

FLAME HARDENING
WITH AN
ELECTRICALLY AUGMENTED BURNER

A thesis presented for
the degree of Doctor of Philosophy
in the
University of London

by
ROBERT JAMES TAIT

September 1971

The John Percy Research Group
in Process Metallurgy,
Department of Metallurgy,
Imperial College,
London S.W.7.

ABSTRACT

An electrically augmented burner has been developed which burns pre-mixed natural gas and air in a turbulent flame. An a.c. discharge of up to 4 amps at 1 kilovolt has been initiated and maintained in the flame without the necessity of increasing flame conductivity by seeding with alkali metal salts.

Heat transfer measurements at water-cooled surfaces have shown that the highest rates of heat transfer are obtained when the mixture is fuel-rich. The distribution of heat flux at a water-cooled surface has been determined with a split-plate calorimeter, using the method of Nestor and Olsen⁽³³⁾. The maximum values of these distributions lie within the range of values obtained by other workers⁽³⁴⁻³⁷⁾ using similar systems.

The burner has been used for flame hardening the surface of En 8, a medium carbon steel, by moving slabs of this steel at known speeds over the burner and an adjacent water quench. The distribution of the burner heat flux at the surface of a moving slab has been assumed to have a similar shape to the distributions obtained at the surface of the water-cooled calorimeter. The maximum value and the spread of the distribution at the surface of the slab have been determined by comparing the temperatures measured at the surface of the slab with the surface temperatures predicted by an integral profile solution of the heat conduction equation for the moving slab. The integral profile solution has also predicted the limiting values of slab speed and quench heat flux at which martensite begins to form on the surface of the slab. The effect of conduction in the direction of motion of the slab on these predictions has been discussed. Under certain conditions, hardened surface layers of appreciable thickness have been produced which are similar to those obtained commercially.

CONTENTS

	page
LIST OF PLATES, TABLES, FIGURES	5
<u>I</u> INTRODUCTION	10
<u>II</u> PREVIOUS WORK	
1 Electrically augmented burners	13
2 Flames and burners	20
3 Electrical discharges in gases	26
4 Electrodes	30
5 Heat transfer from flames and arcs	34
6 Heat conduction in slabs	43
7 Flame hardening	50
<u>III</u> THEORY	60
<u>IV</u> EXPERIMENTAL APPARATUS	
1 Burners	71
2 Electrical power supplies	78
3 Gas and air supplies	80
4 Calorimeters	83
5 Steel slabs	90
6 The quench	92
<u>V</u> EXPERIMENTAL TECHNIQUES	
1 Burner development	95
2 Arc characteristics	102
3 High speed photography.	104
4 Calorimetry	105
5 Temperature profiles in slabs	107
6 Flame hardening	107
<u>VI</u> RESULTS	
1 Burner operation	109
2 Heat transfer to water-cooled surfaces	119
3 Heat transfer to the surface of a steel slab	143
4 Quenching the surfaces of slabs	158
5 Flame hardening the surfaces of slabs	164

	page
<u>VII</u> DISCUSSION	
1 Burner design	171
2 The nature of the discharge	174
3 Heat transfer to water-cooled surfaces	176
4 Heat transfer to the surface of a steel slab	181
5 Quenching	198
6 Flame hardening	201
<u>VIII</u> CONCLUSIONS	207
APPENDIX A	208
APPENDIX B	215
APPENDIX C	230
LIST OF SYMBOLS	240
LIST OF REFERENCES	242
ACKNOWLEDGEMENTS	244

PLATES

plate		page
<u>IV</u>	1 The small vortex-stabilized burner	73
	2 The large vortex-stabilized burner	76
	3 66 kVA transducer and earthed cage	81
	4 Slab moving over the burner and quench	94
<u>VI</u>	1 Voltage transients in a.c. discharges	116
	2 Sequences from high speed cine films	117
	3 Microstructure showing thin layer of martensite	167
	4 Microstructure showing thick layer of martensite	168

TABLES

table		
<u>II</u>	1 Work functions of various materials	31
	2 Electrical resistance of various oxides	32
	3 Calculated heat fluxes	36
	4 Reported values of heat flux	41
<u>IV</u>	1 Dimensions of the concentric calorimeters	86
<u>VI</u>	1 Operating conditions for the burners	109
	2 Conditions under which voltage transients were obtained	115
	3 Conditions under which high speed cine films were taken	118
	4 Range of stable airflows for various methane flow rates	125
	5 Radii of concentric calorimeters	132
	6 Temperature increases from split-plate calorimeter	138
	7 Distance between thermocouples and surface of slab	143
	8 Experimental conditions for runs C1 to C7	144
	9 Calculated values of 'a' for run C1	148
	10 Values of $\dot{q}''$ and 'a' for runs C1 to C7	150
	11 Slab speeds and penetration distances	154
<u>VII</u>	1 Flow regimes during blow-off	172
	2 Slab depths and rate of quenching	199
	3 Rate of quenching and slab speed	200

table		page
A 1-5	Temperature differences obtained from concentric calorimeters	209
6	Heat fluxes from the small vortex-stabilized burner	214
B 1	Calibration of thermistors	216
2-11	Thermistor readings	218
12	Temperatures obtained from table B2	228
13	Heat fluxes obtained from B2 - B11	229
C 1	Physical properties of En 8	238
2	Data obtained from figures C1 - C7	239
3	Slab temperatures (calculated)	239

FIGURES

figure

<u>II</u> 1	Karlovitz-type burners	14
2	Detail of a Karlovitz burner	15
3	Modified Karlovitz burner	15
4	Plasma jet type of burner	16
5	Burner with porous annulus	18
6	Burner design of Fells et al	19
7	Flame front	20
8	Flash-back	21
9	Blow-off	22
10	Flash-back and blow-off limits for methane	22
11	Burning velocity of methane	23
12	Inflammability limits for methane	23
13	Sudden expansion type of burner	24
14	Vortex-stabilized type of burner	25
15	Characteristics of electrical discharges	26
16	Paschen's law curve	27
17	Structure of an arc	27
18	Static characteristics of an arc	28
19	Characteristics of an a.c. arc	28
20	Load line and static characteristics	29
21	Heat fluxes from point, line and surface probes	37
22	Split-plate calorimeter	40
23	Co-ordinate system for a semi-infinite slab	43
24	Surface heat flux as a function of time	46

figure		page
<u>II</u>	25 Slab divided for finite difference method	48
	26 Iron carbon diagram	50
	27 Effect of carbon content on hardness	51
	28 Temperature-time relationship for austenite transformation	52
	29 Isothermal transformation diagram for En 8	53
	30 Results of Jominy test on En 8	54
	31 Theoretical rate of cooling of a bar	55
	32 Progressive flame hardening	57
	33 Heat flux during boiling	59
<u>III</u>	1 Co-ordinates of the system	61
	2 Burner and quench heat flux	61
	3 " " " " "	62
	4 Normal distribution of heat flux	64
	5 Triangular distribution of heat flux	66
	6 Rectangular distribution of heat flux	66
	7 Surface temperatures from various heat flux distributions	68
	8 Surface temperatures from burner and quench heat fluxes	70
<u>IV</u>	1 The small vortex-stabilized burner	72
	2 The large vortex-stabilized burner	75
	3 Power supply for the small burners	79
	4 Plan of earthed cage and transducer	82
	5 Tube calorimeter	85
	6 A typical concentric calorimeter	85
	7 Split-plate calorimeter	87
	8 Thermistor circuit	89
	9 Arrangement of a slab over the burner	91
	10 Cross-section of slab, showing thermocouples	91
	11 Positions of thermocouples in an insulated element	91
	12 Arrangement of quench	93
<u>V</u>	1 Simple augmented burner	96
	2 Arc filament	96
	3 Cathode and simple tube burner	96
	4 Sudden enlargement burner	100
	5 " " " in lime zirconia	100

figure		page
<u>V</u>	6 Vortex-stabilized burner	100
	7 " " " with annular electrode	100
	8 Arc attached to top of annular electrode	100
	9 Circuit used to obtain voltage transients	103
<u>VI</u>	1 Blow-off limits for simple tube burners	111
	2 Criterion for blow-off on simple tube burners	111
	3 Methane inflammability limits	112
	4 D.c. characteristics of the small vortex-stabilized burner	114
	5 Effect of water flow rate on rate of heat transfer to tube calorimeter	120
	6 Heat transfer from small vortex-stabilized burner to tube calorimeter.	122
	7 Heat transfer from small vortex-stabilized burner to tube calorimeter	124
	8 Heat transfer from small vortex-stabilized burner to tube calorimeter	126
	9 Effect of water flow rate on rate of heat transfer to concentric calorimeters	127
	10 Heat transfer from small vortex-stabilized burner to large concentric calorimeter	129
	11 Heat transfer from small vortex-stabilized burner to large concentric calorimeter	130
	12' Heat flux distribution from concentric calorimeters	131
	12 Heat flux distributions from small vortex-stabilized burner	133
	13 Heat flux distributions from small vortex-stabilized burner	134
	14 Increase in water temperature for split-plate calorimeter	137
	14' Flow chart for computer programme used to solve equation <u>VI</u> 11	139
	15 Heat flux distributions from large vortex-stabilized burner	140
	16 Heat flux distributions from large vortex-stabilized burner	141
	17 Heat flux distributions from large vortex-stabilized burner	142
	18 Temperature profile measured in the slab for run C1	146
	19 Comparison between measured and predicted surface temperatures	149

figure		page
<u>VI</u>	20 Heat flux distributions at the surface of a slab	151
	21 Heat flux distributions at the surface of a slab	152
	22 Heat flux distributions at the surface of a slab	153
	23 Measured and calculated temperature profiles in the slab	156
	24 Surface temperatures, inside and outside insulated element	157
	25 Surface temperatures measured during quenching	159
	26 " " " " " " " "	160
	27 " " " " " " " "	161
	28 " " " " " " " "	163
	29 Tallysurf measurements	170
<u>VII</u>	1 Surface temperatures predicted from a rectangular distribution of heat flux	182
	2 Effect of dimensionless slab speed on the maximum value of dimensionless surface temperatures	184
	3 Dimensionless surface temperatures obtained using quadratic and cubic temperature profiles	187
	4 Asymmetric distribution of burner heat flux	192
	5 Rates of heat transfer plotted against radius	194
	6 Comparison between heat flux distributions obtained from split-plate calorimeter and from moving slab	197
B	1 Calibration curve for no.1 thermistor	217
C	1-7 Temperatures measured in the slab	231

I INTRODUCTION

An electrically augmented flame is a combination of a combustion flame and an electrical discharge, the input of electrical energy being a significant proportion of the total energy of the flame. This description does not include either the plasma jet which comprises an electric arc stabilized by a stream of inert gas or a flame in an electric field.

Considerable effort has been devoted to designing electrically augmented burners ⁽¹⁻⁹⁾, and rates of heat transfer have been determined from these burners ^(3,4). Possible industrial applications do not appear to have been investigated in detail, with the exception of one patent which describes a flame-arc combination installed at the bottom of a cupola ⁽¹⁴⁾. Electrically augmented burners would be expected to have widespread application in the high temperature processing of metals if the following remarks hold true:-

1. High rates of heat transfer are available.
2. The electrical discharge can be used to stabilize the combustion of a fuel-rich mixture to produce a high temperature reducing gas.

The rate of heat transfer obtained from an electrically augmented flame would be expected to be intermediate between the rates obtained from a fuel-air flame and an electric arc. This suggests that electrically augmented burners could be used for heating, melting, welding and cutting metals. If the composition of the combustion products from this type of burner could be made sufficiently reducing then the smelting of metallic oxides could be carried out but even if these extreme conditions could not be obtained, a non-oxidising atmosphere would be a distinct advantage when heating metals.

All the burners reported in the literature ⁽¹⁻⁹⁾ require that the electrical conductivity of the flame is increased by the addition of a very small concentration of a readily ionized salt so that an electrical discharge can be passed through the flame. The industrial application of this type of burner would seem to be more promising if this process of "seeding" the flame with salts such as alkali metal halides could be eliminated, for the following reasons:-

1. Seeding will result in corrosive products of combustion, particularly at high temperatures.
2. Both the seed material and the equipment required to introduce the seed into the gas stream are likely to be expensive.

In this work, flame hardening was chosen as the specific application for an electrically augmented burner because this process requires a high rate of heat transfer under non-oxidising conditions. Also, flame hardening is an important method of heat treatment, giving a hard surface to a component made from plain carbon steel yet leaving the desirable properties of the untreated steel in the main body of the component. Alternative methods of surface hardening usually require the use of alloy steels which are more expensive than plain carbon steels. Flame hardening is carried out using an oxy-gas flame which could be replaced by an electrically augmented burner with the following advantages:-

1. Oxidation of the surface would be less likely because the electrically augmented burner could be operated with a fuel-rich mixture.
2. Fuel costs would be reduced if natural gas and electricity replace cylinders of oxygen and acetylene.

This thesis describes the development of a small electrically augmented burner and the use of a scaled-up version for flame hardening. The importance of developing a burner for an industrial application has been considered throughout this work. For this reason, natural gas has been used as a fuel and a.c. power has been supplied to the electrodes to eliminate rectification. All the experiments on flame hardening have been carried out with one type of steel in order to eliminate the effect of composition of the steel on the process. Normalised En 8 was chosen, a medium carbon steel which is commonly used in flame hardening.

A theoretical section is included in which the heat conduction equation is solved for a semi-infinite slab moving over a heat flux distribution of known shape. This solution is relevant to the process of flame hardening in which a slab moves over a burner and quench. The temperature at the surface of the slab can be predicted by the solution if the distribution of surface heat flux is known. Alternatively, the solution will predict the surface heat flux if the surface temperature is measured as the slab passes over the burner and quench.

The objectives of the work presented in this thesis can be summarized as follows:-

1. To develop an electrically augmented burner which will operate with a.c. power on a natural gas-air mixture without the necessity of seeding the flame.
2. To determine rates of heat transfer from this burner to solid surfaces.
3. To investigate the feasibility of flame hardening a medium carbon steel with this burner.

II PREVIOUS WORK

1. ELECTRICALLY AUGMENTED BURNERS.

The first successful attempt at augmenting combustion flames with electrical power was made in 1924 by Southgate (45) who passed burning gas through an arc discharge. Interest in combining flames with electrical discharges was revived in the early 1960's when Karlovitz in the U.S.A. and Weinberg and co-workers at Imperial College published their findings. Since then, Fells and co-workers at the University of Newcastle-upon-Tyne have investigated electrically augmented burners.

The following section will deal with investigations carried out on the Karlovitz type of burner, the work of Weinberg and of Fells, and will end with a brief review of the relevant patent literature.

(a) Burners of the Karlovitz type.

Karlovitz claims to have produced a diffuse electrical discharge over the entire flame in a number of different burner configurations (1). An advantage that the diffuse discharge has over the electric arc is that the stream of combustion products is heated uniformly in the absence of the very steep temperature gradients which are present in electric arcs. The diffuse discharge was produced by applying a high voltage across a highly turbulent flame whose electrical conductivity had been increased by "seeding". The technique of seeding involved the addition of traces of readily ionized salts to the gas, upstream of the discharge zone. It is not clear from Karlovitz's paper whether seeding was used in all his systems. The high degree of turbulence and the enhanced electrical conductivity of the combustion products prevented the discharge from contracting into a narrow arc, according to the following argument:-

The time required to exchange a small volume of gas in an arc filament with the surrounding gas is given by the characteristic time of turbulence t' , where

$$t' = \frac{l}{u'}$$

(l is the scale of turbulence and u' is the intensity of turbulence.)

The time t required for the development of an arc column is given by

$$t = \frac{c_p \rho}{e K_e E^2 \frac{dN_e}{d\theta}}$$

(c_p, ρ, θ are specific heat, density and temperature of the gas,
 e is the charge on an electron,
 K_e is the mobility of an electron,
 N_e is the concentration of electrons, and
 E is the field strength.)

According to the argument, if t'/t is less than unity then the discharge will be diffuse.

t' can be made small by reducing l and increasing u' , i.e. by increasing the amount of small scale turbulence. t can be made large by reducing both E and $dN_e/d\theta$ and this is achieved by seeding.

Examples of burner configurations which were suggested by Karlovitz to be suitable for electrical augmentation with diffuse discharges are given in figure II 1.

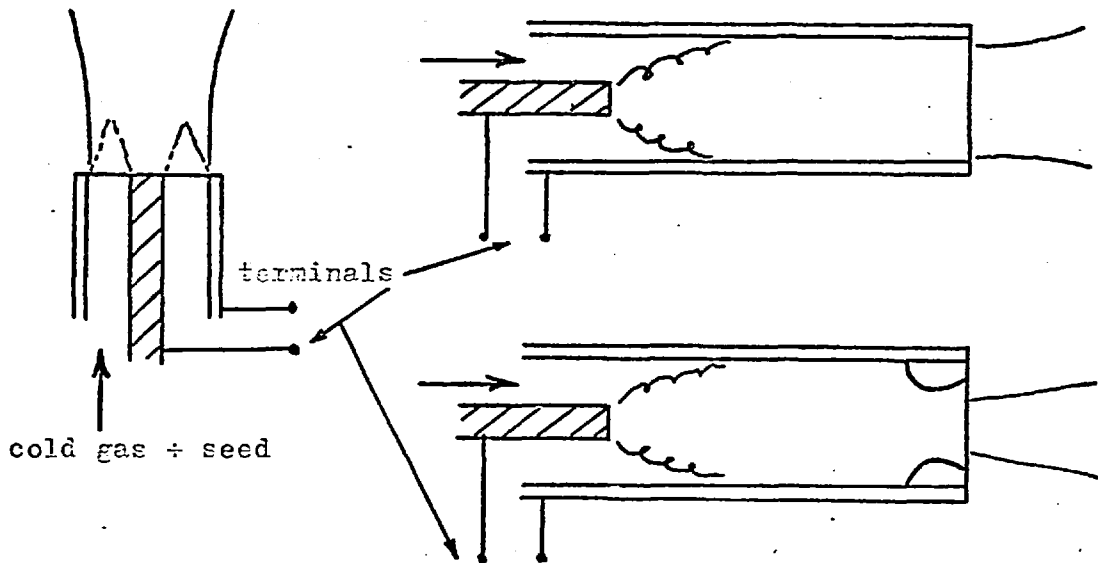


Figure II 1

The electrical power terminals are connected to a central electrode and to the burner tube in all cases and the discharge is initiated in the combustion zone.

Another paper, by Karlovitz and Richardson ⁽²⁾, gives a more detailed drawing of one of the above burners and this is reproduced in figure II 2.

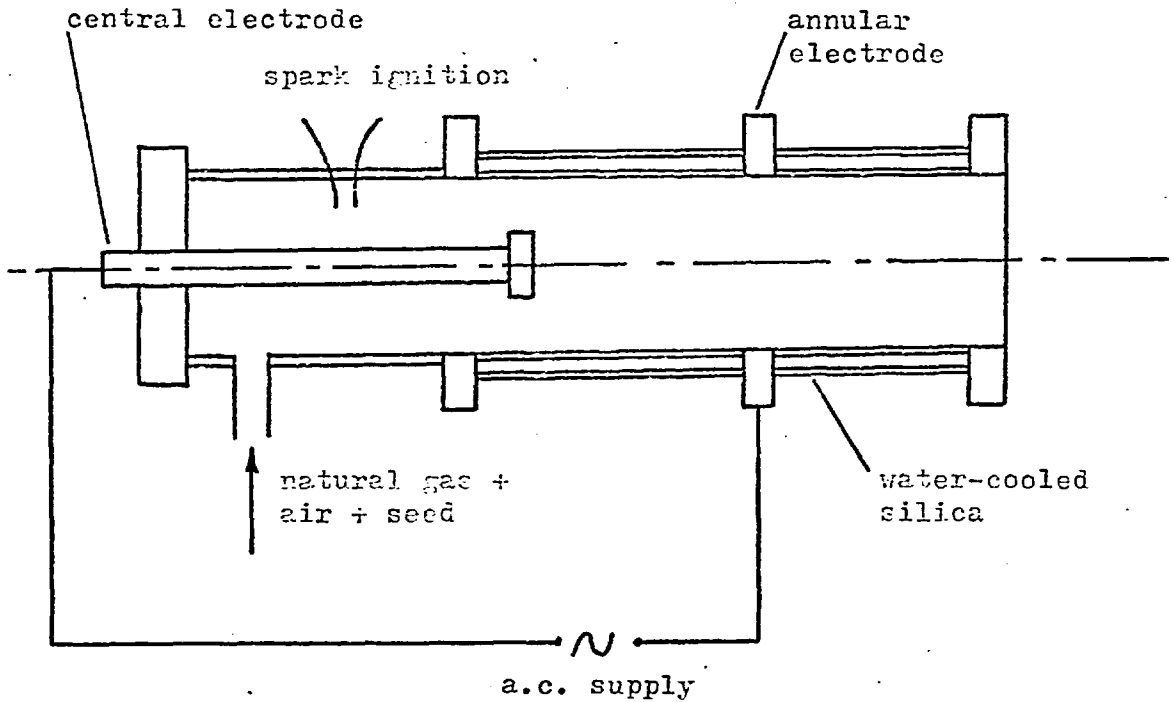


Figure II 2

A turbulent, seeded flame was stabilized on the central electrode and an a.c. discharge was passed between this electrode and a water-cooled electrode of annular shape. This annulus was positioned some way along a transparent tunnel of silica which was also water-cooled.

A later paper, by Marynowski, Karlovitz and Hirt (3), stated that the burners shown in figures II 1 and II 2 did not produce diffuse discharges. The discharges appeared diffuse to the eye but photographic evidence showed them to be filamentary. A modified burner, shown in Figure II 3, was developed in which the discharge zone occurred downstream of the combustion zone.

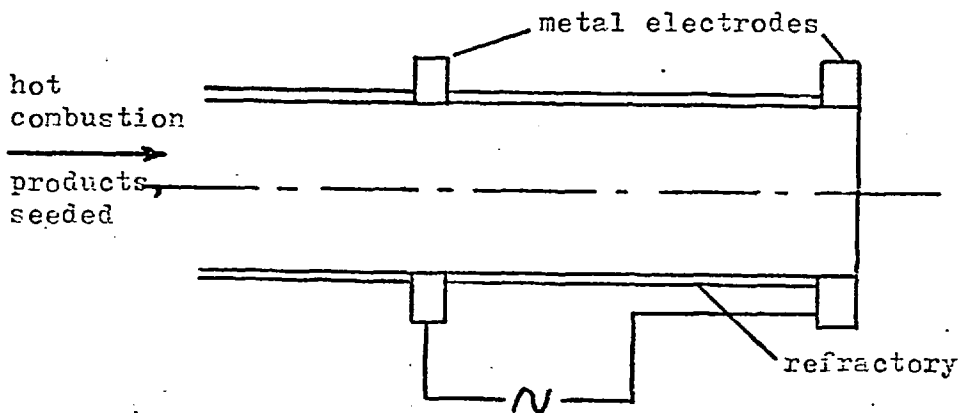


Figure II 3

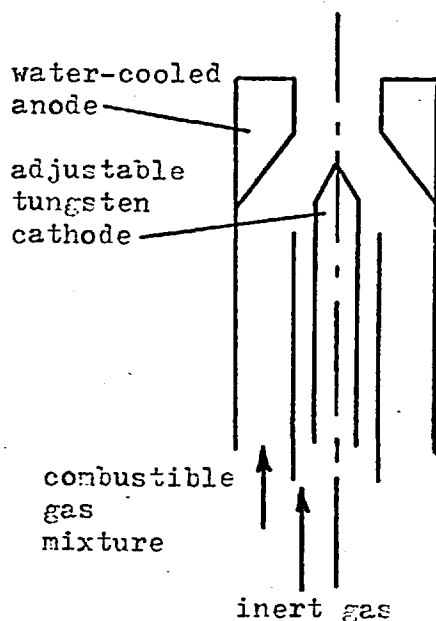
II 1 (a)

High speed photography showed that the discharge was diffuse. These burners have been operated at 100 kW of electrical power and 100 kW of combustion power. The fuel was natural gas and the electrical power was supplied by a single phase transformer at voltages from 2 kV to 8 kV. Inductive ballasting was used to limit the short-circuit current, as will be explained on page 28.

Davies used a Karlovitz-type of burner to electrically augment an air-town's gas flame⁽⁴⁾. He converted a standard tunnel burner by equipping it with two electrodes, one of which was positioned centrally in the inlet to the tunnel and the other was added to the exit of the tunnel in the form of a water-cooled annulus. The discharge was stabilized in the combustion zone. Davies deduced that the exit gas was not heated uniformly by the discharge because heat fluxes determined by a probe were high at the burner axis but decreased rapidly as the probe was moved away from the axis.

(b) Weinberg and co-workers.

Lawton, Payne and Weinberg have described electrically augmented burners which were based on d.c. plasma jet designs⁽⁵⁾ and this type of burner is illustrated in figure II 4.



An arc was produced between the tip of the cathode and the water-cooled anode. Combustion also occurred in this region, the arc helping to stabilize the flame. The cathode was shielded from chemical attack by a low flow rate of inert gas which also prevented ignition of the gas mixture before it reached the arc region. An alternative device is

described in which the gas mixture was introduced through a ring of nozzles which was positioned above the anode. The nozzles were almost horizontal and created a high level of

turbulence just above the arc. Unlike the Harlovitz designs (1,2,3) these plasma jet devices do not depend on the stabilization of a flame upstream of the discharge in order to maintain the discharge.

Chen, Lawton and Weinberg have discussed the methods available for spreading the discharge through the flame in order to heat the gas uniformly.⁽⁶⁾ They emphasize that an arc in a gas stream is usually a fluctuating channel of narrow width, although it probably appears to the eye as a continuous and uniformly luminous flame across the entire width of the gas stream. The arc does not fluctuate with sufficient rapidity to heat the gas stream uniformly. High speed photography was used to show that the plasma jet type of augmented burner does not heat the gas uniformly because a large proportion of the gas flow by-passes the arc. The authors then outlined four methods which have been used in attempts to spread discharges in gases:-

- (1) inducing turbulence in the gas stream,
 - (2) seeding the gas stream with readily ionizable salts,
 - (3) using high frequency discharges,
- and (4) moving the arc with magnetic fields.

The success of method (1) depends on the scale of turbulence that is induced. The vortex-spun arc is an example of the use of large-scale turbulence which has been produced by injecting the gas stream tangentially at a point below the arc. The arc is made to rotate by the vortex and moves at about the same speed as the gas which implies that the gas does not make increased contact with the arc. The authors developed a burner which is an example of the use of small-scale turbulence to increase the contact between gas and arc. The combustible mixture was fed through a porous alumina annulus which was situated between a central cathode and a ring anode as shown in figure II 5. This burner allows the arc channel to be close to the walls because they are cooled regeneratively by the gas flow.

The generation of sufficiently intense small-scale turbulence which would enable an electric arc to heat the surrounding gas uniformly is considered by the authors to be difficult to achieve. Usually, turbulence has to be used in conjunction with another method, such as (2) - seeding.

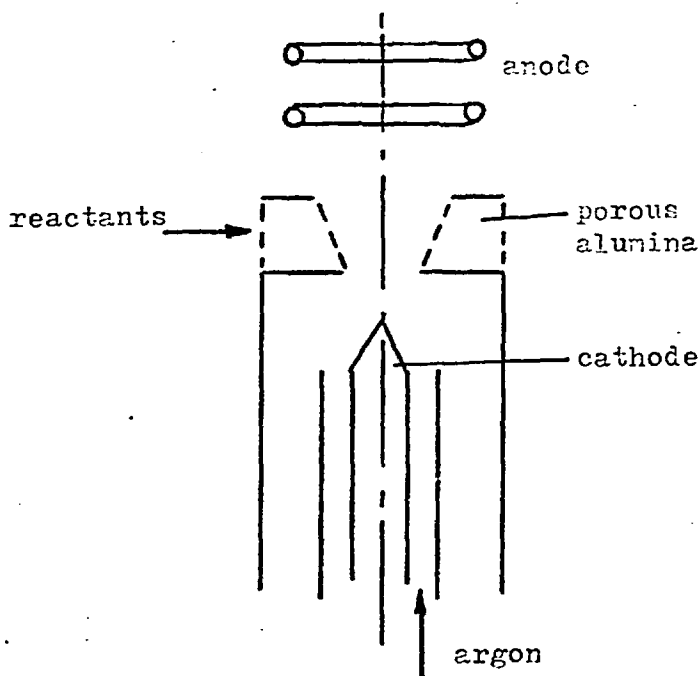


Figure II 5

Electric arcs occupy narrow channels because the initial heating by the arc ionizes the gas and reduces its electrical conductivity. The arc chooses the path of reduced conductivity and heats the gas further, reducing the conductivity still further. Seeding tends to prevent this process from occurring by reducing the conductivity of the gas stream at relatively low temperatures. Seeding has been shown to be successful only well downstream of the luminous region of a flame. This would explain why a diffuse discharge was not obtained with the Zarlovitz type of burner (1,2,4).

Method (3) is not as successful in heating gases uniformly over their cross-sections of flow as would appear to the eye. The ball of plasma formed by a radio frequency discharge does not extend over the entire cross-section of the gas stream but allows a peripheral annulus of cold gas to by-pass the plasma, accounting for a large proportion of the total gas flow⁽⁶⁾.

Method (4) has been widely used, e.g. the Birkeland and Eyde process in which nitrogen and oxygen combine in an arc which has been spread into a disc by a magnetic field. The authors⁽⁶⁾ applied a magnetic field to the mouth of the plasma jet which is illustrated in figure II 4. The arc was made to spin or oscillate according to whether d.c. or a.c. potential was applied to the field coil.

They concluded that the magnetically rotated arc and the porous cavity burner provided high combustion stability whilst

a combination of seeding and small-scale turbulence was suitable for heating combustion products if additional combustion stability was not required.

(c) Fells and co-workers.

Fells, Gaven and Barker developed a seeded propane-air burner which was augmented with d.c. power ⁽⁷⁾. Figure II 6 shows the arrangement of the burner. The flame was stabilized

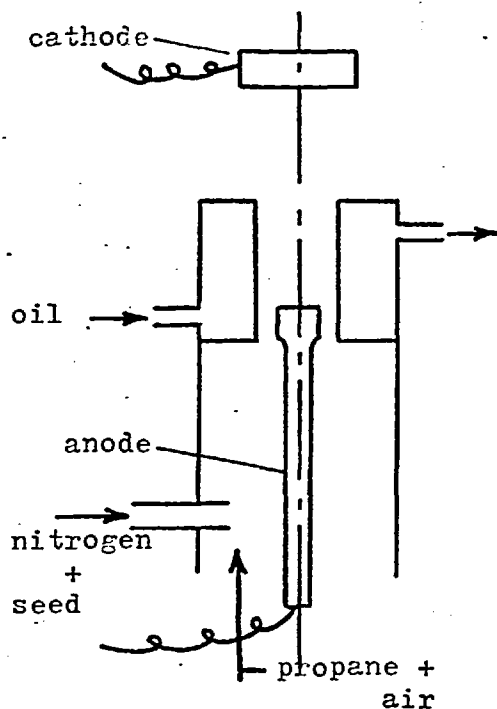


Figure II 6

on a central anode of copper in an oil-cooled tunnel. The cathode was placed in the path of the exit gases from the burner at distances of up to 15 cm from the anode and was made from graphite, water-cooled copper or water-cooled stainless steel. A ballast resistor of $400\ \Omega$ was placed in series with the anode and a d.c. supply which provided 5 A at 2 kV was used. Seed concentration was found to affect the voltage required to initiate a discharge but to have no

significant effect on the voltage required to maintain the arc. If the polarity of the electrodes was reversed, the stability of the discharge was reduced and a number of anode spots were formed. These results were only valid when the graphite cathode was used. The water-cooled cathodes imposed a restriction on the discharge by requiring a current of at least 8 A for stability.

The same authors described a non-seeded d.c. system ⁽⁸⁾. Using the burner shown in figure II 6, they found that seeding was required to initiate the discharge but it could be maintained without seed. When a.c. power was used to augment the flame, seeding was required continuously. The method of seeding is described in another paper ⁽⁹⁾. A stream of nitrogen was passed over a bed of molten potassium carbonate and the stream was quenched before it reached the burner. Fine particles of seed were therefore carried into the burner.

(d) Relevant patents.

The Karlovitz-type of burners are covered by various patents^(10,11,12).

A further patent⁽¹⁴⁾ is of considerable metallurgical interest. This describes a cupola which incorporated an electrically augmented burner. Fuel gas and air are passed through an arc prior to entering the cupola. The electrical energy augments the gas flame and enables a reducing atmosphere to be maintained in the cupola by allowing a fuel-rich mixture to be burnt. The arc is struck between a water-cooled copper ring and a graphite or tungsten rod.

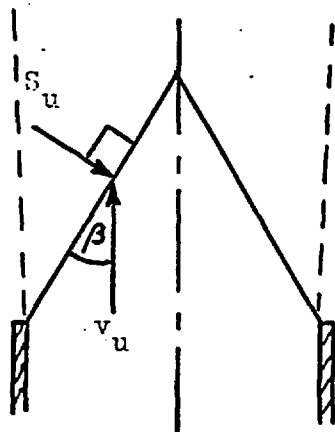
2. FLAMES AND BURNERS.

The design of an electrically augmented burner depends to a large extent on an understanding of flame stability. In this section the meaning of flash-back and blow-off will be explained, these being the limiting conditions of flame stability. Means of increasing the flame stability limits and also of increasing the intensity of combustion will be described. The limits of inflammability of methane are included.

(a) Flame stability.

The simple tube burner will be used to indicate how flash-back and blow-off can occur when pre-mixed reactants are burnt in a stationary flame which is attached to the rim of the burner tube⁽¹⁵⁾.

The mixture of oxidant and fuel burns at a thin reaction zone or flame front which can be approximated to a cone for a



laminar flow of reactants burning on top of a tube as shown in figure II 7. The burning velocity of the mixture is S_u , relative to the unburnt reactants. The velocity of the unburnt reactants is v_u , parallel to the axis of the tube.

S_u and v_u can be related by

$$S_u = v_u \sin \beta \quad \text{--- II 1.}$$

Figure II 7

where β is the angle between the flame front and the axis of the tube. Hence the ratio of v_u/S_u fixes β . If the flame front

is complex in shape then this ratio determines β locally. As v_u is increased, β becomes smaller and approaches zero when stabilization fails and blow-off occurs. Conversely, if v_u is reduced until it approaches S_u , β approaches $\pi/2$ and the flame is on the point of flash-back. The situation is more complex than this because the burner walls exert an influence on the processes of flash-back and blow-off as described below.

(i) Flash-back or blow-back.

For the simple tube burner shown in figure II 8, v_u approaches zero as r

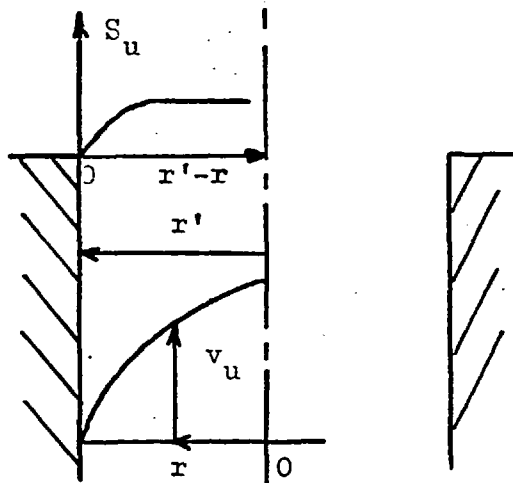


Figure II 8

approaches r' because of viscous drag. If S_u is constant then equation II 1 indicates that the flame will always flash back. S_u is not constant but approaches zero at the walls because heat is lost to the walls. The criterion for flash back is whether S_u or v_u approaches zero more rapidly as r approaches r' , i.e. flash-back occurs when

$$\left(\frac{dv_u}{dr}\right)_{r'} \leq \left(\frac{dS_u}{dr}\right)_{r'}$$

— II 2

$\left(\frac{dS_u}{dr}\right)_{r'}$ is a property of the reactant mixture and is independent of the nature of the tube walls because the rate of heat transfer to the walls is controlled by the gaseous boundary layer.

(ii) Blow-off or lift-off.

The upper limit to stability is also controlled by a critical velocity gradient. Figure II 9 shows how the flame front is positioned relative to the wall of a simple tube burner. Loss of heat to the wall of the tube leaves a gap between the flame front and the wall. The flame is stabilized above the tube and cold reactants can escape through the gap at A.

As v_u is increased the gap at A also increases. Each value of v_u is balanced by an S_u of the same magnitude until S_u cannot

II 2 (a)

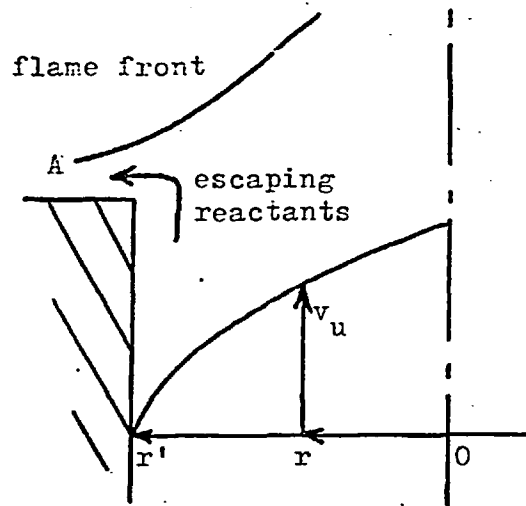


Figure II 9

increase further. Blow-off then starts and the condition for blow-off is given by

$$\left(\frac{dv_u}{dr} \right)_{r'} \geq \left(\frac{dS_u}{dr} \right)_{r'} \quad \text{--- II 3}$$

This condition is dependent on the reactant mixture and on the surrounding atmosphere, e.g. blow-off occurs more readily in nitrogen than in air.

Figure II 10 shows the flash-back and blow-off limits for natural gas-air mixtures in simple tube burners and is due to Lewis and von Elbe (15)

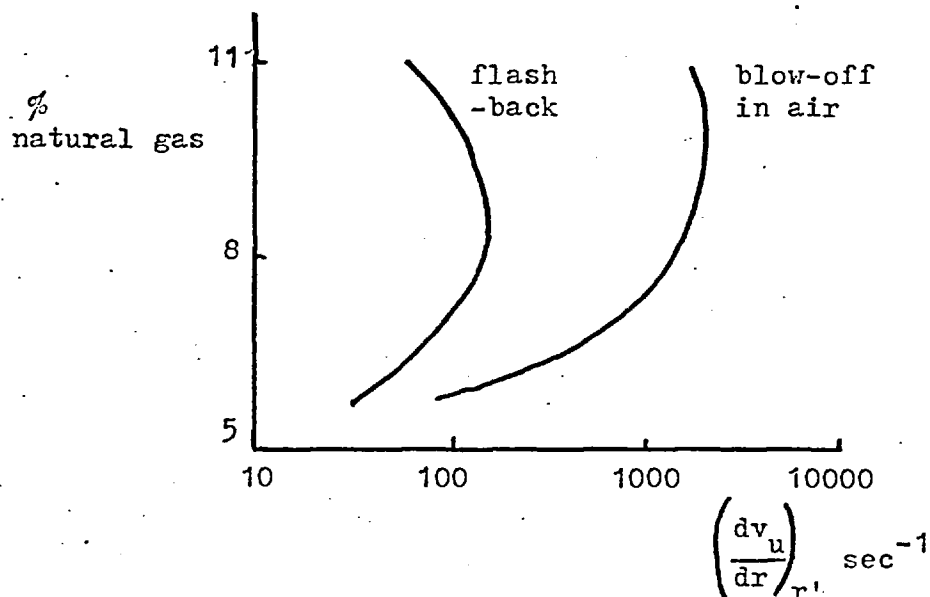


Figure II 10

Lewis and von Elbe have also investigated the burning velocity of natural gas-air flames and their results are shown in figure II 11 (15).

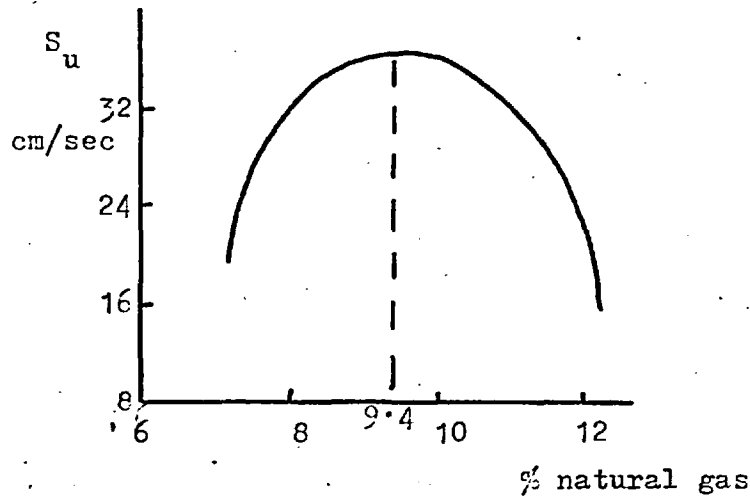


Figure II 11

(iii) Limits of inflammability.

Egerton and Thabet have investigated the limits of inflammability of methane in air (16). These limits correspond to a certain minimum flame temperature which is about 1100°C for methane. The results are shown in figure II 12.

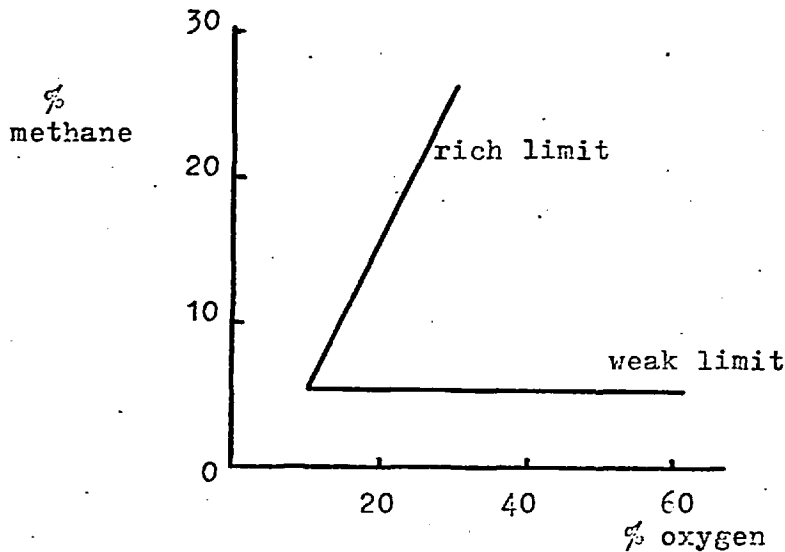


Figure II 12

(b) High intensity combustion.

A discussion of ways of increasing combustion intensity is to be found in standard works. (17,18)

In laminar flow burners, the mixing required for flame propagation occurs by molecular transport processes which produce a reaction zone or flame front of small surface area. In the turbulent flow regime, mixing is controlled by turbulence and the flame front has a wrinkled appearance and a much larger surface area which gives rise to the higher combustion intensities produced by turbulent flames. The combustion intensity can be increased further if blow-off can be prevented. The usual method is to generate large-scale turbulence and recirculate some of the hot products of combustion which heat the cold reactants rapidly. Throughput can then be increased without blow-off occurring. Examples of burners in which large-scale turbulence is generated are:-

(i) Sudden expansion type.

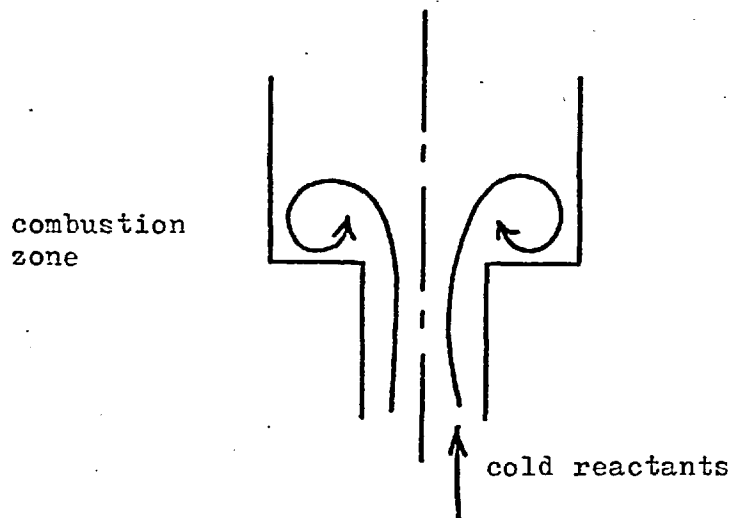


Figure II 13

A toroidal vortex is formed just upstream of the sudden expansion. These large scale eddies recirculate hot combustion products and stabilize the flame in the large diameter section of the burner, as shown in figure II 13.

(ii) Vortex-stabilized type.

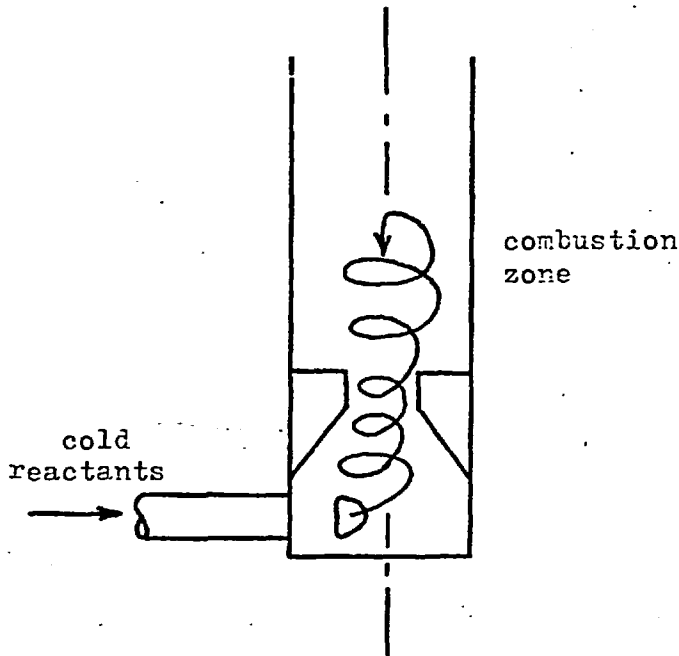


Figure II 14

This type of burner is shown in figure II 14 and is usually called a tunnel burner. The cold reactants are introduced at a tangential inlet which produces a vortex. Hot products are recirculated down the centre of the vortex and combustion is stabilized in the tunnel.

(iii) Electrical stabilization.

A further means of stabilizing flames is to use electrically augmented burners of the type described in section II 1 (b). The electrical discharge should be applied near the combustion zone. If the discharge occurs in the products of combustion downstream from the combustion zone, then combustion will not be stabilized unless the discharge-heated products of combustion recirculate into the combustion zone⁽⁵⁾.

3. ELECTRICAL DISCHARGES IN GASES.

A brief review of the aspects of electrical discharges in gases which are relevant to electrically augmented flames is given in this section. The information has been obtained from standard texts and reviews. (19,20,21).

Figure II 15⁽²⁰⁾ shows the voltage-current characteristics of the various types of self-sustaining discharges that occur when a potential is applied between plane electrodes situated in an envelope containing gas at 1 mm Hg pressure.

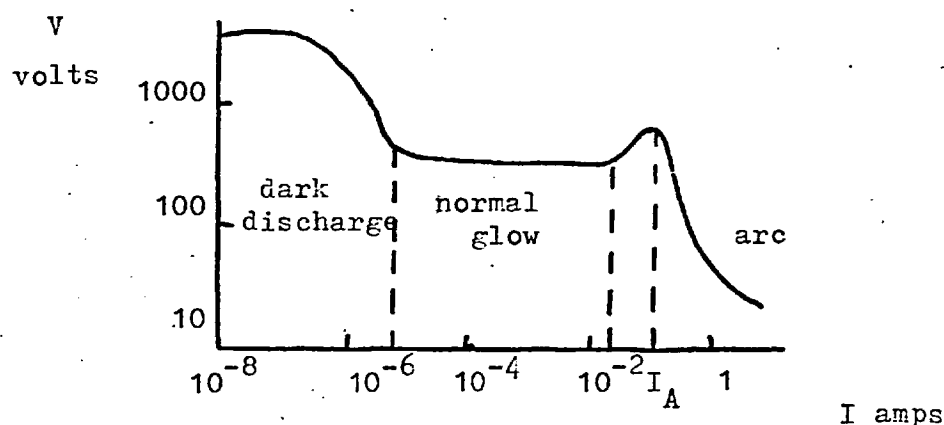


Figure II 15

The dark discharge is inapplicable to electrically augmented flames because it passes a very low current and is only observed at reduced pressure, whereas glows and arcs can occur at atmospheric pressures and above when they pass high currents.

(a) Glow discharges.

As shown by Figure II 15, the glow discharge is characterised by a constant voltage between the electrodes. The cathode emits electrons by the action of ionic bombardment from the gas and this process is known as secondary emission. No thermal effects are present at the cathode. There is a large fall in voltage at the cathode which approximates to the potential drop over the entire discharge. Although most glows are associated with reduced pressure, Fan⁽²²⁾ was able to produce glow discharges in air, oxygen and nitrogen at pressures of 1 atmosphere and in hydrogen at up to 13 atmospheres. The electrodes were of water-cooled copper and were separated by a few millimetres. The most stable glows were achieved in hydrogen with currents reaching 14 amps. Fan investigated the transition from a glow

to an arc discharge and found that the change occurred when either the current was increased at constant pressure or the pressure was increased at constant current.

(b) Arc discharge.

An arc is produced when the current exceeds a certain value, shown as I_A in figure II 15 , and is characterised by a rapid drop in potential as the current increases. The cathode fall is much lower than for a glow because electrons are emitted from the cathode by more efficient processes than ionic bombardment. Examples of these processes are thermionic emission, thermal vapourization of the electrode and the use of high electric fields. The arc will be considered here in detail because it is an important means of augmenting a flame.

(i) Striking an arc.

Arcs can be struck by connecting the electrodes with fine wire, by initially bringing the electrodes into contact and by exceeding the breakdown voltage of the gas between the electrodes. The breakdown voltage V_B can be expressed in terms of pressure p and arc gap d

$$V_B = f(p.d) \quad \text{--- II 4}$$

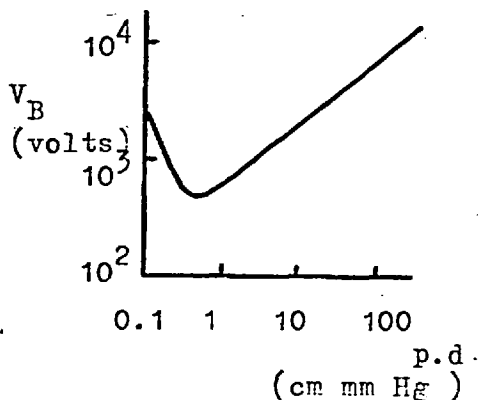


Figure II 16

Equation II 4 is Paschen's Law and a plot of V_B against $p.d$ for air is shown in figure II 16. (20). Similar results have been obtained for other gases and all the curves have been found to pass through a minimum.

(ii) Structure of an arc.

Figure II 17 shows an arc, consisting of a positive column and two regions close to the electrodes. The positive column is a luminous region of ionized gas which has zero net space charge, i.e. a plasma. At the electrodes the arc contracts to give regions of higher current density j . The cathode and

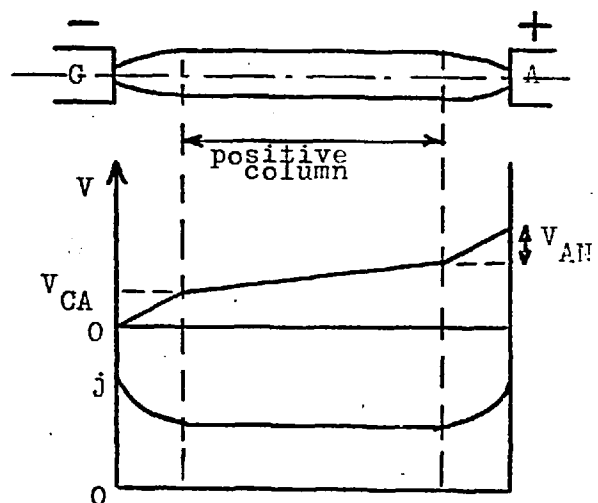


Figure II 17

anode falls are given by V_{CA} and V_{AE} respectively. The cathode fall is much smaller than that of a glow discharge and thermal effects are usually present at the cathode.

(iii) Arc characteristics.

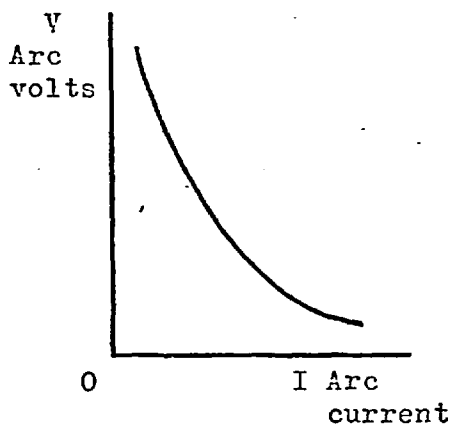


Figure II 18

The characteristic of a 60 cycle, 40 mm arc in hydrogen is shown in figure II 19 (23). The voltage rises from zero to a peak where the arc is initiated. The voltage then drops until the static characteristic is reached. In this circuit the current is limited to 6.4 amps r.m.s. and the current then drops back to zero. The same process occurs

The static characteristic of a d.c. arc is shown in figure II 18. The precise form of the relationship between V and I will depend upon the electrode materials, the length of the arc, the gaseous medium and the pressure of the gas. At high currents, the voltage may start to rise again (not shown in figure II 18).

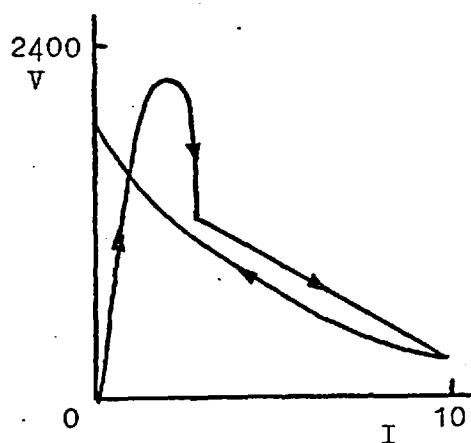


Figure II 19

in the next half-cycle with V and I both negative. Hysteresis effects do not become apparent until much higher frequencies are used.

(iv) Arc stability.

A consequence of the falling $V - I$ static characteristic shown in figure II 18 is that a steady arc in a circuit of constant e.m.f. must have a stabilizing resistance in series with the arc. An example of the use of a stabilizing or ballast resistance is given by Browne (23). Figure II 20 shows the $V - I$ characteristic of a 40 mm d.c. arc struck between copper electrodes.

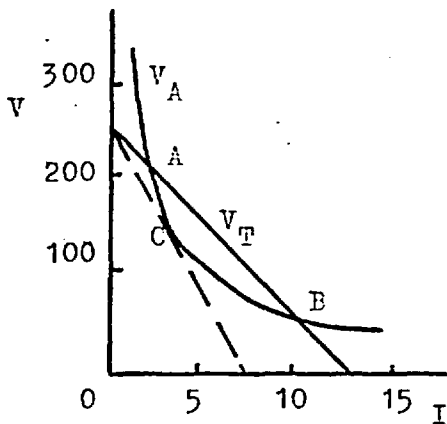


Figure II 20

The voltage available at the arc terminals V_T in a 250 volt circuit with a resistance of 20 ohms is given by the load line,

$$V_T = 250 - 20 I,$$

and is plotted as the straight line on the diagram. A steady arc will exist where the characteristic crosses this line, i.e. at A or B, but a stable arc will only exist

at B. Lengthening the arc will cause A and B to merge. If the ballast resistance is increased, A and B will merge until point C is reached when the arc will go out. The load line in this example for the condition of extinction becomes

$$V_T = 250 - 36 I$$

A condition of thermal stability is also required by an arc and this is usually satisfied by one of three ways:-

(1) Wall stabilization.

When the boundary of the positive column of an arc is maintained at a constant temperature, the arc is said to be wall-stabilized. Energy is lost from the arc by thermal conduction through the boundary which can be in the form of either a tube of solid material or a vortex of gas or liquid.

(2) Convection stabilization.

The arc may be stabilized by convection of heat from the positive column to the surrounding gas and is then said to be a "free-burning" arc. At the edges of the temperature distribution in the arc column, conduction losses become negligible and convection losses predominate. The temperature distribution across the positive column is much flatter for a free-burning arc than for a wall-stabilized arc. Convection tends to produce a vertical arc which is broader at the top than the bottom and a horizontal arc which has an upward bow.

(3) Electrode stabilized.

With small arc length and large electrodes the arc may be stabilized by energy loss to the electrodes.

4. ELECTRODES.

The materials which are used for cathodes in glow and arc discharges have a large effect on the nature and characteristics of the discharges. Cathodes can be divided into two types - those which vaporize readily and those which are refractory. The former type is the so-called cold cathode made from metals such as copper, silver and mercury which are unsuitable for use in flames but the refractory cathode has a sufficiently high melting point to be used in flames and can be operated at temperatures which allow thermionic emission to occur.

(a) Thermionic emission.

Electrons can be emitted from the surface of an electrode by heating the surface. This process is known as thermionic emission and only occurs significantly at temperatures above the melting points of most metals. Positive ions are emitted to a much lesser degree and will be neglected in the following account.

The emission of electrons depends upon the surface temperature and the material of the surface, assuming that there is no electric field present. A minimum energy is required to remove an electron from the surface and this is given by $e\phi$ where e is the electronic charge and ϕ is the work function of the material in volts. The current density j flowing from the surface due to emission of electrons is given by Dushman's equation -

$$j = BT^2 \exp \left[- \frac{e\phi}{kT} \right] \text{ amp cm}^{-2} \quad \text{--- II 5}$$

where k is the Boltzmann constant and T is the absolute temperature.

Both B and ϕ are characteristics of the material and typical values are given in table II 1.

<u>Material</u>	B (A cm ⁻²)	ϕ (V)	j at T (A cm ⁻²) (°K)	Ref.
Carbon	48	4.4	0.15 2400	24
Tungsten	70	4.5	0.27 2500	24
Thoria	3	3.1	2 1900	25
Baria/ Strontia	0.1	1.0	1 1100	25
Ceria	5	2.3	2.6 1900	25
Zirconia	-	3.9	0.07 2000	25

Table II 1

In the presence of an applied field at the surface of the cathode, the work function of the material will be decreased if the direction of the field helps the removal of electrons from the surface. This is known as the Schottky effect and Dushman's equation becomes

$$j = BT^2 \exp \left[- \frac{e}{kT} (\phi - \sqrt{eE}) \right] \text{ amp cm}^{-2} \quad \text{--- II 6}$$

where the new work function is given by $(\phi - \sqrt{eE})$, E being the field strength.

If the applied field opposes thermionic emission then Dushman's equation is

$$j = BT^2 \exp \left[- \frac{e}{kT} (\phi + Ex) \right] \text{ amp cm}^{-2} \quad \text{--- II 7}$$

where x is the distance over which the field is applied.

(b) Electrode materials.

The desirable properties of a material which may be used as electrodes in electrically augmented burners can be summarized as:-

- (1) high melting point, greater than 2500°C, say,
- (2) chemical stability up to the melting point,
- (3) low electrical resistivity,
- and (4) low work function.

Various elements, compounds and alloys whose properties make them possible electrode materials are reviewed below.

(i) Metals.

The metals with melting points above 2500°C are molybdenum, osmium, rhenium, tantalum and tungsten. Of these, tungsten has the highest melting point at 3380°C . All these metals are readily oxidised at the temperatures experienced in flames which restrict their use to non-oxidising atmospheres.

(ii) Oxides.

Some metallic oxides have properties which enable them to be used as electrodes. They have low resistivity at high temperatures, high melting points, and low work functions (see table II 1) and are chemically stable. Ceria, thoria and zirconia are examples of these oxides and table II 2 below gives some of their relevant properties (26)

<u>Oxide.</u>	<u>M. Pt.</u> ($^{\circ}\text{C}$)	<u>Elec.resist.</u> (ohm.cm)	<u>Stability.</u>
Ceria (CeO_2)	2750	240 at 1200°C (compressed in air)	Reduced by hydrogen at 500°C or more. Decomposes at high temperatures.
Thoria (ThO_2)	3300	500 at 2000°C (sintered) 1 at 1630°C (compacted)	Reacts with carbon at high temperatures.
Zirconia - pure (ZrO_2)	2715	10^8 at 1000°C	Volume instability at 1000°C . No strength when fired.
Zirconia stabilized with MgO	2500	10^4 at 700°C	Can be destabilized under certain thermal conditions
Zirconia stabilized with CaO	2500	0.6 at 2000°C (5 mole % CaO)	Zirconia reacts with carbon at high temperatures.

Table II 2

Table II 2 shows that ceria is unstable at high temperatures in both reducing and oxidising conditions whilst zirconia and thoria have better chemical stability. Pure zirconia undergoes

a contraction amounting to about 9% by volume when it transforms from the monoclinic to the tetragonal modification at about 1000°C. As a result, sintered components of pure zirconia have no strength. Both the monoclinic and tetragonal modifications differ only slightly from a cubic form and the addition of small amounts of oxide with a cubic structure has been found to stabilize the zirconia lattice in the cubic form. Magnesia, lime and yttria are examples of additives which form solid solutions with zirconia, forcing it to assume a stable cubic lattice after firing. Magnesia-zirconia can be destabilized by holding it at 900-1400°C for many hours but lime stabilizes zirconia more permanently.

The electrical conductivity of most of these oxides increases rapidly as their temperature is raised. The actual values depend on the treatment that the oxide has received. Pure zirconia is a good insulator but alloys of zirconia with magnesia, lime, hafnia, yttria and other oxides have much lower resistivity values which vary with the proportion and type of oxide added to the zirconia.

(iii) Carbon and carbides.

Carbon in the form of graphite has the highest melting point of all materials, has a low resistivity and is a thermionic emitter. It is extensively used for making electrodes for electric arcs but is oxidized at flame temperatures. Carbides also appear to be oxidised at flame temperatures. Silicon carbide is an exception because it is protected by a surface layer of silica which forms under oxidising conditions. The presence of silica would be expected to rule out the use of silicon carbide as an electrode material in electrically augmented burners.

5. HEAT TRANSFER FROM FLAMES AND ARCS TO SOLID SURFACES.

Flame hardening requires a high rate of heat transfer between the flame and the surface to be hardened. The high rates of heat transfer which can be obtained from arcs and certain types of flame are explained below, together with methods of measuring and calculating heat fluxes from flames and arcs.

(a) Effect of chemical reaction on rate of heat transfer.

When heat is transferred from flames and arc-heated gases to surfaces at much lower temperatures, the rates of heat transfer may be significantly altered by chemical reactions occurring at or near the surfaces. Arc-heated gas is usually highly ionized and most flames contain a large proportion of atoms and free radicals, all of which are chemically reactive. These active species may recombine in the boundary layer next to a cool surface and on the cool surface itself by the process of three-body recombination⁽²⁰⁾. An example of this is the recombination of hydrogen atoms which is accompanied by a large release of energy. Cookson and Killam⁽²⁷⁾ have measured the rate of heat transfer from lean hydrogen-air flames and have shown the importance of hydrogen atoms in the heat transfer process.

(b) Methods of calculating the rates of heat transfer.

Heat is transferred from an undissociated gas to a solid surface by molecular conduction of energy through a boundary layer near the surface. The heat flux $\dot{q}''$ to the surface is given by

$$\dot{q}'' = -k \frac{\partial \theta}{\partial y} = -c_p \rho \alpha \frac{\partial \theta}{\partial y} \quad \text{--- II 8}$$

where θ is the temperature, y is the direction normal to the surface and k , ρ , c_p and α are the thermal conductivity, density, specific heat and thermal diffusivity of the gas.

The total rate of heat transfer $\dot{q}_T''$ from a dissociated gas to a solid surface is given by

$$\dot{q}_T'' = \dot{q}'' + \dot{q}''_E \quad \text{--- II 9}$$

where $\dot{q}''_E$ is the enhancement of $\dot{q}''$ by the recombination of dissociated species at the solid surface.

Therefore

$$\dot{q}''_T = -\rho c_p \alpha \frac{\partial \theta}{\partial y} - \rho D \Delta H \frac{\partial m^*}{\partial y}$$

where D is the diffusivity, ΔH is the heat of recombination and m^* is the mass fraction of the dissociated species.

Hence

$$\dot{q}''_T = -\rho c_p \alpha \left[1 + \frac{D}{\alpha} \cdot \frac{\Delta H}{c_p} \cdot \frac{\partial m^*}{\partial y} \right] \frac{\partial \theta}{\partial y}$$

If D/α , the Lewis number, is equal to unity and m^* and θ only vary with y , then

$$\begin{aligned} \dot{q}''_T &= -\rho c_p \alpha \left[\frac{c_p \frac{d\theta}{dy} + \Delta H \frac{dm^*}{dy}}{c_p \frac{d\theta}{dy}} \right] \frac{d\theta}{dy} \\ &= -\frac{k}{c_p} \cdot \frac{dH_T}{d\theta} \cdot \frac{d\theta}{dy} \quad \text{--- II 10} \end{aligned}$$

where H_T is the total enthalpy of the system.

By analogy with equation II 8, the term

$$\frac{k}{c_p} \cdot \frac{dH_T}{d\theta}$$

is known as the effective thermal conductivity and was first proposed by Butler and Brokaw⁽²⁸⁾. For combustion processes, the Lewis number is unlikely to be unity. Equation II 10 has been modified by Rosner⁽²⁹⁾ to incorporate values of the Lewis number, Le , which are different from unity by multiplying $\dot{q}''_T$ by the factor

$$\left[1 - \left(1 - \frac{1}{Le} \right) \frac{\Delta \psi_E}{\Delta \psi_T} \right]^{\frac{1}{3}} \left[1 + (Le-1) \cdot \frac{\Delta H}{\Delta H_T} \right] \quad \text{--- II 11}$$

Where ψ , the heat flux potential is given by

$$\psi = \int_0^\theta k d\theta$$

Two further methods have been proposed for calculating heat fluxes from dissociated gases. The first of these is due to Altmann and Wise⁽³⁰⁾ who assumed, for turbulent flow, that recombination occurred in the turbulent region of the boundary layer and that the diffusivities were small compared to the eddy kinematic viscosity ϵ .

Hence

$$\frac{D + \epsilon}{\alpha + \epsilon} \approx 1$$

The following expression was then used to calculate the total heat flux to the surface

$$\dot{q}''_T = \dot{q}'' + \sum \frac{\Delta H_i (C_{i,b}^* - C_{i,s}^*)}{(1 - C_{i,l}^*) c_p (\theta_b - \theta_s)} \quad \text{--- II 12}$$

where i is the i th component, C^* is the mole fraction and subscripts b , s and l refer to bulk gas, surface and logarithmic mean, respectively.

The final method is due to Rosner (31) who derived theoretical equations for the heat transfer at a stagnation point in a multi-component system for both $Le = 1$ and $Le \neq 1$.

Chen and McGrath (32) have calculated the rate of heat transfer from a stoichiometric oxy-propane flame at $3000^\circ K$ to a sphere of diameter d at $1000^\circ K$ using the methods given above. The rate of heat transfer was calculated for the undissociated gas, using the correlation

$$\frac{hd}{k} = 0.42 \left[\frac{dG}{\mu} \right]^{0.6} \left[\frac{\mu c_p}{k} \right] \quad \text{--- II 13}$$

where h is the heat transfer coefficient, G is the mass velocity and μ is the viscosity. Table II 3 gives the results.

Method of calculation and equation numbers.	Heat flux $W \text{ cm}^{-2}$	
	$Le = 1$	$Le \neq 1$
For undissociated gas, II 13	108	-
Butler and Brokaw, II 10, 11	266	310
Altmann and Wise II 12	223	-
Rosner. (Eqs. not given)	340	414

Table II 3.

From equation II 9 and table II 3,

$$\dot{q}''_E = \dot{q}''_T - \dot{q}'' \geq 115 \text{ W cm}^{-2}$$

and

$$\frac{\dot{q}''_E}{\dot{q}''_T} \geq 0.51$$

showing that surface recombination can have a large effect on rates of heat transfer between flames and surfaces.

(c) Heat flux measurements.

The distribution of heat flux in flames and arcs has been determined using line and surface probes and the results have been analysed by the method of Nestor and Olsen (33).

(i) Radial distribution of heat flux.

Nestor and Olsen's method allows the radial distribution of heat flux to be determined from measurements recorded by one of three types of calorimeter:-

- A. A point probe which measures directly the heat flux at any point in the flame.
- B. A line probe which measures the rate of heat transfer to a chord in the cross-section of the flame.
- C. A surface probe which measures the rate of heat transfer over a segment of the flame's cross-section.

Figure II 21 shows the functions $f_1(x)$, $f_2(x)$ and $f_3(x)$ which are measured by the three types of probe. In this example the flame has a cylindrical distribution of heat flux over its cross-section, such that the radial distribution of heat flux $f(r)$ is constant for $0 < r < r'$ and zero for $r > r'$.

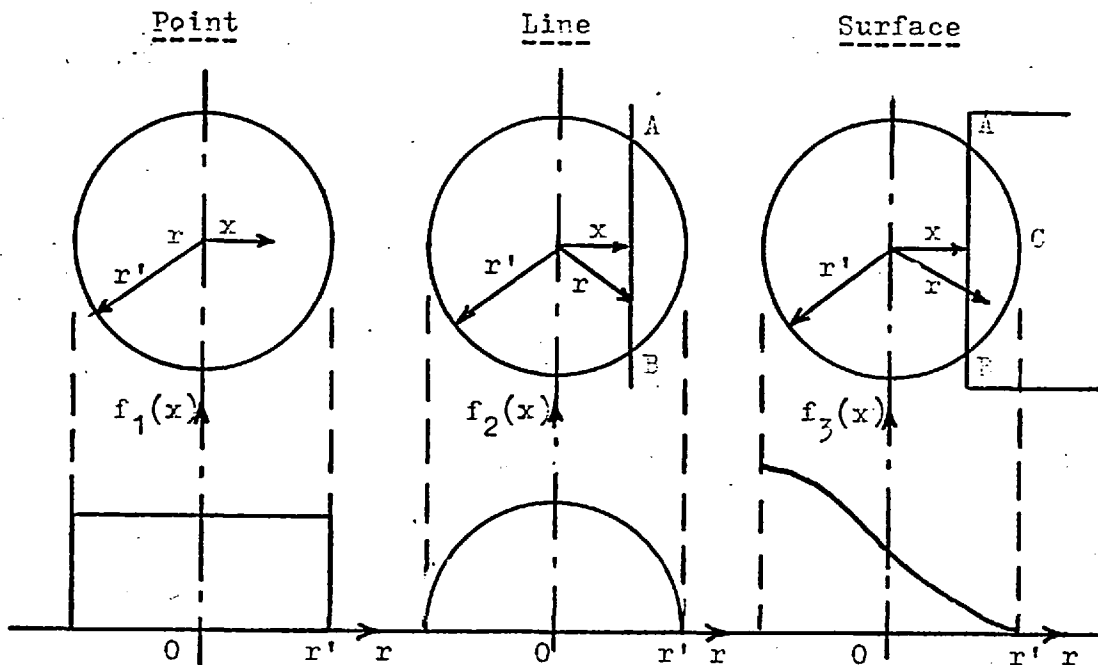


Figure II 21.

The point probe gives a direct measure of the heat flux such that

$$f_1(x) = f(r) = \dot{q}''$$

The line probe measures a function $f_2(x)$ which is the integral of the radial function $f(r)$ over the line AB:-

$$f_2(x) = 2 \int_x^{r'} \frac{f(r) r dr}{\sqrt{(r^2 - x^2)}} \quad \text{--- II 13}$$

The surface probe measures a function $f_3(x)$ which is the integral of $f(r)$ over the segment AEC:-

$$f_3(x) = 2 \int_x^{r'} f(r) \cos^{-1} \left[\frac{x}{r} \right] r dr \quad \text{--- II 14}$$

Equation II 13 is the Abel integral equation which has the solution

$$f(r) = - \frac{1}{\pi} \int_x^{r'} \frac{f'_2(x) dx}{\sqrt{(r^2 - x^2)}} \quad \text{--- II 15}$$

Equation II 15 can be integrated numerically using the transformation $r^2 = v$ and $x^2 = u$ which gives

$$f[r(v)] = - \frac{1}{\pi} \int_v^{r'^2} \frac{f'_2(u) du}{\sqrt{v - u}}$$

The x-axis is divided into zones of equal width s , the i th zone lying between x_i and x_{i+1} , where $x_i = is$ and the radius is divided similarly so that $r_j = js$. By the numerical method

$$[f(r)]_j = - \frac{2}{\pi s} \sum_{i=j}^n B_{j,i} [f_2(x)]_i \quad \text{--- II 16}$$

where $B_{j,i}$ can be obtained from a matrix published by Nestor and Olsen ⁽³³⁾ for $0 \leq i \leq 29$ and $0 \leq j \leq 29$.

The solution to equation II 14 can be obtained from equation II 16 because $f_3(x)$ is the integral of $f_2(x)$, except for algebraic sign. Using the same division of the x axis and neglecting terms of the second order

$$\begin{aligned} [f'_3(x)]_i &= \frac{1}{2s} \left\{ [f_3(x)]_{i+1} - [f_3(x)]_{i-1} \right\} \\ &= [f_2(x)]_i \end{aligned}$$

Substituting for $[f_2(x)]_i$ in equation II 16 gives

$$[f(r)]_j = \frac{1}{\pi s^2} \sum_{i=j}^n B_{j,i} \left\{ [f_3(x)]_{i+1} - [f_3(x)]_{i-1} \right\}$$

which can be simplified to

$$[f(r)]_j = \frac{1}{\pi s^2} \sum_{i=j}^n C_{j,i} [f_3(x)]_i \quad \text{--- II 17}$$

Values of $C_{j,i}$ are given in Nestor and Olsen's paper for $-1 \leq i \leq 29$ and $0 \leq j \leq 29$.

(ii) Results from line and surface probe.

Equations II 16 and II 17 have been used in the determination of heat fluxes by tube and split plate calorimeters, respectively.

Anderson and Stresino⁽³⁴⁾ and Davies⁽⁴⁾ have determined the distribution of heat flux from flames and electrically augmented flames using tube calorimeters. This type of calorimeter consisted of a fine water-cooled tube with a thermocouple or mercury-in-glass thermometer at either end. The flow rate and increase in temperature of the cooling water were measured as the tube was traversed through the flame in a plane normal to the axis of the burner. By this means, values of $[f_2(x)]_i$ were obtained which were substituted in equation II 16 to give $[f(r)]_j$. In addition, the heat flux to the tube was calculated using the method of Butler and Brokaw outlined in section II 5 (b) above. The results are given in table II 4 in the form of maximum values.

Anderson and Stresino⁽³⁴⁾, Nestor⁽³⁵⁾, Reed⁽³⁶⁾ and Fay⁽³⁷⁾ have used surface probes to determine the distributions of heat flux from arcs⁽³⁵⁾, induction plasmas⁽³⁶⁾ and flames^(34,36,37). A calorimeter of the split-plate type was used in

all cases. It consisted of two water-cooled plates separated by a thin layer of insulation as shown in figure II 22.

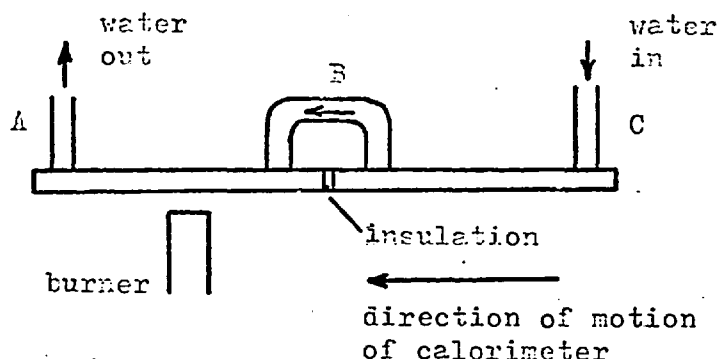


Figure II 22.

The calorimeter was drawn across the burner and the temperature of the cooling water was measured at A, B and C using either thermocouples or thermistors. The values of $[f_3(x)]_i$ were calculated from the temperature differences between either A and B or B and C and the flow rate of the cooling water. These values were substituted in equation II 17 to give $[f(r)]_j$. The maximum heat fluxes are given in table II 4.

Table II 4 shows experimentally determined values of heat fluxes ranging from 7000 W cm^{-2} for an electric arc to 82 W cm^{-2} for an air-methane flame. A strict comparison of values obtained by the different workers is impossible due to variations in distances between the heat sources and the probes and the varying geometry of the heat sources. The distribution of heat flux is especially dependent on the geometry of the heat source.

The results of Anderson and Stresino show that the tube calorimeter gives higher values than the split-plate calorimeter. Their calculated values will depend on the average values of the transport properties which they used, as pointed out by Chen and McGrath⁽³²⁾.

Davies states that the heat flux distributions which were obtained for lean flames were similar to those obtained for stoichiometric flames and only gives results for stoichiometric flames. The electrical power added to the air-town's gas flame was equivalent to 60% of the rate at

which heat was released by combustion. Although the maximum heat flux was higher than that obtained from the oxy-town's gas burner, the distribution of heat flux showed a steep heat flux gradient near the centre-line of the burner. The distribution obtained with the oxy-town's gas burner was much flatter in comparison.

Reference	Heat Source	Distance (cm)	Max. heat flux ($W\ cm^{-2}$)		
			Tube	Split Plate	Calculated.
Anderson and Stresino (34)	Stoich. H_2-O_2 jet	Close. Position of max. flux	4.1×10^3	2.9×10^3	1.6×10^3
	Air- CH_4 flame		1.1×10^2	82	94
	$O_2-C_2H_2$ flame		1.2×10^3	2.9×10^2	-
Davies (4)	Air-town's gas	0.6	1.9×10^2	-	-
	O_2 - " "		5.3×10^2	-	-
	Air- " "				(Mean)
	+60% elec.		7.2×10^2	-	2.5×10^2
Nestor (35)	D.C. arc, 200A 6.3 mm long in argon	probe at anode	-	7×10^3	-
Reed (36)	Induction plasma	2.5	-	70 - 90	-
	in argon	5.0	-	60	-
	H_2-O_2 flame	5.0	-	110-150	-
Fay (37)	Lean $C_2H_2-O_2$	position of max. flux	-	} $3.5-4.0 \times 10^2$	-
	" $C_2H_6-O_2$		-		

Table II 4

Fay has determined heat flux distributions from oxy-gas burners at the oxygen-to-gas ratios encountered in welding and flame hardening processes, i.e. under fuel-rich conditions.

Further results of interest were obtained by Marynowski, Karlovitz and Hirt⁽³⁾ and by Fells and Harker⁽³⁸⁾. Marynowski and co-workers determined the heat flux to a cold wall stagnation

-point probe from an electrically augmented flame. The flame was a stoichiometric natural gas-air flame to which had been added 100% electrical power. The maximum heat flux was found to be about 300 W cm^{-2} . Fells and Parker measured the heat flux from an electrically augmented propane-air flame to a flat water-cooled surface of 0.95 cm diameter which was surrounded by a larger water-cooled surface. The highest value of the heat flux was 190 W cm^{-2} for an electrical power input which was probably equivalent to 140% of the rate at which heat was released by combustion. They state that heat flux was largely independent of propane to air ratio and confine their results to flames containing 5% propane.

6. HEAT CONDUCTION IN SLABS

The process of flame hardening the surface layers of a steel slab requires that a well defined temperature distribution is produced in the slab. This temperature distribution can be used to determine the heat flux necessary for surface hardening by solving the heat conduction equation.

The heat conduction equation and its solution under conditions which are similar to those encountered in flame hardening is reviewed below. A semi-infinite slab is considered throughout and its initial temperature is taken as zero.

(a). The heat conduction equation.

A semi-infinite slab extending over positive x is shown in figure II 23. The slab is heated at its surface in the plane of $x = 0$.

Conduction of heat in the x direction inside the slab is described by the heat conduction equation

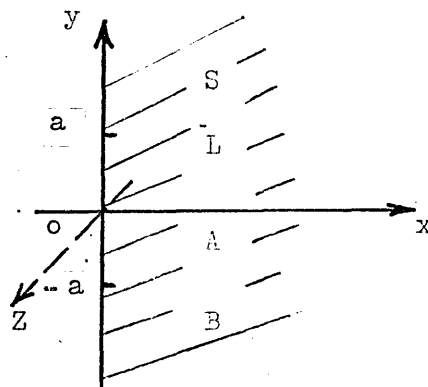


Figure II 23

$$\rho c \frac{\partial \theta}{\partial t} = \frac{\partial}{\partial x} \left[k \frac{\partial \theta}{\partial x} \right] \quad \text{II 19}$$

Equation II 19 assumes that there is no heat generated in the slab and that the slab is homogeneous. If ρ, c and k are functions of temperature, equation II 19 may be written as

$$\rho c \frac{\partial \theta}{\partial t} = k \frac{\partial^2 \theta}{\partial x^2} + \frac{dk}{d\theta} \left[\frac{\partial \theta}{\partial x} \right]^2 \quad \text{II 20.}$$

If ρ, c and k are independent of temperature, equation II 19 becomes

$$\frac{\partial \theta}{\partial t} = \alpha \frac{\partial^2 \theta}{\partial x^2} \quad \text{II 21}$$

where

$$\alpha = \frac{k}{\rho c}$$

(b) Methods of solving the heat conduction equation.

(i) Exact solutions

(39)

Carslaw and Jaeger have solved equation II 21

and its three dimensional equivalent,

$$\frac{\partial \theta}{\partial t} = \alpha \nabla^2 \theta \quad \text{II 21a}$$

for a large number of boundary conditions. A simple example occurs when the slab shown in figure II 23 is subjected to a uniform heat flux $\dot{q}_0''$ at its surface. The heat flux at any point in the slab is described by

$$\dot{q}'' = -k \frac{\partial \theta}{\partial x} \quad \text{--- II 22}$$

which satisfies the same differential equation as θ , viz.

$$\frac{\partial \dot{q}''}{\partial t} = \alpha \frac{\partial^2 \dot{q}''}{\partial x^2}, \quad x > 0, \quad t > 0 \quad \text{--- II 23}$$

The solution of equation II 23 with $\dot{q}'' = \dot{q}_0''$ at $x = 0$, $t > 0$ is

$$\dot{q}'' = \dot{q}_0'' \cdot \operatorname{erfc} \frac{x}{2\sqrt{\alpha t}} \quad \text{--- II 24}$$

Substituting for $\dot{q}''$ in equation II 22 and integrating over the semi-infinite slab gives

$$\theta = \frac{2\dot{q}_0''}{k} \left[\left(\frac{\alpha t}{\pi} \right)^{\frac{1}{2}} e^{-\frac{x^2}{4\alpha t}} - \frac{x}{2} \operatorname{erfc} \frac{x}{2\sqrt{\alpha t}} \right] \quad \text{--- II 25}$$

or

$$\theta = \frac{2\dot{q}_0''\sqrt{\alpha t}}{k} \left[\operatorname{ierfc} \frac{x}{2\sqrt{\alpha t}} \right] \quad \text{--- II 25a.}$$

The surface temperature θ_s is given by putting $x = 0$ in equation II 25a.

$$\theta_s = \frac{2\dot{q}_0''}{k} \sqrt{\frac{\alpha t}{\pi}} \quad \text{--- II 26}$$

If the supply of heat ceases at $t = T$ and the surface $x = 0$ is then thermally insulated, θ is given by equation II 25 or 25a, for $0 < t < T$. If $t > T$ then

$$\theta = \frac{2\dot{q}_0''\sqrt{\alpha}}{k} \left[\sqrt{t} \operatorname{ierfc} \frac{x}{2\sqrt{\alpha t}} - (t-T) \operatorname{ierfc} \frac{x}{2\sqrt{\alpha(t-T)}} \right] \quad \text{--- II 27}$$

Carslaw and Jaeger also solve equation II 21a for a semi-infinite slab moving across an infinite strip source. This case approximates to a slab moving across any given heat flux distribution. Referring to the axes of figure II 23 the strip source extends over $-a < y < a$, $-\infty < z < \infty$ in the plane $x = 0$ and the slab moves across the strip in the y direction with velocity u . A uniform heat flux $\dot{q}_0''$ is supplied by the strip to the surface of the slab. Curves

of dimensionless surface temperature, $\frac{\pi \theta k u}{2 \alpha \dot{q}_0''}$, against dimensionless distance along the slab, y/a , are plotted for various values of $\frac{u a}{2 \alpha}$.

(ii). Integral methods.

The analytical solutions described above are only available for the heat conduction equation when the thermal properties ρ , c and k are independent of temperature and when the boundary conditions are linear. Integral methods have been used by a number of workers (40,41) to overcome these disadvantages, particularly where non-linear boundary conditions are involved.

The example quoted in (i) above will be used to illustrate the general principles of the method. A penetration distance δ is defined such that when $x = \delta$, $\dot{q}'' = 0$. Equation II 21 is integrated between $x = 0$ and $x = \delta$ by applying the Liebnitz integral formula (42) to the right hand side to give

$$\alpha \left(\frac{\partial \theta}{\partial x} \right)_{\delta, t} - \alpha \left(\frac{\partial \theta}{\partial x} \right)_{0, t} = \frac{d}{dt} \left[\int_0^{\delta} \theta \, dx \right] - (\theta)_{\delta, t} \cdot \frac{d\delta}{dt} \quad \text{II 28}$$

Since the heat flux at the surface of the slab is uniform at $\dot{q}_0''$,

$$\left(\frac{\partial \theta}{\partial x} \right)_{0, t} = - \frac{\dot{q}_0''}{k} \quad \text{II 29}$$

and there is no heat transferred beyond $x = \delta$,

$$(\theta)_{\delta, t} = 0 \quad \text{II 30}$$

and

$$\left(\frac{\partial \theta}{\partial x} \right)_{\delta, t} = 0 \quad \text{II 31}$$

Assume that the temperature profile in the slab can be represented by a quadratic

$$\theta = A_0 + A_1 x + A_2 x^2 \quad \text{II 32}$$

The A's can be evaluated using equations II 29-31 to give

$$\theta = \frac{\dot{q}_0''}{2k\delta} (\delta - x)^2 \quad \text{II 33}$$

Substituting from equations II 29,30,31 and 32 into equation II 28 gives an ordinary differential equation in δ ,

$$\alpha = \frac{d}{dt} \cdot \left(\frac{\delta^2}{6} \right)$$

which can be integrated from 0 to t to give

$$\delta = \sqrt{6 \alpha t} \quad \text{--- II 34}$$

The surface temperature θ_s is obtained by putting $x = 0$ in equation II 33, and by substituting for δ from equation II 34.

$$\theta_s = \frac{\dot{q}_0''}{k} \sqrt{\frac{3 \alpha t}{2}} \quad \text{--- II 35}$$

Comparison with the exact solution, equation II 26 shows that

$$\frac{\theta_s \text{ (integral method)}}{\theta_s \text{ (exact solution)}} = \frac{\sqrt{\frac{3}{2}}}{\sqrt{\frac{4}{\pi}}} = 1.09$$

This 9% error can be reduced if equation II 32 is replaced by a cubic

$$\theta = A_0' + A_1'x + A_2'x^2 + A_3'x^3$$

giving

$$\theta_s = \frac{\dot{q}_0''}{k} \sqrt{\frac{4 \alpha t}{3}} \quad \text{--- II 35a}$$

Goodman (40) has used the integral method to find the temperature distribution in a slab which is subjected to a heat flux that varies with time.

The surface heat flux

$\dot{q}''$ is shown in figure II 24 as a function of time t.

For $0 < t < t_M$:-

The method outlined above is valid. Equation

II 29 becomes

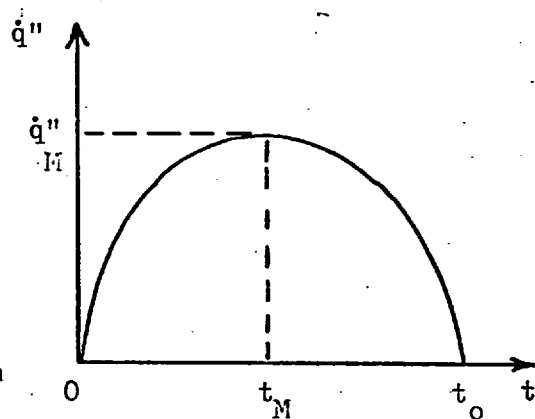


Figure II 24

$$\left(\frac{\partial \theta}{\partial x} \right)_{0,t} = - \frac{\dot{q}''}{k} \quad \text{--- II 29a}$$

giving

$$\theta = \frac{\dot{q}''}{2k\delta} (\delta - x)^2 \quad \text{--- II 33a}$$

and

$$\delta = \sqrt{6\alpha} \left[\frac{1}{\dot{q}''} \int_0^t \dot{q}'' dt \right]^{\frac{1}{2}} \quad \text{--- II 34a}$$

$$\text{and } \theta_s = \frac{1}{k} \sqrt{\frac{3\alpha}{2}} \left[\dot{q}'' \int_0^t \dot{q}'' dt \right]^{\frac{1}{2}} \quad \text{--- II 35b}$$

Equation II 35b does not hold true when $t > t_M$ because θ_s will not start to fall immediately that t reaches t_M , as predicted by equation 35b, but will continue to rise for some little time.

For $t > t_M$:-

The temperature is assumed to consist of two parts

$$(\theta)_{x,t} = (\theta_1)_{x,t} + (\theta_2)_{x,t}$$

where θ_1 satisfies a fictitious boundary condition

$$\left(\frac{\partial \theta}{\partial x} \right)_{0,t} = - \frac{\dot{q}''_M}{k}$$

and is associated with a penetration distance δ_1 . By analogy with equation II 34a

$$\delta_1 = \sqrt{\frac{6\alpha}{\dot{q}''}} \left[\int_0^{t_M} \dot{q}'' dt + \dot{q}''_M (t - t_M) \right]^{\frac{1}{2}} \quad \text{--- II 34b}$$

The surface heat flux associated with θ_2 is negative, begins at t_M , and is such that the total heat flux follows the curve shown in figure II 24. A penetration distance δ_2 is associated with θ_2 which is given by

$$\theta_2 = - \frac{(\dot{q}''_M - \dot{q}'')}{2k\delta_2} (x - \delta_2)^2 \quad \text{--- II 33b}$$

by the method,

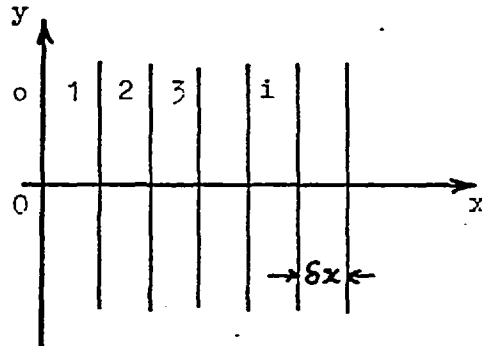
$$(\theta_s)_{t > t_M} = \frac{1}{k} \sqrt{\frac{3\alpha}{2}} \left\{ \sqrt{\dot{q}_M''} \left[\int_0^{t_M} (\dot{q}'' - \dot{q}_M'') dt + \dot{q}_M'' t \right]^{\frac{1}{2}} - (\dot{q}_M'' - \dot{q}'')^{\frac{1}{2}} \left[\int_{t_M}^t (\dot{q}_M'' - \dot{q}'') dt \right]^{\frac{1}{2}} \right\} \quad \text{--- II 35c}$$

$$(\theta_s)_{t > t_0} = \frac{1}{k} \sqrt{\frac{3\alpha \dot{q}_M''}{2}} \left\{ \left[\int_0^{t_M} (\dot{q}'' - \dot{q}_M'') dt + \dot{q}_M'' t \right]^{\frac{1}{2}} - \left[\dot{q}_M'' (t - t_M) - \int_{t_M}^{t_0} \dot{q}'' dt \right]^{\frac{1}{2}} \right\} \quad \text{--- II 35d}$$

(iii) Finite difference techniques.

These numerical methods have been widely used for determining the temperature distributions in solids. (43,44)

The semi-infinite slab shown in figure II 25 is divided into layers, each of thickness δx . At the plane separating the i th and $(i + 1)$ th layers



$$x = x_i = i \delta x$$

where i is an integer.

Figure II 25

Assuming that conduction of heat occurs in the x direction only, then θ_i is the temperature of the plane x_i and θ_{i+1} can be expanded in a Taylor's series to give

$$\theta_{i+1} - 2\theta_i + \theta_{i-1} = (\delta x)^2 \left(\frac{\partial^2 \theta}{\partial x^2} \right)_i + \frac{(\delta x)^4}{12} \left(\frac{\partial^4 \theta}{\partial x^4} \right)_i + \dots$$

which approximates to

$$\left(\frac{\partial^2 \theta}{\partial x^2} \right)_i = \frac{\theta_{i+1} - 2\theta_i + \theta_{i-1}}{(\delta x)^2} \quad \text{--- II 36}$$

The time t can be divided into intervals δt , to give the approximation

$$\frac{\partial \theta}{\partial t} = \frac{\theta_{t+\delta t} - \theta_t}{\delta t} \quad \text{--- II 37}$$

Substituting from equations II 36 and 37 into equation II 21 gives

$$\theta'_i = \theta_i + \frac{\alpha \delta t}{(\delta x)^2} (\theta_{i-1} - 2\theta_i + \theta_{i+1}) \quad \text{--- II 38}$$

where θ'_i is the temperature at time $t + \delta t$.

The method can only be used if errors do not increase,

i.e.
$$\frac{\alpha \delta t}{(\delta x)^2} \leq \frac{1}{2}$$

This is the stability condition.

Schmidt's graphical method is based on

$$\frac{\alpha \delta t}{(\delta x)^2} = \frac{1}{2}$$

i.e.
$$\theta'_i = \frac{\theta_{i-1} + \theta_{i+1}}{2}$$

The boundary condition, equation II 22, may be written

$$(\dot{q}'')_{\theta_o, t} = -k \left(\frac{\partial \theta}{\partial x} \right)_o \quad \text{--- II 39}$$

An approximation can be obtained for $\left(\frac{\partial \theta}{\partial x} \right)_o$ by considering a fictitious layer of thickness δx beyond the surface of the slab. The temperature of the surface of this layer is assumed to be θ_{-1} and equation II 39 becomes

$$(\dot{q}'')_{\theta_o, t} = k \frac{\theta_{-1} - \theta_1}{2 \delta x} \quad \text{--- II 40}$$

The surface temperature θ'_o is given by

$$\theta'_o = \theta_o + \frac{\alpha \delta t}{(\delta x)^2} \left(\theta_1 - 2\theta_o + \frac{2 \delta x \dot{q}''}{k} \right) \quad \text{--- II 41}$$

Allowance can be made in this method for thermal properties which vary with temperature. Non-linear boundary conditions can also be included.

7. FLAME HARDENING.

Certain types of steel can be hardened if the steel is held at a suitably high temperature for sufficient time to form austenite and is then cooled rapidly to a much lower temperature to transform the austenite to martensite. Martensite is the hard constituent and successful hardening is dependent upon establishing temperature-time relationships which allow austenite to be formed and then transformed to martensite. These temperature-time relationships will be discussed below, together with the process of flame hardening and some methods of measuring hardness.

(a) Formation of martensite.

The principles of heat treating steel in order to harden it are given in standard texts such as that by Grossman and Bain (46).

Steels consist of one or more of the following constituents, if alloying elements other than carbon are ignored:-

Ferrite - soft, α iron containing carbon in solution .

Cementite - iron carbide, Fe_3C , which is hard and brittle.

Pearlite - an intimate mixture of ferrite and cementite, usually appearing as alternate lamellae of ferrite and cementite.

Austenite - γ iron containing carbon in solution.

Martensite - very hard.

The iron carbon diagram shown in figure II 26 illustrates all of these phases, except martensite, for plain carbon steels in the range 0 to 1.0% carbon.

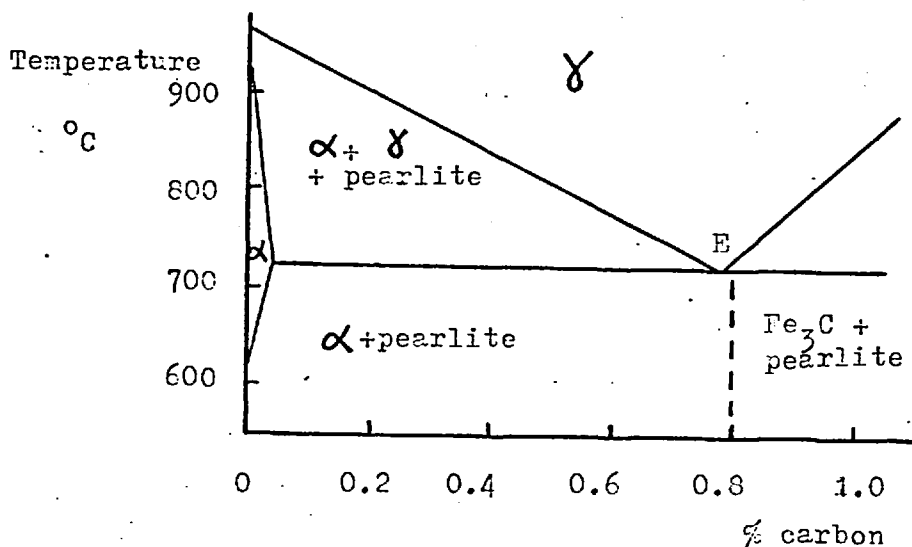


Figure II 26

E on the diagram represents the eutectoid which occurs at 0.8% carbon. In the annealed state steel at E is composed entirely of pearlite. Steels containing less than 0.8% carbon are known as hypoeutectoid steels and consist of pearlite with some ferrite, the proportion of ferrite increasing as the carbon content decreases. Hypereutectoid steels contain more than 0.8% carbon and consist of pearlite and cementite, the cementite content increasing as the carbon content increases. Hypoeutectoid steels are usually chosen for flame hardening because the high strength of the pearlite and ferrite is retained in the main mass of the material beyond the hardened surface layer of martensite. Consequently, the following explanations will relate to hypoeutectoid steels, with special reference to En 8 which is a 0.4% carbon steel often used for flame hardening. Although En 8 contains some manganese and silicon which modify figure II 26 slightly, this steel may be regarded as a plain carbon steel.

From figure II 26, a sample of steel would have to be heated to about 720°C in order to begin the transformation to austenite (γ). At E, transformation to austenite is complete at 720°C but for En 8 (0.4% carbon) the transformation is not complete until about 800°C is reached. Martensite, the hard constituent, can then be formed by quenching the austenite to below the martensite transformation temperature. A plot of the maximum hardness attainable against carbon content for plain carbon steels is shown in figure II 27 (47), where V.P.N. is the Vickers hardness (to be explained below). In the fully hardened state En 8 may be expected to reach a V.P.N. of 600.

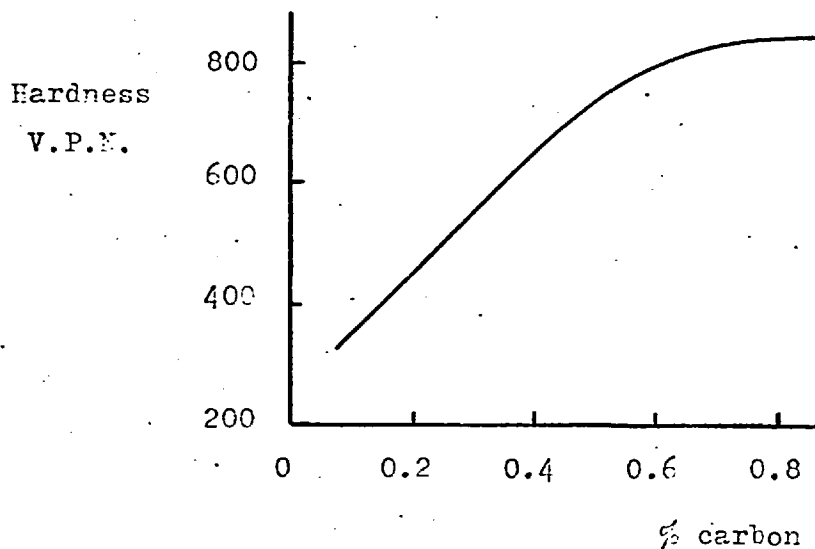


Figure II 27

Figure II 26 is an equilibrium diagram and does not provide information about the time required for transformations to occur. The kinetics of the austenite and martensite transformations will be considered.

(i) Austenite transformation.

The time required for the formation of austenite from a fine-grained pearlite structure has been determined for plain carbon eutectoid steel at various temperatures as shown in figure II 28 (48).

These curves are probably representative of En 8 also but they do not appear to have been determined for this steel.

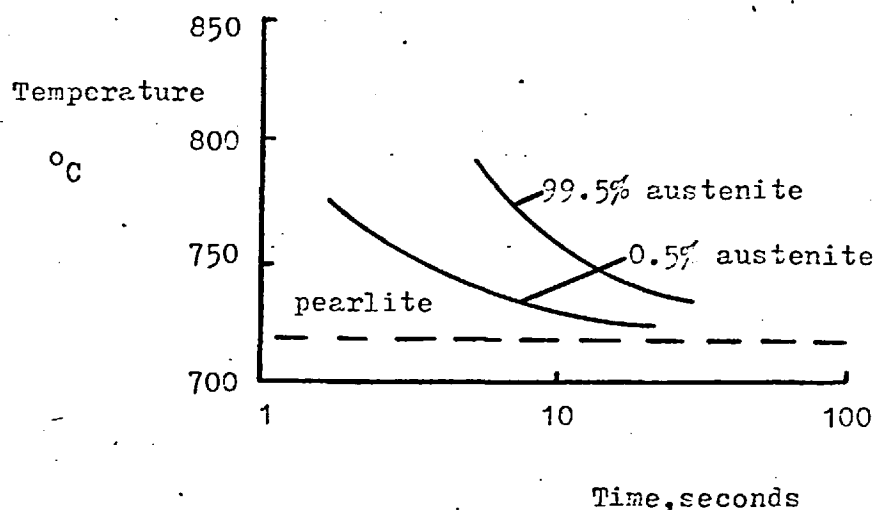


Figure II 28

The two curves in figure II 28 indicate the beginning and end of the austenite transformation. An approximate value of the time required for austenitisation at a specified temperature can be obtained from these curves.

(ii) Martensite transformation.

An approximate value of the rate of cooling required to transform austenite to martensite can be obtained from an isothermal transformation diagram. The diagram for En 8 ASTM 10 grain size is given in figure II 29 (49). This diagram has been constructed by austenitising a thin sample of the En 8 almost completely by holding it at 850°C for half-an-hour. The sample is then cooled extremely rapidly to the temperature at which the transformation of austenite is to be investigated and is held at this temperature for a certain length of time.

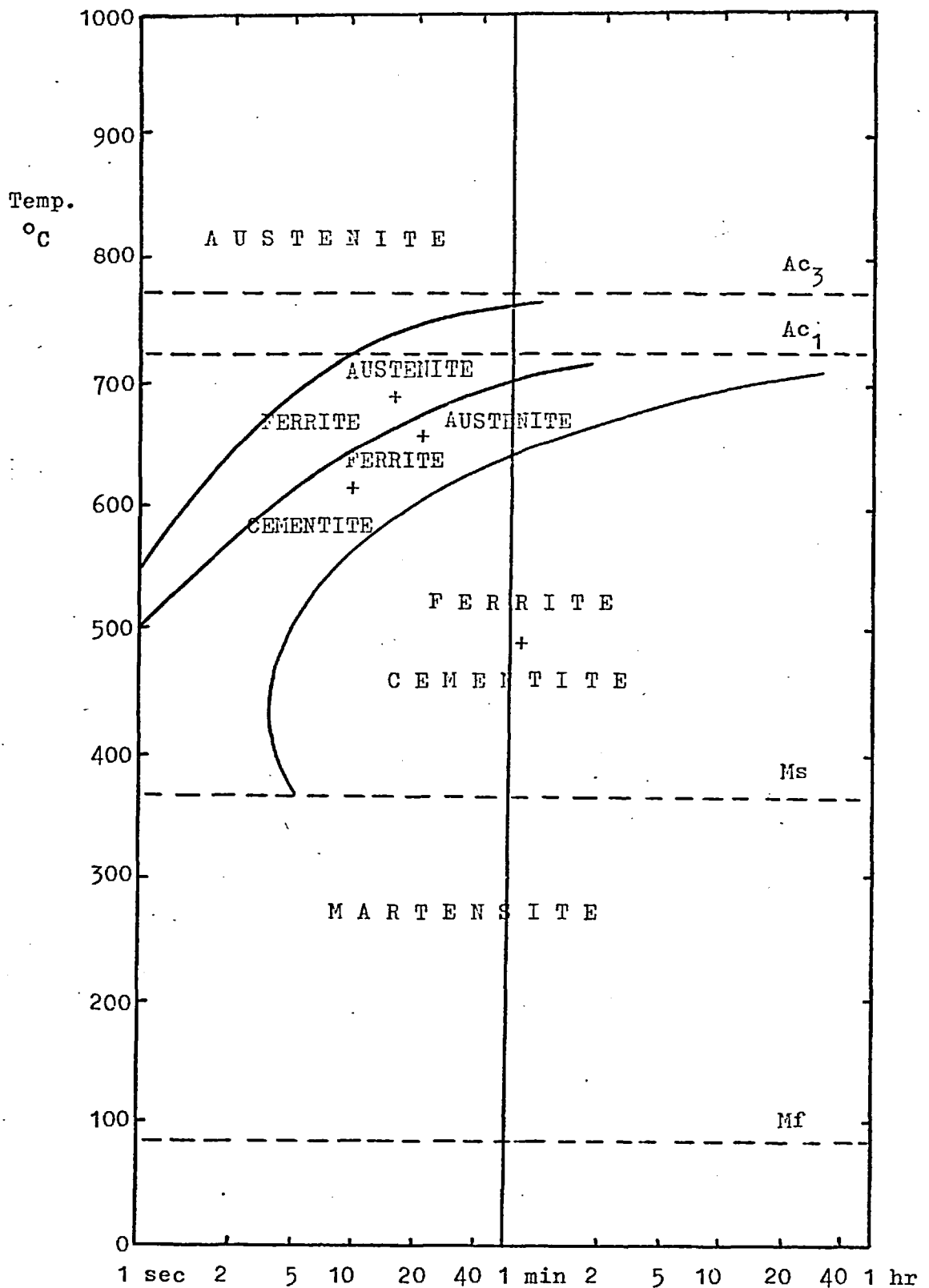


FIGURE II 29 - ISOTHERMAL TRANSFORMATION DIAGRAM FOR EN 8

This procedure is repeated for many temperatures and time intervals. After each time interval the sample is again quenched and is examined to find the amount of pearlite present. These results are plotted on the diagram and will probably only approximate to isothermal transformations because of the impossibility of cooling the specimen from 850°C to the required temperature in zero time. The diagram is very dependent on the grain size of the original structure and on the steel's composition. Martensite begins to occur at the M_s line and transformation is completed at the M_f line, these lines appearing on figure II 29.

In flame hardening practice, steels do not transform isothermally because certain cooling conditions are imposed, such as a water quench. Also the transformation of austenite is accompanied by a heat release.

(b) Hardenability and hardness.

(i) Jominy test.

The hardenability of a steel is often assessed by the Jominy test. A standard bar of the steel is austenitised at a specified temperature and then one end of the bar is quenched by a water jet under standard conditions. The hardness is measured at various distances from the quenched end. A curve of hardness against the distance from the quenched end can be plotted and a typical result for En 8 is shown in figure II 30⁽⁴⁹⁾.

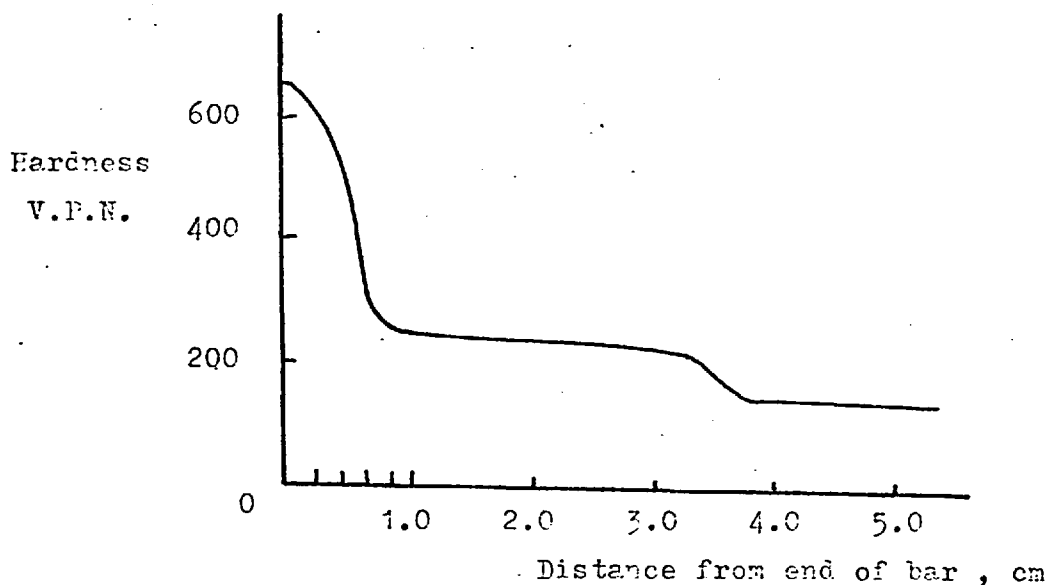


Figure II 30

This curve shows that a V.P.N. of 600 may only be expected over a depth of 2 to 3 mm and that the hardness drops to that of the untreated specimen at a depth of about 10 mm. En 8 is therefore known as a shallow hardening steel.

A curve of the theoretical rate of cooling expected during a Jominy test against distance along the bar is shown in figure II 31 (50).

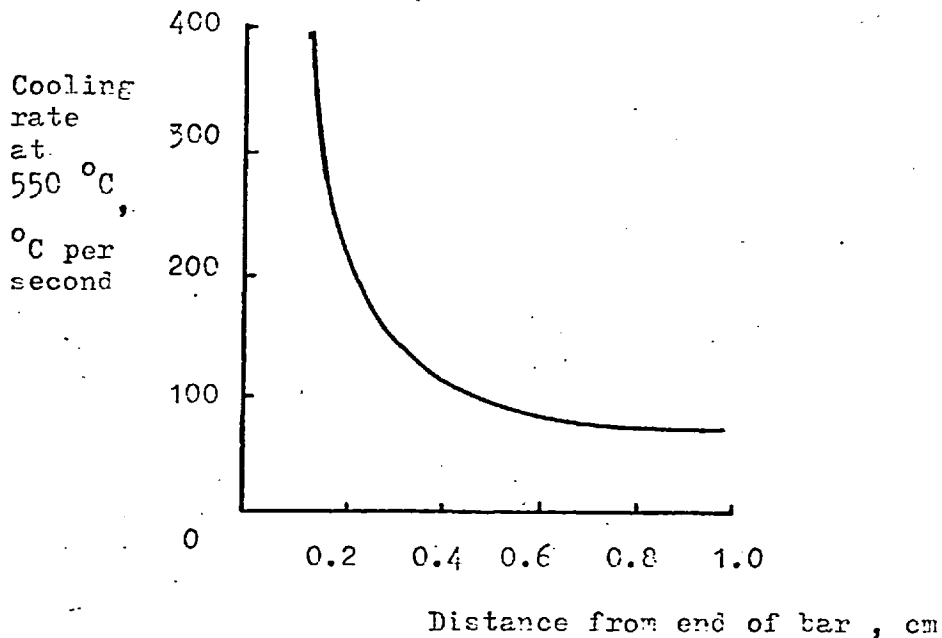


Figure II 31

The rate of cooling varies with the temperature of the bar and has therefore been expressed at 550°C. Any heat of transformation was neglected in the calculation.

(ii) Hardness testing.

The Brinell method is the most well-known hardness test and is the basis of many other tests. A small steel sphere of high hardness is pressed into the surface of the piece whose hardness is to be tested by applying a standard load to the sphere. The diameter of the depression made by the sphere is measured. The resistance to deformation is given by

$$\frac{\text{applied load (kg)}}{\text{cross-sectional area of indentation (mm}^2\text{)}}$$

This is also the elastic limit of the material in compression to a first approximation. The metal under the sphere is cold worked during the test and offers more resistance to penetration than would be expected from the undeformed metal. Brinell attempted to correct for this effect by modifying the expression for the resistance to deformation to

$$\frac{\text{applied load (Kg)}}{\text{surface area of indentation (mm}^2\text{)}}$$

The value of this ratio is called the Brinell hardness number.

The Vickers test is a modification of the Brinell test. The sphere is replaced by a pyramid-shaped diamond which has the advantage of not measurably deforming when indenting hard steels. The Vickers pyramid number, V.P.N., has the same definition as the Brinell hardness number.

A microhardness test also exists which is similar to the Vickers test but leaves a much smaller indentation, of the order of 10 microns. The results are expressed in V.P.N. and the indenting mechanism is incorporated in the stage of an optical microscope.

(c) The process of flame hardening.

Descriptions of modern flame hardening practice are given by Scott (51) and in the Metals Handbook (52).

Flame hardening enables the surface of a hardenable ferrous alloy to be rapidly heated above the austenite transformation temperature by direct impingement of a high temperature flame and then cooled sufficiently quickly to transform the austenite to martensite. Depths of hardening up to about 6 mm can be obtained depending on the hardenability of the ferrous alloy, the flame, the time of heating and the method of quenching.

The process is used for the following reasons:-

- (1) For hardening large parts where conventional furnace heating is impracticable, e.g. large gears, rolls and dies.
- (2) For hardening a small area of a component, e.g. ends of valve stems and wearing surfaces of cams.
- (3) If dimensional accuracy is difficult to attain by furnace heating, e.g. large gears.
- (4) In order to use cheaper materials, e.g. replacement of carburised low carbon alloy steel by plain carbon steel.

Flame hardening competes directly with induction hardening, a process in which the surface heating is performed by a high frequency induction coil but which is otherwise similar to flame hardening. Other methods of surface hardening are carburising, nitriding and carbonitriding, all of which require furnace heating.

(i) Selection of materials.

All cast irons except white iron can be flame hardened. Plain carbon steels containing more than about 0.35% carbon are used, e.g. En 5, En 8 and En 9. Surface hardness values which may be expected by flame hardening En 8 are 365 to 510 V.P.N.⁽⁴⁹⁾. These values compare favourably with those obtained by the Jominy hardenability test, see figure II 30. Alloy steels containing 1% chromium are also flame hardened, e.g. En 15 and En 18.

(ii) Methods.

All methods of flame hardening employ a flame head and a spray or tank quench. The design of the flame head varies according to the type of fuel used and the method of flame hardening but, in general, consists of either a single or a multiple orifice burner. Quenchants may be oil, water, brine and gas according to the type of ferrous alloy which is to be hardened.

The various methods are:-

- (1) Spot hardening - a flame is played on the small area to be hardened and then the work-piece is quenched, e.g. hardening a valve stem end.
- (2) Progressive hardening - a flame and quench is moved over the surface which is to be hardened, as shown in figure II 32. The quenching spray is often incorporated in the flame head as shown in the figure.

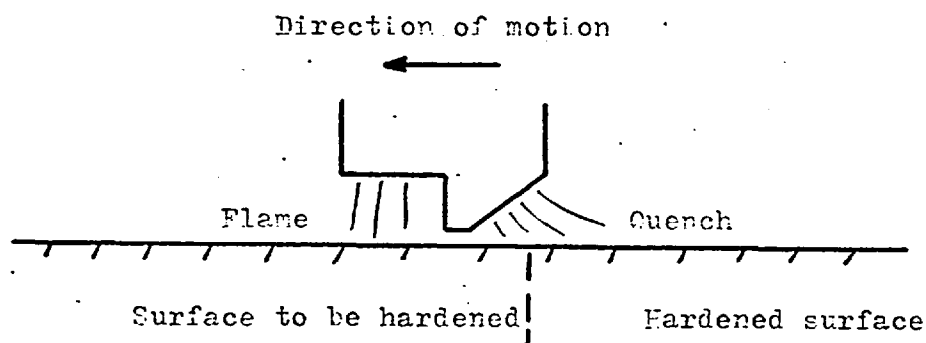


Figure II 32

- (3) Spin hardening - the work piece is rotated and its surface moves past a flame head. A spray quench is positioned near the flame head.
- (4) Progressive spin hardening - a combination of methods (2) and (3) which can be used to harden shafts. The shaft is simultaneously rotated about its axis and advanced in an axial direction past a flame head. The shaft is then quenched by a spray or by immersion in a tank.

(iii) Fuels.

The most common heat sources are oxy-gas flames. Air-gas flames are sometimes used for steels of high hardenability but shallow-hardening steels, such as En 8, require the higher heat fluxes which are associated with oxy-gas flames. The fuel gas is supplied with a sub-stoichiometric proportion of oxygen or air in order to avoid oxidation of the hardened surface. The fuel gases which are used include acetylene, propane, natural gas and town's gas. Howatt⁽⁵³⁾ has given the oxygen to acetylene ratio used in flame hardening as 1.0 to 1.2, whereas the maximum temperature of the oxy-acetylene flame occurs at a ratio of 1.7. Carbon is deposited at below 1.0 and surface oxidation occurs above 1.6.

(d) Quenching

The success of the flame hardening process depends on achieving a sufficiently rapid rate of cooling to transform austenite to martensite. Plain carbon steels are usually hardened using a water quench but little information appears to be available on the magnitude of the heat flux involved.

The boiling of liquids on hot surfaces is a similar process to quenching. Nukiyama (55) has studied the boiling of water on an electrically heated platinum wire. His plot of the heat flux at the surface of the wire against the difference between the temperature of the surface of the wire and the saturation temperature of water is shown in figure II 33.

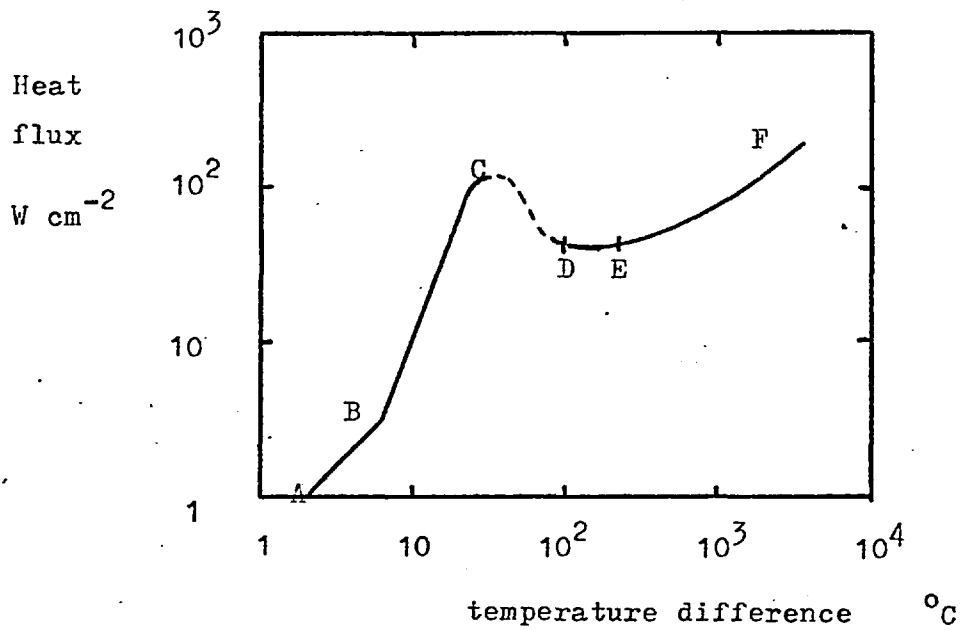


Figure 11 33 - Nukiyama's results for boiling heat transfer

In the region AB, water is being heated by natural convection, and evaporation only occurs at the surface of the pool of water. Region BC represents nucleate boiling, where bubbles are formed which rise through the water. As more and more bubbles are nucleated, they start to join together to form a film of vapour on the surface of the metal and the heat flux decreases until a minimum is reached at D. In the film boiling region EF, the heat flux increases because heat is transferred through the film of vapour by conduction and radiation. This region of film boiling should represent the important part of the quenching process where the surface temperature is reduced from about 800°C to 360°C before martensite transformation starts. To a first approximation, the heat flux can be considered constant during this part of the quenching process at about 50 W cm^{-2} .

III THEORY.

The integral profile method of Goodman (see page 46) has been modified to predict the surface temperature of a semi-infinite slab moving with constant velocity over a burner and quench whose heat flux can be expressed as a function of distance along the surface of the slab. The method allows the heat flux distribution to be split into any number of imaginary parts, the sum of these imaginary heat fluxes at any point on the surface of the slab being equal to the heat flux at that point. The integral method enables a surface temperature to be found for each imaginary heat flux. The slab's surface temperature is the sum of these individual surface temperatures. Examples of normal, triangular and rectangular distributions of heat flux are given, with each distribution being interrupted by a quench of negative flux. The quench is assumed to be of constant flux (see page 59).

The limiting conditions required by the process of flame hardening, such as the maximum slab velocity and the minimum distance between the burner and the quench, are obtained from the relationships between surface temperature and heat flux distribution.

1. THE INTEGRAL PROFILE METHOD APPLIED TO A SEMI-INFINITE SLAB MOVING ACROSS A GIVEN BURNER HEAT FLUX DISTRIBUTION FOLLOWED BY A QUENCH.

The co-ordinates of the system are shown in figure 1. The slab moves over the burner and quench at a velocity u .

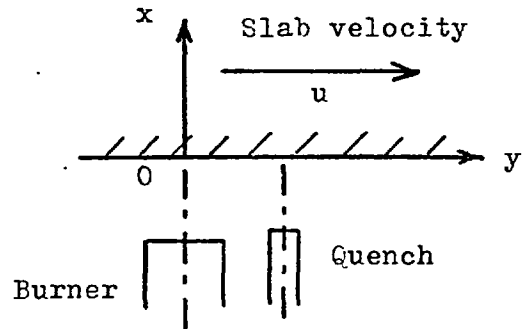


Figure 1.

The following assumptions are made:-

1. Conduction of heat occurs in the x - direction only.
2. A steady temperature field exists in the slab relative to the burner.
3. The initial temperature of the slab is taken to be at the zero of the temperature scale.
4. The physical properties of the slab do not vary with temperature.

The heat conduction equation for this system is

$$\alpha \frac{\partial^2 \theta}{\partial x^2} - u \frac{\partial \theta}{\partial y} = \frac{\partial \theta}{\partial t}$$

By assumption 2:-

$$\frac{\partial \theta}{\partial t} = 0$$

Hence

$$\alpha \frac{\partial^2 \theta}{\partial x^2} = u \frac{\partial \theta}{\partial y}$$

$$\frac{\partial^2 \theta}{\partial x^2} = \frac{u}{\alpha} \frac{\partial \theta}{\partial y} \quad \text{or} \quad \frac{\partial^2 \theta}{\partial x^2} = \frac{u}{\alpha} \frac{\partial \theta}{\partial y}$$

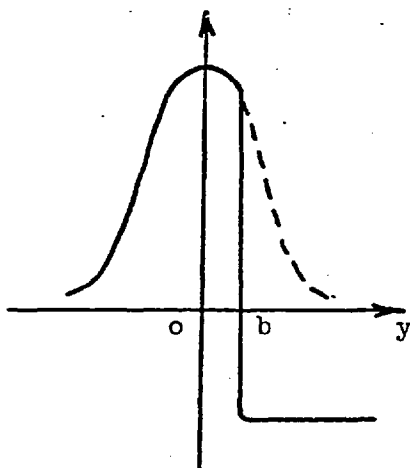


Figure 2.

The heat flux q'' crosses the surface of the slab in the x - direction and is distributed over the surface in the y - direction as shown in figure 2. The distribution due to the burner lies between $-\infty$ and b and that due to the quench lies between b and ∞ .

At all points on the surface of the slab:-

$$k \left(\frac{\partial \theta}{\partial x} \right)_{x=0} = - \dot{q}'' \quad \text{--- 2.}$$

The temperatures within the slab must satisfy equation 1, subject to equation 2 and the boundary conditions remote from the burner. It is impossible to obtain a formal solution to equation 1 in this case and so an approximate method is developed below.

The heat flux distribution shown in figure 2 can be split into three imaginary parts as shown in figure 3 so that

$$\dot{q}'' = \dot{q}_1'' + \dot{q}_2'' + \dot{q}_3''$$

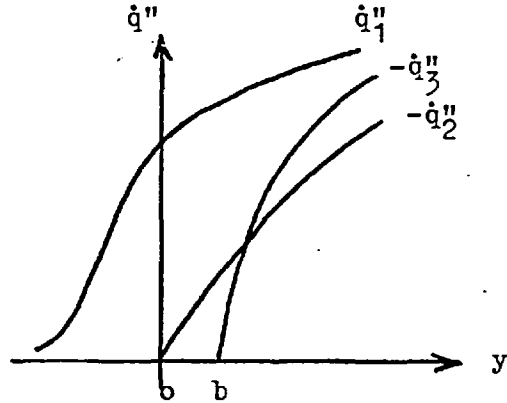


Figure 3.

The temperature in the slab can also be split into three imaginary parts, each of which will correspond to one of the imaginary heat fluxes, $\dot{q}_j''$, so that:-

$$\dot{q}_j'' = -k \left(\frac{\partial \theta_j}{\partial x} \right)_{x=0} \quad \text{--- 3.}$$

$$\text{and } \theta = \theta_1 + \theta_2 + \theta_3 = \sum_0^n \theta_j$$

Since equations 1 and 2 are linear, θ will satisfy them both, provided each θ_j does. Each imaginary temperature is assumed to penetrate for a certain distance δ_j so that:-

$$(\theta_j)_{x=\delta_j} = 0 \quad \text{--- 4.}$$

$$\left(\frac{\partial \theta_j}{\partial x} \right)_{x=\delta_j} = 0 \quad \text{--- 5.}$$

Integrating equation 1 for each θ_j between $x=0$ and $x=\delta_j$ and applying the Liebnitz integral formula to the right hand side gives:-

$$\alpha \left(\frac{\partial \theta_j}{\partial x} \right)_{\delta_j, y} - \alpha \left(\frac{\partial \theta_j}{\partial x} \right)_{0, y} = u \cdot \frac{d}{dy} \int_0^{\delta_j} \theta_j dx - (\theta_j)_{\delta_j} \frac{d \delta_j}{dy}$$

Substituting for $(\theta)_{\delta_j}$ from equation 4 gives

$$\propto \left[\left(\frac{\partial \theta_j}{\partial x} \right)_{\delta_j, y} - \left(\frac{\partial \theta_j}{\partial x} \right)_{0, y} \right] = u \cdot \frac{d}{dy} \int_0^{\delta_j} \theta_j dx$$

Assume that the temperature profile in the slab is given by

$$\theta_j = A_0 + A_1 x + A_2 x^2 \quad \text{where the A's may be functions of } y \text{ and are evaluated using equations 3, 4 and 5 to give:-}$$

$$\theta_j = \frac{\dot{q}''_j (\delta_j - x)^2}{2k\delta_j} \quad \text{--- 7}$$

$\dot{q}''_j$ in equation 7 is the value of the imaginary surface heat flux at the point y . Equations 3, 5, 7 and 6 give an ordinary differential equation for δ_j .

$$\frac{\alpha \dot{q}''_j}{k} = u \cdot \frac{d}{dy} \left(\frac{\dot{q}''_j \delta_j^2}{6k} \right)$$

which can be solved between $-\infty$ and y to give

$$\delta_j^2 = \frac{6\alpha}{u\dot{q}''_j} \int_{-\infty}^y \dot{q}''_j dy \quad \text{--- 8}$$

Since $\delta_j = 0$ at $y = -\infty$.

At the surface of the slab $x = 0$ and $\theta_j = \theta_{j,s}$,

Substituting δ_j from equation 8 into equation 7 and putting

$x = 0$ in equation 7 gives:-

$$\theta_{j,s} = \frac{1}{k} \left[\frac{3\alpha \dot{q}''_j}{2u} \int_{-\infty}^y \dot{q}''_j dy \right]^{\frac{1}{2}} \quad \text{--- 9}$$

The surface temperature θ_s is given by

$$\theta_s = \sum_0^n \theta_{j,s} = \frac{1}{k} \sqrt{\frac{3\alpha}{2u}} \sum_0^n \left(\dot{q}''_j \int_{-\infty}^y \dot{q}''_j dy \right)^{\frac{1}{2}} \quad \text{--- 10 or III 2}$$

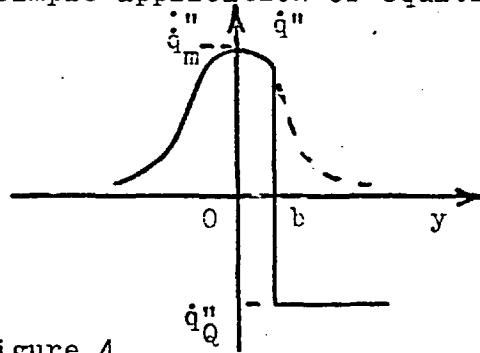
If the temperature profile in the slab is assumed to be given by:-

$$\theta_j = A_0 + A_1 x + A_2 x^2 + A_3 x^3,$$

i.e. a cubic instead of a quadratic, equation 10 becomes:-

$$\theta_s = \frac{1}{k} \sqrt{\frac{4\alpha}{3u}} \sum_0^n \left(\dot{q}''_j \int_{-\infty}^y \dot{q}''_j dy \right)^{\frac{1}{2}} \quad \text{--- 10a.}$$

A simple application of equation 10 is the case of a burner



heat flux with a normal distribution as shown in figure 4. A good approximation to the normal error function is given by a curve which is known as the "Witch of Agnesi":-

Figure 4.

$$q'' = q''_M \left[\frac{1}{1 + \left(\frac{y}{a}\right)^2} \right] \quad \text{--- 11.}$$

When $y = \pm a$, $q'' = \frac{q''_M}{2}$

The imaginary heat fluxes q''_j become:-

y	q''_1	q''_2	q''_3
< 0	$q''_M \cdot \left[\frac{1}{1 + \left(\frac{y}{a}\right)^2} \right]$	0	0
> 0 No quench	q''_M	$q''_M \left[1 - \frac{1}{1 + \left(\frac{y}{a}\right)^2} \right]$	0
> 0 With quench	q''_M	$q''_M \left[1 - \frac{1}{1 + \left(\frac{y}{a}\right)^2} \right]$	$y > b$ q''_Q

When $y < 0$, equation 10 becomes:-

$$\begin{aligned} (\theta_s)_{y < 0} &= \frac{q''_M}{k} \sqrt{\frac{3\alpha}{2u}} \left[\frac{1}{1 + \left(\frac{y}{a}\right)^2} \int_{-\infty}^y \frac{dy}{1 + \left(\frac{y}{a}\right)^2} \right]^{\frac{1}{2}} \\ &= \frac{q''_M}{k} \sqrt{\frac{3\alpha}{2u}} \left[\frac{1}{1 + \left(\frac{y}{a}\right)^2} \left(\tan^{-1} \frac{y}{a} + \frac{\pi}{2} \right) \right]^{\frac{1}{2}} \end{aligned} \quad \text{--- 12.}$$

When $y > 0$ without quench or $0 < y < b$ with quench

$$\begin{aligned} (\theta_s)_{y>0} &= \frac{\dot{q}_M''}{k} \sqrt{\frac{3\alpha}{2u}} \left\{ \left[\int_{-\infty}^0 \frac{dy}{1 + \left(\frac{y}{a}\right)^2} + \int_0^y dy \right]^{\frac{1}{2}} - \right. \\ &\quad \left. \left[\left(1 - \frac{1}{1 + \left(\frac{y}{a}\right)^2}\right) \int_0^y \left(1 - \frac{1}{1 + \left(\frac{y}{a}\right)^2}\right) dy \right]^{\frac{1}{2}} \right\} \\ &= \frac{\dot{q}_M''}{k} \sqrt{\frac{3\alpha}{2u}} \left\{ \left[\frac{y}{a} + \frac{\pi}{2} \right]^{\frac{1}{2}} - \left[\frac{\left(\frac{y}{a}\right)^2}{1 + \left(\frac{y}{a}\right)^2} \left(\frac{y}{a} - \tan^{-1} \frac{y}{a} \right) \right]^{\frac{1}{2}} \right\} \end{aligned}$$

13.

Similarly, when $y > b$ with quench:-

$$\begin{aligned} (\theta_s)_{y>b} &= \frac{\dot{q}_M''}{k} \sqrt{\frac{3\alpha}{2u}} \left\{ \left[\frac{y}{a} + \frac{\pi}{2} \right]^{\frac{1}{2}} - \left[\frac{\left(\frac{y}{a}\right)^2}{1 + \left(\frac{y}{a}\right)^2} \left(\frac{y}{a} - \tan^{-1} \frac{y}{a} \right) \right]^{\frac{1}{2}} \right. \\ &\quad \left. - \frac{\dot{q}_Q''}{\dot{q}_M''} \left[\frac{y - b}{a} \right]^{\frac{1}{2}} \right\} \end{aligned} \quad 14.$$

Equations 12, 13, and 14 can be expressed in dimensionless form by putting

$$\theta_s^* = \frac{\theta_s}{\theta_A} ; \quad u^* = \left(\frac{k \theta_A}{\dot{q}_M''} \right)^2 \cdot \frac{u}{\alpha a} ; \quad y^* = \frac{y}{a} ; \quad \dot{q}''^* = \frac{\dot{q}_Q''}{\dot{q}_M''}$$

Equation 12 becomes:-

$$(\theta_s^*)_{y^*<0} = \sqrt{\frac{3}{2u^*}} \left(\frac{\tan^{-1} y^* + \frac{\pi}{2}}{1 + (y^*)^2} \right)^{\frac{1}{2}} \quad 15. \text{ or III } 3a$$

Equation 13 becomes:-

$$\begin{aligned} (\theta_s^*)_{y^*>0} &= \sqrt{\frac{3}{2u^*}} \left[\left(y^* + \frac{\pi}{2} \right)^{\frac{1}{2}} - y^* \left(\frac{y^* - \tan^{-1} y^*}{(y^*)^2 + 1} \right)^{\frac{1}{2}} \right] \\ &\quad \text{no quench,} \\ &\quad \text{or } 0 < y^* < \frac{b}{a} \\ &\quad \text{with quench} \end{aligned} \quad 16. \text{ or III } 3b$$

Equation 14 becomes:-

$$\left(\theta_s^*\right)_{y^* > \frac{b}{a}} = \sqrt{\frac{3}{2u^*}} \left[\left(y^* + \frac{\pi}{2}\right)^{\frac{1}{2}} - y^* \left(\frac{y^* - \tan^{-1} y^*}{y^{*2} + 1} \right)^{\frac{1}{2}} - \dot{q}''^* \left(y^* - \frac{b}{a}\right)^{\frac{1}{2}} \right]$$

with quench

_____ 17 or III 3c.

Two further examples of the distribution of the burner heat flux may be considered, one having a triangular distribution and the other having a rectangular distribution, as shown in figures 5 and 6 respectively.

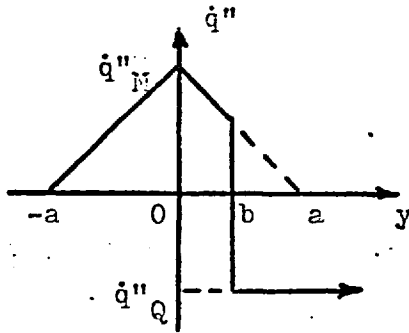


Figure 5.

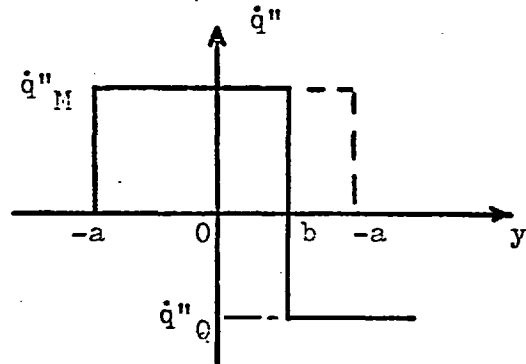


Figure 6.

For the triangular and rectangular distributions

y	$\dot{q}''_1$		$\dot{q}''_2$		$\dot{q}''_3$	
	Triang.	Rect.	Triang.	Rect.	Triang.	Rect.
$-a < y < 0$	$\dot{q}''_M \cdot \left(1 + \frac{y}{a}\right)$	$\dot{q}''_M$	0	0	0	0
$0 < y < b$	$\dot{q}''_M$	$\dot{q}''_M$	$\dot{q}''_M \cdot \frac{y}{a}$	0	0	0
$b < y < a$	$\dot{q}''_M$	$\dot{q}''_M$	$\dot{q}''_M \cdot \frac{y}{a}$	0	$\dot{q}''_Q$	$\dot{q}''_Q$
$y > a$	$\dot{q}''_M$	$\dot{q}''_M$	$\dot{q}''_M$	$\dot{q}''_M$	$\dot{q}''_Q$	$\dot{q}''_Q$

For a triangular distribution of heat flux equation III 2 becomes:-

$$\left(\theta_s^*\right)_{-1 < y^* < 0} = \frac{1}{2} \sqrt{\frac{3}{u^*}} \left(1 + y^*\right)^{3/2} \quad \text{_____ III 4 a.}$$

$$\left(\theta_s^*\right)_{0 < y^* < 1} = \frac{1}{2} \sqrt{\frac{3}{u^*}} \left[(2y^* + 1)^{\frac{1}{2}} - (y^*)^{3/2} \right] \quad \text{_____ III 4 b.}$$

no quench
or $0 < y^* < \frac{b}{a}$
with quench.

$$\left(\theta_s^*\right)_{\frac{b}{a} < y^* < 1} = \frac{1}{2} \sqrt{\frac{3}{u^*}} \left[(2y^* + 1)^{\frac{1}{2}} - (y^*)^{3/2} - \dot{q}''^* \left(y^* - \frac{b}{a}\right)^{\frac{1}{2}} \right] \quad \text{_____ III 4 c.}$$

With quench.

$$\left(\theta_s^*\right)_{y^* > 1} = \frac{1}{2} \sqrt{\frac{3}{u^*}} \left[(2y^* + 1)^{\frac{1}{2}} - (2y^* - 1)^{\frac{1}{2}} \right] \quad \text{_____ III 4 d.}$$

no quench.

$$\left(\theta_S^*\right)_{y^* > 1} = \frac{1}{2} \sqrt{\frac{3}{u^*}} \left[(2y^* + 1)^{\frac{1}{2}} - (2y^* - 1)^{\frac{1}{2}} - \dot{q}''^* \left(y^* - \frac{b}{a}\right)^{\frac{1}{2}} \right] \quad \text{with quench.} \quad \text{III 4 e.}$$

For a rectangular distribution of heat flux, equation III 2 becomes:-

$$\left(\theta_S^*\right)_{-1 < y^* < 1} = \sqrt{\frac{3}{2u^*}} (y^* + 1)^{\frac{1}{2}} \quad \text{III 5a.}$$

no quench,
or $-1 < y^* < \frac{b}{a}$
with quench.

$$\left(\theta_S^*\right)_{\frac{b}{a} < y^* < 1} = \sqrt{\frac{3}{2u^*}} \left[(y^* + 1)^{\frac{1}{2}} - \dot{q}''^* \left(y^* - \frac{b}{a}\right)^{\frac{1}{2}} \right] \quad \text{III 5b.}$$

with quench

$$\left(\theta_S^*\right)_{y^* > 1} = \sqrt{\frac{3}{2u^*}} \left[(y^* + 1)^{\frac{1}{2}} - (y^* - 1)^{\frac{1}{2}} \right] \quad \text{no quench} \quad \text{III 5 c.}$$

$$\left(\theta_S^*\right)_{y^* > 1} = \sqrt{\frac{3}{2u^*}} \left[(y^* + 1)^{\frac{1}{2}} - (y^* - 1)^{\frac{1}{2}} - \dot{q}''^* \left(y^* - \frac{b}{a}\right)^{\frac{1}{2}} \right] \quad \text{with quench.} \quad \text{III 5 d.}$$

When the quench is positioned at $y^* = \frac{b}{a}$, where $b > a$, equations -III 4c and III 5b are not required.

The curves of $\theta_S^* \sqrt{u^*}$ against y^* which are shown in figure III 7 have been obtained from equations III 3a, b, III 4 a, b, d and III 5a, c and represent surface temperatures obtained from "normal", triangular and rectangular distributions of heat flux without quenches. The heat flux distributions have been plotted directly beneath the surface temperature curves.

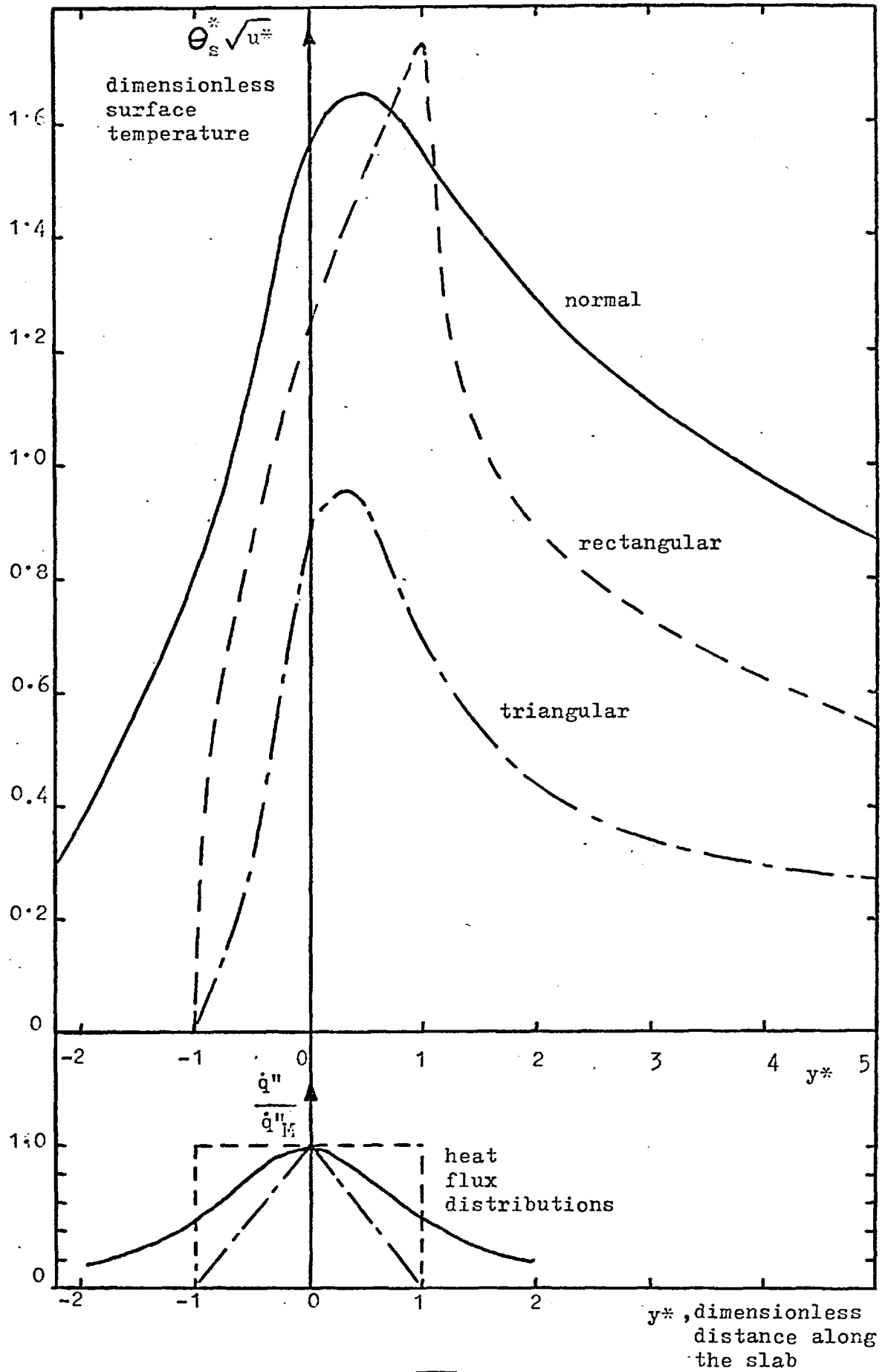


FIGURE III - 7

SURFACE TEMPERATURES FROM VARIOUS DISTRIBUTIONS OF HEAT FLUX

2. CONDITIONS FOR FLAME HARDENING.

Equations III 3 provide sufficient information to give the limiting conditions for flame hardening, assuming that the burner heat flux is distributed normally over the surface of the slab and that a quench of constant flux is positioned next to the burner. From figure III 7 the peak surface temperature which is obtained with a normal distribution is given by:-

$$\theta_S^* \sqrt{u^*} = 1.65, \text{ at } y^* = 0.4 \quad \text{---} \quad \text{III 6}$$

The limiting conditions for flame hardening are:-

1. The slab surface just attains the austenitizing temperature θ_A (see page 51).
2. The quench is applied as soon as the slab surface attains the austenitizing temperature θ_A .

These two conditions are satisfied when

$$\theta_S = \theta_A \text{ or } \theta_S^* = 1.0, \text{ at } y^* = 0.4 \text{ and the quench is applied at } y^* = 0.4 \quad \text{---} \quad \text{III 7}$$

Relationships III 6 and III 7 give:-

$$\text{Maximum value of } u^* \text{ for surface hardening} = 2.72 \quad \text{---} \quad \text{III 8}$$

In practice, surface hardening will occur when:-

1. $u^* < 2.72$
2. $\theta_S^* > 1.0$
3. Quench is applied at $y^* > 0.4$

These conditions will allow time for austenite to form and will give a finite depth of martensite (see page 54), assuming that the quench is sufficiently severe.

The effect of severity of quench on surface temperature is shown in figure III 8 in which $\theta_S^* \sqrt{u^*}$ has been plotted against y^* for a normal distribution of heat flux followed by various quenches of constant flux applied at $y^* = 0.4$. Severity of quench is represented by $\dot{q}^* = \dot{q}_Q / \dot{q}_M$.

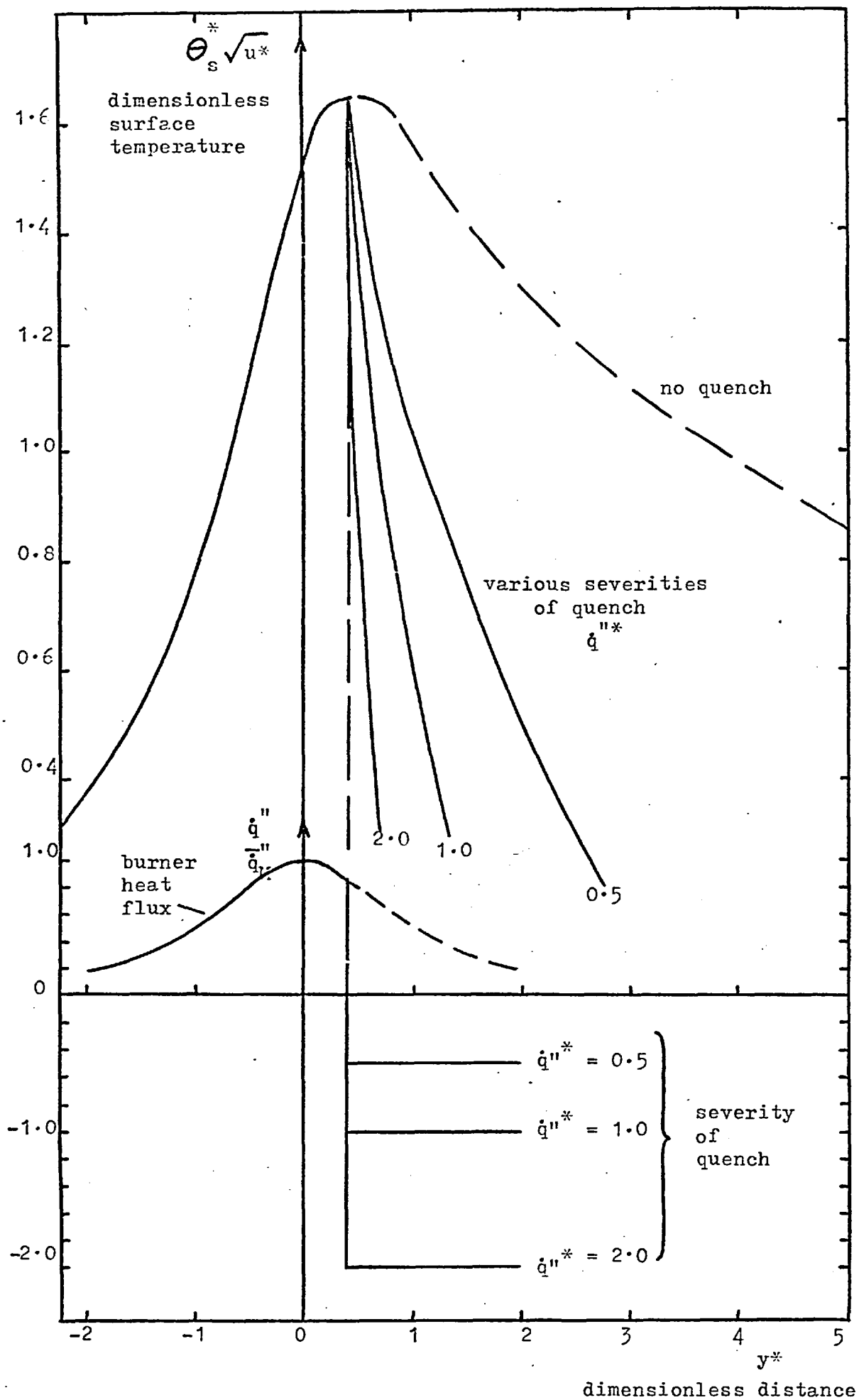


FIGURE III 8 - SURFACE TEMPERATURES FROM BURNER AND QUENCH HEAT FLUXES

IV. EXPERIMENTAL APPARATUS.

1. BURNERS.

Related designs have been developed for small and large burners, as described in Chapter V, page 95. The final version of the small burner was capable of burning 1 litre per minute of methane, whilst the larger burner had about ten times the capacity. They are both described below:-

(a) Small vortex-stabilized burner.

A full-scale sectional elevation and plan of this burner are shown in figure IV 1 and a photograph is shown in plate IV 1. A mixture of methane and air enters the swirl chamber A from the tangential inlet and then flows into the combustion chamber B where the gas is burnt. An electric arc is struck between the central electrode and the annular electrode. The products of combustion pass through this gap between the electrodes and then emerge from the nozzle.

(i). Swirl Chamber A.

The horizontal gas inlet tube was brazed in a tangential position to a vertical tube of larger diameter to comprise the swirl chamber shown in figure IV 1. Two flanges of syndanyo were held above and below the swirl chamber by three securing bolts, the upper flange being machined to provide a conical entry to the combustion chamber B.

(ii). Combustion Chamber B.

The walls of the combustion chamber were formed by a tube of re-crystallized alumina. The narrow entrance to the chamber was also made from an alumina tube and the gap between this tube and the tube comprising the combustion chamber was filled with asbestos fibre. This assembly was cemented into a groove which was machined in the top of the syndanyo flange. A close-fitting outer tube of alumina was placed round the combustion chamber. This tube was made slightly longer than the combustion chamber in order to locate positively the annular electrode, or nozzle, on top of the combustion chamber.

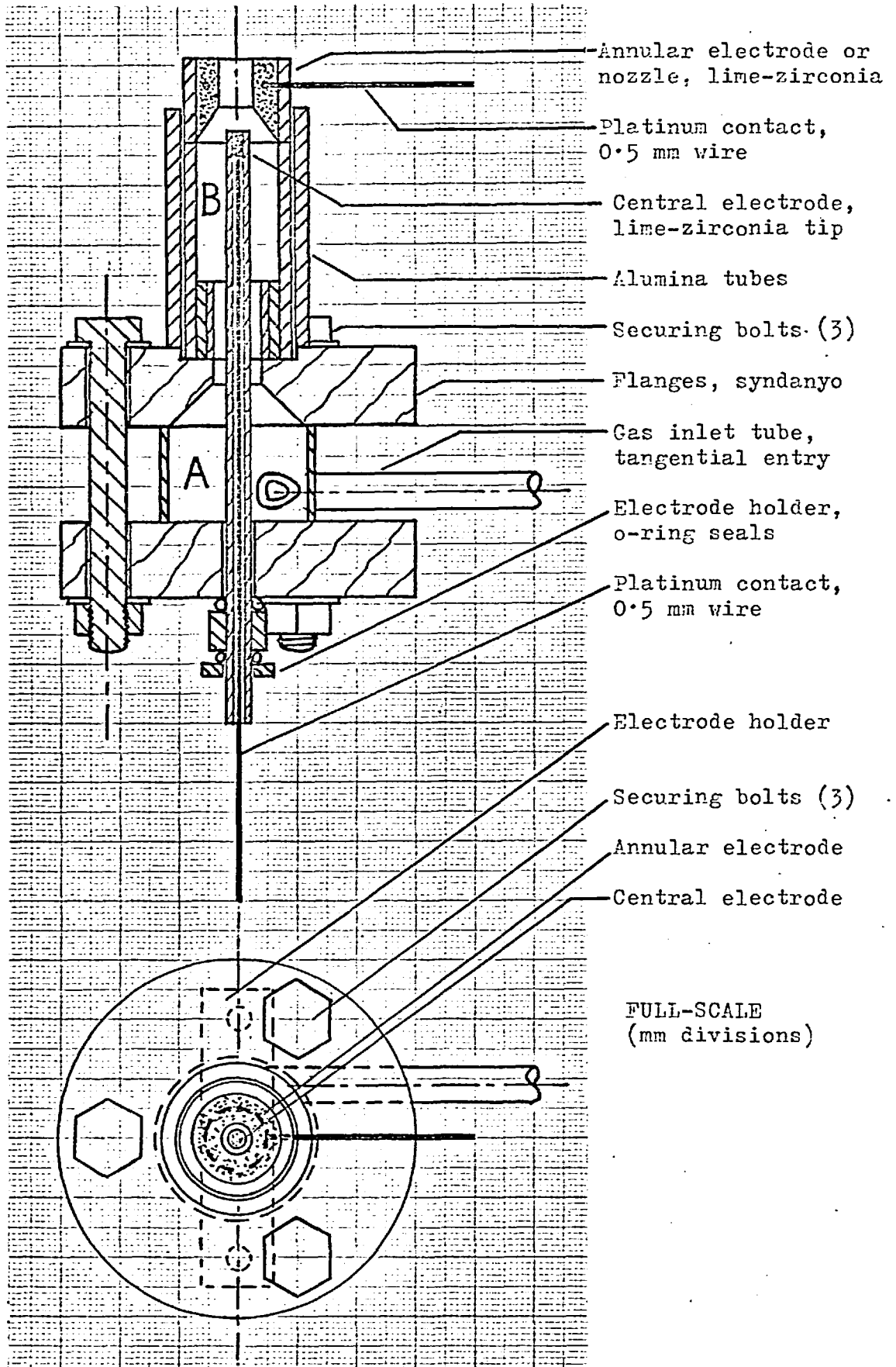


FIGURE IV 1 - SMALL VORTEX-STABILIZED BURNER,
SECTIONAL ELEVATION AND PLAN



PLATE IV 1 - THE SMALL VORTEX-STABILIZED BURNER
(SEE FIGURE IV 1 FOR DETAILS OF CONSTRUCTION)

(iii) Electrodes.

The central electrode consisted of a tube of magnesia-stabilized zirconia which was closed at its upper end by a small quantity of lime-zirconia cement. The tube contained a platinum wire which was embedded in the cement in order to conduct current to the tip of the electrode. The lower end of the platinum wire was connected to the power supply detailed on page 78. The electrode was located centrally in the burner. Two 'O' rings, separated by a metal spacer, gripped the ceramic tube just below the lower syndanyo flange. The 'O' ring assembly was supported on a plate which was bolted to the flange.

The annular electrode or nozzle was positioned on top of the combustion chamber and was made by casting lime-zirconia cement inside an alumina tube of the same diameter as the tube which formed the combustion chamber. An aluminium rod was machined to the profile of the nozzle and was used as a mould for the lime-zirconia casting. A ring of platinum wire was positioned concentrically inside the electrode during casting and a tail of the wire was led out of the electrode through a slit which was cut in the alumina tube.

The zirconia cement used to construct the electrodes was a lime-stabilized zirconia supplied by Zircoa Ltd., together with a separate binder. The cement was ground, dry, in a pestle and mortar to reduce the particle size. This was necessary to reduce the size of the pores in the finished casting. A minimal quantity of binder (unidentifiable) was also necessary to keep the pore size small. The zirconia castings were air-dried but not sintered before use.

(b) Large Vortex-Stabilized Burner.

The large burner was similar in design and construction to the small burner and is shown in figure IV 2 and plate IV 2. The differences between the final versions of the two designs are detailed below:-

(i). Combustion Chamber.

An alumina tube was no longer suitable as the combustion chamber because it cracked due to thermal shock. This was

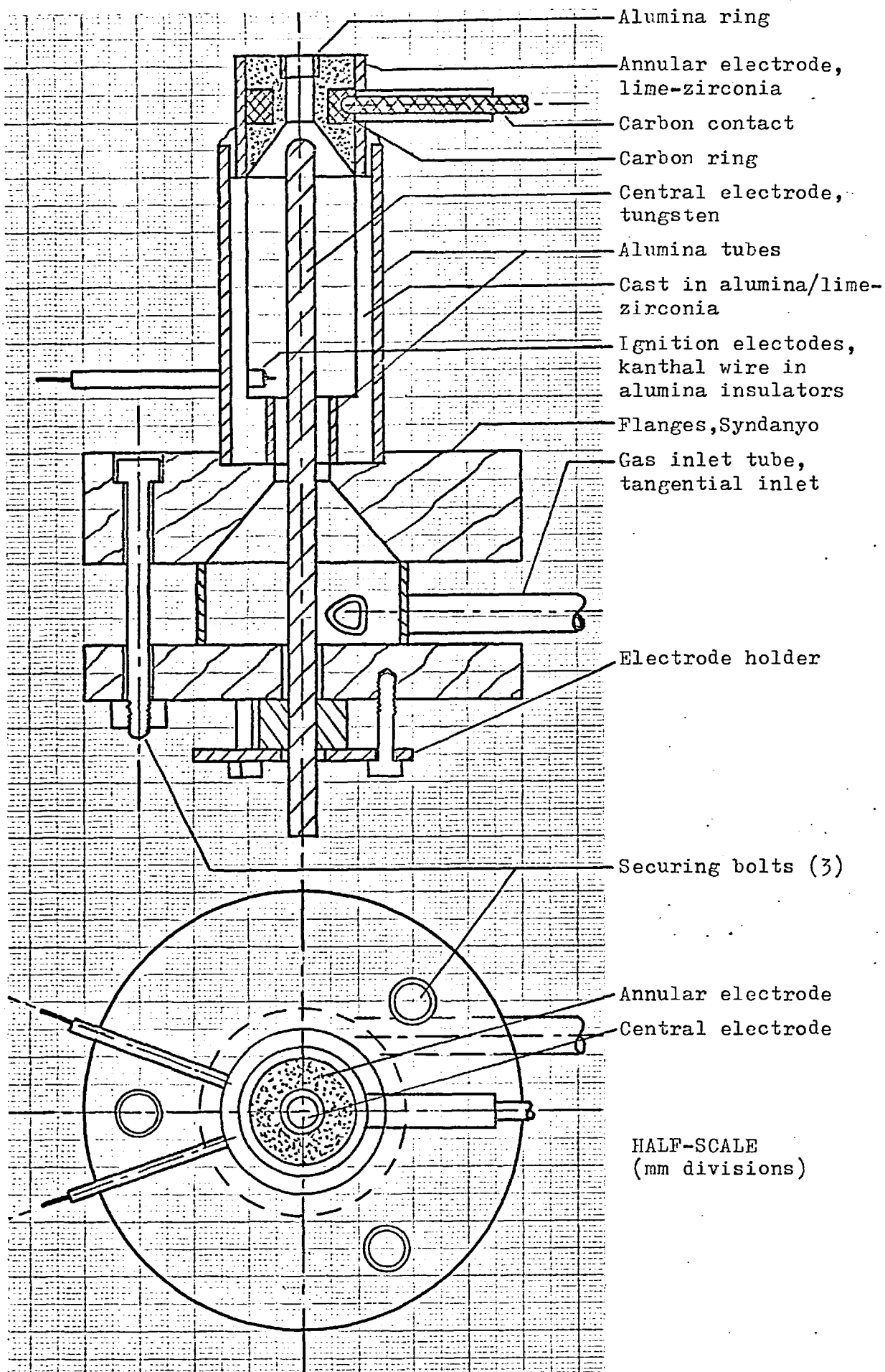


FIGURE IV 2 - LARGE VORTEX-STABILIZED BURNER,
SECTIONAL ELEVATION AND PLAN

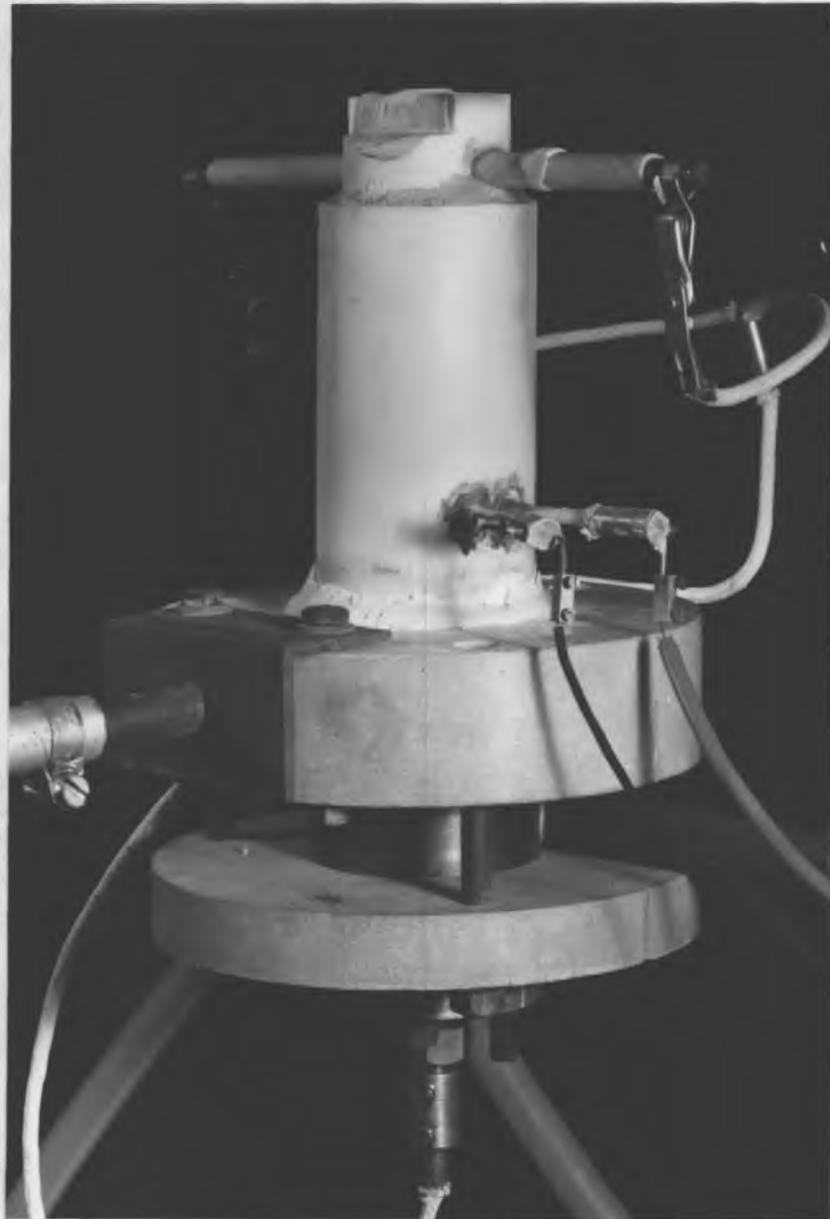


PLATE IV 2 - THE LARGE VORTEX-STABILIZED BURNER
(SEE FIGURE IV 2 FOR DETAILS OF CONSTRUCTION)

IV 1(b)

overcome by using an alumina tube of larger diameter which was lined with a refractory. The lining was cast from a 50:50 mixture of alumina cement and lime-zirconia cement because this material was found to have greater strength than either of its constituents alone.

(ii) Electrodes.

The platinum wire contacts used in the electrodes of the small burner were found to be unsatisfactory because they melted and disappeared into the pores of the refractory. This problem was solved for the annular electrode by using a ring machined from graphite to make electrical contact with the zirconia as shown in figure IV 2. Two graphite rods were fitted into holes on opposite sides of the graphite ring and passed through corresponding slots cut in the wall of the alumina tube which surrounded the electrode. Both rods were connected to the earthed side of the transducer power supply (see page 80) and were enclosed in ceramic tubing to ensure that the rate of oxidation from the hot surfaces of the graphite was kept at a low level.

The annular electrode was cemented in position on top of the combustion chamber, unlike the annular electrode of the small burner. The reasons for this and for replacing the ceramic central electrode by a tungsten rod are given on page 101. The upper end of the rod was ground to a rounded profile prior to its installation in the burner in order to provide a central point of attachment for the arc.

(iii) Ignition system.

Two holes were drilled radially through the wall of the combustion chamber to accommodate the electrodes for the spark ignition system. These consisted of 16 s.w.g. Kanthal wires held in alumina tubes which were positioned in such a way that the exposed tips of the wires were separated by 4 to 5 mm and were located just above the entrance to the combustion chamber. Their positioning was found to be critical to provide easy ignition and is shown in figure IV 2. The circuitry required to provide the spark is described in section 2 (c) below.

2. ELECTRICAL POWER SUPPLIES.

Two different sizes of electrical power supply were required by the burners described above and in section V1. One was of about 0.5 kVA output which supplied power to the simple tube burner, the sudden enlargement burner, and the small vortex-stabilized burner, whilst the other provided an output of up to 66 kVA and was used as a supply for the large vortex-stabilized burner. Descriptions of these power supplies are given below.

Also described below is the high tension spark ignition system used in the large vortex-stabilized burner (see section I (b)(iii) above).

(a) 0.5 kVA supply.

The circuit diagram of this supply unit is given in figure IV 3. Two mains voltage transformers were available, each rated at 0.25 kVA and equipped with a range of output voltages up to 800-0-800 V. The primary windings of the transformers were connected in parallel and the secondary windings were connected in series to give an output of up to 3.2 kV from a mains input of 240 V. The output could be connected directly to the electrodes of the burner to provide an a.c. arc, or a d.c. arc could be obtained by passing the output from the transformers through a full-wave rectifier and smoothing circuit. Instruments were provided in the circuitry to measure the current, voltage and power supplied to the burner. Switching from a.c. to d.c. output was achieved by connecting the appropriate terminals when the power supply was disconnected from the mains.

The first rectifier consisted of a bridge network of one solid state diode, rated at 1 A and 800 V, in each arm and was therefore limited to an operating voltage of 1.6 kV. The rectifier was later modified to operate up to 4.8 kV. by incorporating three of these diodes in series in each arm of the bridge. A resistor of 390 k Ω and a capacitor of 0.004 μ F were placed in parallel with each diode to protect it from surges produced by the transformers. Two capacitors of 1 μ F each were placed in parallel with the rectifier output and were separated by a resistor of 100 Ω to provide a smoothing circuit. An earth leakage resistance of 27.2 M Ω was used to discharge the capacitors when the power supply was disconnected from the mains.

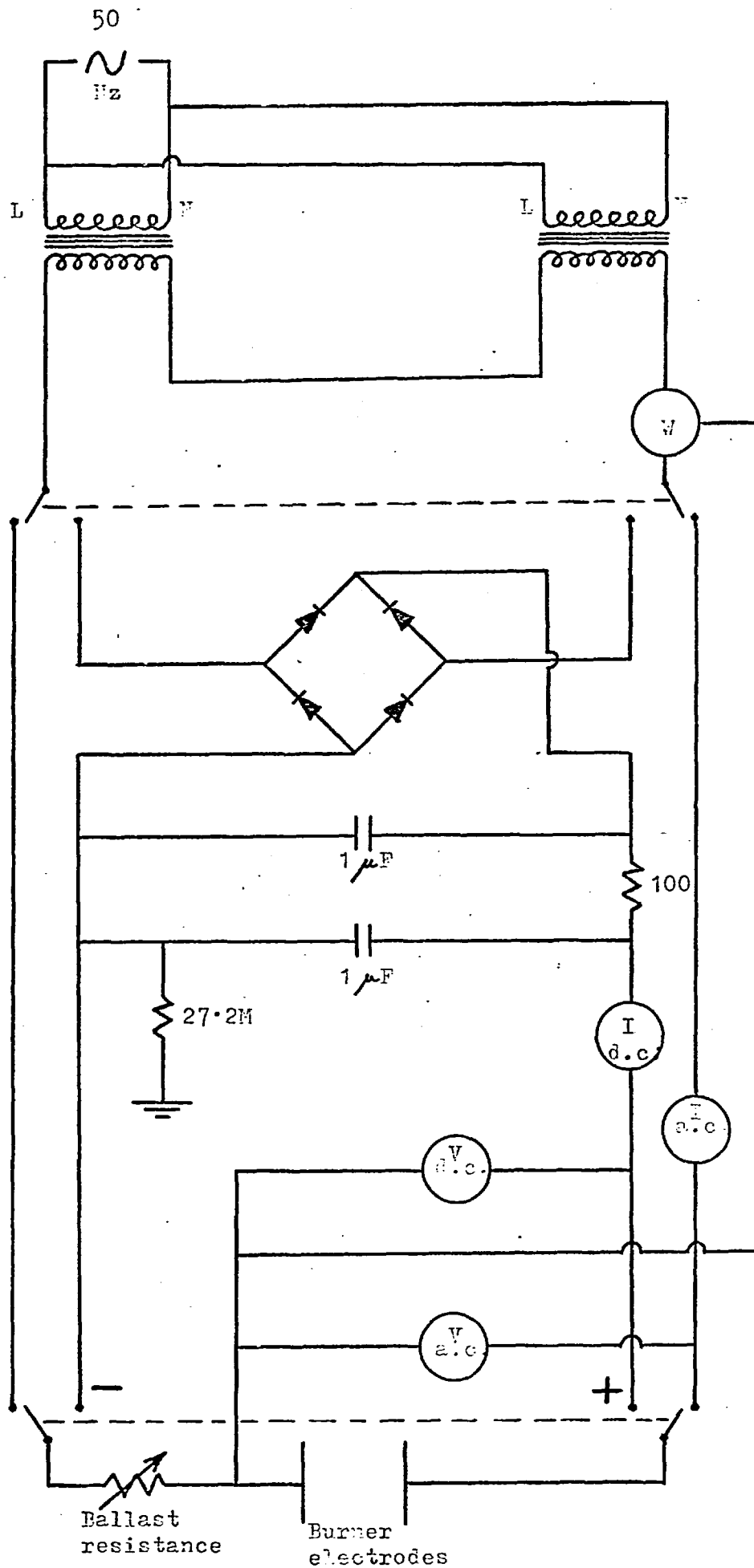


FIGURE IV 3 - 0.5 kVA POWER SUPPLY.

A ballast resistance was placed in series with the burner electrodes in order to limit the current supplied to the arc; the necessity for this procedure is explained in section II 3 (b)(iv). The resistance comprised a bank of 6 wire-wound resistors, of 2 k Ω each, which could be wired in any series-parallel order by a patchboard and connecting wires.

(b) 66 kVA supply.

The Foster transductor which was used to supply power to the large vortex-stabilized burner consisted of a single phase transformer, providing a controlled current at voltages up to 6.6 kV. The current could be set at any value between 0 and 10 A and would remain at this value even if the transformer output was short-circuited, an essential characteristic of a supply for electric arcs (section II 3(b)(iv)).

The transductor and burner were enclosed in an earthed cage which was constructed from steel mesh welded to a framework of steel angle. The door of the cage was interlocked with the mains supply in such a way that the operator was prevented from gaining access to the high voltage side of the equipment when the supply was switched on. The output terminal which was connected to the annular electrode of the burner was also earthed. The other terminal was connected to the central electrode.

A photograph of the transductor, earthed cage and large burner appears as plate IV 3 and a plan is shown in figure IV 4

(c) Spark ignition system.

The electrodes which were used to ignite the mixture in the combustion chamber of the large burner (see section IV 1 (b)) were connected to circuitry identical to the 12 V coil ignition system of a motor car, except that the battery was replaced by a 12 V transformer and rectifier. A press button switch was used to discharge a 0.1 μ F capacitor through the low tension side of a standard ignition coil. The high tension side of the coil was connected to one of the ignition electrodes in the burner and the other electrode was earthed.

3. GAS AND AIR SUPPLIES.

Methane was used as the fuel gas for the burners and was supplied by Mogden Sewage Treatment Works to the approximate



PLATE IV 3 - THE 66 KVA TRANSDUCTOR, EARTHED CAGE AND THE
LARGE VORTEX-STABILIZED BURNER

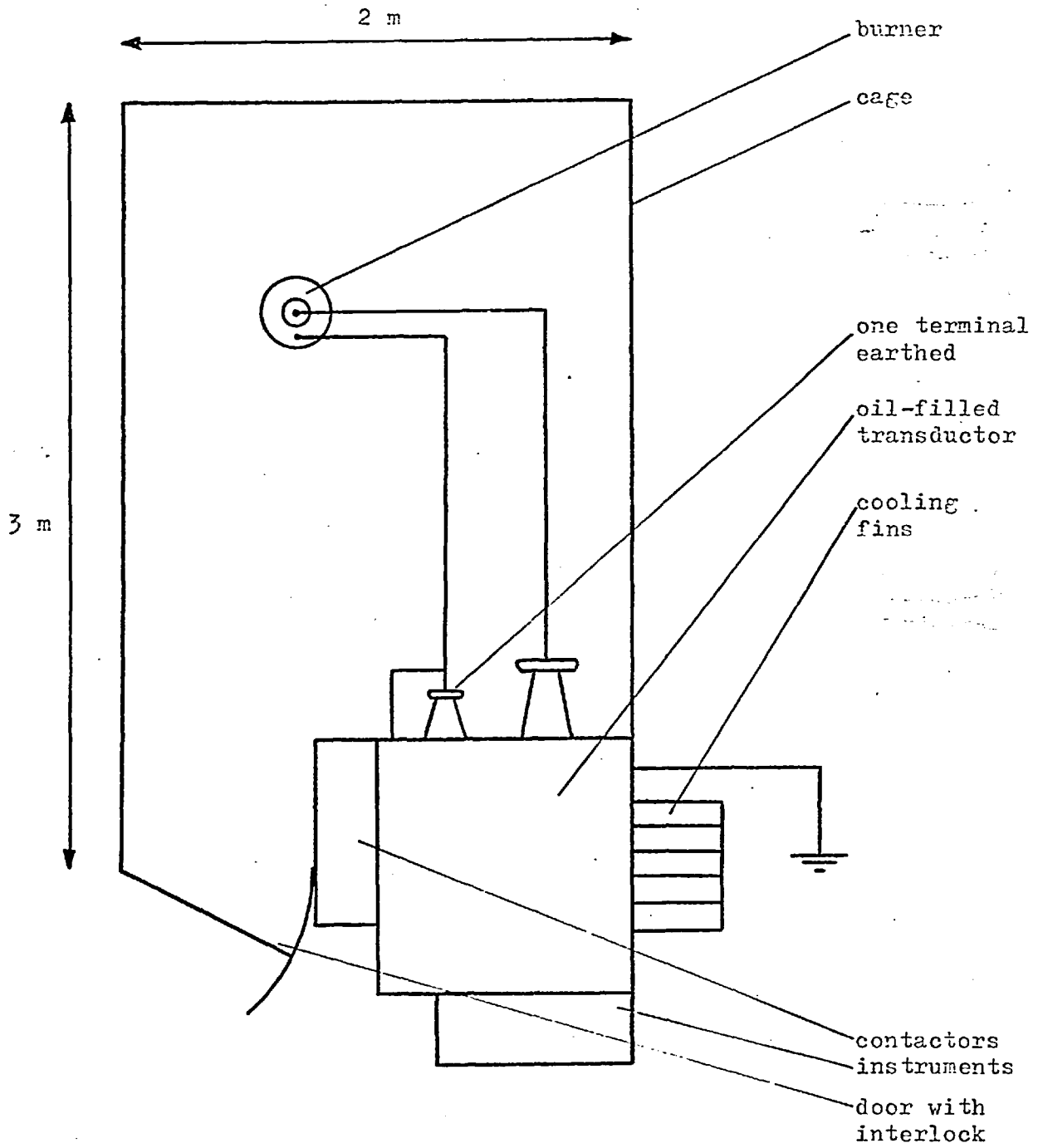


FIGURE IV 4 - PLAN OF TRANSDUCTOR, CAGE AND BURNER.

IV 2(b)

analysis:-

	%
CH ₄	97
CO ₂	3-4
O ₂	0.1-0.2
N ₂	0.2-0.4

The method of supplying air and methane depended on the size of burner in operation.

All the small burners were supplied by the same system. These burners are described in section V 1 and section IV 1 (a). Air from the compressed air line in the laboratory and methane from a cylinder were individually metered and then mixed in a column which was packed with silica-gel. The silica-gel dried the gases, particularly the air supplied by the compressor, and the gas mixture then passed into the burner.

The large burner (see section IV 1 (b)) was supplied with air by a centrifugal blower. The gas system had to be designed so that the total pressure drop did not exceed the pressure head developed by the blower. As a result of this condition the packed bed mixer was replaced by a device with a low pressure drop. The methane was introduced into the air-stream at a T-piece which was situated well down-stream of the burner. Mixing was assumed to occur in the pipe between the T-piece and the burner and in the swirl chamber of the burner.

Air and methane were metered using variable area flow meters such as Rotameters and Meterate tubes. All the burners were equipped with flame arrestors to prevent flash-back along the gas supply line. These flame arrestors were situated in the gas inlet pipe close to the burner and consisted of strips of copper gauze which were tightly coiled to fit the gas inlet pipes.

4. CALORIMETERS.

Various types of water-cooled calorimeters have been used to determine heat fluxes from the burners outlined in section V 1. A description of these calorimeters appears below but their use is given in section V 2.

(a) Tube calorimeter.

The water-cooled tube which was used both as an electrode and as a calorimeter for the sudden enlargement and small

vortex-stabilized burners (see sections IV 1(b) and (c)) is shown in figure IV 5. The calorimeter was designed to lie horizontally in the flame and comprised a narrow bore copper tube which was equipped with thermocouples at either end. Larger bore copper tubes, in the shape of right-angled elbows, were brazed to the ends of the narrow bore tube. The thermocouples were passed through holes in the walls of the elbows and were brazed in the position shown in the figure. Chromel-alumel thermocouples contained in a mineral-insulated sheath of stainless steel were used. The two negative leads of the thermocouples were joined together and the positive leads were connected to the terminals of a potentiometric strip chart recorder. This arrangement formed a differential thermocouple which enabled a direct reading of the temperature rise of the cooling water to be made.

A closed circuit was used for the cooling water. Distilled water was pumped from a reservoir of 20 litres capacity through a control valve, a variable area flow meter, the calorimeter and a heat exchanger before returning to the reservoir. Tap water was used as the coolant in the heat exchanger.

The tube calorimeter could be traversed in a horizontal plane in the flame by means of a sliding carriage which was controlled by a screw.

(b) Concentric calorimeters.

The distribution of heat flux from the small vortex-stabilized burner (see section IV 1 (a)) to a water-cooled flat plate was determined using a series of water-cooled circular plates. Figure IV 6 shows a typical plate which has a diameter of 50 mm. and lies inside a water-cooled guard ring. The surface of the plate calorimeter lies flush with the surface of the guard ring and the two surfaces are thermally insulated from each other with fuller's earth. Water enters the calorimeter through a nozzle to ensure turbulent flow over the plate and leaves by a tube of larger diameter. The thermocouples in the water inlet and outlet pipes were made from copper-constantan wire covered with fibreglass insulation. Araldite was used to seal the thermocouple wire to the walls of the pipes. The leads were connected to form a differential thermocouple as described in section (a) above.

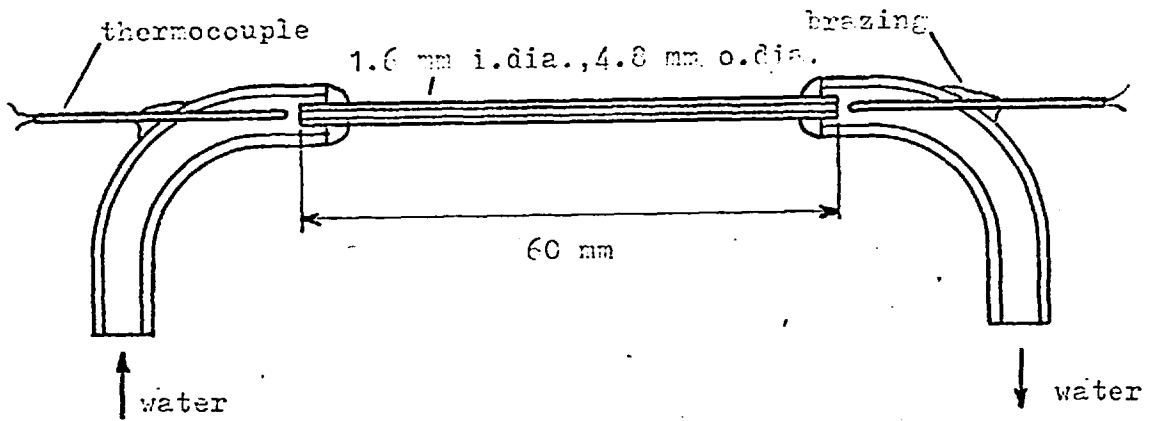


FIGURE IV 5 - TUBE CALORIMETER

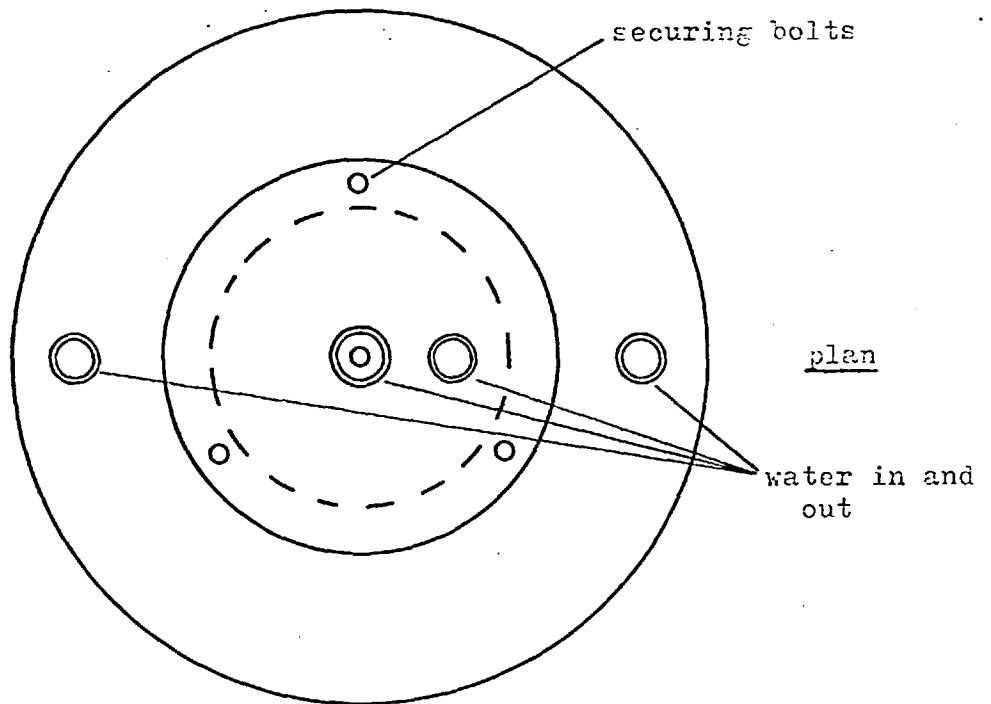
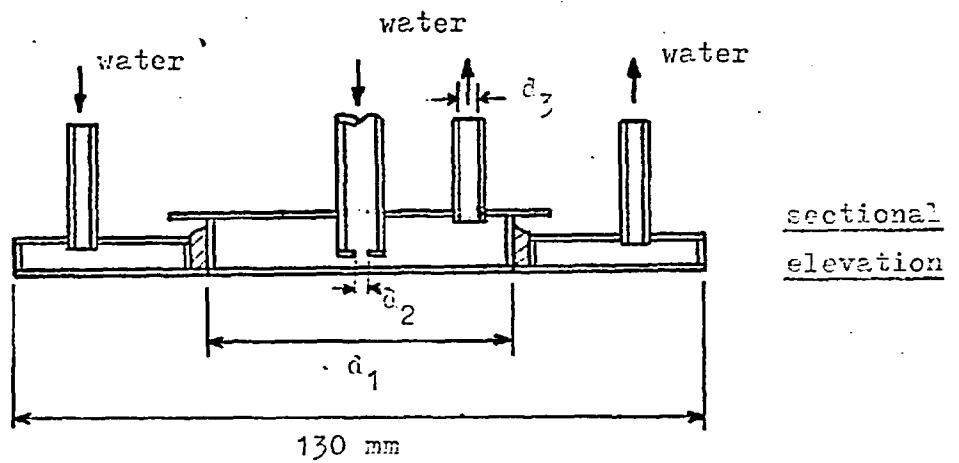


FIGURE IV 6 - TYPICAL CONCENTRIC CALORIMETER

The calorimeter and guard ring were made from copper with soldered joints.

The diameters of the calorimeters and their corresponding inlet nozzle and outlet tube diameters are given in table IV 1. The outer diameter of all the guard rings was 130 mm. but the inner diameter varied, being just sufficient to accommodate the parent calorimeter. The largest calorimeter had a diameter of 130 mm and did not require a guard ring. It was equipped with 6 water outlets, equispaced on its top surface, to ensure uniform distribution of water over the surface of the plate. The two smallest calorimeters had their outlets positioned at the side of the calorimeter because there was insufficient room at the top.

Dia. of calorimeter d_1 mm	Dia. of nozzle d_2 mm	Dia. of outlet d_3 mm	Remarks. (see figure <u>IV</u> 6)
6	0.5	1.6	Side outlet
13	0.8	1.6	" "
25	1.2	3.2	As in figure <u>IV</u> 6
50	1.6	3.2	" " " "
130	2.5	6 x 1.6	No guard ring Six outlets.

Table IV 1.

The water supply to the calorimeters was identical to that used for the tube calorimeter described in section (a) above. Tap water was metered and then passed through the guard rings.

(c) Split plate calorimeter.

A split plate calorimeter was used to determine the distribution of heat flux from the large vortex-stabilized burner detailed in section IV 1 (b). This calorimeter was constructed from two identical water-cooled plates, each 25 cm by 25 cm in area, which were placed edge-to-edge and separated by a layer of fiberfrax about 0.2 mm thick. Figure IV 7 shows one plate in detail with part of the other plate adjacent to it. Each plate was made from two sheets of copper, the lower sheet having been milled to leave space for the cooling

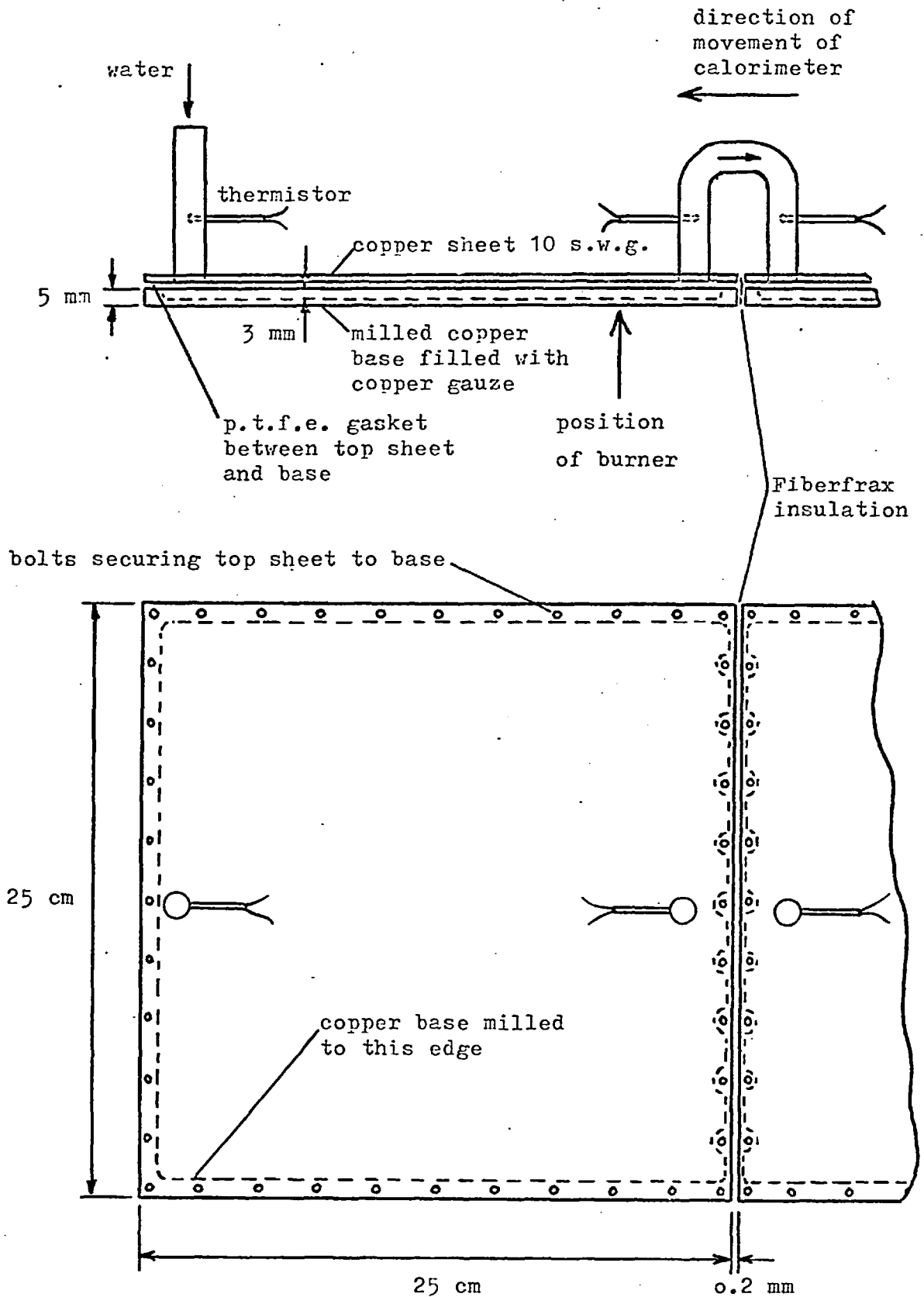


FIGURE IV 7 - SPLIT-PLATE CALORIMETER

water whilst the top sheet was fitted with the inlet and outlet tubes for the cooling water. The space which was left by the milling operation on the lower sheet was filled with copper gauze and then the two sheets were bolted together, using PTFE cord as a gasket.

A thermistor was placed in each of the water inlet and outlet tubes in order to determine the rise in temperature of the cooling water passing over each plate. Four matching thermistors were chosen with the same temperature-resistance characteristics which allowed the circuitry shown in figure IV 8 to be used. The voltage supplied to the two Wheatstone's bridges was provided by a dry cell which was stabilized with a zener diode. The current in the circuit was limited by a series resistance which could be varied for trimming purposes. Each bridge consisted of two thermistors in adjacent arms and matched resistors in the other arms separated by a low value potentiometer for trimming the circuit. Matched resistors could be switched into each bridge to replace a thermistor, allowing a voltage to be measured with only one thermistor in circuit. This voltage was indicated on a digital voltmeter.

The water supply to the calorimeter was similar to that described in section (a) above but was on a larger scale to accommodate higher flow rates.

A gantry was constructed over the burner to support a pair of rails. The calorimeter was suspended from these rails and could be traversed horizontally across the flame. A shaded pole motor and gearbox driving a sprocket at 1 r.p.m. were attached to one end of the gantry. A chain was passed from the calorimeter to the sprocket and the calorimeter was drawn along the rails as the sprocket rotated. The calorimeter could also be advanced for a distance of 2 mm along the rails by controlling the motor from a pinwheel which rotated with the drive sprocket. A microswitch made contact with the pins on the wheel and actuated a relay. The pin wheel rotated until a pin made contact with the microswitch which caused the relay to stop the motor. The motor could not be started again until a switch outside the earthed cage was manually depressed.

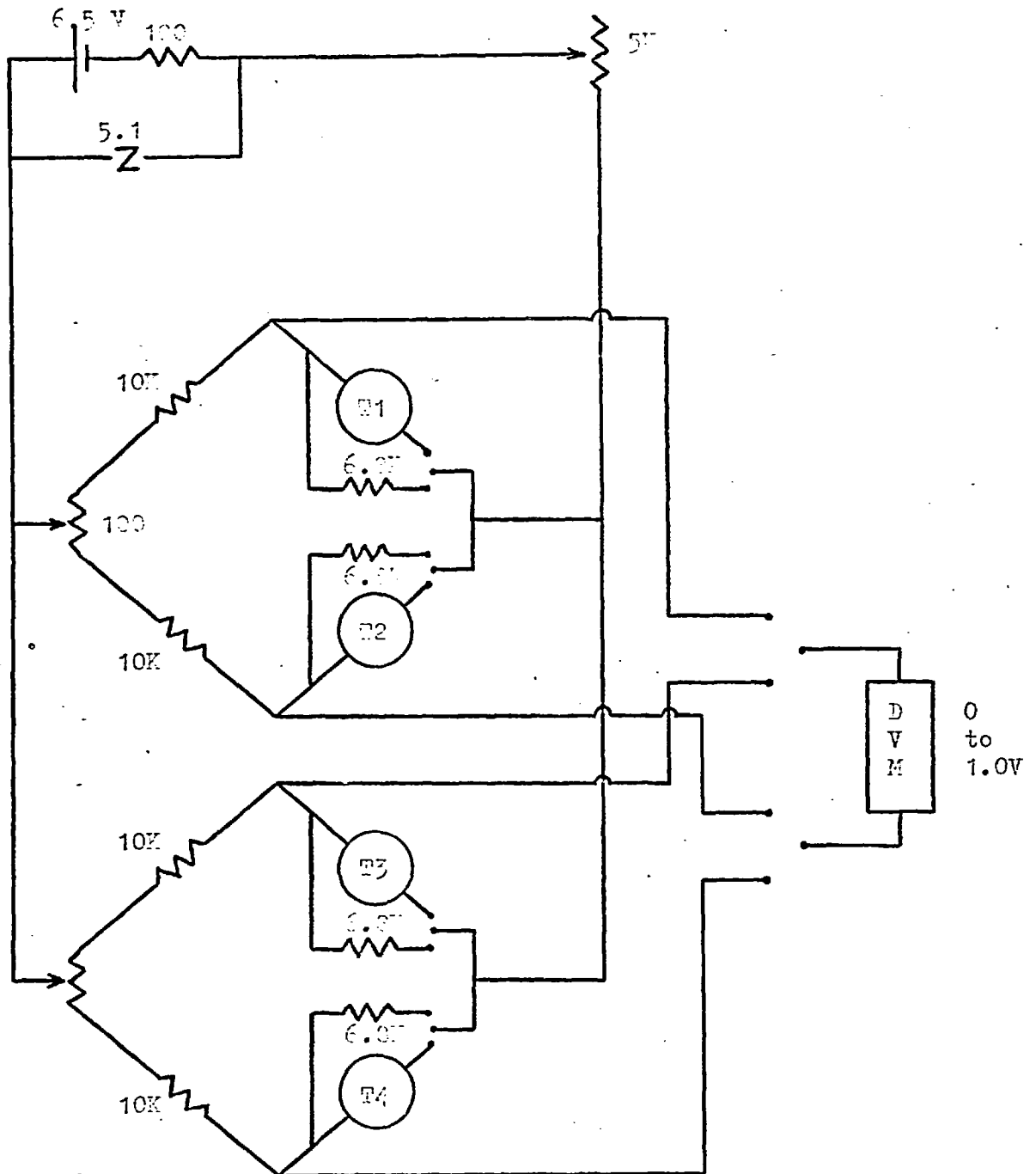


FIGURE IV 8-CIRCUIT DIAGRAM FOR THERMISTORS IN
SPLIT PLATE CALORIMETER

5. STEEL SLABS.

Steel slabs of En 8 were passed over the large vortex-stabilized burner at various known speeds for two purposes. The first was to obtain temperature profiles in the depth of the slab and the second was to flame harden one surface of the slab. A slab was traversed over the burner by the same equipment which was used to advance the split plate calorimeter (see section IV 4 (c)). The drive sprocket was changed when another speed was required.

(a) Slabs for determining temperature profiles.

Mineral insulated, chromel-alumel thermocouples sheathed in stainless steel of 0.5 mm external diameter were used to measure temperatures inside the slab when one surface was heated by the burner, as shown schematically in figure IV 9. The thermocouples had been constructed with their junctions bonded to the ends of the sheaths in order to ensure quick response. Typical positions for the thermocouples in the side of the slab are shown in figure IV 9 and 10. The slabs were placed between two pieces of steel as shown in figure IV 10 and were insulated from them by Fiberfrax paper. The exposed sides of the slabs were insulated by Kaowool.

A slab was also prepared in which the thermocouples were placed in an element of the slab, as shown in figure IV 11. This element was insulated from the rest of the slab by Fiberfrax paper.

(b) Slabs for flame hardening.

These slabs were prepared both with thermocouples, as described in (a) above, and without thermocouples. The general arrangement of a slab for flame hardening is shown in figure IV 9. A quench, yet to be described, was positioned adjacent to the burner. Flame hardening was not carried out on the slab which incorporated an insulated element.

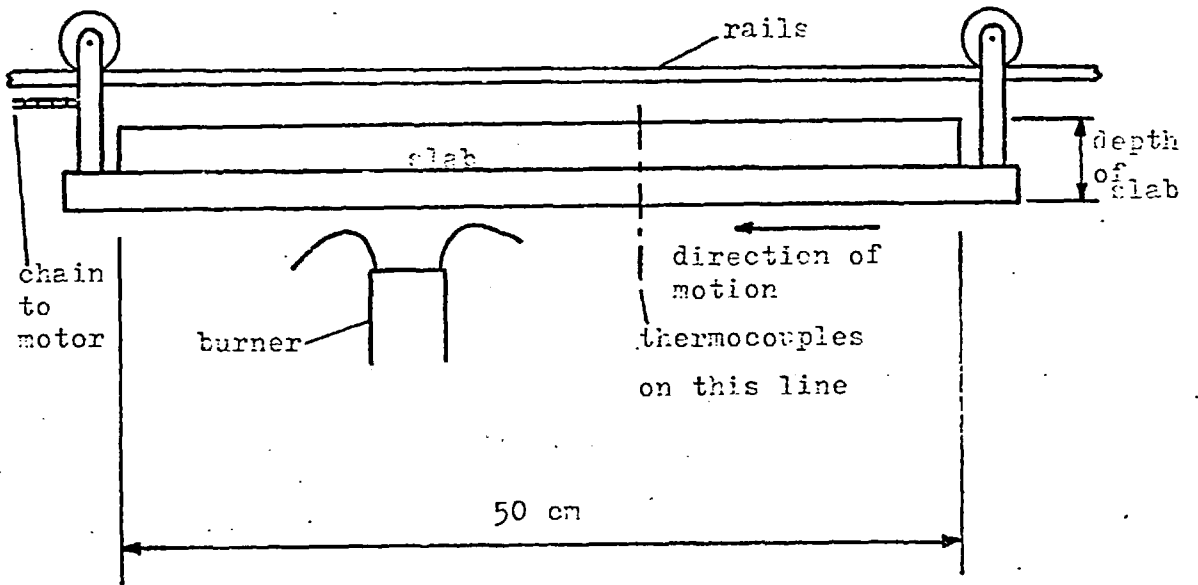


FIGURE IV 9 - ARRANGEMENT OF SLAB OVER THE BURNER

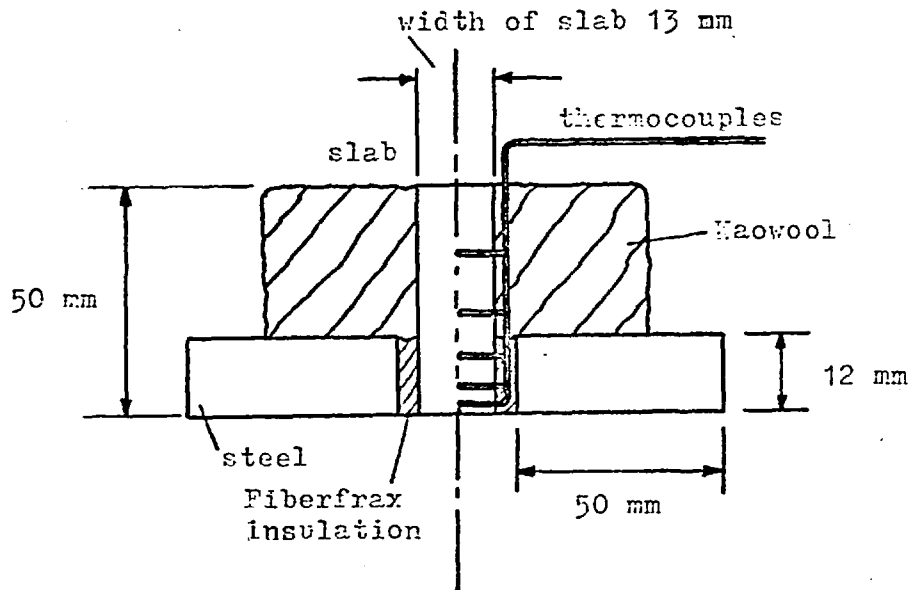


FIGURE IV 10 - CROSS SECTION SHOWING A SLAB, THERMOCOUPLES AND INSULATION

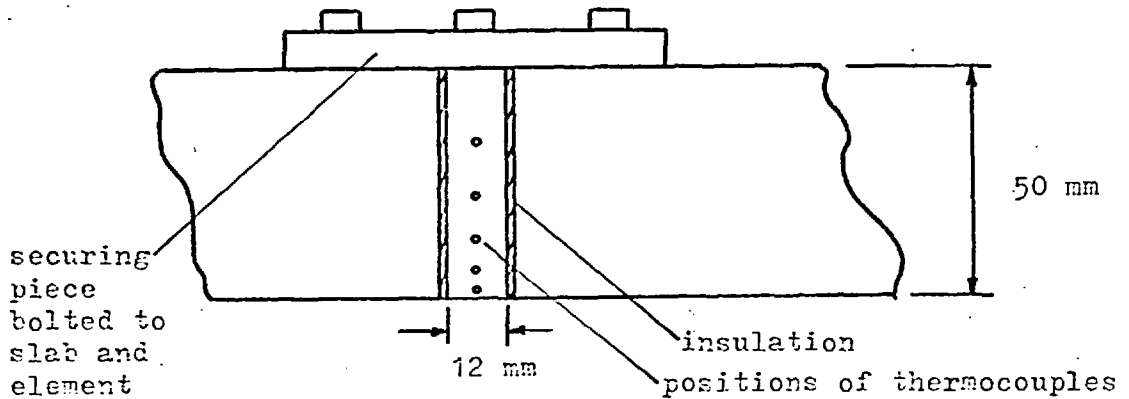
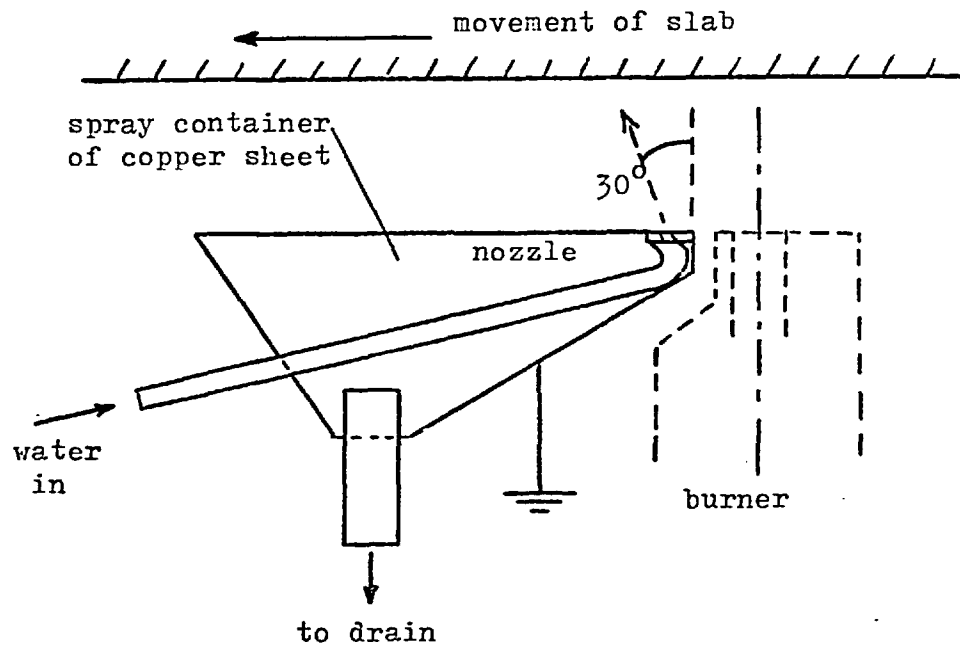


FIGURE IV 11 - VIEW OF A SLAB SHOWING POSITIONS OF THERMOCOUPLES IN INSULATED ELEMENT

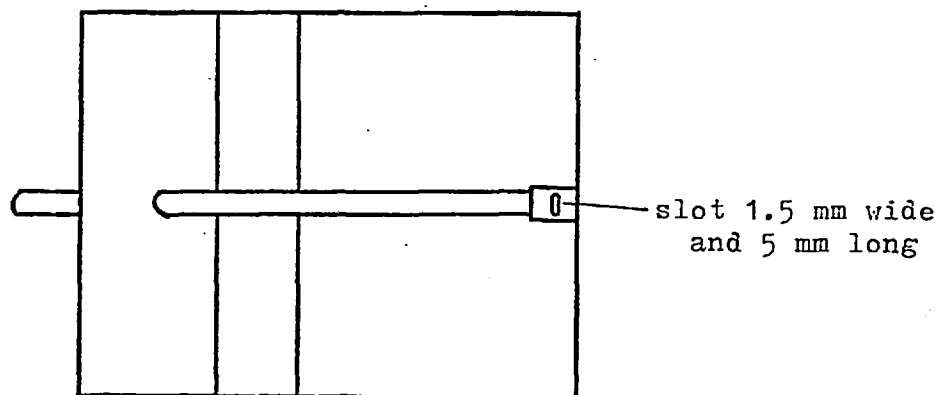
6. THE QUENCH

The surfaces of slabs of E8 were heated by passing them over the large vortex-stabilized burner and were then quenched by a water spray which was positioned adjacent to the burner. The general arrangement of the quench and the burner which were used to flame harden the surfaces of the slabs is shown in figure IV 12 and plate IV 4.

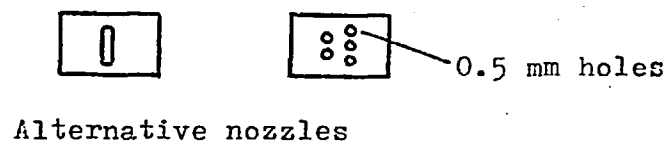
Water was pumped at a controlled and metered rate from a constant head supply to the quenching nozzle. The nozzle was surrounded by a tray which collected most of the quenching water. The burner was cut away to accommodate the tray when the nozzle was positioned close to the burner. The distance between the burner axis and the nozzle could be varied from 2.5 cm to 15 cm. Two types of nozzle were used, one being a slot 5 mm by 1.5 mm which had been milled at an angle of 20° in a piece of 10 s.w.g. copper sheet. The slot was angled at 20° to the vertical in an attempt at preventing water from entering the flame. The other nozzle comprised a number of 0.5 mm diameter holes which had been drilled vertically through a piece of 10 s.w.g. copper. Both nozzles are shown in detail in figure IV 12 and were attached to the end of the water inlet pipe by solder.



Sectional elevation of quench (about half size)



Plan of quench (about half size)



Alternative nozzles

FIGURE IV 12 - ARRANGEMENT OF THE QUENCH

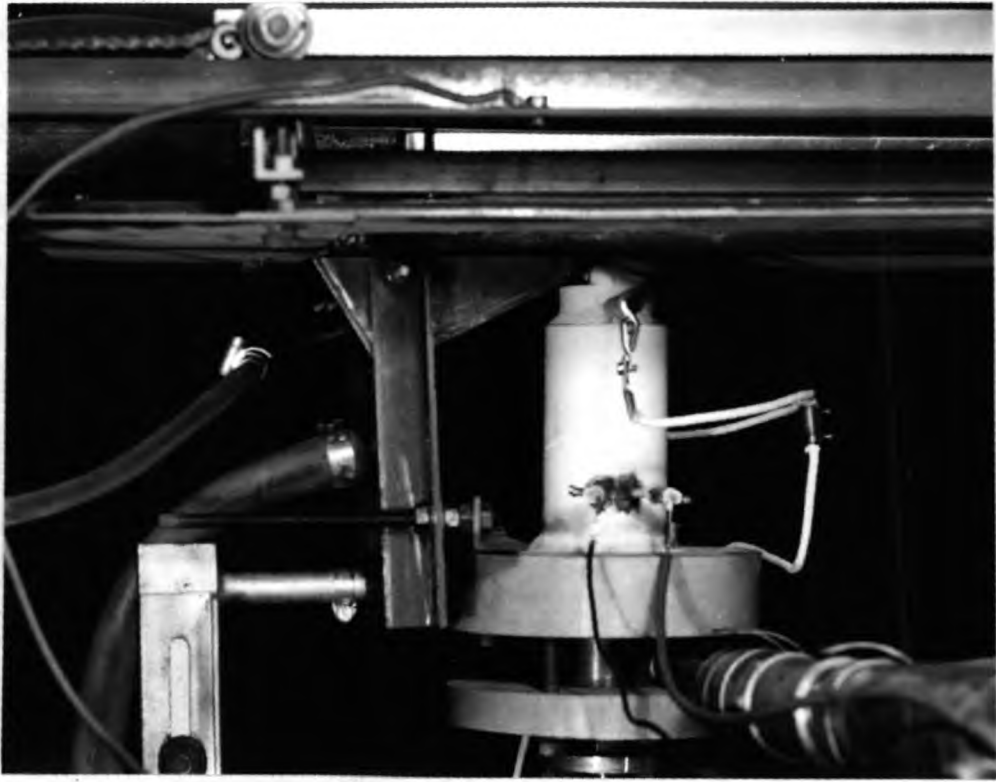


PLATE IV 4 - TROLLEY CONTAINING A SLAB MOVING OVER THE
BURNER AND QUENCH

V EXPERIMENTAL TECHNIQUES.

1. BURNER DEVELOPMENT.

(a). Simple tube burner

An electrically augmented flame was first produced using the burner and system of electrodes shown in figure V 1. A stream of metered and pre-mixed methane and air was passed through the burner and ignited. The burner comprised a copper tube to which had been glued a glass tube equipped with a horizontal arm. The side arm was filled with potassium carbonate powder and a wire "pusher" was used to introduce small quantities of the powder into the gas stream. The potassium carbonate was carried into the flame to act as seed (see page 13). A flame length of 14 cms. was observed when the rates of flow of air and methane were 9.7 and 1.2 litres per minute respectively.

The copper tube also served as the cathode, and a flat copper plate which could be positioned at a variable height above the burner was used as the anode. A d.c. potential of 800 volts was applied across the electrode from the power supply described on page 78. An arc discharge was initiated when the flame was seeded and the electrodes were set 14 cms. apart, or less. The arc was maintained by ensuring that the flame was continually seeded. Figure V 2 shows how the arc filament followed the boundary of the flame. The filament was attached to a fixed spot on the rim of the burner tube and to a moving spot on the flat copper plate. When the polarities of the electrodes were reversed, larger quantities of seed were required to initiate an arc. The arc was less stable under the reversed polarity and could not be initiated if the distance between the electrodes exceeded 12.5 cms.

Further work was carried out on the simple tube burner using the existing anode but different cathodes of various shapes and materials were inserted into the flame between the burner tube and the anode, as shown in figure V 3. Cathodes in the form of rods of copper, tungsten, nickel and stainless steel provided stable arc discharges. The arc was always attached to the hottest part of the cathode which was the region where the flame boundary impinged on the rod.

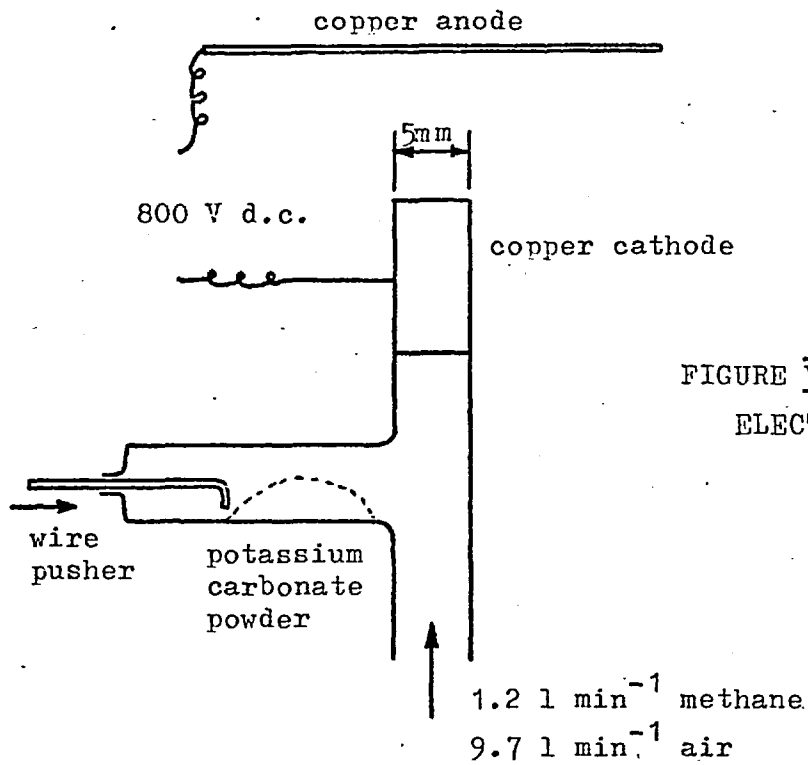


FIGURE V 1 - SIMPLE
ELECTRICALLY AUGMENTED
BURNER

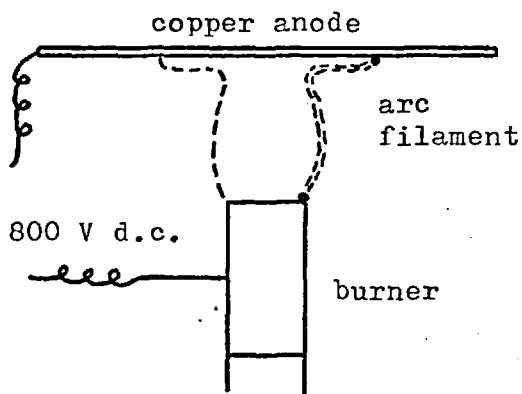


FIGURE V 2 - ARC FOLLOWING
BOUNDARY OF FLAME

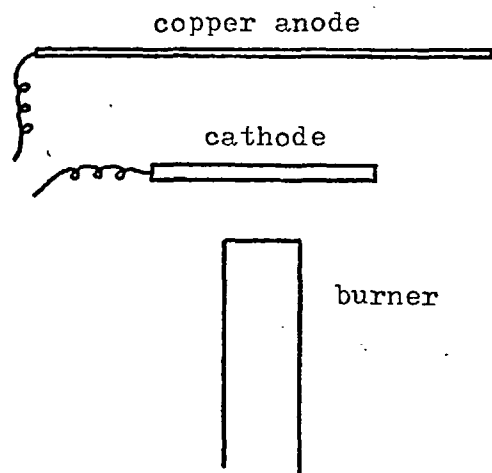
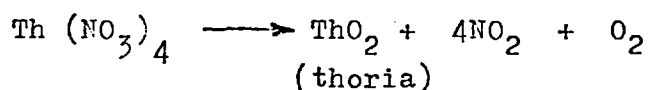


FIGURE V 3 - POSITION OF
CATHODES OF VARIOUS
MATERIALS AND
VARIOUS SHAPES

An attempt was made to increase the electron emission from a stainless steel cathode by coating it with thorium. Thorium has a low work function (see pages 31 and 32) and has the additional advantage of being very refractory. The stainless steel rod was dipped in a solution of thorium nitrate and, on removal from the solution, crystals of nitrate were formed on the surface of the rod. When the rod was placed in the flame, the nitrate was expected to be calcined to thorium according to the reaction:-



The experiment was inconclusive because the coating disappeared quickly once the arc had been initiated.

Carbon, in the form of rods of dense graphite, was then used for the electrodes. The arc was found to be unstable with a carbon rod as the cathode and the existing copper anode but stability was regained by using either carbon for both electrodes or a metal cathode and a carbon anode. The arc obtained with carbon electrodes was unique in that a large number of electrode spots or points of attachment, all of small size, appeared on the surfaces of the electrodes.

All the electrode materials described so far in this section suffered from rapid erosion and, in order to overcome this problem, stabilized zirconia was tried. It has a low work function and is an electronic conductor at flame temperatures (see page 31). Cathodes were constructed from tubes of either lime - or yttria - stabilized zirconia by cementing platinum wires to their inner surfaces. The platinum wire could then make electrical contact with the hot region of the zirconia tube. No arc could be initiated when these cathodes were inserted in the flame. It was believed that the zirconia was not hot enough to conduct and an attempt was made to raise the cathode temperature by increasing the gas flow rate through the burner. This required a pilot light adjacent to the burner to prevent the flame lifting off the burner rim (see page 21). By this means the methane and air flow rates were increased to 1.6 and 10 litres per minute but still no arc could be initiated.

(b). Sudden enlargement burner.

A burner with a sudden increase in diameter at its mouth was developed to give more intense combustion than is possible with a simple tube burner, as described on page 24 . The burner geometry is shown in figure V 4. The diameter of the mouth was fixed at 13 mm. and the depth of the enlarged section m_1 and the diameter of the inlet tube m_2 were varied until 1 litre per minute of methane could be burnt with the stoichiometric quantity of pre-mixed air, viz. 10 litres per minute, without blow-off or blow-back of the flame. Satisfactory values of m_1 and m_2 were found to be 5 mm. and 7 mm. respectively. A stable arc was obtained when the zirconia cathode and copper anode, used previously with the simple tube burner, were positioned 2 cms. apart, above the flame from the sudden enlargement burner. A higher potential of 3200 volts was applied across the electrodes from a re-designed power supply (see page 78) and, as a result, seeding was only required to initiate the arc. A stable arc could then be maintained without further seeding. When the zirconia cathode was replaced by a metal, continual seeding was required to maintain the arc.

A sudden enlargement burner was constructed from stabilized lime-zirconia as shown in figure V 5. The burner rim served as the cathode and electrical contact was made by incorporating platinum wire in the zirconia casting. The cathode was mounted on an alumina tube to insulate it from the rest of the burner. The dimensions of this sudden enlargement burner were the same as in figure V 4, except that m_1 was increased from 5 mm. to 25 mm. to ensure that the burner rim was raised to a high temperature by the flame which was stabilized inside the burner mouth. Either a copper plate or a water-cooled copper tube was used for an anode. Seeding was only required to initiate the arc which formed at a hot spot on the burner/cathode rim. When the polarity of the applied potential was reversed or an a.c. potential of the same magnitude was used, the flame required continuous seeding to maintain a stable arc.

(c). Vortex-stabilized burner.

The sudden enlargement burner suffered from the disadvantage that the arc was stabilized in one place on the rim of the burner with consequent erosion of the refractory at this spot. The asymmetric position of the arc would also

be likely to limit the burner's usefulness as a heater, particularly when the point of attachment of the arc could be anywhere on the rim and could not be predicted.

This inherent asymmetry was overcome by stabilizing the arc on a central cathode of zirconia which was heated in a vortex-stabilized burner (see page 25). Figure V 6 shows how the electrode is positioned in the burner. The construction of the burner is described on page 71. The diameter of the combustion chamber was set at 13 mm., as in the sudden enlargement burner above, and the dimensions p_1 , p_2 , p_3 and p_4 were chosen so that the tip of the central electrode was raised to yellow heat by the hot combustion products. Suitable values of p were found to be:-

$$p_1 = 30 \text{ mm.}, \quad p_2 = 8 \text{ mm.}, \quad p_3 = 4 \text{ mm.}, \quad p_4 = 17 \text{ mm.}$$

The mixture was ignited in the burner mouth and a few minutes' heating were required before the cathode reached yellow heat. A water-cooled copper tube was used as an anode and was positioned in the flame above the cathode as shown in figure V 6. When the distance between the two electrodes was restricted to 2 cm., or less, and a d.c. potential of 3200 volts was applied, a stable arc was both initiated and maintained without the use of seed. No seeding was required when an a.c. potential of 3000 volts was applied across the electrodes. An oscilloscope showed that the a.c. arc only existed during every alternate half-cycle (see page 115). When the water-cooled copper anode was replaced by a zirconia anode of similar construction to the cathode, an a.c. arc was produced during every half-cycle.

On the basis of these observations, a non-seeded a.c. system was designed by incorporating a lime-zirconia annular anode or nozzle in the burner mouth as shown in figure V 7. The construction is detailed elsewhere (see page 71). The gas mixture, comprising 1 litre per minute of methane and at least 10 litres per minute of air, was ignited with the anode removed. When the flame had become stable in the combustion chamber, the anode was replaced and a few minutes were allowed for the electrodes to become hot enough to conduct electricity. The supply of 3000 volts a.c. was switched on across the electrodes and the arc was initiated. The arc could be switched on and off repeatedly without damaging the electrodes. With the power

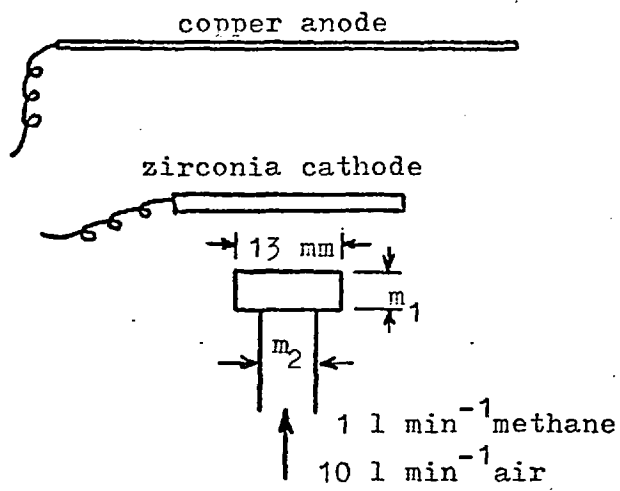


FIGURE V 4

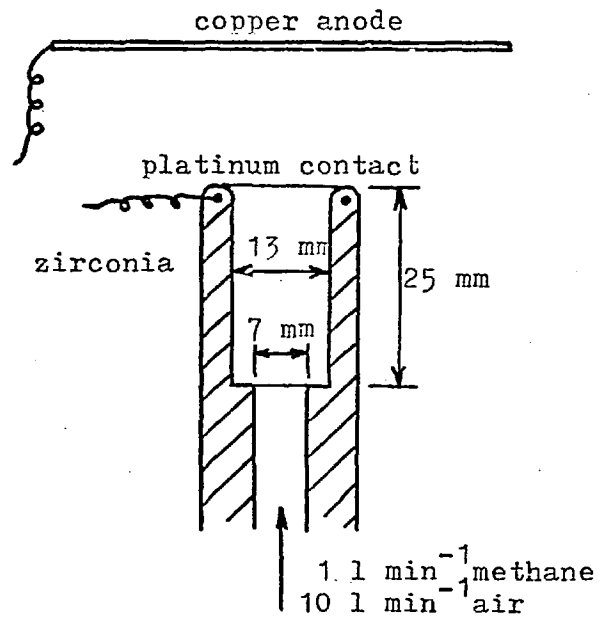


FIGURE V 5

SUDDEN ENLARGEMENT BURNERS

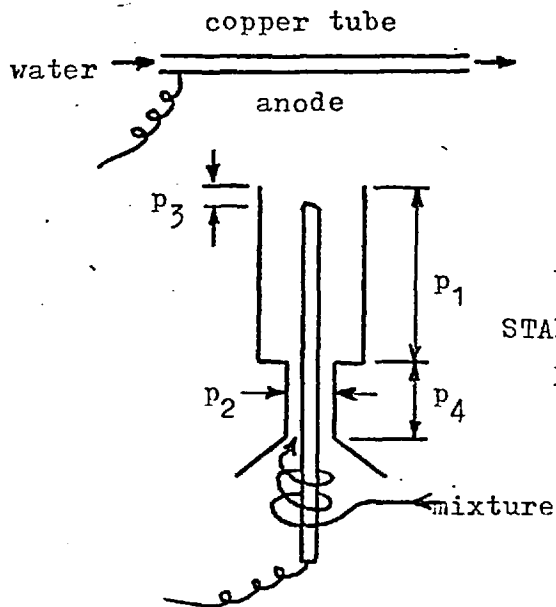


FIGURE V 6

VORTEX
STABILIZED
BURNERS

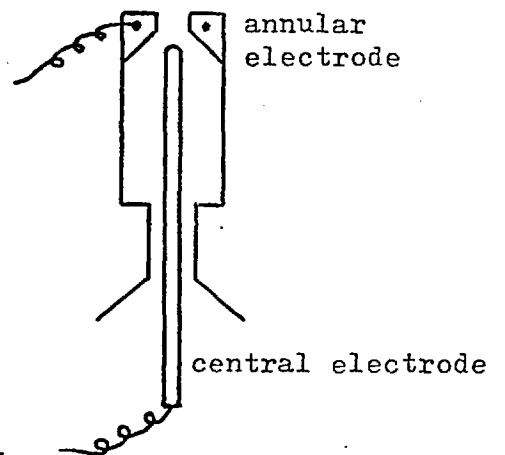


FIGURE V 7

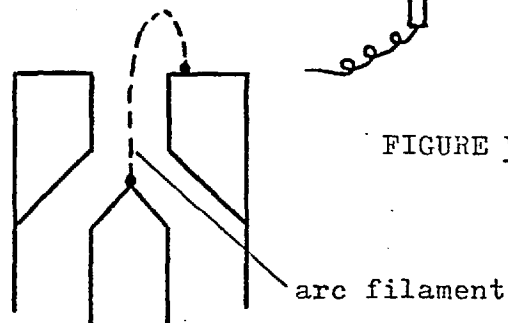


FIGURE V 8

on, the airflow could be reduced to 7.5 litres per minute before any instability occurred. Below this value, the flame lifted out of the combustion chamber and became stabilized on top of the annular electrode. Sooting started to occur and the nozzle become blocked with carbon after a few minutes running. Without the arc and at airflows below 10 litres per minute, the flame became stabilized on top of the nozzle instead of in the combustion chamber.

(d). Large-scale vortex-stabilized burner.

The vortex-stabilized burner was the basis for the design of a larger burner which was to be used for flame-hardening. The first attempt aimed at combusting 10 litres per minute of methane instead of 1 litre per minute burnt in the small burner and so the diameters of the components of this larger burner were increased by a factor of $\sqrt{10}$.

The most significant modifications required to scale-up the small vortex-stabilized burner are detailed below:-

- (i) The ceramic tube used in the construction of the central electrode suffered from thermal shock, alumina being slightly more resistant to this than zirconia. A graphite rod was substituted for the ceramic tube and was found to give a satisfactory arc. The graphite suffered from severe erosion, at the rate of 0.5 mm. per minute diminution in length accompanied by reduction in diameter. A more durable electrode was made from a tungsten rod when the rate of oxidation could be kept low by burning the methane with less than its stoichiometric air requirement.
- (ii). The small vortex-stabilized burner required the removal of its annular electrode before the gas mixture could be ignited with a pilot light. This technique was unsatisfactory for the large burner because the combustion products exerted sufficient force on the annular electrode to lift it and they escaped from the gap which was formed between the electrode and the combustion chamber. The electrode was cemented in position to prevent this leakage, necessitating an alternative method of ignition which was achieved using a high tension spark. Details are given on page 80.
- (iii). The arc which was produced in the larger burner was observed to attach itself to the top surface of the annular electrode, as shown in figure V 8, and to be noisy and unstable.

An alumina ring was built into the top of the electrode in order to insulate this region and cause the arc to strike in the lower part of the nozzle. The arc was then found to be less noisy and more stable.

Annular electrodes with nozzle diameters which varied from 8 mm. to 25 mm. were tried in conjunction with a tungsten central electrode of 10 mm. diameter. The 8 mm. nozzle was found to exert too great a pressure loss on the burner causing the flame to light back into the swirl chamber, whilst the 25 mm. nozzle gave rise to a noisy, unstable arc. A 10 mm. nozzle was found to provide a stable arc with no tendency for the flame to flash back and was used in all further work on this burner.

The gas mixture could only be ignited successfully when the methane and air flow rates were controlled at 6.7 and 87 litres per minute, respectively. The burner was then operated at 7.5 litres per minute of methane and 75 litres per minute of air for 15 minutes in order to heat the electrodes. Electrical power was then supplied to the electrodes from the transducer described on page 80. Once an arc had been struck between the electrodes, the air flow rate could be reduced to 55 litres per minute. The potential applied to the electrodes was 1 kilovolt for an electrode separation of 6 mm. and currents up to 10 amps were used. Electrode erosion became severe at currents greater than 4 amps. The optimum distance between the electrodes was found to be about 6 mm. because larger distances gave rise to increasing instability of the arc and smaller distances caused an increase in the pressure drop in the burner and a corresponding reduction in the gas flow rate.

A detailed description of this burner is given on page 74 .

2. ARC CHARACTERISTICS.

The characteristics of both d.c. and a.c. discharges were investigated for the small vortex-stabilized burner. The static or d.c. characteristic is explained in section II 3 (b) (iii) and (iv) and the technique for obtaining it is given below. An oscilloscope was used to measure the current and voltage-time characteristics for the a.c. discharges.

(a) D.c. characteristics.

The small vortex-stabilized burner was set up as shown in figure V 6 with a water-cooled tube as the anode which was earthed. Values of voltage across the burner electrodes and corresponding values of current flowing through the arc were obtained by varying the series resistance in the burner electrode circuit. The series resistance is shown in figure IV 3 and was varied between 1 kilohm and 12 kilohms.

(b) Oscilloscope Traces.

A twin beam oscilloscope was used to display voltage and current transients during the a.c. discharge in the small vortex-stabilized burner. Oscilloscope traces were photographed when an arc was struck between:-

- (1) the central electrode and the earthed and water-cooled tube shown in figure V 6.
- and (2) the central electrode and the annular electrode in position on top of the burner, as shown in figure IV 1.

Figure V 9 shows the circuitry that was used to obtain current and voltage traces on the oscilloscope.

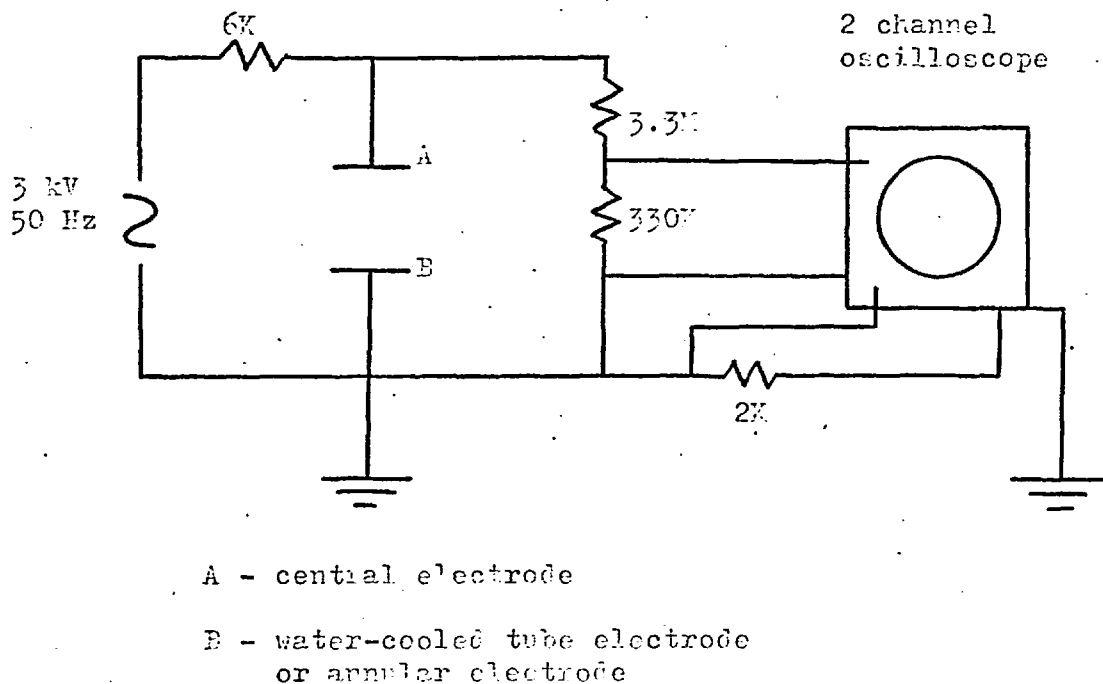


Figure V 9.

Resistors were placed in parallel with the burner electrodes in order to provide the oscilloscope with a voltage that was proportional to, but considerably less than, the voltage across the electrodes. A voltage was also obtained which was proportional to the current flowing in the arc when the water-cooled tube electrode was used. Measurement of voltage drop across a resistor placed in series with either of the electrodes would have enabled the current in the discharge to be calculated by Ohm's law. This procedure was not possible because the central electrode was at too high a potential and the water-cooled electrode was earthed. The alternative was to measure the small voltage drop which occurred across the 2 kilohm resistor shown in figure V 9. This voltage was not measured when the annular electrode of zirconia was in use.

The distance between the burner electrodes was varied and voltage and current traces were photographed for each distance for a range of air to methane ratios.

3. HIGH SPEED PHOTOGRAPHY.

A Fastax high speed camera, capable of up to 6000 frames per second, was used to obtain cine records of electrically augmented flames. The small vortex-stabilized burner provided a flame that was too small to be successfully photographed with the existing lens on the camera. All filming was restricted to the large vortex-stabilized burner and the camera was set up inside the earthed cage shown in plate IV 3. A remote control was constructed in order to start the camera.

Although the camera was angled to view as much of the inside of the burner nozzle as possible, any points of attachment of the arc to the electrodes in the burner could not be photographed. Further films were taken of the augmented flame impinging on a flat, water-cooled surface.

4. CALORIMETRY.

The use of the calorimeters described in sections IV 4 (a) - (c) is outlined below. Rates of heat output and heat fluxes from the various burners described in sections IV 1 (a), (b) and V 1 (a) - (d) have been determined with these calorimeters.

(a) Tube calorimeter.

The tube calorimeter is described in sections II (d) (ii) and IV 4 (a) and was used both as an electrode and as a calorimeter for the sudden expansion and the small vortex-stabilized burners (see sections IV 1 (a) and V 1 (b) and (c)).

The rates of heat transfer from these burners were determined by moving the tube (which was held horizontally) through the flame at a height h above the burner. The tube was halted on the flame axis and readings of the water flow rate and rise in temperature of the cooling water were taken when the steady state had been reached.

Prior to determining heat fluxes, the rate of heat removal by the cooling water was found at various flow rates with the calorimeter receiving a steady input of heat from the burner. This information was required to find the dependence of the measured heat input on the water flow rate, if any.

(b) Concentric calorimeters.

The construction of these calorimeters is outlined in sections IV 4 (b) and their use in determining the heat flux distribution from the small vortex-stabilized burner to a large water-cooled plane surface is described below.

To determine the heat flux distribution from the burner, each calorimeter was positioned, in turn, at a height h above the burner and co-axially with it. In the steady state, the water flow rate to the calorimeter was measured by a flow meter of the variable area, tapered-tube type and the difference in temperature between the inlet and outlet water flows was recorded using a differential thermocouple.

The water flow rate was varied for each calorimeter in order to find the influence of the water flow rate on the rate of heat input determined by the calorimeter in the steady state.

The flow rate of water to the guard ring was also varied for the same purpose. When the inlet water to the calorimeter was at room temperature, the water vapour in the products of combustion from the burner was observed to condense on the surface of the calorimeter. This problem of condensation was overcome by reducing the flow rate of water to the heat exchanger (see page 84) in which the water from the calorimeter was cooled. Consequently, the temperature of the inlet water was raised and condensation was prevented.

(c) Split plate calorimeter.

This calorimeter is described in sections IV 4 (c) and II (d) (ii) and was used to determine the heat flux distribution from the large vortex-stabilized burner (see sections IV 1 (b) and V 1 (d)).

Early attempts at determining heat fluxes using this calorimeter involved measuring the water temperatures with thermocouples. The thermocouples were of the copper-constantan type but were found to be too inaccurate. They were replaced by thermistors in water-tight plastic sheaths which were individually calibrated against a mercury-in-glass thermometer using a thermostatically controlled water bath. The thermistors were connected to the bridge circuit described in section IV 4 (c) and readings were obtained on the digital voltmeter over a range of temperatures measured on the thermometer. After calibration, the thermistors were sealed into the water pipes of the calorimeter using a silicone rubber adhesive.

The calorimeter was then traversed over the burner and halted at intervals of 2 mm by the mechanism described on page 88. When the steady state had been reached the output of the digital voltmeter was noted for each of the four thermistors and the flow rate of water through the calorimeter was measured using a Rotameter. From the calibration curves previously obtained for the thermistors, the voltage readings were transformed to temperatures. The rate of heat removal by the cooling water was obtained from these readings for each position of the calorimeter.

5. TEMPERATURE PROFILES IN SLABS.

The steel slabs described in section IV 5 (a) were passed over the large vortex-stabilized burner in order to determine the effects of slab speed, slab cross-section and burner operating conditions on the temperature profiles measured by thermocouples in the slabs.

A satisfactory means of incorporating a thermocouple in a slab was to drill a hole of 0.5 mm diameter in the side of the slab to the required depth. The diameter of the stainless steel sheath of the thermocouple was then reduced slightly from its original 0.5 mm diameter using fine carborundum paper until the thermocouple could be pushed with difficulty to the end of the hole in the slab. The tip of the thermocouple was then known to be at the end of the hole and would remain there because the sheath was tightly held in the slab. X-ray photography was used on a trial slab to show that the holes which were drilled in the slab lay perpendicular to the side of the slab and extended as far as the vertical mid-plane of the slab, as shown in figure IV 10.

The chromel and alumel leads from each thermocouple were welded to lengths of copper wire which were connected to a data logger. These copper-chromel and copper-alumel junctions were enclosed in glass sheaths which were immersed in melting ice contained in a Dewar vessel. This procedure eliminated the use of compensating cable and provided a reference junction at 0°C. The thermocouple sheaths and the slab were earthed in order to obtain stable emf. outputs on the data logger when the slab was heated by the electrically augmented flame.

The ignition, warming-up and operating procedures for the large vortex-stabilized burner were carried out, as described on page 102. A prepared slab was then drawn across the flame at a selected speed and data logging of the millivolt output from the thermocouples commenced.

6. FLAME HARDENING.

The technique outlined in section V 5 above was also used to flame harden the surface of the slab. The general arrangement of the apparatus is shown in figure IV 9 with the addition of a quench placed next to the burner as described in section IV 6.

Slab speed, quench water flow rate and the distance between the quench and the burner were varied. After treatment, the slabs were sectioned at a number of positions and hardness traverses were made from the heat treated surface towards the centre of the slab. Both the Vickers hardness test and the microhardness test were used to obtain these hardness traverses. These tests are described in section II 7 (b). Metallographic examination was carried out on polished sections of the heat treated steel.

Microstructures of the En 8 prior to treatment showed that a decarburised layer existed at the surfaces of the slabs. This region was removed from the surface which was to be flame hardened by grinding a layer of thickness 0.5 mm from the surface. The surface finish, before and after treatment, was assessed using a Tallysurf machine. This instrument has an arm with a diamond stylus which follows the surface of the specimen. The magnified motion of the arm is recorded on a chart.

VI RESULTS.

1. BURNER OPERATION.

Section V 1 describes many of the experimental observations which were made during the development of an electrically augmented burner for flame hardening.

The range of air and methane flows and the extent of electrical augmentation for the various types of burner are given in table VI 1.

Burner type and throat dimension mm	Air l min ⁻¹	Methane l min ⁻¹	Electrical augmentation	
			amps	volts
Simple tube, dia. 9.5	9.7	1.2	0.1	800 d.c.
Sudden enlargement, dia. 7.0	7.0 to 12.0	0.7 to 1.2	0.1	800 d.c.
Small vortex-stabilized, effective dia. 7.0	7.5 and 11.0	1.0	0.4	1000 a.c./d.c.
Large vortex-stabilized, effective dia. 17	55 and 75	7.5	3.0 to 4.0	1000 a.c.

Table VI 1

The current and voltage figures were found to vary according to the distance between the electrodes and the air and methane flow rates. The usual operating practice is quoted under "electrical augmentation" in table VI 1. The airflow for the small and large vortex-stabilized burners was 11.0 and 75 litres per minute, respectively, during the warm-up period prior to augmentation. As soon as warm-up had been completed and the electrical discharge had been initiated, the airflows were reduced to 7.5 and 55 litres per minute, as shown in table VI 1.

The effective throat diameters were calculated for the large and small vortex-stabilized burners using the relationship:

$$\text{effective area of throat} = \text{area of throat} - \text{cross-sectional area of central electrode,}$$

where the effective area is the area available for gas flow and the relevant burner dimensions are given in figures IV 1 and IV 2.

(a) Flame stability.

(i) Simple tube burner.

Blow-off limits for a flame stabilized on a tube are discussed in section III 2 (a) under "flame stability". Flow-off was investigated for tubes of 7 mm and 8 mm diameter. The maximum flow rates of methane and air at which stable flames could exist on the tubes were measured for a range of methane to air ratios. The results have been plotted in figure VI 1 as the percentage of methane in the total gas flow against the total gas flow when the flame was about to lift off the burner tube. The lines due to Lewis and von Elbe will be considered in the next chapter.

If fully developed laminar flow is assumed to exist in the burner tube then the distribution of velocity of the unburnt mixture in the tube will be given by

$$v_u = 2 \bar{v}_u \left(1 - \frac{r'^2}{r^2} \right) \quad \text{--- VI 1}$$

where $\bar{v}_u$ is the mean velocity in the tube and the other symbols have the significance allocated to them in section III 2 (a). The velocity gradient at any position in the tube is given by

$$\frac{dv_u}{dr} = \frac{4\bar{v}_u r}{r^2} \quad \text{--- VI 2}$$

At the wall of the tube, equation VI 2 becomes

$$\left[\frac{dv_u}{dr} \right]_{r'} = \frac{4\bar{v}_u}{r'} \quad \text{--- VI 3}$$

Equation VI 3 defines the region of stability of a flame which is burning on a circular tube for fully developed laminar

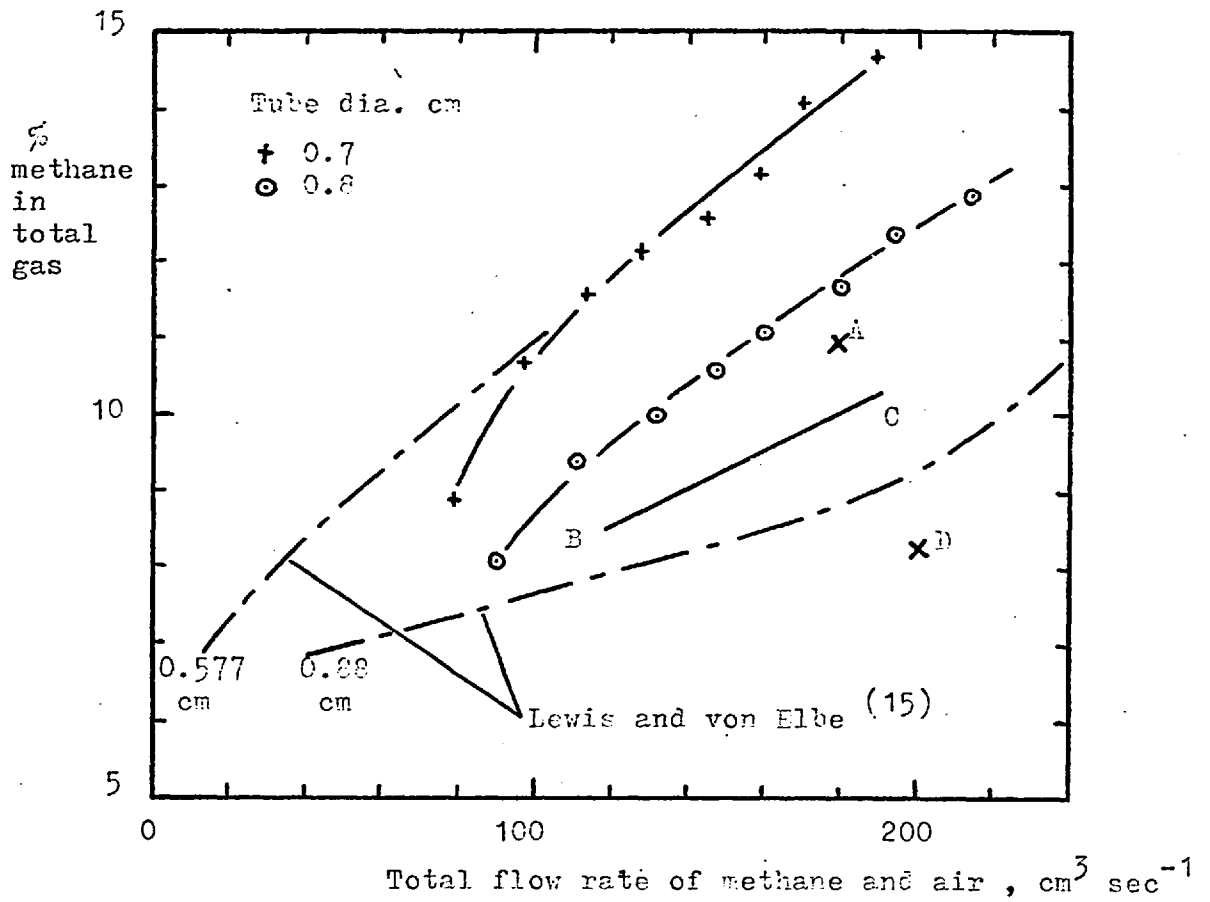


FIGURE VI 1 - BLOW-OFF LIMITS ON SIMPLE TUBE BURNERS

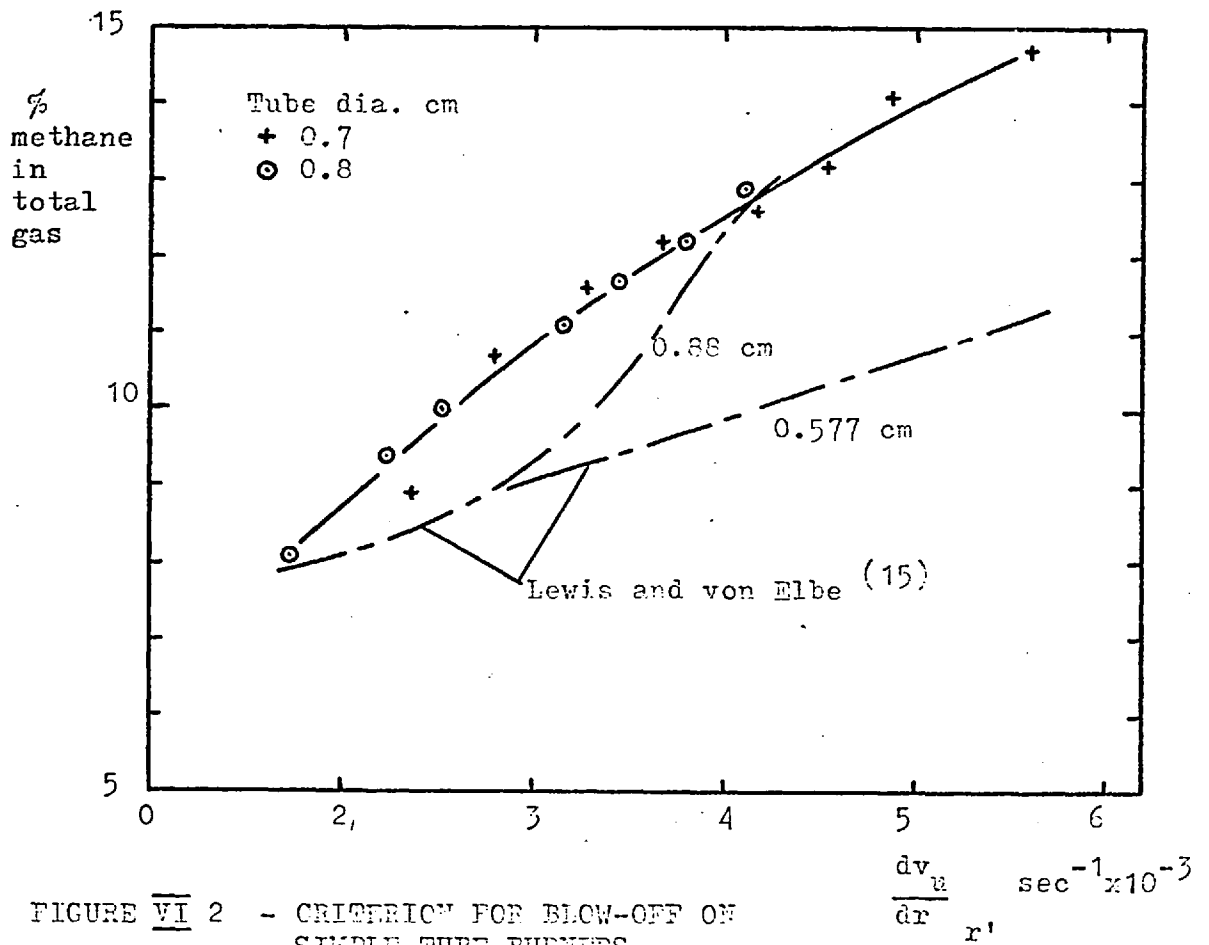


FIGURE VI 2 - CRITERION FOR BLOW-OFF ON SIMPLE TUBE BURNERS

flow of the unburnt mixture in the tube. The data in figure VI 1 have been used to calculate the maximum value of $\bar{v}_u$ at which the flame is stable. The value of $\left[\frac{dv_u}{dr}\right]_{r'}$

has been calculated using $(\bar{v}_u)_{\max}$ in equation VI 3.

$\left[\frac{dv_u}{dr}\right]_{r'}$ is then the criterion for blow-off. This criterion

is shown in figure VI 2. over a range of methane percentages in the total gas flow for the tubes of 7 mm and 8 mm diameter.

Point A in figure VI 1 corresponds to the operating conditions of the simple tube burner of 9.5 mm diameter given in table VI 1.

(ii) Sudden enlargement burner.

The operating conditions for this burner are given in table VI 1 and are represented approximately by the line BC in figure VI 1. Line BC corresponds to stable operation of the burner and does not indicate the condition of blow-off.

(iii) Small vortex-stabilized burner.

Table VI 1 shows that this burner was operated at a maximum airflow of 11 litres per minute with a methane flow of 1 litre per minute. This operating condition is represented by point D on figure VI 1 which does not correspond to the blow-off limit.

(iv) Methane inflammability limits.

Figure VI 3 gives the rich and weak limits of inflammability as explained in section II 2.(a)(iii).

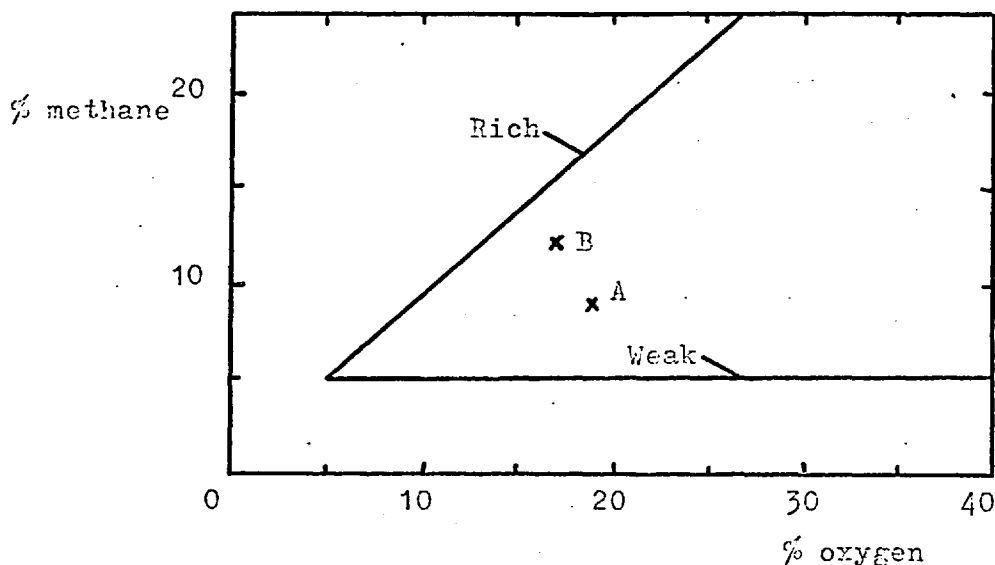


Figure VI 3

The operating points for the large vortex-stabilized burner are superimposed on the figure. Point A corresponds to the warm-up period when the methane flow rate was 7.5 litres per minute and the air flow rate was 75 litres per minute whilst point B corresponds to the mixture which was used during electrical augmentation of the flame (given in table VI 1).

Points A and B also represent the mixture strengths which were used in the small vortex-stabilized burner during warm-up and during electrical augmentation, respectively.

(b) Arc characteristics.

D.C. arc characteristics and current and voltage transients for a.c. arcs were obtained for the small vortex-stabilized burner as described in section V 2 (a) and (b).

(i) D.c. characteristics.

Voltage against current is plotted in figure VI 4 for arc lengths of 5 mm and 10 mm. The lines AB and AC are the load lines for the 5 mm and 10 mm arcs, respectively, when the arcs are about to be extinguished according to section III3(b)(iv) on arc stability. The general equation of these load lines is

$$V = E - IR$$

where E is the supply voltage and R is a constant resistance in series with the arc. The supply voltage was 3000 V, giving

$$V = 3000 - IR \quad \text{-----} \quad \text{VI} \quad 4$$

For line AB:-

$$\text{when } V_{AB} = 0V, I_{AB} = 0.59A, \text{ giving } R_{AB} = 5100 \Omega$$

$$\text{-----} \quad \text{VI} \quad 5$$

For line AC:-

$$\text{when } V_{AC} = 0V, I_{AC} = 0.56A, \text{ giving } R_{AC} = 5400 \Omega$$

$$\text{-----} \quad \text{VI} \quad 6$$

The normal operation of the vortex-stabilized burner required a current of 0.4A, as given in table VI 1. A load line passing through the characteristics at 0.4A would require a series resistance of 4000 to 4500 Ω .

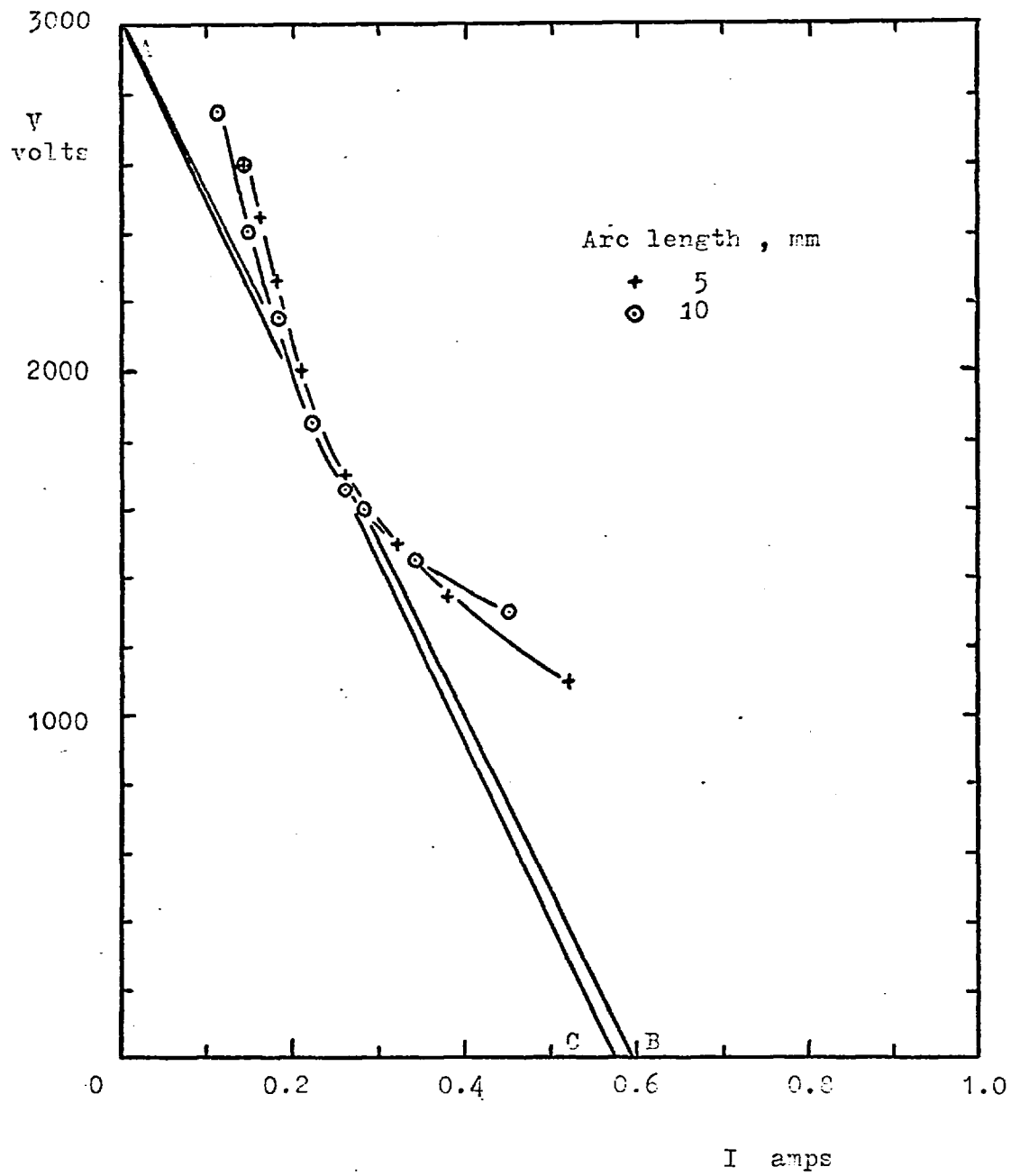


FIGURE VI 4 D.C. CHARACTERISTICS OF THE SMALL VORTEX-STABILIZED BURNER

(ii) Current and voltage transients in a.c. arcs.

Plate VI 1 shows photographs of oscilloscope traces obtained for 50 Hz arcs with a 3000 V supply and a 6 kilohm series resistance. The voltage scale is shown beside each photograph and each half-cycle corresponds to 0.01 second. The current scale is not shown because the constant of proportionality between the voltage which was measured by the oscilloscope and the current in the arc was unknown. Table VI 2 gives the conditions under which each photograph was taken.

Photo.	Electrode system, - arc strikes between:-	Distance between electrodes	Methane flow 1 min ⁻¹	Air flow 1 min ⁻¹
(a)	Central and water-cooled tube	5 mm	1	10
(b)	Central and annular	5 mm	1	10
(c)	"	2 mm	1	10
(d)	"	5 mm	1	0

Table VI 2

(c) High speed photography.

Plate VI 2 shows four sequences of frames which have been taken from cine films of the flame from the large vortex-stabilized burner. The camera was operating at about 2600 frames per second for all four sequences; hence each half-cycle of the 50 Hz supply to the burner occupies 26 frames.

Table VI 3 gives the operating conditions for the burner and the corresponding sequence of frames. In all cases, the burner was supplied with 7.5 litres per minute of methane and 55 litres per minute of air.

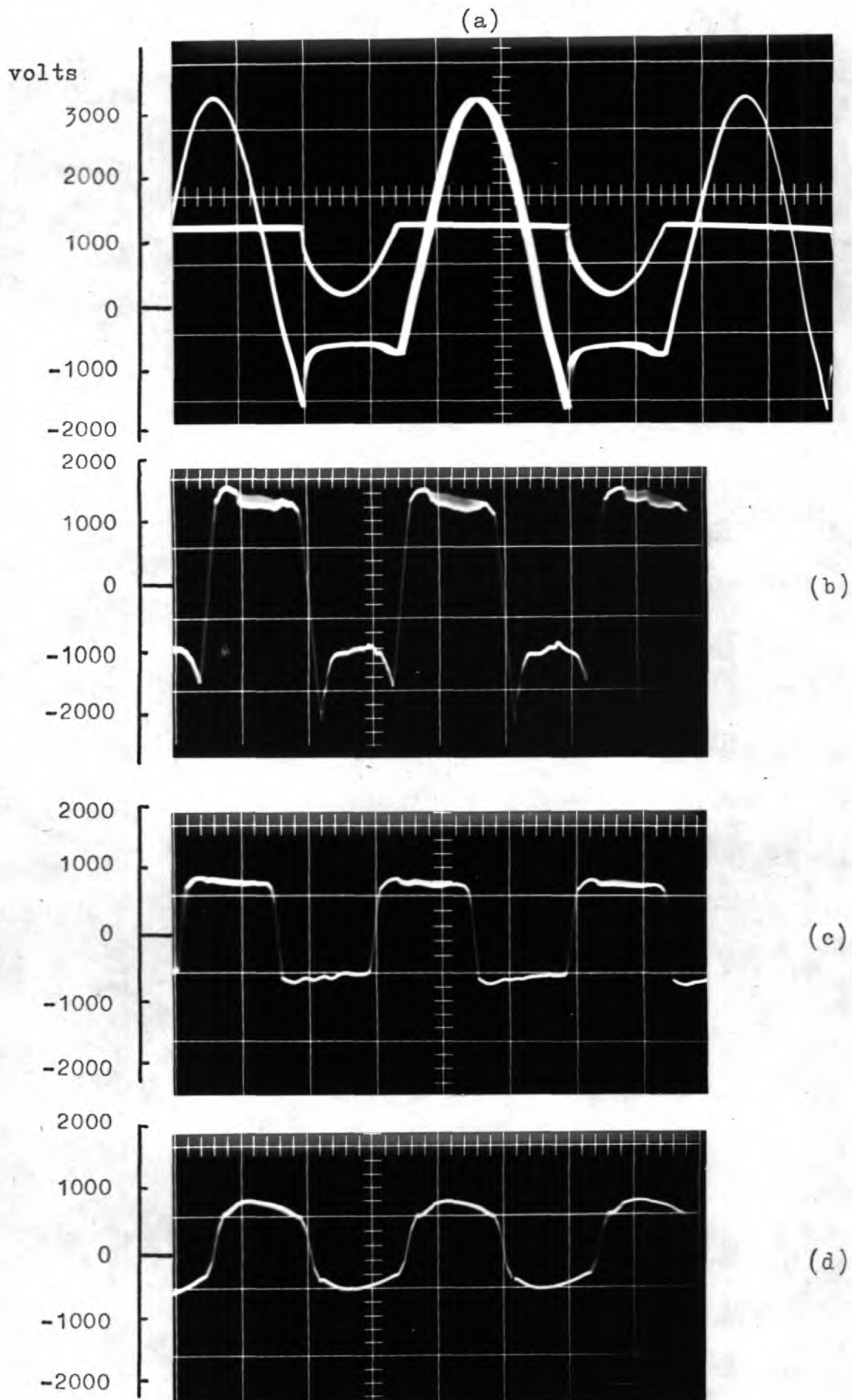
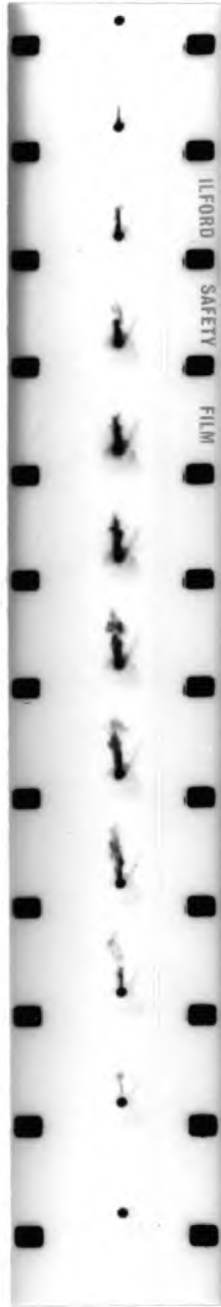


PLATE VI 1 - VOLTAGE TRANSIENTS OBTAINED FROM THE OSCILLOSCOPE



(a)



(b)



(c)



(d)

PLATE VI 2 - SEQUENCES FROM HIGH SPEED CINE FILM SHOWING
ELECTRICALLY AUGMENTED FLAMES
(SEE TABLE VI 3 FOR DETAILS)

Sequence	System	Electrical augmentation	
		Amps	Volts
(a)	Flame only	0.6	1000
(b)	" "	3.0	1000
(c)	Flame to plate at 1 cm gap	3.0	1000
(d)	Flame to plate at 2 cm gap	3.0	1000

Table VI 3

(d) Electrode life.

The components of the final designs of the vortex-stabilized burners are shown in figures IV 1 and 2. The electrodes were the components with the shortest life, the other parts showing no marked deterioration during the entire investigation.

The life of the electrodes in the small vortex-stabilized burner was difficult to assess because operation of the burner was often limited to a few minutes. The volume of methane consumed by the burner was about 3600 litres at a rate of 1 litre per minute. On this basis, the burner was operated for a total of 60 hours and during this time one replacement was necessary - a central electrode.

The large vortex-stabilized burner was operated for longer periods of time, usually 0.5 to 2 hours. The annular electrode required replacement after 30 to 40 hours because a gap developed between the carbon ring and the zirconia refractory due to oxidation of the surface of the carbon ring. Arcing then commenced inside the electrode, resulting in a loss of electrical power to the flame. The central electrode of tungsten was found to erode at an average rate of 0.5 mm reduction in length per hour. The reduction in length was accompanied by a reduction in area of cross-section in the region of the electrode which was exposed to the flame. Yellow tungstic oxide appeared in this region.

2. HEAT TRANSFER TO WATER-COOLED SURFACES.

The various water-cooled calorimeters which were used to measure rates of heat transfer from the sudden enlargement and vortex-stabilized burners to water-cooled surfaces are described in sections IV 4 (a), (b) and (c). The techniques which were used to obtain results from these calorimeters are explained in sections V 4 (a), (b) and (c). The experimental measurements and derived results are given below.

(a) Tube calorimeter.

The flow rate of the cooling water through the tube was found to affect the rate of heat transfer from the flame to the tube. Results were obtained with the calorimeter horizontal and directly above the sudden enlargement burner so that the burner axis coincided with the mid-point of the tube of the calorimeter. The distance between the calorimeter and the burner was set at 2 cm and the burner was operated at 0.72 l min^{-1} of methane and 7.6 l min^{-1} of air, giving a stable flame. The increase in temperature of the cooling water was measured by the thermocouples positioned at the inlet and outlet of the tube (see figure IV 5) for a number of water flow rates. The temperature of the cooling water entering the calorimeter was maintained at $20^{\circ}\text{C} \pm 1^{\circ}\text{C}$ during the investigation. Condensate was observed on the surface of the tube when the water outlet temperature was less than 40°C , i.e. when the increase in temperature of the cooling water was less than 20°C .

A plot of the difference in temperature between the outlet and the inlet against the water flow rate is shown in figure VI 5(a). The rate of the heat transfer from the flame to the tube was obtained from the data of figure VI 5 (a) using:-

$$\dot{q} = 4.186 Q \cdot \Delta\theta \quad \text{-----} \quad \text{VI } 7$$

where

$\dot{q}$ = rate of heat transfer, W,

Q = water flow rate, $\text{cm}^3 \text{ sec}^{-1}$,

$\Delta\theta$ = temperature difference, $^{\circ}\text{C}$.

The rate of heat transfer $\dot{q}$ was plotted against water flow rate to give figure VI 5 (b). The Reynolds number for the flow of cooling water along the tube is shown against water flow rate in figure VI 5 (c), where

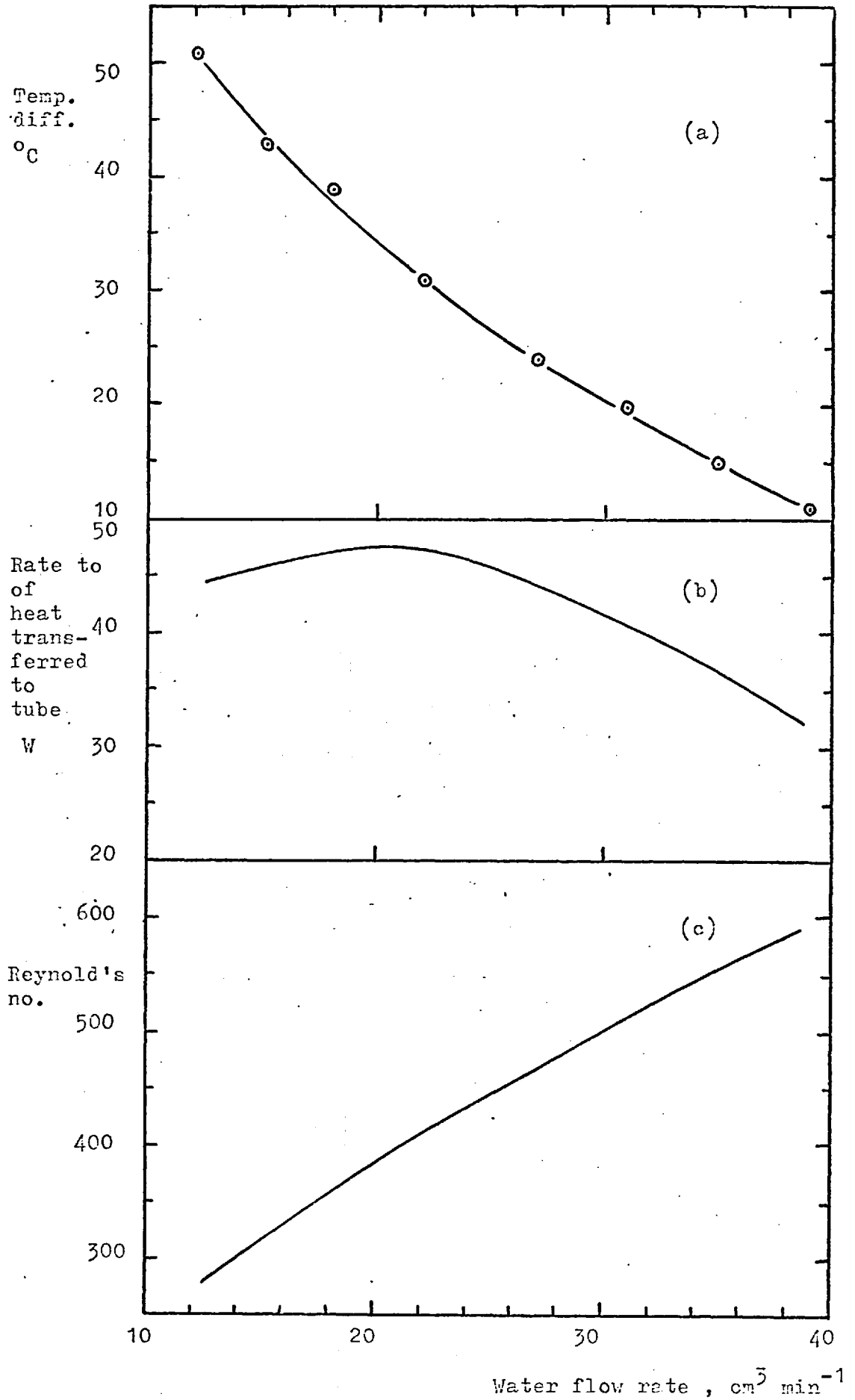


FIGURE VI 5 - EFFECT OF WATER FLOW RATE ON THE RATE OF HEAT TRANSFER TO THE TUBE CALORIMETER

$$\text{Reynolds no.} = \frac{4 Q}{\pi d \nu} \quad \text{--- VI 8}$$

where

Q = water flow, $\text{cm}^3 \text{sec}^{-1}$,

d = internal diameter of tube, cm

= 0.16 cm

ν = kinematic viscosity, $\text{cm}^2 \text{sec}^{-1}$

Values of the kinematic viscosity were obtained from the literature at the mean temperature of the water in the tube (54).

(i) Sudden enlargement burner

The rates of heat transfer from this burner to the tube calorimeter were determined for the following conditions:-

methane flow rate = 0.72 l min^{-1}

air flow rates = 6.8, 7.6 and 8.4 l min^{-1}

(i.e. % methane in total gas = 9.5, 8.6 and 7.9)

cooling water flow rate = $26 \text{ cm}^3 \text{min}^{-1}$

distances between burner and tube = 0.5, 1.0, 2.0 and 3.0 cm

electrical enlargement = 0.1 A at 800 Vd.c

and 0.05 A at 800 Va.c.

The flame was augmented by both a.c. and d.c. power using the burner rim as one electrode and the earthed tube as the other. The airflow was maintained at 7.6 lmin^{-1} during augmentation and the distance between the burner and the tube was set at 2.0 cm. D.c. augmentation was carried out both with the burner rim as the cathode and with the tube as the cathode. Continuous seeding was required when the tube was used as a cathode and when a.c. augmentation was carried out. When d.c. augmentation was used with the burner rim as a cathode, seeding was only required to initiate the arc.

Figure VI 6 (a) shows the rates of heat transfer for the augmented flame when both the distance between the burner and the tube and the proportion of methane in the mixture were varied. Point A in this figure is the rate of heat transfer obtained with d.c. augmentation, irrespective of polarity, and point B is the corresponding value for a.c. augmentation.

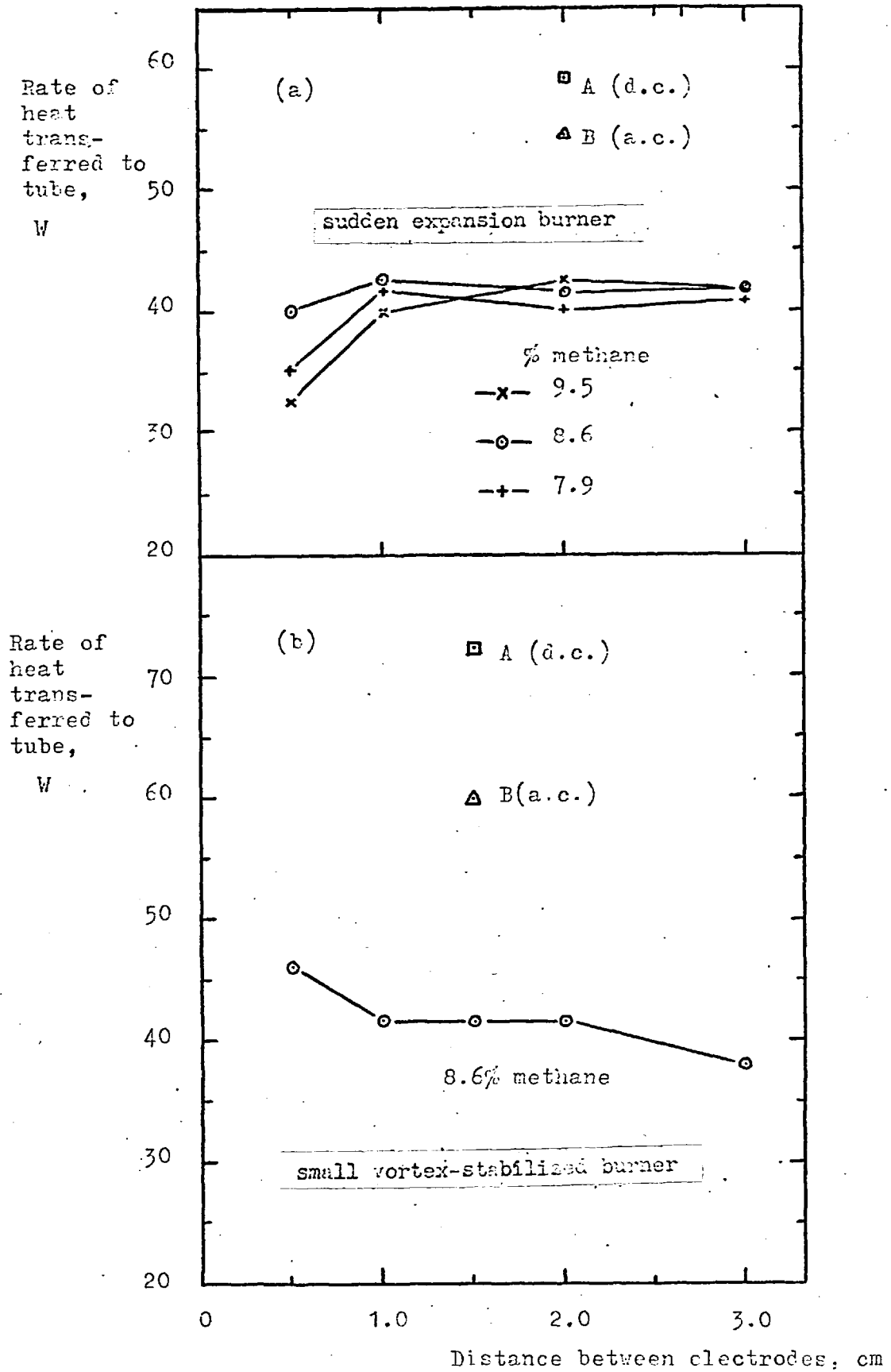


FIGURE VI 6 - HEAT TRANSFER TO THE TUBE CALORIMETER

(ii) Small vortex-stabilized burner without the annular electrode

The rates of heat transfer from this burner to the tube calorimeter were determined in a similar manner to (i) above and under similar conditions, except that:-

Airflow rate = 7.6 l min^{-1} only
Electrical augmentation = 0.2 A at 1 kV d.c.
and 0.1 A at 1 kV a.c.

Seeding was only required when the tube was used as the cathode.

The results are shown in figure VI 6 (b) and are analagous to those in figure VI 6 (a).

Further results were obtained with this burner in order to investigate the influence exerted by the proportion of methane in the combustible mixture on the rate of heat transfer, both with and without electrical augmentation. The following conditions were imposed:-

Methane flow rate = 0.72 l min^{-1}
Air flow rates = 2 to 11 l min^{-1}
Cooling water flow rate = $26 \text{ cm}^3 \text{ min}^{-1}$
Distance between burner and tube = 1.0 cm
Electrical augmentation = 0.2A at 1 kV d.c.

The central electrode of the burner was made the cathode and seeding was not required. The flame would not exist at less than 2 l min^{-1} of air or at greater than 11 l min^{-1} of air, i.e. 19.9% methane and 6.4% methane respectively. The results are shown in figure VI 7 as the rate of heat transfer from the flame to the tube against the percentage of methane in the total mixture. The regimes A to G which are shown in the figure correspond to the the following observations:-

- A - flame was stabilized on central electrode,
- B - flame was stabilized in the combustion chamber and heated the central electrode to orange heat (normal operation),
- C - as for B but flame became noisy,
- D - flame was stabilized on the rim of the combustion chamber and had the appearance of a diffusion flame,
- E - arc in flame was narrow and intensely white and a stationary anode spot appeared on the water-cooled tube,
- F - as for E but the arc appeared to be wider and less intense,
- G - as for F but the anode spot moved continuously over the lower

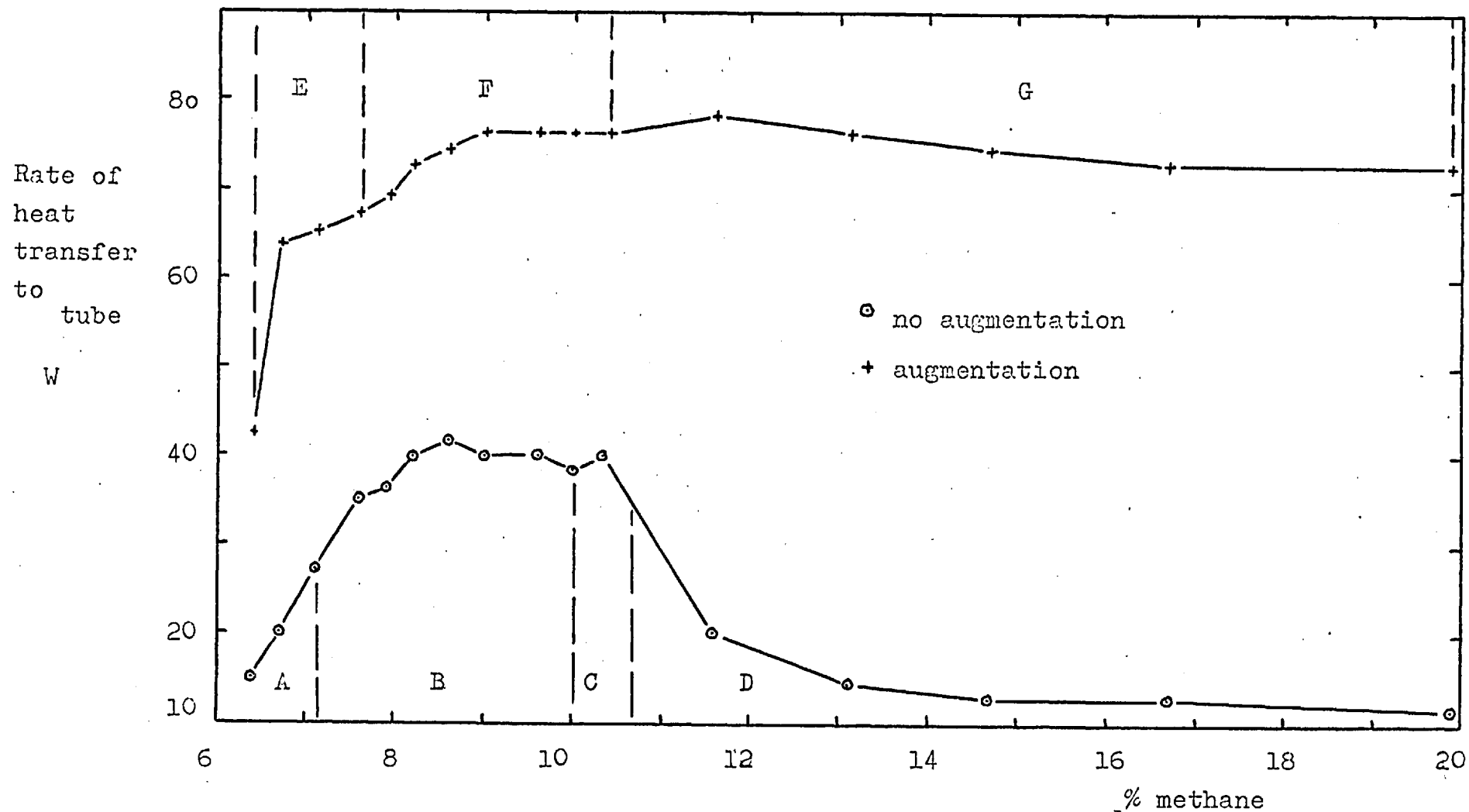


FIGURE VI 7 HEAT TRANSFER FROM SMALL VORTEX-STABILIZED BURNER TO TUBE CALORIMETER

surface of the tube.

The flame went out when the methane content exceeded 20%.

A further series of results was obtained with this burner in order to investigate the effect of methane flow rate and the percentage of methane on the rate of heat transfer from the unaugmented flame to the tube. A wide range of airflows were used for each of a number of methane flow rates, the range of airflows being limited as shown in table VI 4 in order to maintain a stable flame in the combustion chamber for each methane flow rate.

Methane flow rate $l\ min^{-1}$	Stable airflows, $l\ min^{-1}$	
	Maximum	Minimum
0.30	3.25	5.15
0.60	4.75	6.50
0.90	7.25	9.20
1.06	8.4	10.4
1.16	9.6	11.2
1.45	12.3	12.3
1.57	13.4	13.4

Table VI 4

The distance between the burner and the tube was set at 1.0 cm and the cooling water flow rate was maintained at $26\ cm^3\ min^{-1}$. The rates of heat transfer are plotted in figure VI 8 against the percentage of methane in the gas mixture for the various methane flow rates.

(b) Concentric calorimeters

These calorimeters are described in section IV 4 (b) and section V 4 (b) and were used to determine the distribution of heat flux from the small vortex-stabilized burner.

Preliminary results were obtained in order to determine the effect of cooling water flow rate on the rate of heat transfer from the flame to each calorimeter in an analogous manner to the experiment described at the beginning of section VI 2 (a).

VI 2 (b)

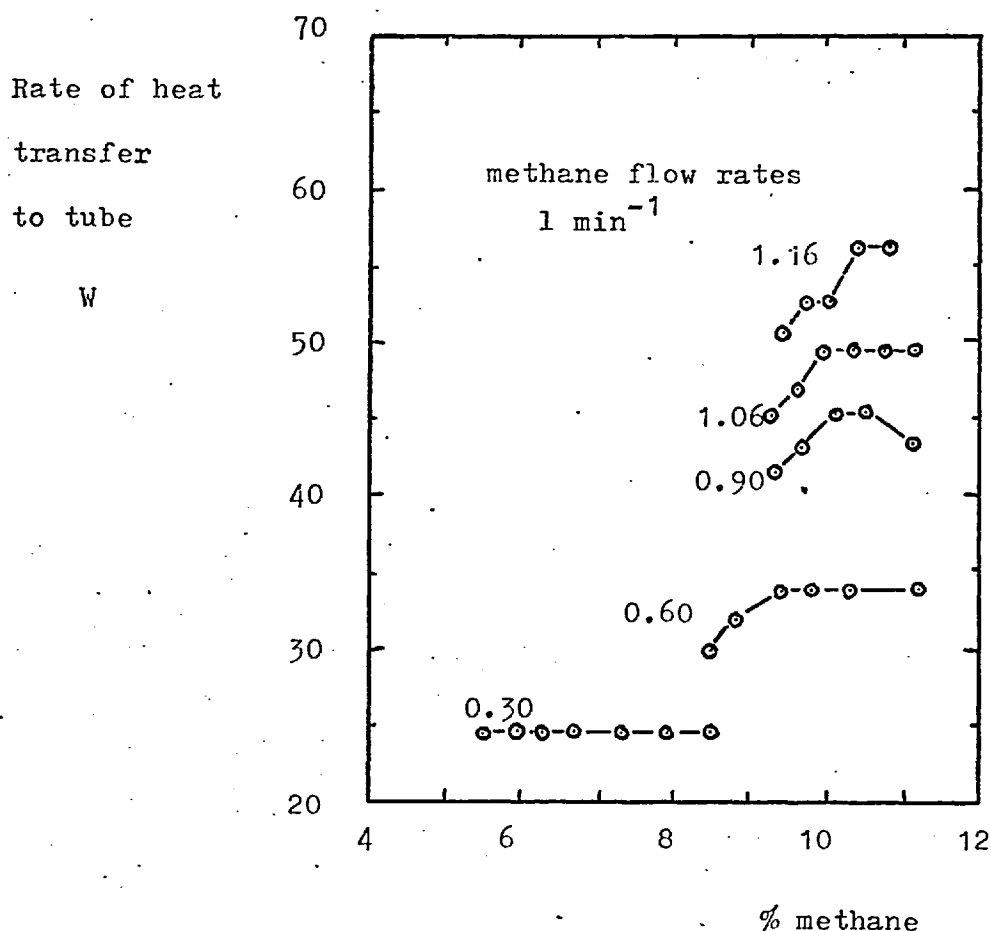


Figure VI 8 - Effect of methane flow rate and proportion of methane on rate of heat transfer to tube

Typical results are given in figure VI 9 for the calorimeters with diameters of 1.3, 5.0 and 13.0 cm. The unaugmented flame was used as the source of heat for the experiments carried out with the 1.3 and 13.0 cm calorimeters but the 5.0 cm calorimeter was tested with both the augmented and unaugmented flames. Condensation was observed on the 13.0 cm calorimeter and on the 5.0 cm calorimeter when they were heated by the unaugmented flame. In general, condensation did not occur if the temperature of the outlet water from a calorimeter was in excess of $40^{\circ}C$. The water flow rate to the guard ring was also varied to determine any effect this may have had on the rate of heat transfer from the flame to the calorimeter. There was found to be no noticeable change in rate of heat transfer due to change in water flow rate to the guard ring. e.g. for the 5.0 cm calorimeter, the water flow to the guard ring was varied between 60 and $640\ cm^3\ min^{-1}$ and the increase in temperature of the calorimeter water due to heat transfer from the flame remained at $19^{\circ}C \pm 0.5^{\circ}C$.

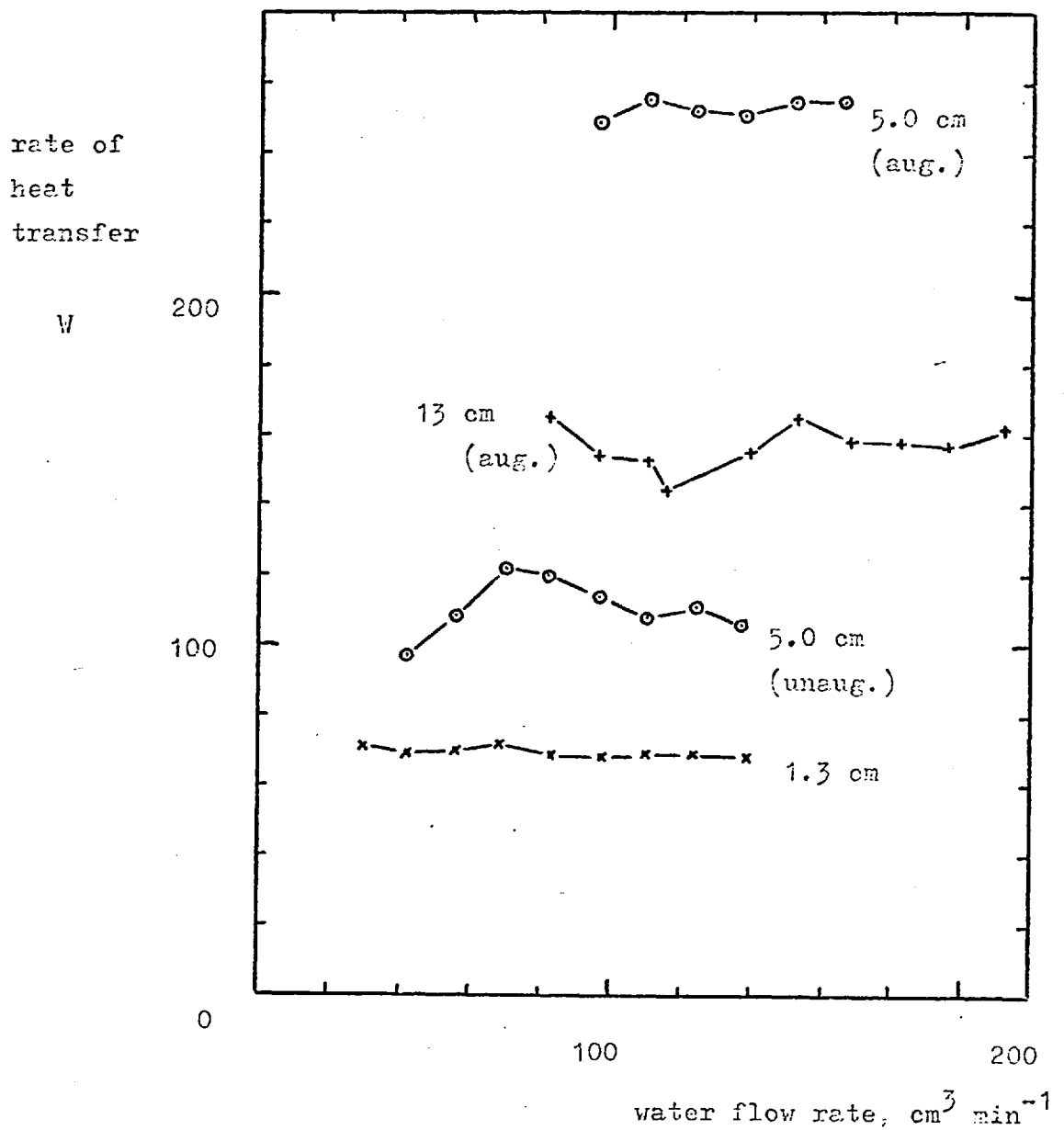


FIGURE VI 9 - EFFECT OF WATER FLOW RATE ON OVERALL RATE OF HEAT TRANSFER TO 1.3 CM, 5.0 CM, AND 13.0 CM CALORIMETERS

(i) Rate of heat transfer to a large water-cooled surface

The overall rate of heat transfer from the small vortex-stabilized burner (with and without annular electrode, with and without electrical augmentation) to a large water-cooled surface was determined using the largest concentric calorimeter (diameter 13 cm). The following operating conditions were used:-

Methane flow rate = 0.9 l min^{-1}
 Air flow rates = $6.9 \text{ to } 12.7 \text{ l min}^{-1}$
 (% methane in total gas flow = 6.6 to 11.5)
 Cooling water flow rate = $215 \text{ cm}^3 \text{ min}^{-1}$

Distance between burner and calorimeter = 3.2 mm

Temperature increases in the cooling water were measured at a number of air flow rates for the following forms of the small vortex-stabilized burner:-

- A. Unaugmented - no annular electrode.
- B. Augmented - no annular electrode, earthed calorimeter acting as an electrode for an a.c. supply of 0.2 A at 1 kV.
- C. Unaugmented - with the annular electrode.
- D. Augmented - with the annular electrode, a.c. supply of 0.4 A at 1 kV.

The rates of heat transfer to the calorimeter were calculated from the measured temperature differences using equation VI 7, and are shown in figure VI 10 for cases A, B, C and D.

Further results were obtained for forms C and D of the small vortex-stabilized burner when the distance between the burner and the calorimeter was varied from 1.6 mm to 76 mm. These rates of heat transfer are shown in figure VI 11 (a) and (b) for methane percentages of 6.6 and 10.7. Rates of heat transfer are also shown at 11.5% methane for an augmented flame only (case D) in figure VI 11 (a). This percentage of methane was too high to allow an unaugmented flame to be stabilized in the combustion chamber of the burner (see region D of figure VI 7).

(ii) Distribution of heat flux at a water-cooled surface.

Figure VI 12' shows schematically the distribution of heat flux which might be expected from measurements taken with the concentric calorimeters. Calorimeter no.1 has a radius r_1 , no.2 has a radius r_2 and no.3 has a radius r_3 . The overall rate of heat

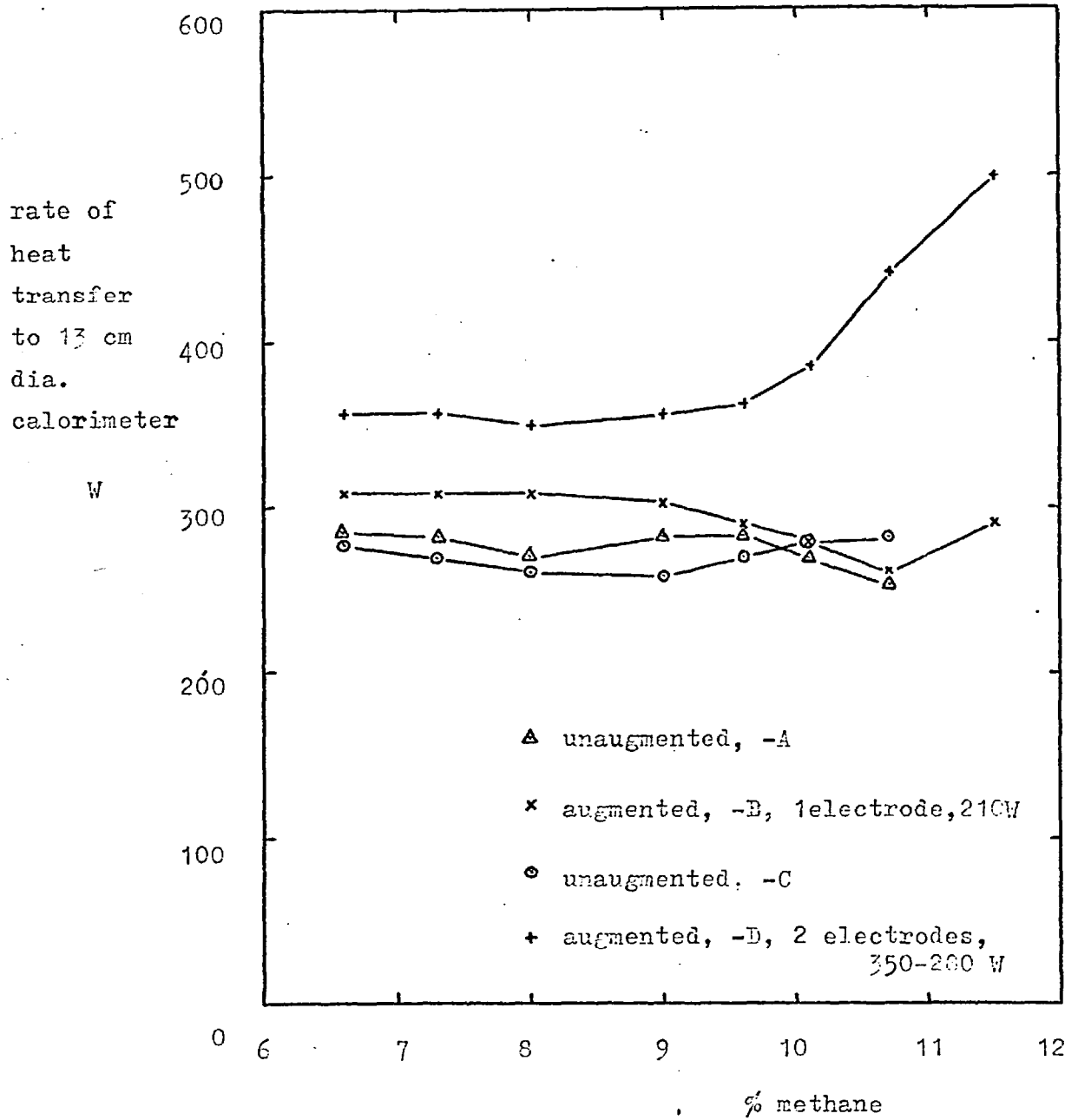


FIGURE VI 10 - EFFECT OF PROPORTION OF METHANE ON OVERALL RATE OF HEAT TRANSFER

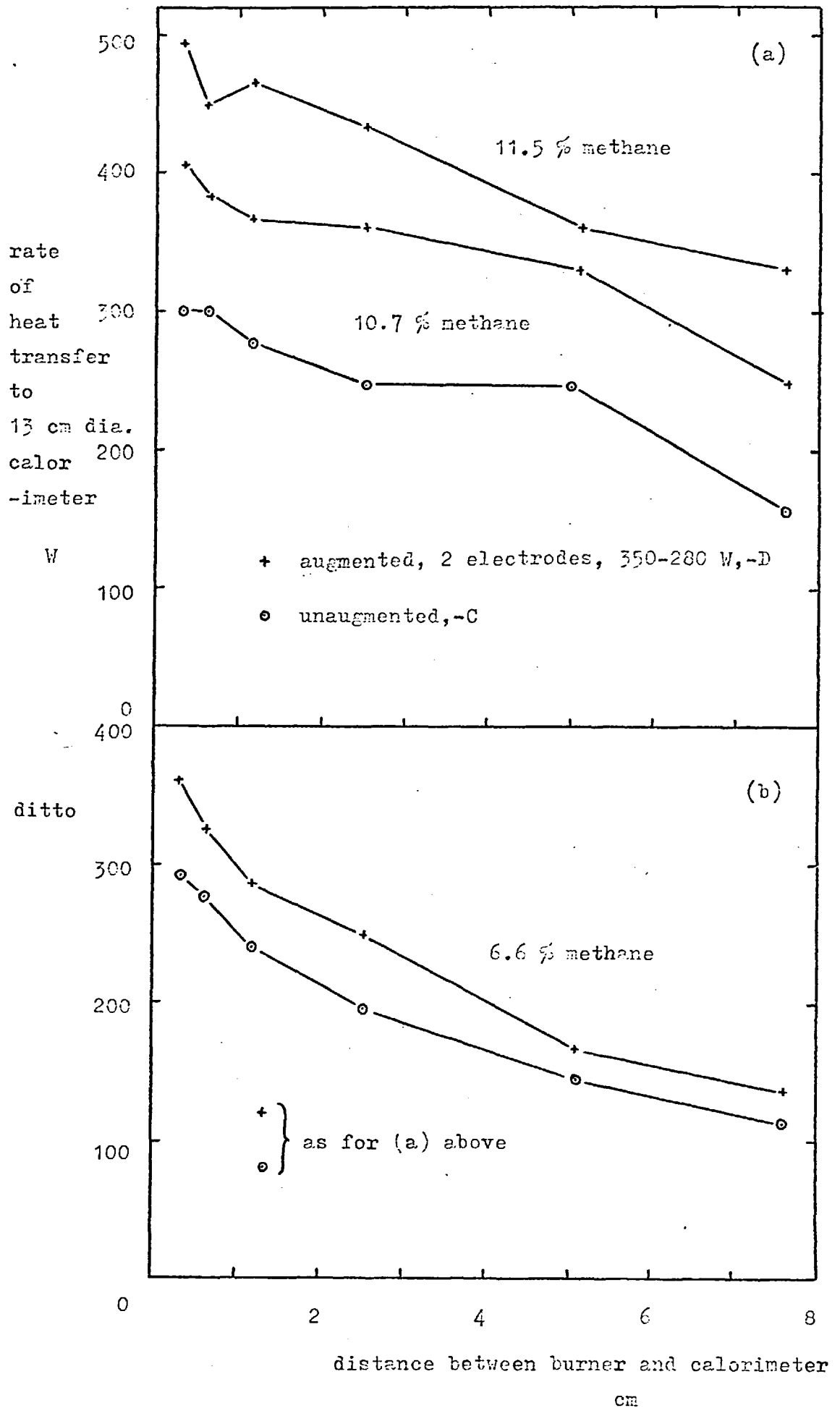


FIGURE VI 11 - OVERALL RATES OF HEAT TRANSFER

transfer to no.1 is $\dot{q}_1$ and to no.i is $\dot{q}_i$. The mean heat flux over the surface of no.1 is given by

$$\dot{q}_1'' = \frac{\dot{q}_1}{\pi r_1^2}$$

The mean heat flux over the annulus by which calorimeter no.2 is larger than calorimeter no.1 is

$$\dot{q}_{2-1}'' = \frac{\dot{q}_2 - \dot{q}_1}{\pi (r_2^2 - r_1^2)}$$

Similarly

$$\dot{q}_{i-(i-1)}'' = \frac{\dot{q}_i - \dot{q}_{i-1}}{\pi (r_i^2 - r_{i-1}^2)} \quad \text{--- VI 9}$$

where $\dot{q}_{i-(i-1)}''$ is the mean value of the heat flux which occurs over the annulus lying between r_i and r_{i-1} . The heat flux $\dot{q}_{i-(i-1)}''$ may be considered to exist at some radius r which lies between r_i and r_{i-1} , such that $\pi (r^2 - r_{i-1}^2) = \pi (r_i^2 - r^2)$, giving

$$r = \sqrt{\frac{r_i^2 + r_{i-1}^2}{2}} \quad \text{--- VI 10}$$

Table VI 5 gives the radii of the various calorimeters used, the area over which the heat fluxes $\dot{q}_{i-(i-1)}''$ were determined and the radii r at which the heat fluxes $\dot{q}_{i-(i-1)}''$ were considered to exist.

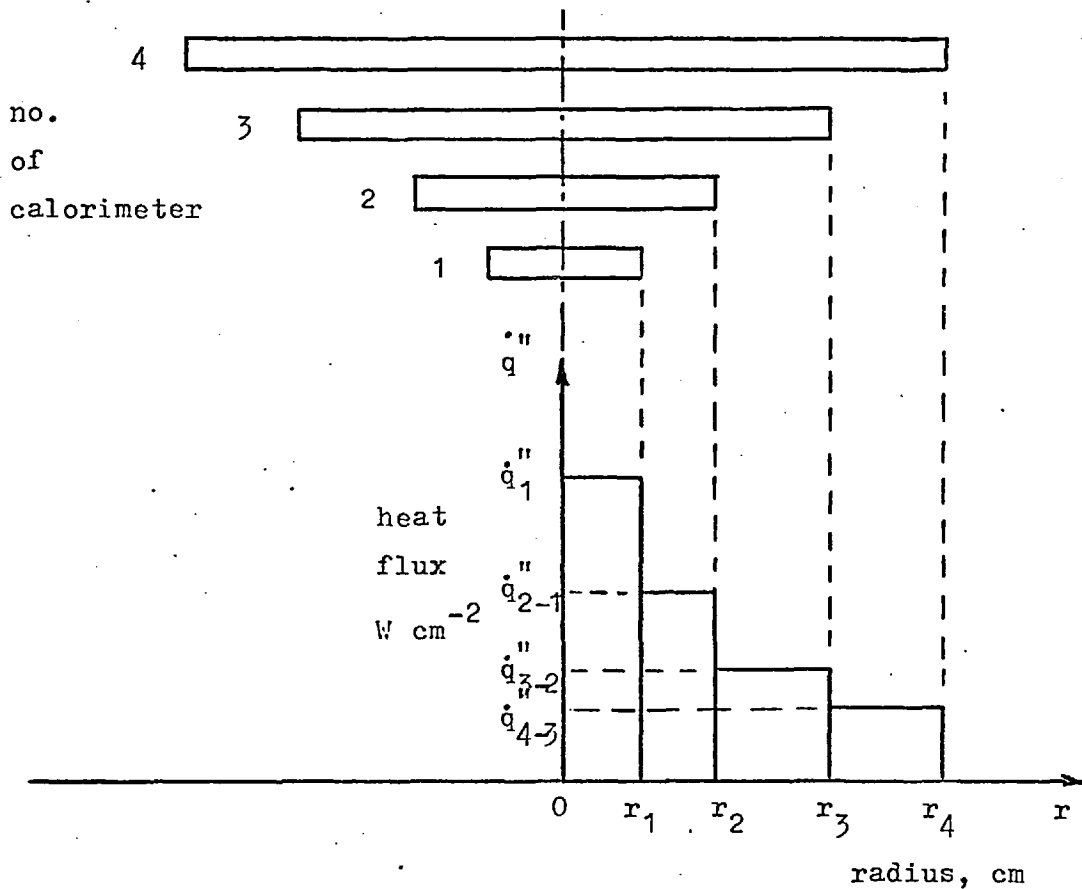


Figure VI 12'

Calorimeter no., i	Calorimeter radii, cm	Area, cm ² $\pi(r_i^2 - r_{i-1}^2)$	Radius, cm. r
1	0.3	0.283	0.21
2	0.65	1.04	0.51
3	1.25	3.58	1.00
4	2.5	14.7	1.98
5	6.5	11.3	4.93

Table VI 5

Distributions of heat flux were determined at distances between the burner and each calorimeter in the range 3.2 mm to 76 mm. The air flow to the burner was varied from 6.9 l min⁻¹ to 12.7 l min⁻¹ for a constant flow rate of methane of 0.9 l min⁻¹. The annular electrode was attached to the burner throughout the investigation and the burner was operated both with and without a.c. electrical augmentation. The temperature differences which were measured by the differential thermocouples in each calorimeter are given in tables A1 to A5 of appendix A.

The rates of heat transfer $\dot{q}_i$ were calculated from the temperature differences and the water flow rates given in tables A1 to A5 using equation VI 7. The heat fluxes $\dot{q}_{i-(i-1)}''$ were obtained from equation VI 9 and the radii at which these fluxes occurred are given in table VI 5. Table A6 in appendix A gives examples of these heat fluxes which have been calculated for various distances between the burner and the calorimeter at selected percentages of methane. Some distributions of heat flux from augmented and unaugmented flames have been obtained from table A6 and have been plotted in figure VI 12. The effect of the distance between the burner and the water-cooled surface on the heat flux is shown in figure VI 13, values of the heat flux having been plotted at the radii given in table VI 5.

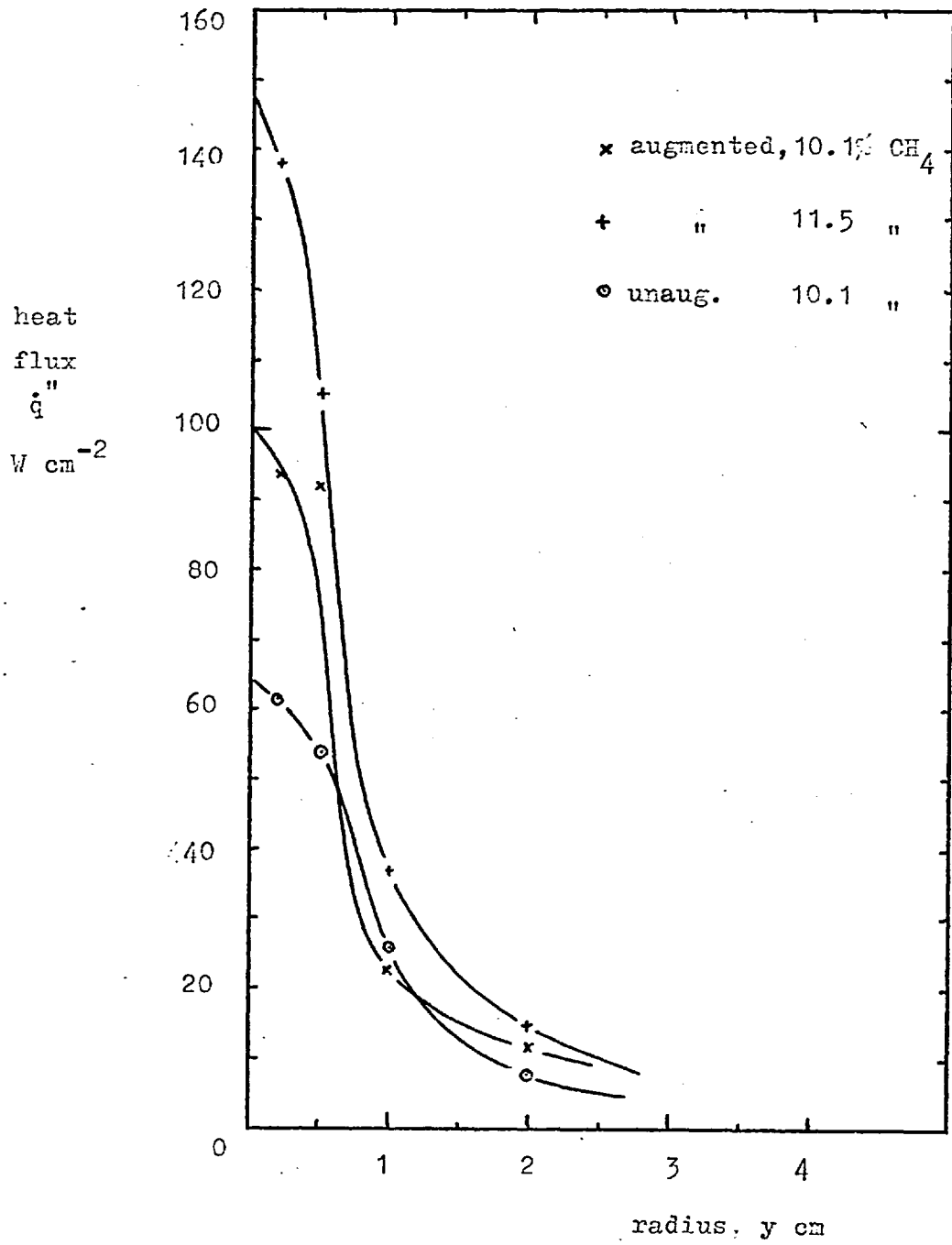


FIGURE VI 12 - HEAT FLUX DISTRIBUTIONS FROM THE SMALL VORTEX-STABILIZED BURNER TO A WATER-COOLED SURFACE

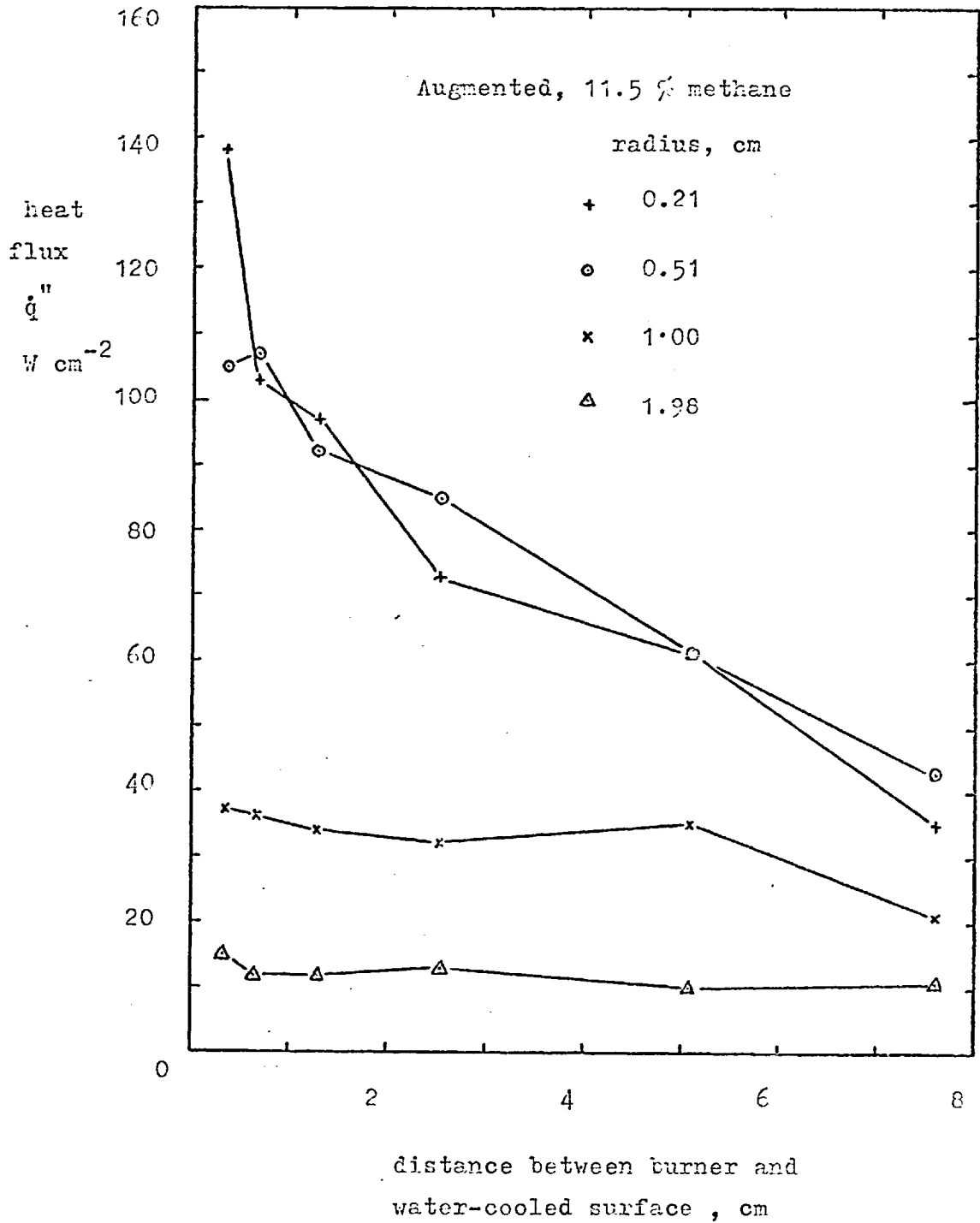


FIGURE VI 13 - EFFECT OF DISTANCE BETWEEN BURNER AND WATER-COOLED SURFACE ON HEAT FLUX

(c) Split plate calorimeter

The split plate calorimeter was used to determine heat fluxes from the large vortex-stabilized burner and is described in sections IV 4 (c) and V 4 (c).

The thermistors which were used to measure the temperature of the inlet and outlet water to each plate of the calorimeter were calibrated in a water bath against a mercury-in-glass thermometer which could be read to $\pm 0.1^{\circ}\text{C}$. The voltage output from each thermistor was read on a digital voltmeter to an accuracy of ± 1 mV. The calibration of voltage against temperature for all the thermistors is given in table E1 of appendix E and a calibration curve for thermistor no.1 is plotted in figure E1 of appendix E.

(i) Distribution of heat flux at a water-cooled surface

The output was obtained from each thermistor when the split plate calorimeter was traversed in steps over the large vortex-stabilized burner in the manner described in section V 4 (c). The following operating conditions were employed:-

Methane and air flow rates as given in table VI 1 for the unaugmented and augmented flames.

Electrical augmentation of 0A, 1.2A and 3.0A at 1 kV.

Burner-to-plate distances of 1.0, 1.5, 2.0 and 3.0 cm.

Step lengths of 2.0 mm and 2.6 mm.

Water flow rate to the calorimeter of 2.75 l min^{-1} .

The results are given in tables E2 to E11 in appendix E where thermistor outputs are shown for each step advanced by the calorimeter. The method of calculating the distribution of heat flux at the water-cooled surface from these results is given below, using the results in table E2 as an example. Table E2 was chosen as it represents the least accurate case, the measured temperature differences being the lowest in this experiment because the flame was not augmented and was at the greatest distance (3.0 cm) from the plate.

The thermistor outputs are converted to degrees centigrade using the calibration data given in table E1 of appendix E. The increase in temperature $\Delta\theta$ for each plate of the calorimeter is given by:-

$$\text{for plate 1, } \Delta\theta_1 = \theta_1 - \theta_2$$

$$\text{and for plate 2 } \Delta\theta_2 = \theta_4 - \theta_3$$

where θ_1 , θ_2 , θ_3 and θ_4 are the temperatures indicated by thermistors 1, 2, 3 and 4. The temperature differences

$\Delta\theta_1$ and $\Delta\theta_2$ are given in table B 12 of appendix B and are shown plotted against step number in figure VI 14.

Curves have been drawn through the experimental points as shown in figure VI 14. These two curves are symmetrical about the horizontal and vertical lines drawn through the point of intersection C because the heat flux is assumed to be distributed symmetrically about the burner axis. The curve AC provided the values of $(\Delta\theta)_i$ to give $[f_3(x)]_i$ in equation II 17, page 39, i.e.:-

$$[f(r)]_j = \frac{1}{\pi s^2} \sum_{i=1}^n C_{j,i} [f_3(x)]_i \quad \text{--- II 17}$$

where

$$[f_3(x)]_i = (\Delta\theta)_i \times Q \times 4.186 \rho c \quad \text{watts}$$

$$Q = \text{water flow rate cm}^3 \text{ sec}^{-1}$$

$$\rho = \text{density of water g cm}^{-3}$$

$$c = \text{specific heat of water cal g}^{-1} \text{ } ^\circ\text{C}^{-1}$$

For the example chosen, $s = 0.26$ cm, giving

$$[f(r)]_j = 904 \sum_{i=1}^n C_{j,i} (\Delta\theta)_i \quad \text{--- VI 11}$$

The coefficient matrix $C_{j,i}$ is given by Olsen and Nestor⁽³³⁾

where i is the step number in the x direction and j is the step number in the radial direction. The step numbers $i = 0$ and $j = 0$ occur when the division between the two plates coincides with the burner axis, i.e. at position C in figure VI 14.

Temperature
increase
over a
plate

$^{\circ}\text{C}$

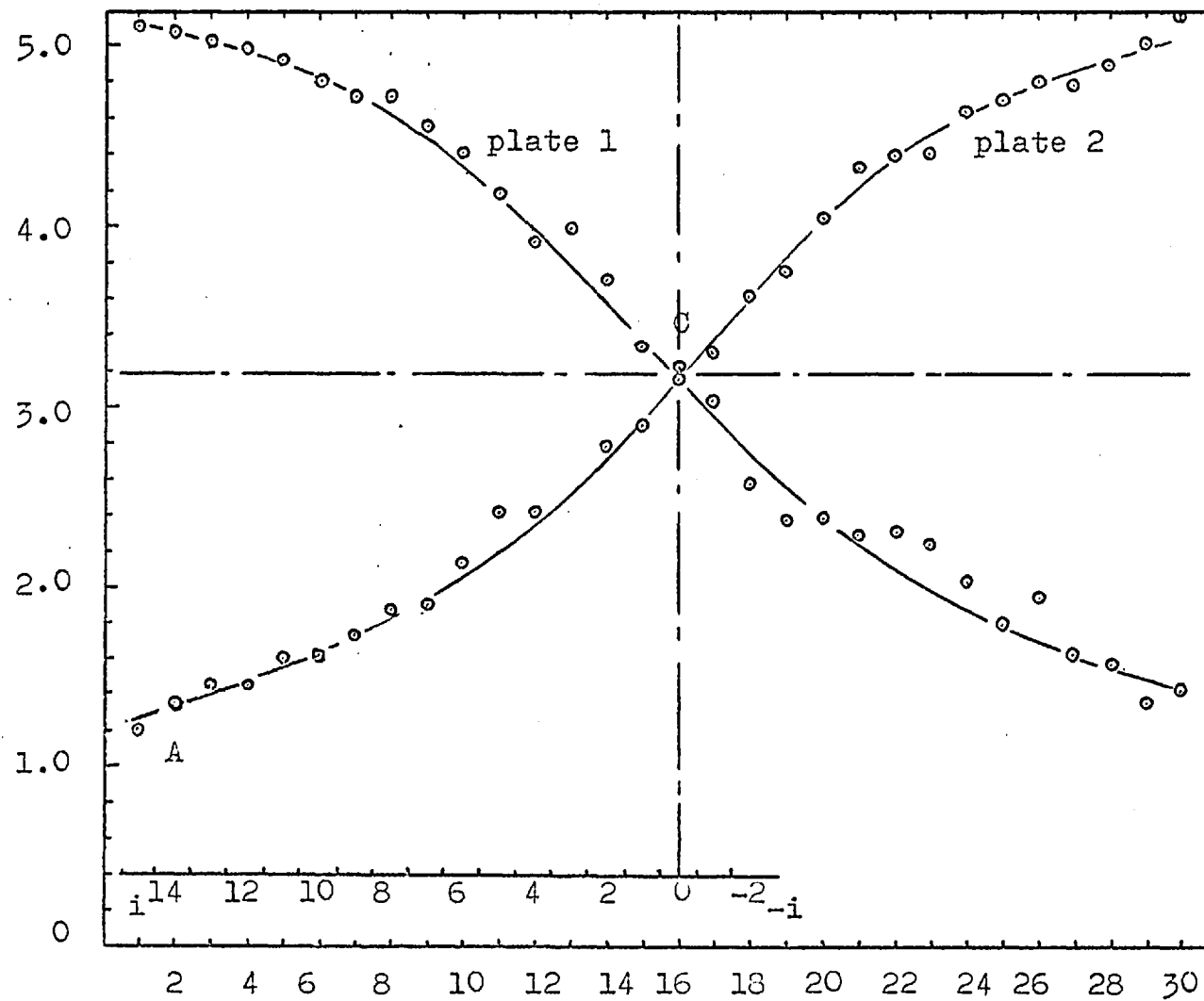


FIGURE VI 14 - INCREASE IN COOLING WATER TEMPERATURE FOR EACH PLATE OF THE CALORIMETER

Step no i	Temperature increase $(\Delta \theta)_i$ °C
- 1	3.40
0	3.15
1	2.95
2	2.75
3	2.55
4	2.35
5	2.15
6	2.00
7	1.90
8	1.75
9	1.65
10	1.55
11	1.45
12	1.35
13	1.30
14	1.20
15	1.10

TABLE VI C - TEMPERATURE INCREASES OBTAINED
FROM CURVE CA OF FIGURE VI 14.

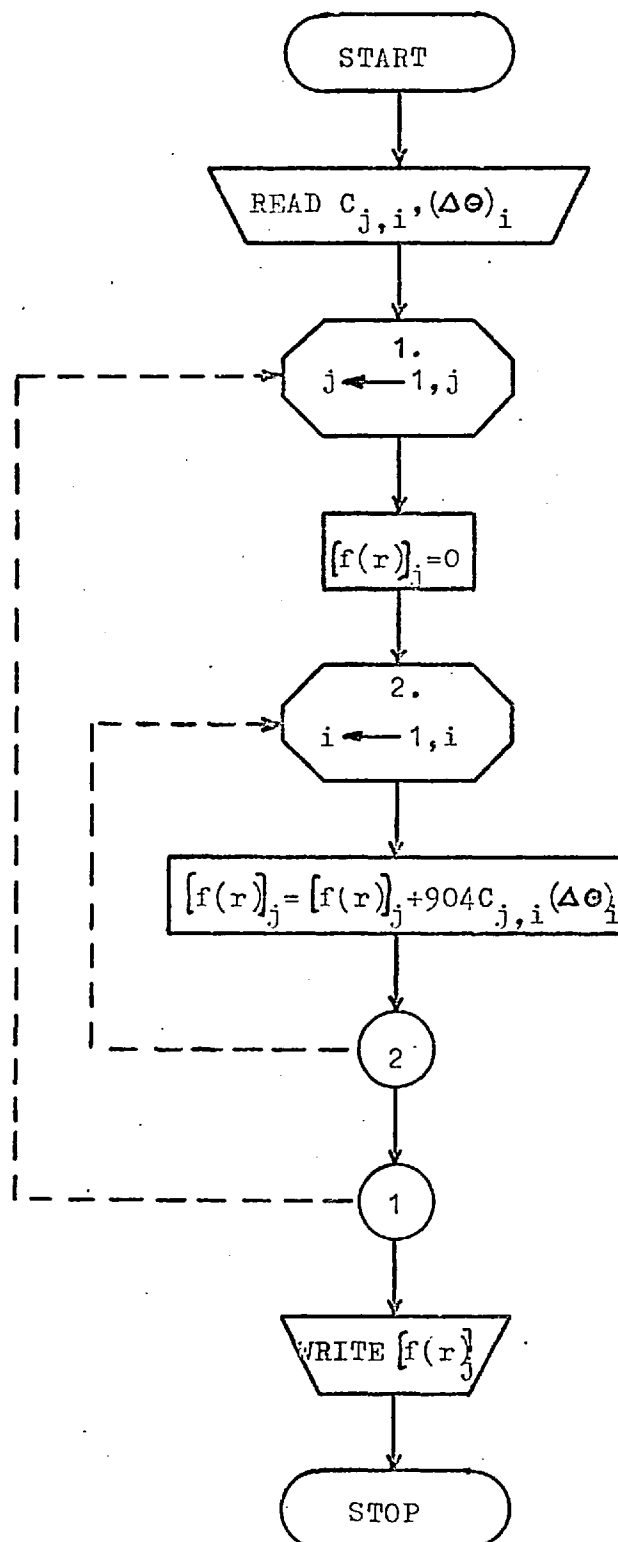


FIGURE VI 14' - FLOW CHART FOR COMPUTER PROGRAMME USED TO SOLVE EQUATION VI 11 FOR HEAT FLUX $[f(r)]_j$

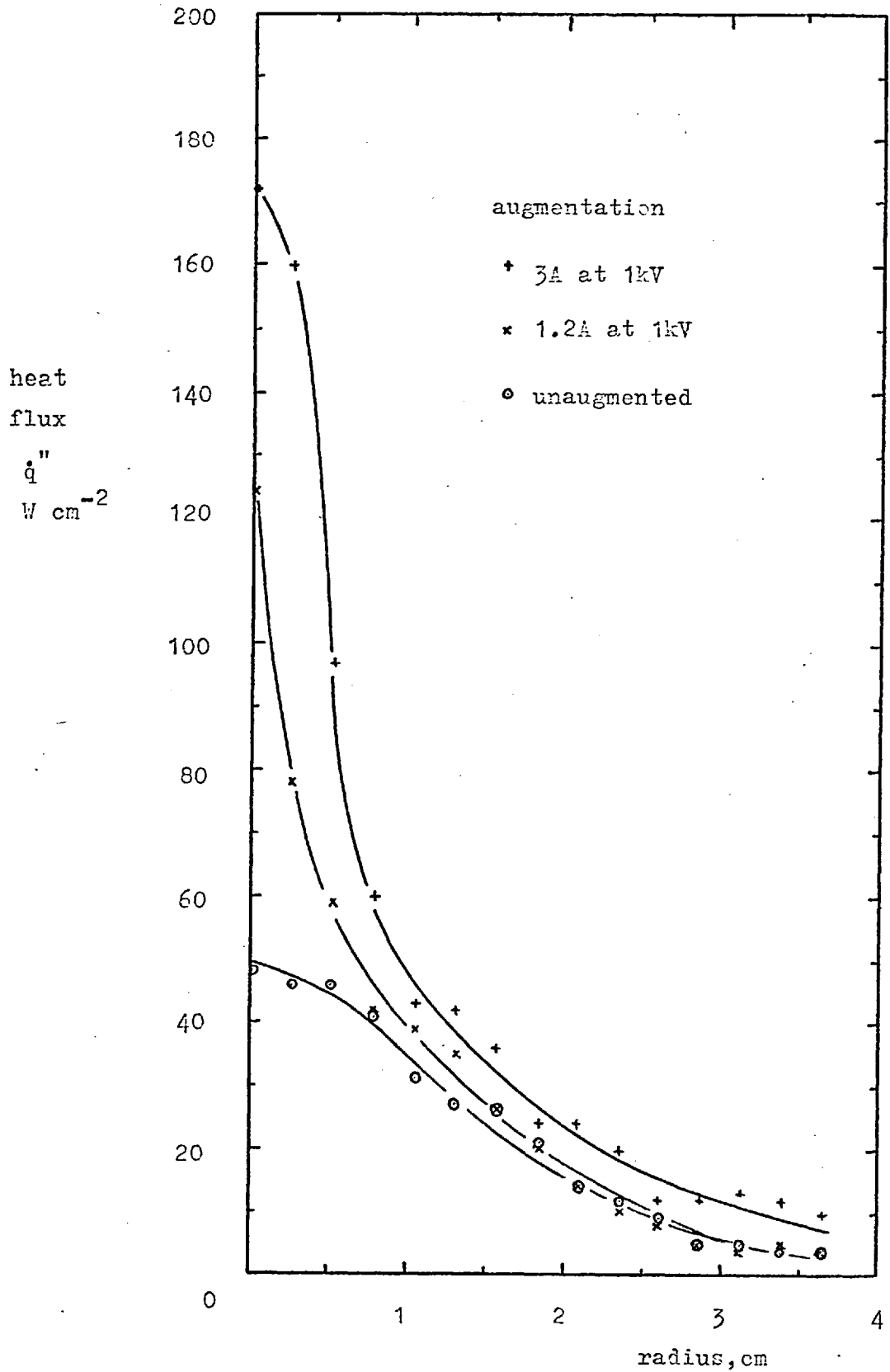


FIGURE VI 15 - HEAT FLUX DISTRIBUTIONS FOR A BURNER TO
CALORIMETER DISTANCE OF 2.0 CM

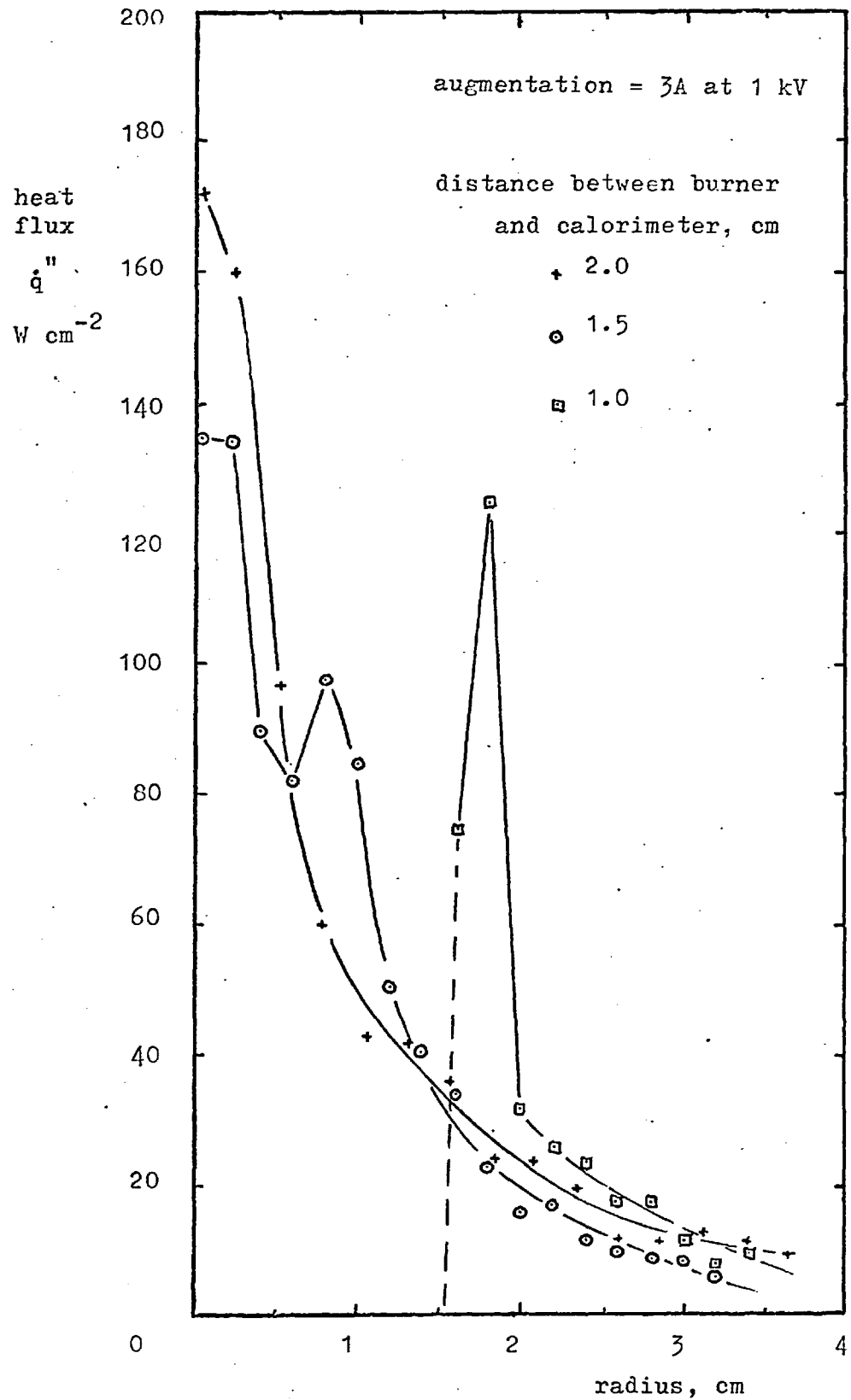


FIGURE VI 16 - HEAT FLUX DISTRIBUTIONS AT VARIOUS BURNER TO CALORIMETER TO DISTANCES

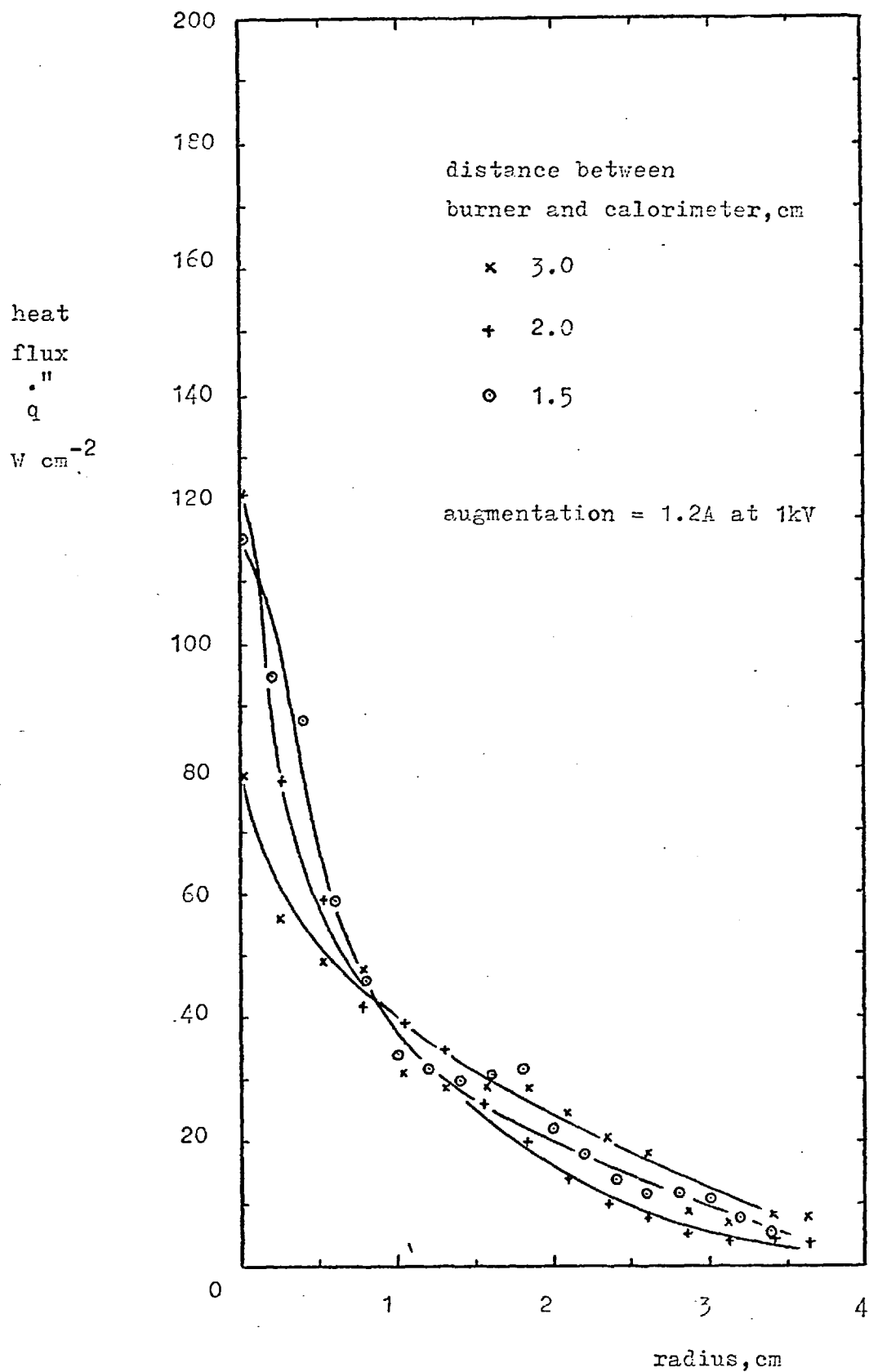


FIGURE VI 17 - HEAT FLUX DISTRIBUTIONS AT VARIOUS BURNER TO CALORIMETER DISTANCES

Values of $(\Delta\theta)_i$ were then obtained from the curve ACD for values of i from -1 to 16, the length of each step being maintained at a constant 0.26 cm. These values are given in table VI 6 and equation VI 11 was solved for $[f(r)]_j$ using a computer programme whose flow sheet is given in figure VI 14. The computed values of the heat flux $[f(r)]_j$ are given in table B 13 of appendix B for all the results quoted in tables B3 to B 11. Figure VI 15 shows the heat fluxes from the augmented and unaugmented flames plotted against radius r when the burner was 2 cm from the surface of the calorimeter. Figures VI 16 and 17 show the heat flux plotted against radius for the augmented flame when the distance between the calorimeter and the burner was varied.

3. HEAT TRANSFER TO THE SURFACE OF A STEEL SLAB

The apparatus and the techniques involved in determining the rates of heat transfer from the large vortex-stabilized burner to a steel slab are described in sections IV 5 (a) and V 5. The temperatures which were measured in the slab were used to determine the distribution of heat flux according to the method developed under "Theory", chapter III

(a) Heat flux distributions obtained from temperatures measured in an insulated element of a steel slab

(i) Temperatures in the slab

The positions of the thermocouples in the insulated element are shown in figure IV 11. The distance between the axis of each couple and the surface is given in table VI 7.

Thermocouple no.	1	2	3	4	5
distance from surface, mm.	0.4	2.2	7.0	17	34

Table VI 7

The large vortex-stabilized burner was operated both with and without electrical augmentation at the gas flow rates given in table VI 1. The level of augmentation was 3A at 1 kV

and the following experimental conditions were employed:-

Slab velocities = 2.62, 4.8 and 7.5 cm min⁻¹

Distance between burner and surface of slab

= 0.5, 1.5 and 3.0 cm

The mV readings from the thermocouples were recorded on paper tape at the rate of one reading per second by the data logger. A printed output was obtained from the paper tape and the mV readings were converted to degrees centigrade. These temperatures were plotted against distance along the surface of the slab and are shown in figures C1 to C7 of appendix C, the details of the experimental conditions being given in table VI 8 for each run.

Figure no.	Augmentation	Slab velocity cm.min ⁻¹	Distance between burner and slab, cm
C1	0	2.62	0.5
C2	0	"	1.5
C3	3A, 1kV	"	0.5
C4	" "	4.8	"
C5	" "	7.5	"
C6	" "	4.8	1.5
C7	" "	"	3.0

Table VI 8

In obtaining the data shown in figures C1 and C2 it was possible to observe approximately when the insulated element was directly over the burner because the flame was not augmented and the door of the earthed cage could be left open (see section IV 2 (b)) allowing access to the burner. The centre line of the insulated element was observed to be coincident with the axis of the burner before the output from the no.1 thermocouple reached a maximum value. The slab was observed to travel another 5 to 10 mm before this maximum value was reached. This type of observation

VI 3 (a)

was impossible with the augmented flame because the cage door had to be locked.

(ii) Heat fluxes

The method of determining the distribution of heat flux at the surface of a slab from the data given in figures C1 to C7 in appendix C requires the use of equations III 3 (a) and (b) which are given on page 65. The data of figure C1 will be used to illustrate the method. The heat flux $\dot{q}''$ can be expressed in terms of the maximum heat flux $\dot{q}''_M$ and the "spread" of the heat flux distribution, "a", as in equation 11 on page 64, where

$$\dot{q}'' = \dot{q}''_M \left[\frac{1}{1 + \left(\frac{y}{a}\right)^2} \right] \quad \text{--- VI 12}$$

On page 65

$$\sqrt{u^*} = \frac{k\theta_A}{\dot{q}''_M} \sqrt{\frac{u}{\alpha a}} \quad \text{--- VI 13}$$

giving

$$\dot{q}''_M \sqrt{a} = k\theta_A \sqrt{\frac{u}{\alpha u^*}} \quad \text{--- VI 14}$$

Equation III 3 (b) and figure III 7 both show that

$$\theta_S^* \sqrt{u^*} = \frac{\theta_S}{\theta_A} \sqrt{u^*} = 1.65 \text{ at } \frac{y}{a} = 0.4 \quad \text{--- VI 15}$$

where $\theta_S = (\theta_S)_M$, the maximum value of the surface temperature θ_S . The data of figure C1 can be extrapolated to give $(\theta_S)_M$ as shown in figure VI 18; -

$$\begin{aligned} (\theta_S)_M &= 580^\circ\text{C} - \text{initial slab temperature} \\ &= 560^\circ\text{C} \end{aligned}$$

The temperature θ_A is an arbitrary constant which will be chosen as the austenitising temperature in order to relate this analysis to flame hardening. Figure II 28 indicates that an austenitising temperature of 825°C requires an austenitising time of a few seconds which is the length of time required by the flame hardening process. Hence,

$$\begin{aligned} \theta_A &= 825^\circ\text{C} - \text{initial slab temperature} \\ &= 805^\circ\text{C} \end{aligned}$$

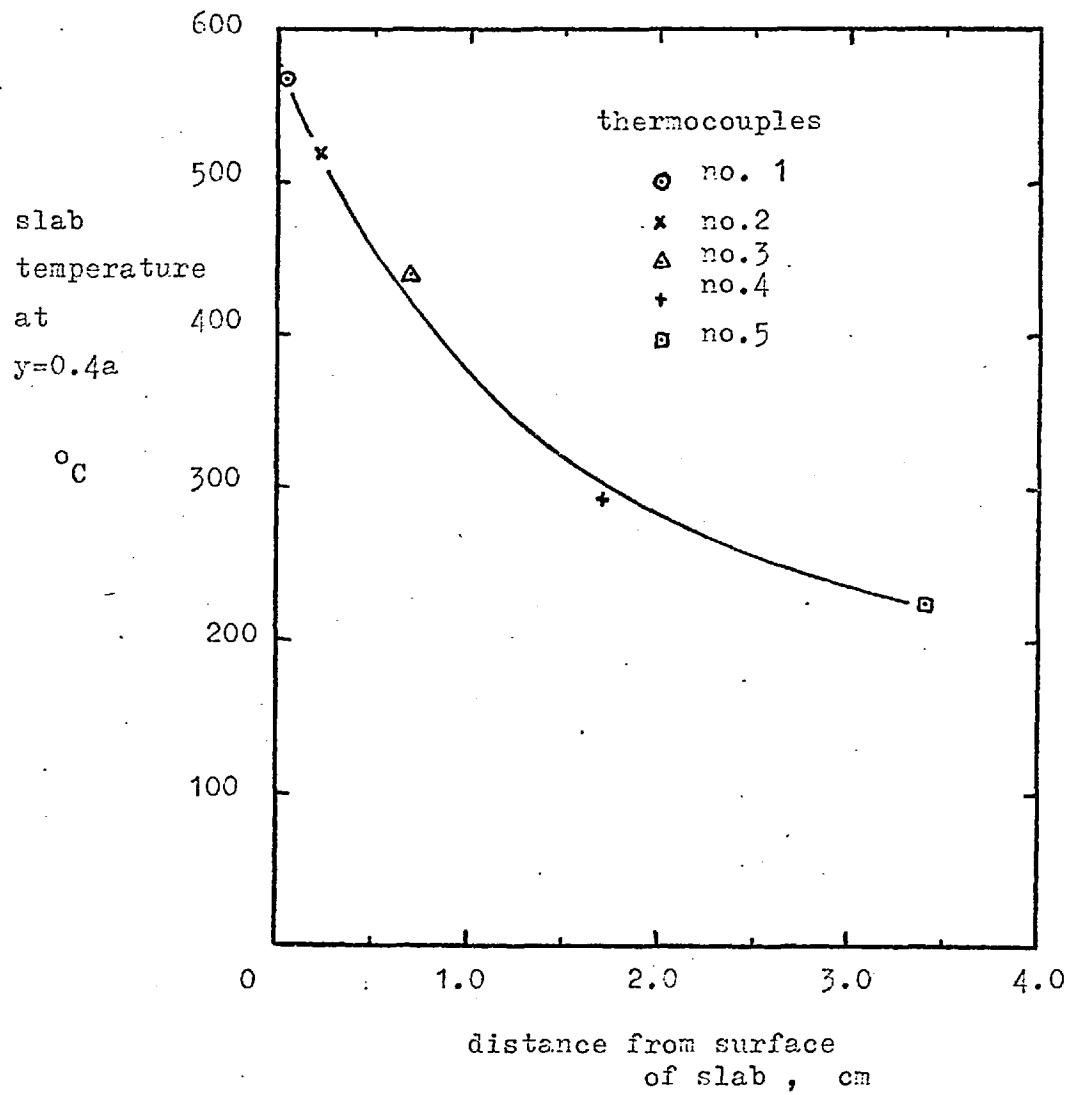


FIGURE VI 18 — TEMPERATURE PROFILE IN THE SLAB
FOR RUN C1

Substituting these values of $(\theta_s)_y$ and θ_A in equation VI 15 gives

$$u^* = 2.36 \text{ at } \frac{y}{a} = 0.4$$

and equation VI 13 shows that u^* is constant for all values of y .

A flame-hardenable steel, En 8, was used in all the experiments. Values of the physical properties k , ρ and c for En 8 are given in table C1 of appendix C. The method used to derive equation III 3 (a), (b) and (c) assumed that k , ρ and c remained independent of slab temperature θ . The following values will be used in all further calculations:-

$$k = 0.100 \text{ cal } ^\circ\text{C}^{-1} \text{ cm}^{-1} \text{ sec}^{-1} \text{ (value at } 400^\circ\text{C)}$$

$$\rho = 7.80 \text{ g cm}^{-3}$$

$$c = 0.159 \text{ cal } ^\circ\text{C}^{-1} \text{ g}^{-1} \text{ (mean value from } 20^\circ\text{C to } 800^\circ\text{C)}$$

$$\text{and } \alpha = \frac{k}{\rho c} = 8.06 \times 10^{-2} \text{ cm}^2 \text{ sec}^{-1}$$

In the example chosen, the slab velocity $u = 0.0437 \text{ cm sec}^{-1}$

Substituting these values of k , θ_A , u , α and u^* in equation VI 14 gives

$$\dot{q}_M \sqrt{a} = 105 \text{ W cm}^{-\frac{3}{2}} \quad \text{-----} \quad \text{VI } 16$$

Figure VI 18 shows that the surface temperature is approximately equal to the temperature recorded by the no.1 thermocouple and so θ_s can be obtained directly from the temperatures recorded by the no.1 thermocouple at any selected values of y in figure C1 where y is expressed in terms of "a" using equation VI 15, e.g.

at $y = 0.4 a$ cm, $\theta_s = 560^\circ\text{C}$ and at

$y = (0.4 a - 1.0) \text{ cm}$, $\theta_s = 480^\circ\text{C}$. Values of θ_s^* and consequently $\theta_s^* \sqrt{u^*}$ can be calculated from θ_s , using

$$\theta_s^* = \frac{\theta_s}{\theta_A} \text{ and } u^* = 2.36$$

For each value of $\theta_s^* \sqrt{u^*}$ there is a corresponding value of y^* given in figure III 7. A value for "a" can be calculated by equating this value of y^* to the appropriate value of y , using the relation

$$y^* = \frac{y}{a}$$

Table VI 9 shows values of y at intervals of 1.0 cm and corresponding values of θ_s , θ_s^* , $\theta_s^* \sqrt{u^*}$ and y^* , together with the calculated values of "a".

y cm	θ_s °C	θ_s^*	$\theta_s^* \sqrt{u^*}$	y^* from fig, <u>III</u> 1	$a = \frac{y}{y^*}$ cm
0.4a-4.0	90	0.112	0.264	-2.35	1.46
0.4a-3.0	155	0.192	0.454	-1.77	1.38
0.4a-2.0	290	0.360	0.850	-0.90	1.54
0.4a-1.0	480	0.596	1.41	-0.20	1.67
0.4a	560	0.695	1.64	0.40	-
0.4a+1.0	470	0.584	1.38	1.60	0.83
0.4a+2.0	375	0.465	1.10	3.05	0.76
0.4a+3.0	320	0.397	0.936	4.35	0.76
0.4a+4.0	295	0.366	0.866	5.00	0.87

Table VI 9

Values of "a" were obtained in a similar manner from the data given in figures C2 to C7. Figures C6 and C7 do not show temperatures from the no.1 thermocouple because it had failed. Surface temperatures in these cases were obtained by extrapolating the temperatures measured by the remaining thermocouples. A complete set of values of 'a' is given in table C2.

The value of $\dot{q}_s''$ has been calculated from equation VI 16, using mean values of 'a' at $y < 0.4a$ and $y > 0.4a$. The results are summarised in table VI 10. Figure VI 19 shows the experimentally determined surface temperature for run C1 compared with the calculated surface temperatures for both the values of 'a' given in table VI 10. For reasons to be stated later under "discussion" in chapter VII, the mean values of 'a' which have been obtained when $y < 0.4a$ will be used in all further work.

The heat flux $\dot{q}''$ can be calculated, using equation VI 12 and the appropriate values of 'a' and $\dot{q}_s''$ from table VI 10. Figures VI 20, 21 and 22 show the effects of augmentation, slab speed and slab to burner distance on the distribution of heat flux.

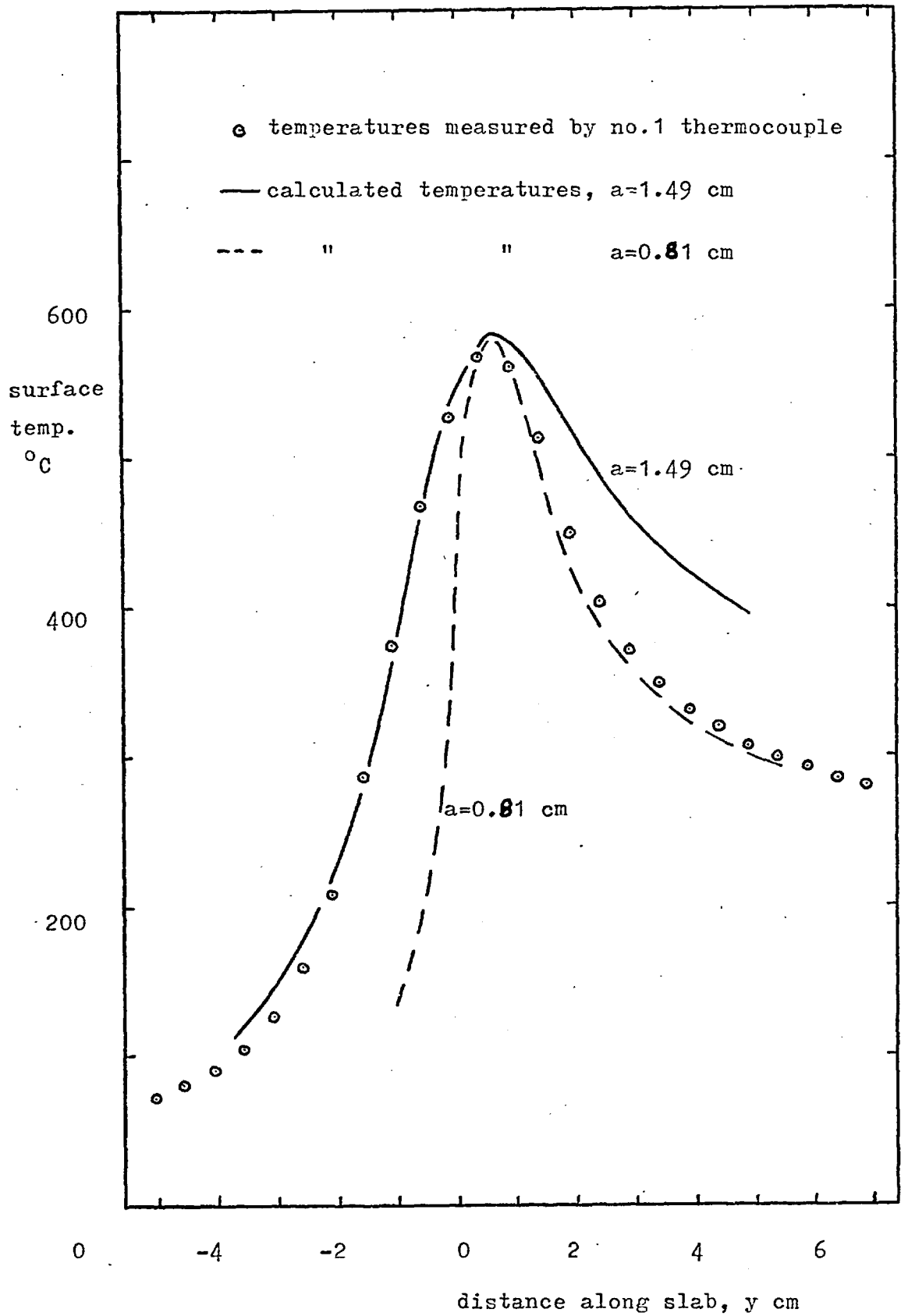


FIGURE VI 19 - COMPARISON BETWEEN EXPERIMENTALLY DETERMINED SURFACE TEMPERATURES AND CALCULATED SURFACE TEMPERATURES

VI 3(a)

	Figures 01 to 07 of appendix C						
	01	02	03	04	05	06	07
Slab speed cm min^{-1}	2.62	2.62	2.62	4.8	7.5	4.8	4.8
Distance between burner and calorimeter, cm	0.5	1.5	0.5	0.5	0.5	1.5	3.0
Augmentation at 1kV, amps	0	0	3.0	3.0	3.0	3.0	3.0
Mean "a" at $y < 0.4a$ cm	1.49	1.52	1.09	1.11	0.89	0.97	1.16
Mean "a" at $y > 0.4a$ cm	0.81	0.81	0.56	0.55	0.60	0.59	0.59
$\dot{q}_M$ for $y < 0.4a$ W cm^{-2}	86	81	165	175	204	165	151
$\dot{q}_M$ for $y > 0.4a$ W cm^{-2}	134	128	231	248	249	211	212

TABLE VI 10 - Values of $\dot{q}_M$ and a.

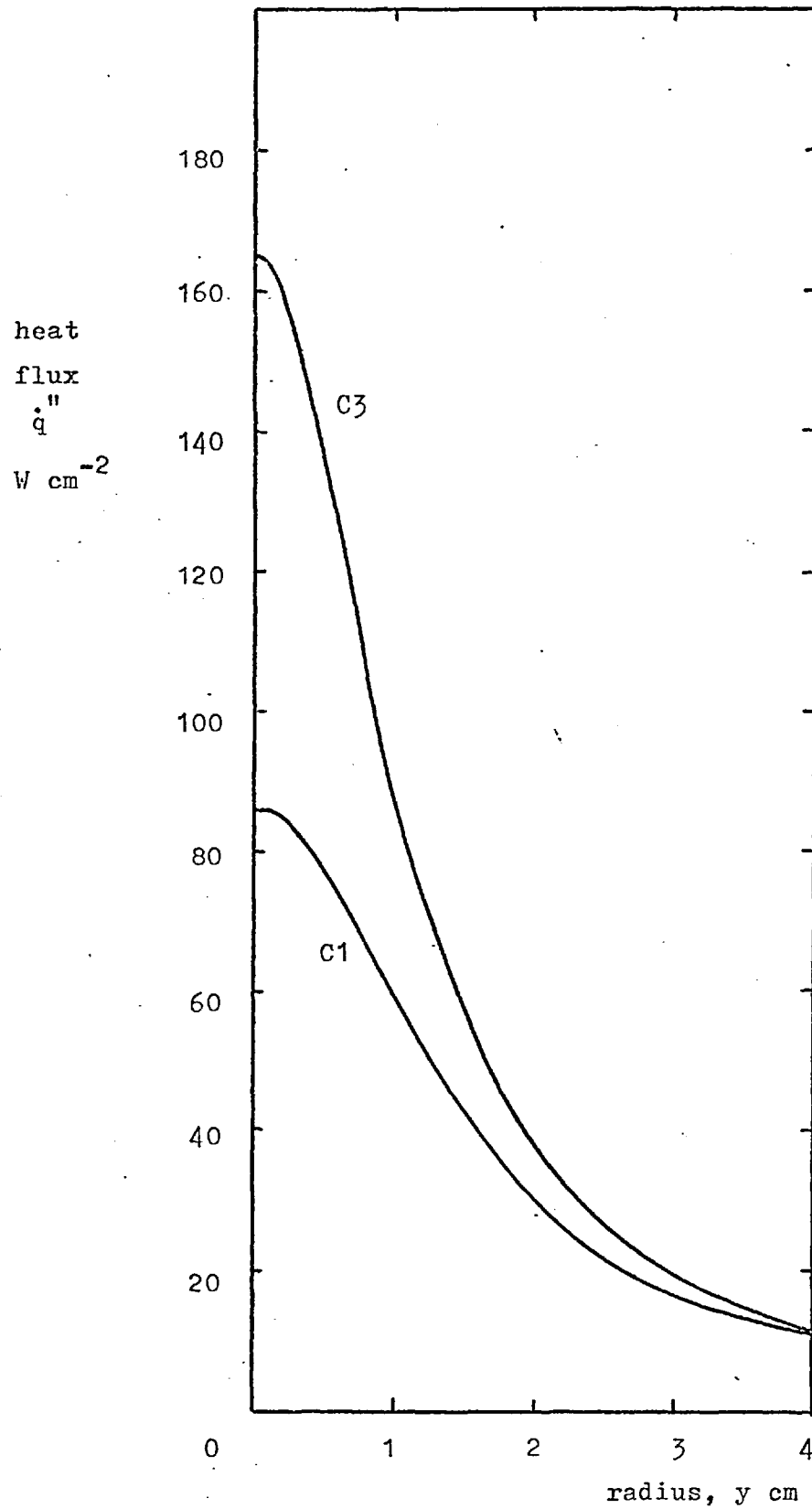


FIGURE VI 20 - HEAT FLUX DISTRIBUTIONS

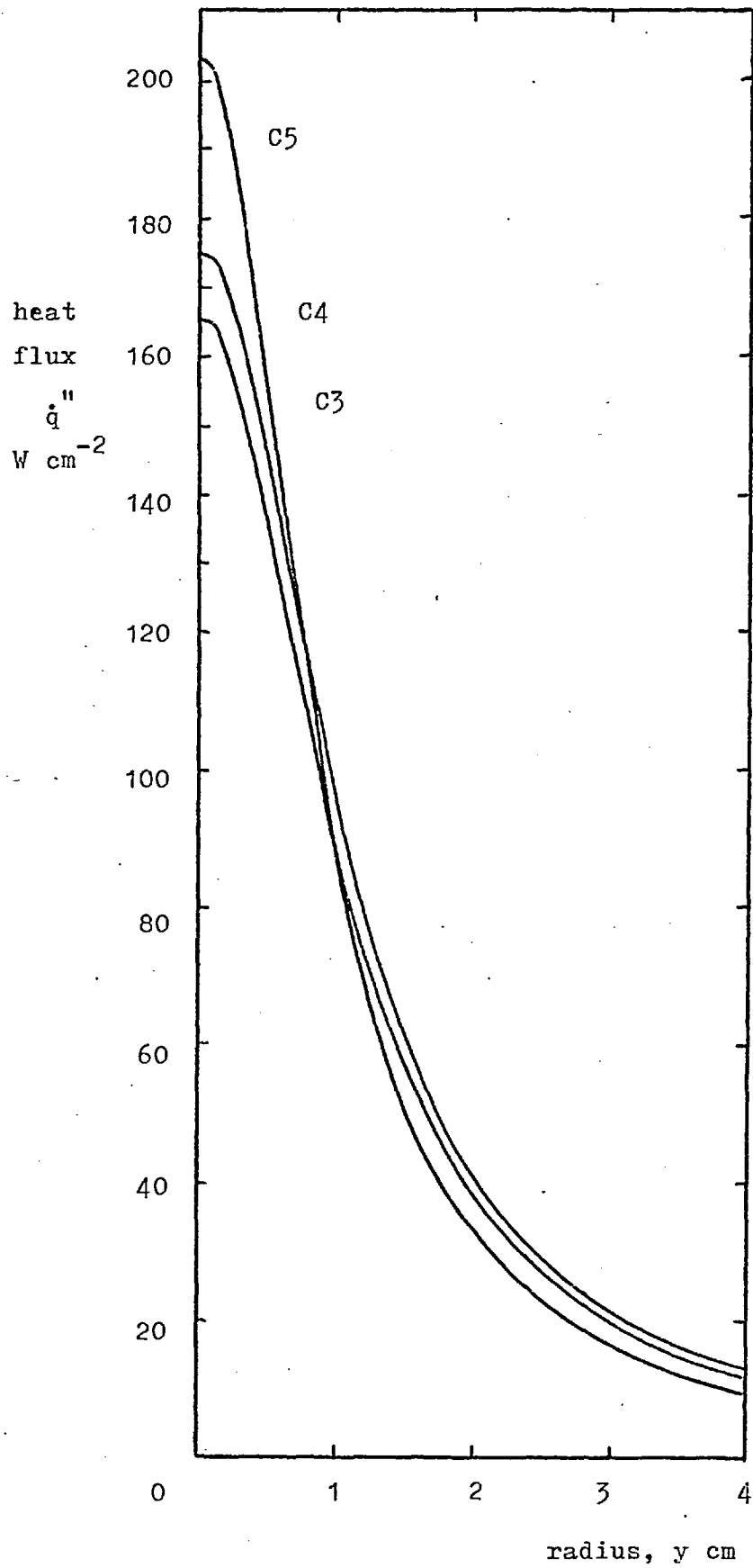


FIGURE VI 21 - HEAT FLUX DISTRIBUTIONS

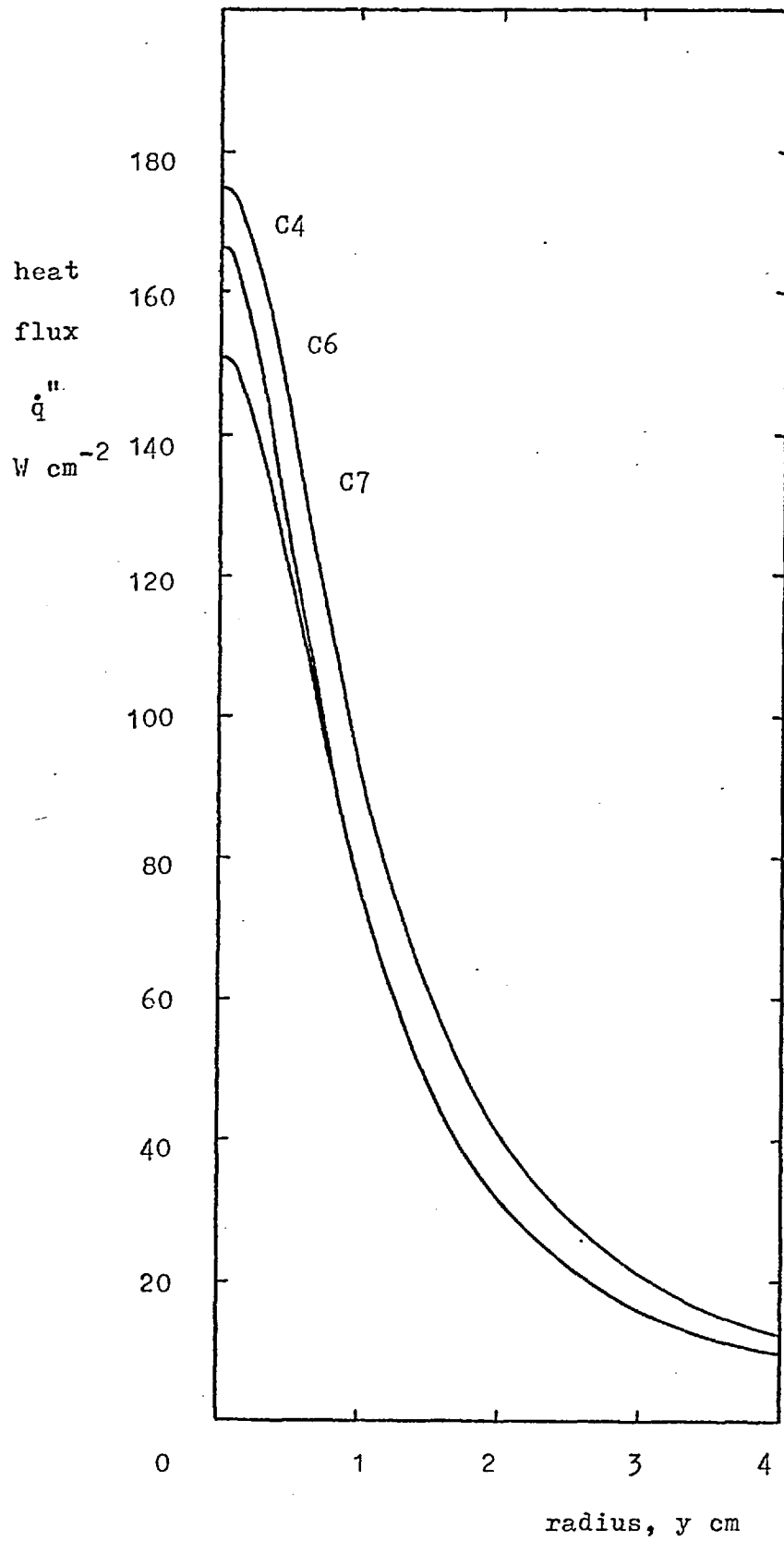


FIGURE VI 22 - HEAT FLUX DISTRIBUTIONS

(iii) Temperature profiles in the slab

Equation 7, page 64, relates the temperature θ at a distance x in the slab to the heat flux $\dot{q}''$ and the penetration distance δ . The distance δ can be found from equation 8 on page 64 enabling the temperature profile in the slab, θ against x , to be determined. The penetration distance δ can be readily obtained when $y = 0$. In this case, equation 8 becomes

$$\delta^2 = \frac{6\alpha}{u\dot{q}''} \int_{-\infty}^0 \dot{q}'' dy \quad \text{--- VI 17}$$

Substituting for $\dot{q}''$ from equation VI 12 gives

$$\delta^2 = \frac{6\alpha}{u\dot{q}''_M} \int_{-\infty}^0 \frac{\dot{q}''_M}{1 + \left[\frac{y}{a}\right]^2} dy$$

$$= \frac{6\alpha a}{u} \cdot \frac{\pi}{2}$$

$$\delta_{y=0} = \sqrt{\frac{3\pi\alpha a}{u}} \quad \text{--- VI 18}$$

Table VI 11 gives values of δ for the various slab speeds used.

Slab speed cm min ⁻¹	Penetration distance $\delta_{y=0}$ cm
2.62	4.2√a
4.8	3.1√a
7.5	2.5√a

TABLE VI 11

When $y = 0$, equation 7 becomes

$$\theta = \frac{\frac{q''}{M}}{2k\delta} (\delta - x)^2 \quad \text{--- VI 19}$$

The slab temperature θ at $y = 0$ can be obtained from equation VI 19 for values of $x \leq \delta$, where δ is given by equation VI 18.

These slab temperatures are given in table C3 of appendix C for runs C1 to C7 and have been calculated using the appropriate value of $\dot{q}_M''$ obtained from table VI 10. The measured slab temperatures were obtained from the data given in figures C1 and C3 and are compared with the calculated temperatures in figure VI 23.

(b) Temperatures measured in a slab without an insulated element

These slab temperatures were measured by a thermocouple which was positioned near the surface as described in sections IV 5(a) and V 5. Unlike the set-up required to obtain the results given in section VI 3(a)(i) and shown in figures C1 to C7 of appendix C, the thermocouple was not placed in an insulated element of the slab but was positioned in the main body of the slab.

Figure VI 24 shows the temperature measured by a thermocouple positioned at 0.7 mm from the surface of the slab and the temperatures measured by the no.1 thermocouple in the insulated element under identical experimental conditions, i.e.

Slab speed	= 2.62 cm.min ⁻¹
Methane flow rate	= 7.5 l min ⁻¹
Air flow rate	= 55 l min ⁻¹
Augmentation	= 3A at 1 kV
Distance between burner and slab surface	= 0.5 cm

The temperatures measured in the insulated element are also given in figure C3. The no.1 thermocouple was positioned at 0.4 mm from the surface of the insulated element.

A further test was carried out under the same experimental conditions except that the slab was equipped with three thermocouples, all positioned within 1 mm of the surface of the slab

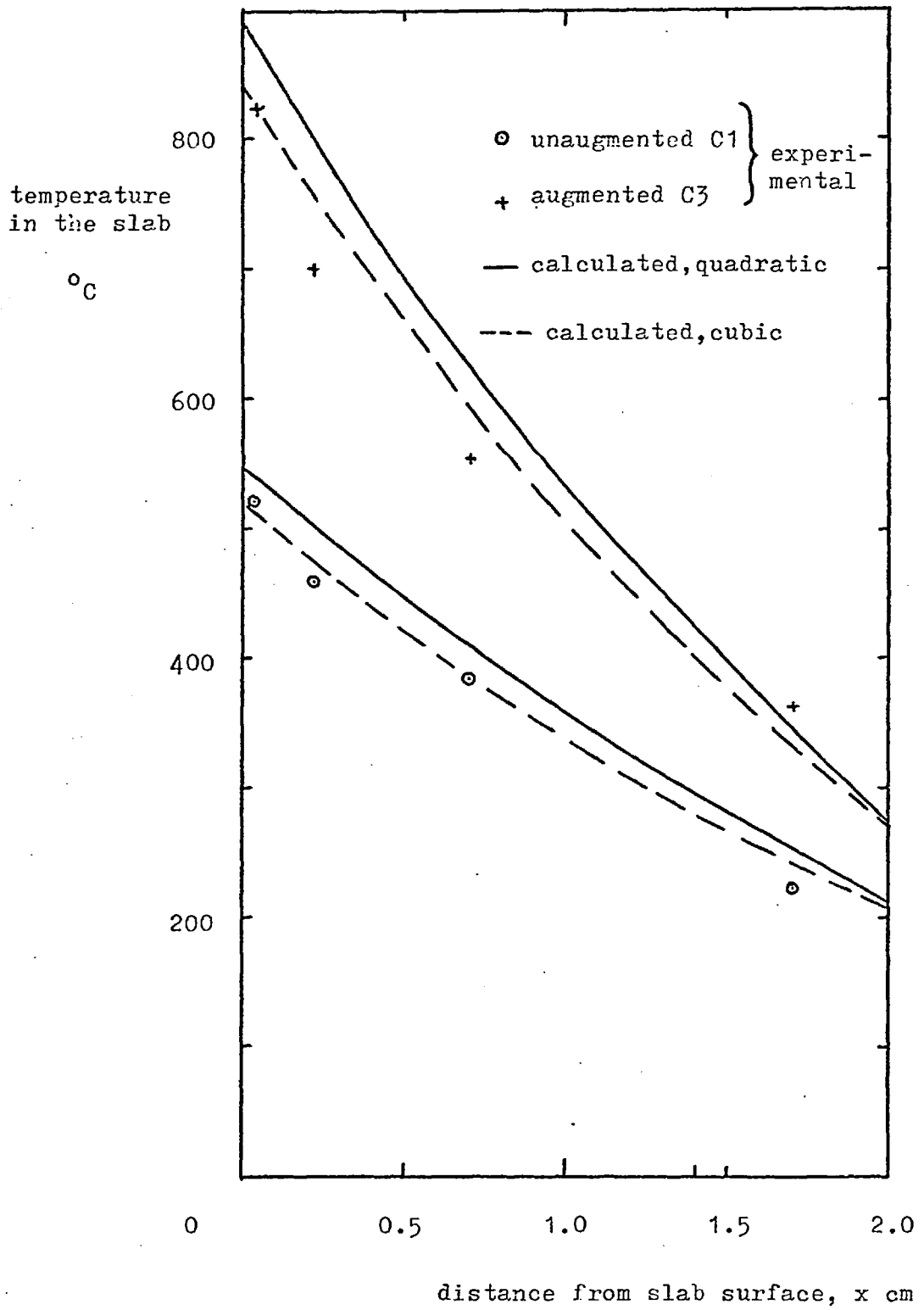


FIGURE VI 23 - TEMPERATURE PROFILES IN THE SLAB ON THE BURNER
AXIS (AT Y=0)

VI 3(a)

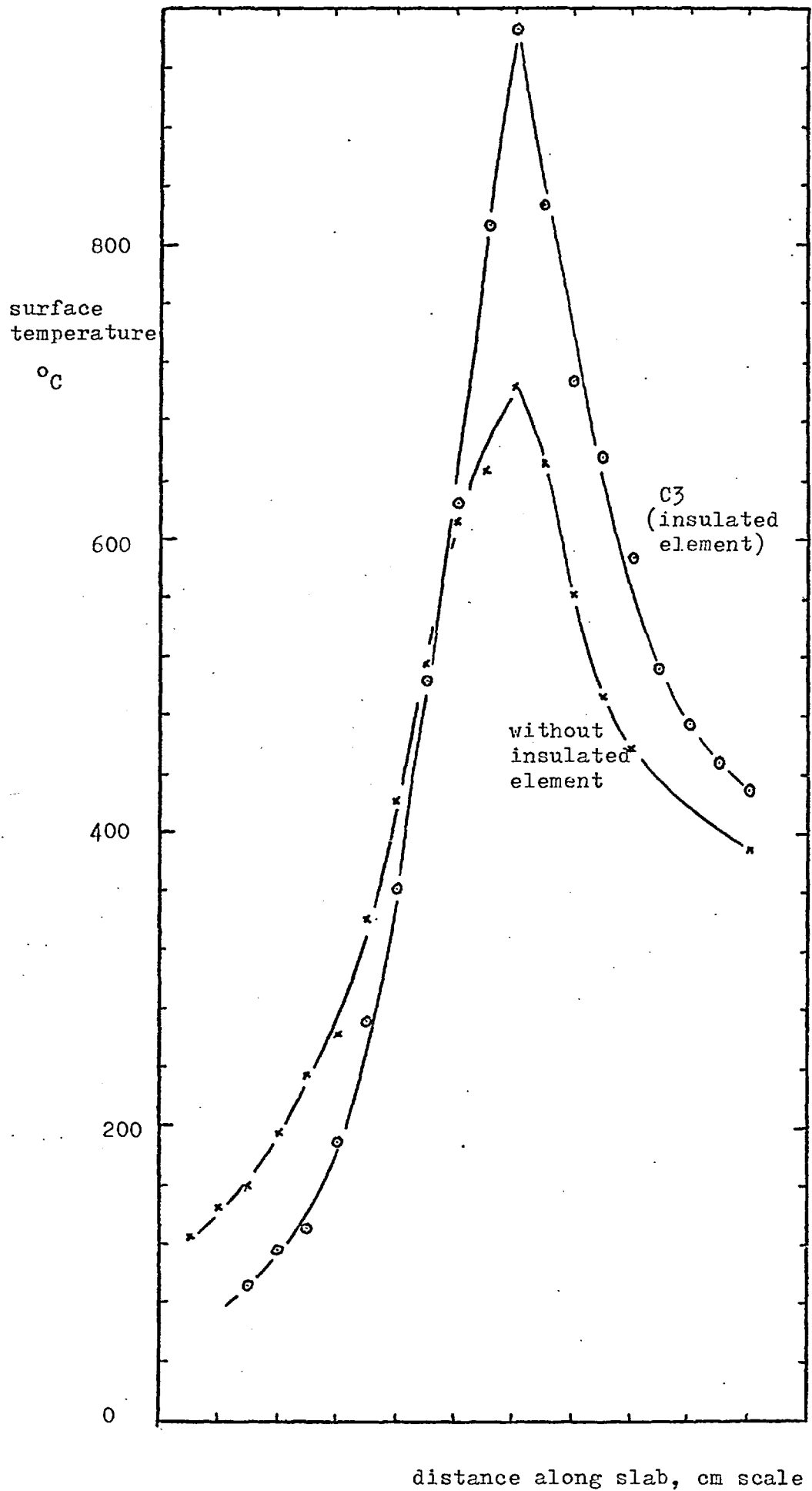


FIGURE VI 24 - SURFACE TEMPERATURES

and separated from each other by 5 cm measured lengthwise along the slab. The first of the three thermocouples was situated at a distance of 20 cm from the leading end of the slab. The temperature profiles which were obtained from the three thermocouples all lay within $\pm 15^{\circ}\text{C}$ of one another and within $\pm 15^{\circ}\text{C}$ of the temperature profile obtained from the slab without the insulated element (see above).

4. QUENCHING THE SURFACES OF SLABS

The quench arrangements are described in section VI 5 and shown in figure IV 12 and plate VI 4. The following experimental conditions were employed:-

Distance between slab surface and burner	= 0.5 cm
Electrical augmentation	= 3.5 A at 1 kV
Burner airflow	= 55 l min ⁻¹
" methane flow	= 7.5 l min ⁻¹
Slab speeds	= 1.45, 2.62 and 4.80 cm min ⁻¹
Slab cross-sections	= 50x12, 25x12, 12x12 mm ² , the 12 mm wide surface being quenched.

Both of the nozzles described on page 92 were used at various burner to nozzle distances and a range of water flow rates. These nozzles will subsequently be referred to as the slotted nozzle and the five hole nozzle.

(a) Slotted nozzle

Figures VI 25, 26 and 27 show the effects of altering

- 1 Slab cross sectional area
- 2 Water flow rate
- 3 Burner to quench distance

on the surface temperature of the slab. These "surface temperature" profiles were obtained from a thermocouple which was inserted in each slab within 1 mm of the surface of the slab. The experimental conditions are detailed on each figure.

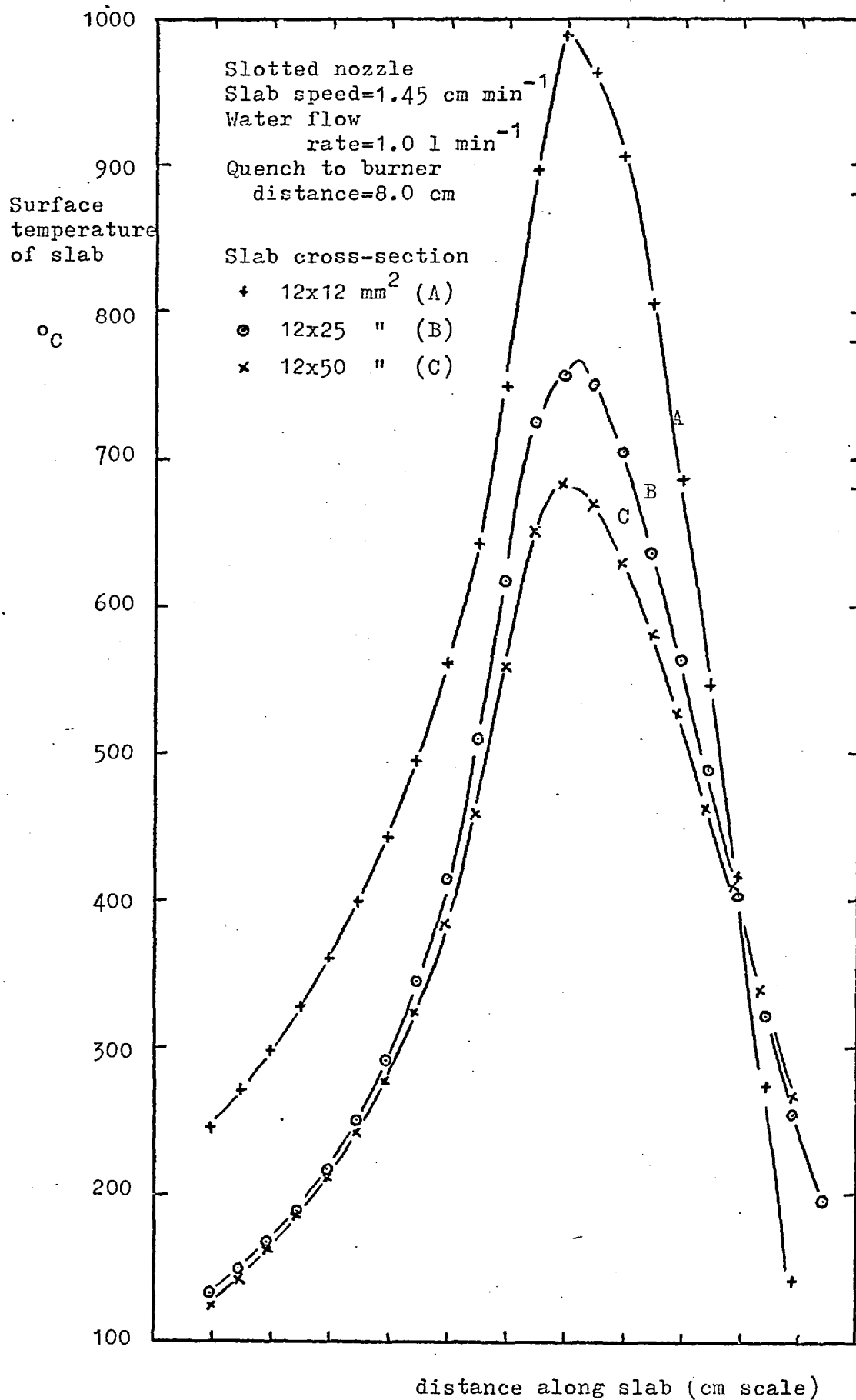


FIGURE VI 25 - EFFECT OF ALTERING SLAB DEPTH ON THE SURFACE TEMPERATURE PROFILE OBTAINED DURING QUENCHING

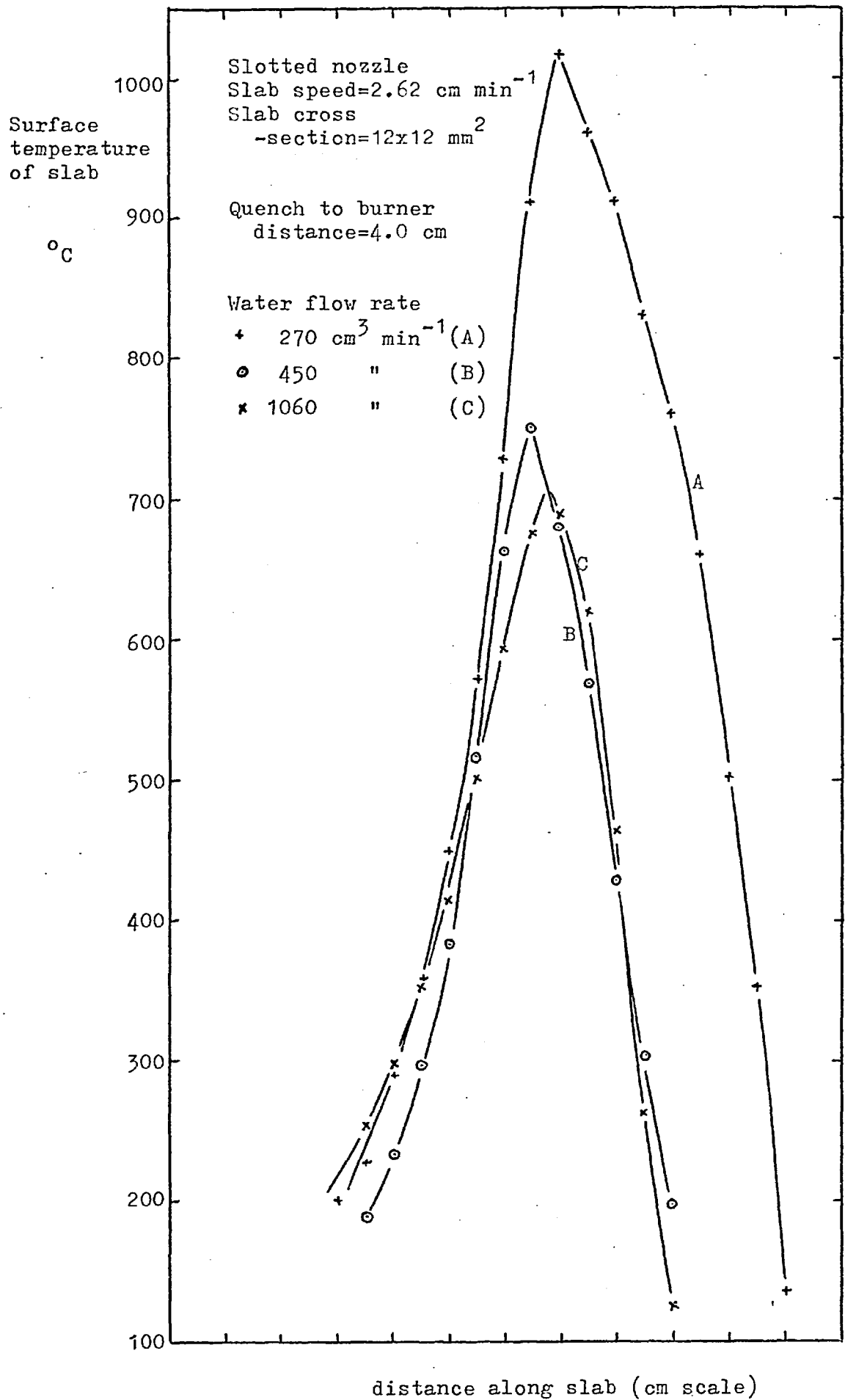


FIGURE VI 26 - EFFECT OF THE FLOW RATE OF QUENCH WATER ON THE SURFACE TEMPERATURE OF THE SLAB

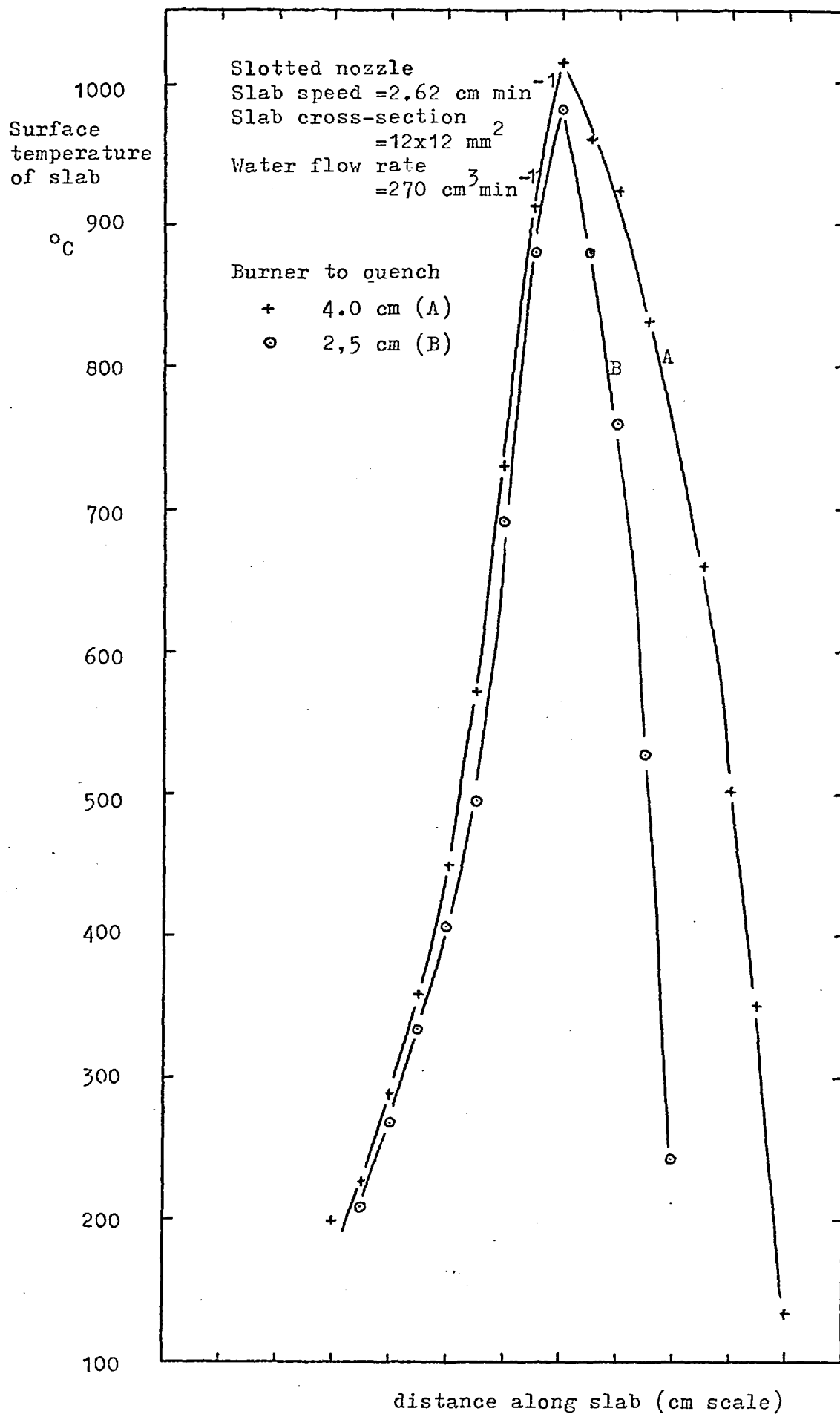


FIGURE VI 27 - EFFECT OF ALTERING BURNER TO QUENCH DISTANCE ON SURFACE TEMPERATURE PROFILE

(b) Five hole nozzle

Figure VI 28 shows the temperature profile which was measured by a thermo-couple positioned within 1 mm of the surface of a slab when the five hole nozzle was used to quench the slab. The flow rate of water to the nozzle was maintained at the minimum value which allowed five separate jets of water to impinge on the slab without coalescing. The experimental conditions are detailed on the figure.

(c) Experimental observations

- 1 When the quench water flow rate was high and/or the quench was placed very close to the burner, water tended to run along the surface of the slab into the area where the arc-heated gas impinged on the slab.
- 2 All the results given above were obtained when water from the quench covered the entire width of the slab.

(d) Rate of quenching

The rate of quenching can be defined as the rate of cooling of the surface of the slab in $^{\circ}\text{C sec}^{-1}$. Approximate values of the rates of quenching occurring during the above experiments can be obtained from figures VI 25 to 28. These rates of quenching are approximately constant because the cooling curves in figures VI 25 to 28 are linear over most of their length. Hence,

$$\text{rate of quenching } (^{\circ}\text{C sec}^{-1}) = \frac{\text{gradient of linear portion of cooling curve } (^{\circ}\text{C cm}^{-1}) \times \text{slab speed (cm sec}^{-1}\text{)}}{1}$$

(e) Quench heat flux

An approximate value of the quench heat flux can be determined if the burner heat flux distribution and the rate of quenching are known. The quench heat flux will be calculated for a constant rate of quenching of $20^{\circ}\text{C sec}^{-1}$ at a slab speed of 2.62 cm min^{-1} .

Assume that the burner heat flux distribution is identical to that obtained from run C3. Table VI 10 on page 150 gives the following information:-

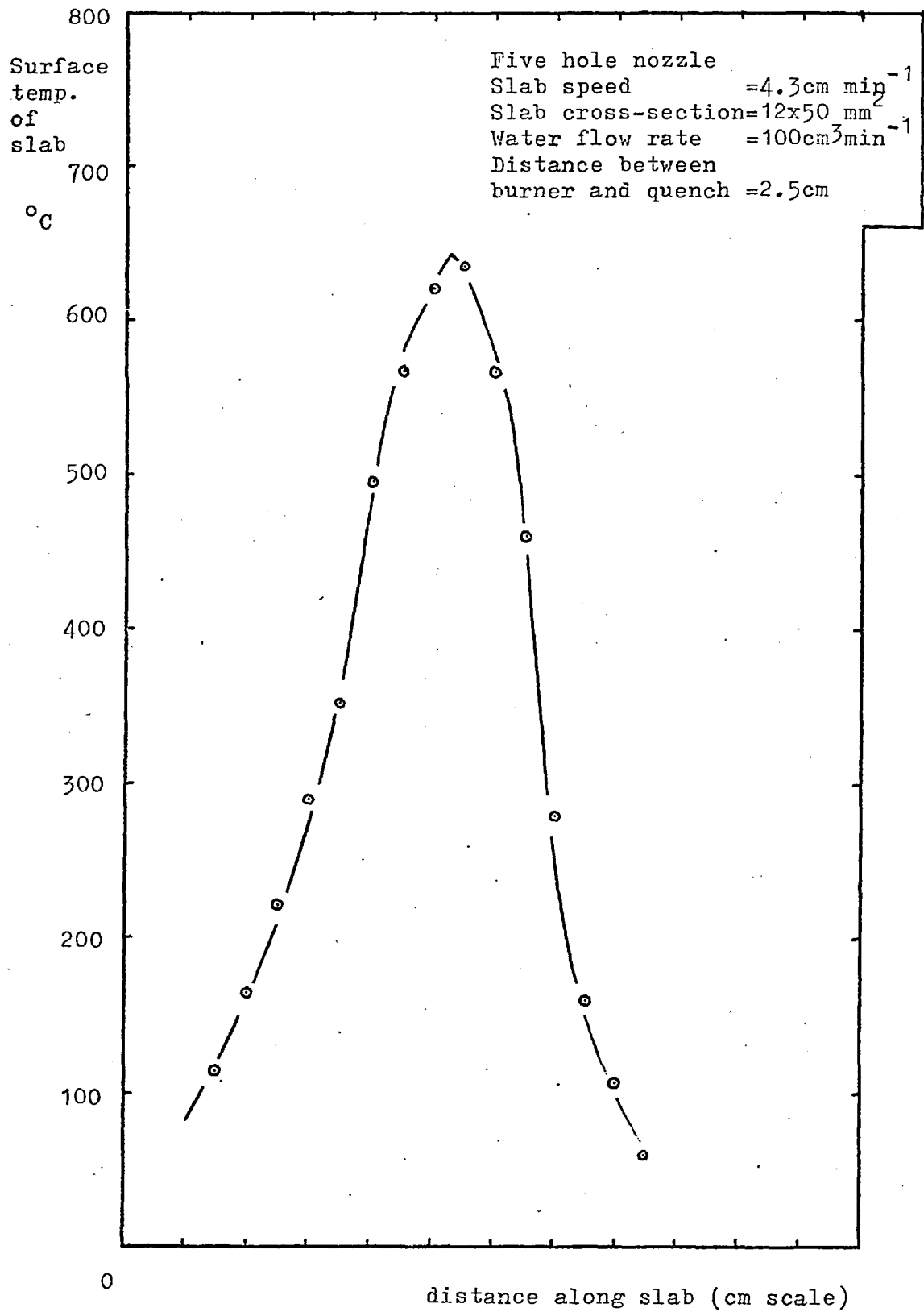


FIGURE VI 28 - SURFACE TEMPERATURES MEASURED DURING QUENCHING WITH THE FIVE HOLE NOZZLE

$$a = 1.09 \text{ cm and } \dot{q}''_M = 165 \text{ W cm}^{-2}$$

The maximum surface temperature is obtained when $y^* = 0.4$
(see figure III 7 on page 68).

$$\text{i.e. } y = y^*_a = 0.44 \text{ cm}$$

Figure C3 of appendix C gives the maximum value of the surface temperature as 740°C . At a constant quenching rate of $20^\circ\text{C sec}^{-1}$ the time required to quench the surface to 20°C (or $\theta_s = 0$) is

$$t = \frac{740 - 20}{20} = 36 \text{ sec}$$

$$\begin{aligned} \text{and } y_{\theta_s=0} &= y_{\theta_{s_{\max}}} + ut \\ &= 0.44 + 2.62 \times \frac{36}{60} \\ &= 2.01 \text{ cm} \end{aligned}$$

$$\text{giving } y^*_{\theta_s=0} = \frac{2.01}{1.09} = 1.85$$

Figure III 8 on page 70 gives quenching rates at various values of $\dot{q}''^*$, where

$$\dot{q}''^* = \frac{\text{quench heat flux}}{\dot{q}''_M}$$

These quenching rates which are given as $\theta_s^* \sqrt{u^*}$ against y^* , instead of in $^\circ\text{C sec}^{-1}$, are approximately linear. A straight line drawn from $\theta_s^* \sqrt{u^*} = 1.65$ at $y^* = 0.4$ to $\theta_s^* \sqrt{u^*} = 0$ at $y^* = 1.85$ almost coincides with the quenching rate for $\dot{q}''^* = 1.0$. In this case, a quenching rate of $20^\circ\text{C sec}^{-1}$ at a slab speed of 2.62 cm min^{-1} corresponds to

$$\begin{aligned} \text{quench heat flux} &\approx \dot{q}''_M \\ &= 165 \text{ W cm}^{-2} \end{aligned}$$

5 FLAME HARDENING THE SURFACES OF SLABS

Slabs of normalised En 8 were subjected to the process of flame hardening as described in sections IV 5 (b) and V 6.

Figures IV 9 and IV 10 show the arrangement of a slab over the burner and figure IV 12 shows the slab, the burner and the quench.

Quenching was carried out using either the slotted nozzle or the five hole nozzle, both being positioned at 2.5 cm from the burner axis. The water flow rates were maintained at

270 cm³ min⁻¹ for the slotted nozzle

and 100 cm³ min⁻¹ for the five hole nozzle

The burner was operated at the same gas flows and electrical augmentation as in section 4 above. Other conditions were:-

Slab speeds of 1.45, 2.62 and 4.8 cm min⁻¹.

Slab cross-sections of 50x12, 25x12 and 12x12 mm²

Distance between the burner and the slab = 0.5 cm

The results can be classified under three headings, as follows:-

(a) No martensite formed

Microstructures of the surface layers showed no evidence of martensite formation and were similar to microstructures of the untreated steel, except that the pearlite and ferrite were in finer grains. The hardness of the surface layers was tested using both the Vickers hardness and the G.K.N. microhardness machines. The hardness values within 1 mm of the treated surface were found to be 200 - 250 V.P.N. both with the Vickers machine using a load of 1 kg and with the microhardness tester using a load of 100 g. The untreated steel gave hardness values of 150 - 200 V.P.N. under the same conditions.

This type of microstructure and the corresponding hardness values were produced by any one of the following treatments:-

- 1 Slab cross-section = 50x12 mm²
Slab speed = 1.45 cm min⁻¹
Augmented burner and slotted nozzle quench.
- 2 Slab cross-section = 25x12 mm²
Slab speed = 2.62 cm min⁻¹
Augmented burner and slotted nozzle quench.

- 3 Slab cross-section = $12 \times 12 \text{ mm}^2$
 Slab speed = 4.8 cm min^{-1}
 Augmented burner and slotted nozzle quench.

(b) Thin layer of martensite formed

A typical microstructure of a cross-section of a hardened slab is shown in plate VI 3. A thin layer of martensite can be seen at the treated surface. The thickness of the layer is about 40 microns. A hardness traverse using the microhardness tester was made from the surface into the main body of the slab and this traverse is shown below plate VI 3. This microstructure and hardness traverse was obtained with the following conditions:-

- Slab cross-section = $12 \times 12 \text{ mm}^2$
 Slab speed = 2.62 cm min^{-1}
 Augmented burner and slotted nozzle quench.

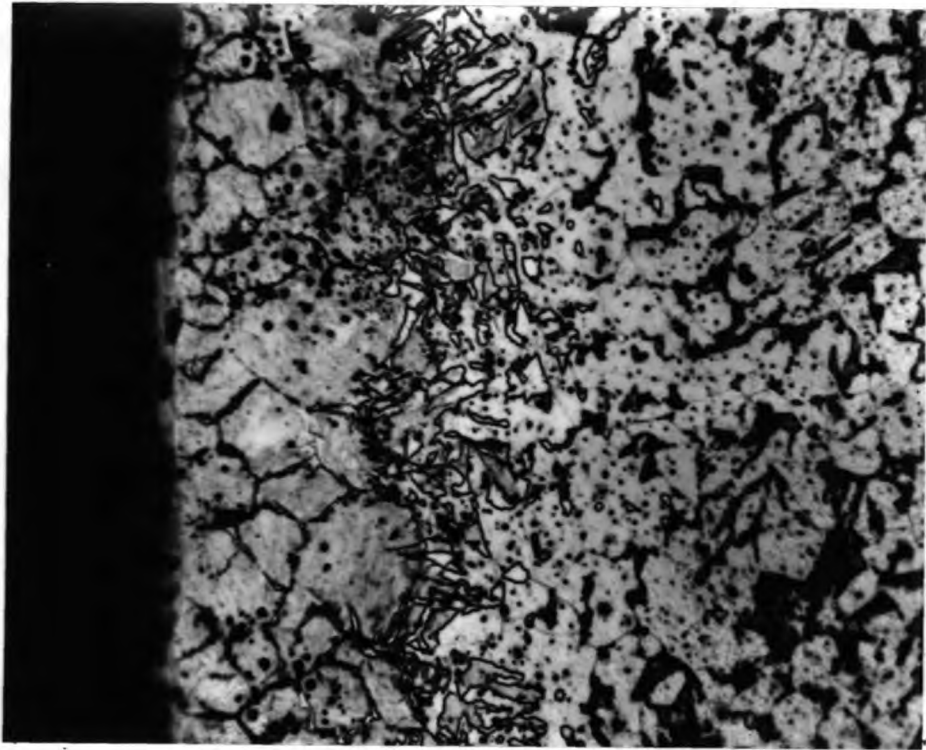
Similar microstructures and hardness traverses were obtained with any of the following sets of conditions:-

- 1 Slab cross-section = $50 \times 12 \text{ mm}^2$
 Slab speed = 1.45 cm.min^{-1}
 Augmented burner and five hole nozzle quench.
- 2 Slab cross-section = $25 \times 12 \text{ mm}^2$
 Slab speed = 1.45 cm min^{-1}
 Augmented burner and slotted nozzle quench.
- 3 Slab cross-section = $12 \times 12 \text{ mm}^2$
 Slab speed = 4.8 cm min^{-1}
 Augmented burner and five hole nozzle quench.

(c) Thick layer of martensite formed

Plate VI 4 shows a typical microstructure where martensite has been formed to a depth of about 2 mm from the treated surface. The hardness traverse shown below the plate was obtained using the microhardness tester.

A thick layer of martensite was only formed when the two pieces of steel which were on either side of the slab were removed. (Figure IV 10 on page 91 shows a slab surrounded by these two pieces of steel). The microstructure shown in plate VI 4 and the



x 1000, etched 2% nital

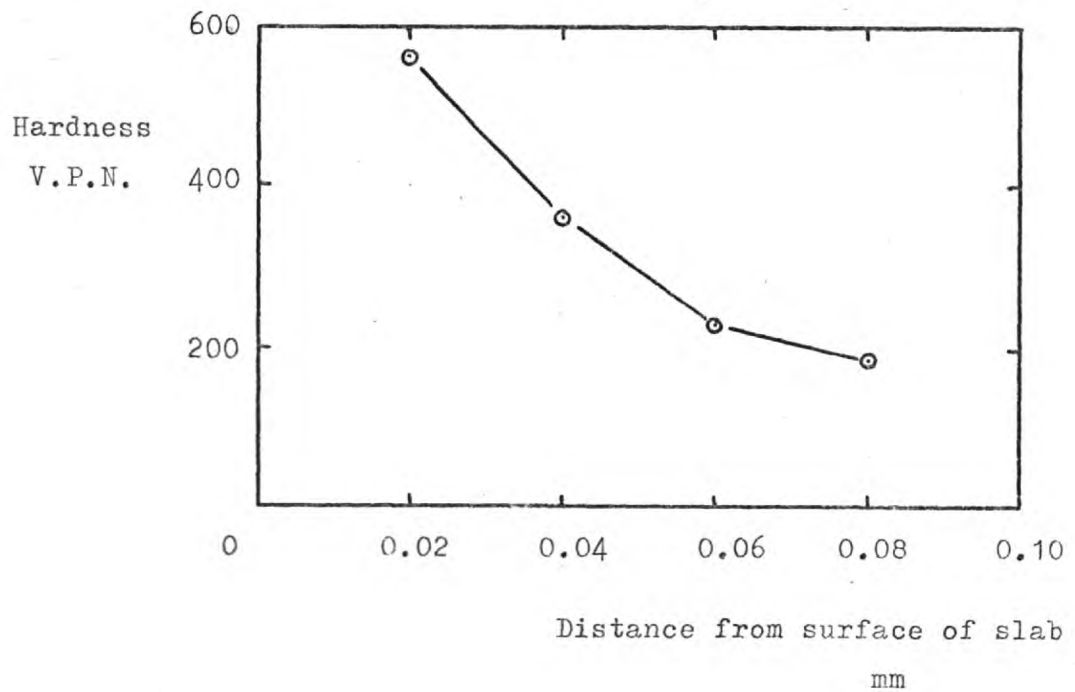
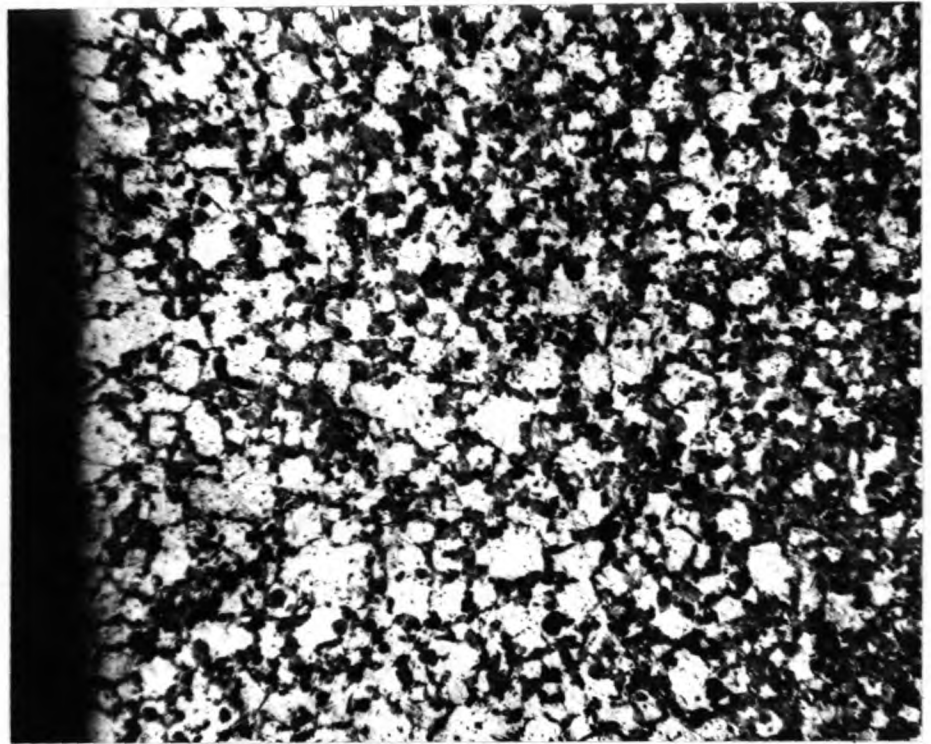


PLATE VI 3 - MICROSTRUCTURE OF A THIN LAYER OF MARTENSITE AT THE SURFACE OF A SLAB WITH THE CORRESPONDING MICROHARDNESS TRAVERSE



x 25, etched 2% nital

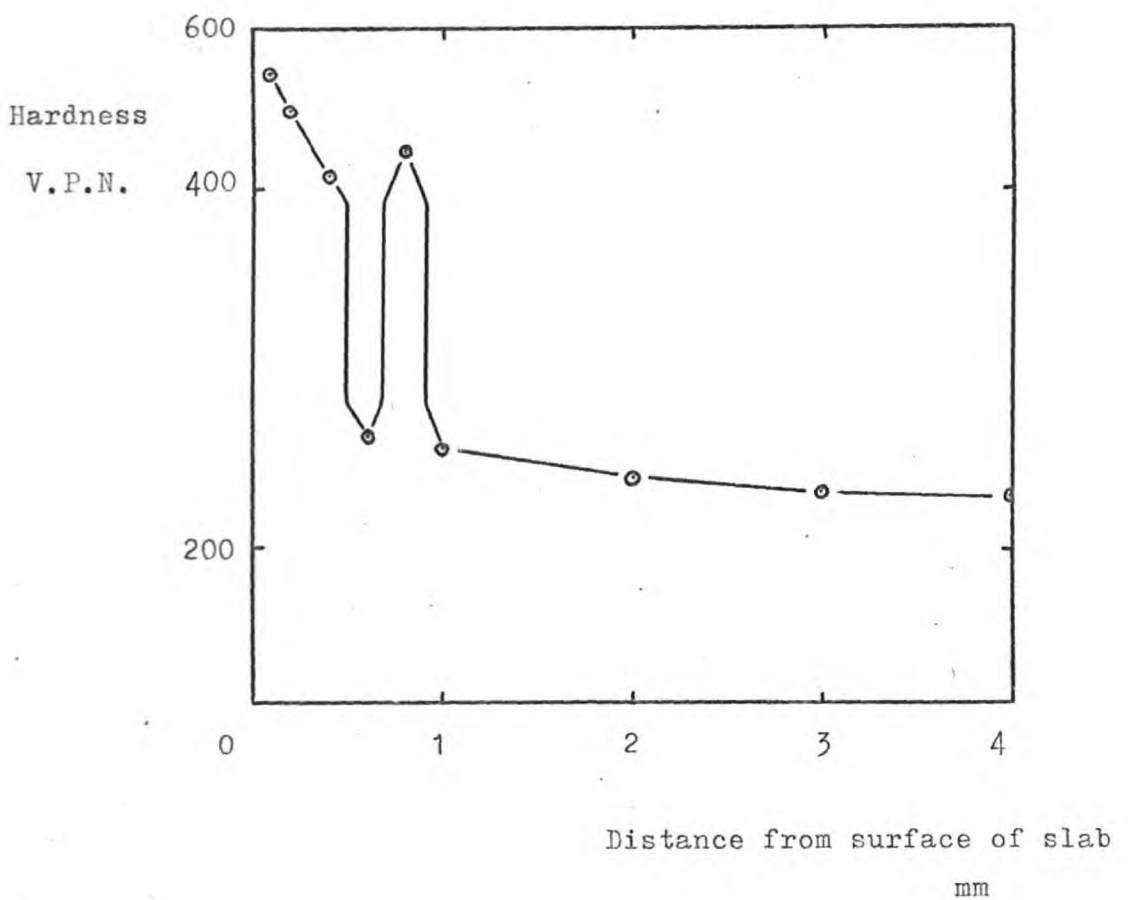


PLATE VI 4 - MICROSTRUCTURE OF A THICK LAYER OF MARTENSITE AT THE SURFACE OF A SLAB WITH THE CORRESPONDING MICROHARDNESS TRAVERSE

corresponding hardness traverse were obtained in this way under the following conditions:-

Slab cross-section = $25 \times 12 \text{ mm}^2$

Slab speed = 4.8 cm min^{-1}

Augmented burner and five hole nozzle quench.

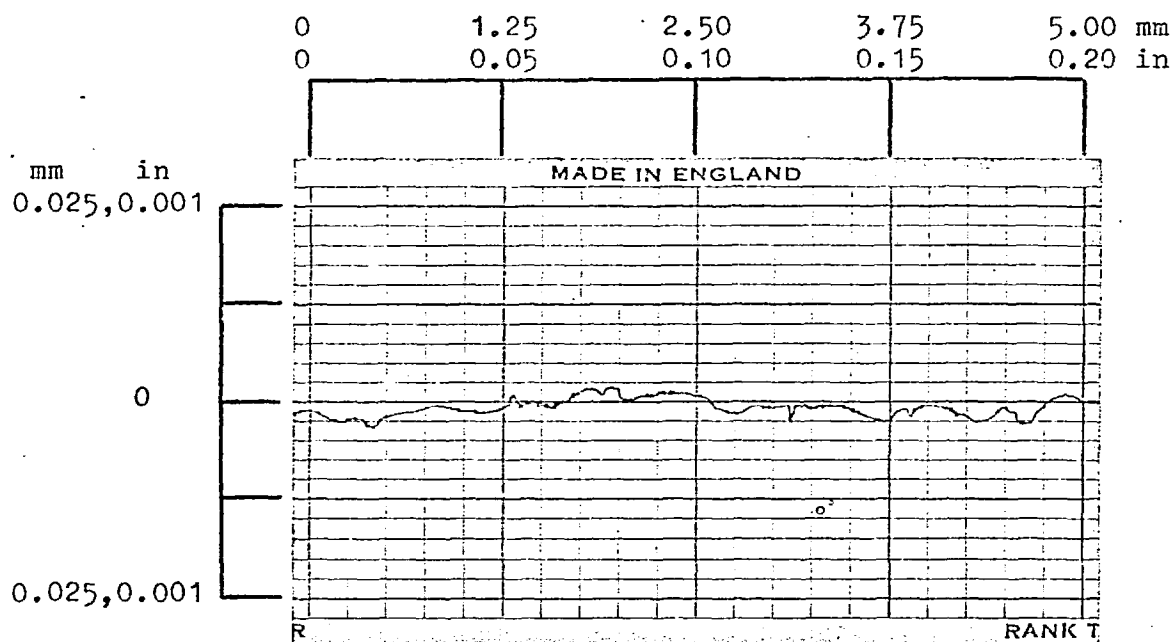
(d) Surface finish

The surface of any slab which was to be flame hardened had a ground finish, the direction of grinding being along the length of the slab. The reason for grinding has been given in section V 6.

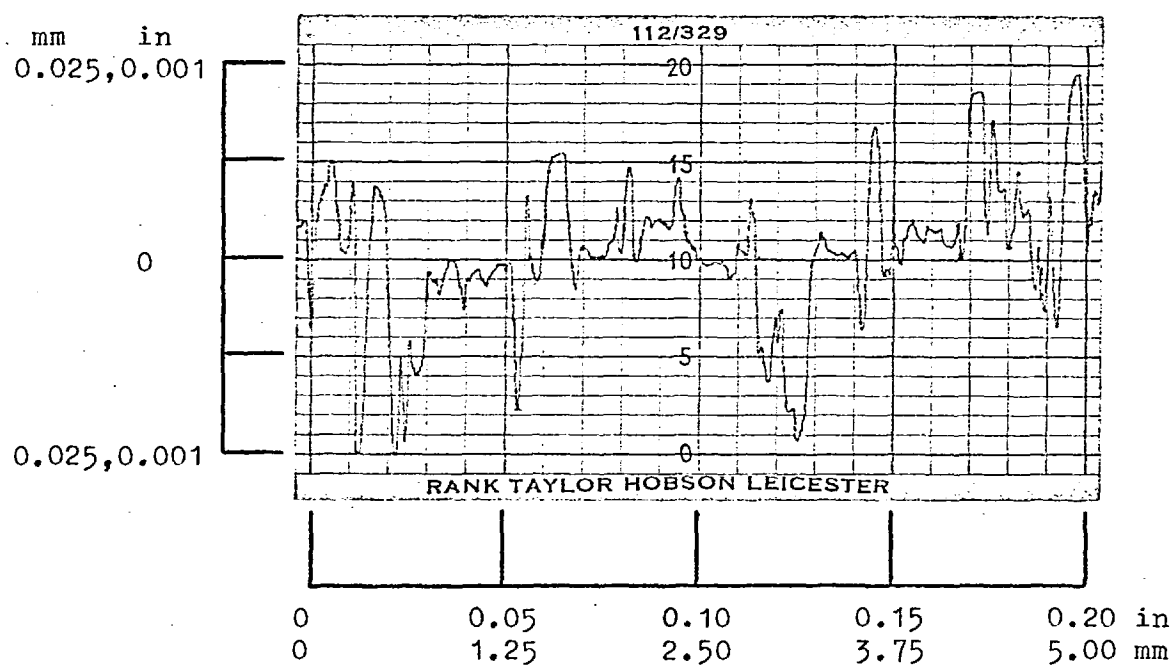
The ground surface was examined after flame hardening and was found to be covered with a uniform distribution of small marks, all considerably less than 1 mm in size, and spaced about 1 mm apart. In order to assess the influence of these marks on the surface finish, the surface was tested using a Tallysurf machine. The results of the Tallysurf measurements for the flame hardened surface are compared with those for the ground, untreated surface in figure VI 29.

An experimental observation was made that could be associated with surface finish; a large number of arc spots could be seen on the surface of the slab when the distance between the slab and the burner was 0.5 cm.

The surface of the slab showed no evidence of scale formation except for a dark discolouration.



(a) Surface of a ground, untreated slab



(b) Surface of a ground, flame-hardened slab

FIGURE VI 29 - TALLYSURF MEASUREMENTS

VII DISCUSSION

1. BURNER DESIGN

(a) Flame stability

Each of the unseeded electrically augmented burners described in sections V 1(b) to (d) requires thermionic emission at its electrodes in order to initiate an electrical discharge. The electrodes must be raised to a high temperature before appreciable emission occurs, as described in section II 4(a) on page 30, and high intensities of combustion are required to achieve these temperatures. High combustion intensities are obtained when the throughput of combustible mixture is relatively large for the size of the burner, the limiting value of the throughput being reached when the flame blows off the burner. Blow-off is explained in section II 2(a)(ii). Suitable operating conditions have been determined for the sudden enlargement burner and for the two vortex-stabilized burners and can be compared with the blow off limits of simple tube burners of equivalent size.

The methane content of the combustible mixture is plotted against the flow rate at which this mixture blows off simple tube burners in figure VI 1 on page 111. These blow-off limits include those obtained during the present work for tubes of 7 mm and 8 mm diameter and those obtained by Lewis and von Elbe ⁽¹⁵⁾ for tubes of 5.77 and 8.8 mm diameter. Blow-off occurs at lower flow rates for the 7 mm and 8 mm tubes than would be expected from Lewis and von Elbe's results. A probable explanation for this is that Lewis and von Elbe used much longer burners which ensured that the gas flow was fully developed when it reached the burner mouth. This point is also illustrated by figure VI 2 where the methane content of the mixture is plotted against the blow-off criterion (see section II 2(a)(ii)). If the ranges of Reynolds number are calculated from figure VI 1 for the blow-off limits of Lewis and von Elbe's tubes then their flow regimes can be found. These regimes are given in table VII 1.

Tube diameter mm	Reynolds number	Flow regime
5.77	240-1500	Laminar
8.8	450-2700	{ Laminar, transitional, turbulent

Table VII 1

Figure VI 2 clearly shows the laminar flow regime for the 5.77 mm tube and the laminar, transitional and turbulent regimes for the 8.8 mm tube. Figure VI 2 also indicates that turbulent flow existed in the 7 mm and 8 mm tubes because these results lie on approximately the same curve as those of the 8.8 mm tube in the turbulent regime.

Line BC in figure VI 1 represents stable operation (not the blow-off limits) for the sudden enlargement burner without electrical augmentation. This line shows that the sudden enlargement burner (throat diameter of 7 mm) was considerably less prone to blow-off than a simple tube burner of the same diameter. Similarly, point D in figure VI 1 represents the usual operating point for the small vortex-stabilized burner without electrical augmentation. This burner with an effective throat diameter of 7 mm (see section VI 1(a)(iii)) can be seen to be even less prone to blow-off than the sudden enlargement burner. The vortex-stabilized burner would therefore be expected to achieve higher combustion intensities than the sudden enlargement or simple tube burners and would be the most suitable design for heating electrodes to emission temperatures.

The operating point during warm-up, A, and the corresponding point during electrical augmentation, B, are shown relative to the inflammability limits of methane in figure VI 3 for both sizes of vortex-stabilized burner. As expected, points A and B lie within the inflammability limits, with point B (at 12% methane) being closer to the rich limit than point A. Point B could not be readily achieved without electrical augmentation because the flame tended to stabilize itself on top of the annular electrode instead of inside the combustion chamber. With a.c. and d.c. augmentation the discharge would exist for appreciable lengths of time at

methane contents of up to 100% of the pre-mix gas flow, as observed in section V 1(c) on page 101. Soot was produced once the rich limit was exceeded and some of the methane was burnt with secondary air on top of the annular electrode in a diffusion flame.

(b) Electrodes

Seeding was eliminated by designing a burner which could operate at a sufficiently high combustion intensity to raise the temperature of refractory electrodes to a value at which appreciable thermionic emission would occur, as described in sections V 1(a) to (d).

Evidence in support of initiation by thermionic emission is provided by plates VI 1(a) and (b) on page 116. Plate VI 1(a) shows the voltage and current traces which were obtained when an a.c. arc was struck between a hot central electrode of zirconia and a water-cooled copper tube. The discharge is only initiated during the half-cycle when the zirconia electrode is the cathode, i.e. during the negative half-cycle. No discharge occurs when the copper tube is negative. Plate VI 1(b) shows that the discharge is initiated in both positive and negative half-cycles when both electrodes are zirconia and are heated by the combustion products.

The current density that would be expected at the hot surface of an electrode due to thermionic emission alone can be calculated from the Dushman equation, equation II 5 on page 30. Typical values of current density for zirconia and tungsten at 2000°K and 2500°K are shown in table III 1 on page 31. These values indicate that current densities due to thermionic emission alone would be unlikely to exceed 0.5 A cm^{-2} if the electrodes were raised to the temperature of a methane-air flame. Current densities which occurred during augmentation of the large vortex-stabilized burner were at least an order of magnitude higher because the cross-sectional area of the central electrode was about 0.75 cm^2 yet currents of 4A could be passed between this electrode and the annular electrode.

The Schottky effect, represented by equation II 6 on page 31, could possibly account for these high current densities.

The work function ϕ is modified by the Schottky effect to

$$\phi - \sqrt{eE} \quad \text{where}$$

$$\begin{aligned} \text{the electronic charge, } e &= 1.6 \times 10^{-19} \text{ coulomb} \\ &= 4.8 \times 10^{-10} \text{ e.s.u.} \end{aligned}$$

$$\text{and the field strength, } E = \frac{\text{voltage across the electrodes}}{\text{distance between the electrodes}}$$

Typical values of applied voltage and electrode gap are 1000 V and 0.5 cm, giving

$$E = 2000 \text{ V cm}^{-1} \text{ or } 6.7 \text{ e.s.u.}$$

$$\begin{aligned} \text{Hence } \sqrt{eE} &= 5.7 \times 10^{-5} \text{ e.s.u.} \\ &= 0.017 \text{ V} \end{aligned}$$

This is a negligible modification of ϕ (values of ϕ are given in table II 1 on page 30) and so the Schottky effect can be ignored.

A likely explanation of the high current density obtained at the electrodes is that thermionic emission initiates the discharge which immediately contracts to a narrow arc channel. This heats a spot on the electrode surface until vaporization occurs when the current density can then increase.

The life-time of each type of electrode is outlined in section VI 1(d). The annular electrode of the large vortex-stabilized burner would probably last longer if the lime-zirconia powder was pressed around the carbon ring and was then sintered instead of being cast around the ring. The improved method would reduce the porosity of the zirconia which should lead to a slower rate of oxidation of the carbon ring. The life of the tungsten central electrode was probably very dependent on the methane to air ratio because the tungsten was oxidised readily at flame temperatures. A fuel-rich mixture was used during electrical augmentation but during the warm-up period prior to augmentation a stoichiometric or even a slightly lean mixture was used which probably caused relatively rapid oxidation.

2. THE NATURE OF THE DISCHARGE

(a) From observations on the small vortex-stabilized burner

The d.c. characteristics of the discharge in this burner

are shown in figure VI 4 on page 114. The discharge was passed between the central electrode of hot zirconia (the cathode) to a water-cooled copper tube situated at 5 mm or 10 mm above the central electrode. The curves in figure VI 4 are similar in shape to the static characteristics of an arc discharge shown in figure II 18 on page 28. The series resistance which was required in order to limit the current in the 5 mm arc to 0.4 A was calculated to be 4.0 to 4.5 kilohms in section VI 1(b)(i) but the series resistance which was actually used to restrict the current to 0.4 A was about 6 kilohms. Large errors were inherent in plotting the characteristics shown in figure VI 4 because the measured arc voltages V also included the voltage drops in the electrodes.

Reference to plates VI 1(a) and (b) in section 1(b) of this chapter shows that emission of electrons from a hot refractory cathode initiates the discharge in the small vortex-stabilized burner. In plates VI 1(a) to (d) each negative half-cycle corresponds to the central electrode acting as the cathode and each positive half-cycle corresponds to the other electrode, either the water-cooled tube or the annular electrode, acting as the cathode. The electrode gaps and methane to air ratios corresponding to each plate are given in table VI 2 on page 115. Plate VI 1(b) shows that the voltage-time relationship in each positive half-cycle was different from that recorded in each negative half-cycle. This asymmetry could have been caused by the burner geometry, the rate of flow of gas past the electrodes and the composition of the gas. When the distance between the electrodes was reduced to 2 mm (plate VI 1(c)) or when the air flow rate was reduced to zero (plate VI 1(d)), the voltage traces became symmetrical about the time axis. In both cases the arc voltage was considerably reduced. The distance between the electrodes, the rate of flow of gas and the methane to air ratio probably all affect the stability of the discharge. The significance of the precise shape of each trace has not been discussed because much more data would be required but each half-cycle of plate VI 1(b) bears some similarity to figure II 18 on page 28.

(b) From observations on the large vortex-stabilized burner

Sequences from high speed cine films are shown in negative in plates VI 2(a) to (d) on page 117 and table VI 3 gives the sequence, the corresponding system and the arc current.

Sequences (b), (c) and (d) which depict the burner operating at 3.0A show that the discharge occurs for 10 or 11 frames in each half-cycle, where each half-cycle occupies 26 frames, i.e. the discharge exists for about 40% of the cycle. Sequence (a) shows that the discharge only exists for 25 - 30% of a cycle when the burner operates at 0.6A. It would appear from plates VI 1(b) to (d) on page 116 that the discharge in the small burner exists for considerably longer, say 60 - 80% of the cycle, depending on electrode gap and gas flow. A possible explanation is that the work function of zirconia is lower than that of tungsten (see table II 1 on page 31). This would enable the discharge to be initiated more readily from a zirconia electrode than from a tungsten electrode.

As observed in section VI 5(d) on page 169, part or all of the arc was transferred to the surface of the steel slab during flame hardening. This effect may well have been restricted to the half-cycle when the central electrode acted as the cathode. The steel slab would not act as the cathode during the other half-cycle because emission would not occur. This was not checked by high-speed photography because of difficulty in sighting the camera when the slab was situated at only 0.5 cm above the burner.

3. HEAT TRANSFER TO WATER-COOLED SURFACES

(a) Sudden enlargement burner

The overall rate of heat transfer from this burner to the tube calorimeter is shown in figure VI 6(a) on page 122. The relative positions of points A and B show that higher rates of heat transfer were obtained when the flame was augmented by d.c. power than by a.c. power. This was because the a.c. discharge could not be initiated during the half-cycles when the tube calorimeter formed the cathode, as pointed out in section VII 1(b).

(b) Small vortex-stabilized burner

(i) Without the annular electrode

The overall rates of heat transfer from this burner to the tube calorimeter are shown in figure VI 6(b). These results are analogous to those shown in figure VI 6(a) and which are discussed above for the sudden enlargement burner; the same explanations are valid.

The effect of the percentage of methane in the combustible mixture on the overall rate of heat transfer is shown in figure VI 7, on page 124. The regions A to G are described in section VI 2(a)(ii) on page 123. The rates of heat transfer from the unaugmented flame are only high for regions B and C, i.e. 7% to 11% methane. In region D the rich limit of inflammability of methane is exceeded according to figure VI 3 on page 112 and so the pre-mixed flame which was stabilized in the combustion chamber becomes a diffusion flame which is stabilized on the burner rim. When this occurs there is a considerable decrease in the rate of heat transfer. When d.c. augmentation is used, a high rate of heat transfer can be maintained over a wider range of methane contents. The rates of heat transfer at methane contents which were less than 9% were lower than those obtained with methane contents which exceeded 9%. About 9% methane is required for stoichiometry. The higher rate of heat transfer obtained with a fuel-rich mixture may be explained by the presence of dissociated hydrogen which will recombine at the cold surface of the tube releasing energy, as mentioned in section II 5(a) on page 34.

Figure VI 8 on page 126 is equivalent to region B in figure VI 7 but has been plotted for a number of methane flow rates. As the flow rate of methane increases, the range of methane contents over which the flame remains inside the combustion chamber becomes smaller until the flame can never be stabilized there when the methane flow rate exceeds 1.6 litres per minute. A reasonable range of stability exists at a flow rate of about 1.0 litre per minute, together with a relatively high rate of heat transfer. This compromise was therefore chosen for all further work with the small vortex-stabilized burner and a similar compromise was found for the larger version. In both cases the methane content of the combustible mixture was varied when necessary by adjusting the air flow only. Typical values of these flow rates are given in table VI 1 on page 109.

The tube calorimeter, because of the simplicity of its design, was only used in the early development of the vortex-stabilized burner. The water-cooled flat plate which then replaced the tube was a more suitable means of assessing the rates of heat transfer from burners to metal surfaces. The overall rate of heat transfer from the small vortex-stabilized burner to a large water-cooled

surface was measured using the water-cooled calorimeter of diameter 13 cm. The results are described in section VII 2(b)(i) and are shown in figure VI 10 on page 129. The burner was operated in the following forms:-

- A - unaugmented, no annular electrode.
- B - augmented, no annular electrode but calorimeter formed the other electrode.
- C - unaugmented, with annular electrode.
- D - augmented, with annular electrode.

Figure VI 10 shows that heat transfer rates are similar for forms A and C. Form B provides a small increase in rate of heat transfer over forms A and C for lean mixtures, i.e. methane content is less than 9%. Form D gives a larger increase in heat transfer rate over the entire range of methane contents, particularly when the mixture is rich, i.e. for a methane content which exceeds 9%. These results show that higher rates of heat transfer were achieved with a.c. discharges when an annular electrode was incorporated in the burner than when the arc was transferred directly to an external cold metal surface. The explanation for this has been discussed in section VII 1(b). The highest rates of heat transfer were obtained with form D when the mixture was fuel-rich. This is explained by the recombination of dissociated hydrogen at the cold surface of the calorimeter (see section III 5 (a) on page 34). These high rates of heat transfer do not occur with form B when the mixture is rich because the fuel by-passes the arc and burns in a diffusion flame outside the burner. All further work was carried out with vortex-stabilized burners in which were incorporated annular electrodes.

(ii) With the annular electrode

The effect of distance between the burner and the water-cooled plate on the overall rate of heat transfer from forms C and D of the burner is shown in figures VI 11(a) and (b) on page 130. The rate of heat transfer for any one methane content decreases as the distance is increased and the rate of heat transfer at any distance decreases as the methane content is reduced.

Figure VI 12 on page 133 shows typical distributions of the heat flux from the burner which were obtained at a water-cooled surface using the concentric calorimeters. The method and the results are given in section VI 2(b)(ii). In common with the overall rates of heat transfer from the augmented flame which were discussed above, an increase in the methane content of the combustible mixture provided an increase in the rate of heat transfer. In particular, the maximum heat flux was increased from 100 W cm^{-2} to 150 W cm^{-2} by increasing the methane from 10.1% to 11.5%. These heat flux distributions were subject to three types of error:-

- 1 The difficulty of aligning the burner and calorimeters to form an axi-symmetric system.
- 2 The terms $(\dot{q}_i - \dot{q}_{i-1})$ and $(r_i^2 - r_{i-1}^2)$ introduce a large error in $\dot{q}_{i-1}$ in equation VI 9 on page 131
- 3 Only four points were obtained for each plot of the radial distribution of heat flux.

Figure VI 13 shows the effect of the distance between the burner and the calorimeters on the heat fluxes at a number of radii. Only the heat fluxes lying on radii of less than 1 cm appear to be significantly affected by distance. The jet of hot combustion products will entrain more and more cold air as the distance increases. This will reduce the heat flux at the water-cooled surface. The heat fluxes which lie on radii of more than 1 cm are little affected by distance. An explanation for this is that the unburnt methane is combusted as a diffusion flame at radii exceeding 1 cm and consequently heat flux from this flame will be almost independent of distance, at least within the range of distances considered in this work.

(c) Large vortex-stabilized burner

Radial distributions of heat flux from this burner to a water-cooled surface were determined using the split-plate calorimeter. This calorimeter was expected to overcome some of the disadvantages associated with the concentric calorimeters, particularly the problem of alignment. Also, a larger number of experimental points could be obtained instead of the few points provided by the concentric calorimeters.

The results are given in section VI 2(c). Figure VI 15 shows the heat flux distributions which were determined when the distance between the burner and the surface of the calorimeter was 2.0 cm. Electrical augmentation increased the maximum value of the heat flux from 50 W cm^{-2} (unaugmented flame) to 170 W cm^{-2} (augmented with 3A at 1 kV). Figure VI 16 shows distributions that were obtained at various burner-to-calorimeter distances. At a distance of 1.5 cm or less, the computer programme used to analyse the results became unstable and this is reflected in the distributions shown in figure VI 16. In these cases, the values of $\Delta\theta_1$ and $\Delta\theta_2$ did not follow the smooth curves which are shown in figure VI 14, probably due to erratic movement of the arc in the burner nozzle. The reduction in step length which would probably have removed the instability was impractical unless the equipment was extensively modified.

Nestor and Olsen⁽³³⁾ have suggested that the water temperatures should be measured to 3 significant figures and that about 20 steps should be taken when each plate of the calorimeter is passed over the burner. These recommendations were followed but a smaller step length would have been an advantage because more experimental points could have been used to define the steeper regions of the heat flux distributions. A much shorter step length could have been used if the chain drive and pin wheel had been replaced by the type of screw mechanism used to advance a lathe turret.

Table II 4 on page 41 gives the results which have been obtained by other workers using tube and split plate calorimeters. Anderson and Stresino obtained a maximum heat flux of 82 W cm^{-2} for an air-methane flame compared to the value of 50 W cm^{-2} given in figure VI 15. There are no comparable values for augmented flames because Davies' results were obtained using a tube calorimeter and tube calorimeters appear to give higher values than split-plate calorimeters, as shown by Anderson and Stresino's work with both types of calorimeter. Figure VI 16 shows that the augmented air-methane flame gives a maximum flux of 170 W cm^{-2} . From table II 4, oxy-acetylene flames gave maximum fluxes of 290 to 400 W cm^{-2} whilst a 200 A d.c. arc gave a maximum flux of 7 kW cm^{-2} , but comparisons between the maximum fluxes obtained from these systems are invalidated because different experimental conditions were used.

4 HEAT TRANSFER TO THE SURFACE OF A STEEL SLAB

(a) Method of predicting the surface temperature of the slab

The integral profile method of solving the heat conduction equation for a slab which moves over a given heat flux distribution has been developed in chapter III. The justification for using this method is discussed, the solutions obtained by the method being compared with the appropriate exact solutions. This comparison is most easily made if the predictions of the integral method which was developed for a uniform heat flux (see "rectangular distribution of heat flux" on page 66) are compared with exact solutions.

The temperature θ_S^* at the surface of a slab moving with velocity u^* over a rectangular distribution of constant flux $\dot{q}_M''$ which exists for $-1 < y^* < 1$ is given by equations III 5 a and c on page 67. These equations are plotted as $\theta_S^* \sqrt{u^*}$ against y^* in figure VII 1 (curve 1).

(i) Exact solution - conduction of heat in the x-direction only

These exact solutions are given by equations II 26 and II 27 in section II 6 (b)(i). They describe the same system as equations III 5 a and c if the following transformations are made:-

$$x = 0, \quad \dot{q}_0'' = \dot{q}_M'', \quad t = \frac{y+a}{u}, \quad T = \frac{2a}{u}$$

Equation II 26 becomes

$$(\theta_S)_{-a < y < a} = \frac{2\dot{q}_M''}{k} \sqrt{\frac{\alpha(y+a)}{\pi u}} \quad \text{--- VII 1 a}$$

Equation II 27 becomes

$$(\theta_S)_{y > a} = \frac{2\dot{q}_M''}{k} \sqrt{\frac{\alpha}{\pi u}} \left[(y+a)^{\frac{1}{2}} - (y-a)^{\frac{1}{2}} \right] \quad \text{--- VII 1 b}$$

Equation VII 1 a and b can be expressed in terms of the dimensionless variables

$$\text{VII 4(a)}$$

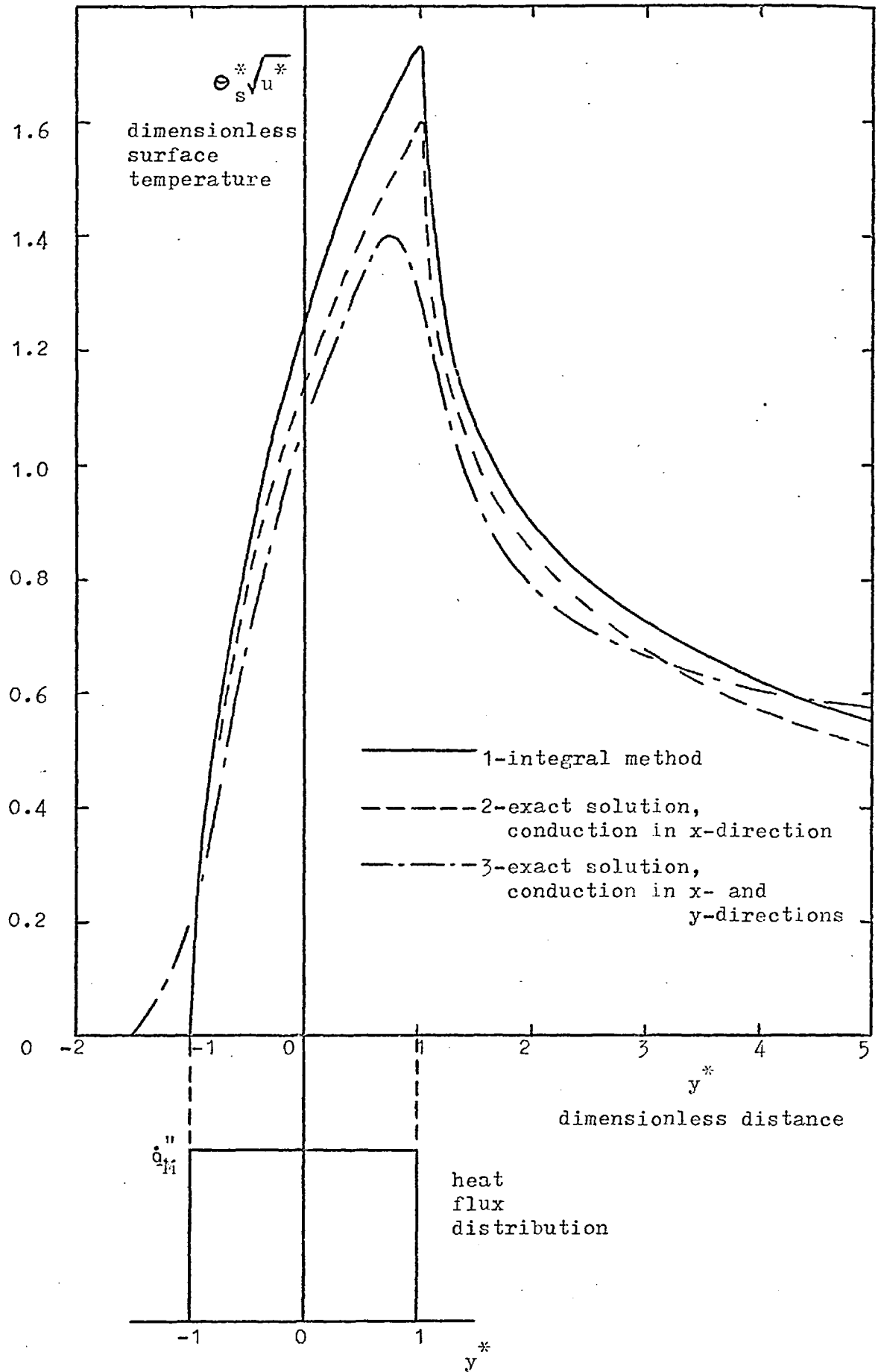


FIGURE VII 1 - DIMENSIONLESS SURFACE TEMPERATURE PROFILES FOR A RECTANGULAR HEAT FLUX DISTRIBUTION

$$y^* = \frac{y}{a}, \quad \theta_S^* = \frac{\theta_S}{\theta_A} \quad \text{and} \quad u^* = \frac{u}{\alpha a} \left[\frac{k \theta_A}{\dot{q}_M''} \right]^2$$

to give

$$(\theta_S^*)_{-1 < y^* < 1} = \sqrt{\frac{4}{\pi u^*}} (y^* + 1)^{\frac{1}{2}} \quad \text{--- VII 2 a}$$

$$\text{and } (\theta_S^*)_{y^* > 1} = \sqrt{\frac{4}{\pi u^*}} \left[(y^* + 1)^{\frac{1}{2}} - (y^* - 1)^{\frac{1}{2}} \right] \quad \text{--- VII 2 b}$$

Equations VII 2 a and b are plotted as $\theta_S^* \sqrt{u^*}$ against y^* to give curve 2 in figure VII 1.

The only difference between equations III 5 a and c and equations VII 2 a and b is that the value of the numerical constant changes from $\sqrt{3/2}$ to $\sqrt{4/\pi}$, i.e.

$$\frac{\theta_S^* (\text{integral solution})}{\theta_S^* (\text{exact solution})} = \frac{\sqrt{\frac{3}{2}}}{\sqrt{\frac{4}{\pi}}} = 1.09$$

The integral solution therefore gives values of θ_S^* which are 9% high. Both solutions show that the maximum value of θ_S^* occurs at $y^* = 1$. At $y^* = 1$,

$$\theta_S^* (\text{integral solution}) = \sqrt{\frac{3}{u^*}}$$

$$\text{and } \theta_S^* (\text{exact solution}) = 2 \sqrt{\frac{2}{\pi u^*}}$$

(ii) Exact solution - conduction of heat in the x and y directions.

As described in section II 6 (b)(i), the exact solution to the heat conduction equation (equation II 21 a on page 43) applied to a slab moving over an infinitely long heat source of known width and uniform flux is given by Carslaw and Jaeger. The situation is analogous to that described by equations III 5 a and c and VII 2 a and b, except that conduction of heat is also occurring in the y direction. The data given in figure 34 on page 270 of reference 39 shows curves of

$$\frac{\pi \theta_S^k u}{2 \propto \dot{q}_M''} \quad \text{against } y^*, \text{ for values of } B^* = 0.2, 0.5, 1, 2, 5 \text{ and } 10,$$

where $B^* = \frac{ua}{2\alpha}$. In order to plot this

data as $\theta_S^* \sqrt{u^*}$ against y^* and compare it with curves 1 and 2 in figure VII 1, the ordinate

$$\frac{\pi \theta_S^k u}{2 \propto \dot{q}_M''} \quad \text{must be multiplied by } \frac{\sqrt{2}}{\pi} \cdot \frac{1}{\sqrt{B^*}}.$$

The data represented by $\frac{\pi \theta_S^k u}{2 \propto \dot{q}_M''}$ against y^* is shown

replotted as $\theta_S^* \sqrt{u^*}$ against y^* for $B^* = 1$ in curve 3 of figure VII 1. The shape of this curve depends on the value of B^* . For high values of B^* , curve 3 is approximately the same as curve 2 but when B^* is small curve 3 becomes much flatter. The maximum value of $\theta_S^* \sqrt{u^*}$ is plotted against B^* in figure VII 2.

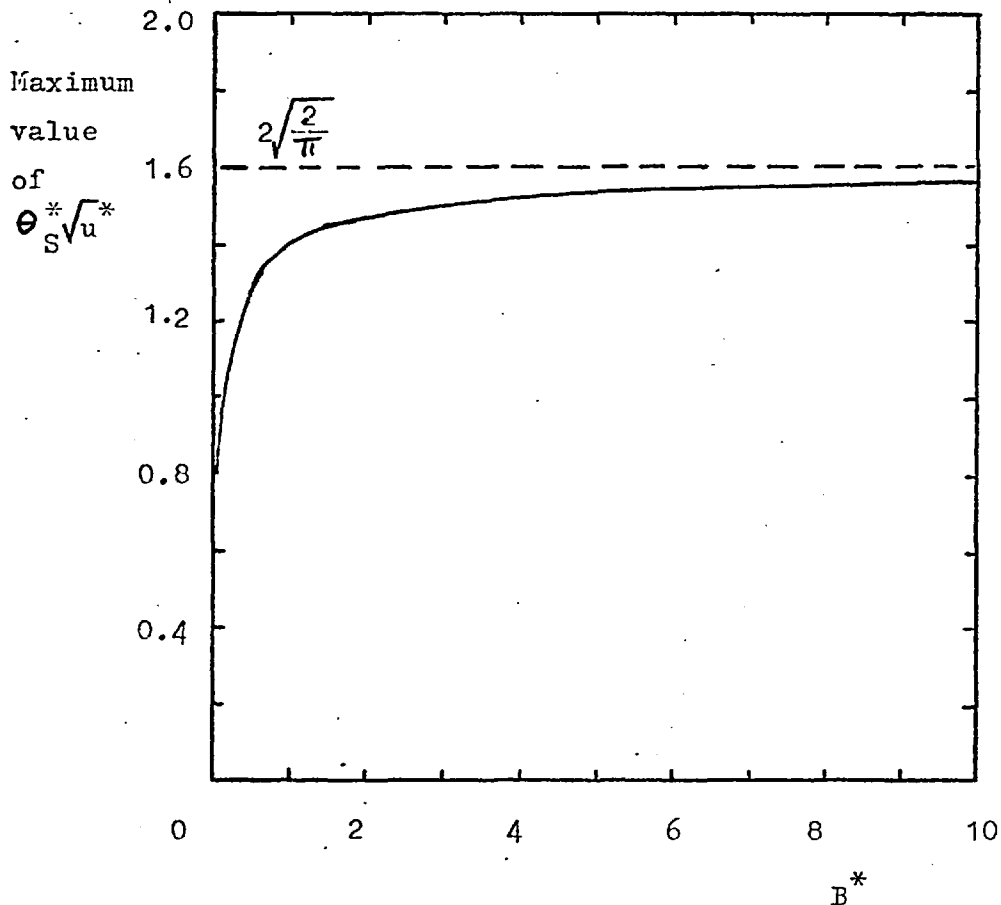


Figure VII 2

At high values of B^* , the maximum value of $\theta_S^* \sqrt{u^*}$ approaches $2\sqrt{\frac{2}{\pi}}$ which is the maximum value of $\theta_S^* \sqrt{u^*}$ given by curve 2 of figure VII 1.

- (iii) Accuracy of surface temperatures predicted by the integral profile method, assuming one dimensional conduction occurs

Curves 1 and 2 in figure VII 1 show that the integral profile method predicts the slab surface temperature to be a constant 9% higher than that given by the exact solution for one dimensional conduction.

A quadratic temperature profile given by equation II 32 on page 45 was assumed during the development of the integral method. If equation II 32 is replaced by a cubic temperature profile of the form

$$\theta = A_0' + A_1'x + A_2'x^2 + A_3'x^3 \quad \text{--- VII 3}$$

and the additional boundary equation

$$\left(\frac{\partial^2 \theta}{\partial x^2} \right)_{s,t} = 0 \quad \text{at } x = \delta \quad \text{--- VII 4}$$

is used, then equation II 35a can be derived. Equation II 35a gives values of θ_S which are closer to those obtained from the exact solution (equation II 26 on page 44) than those obtained from equation II 35 :-

$$\frac{\theta_S \text{ (integral method - quadratic profile)}}{\theta_S \text{ (exact solution)}} = \frac{\sqrt{\frac{3}{2}}}{\sqrt{\frac{4}{\pi}}} = 1.09$$

$$\frac{\theta_S \text{ (integral method - cubic profile)}}{\theta_S \text{ (exact solution)}} = \frac{\sqrt{\frac{4}{3}}}{\sqrt{\frac{4}{\pi}}} = 1.02$$

Similarly, the numerical factor $\sqrt{3/2}$ which appears on the right hand side of equations III 3 a, b and c, III 4 a, b and c, and III 5 a, b and c becomes $\sqrt{4/3}$ when the quadratic temperature

profile is replaced by a cubic.

- (iv) Effect of replacing the quadratic temperature profile by a cubic

Equations III 3 a and b, when derived for the cubic temperature profile given by equation VII 3, become:

$$(\theta_S^*)_{y^* < 0} = \sqrt{\frac{4}{3u^*}} \left[\frac{\tan^{-1} y^* + \frac{\pi}{2}}{1 + (y^*)^2} \right]^{\frac{1}{2}} \quad \text{--- VII 5 a}$$

$$(\theta_S^*)_{y^* > 0} = \sqrt{\frac{4}{3u^*}} \left[\left(y^* + \frac{\pi}{2} \right)^{\frac{1}{2}} - y^* \left(\frac{y^* - \tan^{-1} y^*}{y^{*2} + 1} \right)^{\frac{1}{2}} \right] \quad \text{--- VII 5 b}$$

Equations III 3 a and b and VII 5 a and b are shown in figure VII 3 where they are plotted as $\theta_S^* \sqrt{u^*}$ against y^* .

The heat flux distributions which were obtained in section VI 3 (a)(ii) from the data given in figure C 1 of appendix C can be recalculated using equations VII 5 a and b instead of equations III 3 a and b. Equations VI 12, 13 and 14 still apply but equation VI 15 becomes

$$\theta_S^* \sqrt{u^*} = 1.54 \text{ at } y^* = 0.4 \quad \text{--- VII 6}$$

substituting for $\theta_S^* = \frac{560^\circ\text{C}}{805^\circ\text{C}}$ at $y^* = 0.4$ in equation VII 6

gives $\sqrt{u^*} = 2.21$

substituting for k , θ_A , u , α and $\sqrt{u^*}$ in equation VI 14

gives $\dot{q}_M'' \sqrt{a} = 113 \text{ W cm}^{-2} \quad \text{--- VII 7}$

The values of 'a' which are shown in table VI 9 and were obtained for the quadratic temperature profile will remain unchanged. The reason for this is that

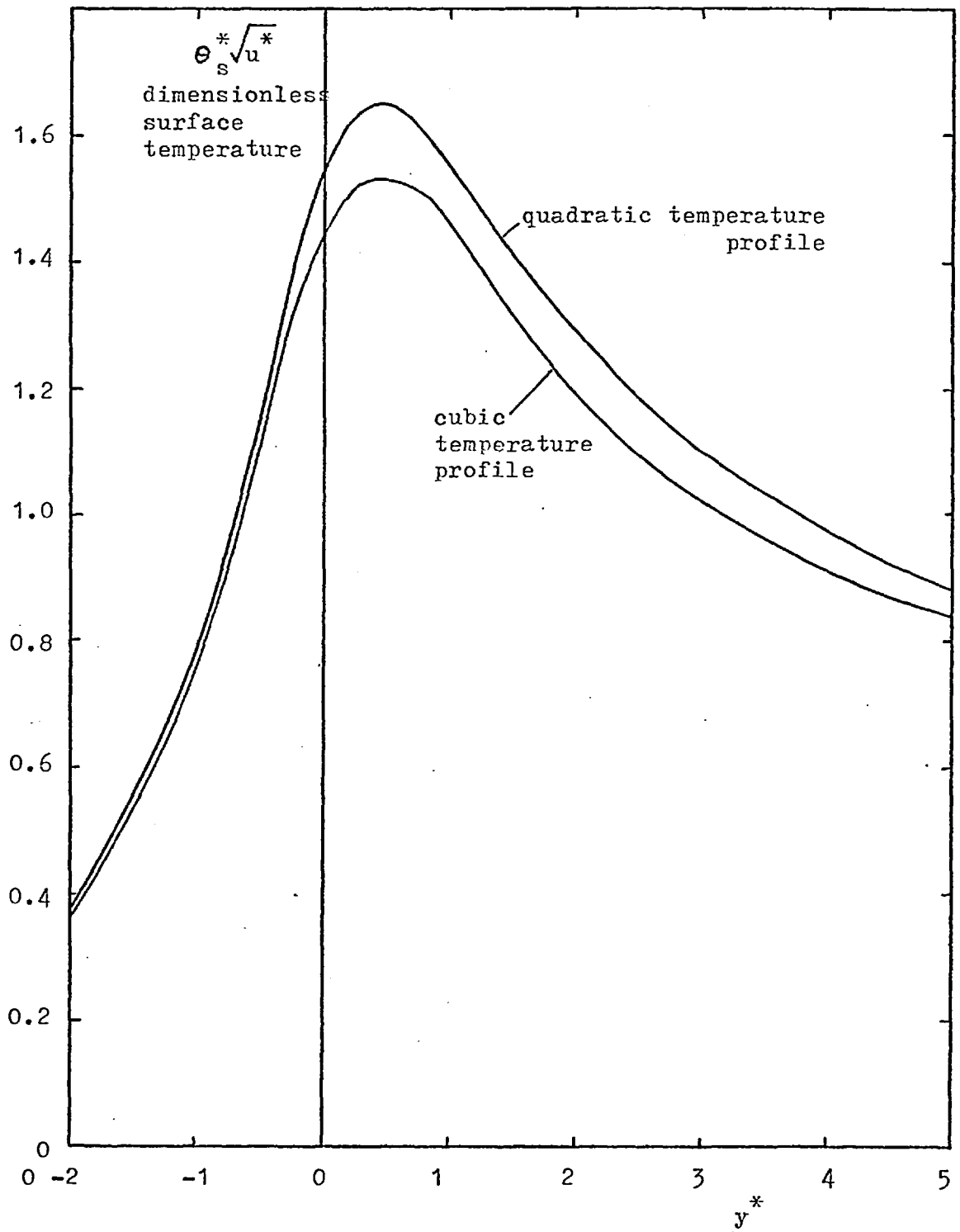


FIGURE VII 3 - SURFACE TEMPERATURES OBTAINED FROM QUADRATIC AND CUBIC TEMPERATURE PROFILES

$$\frac{\Theta_{\delta}^* \text{ (quadratic profile)}}{\Theta_{\delta}^* \text{ (cubic profile)}} = \frac{\sqrt{\frac{3}{2}}}{\sqrt{\frac{4}{3}}} = 1.06,$$

i.e. the two curves shown in figure VII 3 bear a constant relationship to each other. Substituting the mean values of 'a' given in table VI 10 into equation VII 7 gives

$$\dot{q}_M'' \text{ for } y^* < 0.4 = 92 \text{ W cm}^{-2}$$

$$\dot{q}_M'' \text{ for } y^* > 0.4 = 145 \text{ W cm}^{-2}$$

The effect of using a cubic temperature profile on the penetration distance δ will now be considered. Equation 7 on page 63 becomes

$$\Theta = \frac{\dot{q}_M''}{3k\delta^2} (\delta - x)^3 \text{ at } y = 0 \quad \text{--- VII 8}$$

and equation 8 becomes

$$\delta^2 = \frac{12\alpha}{u\dot{q}_M''} \int_{-\infty}^0 \dot{q}'' dy \text{ at } y = 0 \quad \text{--- VII 9}$$

substituting for $\dot{q}''$ from equation VI 12 gives

$$\delta_{y=0} = \sqrt{\frac{6\pi\alpha a}{u}} \quad \text{--- VII 10}$$

Figure VI 23 shows both the quadratic and cubic forms of the temperature profiles compared with the experimentally determined temperatures from runs C1 and C3. The values of 'a' and $\dot{q}_M''$ which are given in table VI 10 at $y < 0.4a$ were used to calculate the temperature profiles from equations VI 18 and 19 and equations VII 8 and 10. It can be seen from figure VI 23 that the cubic temperature profile predicts the actual temperature profile more accurately than does the quadratic temperature profile.

The above comparison shows that the replacement of the quadratic profile by a cubic would:-

1. Have no effect on 'a'
2. Increase $\dot{q}_M''$ by 7%
3. Increase δ by about 40%
4. Predict the actual temperature profile in the slab more accurately.

(v) Effect of conduction occurring in the direction of motion of the slab on the exact solutions.

Figure VII 1 shows that the maximum value of $\Theta_S^* \sqrt{u}^*$ in curve 3 is considerably less than the maximum values in curves 1 and 2 and that it occurs at $y^* = 0.7$ instead of $y^* = 1.0$. These differences are caused by conduction occurring in the y direction as well as in the x direction.

Figure VII 2 shows that the maximum value of $\Theta_S^* \sqrt{u}^*$ in curve 3 is dependent on the value of B^* . Thus for a material of constant thermal diffusivity α and for a heat flux distribution of constant width 'a', the maximum value depends on the velocity u. For $B^* > 10$, conduction in the y direction is negligible. For $B^* < 10$, conduction in the y direction becomes significant and the maximum value of $\Theta_S^* \sqrt{u}^*$ decreases rapidly when $B^* < 1$. These observations can be related to the results given in section VI 3 as follows:-

1. An approximate value of the slab speed u at which conduction in the y direction is negligible can be found from

$$B^* = \frac{ua}{2\alpha} > 10$$

From page 147, $\alpha = 8.06 \times 10^{-2} \text{ cm}^2 \text{ sec}^{-1}$,

'a' can be estimated to be about 1 cm if the heat flux distributions in figure VI 21 are approximated to a rectangular distribution. These values of α and 'a' give

$$u > 96 \text{ cm min}^{-1}$$

This value of u is much higher than any of the slab speeds used.

2. A slab speed of 2.62 cm min^{-1} , typical of the speeds used in the experiments, gives $B^* = 0.27$ which shows that the effect of conduction in the y direction is considerable at this slab speed. The maximum value of $\Theta_S^* \sqrt{u}^*$ decreases from 1.6 to 1.1 when the conduction in the y direction is taken into account, a reduction of 31%. This calculation

applies strictly to a rectangular distribution of heat flux, not to the normal distribution given by equation VI 12. However, the calculation does indicate that the method of measuring temperatures in an insulated element of the slab was justified in order to reduce conduction in the y direction. Figure VI 24 also illustrates this point because the maximum slab temperature is reduced from 940°C to 700°C when the insulation is removed, a reduction of 26%.

(vi) The assumptions required by the integral profile method.

These assumptions are given on page 61 of chapter III and will be discussed separately below.

1. Conduction of heat occurs in the x-direction only.
This has already been discussed in (v) above.
2. A steady state temperature field exists in the slab relative to the burner.

Section VI 3 (b) gives the results obtained from three thermocouples positioned near the surface of a slab and separated from each other by 10 cm. As the temperatures measured by these thermocouples all lay within $\pm 15^{\circ}\text{C}$, then assumption 2 is justified.

3. The initial temperature of the slab is taken to be at the zero of the temperature scale.
This assumption was made for convenience only and does not affect the accuracy of the solution. The ambient temperature was taken to be 20°C .
4. The physical properties of the slab do not vary with temperature.

The physical properties of En 8 are given in table C1 of appendix C. The mean specific heat and the thermal conductivity vary considerably with temperature but the density can be assumed to remain fairly constant. Using the data of figure C1, the thermal diffusivity varies between

$0.12 \text{ cm}^2 \text{ sec}^{-1}$ at 50°C and $0.081 \text{ cm}^2 \text{ sec}^{-1}$ at 400°C .

Despite the considerable variation with temperature of these properties, the integral profile method is not severely affected because all the solutions, e.g. equations VII 5 a and b, have the form

$$\Theta_S^* \sqrt{u^*} = \text{function of } y^*$$

$$\begin{aligned} \text{where } \sqrt{u^*} &= \frac{k}{\sqrt{\alpha}} \cdot \frac{\Theta_A}{\dot{q}_M''} \sqrt{\frac{u}{a}} \\ &= \sqrt{k\rho c} \cdot \frac{\Theta_A}{\dot{q}_M''} \sqrt{\frac{u}{a}} \end{aligned} \quad \text{--- VI 13}$$

and $k\rho c$ remains reasonably constant, e.g.

at 50°C, $\sqrt{k\rho c} = 0.89$ c.g.s units

at 500°C, $\sqrt{k\rho c} = 0.93$ c.g.s. units

A mean temperature of 400°C was assumed for all calculations.

There are two more assumptions inherent in the integral profile method. One of these is the use of a polynomial to represent the temperature profile in the slab and this has already been discussed in section (iv) above. The other assumption is that any heats of transition are negligible. The latent heats of transition that occur are:-

1.22 Kcal g atom⁻¹ at the Curie point & 0.156 Kcal g atom⁻¹ when ferrite (α) transforms to austenite (δ)⁽⁵⁶⁾ over the temperature range 720°C to 840°C for En 8 (see figure II 26 at 0.4% carbon).

The latent heat of transition at the Curie point is greater than the α to δ latent heat of transition. The Curie point is not readily available for En 8 but probably occurs somewhere between 600°C and 700°C as indicated by the sudden increase in the mean specific heat at these temperatures (see table C1 of appendix C). As there were no obvious discontinuities at 600°C to 700°C in the temperature profiles which were measured in the slabs the effect of the transitions were ignored, e.g. the no.1 thermocouple readings given in figure C4 of appendix C show no discontinuities at these temperatures.

(b) Heat Flux distributions

(i) Shape of the distribution

Section VI 3(a)(ii) describes how the heat flux distributions were obtained from the measured surface temperature. Appropriate

values of 'a' were chosen to fit the predicted surface temperature curve to the measured surface temperature curve and figure VII 19 shows that two values of 'a' were required to obtain a good fit. A larger value was required at $y^* < 0.4$, i.e. for a rising slab temperature, than at $y^* > 0.4$, i.e. during the cooling of the slab. Table VII 10 shows the two values of 'a' obtained from all the runs, C1 to C7. The fact that two values of 'a' were necessary suggests that the distribution of heat flux at the surface of a moving slab was asymmetric, having lower values in the region of $y^* > 0$ than in the region of $y^* < 0$ as shown in figure VII 4.

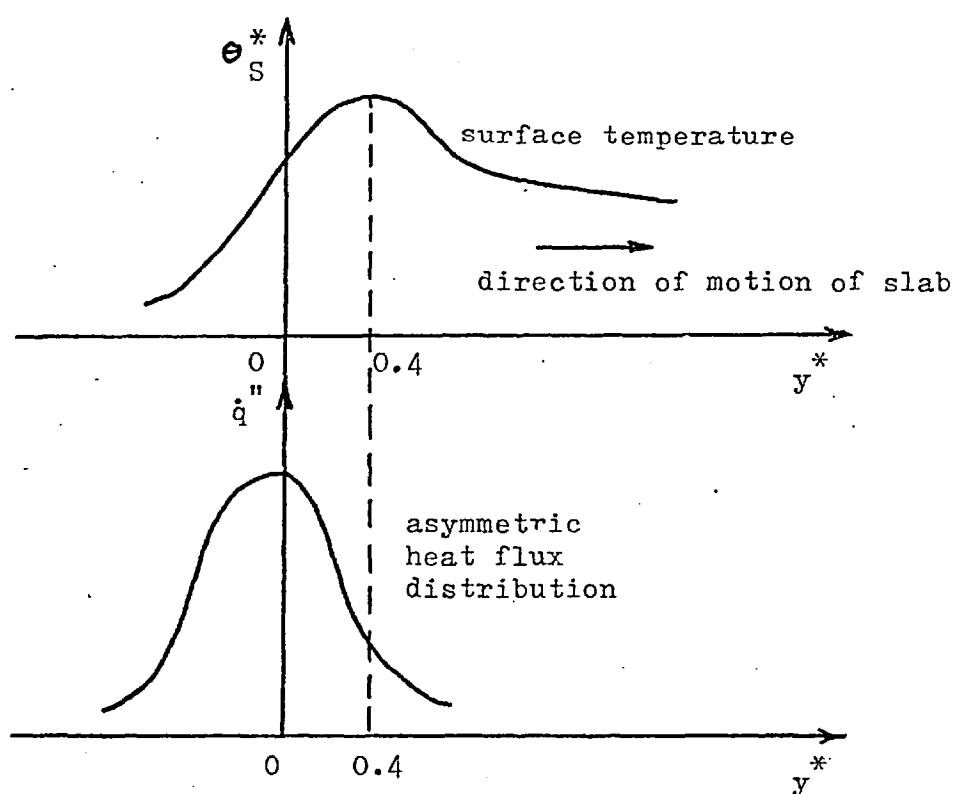


Figure VII 4

An explanation for the reduced heat flux at $y^* > 0$ is that the surface temperature of the slab was generally higher in this region than at $y^* < 0$. The high surface temperature would reduce the surface heat flux by the following means:-

1. by reducing the driving force for heat transfer between the hot products of combustion and the surface of the slab, and
2. by reducing the heat flux due to the recombination of dissociated species at the surface of the slab (see section II 5(a))

- (ii) The effect of electrical augmentation on the heat flux distribution and on the overall rate of heat transfer to the surface.

Figure VI 20 on page 151 shows that electrical augmentation doubled the value of the maximum heat flux that was obtained at the surface of the slab but table VI 10 shows that this twofold increase was not obtained over all y because 'a' was decreased from 1.49 cm to 1.09 cm. Figure VI 20 shows that the heat flux at $y = 4$ cm was barely influenced by electrical augmentation.

The effect of electrical augmentation on the overall rate of heat transfer $\dot{q}$ from the burner to the surface of the slab can be obtained from figure VI 20. The net rate of heat transfer $\dot{q}$ within a radius y is given by

$$\dot{q} = 2\pi \int_0^y y \dot{q}'' dy \quad \text{--- VII 11}$$

Substituting for $\dot{q}''$ from equation VI 12 on page 145 and integrating gives

$$\dot{q} = \pi \dot{q}_M'' \ln \left[1 + \left(\frac{y}{a} \right)^2 \right] \quad \text{--- VII 12}$$

Both $\dot{q}_M''$ and $\frac{y}{a}$ are higher when the flame is electrically augmented, giving rise to considerably increased values of $\dot{q}$ as shown in figure VII 5a where $\dot{q}$ is plotted against y for runs C1 (unaugmented) and C3 (augmented).

It is also of interest to assess the proportion of the combustion and electrical energy supplied to the burner that is transferred to the slab as heat. The total rate of release of combustion energy in the flame was calculated to be 4 kW, assuming that 7.5 l min^{-1} of methane were burnt and that the calorific value of methane was 8 Kcal l^{-1} (54).

The total input of electrical power was 3 kW, assuming that 3.0 A at 1 kV were consumed at a power factor of unity. This data, together with the data given in figure VII 5a, allows the following ratios to be calculated at values of y up to 4 cm:-

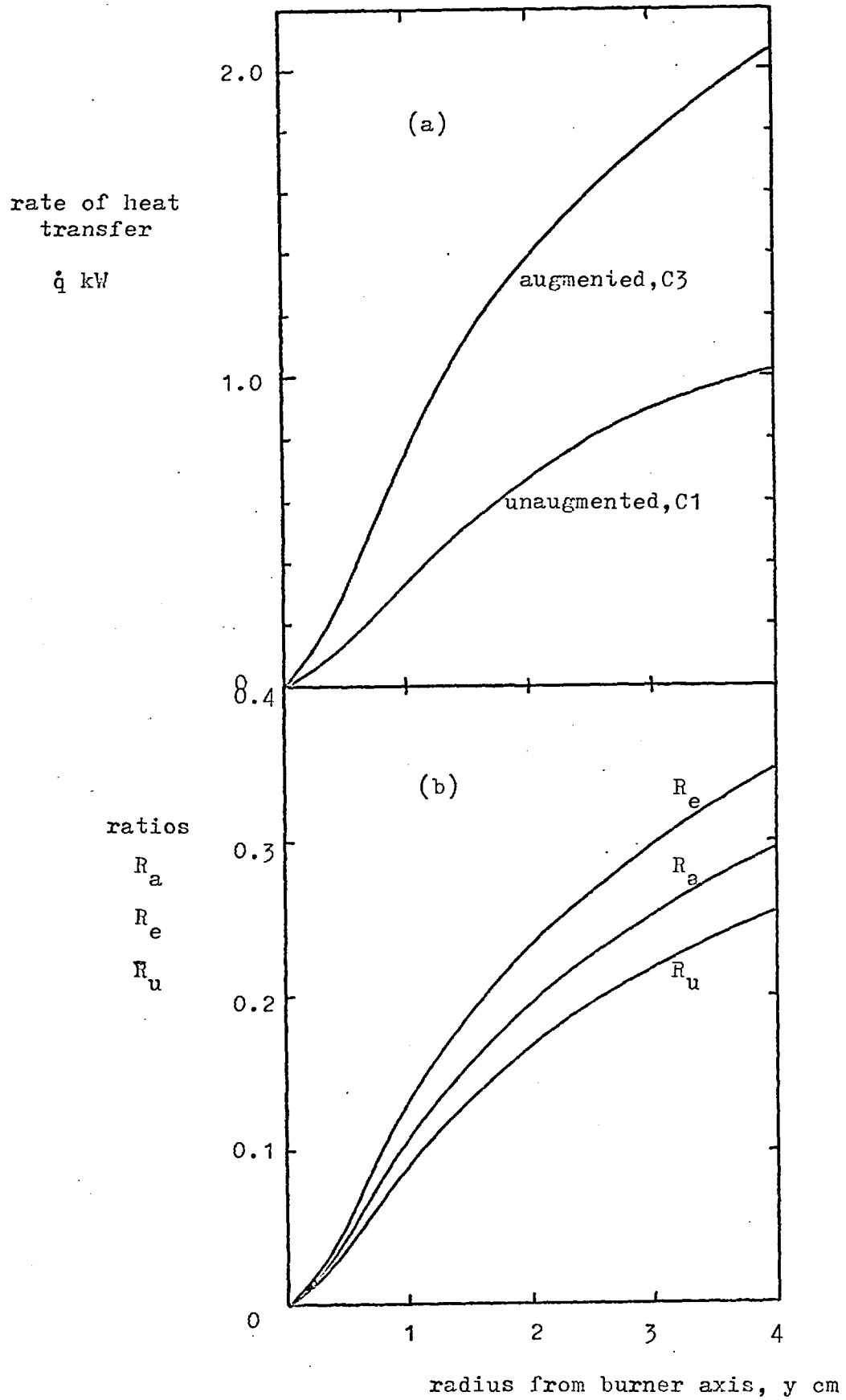


FIGURE VII 5 - RATES OF HEAT TRANSFER AGAINST RADIUS

$$R_u = \frac{\text{rate of heat transfer } \dot{q} \text{ from unaugmented flame to surface}}{\text{total rate of release of combustion energy in the flame}}$$

$$R_a = \frac{\text{rate of heat transfer } \dot{q} \text{ from augmented flame to surface}}{\text{total rate of release of combustion energy + electrical power input}}$$

$$R_e = \frac{\dot{q} \text{ augmented flame} - \dot{q} \text{ unaugmented flame}}{\text{electrical power input to flame}}$$

These ratios are shown plotted against radius y in figure VII 5b. Ratios R_a and R_e , are greater than ratio R_u , showing that a higher proportion of electrical energy than combustion energy is used to heat the slab, in particular

$$\frac{R_e}{R_u} \approx 1.5 \text{ for } 0 < y < 4 \text{ cm}$$

- (iii) The effects of burner to slab distance and slab speed on the heat flux distribution.

Figure VI 20 shows that the effect of increasing the distance between the slab and the burner was to decrease the maximum value of the heat flux.

Figure VI 21 shows that when the slab speed was reduced, the maximum value of the heat flux decreased. Figures C3, C4 and C5 in appendix C show that the surface temperature of the slab would be increased by reducing the speed of the slab. The maximum value of the heat flux would be expected to decrease according to the explanation given in section (i) above (see reasons 1 and 2).

- (iv) The heat flux distribution obtained at the surface of a moving slab compared with that obtained at a water-cooled surface.

The heat flux distribution obtained from run C6 (see table VI 10 and figure VI 22 is shown together with a comparable heat flux distribution provided by the split plate calorimeter (see figure VI 15) in figure VII 6. Both distributions were obtained when the flame was augmented with 3.0 A at 1 kV but the distance between the burner and the slab was 1.5 cm whilst the distance between the burner and the calorimeter was 2.0 cm.

From the two curves it can be seen that there was some justification for assuming that the heat flux distribution at the surface of a slab would have the form

$$\dot{q}'' = \dot{q}_M'' \left[\frac{1}{1 + \left(\frac{y}{a}\right)^2} \right] \quad \text{--- VI 12}$$

because the distribution of heat flux at a water-cooled surface appears to obey the same equation with slightly different values of $\dot{q}_M''$ and a .

(c) Penetration distances

Equation VI 18 on page 154 expresses the penetration distance δ at $y = 0$ as a function of a , u and α . Table VI 10 on page 150 shows that greater values of a were obtained for runs C1 and C2 (unaugmented) than for runs C3 to C7 (augmented). Substitution of these values of a into equation VI 18 gives values of δ which are greater for the unaugmented flame than for the augmented flame, i.e.

$$\text{for run C1, } \delta_{y=0} = 51 \text{ mm}$$

$$\text{for run C3, } \delta_{y=0} = 44 \text{ mm}$$

If the depth of the slab is less than the maximum value of the penetration distance, then a "reflection" of heat will occur inside the slab at the surface opposite the heated surface. The slab temperature will rise to higher values than if the depth of the slab exceeds the penetration distance. This effect probably occurs during runs C1 and C2 where δ exceeded the slab depth of 50 mm and then only to a small extent.

The maximum value of δ can be shown to occur at the same position as the maximum surface temperature by putting $x = 0$ in equation 7 on page 63, i.e. the maximum value of δ occurs at $y = 0.4 a$.

(d) Temperature profiles inside a slab

The temperature profiles inside a slab at $y = 0$ are given in figure VI 23 and have been discussed in section VII 4(a)(iv) above. Quadratic profiles at other values of y can be calculated from equation 7 on page 63. Similarly, cubic profiles can be calculated from

$$\theta_j = \frac{\dot{q}_j''}{3k \delta_j^2} (\delta_j - x)^3 \quad \text{--- VII 13}$$

VII 4(d)

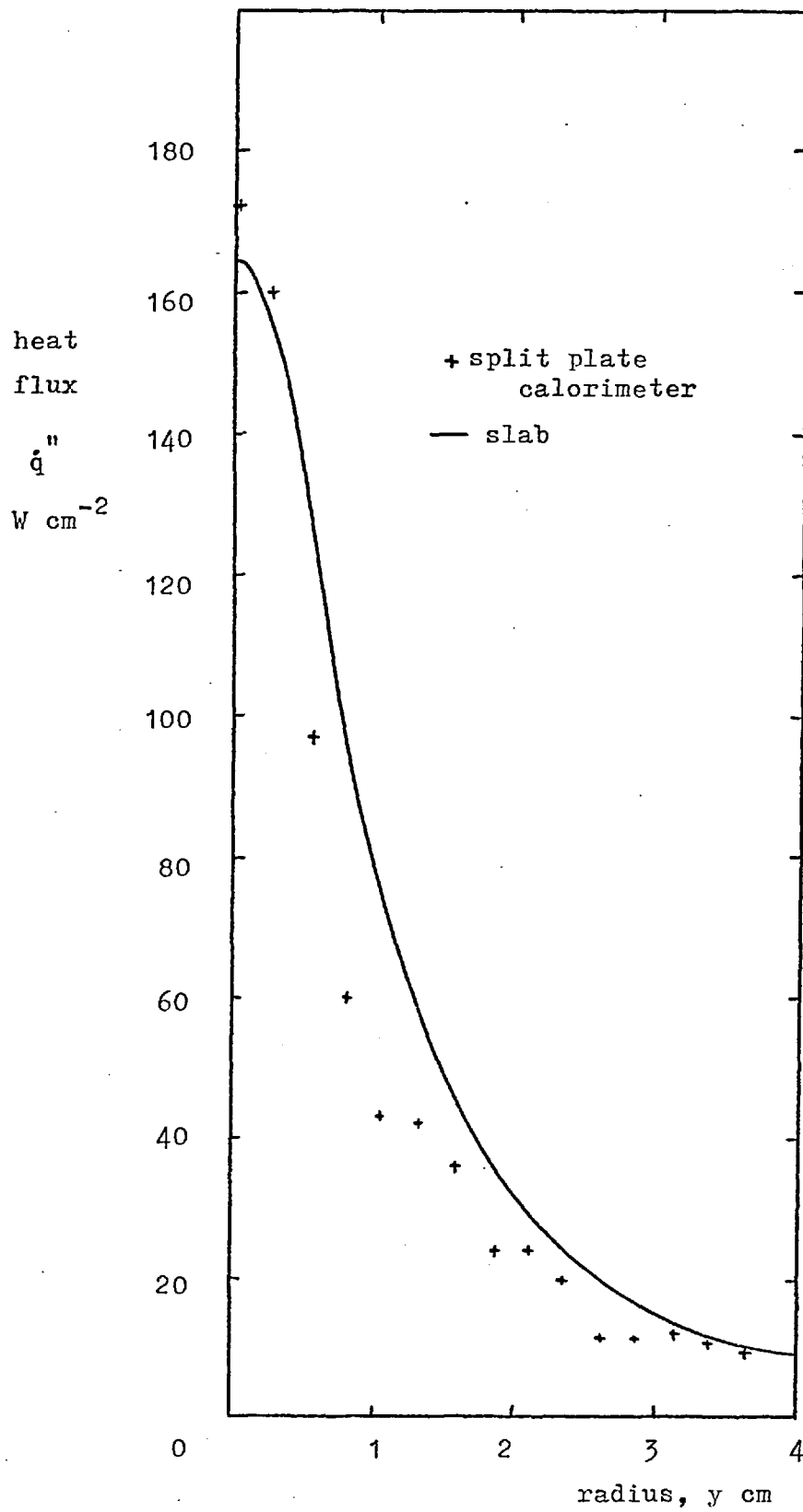


FIGURE VII 6 - HEAT FLUX DISTRIBUTIONS

5. QUENCHING

Section VI 4 describes the conditions under which quenching was carried out and figures VI 25 to 28 give the temperature profiles which were obtained under these conditions. The maximum surface temperatures obtained with quenching are lower than those obtained without a quench (compare figure VI 25, curve C with figure VI 24). Conduction in the y direction probably increases when the slab is quenched.

(a) Effect of slab size

As in all previous work, these experiments were conducted with slabs of width 12 mm. Figure VI 25 shows that the depth of the slab influences both the surface temperature and the rate of quenching.

(i) On surface temperature

The maximum surface temperature of a slab decreases as the depth of the slab is increased, this effect being much greater when the depth is increased from 12 mm to 25 mm than from 25 mm to 50 mm. This effect can be explained by considering the penetration distance calculated for a semi-infinite slab. Section VII 4 (c) above discusses the effect of slab depth in relation to penetration distance on the temperature of the slab.

Substituting $u = 1.45 \text{ cm min}^{-1}$ and $a = 1.0 \text{ cm}$ (approximate value obtained from table VI 10 on page 150) into equation VI 18 on page 154, gives

$$\delta_{y=0} = 56 \text{ mm}$$

The maximum value of δ occurs at $y = 0.4 \text{ cm}$, i.e.

$$\delta_{y=0.4 \text{ cm}} > 56 \text{ mm}$$

A slab depth of probably 60 mm or greater would be required in order to obtain a surface temperature profile which was not influenced by slab depth for a slab speed of 1.45 cm min^{-1} . Table VI 11 gives penetration distances at faster slab speeds.

(ii) On rate of quenching

The rates of quenching expressed as the rate of cooling of the slab surface in $^{\circ}\text{C sec}^{-1}$ can be obtained from figure VI 25, as explained in section VI 4 (d).

Slab depth mm	Quenching rate $^{\circ}\text{C sec}^{-1}$
12	7.5
25	4.0
50	3.3

Table VII 2

Table VII 2 shows that the quenching rates increase as the slab depth is reduced.

(b) Variation of quench water flow rate

Figure VI 26 shows that the water flow rate had little effect on the rate of quenching which remained approximately constant at $15^{\circ}\text{C sec}^{-1}$.

The water flow rate had considerable influence on the surface temperature profile. The maximum temperature increased when the water flow rate was reduced. The slotted nozzle was limited to a minimum flow rate of $270 \text{ cm}^3 \text{ min}^{-1}$ because further reduction in the flow rate prevented the jet of water from impinging on the surface of the slab. The nozzle design was changed to the five-hole nozzle which provided higher velocity jets at lower water flow rates.

Figure VI 28 shows a surface temperature profile obtained using the five-hole nozzle at a water flow rate of $100 \text{ cm}^3 \text{ min}^{-1}$. This profile cannot be compared with those in figure VI 26 because the conditions of the experiment were changed in order to prevent the maximum surface temperature from exceeding 1200°C , the maximum operating temperature of the thermocouple. This was done by using a deeper slab at a faster speed.

A heat balance on the slab allows the minimum water flow rate Q to be calculated, assuming that all the water is evaporated at 100°C . Extrapolation of figure VII 5 a gives the approximate rate of heat transfer to the slab as 2.5 kW over a radius of 6 cm. The latent heat of evaporation of water is 539 cal g^{-1} (54).

$$\begin{aligned}\text{Hence} \quad Q &= \frac{2.5 \times 60}{0.539 \times 4.18} \text{ g min}^{-1} \\ &= 67 \text{ cm}^3 \text{ min}^{-1}\end{aligned}$$

This value of Q is of the same order as the minimum value used in the experiments.

(c) Position of the quench

Figure VI 27 gives surface temperatures at quench to burner distances of 2.5 cm and 4.0 cm. There is a small decrease in the maximum surface temperature when the distance is reduced from 4.0 cm to 2.5 cm but the rate of quenching is increased from about $15^{\circ}\text{C sec}^{-1}$ to about $25^{\circ}\text{C sec}^{-1}$.

(d) Speed of the slab

Insufficient data was obtained to draw definite conclusions about the effect of slab speed on the surface temperatures, or on the rates of quenching. Table VII 3 was obtained from figures VI 25 to 28 and provides some evidence that the rate of quenching increases as the slab speed is increased.

Slab speed cm min^{-1}	Slab depth cm	Quench position cm	Water flow $\text{cm}^3 \text{min}^{-1}$	Rate of quenching $^{\circ}\text{C sec}^{-1}$
1.45	25	8	1000	4
1.45	12	8	1000	7.5
2.62	12	4	270-1000	15
2.62	12	2.5	270	25
4.8	25	2.5	100	32

Table VII 3

An increase in slab speed would be expected to give a higher rate of quenching because the heat content per unit length of the slab would be reduced.

(e) Quench heat flux

A value for the quench heat flux has been calculated in section VI 4 (e). This value of 165 W cm^{-2} is in agreement with the heat fluxes obtained for film boiling (see section II 7 (d)), i.e. 35 W cm^{-2} at 150°C to 315 W cm^{-2} at 1500°C . These results for film boiling show that $\dot{q}^{**}$ falls in the range of 0.5 to 2.0 whereas the calculated value of the quench heat flux of 165 W cm^{-2} corresponds to $\dot{q}^{**} = 1.0$.

(f) Summary

The rate of quenching of a slab will be high if

1. The slab depth is small.

~~2. The quench is positioned close to the burner.~~

2. The quench is positioned close to the burner.

3. The slab speed is fast.

6. FLAME HARDENING

(a) Conditions required by the process

En 8, 0.4% carbon steel, can only be flame hardened when certain conditions are fulfilled. These conditions can be deduced from information given in sections II 7 (a) and (b).

Figure II 28 gives temperature-time curves for the beginning and the end of the transformation to austenite in a 0.8% carbon steel. The equilibrium diagram for plain carbon steels which is shown in figure II 26 indicates that 100% austenite will not occur below 825°C in a steel containing 0.4% carbon. However, 100% austenite will occur at 720°C or more in a steel containing 0.8% carbon. On this basis, figure II 28 can be altered to represent a 0.4% carbon steel if the 99.5% austenite transformation curve is shifted upwards by 105°C. The curve then approaches 825°C instead of 720°C after a long time has elapsed. From the modified figure II 28, about 10 seconds at 850°C would be required to obtain nearly complete transformation to austenite in En 8 steel. The condition for the start of austenite transformation in En 8 is the same as that for all plain carbon steels, i.e. the temperature of the steel should be raised to 720°C.

Figures II 30 and II 31 can be combined to show that high hardness values (corresponding to nearly complete martensite transformation) are obtained for En8 if the rate of quenching is at least 100°C sec⁻¹ at 550°C. The rates of cooling given in figure II 31 were calculated (50) by applying Newton's law of cooling which states that the rate of cooling is proportional to the temperature difference. This law does not necessarily hold true during quenching as shown in figures VI 25, 26 and 27 where the rates of cooling are expressed in °C cm⁻¹ and are approximately linear over a wide range of slab temperatures; Newton's law would predict a falling rate as the slab temperature decreased.

The cooling rate required to produce martensite can also be estimated from the isothermal transformation diagram for En 8 given in figure II 29. This diagram indicates that a cooling rate of about $100^{\circ}\text{C sec}^{-1}$ is required between 825°C and 365°C in order to ensure transformation to martensite. This value of $100^{\circ}\text{C sec}^{-1}$ may be in considerable error because an isothermal transformation diagram does not represent the cooling conditions experienced during flame hardening. This, and other sources of error are explained in section II 7 (a)(ii).

The following conditions for flame hardening are deduced from the integral profile solution in section III 2:-

$$u^* < 2.72; \theta_S^* > 1.0; \text{ quench applied at } y^* > 0.4$$

The maximum value for the slab speed u at which flame hardening will occur can be calculated from these conditions for any measured burner heat flux, e.g. for a heat flux where $\dot{q}_M'' = 165 \text{ W cm}^{-2}$ and $a = 1.09 \text{ cm}$ (obtained during run C3 and given in table VI 10). Substituting these values of $\dot{q}_M''$ and a , and the values of k and α given on page 147 into equation VI 13 on page 145 and rearranging, gives

$$u = 3.46 \text{ cm min}^{-1}$$

The values of 'a' and $\dot{q}_M''$ were obtained at a slab speed of 2.62 cm min^{-1} but values which were not widely different were obtained at higher slab speeds (see run C4 in table VI 10). The quench heat flux required to transform the surface of the slab to martensite can also be obtained from the above conditions. The severity of quenching, $\dot{q}''^*$, which is sufficient to give a quenching rate of $100^{\circ}\text{C sec}^{-1}$ can be calculated using figure III 8 on page 70 as follows:-

The temperature gradient $\frac{d\theta}{dy}$ along the surface of the slab during quenching is given by

$$\frac{d\theta}{dy} = \frac{100}{u} = 1740^{\circ}\text{C cm}^{-1} \text{ for all } \theta$$

$$\text{But } \theta_S = \theta_A = 825 - 20^{\circ}\text{C at } y^* = 0.4, \text{ where } y = y^* a$$

$$\text{and so } \theta_S = 0, \text{ at } y = 0.4 a + \frac{\theta_A}{\frac{d\theta}{dy}}$$

$$\text{giving } \theta_S^* = 0 \text{ at } y^* = \frac{0.90}{a} = 0.82$$

A straight line drawn on figure III 8 from

$$\Theta_S^* \sqrt{u^*} = 1.65 \text{ at } y^* = 0.4 \text{ to } \Theta_S^* \sqrt{u^*} = 0$$

at $y^* = 0.82$ approximates to the curve of $\Theta_S^* \sqrt{u^*}$ against y^* when $\dot{q}^{''*} = 2.0$. This value of $\dot{q}^{''*}$ gives

$$\begin{aligned} \text{quench heat flux} &= \dot{q}^{''*} \cdot \dot{q}_M'' \\ &= 330 \text{ W cm}^{-2} \end{aligned}$$

This value is approximately equal to the heat flux occurring during film boiling at a surface temperature of 1500°C , as given in section II 7 (d). A heat flux of this value could also be obtained at a lower surface temperature if a high velocity water jet were used to disrupt the film of vapour which forms on the surface of the hot metal.

These values of the slab speed and the quench heat flux which have been predicted by the integral profile method using a measured distribution of burner heat flux are the limiting values required to form a very thin layer of martensite at the surface of a slab of En 8. The depth of the slab has been assumed to exceed the maximum value of the penetration distance, estimated at 3 to 4 cm from section VI 3 (a) (iii) and conduction of heat inside the slab has been assumed to occur in the x direction only. Figure VII 2 on page 184 is a plot of the maximum value of $\Theta_S^* \sqrt{u^*}$ against B^* for a rectangular heat flux distribution when conduction occurs in both the x and y directions. A slab speed of 3.5 cm min^{-1} gives $B^* = 0.37$ which shows that conduction in the y direction at this slab speed may be expected to reduce the maximum surface temperature by about 20%. A 5% reduction in the maximum surface temperature would be expected if $u = 36 \text{ cm min}^{-1}$ and $B^* = 3.7$ but the maximum heat flux would need to be about 500 W cm^{-2} in order to raise the temperature of the surface to 825°C .

The amount of martensite which was formed during the experiments given in section VI 5 will now be discussed in relation to the conditions set out above.

(i) No martensite formed.

Surface temperatures were not measured during the experiments numbered 1, 2 and 3 in section VI 5 (a) but surface temperatures were obtained during experiments carried out to investigate quenching under similar conditions. The surface temperatures occurring during experiment 1 approximate to curve C in figure VI 25. The maximum surface temperature of 680°C is below the temperature at which austenite transformation begins. The surface temperatures occurring during experiment 2 will be represented by a curve which reaches a lower maximum temperature than curve B in figure VI 25 because the slab speed used in experiment 2 was higher than that used to obtain curve B. The maximum temperature of curve B is 750°C and so the temperature attained during experiment 2 was unlikely to have been as high as 720°C , the temperature at which austenite transformation begins.

(ii) Thin layer of martensite formed

The experimental conditions under which the martensite shown in plate VI 3 was formed are given in section VI 5 (b) and the surface temperatures are shown as curve B, figure VI 27. The maximum surface temperature was 980°C and the surface spent 45 seconds above 720°C and 35 seconds above 825°C , ensuring that the surface was nearly completely transformed to austenite according to the conditions given above. The rate of quenching is given in table VII 3 as $25^{\circ}\text{C sec}^{-1}$ this being considerably lower than the value of $100^{\circ}\text{C sec}^{-1}$ deduced above for transformation to martensite. The thermocouple which was used to measure the surface temperature was positioned at about 0.5 mm from the surface whereas the layer of martensite was only formed to a depth of 0.04 mm. The rates of quenching were, therefore, probably higher than $25^{\circ}\text{C sec}^{-1}$ at the surface of the slab. Experiments 1, 2 and 3 in section VI 5 (b) also produced thin layers of martensite and it can be deduced that austenite transformation at the surface was nearly complete in all cases. The rate of quenching was limited to about $30^{\circ}\text{C sec}^{-1}$ according to the values given in table VII 3 for similar experiments.

The slab speed predicted by the integral profile method of 3.46 cm min^{-1} would be too great to produce any martensite because conduction in the y direction is significant.

(iii) Thick layer of martensite formed

A thick layer of martensite could only be produced when the two pieces of steel on either side of the slab were removed. The effect of retaining these pieces of steel was probably to decrease the rate of quenching of the slab because the large temperature difference which would exist between these hot pieces of steel and that portion of the slab which was being quenched probably caused some heat to be transferred to the sides of the slab in the region of quenching despite the presence of insulation (see figure IV 10 on page 91). A secondary effect of removing the two pieces of steel would be to increase the burner heat flux at the surface of the slab because that part of the arc which was transferred to the surrounding steel would be transferred to the slab instead. This extra heat flux would enable a higher slab speed to be used which would also increase the rate of quenching. A slab speed as fast as 4.8 cm min^{-1} was used with a slab of depth 25 mm whereas the slab speed was restricted to 2.62 cm min^{-1} when the two pieces of steel were in position.

(b) Microstructures and hardness values.

A detailed examination of the microstructures was not carried out. Martensite was identified by its hardness and appears as the grey phase in plate VI 3 to a depth of 0.04 mm. An indentation from the microhardness tester could be just fitted into this layer and gave a hardness of about 600 V.P.N. The hardness traverse given under plate VI 3 shows that hardness values of about 200 V.P.N. occurred beyond 0.04 mm. These values correspond to a relatively fine-grained structure of ferrite (white) and pearlite (dark). The fine grain was caused by rapid cooling and gave hardness values which were a little higher than those obtained from the normalized coarser structure of the untreated steel.

Plate VI 4 shows the thicker layer of martensite. The hardness traverse immediately below the photograph gives hardness values which correspond to martensite for a distance of about 2 mm, the values which are greater than 400 V.P.W. being obtained when the indentation coincided with a white area. The hardness values expected when En 8 is flame hardened commercially are given in section II 7 (c)(i) and they are in agreement with those shown in the hardness traverse below plate VI 4.

(c) Surface finish

The deterioration in surface finish is shown by the Tallysurf measurements in figure VI 29. The charts show that the ground, untreated surface has a finish of about 2×10^{-4} inch (5×10^{-3} mm) measured over 0.25 inch (6 mm). The corresponding measurement for the hardened steel is about 2×10^{-3} inch or 0.05 mm. This deterioration in surface finish was probably due to the small marks which appeared on the surface and which are described in section VI 5 (d). The marks were probably caused by melting when the arc was transferred to the surface of the slab.

The absence of scale on the surface of the slab was presumably the result of operating the burner with a fuel-rich mixture.

(d) Further work

The burner could well be further developed to give:-

(i) Higher heat fluxes.

This would enable a faster slab speed to be used during flame hardening. Besides increasing productivity, a faster slab speed would probably give higher rates of quenching (see section VII 5(d)).

(ii) Longer electrode life.

This has been discussed in section VII 1(b).

(iii) Improved surface finish

The marks observed on the surface of the slab in section VI 5(d) could be eliminated if the arc is restricted to striking between the electrodes and is therefore not transferred to the slab. The annular electrode would need to be altered.

VIII CONCLUSIONS

Seeding the flame is unnecessary in the electrically augmented burner developed in this work.

The rates of heat transfer from the burner to water-cooled surfaces are highest when the burner is operated with a fuel-rich mixture.

Distributions of heat flux from the burner to a water-cooled surface have been determined and have been found to have maximum values which lie within the range of those reported by other workers⁽³⁴⁻³⁷⁾ using similar systems.

The distribution of burner heat flux at the surface of a moving slab can be predicted using the integral profile method to solve the heat conduction equation for the slab if the surface temperature is measured and conduction in the direction of motion of the slab can be ignored. Similarly, the surface temperature of the slab can be predicted if the distribution of heat flux is known.

The heat flux from the electrically augmented flame to a water-cooled surface or to the surface of a moving slab has a maximum value which is at least twice the maximum value obtained without electrical augmentation.

The ratio

$$\frac{\text{electrical energy transferred to the slab as heat}}{\text{total input of electrical energy to the burner}}$$

is greater than the ratio

$$\frac{\text{chemical energy transferred to the slab as heat}}{\text{total release of chemical energy in the burner}}$$

The integral profile method can be used to predict the slab speed and the quench heat flux which are required for flame hardening if the burner heat flux is already known and is of a sufficiently high value that conduction along the direction of motion of the slab can be neglected.

The electrically augmented burner can be used to surface harden an En 8 steel. Under certain conditions, the hardness values and the thickness of the hardened layer are comparable to those obtained in industrial practice.

APPENDIX A

RESULTS OBTAINED FROM THE CONCENTRIC
CALORIMETERS

Air flow l min ⁻¹	Distance between burner and calorimeter, cm												% methane
	0.32		0.64		1.27		2.54		5.1		7.6		
	U ^o C	A ^o C	U ^o C	A ^o C	U ^o C	A ^o C	U ^o C	A ^o C	U ^o C	A ^o C	U ^o C	A ^o C	
6.9	—	33.0	—	30.0	—	31.0	—	29.0	—	24.0	—	19.0	11.5
7.5	20.0	27.0	20.0	25.5	18.5	24.5	16.5	24.0	16.5	22.0	10.5	16.5	10.7
8.0	19.0	26.0	19.0	24.0	18.0	23.0	15.5	22.0	13.0	19.0	8.5	14.0	10.1
9.1	18.0	25.0	18.0	23.0	16.0	21.5	13.0	20.0	9.5	16.0	7.0	12.0	9.0
10.3	18.0	24.0	17.0	23.0	16.0	21.0	13.0	18.0	9.0	14.0	7.0	10.0	8.0
11.5	—	24.0	—	22.0	16.0	20.0	13.0	16.5	9.0	12.0	7.5	9.0	7.3
12.7	19.5	24.0	18.5	23.0	16.0	19.0	13.0	16.5	9.5	11.0	7.5	9.0	6.6
Water cm ³ min ⁻¹	215	215	215	215	215	215	215	215	215	215	215	215	

U=unaugmented, A=augmented

TABLE A1 — TEMPERATURE DIFFERENCES FOR 130 MM CALORIMETER AT VARIOUS AIRFLOWS AND DISTANCES

Air flow l min ⁻¹	Distance between burner and calorimeter, cm												% methane
	0.32		0.67		1.27		2.54		5.1		7.6		
	U ^o C	A ^o C	U ^o C	A ^o C	U ^o C	A ^o C	U ^o C	A ^o C	U ^o C	A ^o C	U ^o C	A ^o C	
6.9	—	52.0	—	46.5	—	43.0	—	43.0	—	36.5	—	30.0	11.5
7.5	42.0	46.0	40.0	40.0	40.0	39.0	37.5	35.5	30.0	31.0	22.0	28.5	10.7
8.0	41.5	40.0	40.0	37.5	36.0	34.0	35.0	31.0	27.5	24.5	18.0	20.0	10.1
9.1	38.5	37.0	38.5	35.5	33.0	30.0	29.0	26.0	15.5	14.5	10.5	11.0	9.0
10.3	39.0	36.5	37.5	35.0	32.5	30.5	25.5	24.5	15.0	14.0	10.0	10.0	8.0
11.5	40.0	37.0	38.0	35.0	34.0	31.0	27.5	24.5	15.5	14.0	9.5	9.5	7.3
12.7	41.5	37.0	40.0	35.0	36.0	31.5	28.0	25.0	17.0	14.0	10.5	9.5	6.6
Water cm ³ min ⁻¹	96	138	96	138	96	138	96	138	96	138	96	138	

TABLE A2 — TEMPERATURE DIFFERENCES FOR 50 MM CALORIMETER AT VARIOUS AIRFLOWS AND DISTANCES

Air flow l min ⁻¹	Distance between burner and calorimeter, cm												% methane
	0.32		0.64		1.27		2.54		5.1		7.6		
	U°C	A°C	U°C	A°C	U°C	A°C	U°C	A°C	U°C	A°C	U°C	A°C	
6.9	—	24.0	—	23.0	—	21.0	—	19.0	—	36.0	—	26.0	11.5
7.5	14.0	17.5	12.5	18.5	12.5	17.0	20.0	15.5	25.5	31.0	16.5	15.5	10.7
8.0	14.0	17.5	12.5	17.0	12.5	16.5	19.5	14.5	25.0	27.5	10.5	11.0	10.1
9.1	14.0	17.5	12.5	16.5	12.5	16.5	18.5	14.5	19.0	22.5	7.0	9.0	9.0
10.3	14.0	17.5	12.5	16.5	12.5	16.5	18.0	13.5	13.5	18.5	5.0	7.0	8.0
11.51	14.5	17.0	14.0	16.5	13.0	15.5	18.0	13.5	13.0	14.5	4.5	7.5	7.3
12.7	14.5	17.5	14.0	17.5	13.0	16.5	—	18.0*	—	11.0	—	7.5	6.6
Water cm ³ min ⁻¹	168	168	168	168	168	168	108	168	108	82	69	70	

* water=138 cm³min⁻¹ , U=unaugmented, A=augmented

TABLE A3 - TEMPERATURE DIFFERENCES FOR 25MM CALORIMETER AT VARIOUS AIRFLOWS AND DISTANCES

Air flow l min ⁻¹	Distance between burner and calorimeter, cm												% methane
	0.32		0.64		1.27		2.54		5.1		7.6		
	U ^o C	A ^o C	U ^o C	A ^o C	U ^o C	A ^o C	U ^o C	A ^o C	U ^o C	A ^o C	U ^o C	A ^o C	
6.9	—	38.5	—	36.0	—	31.5	—	28.0	—	28.0	—	18.0	11.5
7.5	35.0	33.0	31.5	29.0	30.0	26.5	24.0	24.0	22.0	24.0	19.0	21.5	10.7
8.0	24.5	31.5	24.0	28.0	23.0	25.5	22.0	22.5	18.0	23.0	7.5	16.0	10.1
9.1	25.0	31.5	24.0	28.0	23.0	24.5	22.0	22.5	15.5	20.5	4.5	10.0	9.0
10.3	25.5	31.0	25.0	28.0	23.0	24.0	21.5	22.5	11.5	15.0	4.0	6.5	8.0
11.5	25.5	29.0	24.0	26.0	25.0	24.0	21.5	21.5	8.5	11.5	4.0	5.5	7.3
12.7	25.5	28.0	25.0	25.5	25.0	24.0	21.0	20.5	7.5	8.0	4.0	5.0	6.6
Water cm ³ min ⁻¹	42	56	42	56	42	56	42	56	42	42	42	42	

U=unaugmented, A=augmented

TABLE A4 — TEMPERATURE DIFFERENCES FOR 13 MM CALORIMETER AT VARIOUS AIRFLOWS AND DISTANCES

Air flow l min ⁻¹	Distance between burner and calorimeter, cm												% methane
	0.32		0.64		1.27		2.54		5.1		7.6		
	U°C	A°C	U°C	A°C	U°C	A°C	U°C	A°C	U°C	A°C	U°C	A°C	
6.9	—	16.0	—	12.0	—	22.0	—	16.5	—	14.0	—	6.5	11.5
7.5	16.0	10.5	15.5	7.5	14.5	16.0	14.0	14.0	11.0	12.0	5.5	6.0	10.7
8.0	14.0	10.5	14.0	7.5	13.5	15.5	13.0	13.5	10.0	11.0	3.0	5.0	10.1
9.1	14.0	8.0	14.0	7.0	13.5	15.5	13.0	13.5	7.0	9.0	1.0	2.0	9.0
10.3	13.5	7.5	14.0	7.0	13.0	15.5	12.0	13.0	5.0	6.5	0.5	1.0	8.0
11.5	13.5	7.0	13.5	7.0	13.5	14.0	12.0	12.5	4.0	5.0	0.5	1.0	7.3
12.7	13.5	7.0	13.5	7.0	13.5	14.0	12.5	12.0	3.0	4.0	0.5	1.0	6.6
Water cm ³ min ⁻¹	18	35	18	35	18	18	18	18	18	18	18	18	

U=unaugmented, A=augmented

TABLE A5 - TEMPERATURE DIFFERENCES FOR 6MM CALORIMETER AT VARIOUS AIRFLOWS AND DISTANCES

Radius r cm	Distance between burner and calorimeters , cm												
	0.32	0.64	1.27	2.54	5.10	7.60	0.32	0.32	0.32	0.32	0.32	0.32	0.32
0.21	138	103	97	73	62	35	62	94	90	69	65	60	60
0.51	105	107	92	85	61	43	54	92	99	99	92	92	56
1.00	37	36	34	32	35	21	26	23	21	23	23	24	27
1.98	15	12	12	13	10	11	8	12	16	10	10	11	7
4.93	0	0	0	0	0	0	0	0	0	0	0	0	0
%methane	11.5	11.5	11.5	11.5	11.5	11.5	10.1	10.1	10.7	9.0	8.0	7.3	6.6
A or U	A	A	A	A	A	A	U	A	A	A	A	A	A

A=augmented, U=unaugmented

TABLE A6 - HEAT FLUXES AT VARIOUS DISTANCES BETWEEN THE SMALL VORTEX-STABILIZED BURNER AND THE CONCENTRIC CALORIMETERS (HEAT FLUXES IN WATTS/CM²).

APPENDIX B

RESULTS OBTAINED FROM THE SPLIT-PLATE
CALORIMETER

Water bath temperature °C	Thermistor output. $V \times 10^4$			
	1	2	3	4
17.7	2600	2665	2643	2637
18.3	2766	2826	2802	2795
19.0	2880	2941	2914	2906
20.0	3066	3127	3098	3091
20.8	3216	3274	3244	3237
22.0	3424	3483	3449	3445
22.7	3551	3610	3573	3568
24.1	3810	3867	3826	3823
25.1	3979	4034	4000	3997
26.1	4118	4194	4144	4140
27.2	4276	4350	4314	4317
28.4	4446	4514	4465	4463
29.5	4598	4650	4610	4611
30.9	4749	4805	4755	4764
32.0	4882	4946	4888	4893
33.8	5087	5151	5092	5093
34.3	5176	5224	5167	5167
36.0	5338	5397	5342	5342
38.1	5550	5606	5547	5552

TABLE B 1 - THERMISTOR CALIBRATION.

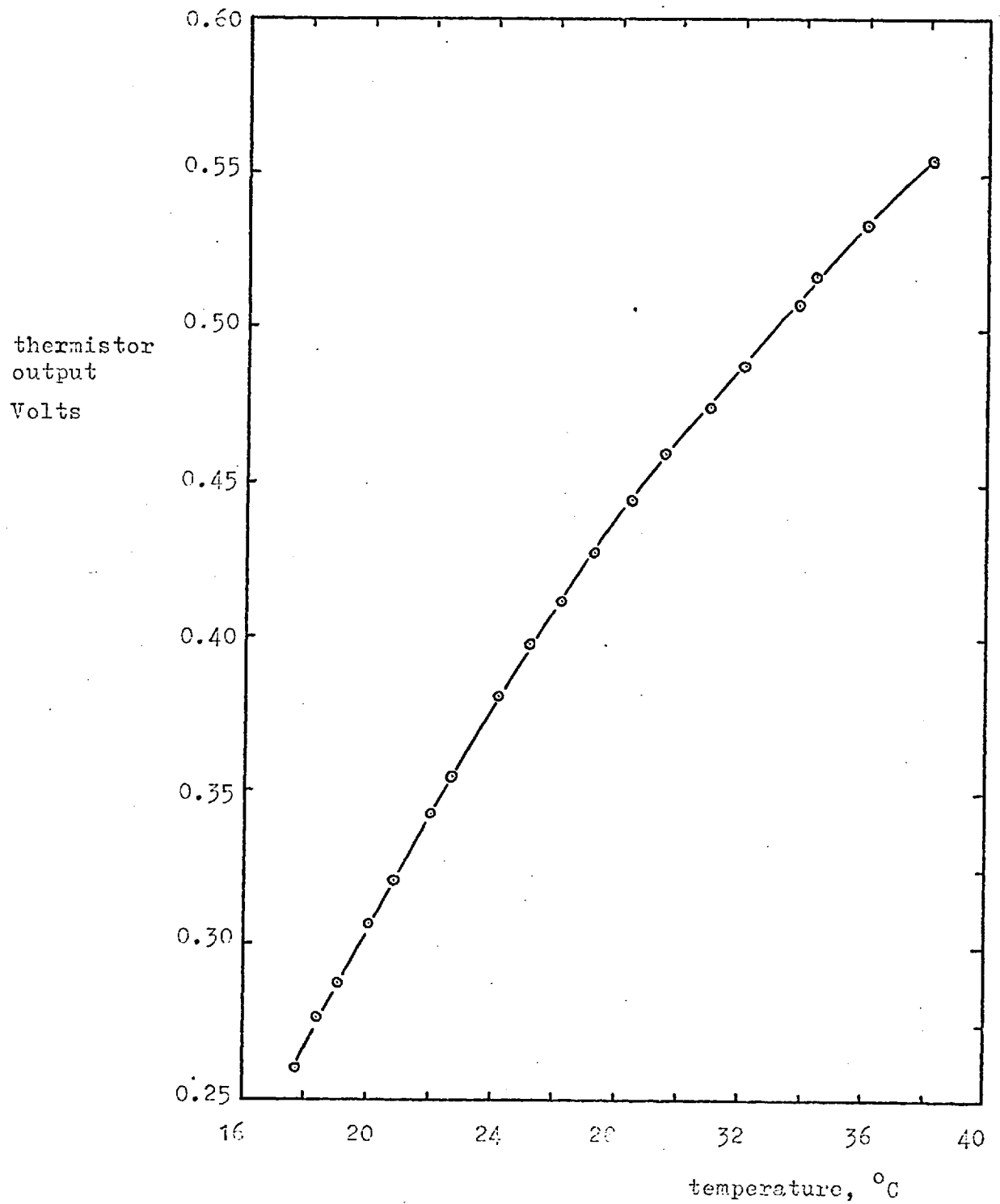


FIGURE B1 — NO.1 THERMISTOR CALIBRATION

Step no.	Thermistor output, $V \times 10^4$			
	1	2	3	4
1	4565	3886	3584	3754
2	4612	3954	3612	3838
3	4656	4027	3672	3914
4	4683	4070	3699	3937
5	4695	4098	3708	3976
6	4724	4150	3744	4016
7	4733	4167	3759	4045
8	4742	4184	3770	4081
9	4746	4210	3794	4106
10	4763	4251	3817	4154
11	4753	4275	3814	4203
12	4747	4303	3813	4202
13	4754	4301	-	-
14	4754	4341	3831	4269
15	4758	4398	3847	4298
16	4757	4416	3857	4341
17	4737	4418	3880	4380
18	4720	4459	3876	4421
19	4732	4498	3878	4441
20	4747	4515	3872	4476
21	4767	4548	3871	4510
22	4756	4536	3869	4517
23	4764	4552	3908	4548
24	4774	4587	3896	4564
25	4763	4607	3899	4570
26	4768	4590	3879	4574
27	4768	4629	3886	4579
28	3748	4618	3878	4585
29	4749	4645	3889	4608
30	4754	4642	3874	4618

Distance = 3.0 cm; unaugmented; length of step = 2.6 mm

TABLE B 2 - THERMISTOR READINGS.

Step no.	Thermistor output, $V \times 10^4$			
	1	2	3	4
1	5007	4007	3734	4004
2	4991	4034	3746	4020
3	5008	4047	3750	4043
4	5019	4070	3775	4063
5	5017	4082	3781	4075
6	5017	4098	3772	4096
7	5020	4111	3771	4112
8	5000	4133	3778	4121
9	5003	4146	3791	4161
10	5000	5151	3797	4179
11	5006	4194	3789	4213
12	4995	4221	3794	4252
13	5010	4252	3804	4284
14	5007	4284	3811	4340
15	5006	4324	3813	4382
16	5001	4354	3808	4416
17	5021	4391	3822	4475
18	5037	4459	3840	4528
19	5020	4517	3834	4579
20	4995	4530	3827	4616
21	5006	4570	3801	4655
22	5016	4612	3828	4688
23	5010	4608	3840	4717
24	5098	4647	3822	4752
25	5017	4671	3832	4785
26	5020	4714	3840	4822
27	5023	4732	3840	4850
28	5031	4760	3820	4864
29	5022	4757	3820	4875
30	5027	4779	3841	4899

Distance = 3.0 cm; augmentation = 1.2 A at 1kV,
length of step = 2.6 mm.

TABLE B 3 - THERMISTOR READINGS.

Step no.	Thermistor output, $V \times 10^4$			
	1	2	3	4
1	5290	4193	3899	4076
2	5290	4231	3926	4117
3	5299	4266	3936	4142
4	5267	4255	3941	4144
5	5281	4251	3933	4160
6	5296	4298	3940	4166
7	5281	4311	3972	4227
8	5292	4329	3985	4244
9	5268	4325	3985	4266
10	5298	4371	3972	4281
11	5289	4420	4004	4333
12	5296	4457	4014	4381
13	5300	4485	4002	4439
14	5255	4511	4032	4490
15	5273	4566	4031	4538
16	5279	4610	4033	4568
17	5261	4636	4041	4623
18	5268	4707	4046	4676
19	5293	4777	4043	4737
20	5265	4795	4042	4792
21	5258	4863	4017	4849
22	5230	4893	4040	4902
23	5244	4931	4053	4952
24	5263	4979	4050	4981
25	5265	5017	4051	5015
26	5271	5052	4044	5036
27	5287	5075	4034	5064
28	5276	5076	4027	5087
29	5272	5093	4032	5083
31	5244	5109	4056	5114

Distance = 3.0 cm; augmentation = 3A at 1 kV;
length of step = 2.6 mm

TABLE B 4 - THERMISTOR READINGS

Step no.	Thermistor output, $V \times 10^4$			
	1	2	3	4
1	4784	3961	3709	3860
3	4824	4060	3775	3929
5	4782	4022	3767	3943
7	4793	4064	3764	3974
9	4795	4090	3784	3999
11	4799	4142	3797	4062
12	4801	4151	3802	4081
13	4793	4178	3808	4105
14	4783	4182	3799	4134
15	4796	4268	3820	4198
16	4787	4280	3810	4258
17	4790	4319	3814	4295
18	4792	4371	3825	4338
19	4769	4396	3809	4372
20	4774	4452	3824	4412
21	4772	4474	3829	4455
22	4779	4498	3819	4490
23	4789	4507	3812	4487
24	4760	4518	3806	-
25	4782	4536	3809	4542
26	4786	4567	3806	4552
27	4808	4578	3815	4576
28	4812	4609	3819	4583
29	4788	4614	3818	4600
30	4794	4616	3818	4608
31	4803	4627	3808	4616
32	4796	4619	3804	4614
34	4808	4648	3799	4622
36	4804	4660	3814	4637
38	4796	4672	3806	4649

Distance = 2.0 cm; unaugmented; length of step = 2.6 mm

TABLE B 5 - THERMISTOR READINGS

Step no.	Thermistor output, $V \times 10^4$			
	1	2	3	4
1	5326	4584	4248	4276
2	5323	4574	4261	4280
3	5318	4607	4267	4277
4	5320	4628	4295	4311
5	5356	4650	4285	4342
6	5336	4689	4309	4397
7	5365	4706	4300	4417
8	5349	4732	4297	4441
9	5349	4736	4307	4500
10	5370	4805	4320	4548
11	5344	4808	4339	4582
12	5375	4862	4326	4641
13	5359	4917	4365	4704
14	5383	4983	4354	4728
15	5403	5065	4375	4814
16	5382	5072	4367	4831
17	5390	5019	4366	4782
18	5388	5017	4379	4802
19	5322	5039	4382	4815
20	5289	5074	4378	4862
21	5299	5081	4359	4889
22	5279	5088	4343	4913
23	5256	5122	4365	4991
24	5262	5132	4357	4974
25	5255	5188	4360	4998
26	5274	5209	4340	5033

Distance = 2.0 cm; augmentation = 1.2A at 1 kV;
length of step = 2.6 mm

TABLE B 6 - THERMISTOR READINGS

Step no.	Thermistor output, $V \times 10^4$			
	1	2	3	4
1	5508	4401	4168	4212
2	5540	4460	4184	4244
3	5535	4451	4198	4290
4	5588	4540	4255	4347
5	5609	4603	4270	4392
6	5630	4661	4318	4441
7	5635	4672	4356	4471
8	5643	4677	4344	4494
9	5638	4708	4386	4525
10	5652	4745	4406	4561
11	5654	4782	4441	4620
12	5656	4773	4449	4647
13	5679	4843	4462	4678
14	5657	4851	4462	4721
15	5665	4887	4458	4762
16	5668	4934	4476	4814
17	5677	4950	4484	4863
18	5654	4972	4495	4910
19	5681	5036	4518	4979
20	5672	5080	4511	5029
21	5684	5133	4519	5103
22	5667	5137	4507	5157
23	5663	5209	4514	5231
24	5670	5260	4527	5280
25	5637	5260	4496	5321
26	5654	5299	4506	5360
27	5649	5327	4515	5374
28	5667	5344	4475	5407
29	5666	5372	4494	5436
30	5680	5392	4518	5464

Distance = 2.0 cm; augmentation = 5 A at 1kV;
length of step = 2.6 mm

TABLE B 7 - THERMISTOR READINGS

Step no	Thermistor output, V x 10 ⁴			
	1	2	3	4
1	4670	3728	3620	3664
2	4715	3842	3661	3720
3	4744	3877	3764	3794
4	4791	3941	3789	3740
5	4831	4023	3838	3916
6	4850	4061	3891	3927
7	4888	4097	3907	4018
8	4896	4125	3928	4054
9	4912	4150	3955	4098
10	4950	4191	3970	4136
11	4957	4226	4018	4174
12	4971	4257	4034	4209
13	4998	4284	4047	4225
14	5006	4318	4078	4293
15	5019	4357	4080	4330
16	5026	4393	4082	4366
17	5019	4436	4102	4412
18	5057	4484	4153	4481
19	5045	4525	4138	4517
20	5060	4562	4190	4567
21	5059	4609	4217	4623
22	5042	4631	4157	4649
23	5057	4685	4156	4704
24	5072	4742	4174	4752
25	5092	4790	4190	4810
26	5069	4797	4178	4832
27	5085	4831	4190	4879
28	5109	4875	4209	4907
29	5103	4879	4215	4949
30	5120	4915	4213	4952

Distance = 1.5cm; unaugmented; length of step = 2.6 mm

TABLE B 8 - THERMISTOR READINGS

Step no.	Thermistor output, $V \times 10^4$			
	1	2	3	4
1	5474	4680	4458	4604
5	5465	4703	4478	4637
9	5484	4736	4476	4684
11	5472	4755	4476	4701
13	5600	4771	4541	4803
15	5591	4774	4545	4834
17	5612	4916	4526	4874
18	5612	4936	4545	4917
19	5614	4977	4557	4956
20	5614	5031	4555	4962
21	5615	5043	4553	5003
22	5589	5061	4560	5028
23	5592	5071	4563	5072
24	5622	5163	4567	5108
25	5602	5153	4541	5135
26	5592	5168	4535	5168
27	5589	5198	4514	5205
28	5579	5209	4523	5235
29	5566	5265	4546	5283
30	5572	5257	4531	5302
31	5587	5272	4502	5302
32	5598	5314	4525	5333
33	5588	5346	4535	5354
34	5576	5334	4514	5359
35	5560	5335	4513	5376
37	5578	5374	4520	5398
39	5620	5435	4564	5448
41	5621	5442	4539	5440
43	5605	5429	4549	5449
46	5565	5417	4478	5432

Distance = 1.5 cm; augmentation = 1.2 A at 1 kV,
length of step = 2.0 mm

TABLE E 9 - THERMISTOR READINGS

Step no.	Thermistor output, $V \times 10^4$			
	1	2	3	4
1	5369	4038	3858	3954
5	5429	4169	3949	4051
9	5495	4270	4090	4192
13	5560	4382	4158	4277
17	5604	4492	4211	4393
21	5614	4601	4270	4479
24	5622	4631	4316	4556
27	5680	4740	4382	4656
30	5691	4832	4445	4755
32	5710	4898	4475	4844
34	5741	5000	4508	4951
36	5724	5046	4532	5030
37	5738	5114	4539	5101
38	5758	5178	4551	5164
39	5798	5254	4571	5237
40	5807	5313	4584	5293
41	5815	5355	4598	5329
42	5811	5399	4637	5414
43	5794	5464	4669	5469
44	5793	5498	4704	5506
45	5807	5567	4720	5541
47	5837	5610	4719	5588
49	5849	5652	4739	5657

Distance = 1.5 cm; augmentation = 3 A at 1 kV;
length of step = 2.0 mm.

TABLE B 10 - THERMISTOR READINGS

Step no.	Thermistor output, $v \times 10^4$			
	1	2	3	4
1	5445	4324	4113	4276
5	5458	4417	4183	4363
9	5509	4506	4239	4424
13	5527	4570	4279	4502
17	5534	4627	4330	4580
20	5560	4693	4381	4653
23	5579	4753	4405	4720
26	5616	4837	4426	4790
28	5648	4916	4468	4879
30	5625	4961	4477	4935
31	5659	5026	4492	5010
32	5681	5108	4530	5069
33	5685	5115	4537	5094
34	5702	5175	4554	5144
35	5713	5164	4563	5124
36	5779	5215	4600	5134
37	5766	5203	4608	5161
38	5771	5216	4602	5187
39	5778	5286	4634	5253
40	5757	5328	4645	5279
41	5739	5366	4652	5384
42	5748	5400	4670	5412
43	5755	5461	4682	5422
44	5783	5501	4704	5473
46	5793	5534	4706	5521
48	5780	5563	4741	5540
50	5742	5566	4719	5532
52	5757	5600	4781	5620
54	5760	5608	4727	5615
57	5812	5653	4750	5650

Distance = 1.0cm; augmentation = 3 A at 1 kv;

length of step = 2.0 mm

TABLE B 11 - THERMISTOR READINGS

Step no.	Thermistor temperature, °C				Plate temp.	
	1	2	3	4	1	2
1	29.35	24.25	22.45	23.65	5.10	1.20
2	29.70	24.65	22.80	24.15	5.05	1.35
3	30.10	25.05	23.15	24.60	5.05	1.45
4	30.30	25.35	23.30	24.75	4.95	1.45
5	30.45	25.50	23.35	25.00	4.95	1.65
6	30.65	25.85	23.60	25.20	4.80	1.60
7	30.75	26.00	23.70	25.40	4.75	1.70
8	30.80	26.10	23.75	25.65	4.70	1.90
9	30.85	26.25	23.90	25.80	4.60	1.90
10	31.00	26.55	24.05	26.15	4.45	2.15
11	30.90	26.70	24.00	26.45	4.20	2.45
12	30.85	26.95	24.00	26.45	3.90	2.45
13	30.90	26.90	-	-	4.00	-
14	30.90	27.20	24.15	26.90	3.70	2.75
15	30.95	27.60	24.20	27.10	3.35	2.90
16	30.95	27.70	24.30	27.45	3.25	3.15
17	30.75	27.75	24.40	27.70	3.00	3.30
18	30.60	28.00	24.40	28.00	2.60	3.60
19	30.70	28.35	24.40	28.15	2.35	3.75
20	30.85	28.45	24.35	28.40	2.40	4.05
21	31.00	28.70	24.35	28.70	2.30	4.35
22	30.95	28.60	24.35	28.75	2.35	4.40
23	31.00	28.75	24.60	29.00	2.25	4.40
24	31.05	29.00	24.50	29.15	2.05	4.65
25	31.00	29.20	24.50	29.20	1.80	4.70
26	31.00	29.05	24.40	29.20	1.95	4.80
27	31.00	29.35	24.45	29.25	1.65	4.80
28	30.85	29.30	24.40	29.30	1.55	4.90
29	30.85	29.50	24.45	29.50	1.35	5.05
30	30.90	29.50	24.40	29.60	1.40	5.20

TABLE B 12 - TEMPERATURES FOR RESULTS
OF TABLE B 2

Radius step no. j	Heat fluxes, $W\text{ cm}^{-2}$, for data given in tables:-									
	B2	B3	B4	B5	B6	B7	B8	B9	B10	B11
0	31	79	83	48	124	172	52	117	1135	-
1	33	56	75	46	78	160	47	95	135	-
2	25	49	67	46	59	97	33	88	90	-
3	27	48	45	41	42	60	34	59	82	-
4	24	31	35	31	39	43	26	46	98	-
5	15	29	31	27	35	42	24	34	85	-
6	15	29	28	27	26	36	24	32	51	-
7	11	29	25	21	20	24	23	30	41	-
8	6	25	25	14	14	24	22	31	34	75
9	5	21	23	12	10	20	20	32	23	125
10	4	18	21	9	8	12	16	22	16	32
11	4	9	18	5	5	12	16	18	17	26
12	5	7	15	5	4	13	17	14	12	24
13	4	8	9	4	5	12	14	12	10	18
14	4	8	7	4	4	10	9	12	9	18
15	-	-	-	-	-	-	-	11	9	12
16	-	-	-	-	-	-	-	8	9	8
17	-	-	-	-	-	-	-	6	6	10
Step length mm	2.6	2.6	2.6	2.6	2.6	2.6	2.6	2.0	2.0	2.0

TABLE B 13 - HEAT FLUXES OBTAINED FROM THE
DATA OF TABLES B 2 to B 11

APPENDIX C

TEMPERATURES MEASURED IN SLABS,
PROPERTIES OF EN 8,
AND DERIVED RESULTS

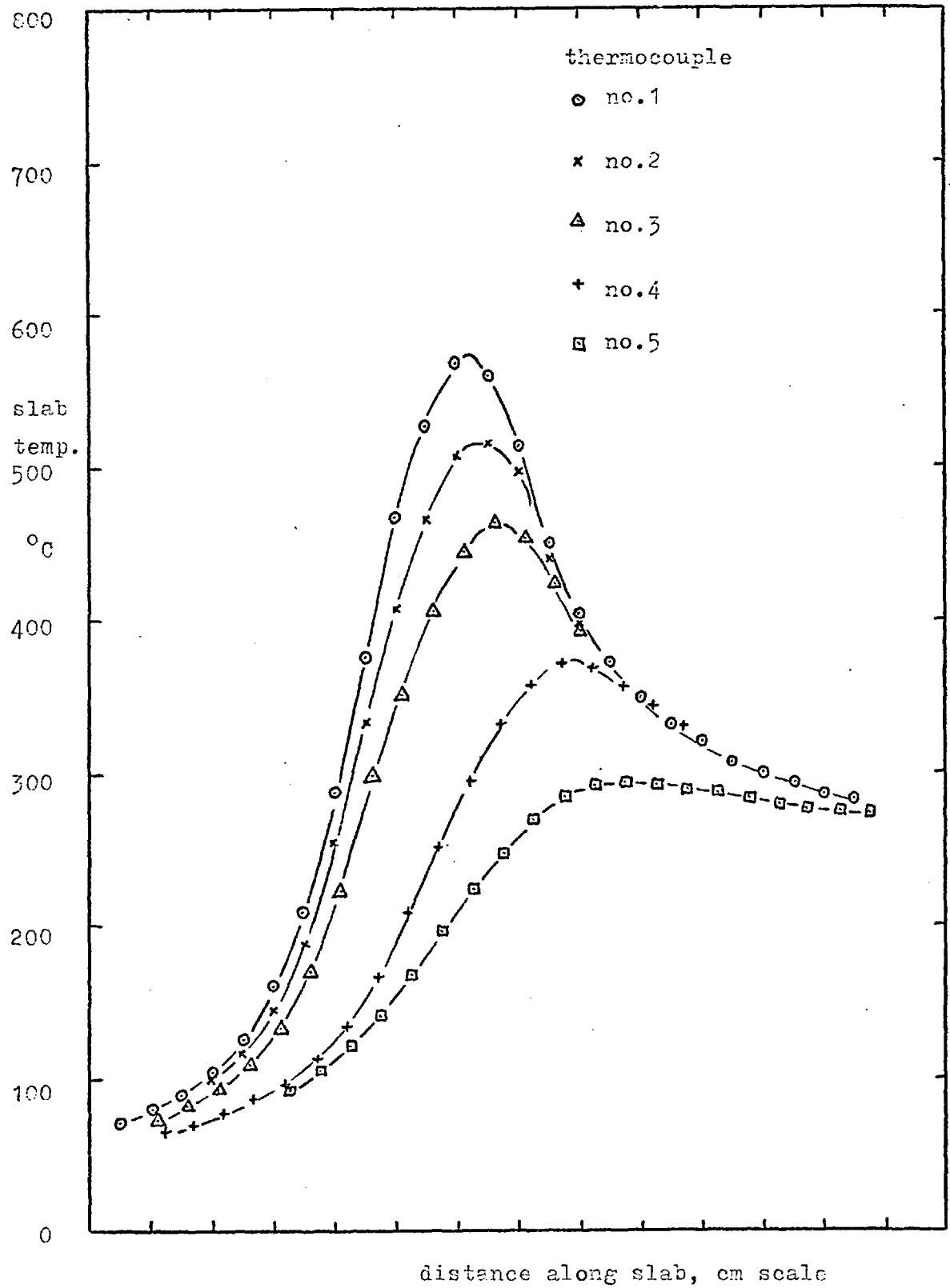


FIGURE C1 - TEMPERATURES MEASURED IN THE SLAB

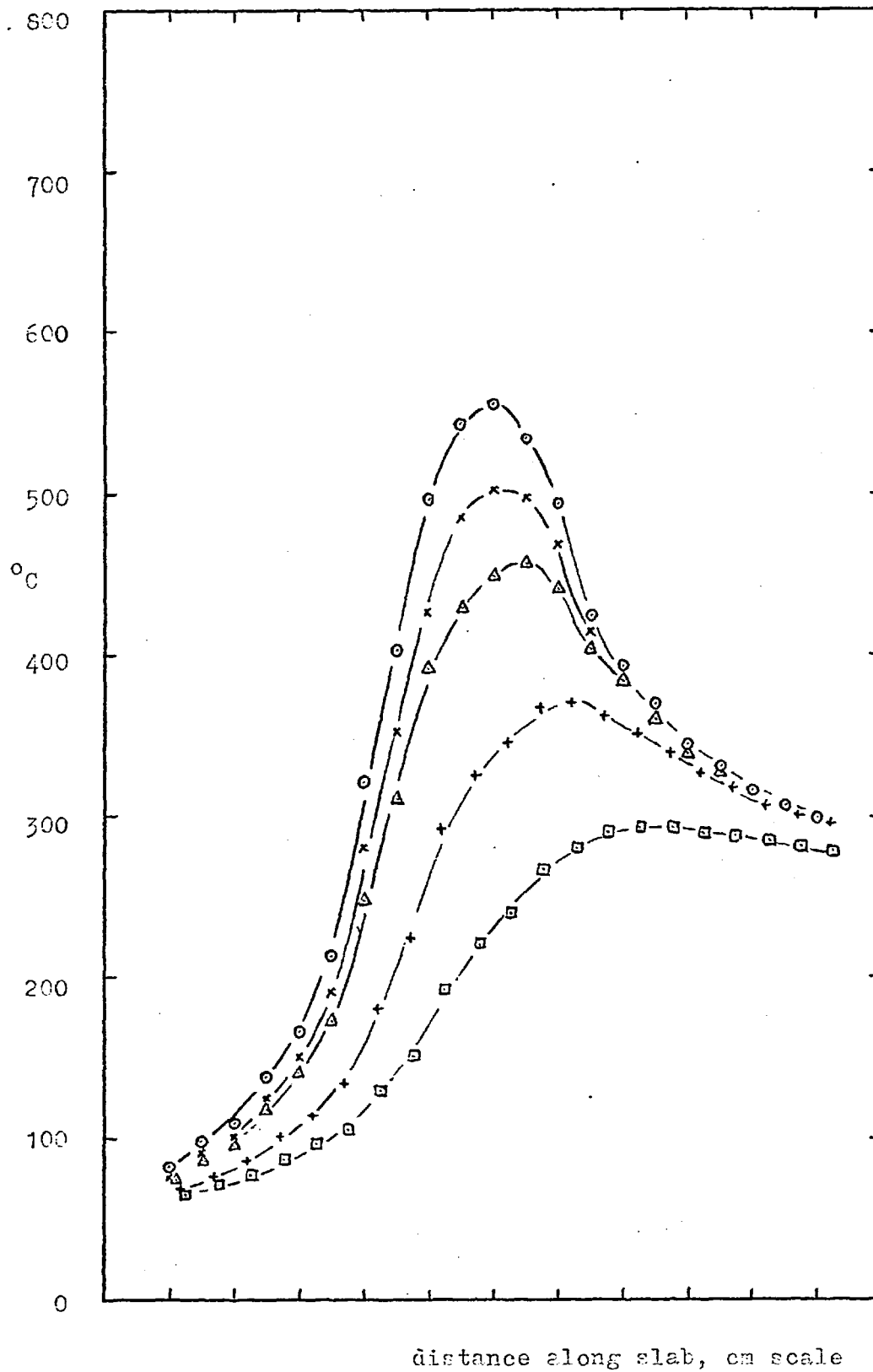


FIGURE 02 - TEMPERATURES MEASURED IN THE SLAB

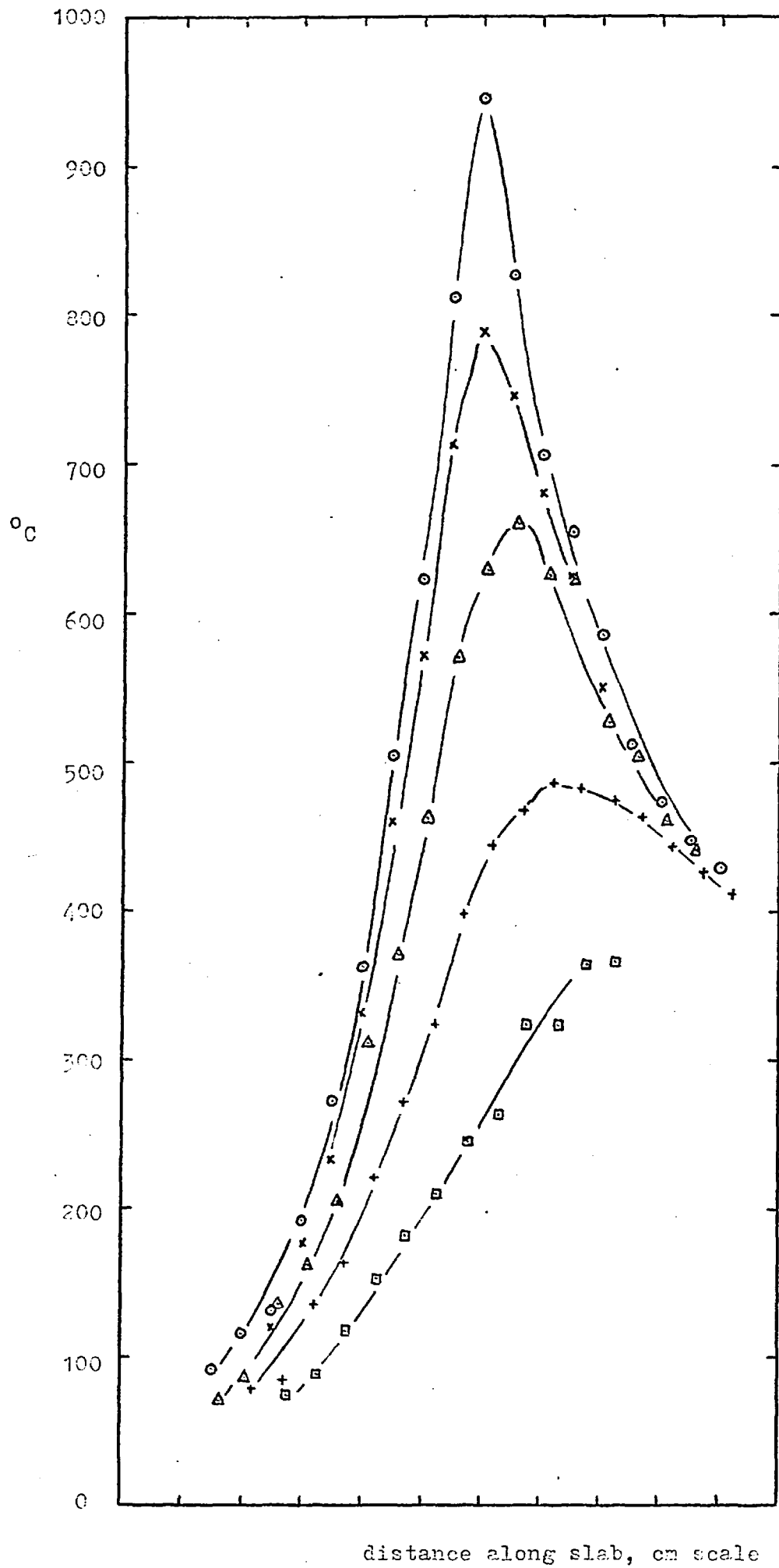


FIGURE C3 - TEMPERATURES MEASURED IN THE SLAB

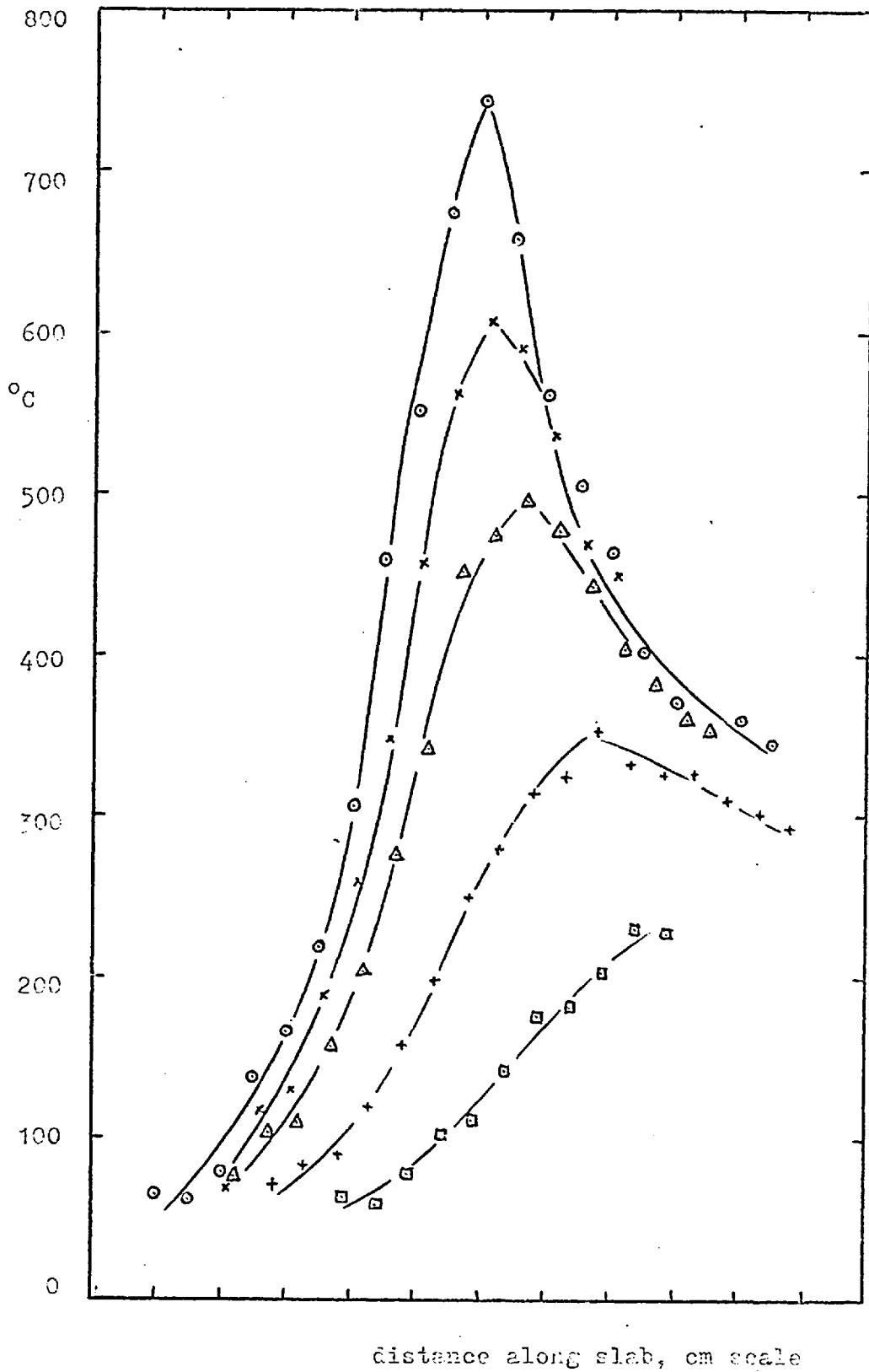


FIGURE C4 - TEMPERATURES MEASURED IN THE SLAB

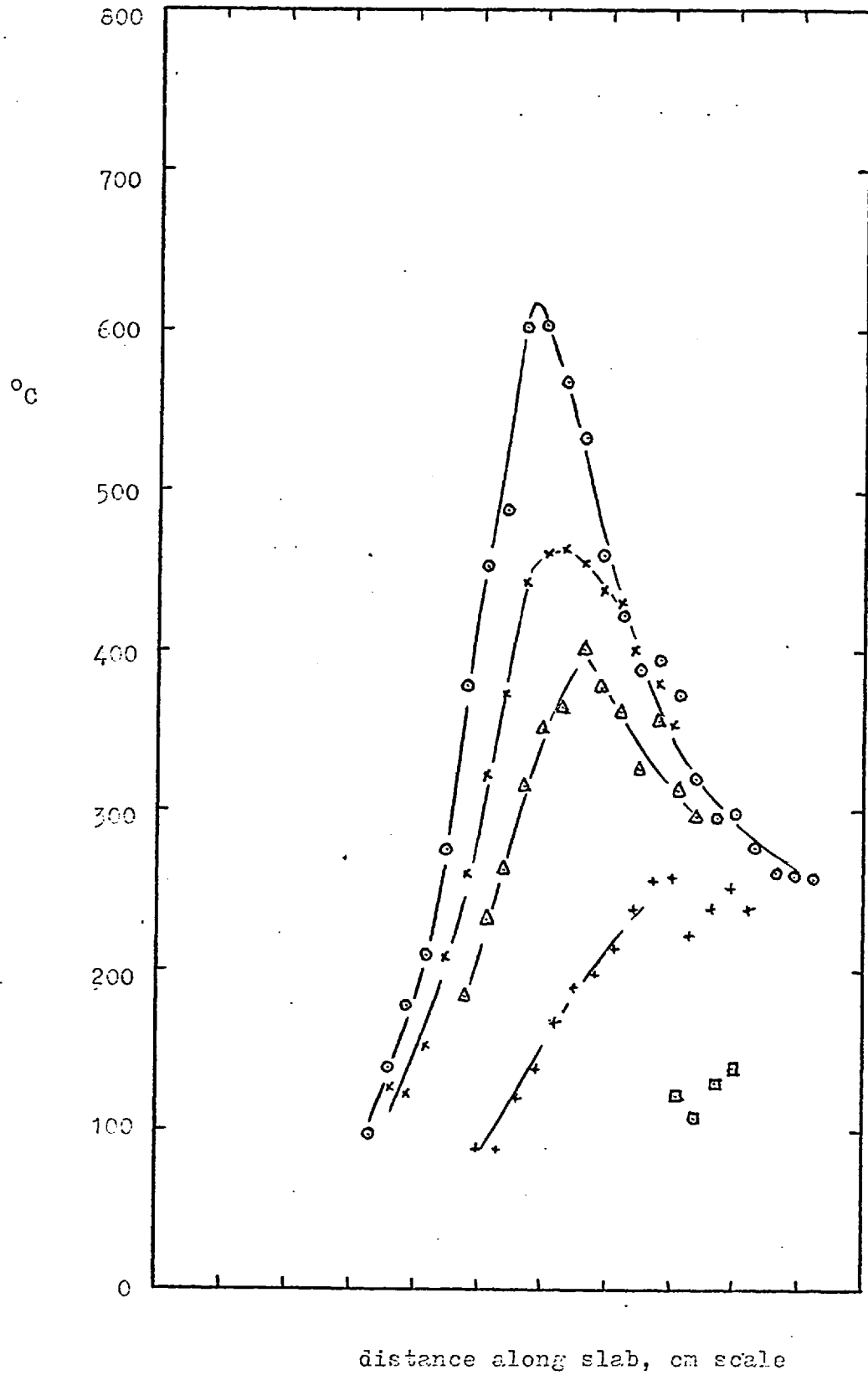


FIGURE C5 - TEMPERATURES MEASURED IN THE SLAB

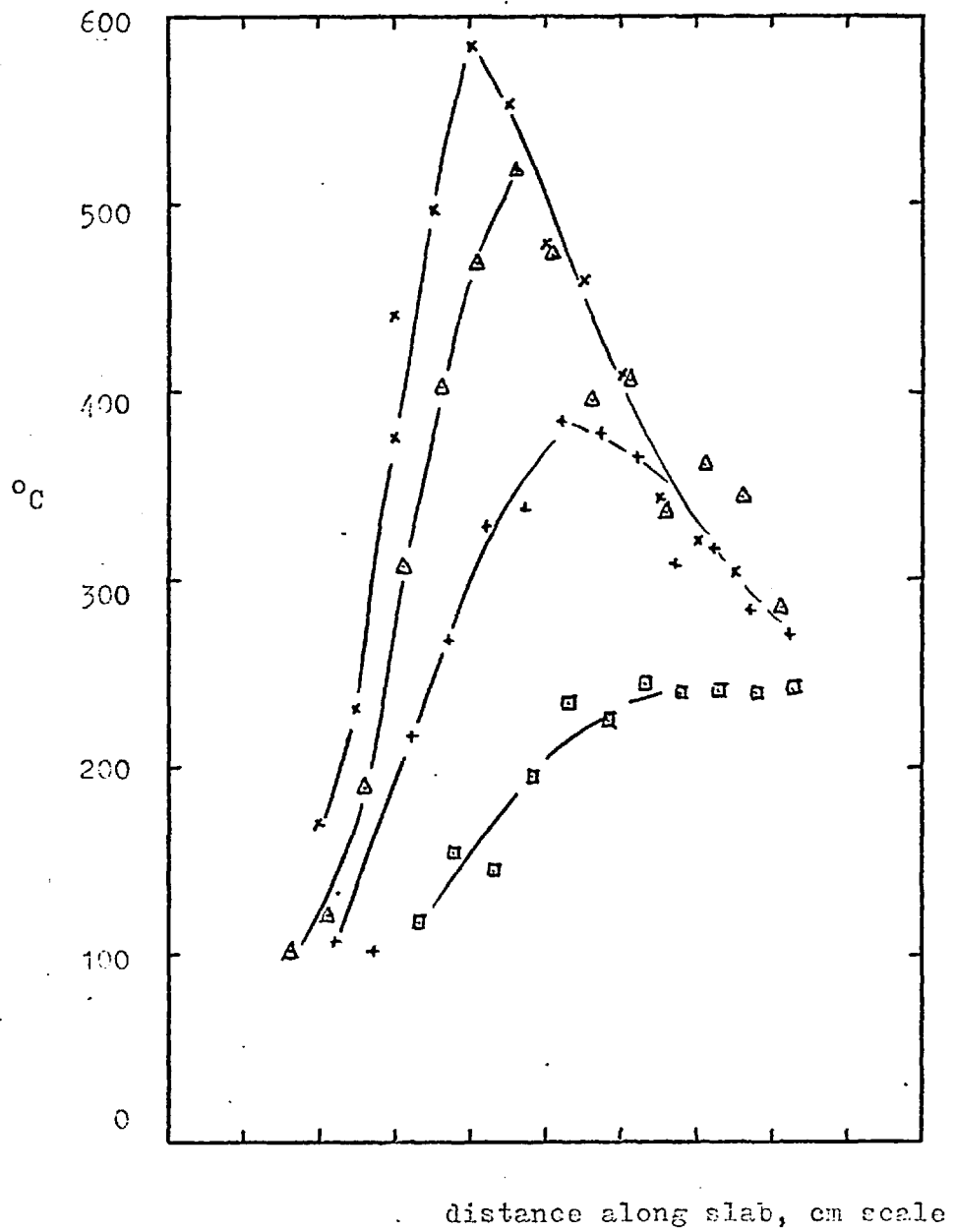


FIGURE C6 - TEMPERATURES MEASURED IN THE SLAB

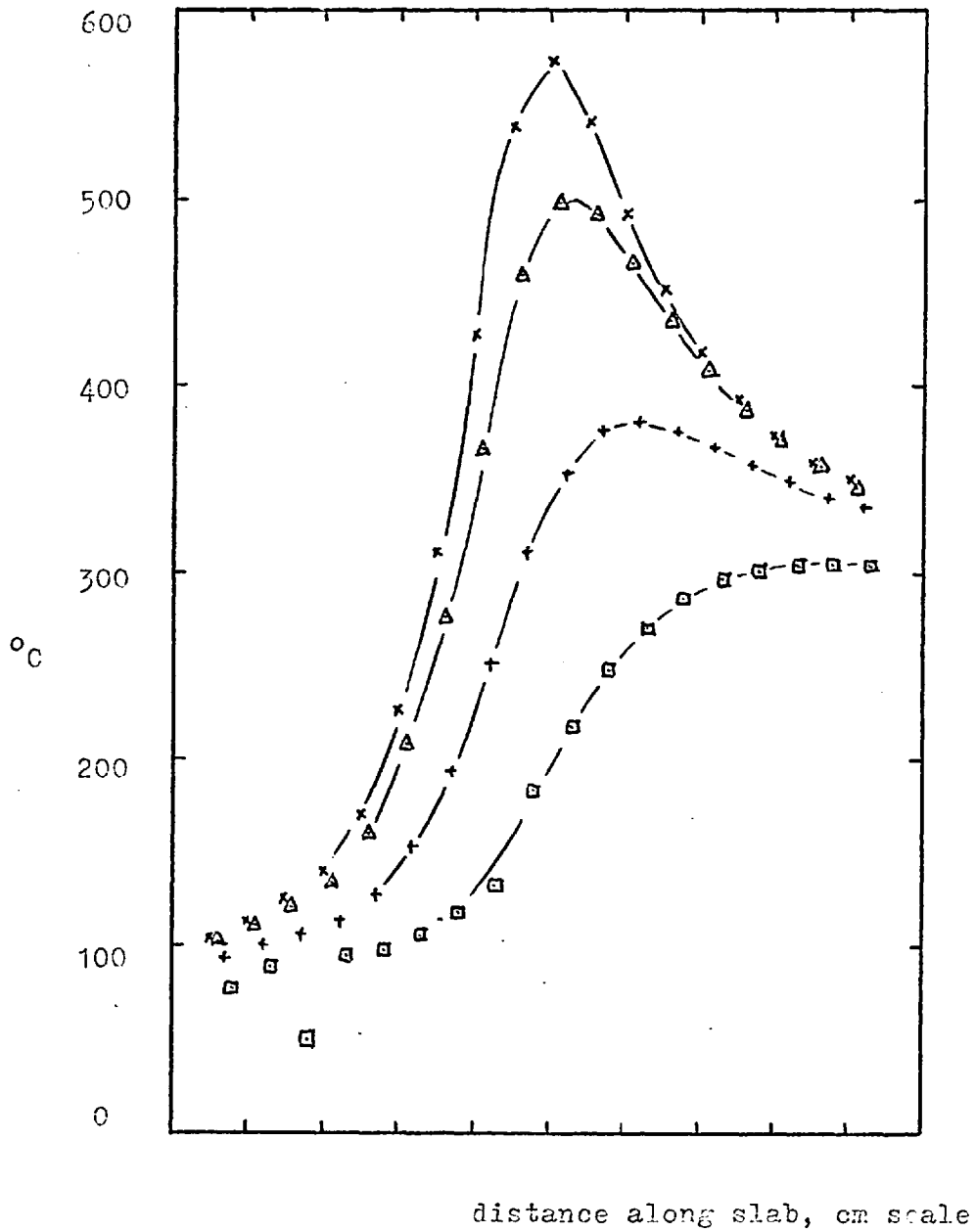


FIGURE C7- TEMPERATURES MEASURED IN THE SLAB

Mean specific heat, $\bar{c}$, from 20°C to °C in cal °C ⁻¹ g ⁻¹		Thermal conductivity, k, (normalized En 8) °C cal cm ⁻¹ sec ⁻¹ °C ⁻¹	
100	0.114	20	0.107
200	0.119	50	0.107
300	0.122	100	0.109
400	0.127	200	0.105
500	0.132	300	0.102
600	0.133	400	0.100
700	0.144	500	0.092
800	0.159	600	0.084
900	0.155	700	0.075
1000	0.155	800	0.068

Specific gravity is 7.82 to 7.85 at 20°C

Table C1 - Physical properties of En 8 obtained from

Woolman and Mottram,

Properties of the En steels, Vol. I (49)

y-C.4a cm	Values of a, cm						
	C1	C2	C3	C4	C5	C6	C7
-4.0	1.46	1.46	-	-	-	-	-
-3.0	1.38	1.34	1.15	1.20	-	-	-
-2.0	1.54	1.60	1.11	1.17	0.87	0.93	1.14
-1.0	1.67	1.67	1.00	0.95	0.91	1.00	1.18
1.0	0.93	0.97	0.50	0.51	0.57	0.61	0.57
2.0	0.76	0.71	0.61	0.53	0.62	0.57	0.60
3.0	0.76	0.79	0.56	0.61	-	0.71	0.74
4.0	0.87	0.87	-	-	-	-	-
$(\theta_s^*)_M$	0.696	0.671	1.16	0.908	0.745	0.807	0.807
$\sqrt{u^*}$	2.36	2.46	1.43	1.82	2.21	2.04	2.04
$\dot{q}_M'' \sqrt{a}$	105	100	173	184	193	163	163

Table C2 - Values of a, $(\theta_s^*)_M$,

$\sqrt{u^*}$ and $\dot{q}_M'' \sqrt{a}$ for data given

in figure C1 to C7.

Distance from slab surface x cm	Calculated temperatures inside slab $(\theta_{y=0} + 20)^\circ\text{C}$						
	C1	C2	C3	C4	C5	C6	C7
0	448	425	843	665	494	627	577
0.5	352	334	658	448	320	422	390
1.0	266	253	493	315	186	297	274
2.0	176	130	244	89	55	93	88

Table C3 - Slab temperatures calculated from
equation VI 19

SYMBOLS

Lower case

- a - spread of heat flux distribution
- b - distance between quench and burner axis
- c - specific heat
- d - diameter or arc length
- e - electronic charge
- h - heat transfer coefficient or distance between calorimeter and burner
- i - integer
- j - integer or current density
- k - thermal conductivity or Boltzmann constant
- l - scale of turbulence
- m - burner dimensions
- n - no. of layers
- p - burner dimensions or pressure
- $\dot{q}$ - rate of heat transfer
- $\dot{q}''$ - heat flux
- r - radius
- r' - radius
- s - step length
- t - time
- t' - time
- u - velocity
- u' - intensity of turbulence
- v - velocity
- x - co-ordinate
- y - "
- z - "

Upper case

- | | |
|----------------------------------|--------------------------|
| A - coefficient | I - current |
| A' - coefficient | K - mobility |
| B - thermionic emission constant | N - concentration |
| D - diffusivity | Q - volumetric flow rate |
| E - field strength | S - velocity |
| G - mass velocity | T - absolute temperature |
| H - enthalpy | V - voltage |
| I - current | |

Dimensionless groups

B*	- velocity	R	- ratio of rate of heat transfer to rate of energy input to burner
C*	- mole fraction	Re	- Reynolds no.
Le	- Lewis no.	u*	- velocity
Nu	- Nusselt no.	y*	- distance
$\dot{m}^*$	- mass fraction	*	- temperature
$\dot{q}^*$	- severity of quench		

Greek

α	- thermal diffusivity		
β	- angle of flame front to direction of flow of unburnt gas		
δ	- penetration distance		
Δ	- difference		
ϵ	- eddy kinematic viscosity		
ϕ	- work function		
ψ	- heat flux potential		
ρ	- density		
θ	- temperature		
μ	- dynamic viscosity	ν	- kinematic viscosity

Subscripts

a	- augmented	A	- arc or austenitising
b	- bulk	AN	- anode
e	- electron or electrical	B	- breakdown
i	- integer	CA	- cathode
j	- integer	E	- enhancement
o	- uniform	F	- final
p	- constant pressure	I	- initial
s	- surface	M	- maximum
u	- unburnt or unaugmented	T	- terminals or total
w	- water		

Superscripts

*	- dimensionless
.	- per unit time
"	- per unit area
-	- mean

REFERENCES

1. KARLOVITZ, B. Pure appl. Chem. 5, 557 (1962).
2. KARLOVITZ, B. and RICHARDSON, D.I. A.S.M.E. reprint 61-WA-251 (1962).
3. MARYNOWSKI, C., KARLOVITZ, B. and HIRT, T.J. Ind. Engng. Chem. (Proc. Res. Dev.) 6, 375 (1967).
4. DAVIES, R.M. 10th Int. Symp. Comb., p.755. Combustion Institute (1965).
5. LAWTON, J., PAYNE, V.G. and WEINBERG, F.J. Nature, 193, 736 (1962).
6. CHEN, D.C.C., LAWTON, J. and WEINBERG, F.J. 10th Int. Symp. Comb., p.743. Combustion Institute (1965).
7. FELLIS, I., GAWEN, J.C. and HARKER, J.H. Combust. Flame II, 309 (1967).
8. GAWEN, J.C., HARKER, J.H. and FELLIS, I. Nature 210, 1149 (1966).
9. FELLIS, I. and HARKER, J.H. J. Inst. Fuel 40 477 (1967).
10. KARLOVITZ, B. Brit. Patent 936,105.
11. " " " 1,112,612.
12. " " " 1,141,741.
14. ZAVOD, P.K. French Patent 1,518,819.
15. LEWIS, B. and von ELBE, G. J. Chem. Phys. 11, 75 (1943).
16. EGERTON, A.C. and THABET, S.W. Proc. R. Soc. A. 211, 45 (1952).
17. LEWIS, B. and von ELBE, G. Combustion, flames and explosions of gases. Academic Press, New York (1961).
18. LAWTON, J. and WEINBERG, F.J. Electrical aspects of combustion. Clarendon Press, Oxford (1969).
19. LOEB, L.P. Fundamental processes of electrical discharge in gases, John Wiley, New York (1939).
20. von ENGEL, A. Ionised Gases. Clarendon Press, Oxford (1965).
21. EDELS, H. I.F.E. Proc. 108, 55, (1961).
22. PAN, F.Y. Phys. Rev. 55, 769 (1939).
23. BROWNE, T.E. Jr. Electrochem. Soc. J1. 102, 27 (1955).
24. HERRING, C. and NICHOLS, H.V. Rev. mod. Phys. 21, 185 (1949).
25. WRIGHT, D.A. I.E.E. Proc. 100, 125 (1953).
26. RYSHEWITCH, E. Oxide ceramics. Academic Press, New York (1960).
27. WILLIAM, J.V. and COGINSO, R.A. 9th Int. Symp. Comb., p.257. Combustion Institute (1963).
28. BUTLER, J.N. and BROOKAW, P.S. J. chem. Phys. 26, 1636 (1957).

29. ROSNER, D.E. A.R.S. J1.31, 816 (1961).
30. ALTMANN, D. and WISE, E. Jet Prop.26, 256 (1956).
31. ROSNER, D.E. A.R.S. J1.29, 215 (1959).
32. CHEN, D.C.C. and McGRATH, I.A. J.Inst.Fuel,42, 12 (1969).
33. NESTOR, O.F. and OLSEN, H.N. S.I.A.M. Rev. 2, 200 (1960).
34. ANDERSON, J.E. and STRESINO, E.F. J. heat Transfer 85, 49 (1963).
35. NESTOR, O.F. J. app.Phys. 33, 1636 (1962).
36. REED, J.E. J. app. Phys. 34, 2266 (1963).
37. PAY, P.F. Welding J1. 46, 380 (1967).
38. FIELDS, I. and HARVER, J.H. Combust. Flame 12,587 (1968).
39. CARSLAW, H.S. and JAEGER, J.C. Conduction of heat in solids. 2nd. Ed. Clarendon Press, Oxford (1959).
40. GOODMAN, T.R. Advances in heat transfer, vol.1, p.52. Academic Press, New York (1964).
41. HILLS, A.W.D. Trans. T.M.S. - A.I.M.E. 245, 1471 (1969).
42. ARPACI, V.S. Conduction heat transfer, p.94. Addison-Wesley, New York (1966).
43. FYRES, H.F. et al. Phil.Trans.Roy.Soc. A.240, 1 (1946).
44. SHERIDAN, A.T. and FITZGERALD, F. Conference on mathematical models in metallurgical process development, p.18 Iron & Steel Institute, London(1970.)
45. SOUTHGATE, G.T. Chem. metall. Engrg.31, 16 (1924).
46. GROSSMAN, M.A. and BAIN, E.C. Principles of heat treatment. A.S.M.E., Ohio (1964).
47. BURNS, J.L., MOORE T.L. and ARCHER R.S., Trans. A.S.M.26, 1 (1938).
48. ROBERTS, G.A. and MEHL, R.F., Trans. A.S.M. 31, 613 (1943).
49. WOOLMAN, J.A. and MOTTMAN, B.A., Properties of the En Steels, Vol.1, Pergamon Press, Oxford (1964).
50. JOYNY, W.E., Trans. A.S.M.27, 1072 (1939).
51. SCOTT, M.R., Metal prog. 77, 4, 69 (1960).
52. A.S.M. Handbook Committee, Metals Handbook, 8th Ed., Vol.2, A.S.M. Ohio (1964).
53. HOWATT, J.T., Metal prog. 77, 4, 76 (1960).
54. SPIERS, P.M. (Ed.), Technical data on fuel, 6th Ed., The British National Committee, World Power Conference, London (1962).
55. NUKIYAMA, S.J. Soc.Mech.Engrs.(Japan) 37 367 (1934)
56. SMITHELLS. C.J. Metals Reference Book, 4th Ed. p.224 Butterworths, London (1967).

ACKNOWLEDGEMENTS

I am very grateful to

Dr. A.W.D.Hills for his enthusiastic help and guidance during this work.

Members of the John Percy Research Group and members of the Department of Metallurgy whose expertise and interest have helped many aspects of this research.

The Electricity Council for financing the development of the large burner for flame hardening, and the British Steel Corporation for supporting some of the heat transfer work.

Shell Research Ltd. for the gift of a 66 kVA transducer, and Gresham Transformers Ltd. for providing two transformers to supply power to the small burners.

Mrs. J. Tait, my mother, for typing this thesis.