the slag bed of a reverberatory furnace would indicate, cannot be expected to lead to highly conclusive results. Nevertheless the results obtained could be useful for general orientation purposes which would justify a short survey on this topics in the scope of this Appendix.

To estimate the velocity distribution in the moving bed of slag, the principle of channel flow may be applied to matte smelting furnaces of rectangular cross section. In general operating practice the depth of the slag bath in the reference furnaces varies between 1,3 and 1,6m, but for the the purpose of illustrating the principles involved the calculations will be based on a shallower depth of 1,3. m.

Extent of the stagnant flow region in the slag bed.

The idealized conditions for the calculation of the flow profiles whereby an average temperature for the slag bath and laminar flow with no electrode action is assumed could be attained in certain areas of the matte settling zone of the furnace. In case of rectangular ducts for a constant axial pressure gradient ($dp/dx = \text{const.}$) the parabolic velocity distribution is given by the well-known formula

$$u(x) = \frac{(-dp/dx) l^2}{2\eta} \left[1 - \left(\frac{x}{l} \right)^2 \right] \quad 1./$$

and for laminar flow according to Geiger and Poirier (Ref 13 Sec. IV) the velocity distribution in the knowledge of the volumetric flow rate may be estimated with the expression

$$u = \frac{3 Q_w}{2 l_w H} \left[1 - \left(\frac{x}{H} \right)^2 \right] \quad 2./$$

here Q_w =volumetric flow rate, l_w =width, H =depth of flow media. For an example let the conditions obtaining with the reference furnaces apply, that is: width (l_w) = 7m; slag depth (H) = 1,3m; average slag flow rate (without converter return)= 21 tons/hr; specific gravity of slag at a reference temperature of 1400°C = 2,8 g/cm³. Therefore/...

Therefore $W = 21,0/2,8 = 7,5 \text{ m}^3 \text{ slag/hr}$

With equ.2./ the expression of vertical velocity distribution in the particular case is then

$$u = \frac{3 Q_w}{2 L_w H} \left[1 - \left(\frac{x}{H} \right)^2 \right] = \frac{3 \times 7,5}{2 \times 7 \times 1,3} \left[1 - \left(\frac{x}{1,3} \right)^2 \right]$$

$$= 1,39 - 0,89 x^2 \quad 3./$$

The mean residence time is expressed as $\theta = L/u$, thus the fraction of slag that remains for 2θ in the furnace is between $x = x'$ and $x = H$ when x' is defined as the value of the distance x in velocity relationships as $u = \bar{u}/2$.

Since $u = \frac{Q}{L_w H} \quad 4./$

$$\text{then } u = \frac{1}{2} \frac{7,5}{7 \times 1,3} = 0,413 \text{ m.hr}^{-1} = 0,023 \text{ cm.sec}^{-1}$$

This particular average velocity will be representative of a slag bed of

$$0,413 = 1,39 - 0,89 x^2 \quad \text{that is } x = 1,0 \text{m}$$

Therefore the fraction of slag with residence time greater than 2θ will be

$$\frac{1,3 - 1,0}{1,3} = 0,23$$

which is the expression of the ratio between the free-flowing and stagnant region of the slag bath and would suggest that only about 23 per cent of the total flow falls into the stagnant flow region. Obviously, under the idealised conditions set for this estimate the stagnant region is smaller than that measured in a working unit with the use of radioactive tracers.

APPENDIX III.

The effect of the extent of overlap between the active areas of electrodes.

Pertaining to the extent of overlap of the working areas between two electrodes it is apparent that the principle of heat or energy distribution in concentric circles around the periphery of the electrodes will break down in the areas of overlap. A more uniform heat, energy or magnetic flux profile with narrower range of variation will be characteristic to these areas especially between electrodes of the same phase. Correspondingly, the relevant viscosity and electrical conductivity profiles outlined in Section IV will be also modified.

As to the energy distribution between the overlapping and the total working regions of the electrodes let the conditions be represented by figure A/III-1, in which A=total cross-sectional active area around one electrode, λ_1 =half width of the area of overlap, $\lambda_2 = d_A - \lambda_1$ (d_A =diameter of active zone). In the graph a uniform y component of the energy field E_y is produced in the region of overlap of the two circles. The energy field is given by (1)

$$E_y = \frac{\Delta_1}{2\pi} \iint \frac{x}{x^2 + y^2} dx dy \quad 1./$$

This equation is integrated in the limits

$$\begin{aligned} \text{for } x & \quad x = -\lambda_1 \text{ to } \lambda_2 \\ \text{for } y & \quad y = 0 \quad \sqrt{\left(\frac{\lambda_1 + \lambda_2}{2}\right)^2 - (x - \lambda_1)^2} \end{aligned}$$

$$\text{that is} \quad E = \frac{\Delta_1}{2\pi} \int_{x=-\lambda_1}^{\lambda_2} \int_{y=0}^{\sqrt{\left(\frac{\lambda_1 + \lambda_2}{2}\right)^2 - (x - \lambda_1)^2}} \frac{x}{x^2 + y^2} dx dy \quad 2./$$

On solving 2./ the total area against the overlap, that is a dimensionless area expression will be

$$\frac{A}{\lambda_1^2} = \frac{(\lambda_2/\lambda_1 + 1)^2}{2} \left[\pi - 2 \cos^{-1} \frac{(\lambda_2/\lambda_1) - 1}{(\lambda_2/\lambda_1) + 1} + \frac{4\sqrt{\lambda_2/\lambda_1} \left[(\lambda_2/\lambda_1) - 1 \right]}{\left[(\lambda_2/\lambda_1) + 1 \right]^2} \right] \quad 3./$$

and the energy distribution per unit active area becomes

$$\frac{E \lambda_1}{\Delta_1 A} = \frac{\lambda_2/\lambda_1}{(1 + \lambda_2/\lambda_1)^2 \left[\pi - 2 \cos^{-1} \frac{(\lambda_2/\lambda_1) - 1}{(\lambda_2/\lambda_1) + 1} + \frac{4\sqrt{\lambda_2/\lambda_1} \left[(\lambda_2/\lambda_1) - 1 \right]}{\left[(\lambda_2/\lambda_1) + 1 \right]^2} \right]} \quad 4.$$

Figure A- 1- 1. CROSS SECTIONAL CONFIGURATION OF TWO ACTIVE AREAS WITH A UNIFORM Y COMPONENT OF THE MAGNETIC FIELD IN THE REGION OF OVERLAP OF THE TWO CIRCLES.

Figure A

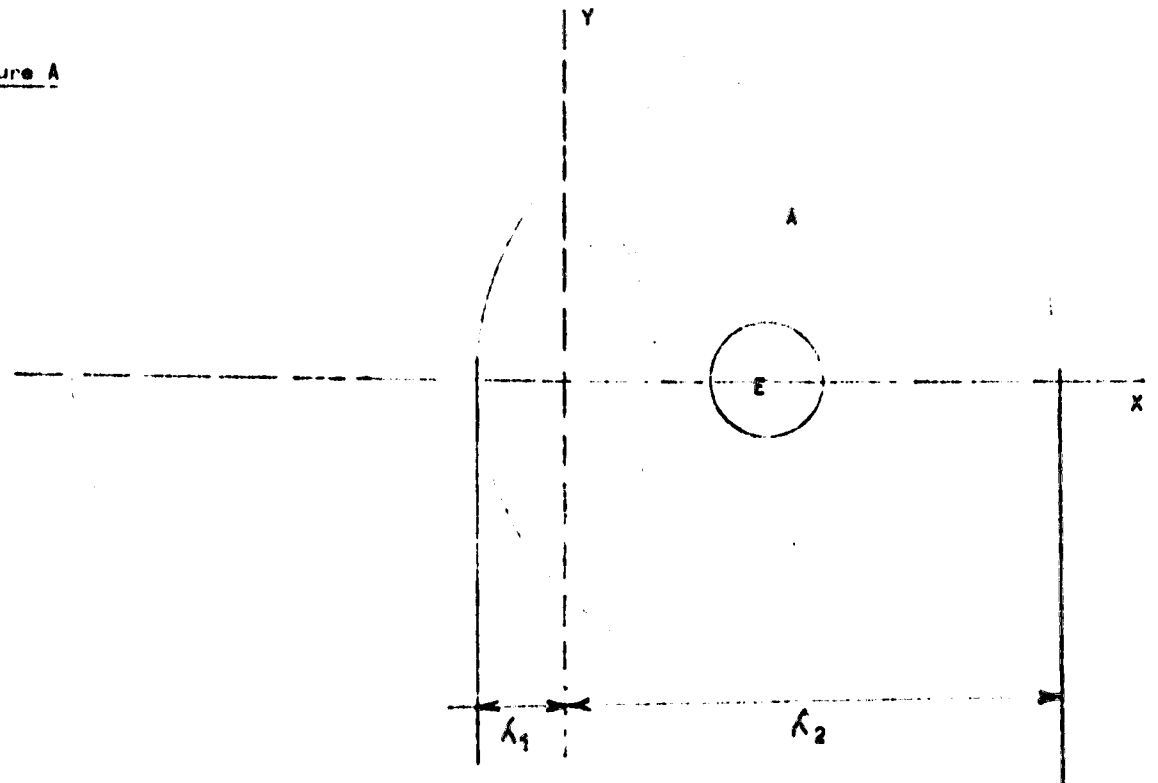


Figure B

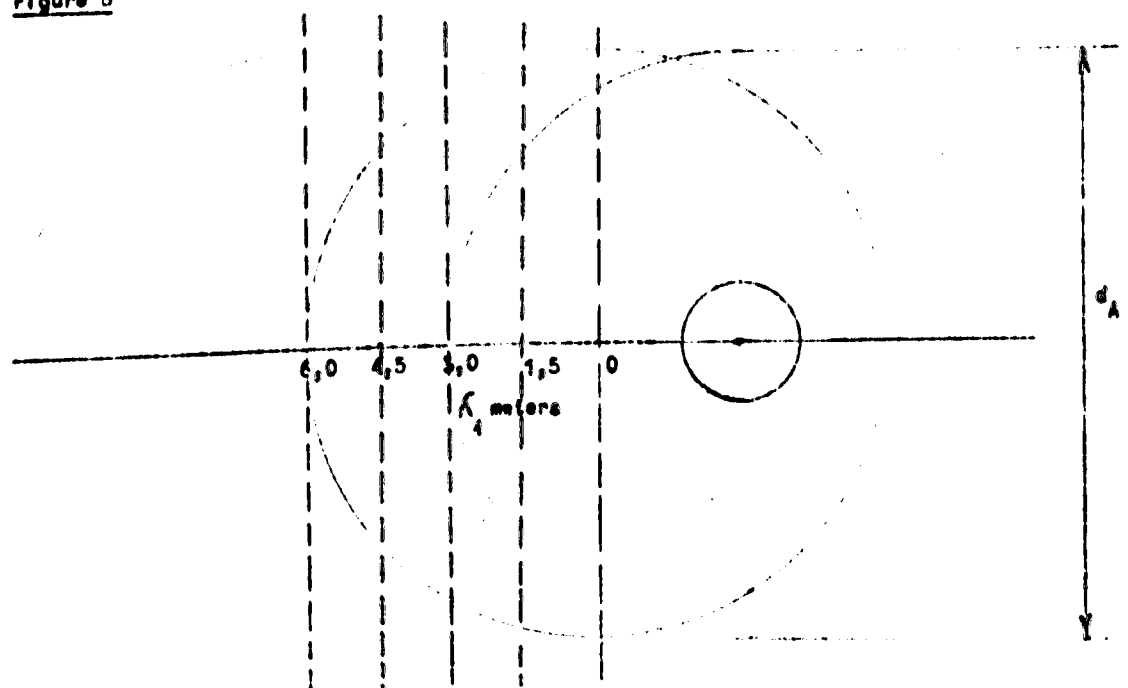
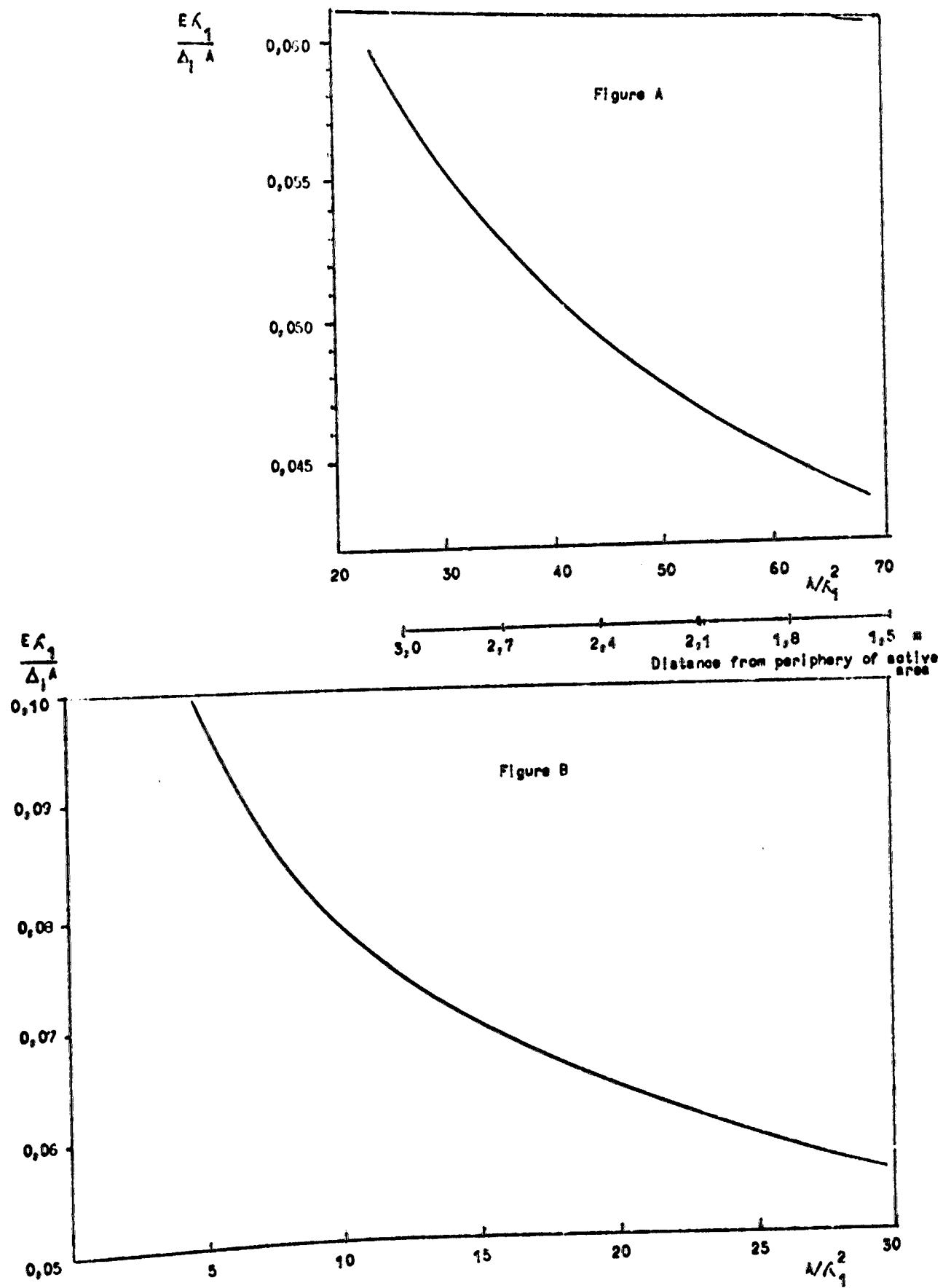


Figure A- 1 -2. ENERGY FIELD PER UNIT ACTIVE AREA OF ELECTRODE AS A FUNCTION OF THE DIMENSIONLESS CROSS SECTIONAL AREA.



To apply equation 4./ to the particular case of rectangular electric furnaces the mode of procedure is as follows: in case of a 6-in-line electrode configuration the value of $2\lambda_1$ is usually between the limits of $0,25 r_A$ and $0,5 r_A$ (r_A = radius of active area), then λ_1 is between $0,125 r_A$ and $0,25 r_A$ as shown in graph A/III-1b. Obviously the range $\lambda_1 = 0,125 r_A$ to $0,25 r_A$ corresponds with $\lambda_2 = 0,875 r_A$ to $0,750 r_A$. On taking now 5 equal distances between $\lambda_1 = 0,125 r_A$ and $0,250 r_A$ the following values will be obtained:

λ_1	0,125	0,150	0,175	0,200	0,225	0,250
$\lambda_2 = 1 - \lambda_1$	0,875	0,850	0,825	0,800	0,775	0,750
λ_2 / λ_1	7,00	5,67	4,71	4,00	3,45	3,00
$(1 + \lambda_2 / \lambda_1)^2$	64,0	49,5	36,3	25,0	19,8	16,0

With these data A/λ_1^2 and the energy distribution $E(\lambda_1/A\lambda_1)$ can be calculated as shown in figure A/III-2a representing the extent of overlap up to the centre line of the electrode, that is a distance of $0,5 r_A$ ($\lambda_1 = 0,25 r_A$) if $r_A = 5 D_e$. Graph A/III-2b is an extension of 2a for even larger values of λ .

These figures clearly indicate the trend given by equ.4., i.e. with the increase of λ_1 the extent of overlap increases leading to the decrease of A . The current density Δ_1 per unit area of A remains constant but the total energy involved increases in the area of overlap represented by λ_1 and decreases in A in proportion to the change of the ratios of the areas. The shift in energy relations becomes more significant with the increase of the extent of overlap and is in proportion with the area increase of this region.

In the overlapping zone one would expect an increase in slag motion on account of the lifting action of both buoyancy and electromagnetic forces in the vicinity of the electrodes. The force generated by each electrode would move the slag in a counter-current fashion from the electrode surface into the active zone (ascending movement) and into the overlapping regions in which, as a result of exchange in momentum, a balancing action in magnetic field, temperature or energy distribution would ensue. As a result, the even distribution of viscosity and electrical conductivity contours as outlined in figures IV-23 and IV-24 for the ideal case of zero, or negligible interaction of the active electrode areas will be considerably modified.

In furnace geometry relations it was shown that the diameter of the active electrode area d_A and the electrode diameter can be correlated for the conditions of the reference furnaces as

$$d_A = k\sqrt{D_e} \quad \text{where } k = 5,0$$

In connection with the foregoing considerations since

$$\lambda_1 = d_A - \lambda_2 \quad 5./$$

we can write

$$\lambda_1 = k\sqrt{D_e} - \lambda_2 \quad 6./$$

or

$$\lambda_1 + \lambda_2 = k\sqrt{D_e} \quad 7./$$

that is for the energy distribution we will get

$$\frac{E(5,0\sqrt{D_e} - \lambda_2)}{\Delta_1 A} = \frac{\lambda_2/\lambda_1}{(1 + \lambda_2/\lambda_1)^2} \cdot \frac{1}{N} \quad 8./$$

$$\text{where } N = \pi - \cos^{-1} \frac{(\lambda_2/\lambda_1) - 1}{(\lambda_2/\lambda_1) + 1} + \frac{4\sqrt{\lambda_2/\lambda_1} (\lambda_2/\lambda_1) - 1}{(\lambda_2/\lambda_1 + 1)^2}$$

In terms of the distance of electrode separation the geometrical relationships are given by the following equation

$$d_A = 2,9 S^{0.82}$$

therefore

$$\frac{E(2,9 S^{0.82} - \lambda_2)}{\Delta_1 A} = \frac{\lambda_2/\lambda_1}{(1 + \lambda_2/\lambda_1)^2} \cdot \frac{1}{N} \quad 9./$$

Equation 8./ expresses the energy distribution in the separate zones as a function of the change of the ratio between the maximum half width of the overlapping zone and the rest of the reaction zone width when the size of the furnace, as expressed by the electrode diameter, is varied. It is basically the expression of conditions shown in figures VI-7 and VI-8.

Reference

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