Investigations on Die Materials

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DOCTOR OF PHILOSOPHY

by

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INVESTIGATIONS ON DIE MATERIALS

1. INTRODUCTION

1.1 The Need for Improved Die Materials

Drop forging is a metal forming process in which hot metal, in the form of cut pieces or bar stock, is shaped by forging between dies which contain an impression of the shape to be formed. It is ideally suited to the production of large numbers of identical components, such as those required by the motor vehicle, aircraft, and general engineering industries.

The most important market for drop forgings is the motor vehicle industry which absorbs about 70% of all the drop forgings produced in Great Britain. Over the last twenty years, there has been a gradual, but significant, decline in the number of forgings used in motor vehicles[1]. This erosion of the principal market for drop forgings has been caused by continual improvements in products made by competing processes such as casting, powder metallurgy, welding, extrusion and cold forging.

Where replacement of forgings has occurred, it has usually been for one of two reasons. Either the first cost of the part made by the alternative method has been lower than that of a forging, or the dimensional accuracy has been greater, thus reducing subsequent machining costs.

Fortunately for the drop forger, the reduced usage of his products has, until now, been more than offset by the large increase in the number of vehicles produced. There are, however, indications that this state of affairs will come to an end so that there could be a reduction in the tonnage of forgings required.

Thus, the drop forging industry is under increasing pressure to produce more accurate parts at reduced cost to maintain its present markets. The accuracy of drop forgings can, of course, only be ensured so long as the shape of the die cavity is maintained. The need for more accurate forgings is synonymous, therefore, with the need for improved die materials.

An approximate breakdown of the costs involved in producing drop forgings is given in Table 1.

Table 1

Approximate Cost Breakdown of Drop Forgings

	% of Total Cost
1. Plant costs	10
2. Labour costs	20
3. Forging material costs	50
4. Heating costs	4
5. Heat treatment and inspection costs	6
6. Die costs	10

Table 1 shows that die costs account for a significant part of the total production costs.

The annual expenditure of the drop forging industry on die materials has been estimated[2] at about £1½m. In addition to this, a further £6m. is spent on machining dies, so that the total die costs amount to £7½m. per annum. These figures show that comparatively small reductions in die costs could save the drop forging industry large sums of money each year. It was with the object of realising such large potential savings that the work to be described was undertaken.

1.2 Die Steels used in the Drop Forging Industry at the Start of the Investigation

Steels for drop forging dies are covered by B.S.S. 224, 1938, which lists only four materials, whose compositions are shown in Table 2.

Table 2

Composition of Die Steels Listed in B.S.S. 224, 1938

Die Steel	Composition							
	C	Si	Mn	S	P	Ni	Cr	Mo
No. 1	.6	.3	.7	.05m	.05m	--	--	--
No. 2	.6	.3	.7	.05m	.05m	1.25	--	--
No. 3	.55	.3	.7	.05m	.05m	1.5	.6	--
No. 5	.55	.3	.7	.05m	.05m	1.5	.6	.3

Of these four steels only the last one, No. 5 Die Steel, is used extensively. This steel, however, is used almost exclusively for hammer dies

/and large

and large press dies. It is invariably supplied to the drop forger in the hardened and tempered condition ready for sinking. The hardness to which it is heat treated depends on the size and complexity of the die cavity. Table 3 shows the hardness levels recommended by a leading die block supplier[3].

Table 3

Recommended Hardness Levels for No. 5 Die Steel for Hammer Dies

Hardness Range	Hardness BHN	Hardness Hv30	Equivalent UTS t/in^2	Application
A	401 / 429	425 / 455	88 / 95	Small, shallow impressions up to ¾ in. deep
B	363 / 388	385 / 401	80 / 85	General forging dies with impressions up to 2 in. deep
C	331 / 352	350 / 370	73 / 77	Larger forgings up to 5 in. deep
D	293 / 321	298 / 335	64 / 71	Very large forgings only

Long experience in the use of No. 5 Die Steel has confirmed the general suitability of these hardness levels for the applications indicated.

A knowledge of the mechanical properties of this steel at these hardness levels, therefore, forms a useful basis for the likely property requirements of alternative materials for hammer dies.

The only other die steel in common use is a 5% chromium steel of the American H.12 type, with the following nominal composition - 0.3 C, 1.0 Si, 5.0 Cr, 2.0 Mo, 1.0 W, and 0.25 V.

This steel is used for press dies and inserts of small and medium size. Dies are normally sunk in the annealed condition and are subsequently hardened and tempered to somewhat higher levels than are used for hammer dies. The dies are also sometimes nitrided after heat treatment.

The material has never gained popularity for hammer dies due to the brittleness compared with No. 5 Die Steel. The original H.12 steel was developed for the die-casting of aluminium alloys[4] and appears to have been introduced into the forging industry in about 1945.

A very small quantity of dies is still made from plain carbon steel, similar to No. 1 Die Steel. The use of this material is confined to dies with small, shallow impressions such as those used in the production of small spanners, pliers and cutlery. The dies are hardened by water quenching of the impression face after machining.

Cutlery dies may be made from a high carbon, high chromium steel (0.6 - 2.0 C, 12 Cr) which is air or oil hardened after sinking. The use of such a brittle material is permissible due to the small size of the forging equipment used and the very shallow nature of the forgings.

1.3 Performance of Forging Dies

It may seem surprising that only two die steels have gained wide acceptance over the last thirty years, during which time vast strides have been made in the development of alloy steels.

The reasons for this are the difficulties encountered in assessing the performance of new die materials in the forge, and an almost complete lack of research into die materials specifically intended for drop forging.

Under production conditions, there is invariably a wide variation in the life of allegedly identical dies used to produce a given forging. Generally, the life is normally distributed about a mean value as illustrated in Figure 1 (p. 7).

In addition to this variation of die life for a given forging, the <u>mean</u> die life varies widely from forging to forging, and as the average life increases, so does the spread about the mean value as indicated in Figure 2 (p. 7). This figure shows the standard deviation of die life plotted as a function of mean die life, and is based on data collected by Jackson et al[5], Littler[6], and data collected during works die wear trials made in connection with the present investigations.

The difficulty involved in establishing an accurate value of mean die life in a forge is well illustrated in Figure 3 (p. 9), which shows the cumulative mean die life for a forging, plotted against the number of impressions used. The data on which Figure 3 is based were taken from die life records maintained by a drop forger. Clearly, from this figure, a reliable indication of mean die life is not obtained until quite a large number of impressions has been used. In practice, the accumulation of such a large amount of data can take several years, especially in jobbing forges where repeat orders of a given pattern are infrequent.

Another complication is the fact that dies can fail by a number of different mechanisms, as discussed in section 1.4. Thus, a material which performs well in one application may not do so in another.

These difficulties have, over the years, been responsible for the failure of drop forgers to introduce improved die materials.

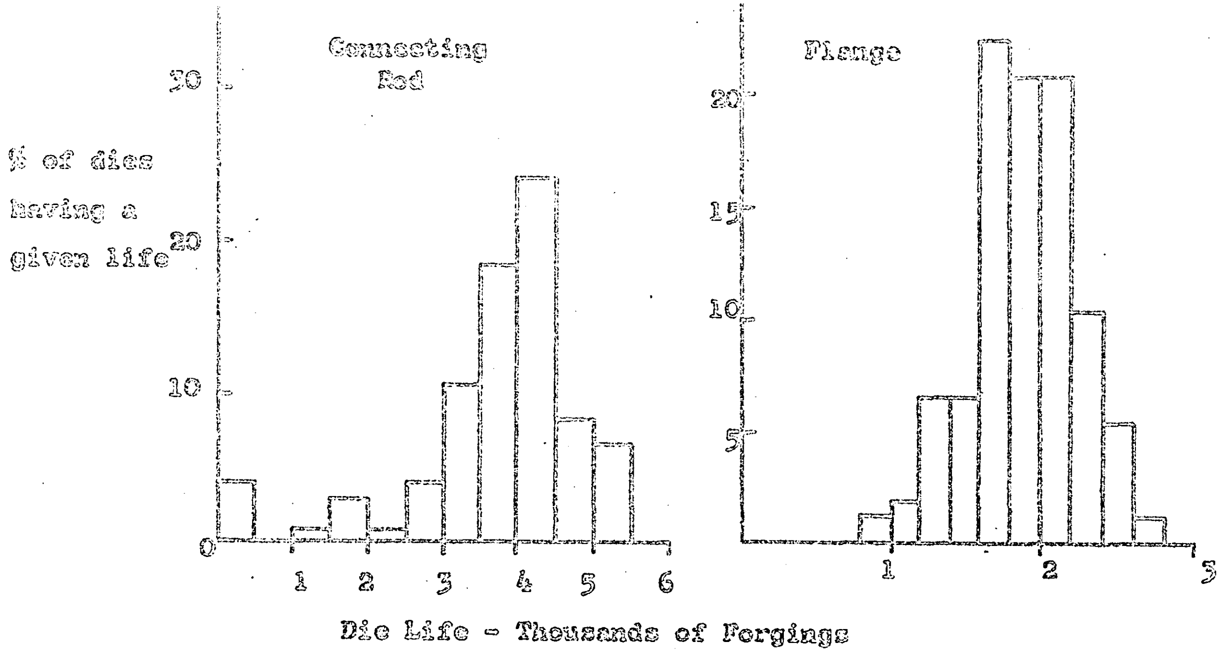


Figure 1

Variation of Forging Die Life

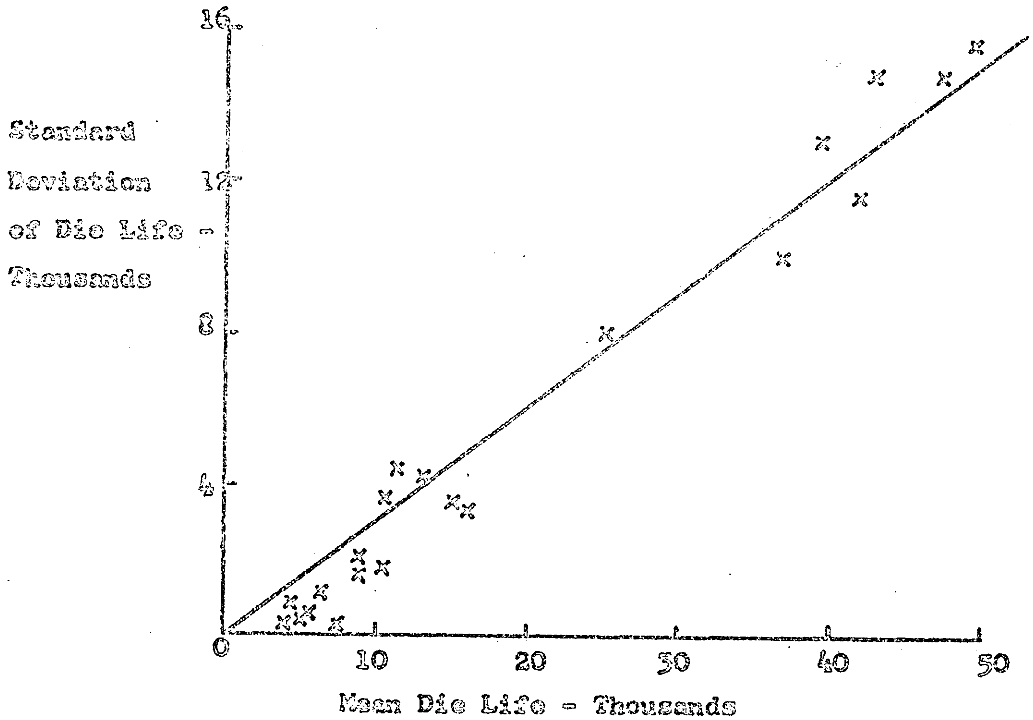


Figure 2.

Relationship Between Standard Deviation of Die Life and Mean Die Life

1.4 Modes of Die Failure

Figure 4 (p. 9) illustrates the various modes of die failure and also indicates the positions in a die cavity where each type of failure is most likely to occur. Typical examples of dies which have suffered erosive wear, heat checking and mechanical fatigue cracking are shown in Figure 5 (p. 10).

1.5 Scope and Objectives of the Present Investigations

The final objective of the investigations was to reduce the contribution which die costs make to the overall cost of producing forgings.

To achieve this objective, it was necessary to establish what properties were required by die materials and then to select or develop materials which satisfied these requirements at a minimum cost.

It was clear from the outset that certain requirements of die materials were related to conventional mechanical properties which are readily determined. It was equally clear that the least understood mechanism of failure was wear. Discussions held with drop forgers showed that wear was also the major cause of die failure in small and medium sized dies which constitute the greatest proportion of dies used in the industry.

Reliable data on the wear resistance of different die materials could not be obtained from forges because of the widespread lack of performance records, the variability in die performance already mentioned, and the slowness with which data could be collected. It was necessary, therefore, to develop a method of studying wear by means of laboratory tests.

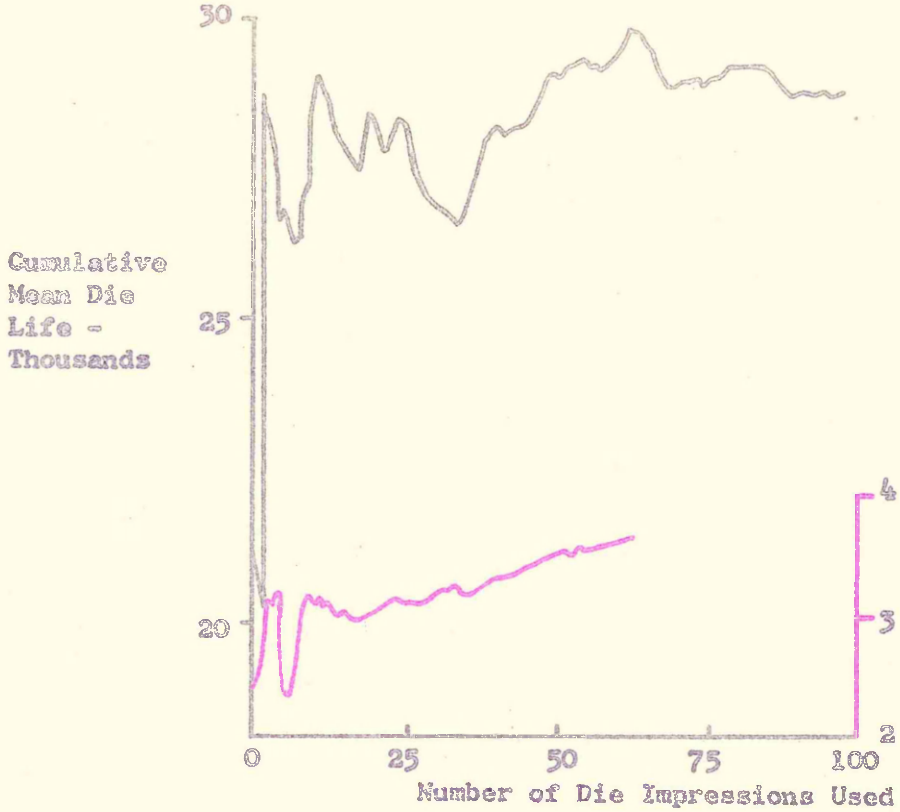


Figure 3

Cumulative Mean Die Life and Number of Impressions used

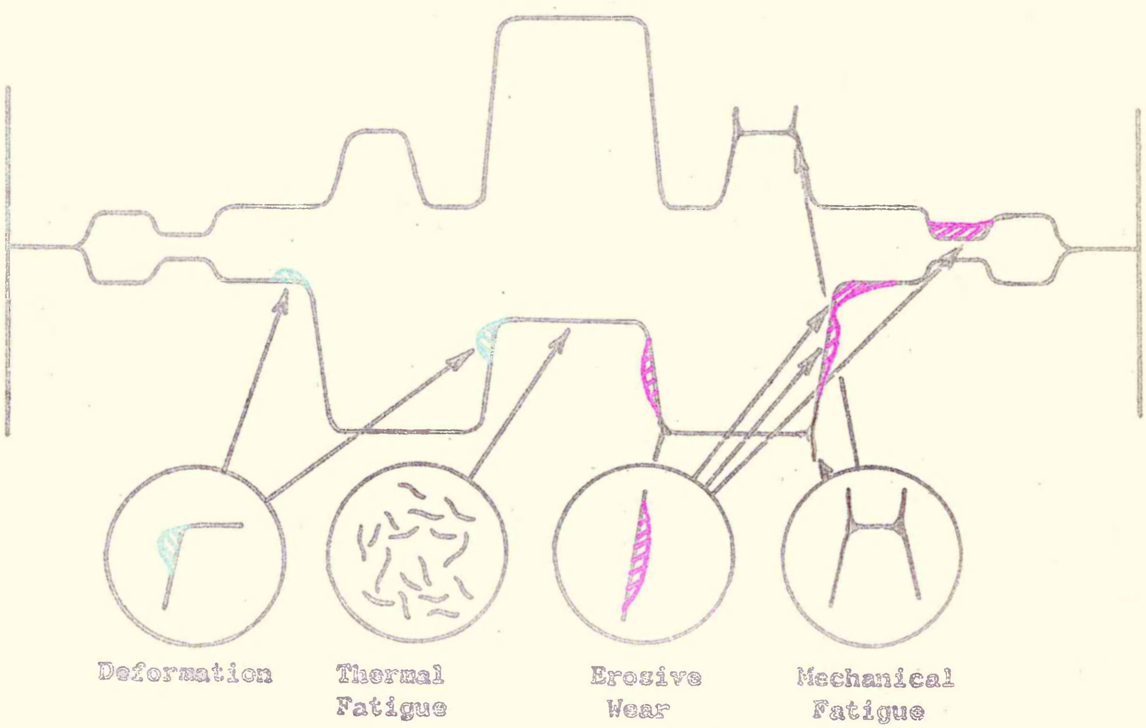
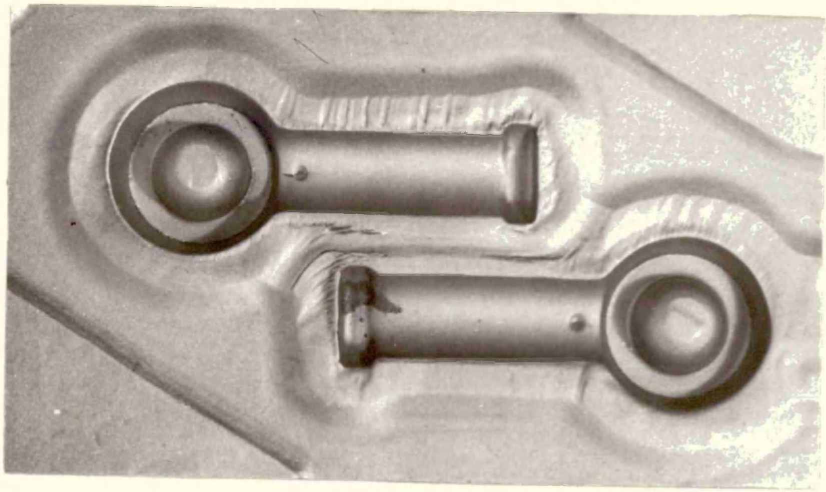


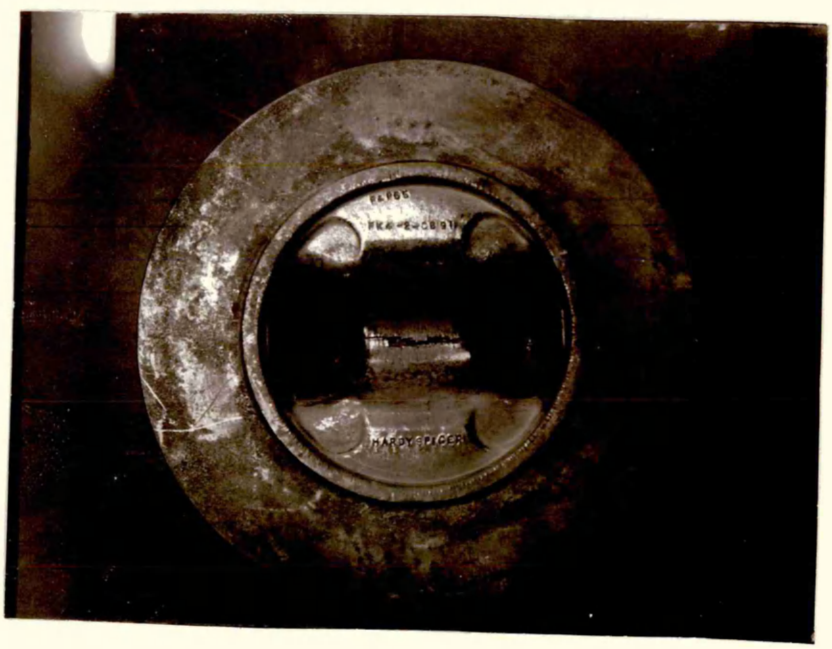
Figure 4

Modes of Die Failure



Erosion of
Flash Lands

Thermal Fatigue
Cracking



Mechanical Fatigue
Cracking

Figure 5

Examples of Three Modes of Die Failure

2. LITERATURE REVIEW

2.1 Service Conditions of Forging Dies

The service conditions of forging dies may be defined in terms of the thermal and stress cycles to which dies are subjected during use.

Knowledge of surface temperatures is important for two reasons. Firstly it will indicate the temperatures up to which die steels must retain adequate strength. Secondly, surface temperature will probably influence the wear of a die material by its effect on the structural stability of the die surface.

Equally important in deciding the property requirements of die materials will be the loads to which a die is subjected. Knowledge of such loads will determine the hardness level to which dies must be heat treated to avoid deformation. The load will also have a marked influence on the likelihood of mechanical fatigue cracking, gross fracture, and the extent of die wear.

2.1.1 Die surface temperatures

The most comprehensive investigation of die temperatures during forging is that of Beck[7], who calculated the maximum theoretical die temperature at the surface as a function of bulk die temperature and stock temperature, as shown in Figure 6 (p.13).

Similar calculations by Kindbom[8] are in close agreement with these of Beck.

Invariably, attempts to measure surface temperatures by means of thermocouples have yielded values below those indicated by Beck's and Kindbom's calculations. Thus, Beck measured maximum temperatures of only 650°C whilst Vigor and Hornaday[9], using the sophisticated thermocouple arrangement shown in Figure 7 (p. 13), obtained a maximum reading of 695°C. Details of stock and die temperature were not recorded in the above investigations.

Metallographic examinations and hardness measurements on used dies have given valuable indications of the temperatures reached during forging. Thelander[10] has made such investigations, and his results are summarised in Table 4.

Table 4

Surface Structure and Microhardness of Worn Forging Dies

Die Material	Original Hardness Hv30*	Surface Microhardness After Use		Forgings Produced	Hammer or Press	Surface Structure
		Min.	Max.			
1. SIS 2550	435	370	800	15 000	H	Regions of untempered martensite.
2. SIS 2550	440	350	850	13 000	H	As above.
3. SIS 2550	595	400	700	5 000	H	Tempered martensite and bainite.
4. Cr-Mo Steel	445	315	900	12 000	H	Tempered and untempered martensite
5. SIS 2242	550	322	470	36 500	SP	Acicular tempered martensite.

* Converted from Rockwell values

H = Hammer SP = Friction Screw Press

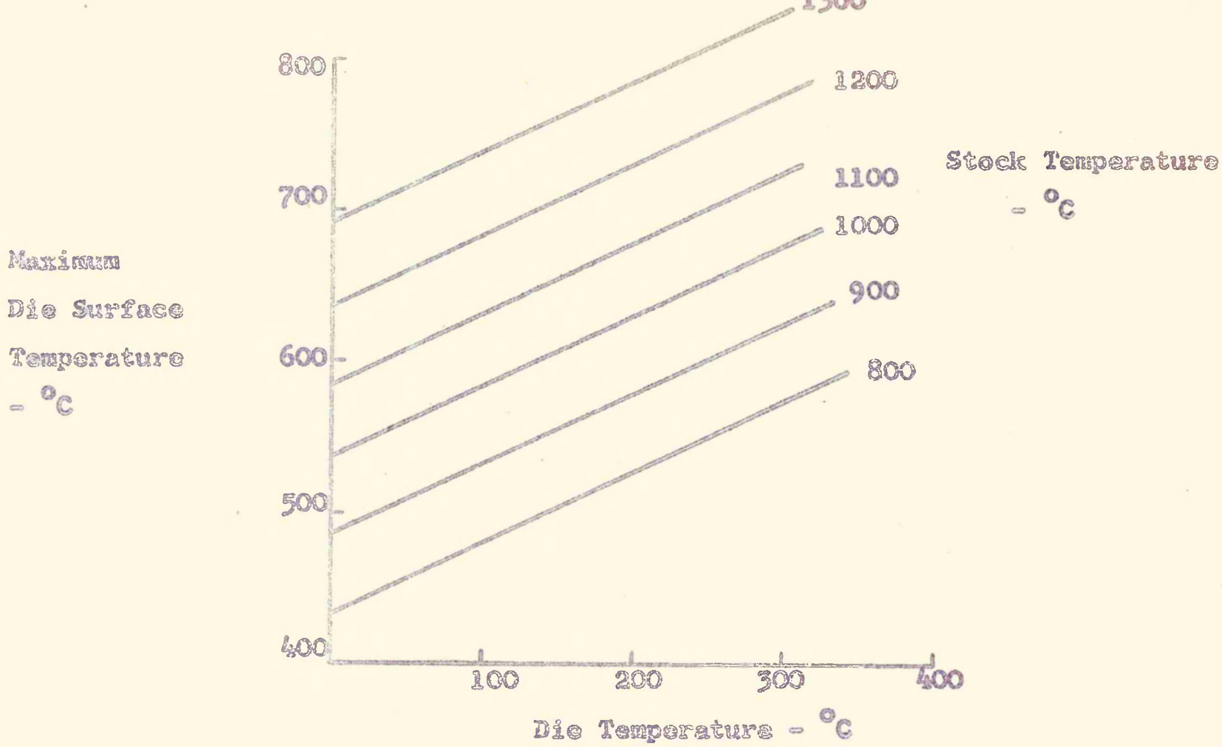


Figure 6

Theoretical Die Surface Temperature as a Function of Stock Temperature and Bulk Die Temperature (After Beck[7])

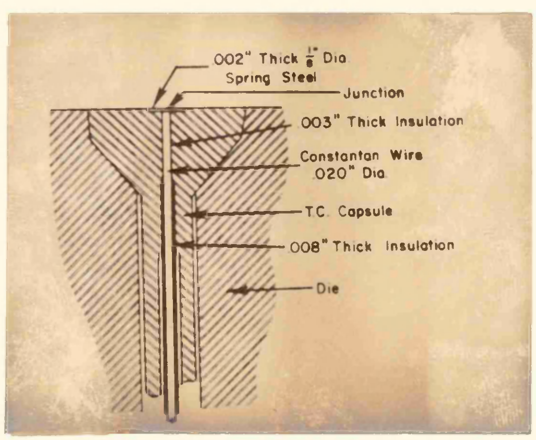


Figure 7

Thermocouple Arrangement Used by Vigor & Hornaday to Measure Die Surface Temperatures.

Table 4 continued

Material	Composition							Ac_1 - °C
	C	Si	Mn	Ni	Cr	Mo	V	
SIS 2550	$\frac{.5}{.6}$	$\frac{.2}{.4}$	$\frac{.3}{.5}$	$\frac{2.8}{3.2}$	$\frac{.9}{1.1}$	$\frac{.25}{.35}$	-	710
Cr-Mo Steel	.45	.25	.6	-	3.0	.5	-	750
SIS 2242	$\frac{.35}{.42}$	$\frac{.8}{1.2}$	$\frac{.3}{.6}$	-	$\frac{5.0}{5.5}$	$\frac{1.2}{1.6}$	$\frac{.85}{1.15}$	840

Tholander concluded that the high surface hardness of the lower alloy steels after use proved that re-austenitisation of the surface occurred during forging, with subsequent transformation after forging finished. He further suggested that, during use, the die surface remained austenitic until the end of forging, as shown in Figure 8 (p. 17).

Since the high alloy steel (SIS 2242) did not transform, Tholander fixed the maximum die surface temperature between 750 and 890°C.

Heller and Truskov[11] deduced, from hardness measurements on worn upsetting machine punches, that surface temperatures of 800 - 850°C were reached. They investigated the service life of four punch materials, their results being summarised in Table 5.

Table 5

Hardness Change and Service Life of Punch Materials in a Horizontal Forging Machine

Material	Composition				
	C	Si	Mn	Cr	Mo
1	.46	3.1	.45	8.18	--
2	.54	.38	1.39	.72	.28
3	.35	1.4	1.05	1.3	--
4	.64	.3	.7	--	--

Material	Original Hardness BHN	Hardness After Use BHN	Ac_3 -°C	Relative Life %
1	364/387	196/241	975	100/120
2	364/418	444/550	790	100
3	286/302	196/241	860	200/220
3	364/444	196/241	860	---
4	340/380	196/241	760	30/40

Tomilin and Belskij[12] also estimated surface temperatures by relating the micro-hardness of used die sections to tempering curves. Extrapolating their results to the die surface, they found temperatures of 700 - 800°C indicated for hammer dies, but only 500 - 650°C for press dies.

In none of the above investigations were bulk die temperatures or stock temperatures reported.

Beck concluded from his investigations that heat transfer from the stock to the die was effective only so long as the forging load was maintained. Because of this load duration and forging contact times for different forging machines may be expected to influence die surface temperatures. Tholander[13] has published such data for a drop hammer, a friction screw press, and a crank press as shown in Figure 9 (p. 17).

2.1.2 Die stresses

The problem of estimating stresses in dies is even more difficult than that of estimating temperatures, because of the wide range of shapes and sizes to be considered.

Several authors[14, 15, 16] have treated the problem analytically using plasticity theory to derive the stress distribution in a forging die. Experimental verification of the predictions has been confined to the simplest cases of deformation, such as the upsetting of cylinders and measurements of the total load on a die during forging.

The complex formulae developed from plasticity theory to predict die stresses depend, for their accuracy, on a knowledge of the coefficient of friction between the stock and the die, and also a knowledge of the yield stress of the stock material at the appropriate temperature and strain rate.

Since these values are not accurately known, an estimate of stresses in dies is more easily made by the use of a simpler formula such as that proposed by Siebel[17].

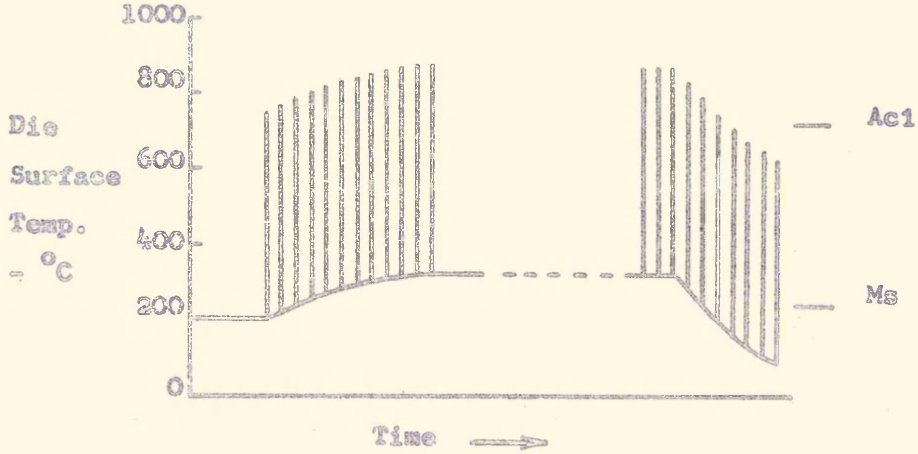


Figure 8

Temperature Fluctuations at the Surface of a Drop - Forging Die (After Tholander[10])

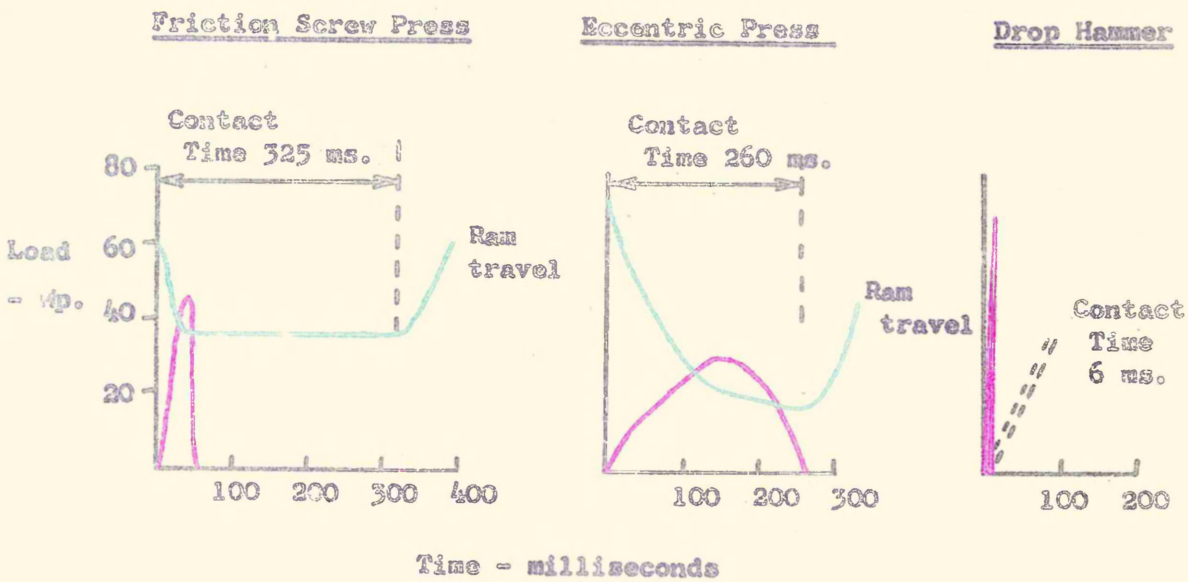


Figure 9.

Die Contact and Load Duration Times in Forging Machines (After Tholander[12])

Siebel suggested that the normal stress "q" at a distance "x" from the free edge of a forging is given by the expression,

$$q = Kf + p \quad \ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots(1)$$

where Kf is the yield stress of the material being forged and "p" is given by the expression,

$$p = \int_0^x \frac{2\mu\, Kf}{h} \, d \quad \ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots(2)$$

in which μ is the coefficient of friction between stock and die and "x" and "h" are the quantities indicated in Figure 10 (p. 19). Whilst Siebel's formula still demands a knowledge of μ and the yield stress of the metal, it is much simpler to apply than most others.

Foster[18] has assumed that a forging may be split into "sections" to each of which he applies Siebel's formula to estimate the die stress distribution as shown in Figure 11 (p. 19).

In discussing stresses in relation to forging dies, it is important to realise that the values estimated from the various formulae proposed are those reached when the forging process is just completed. They take no account of the effect of residual energy in the forging machine after die closure, which has been shown[19] to have a profound effect in determining the maximum stress to which a die is subjected.

More specific information is also needed on the stress levels which exist at any point of a die during the period when metal sliding past that point is occuring, since, as will be shown later, wear occurs only when sliding of the stock takes place.

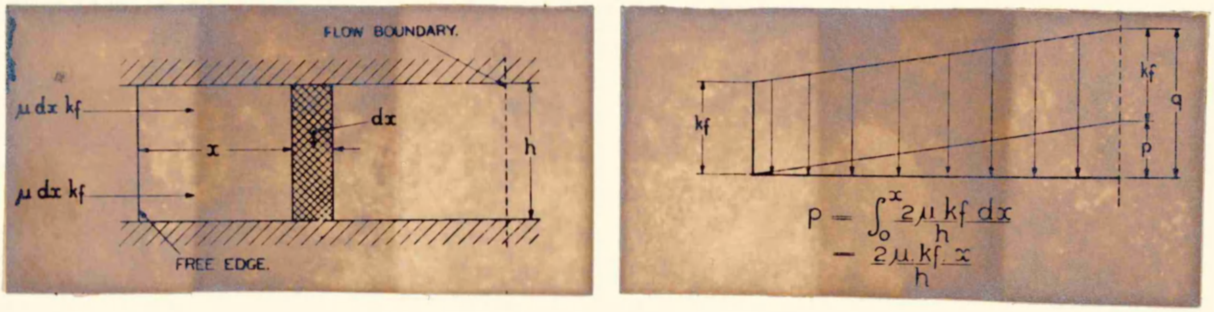


Figure 10

Development of Flow Resistance According to Siebel[17]

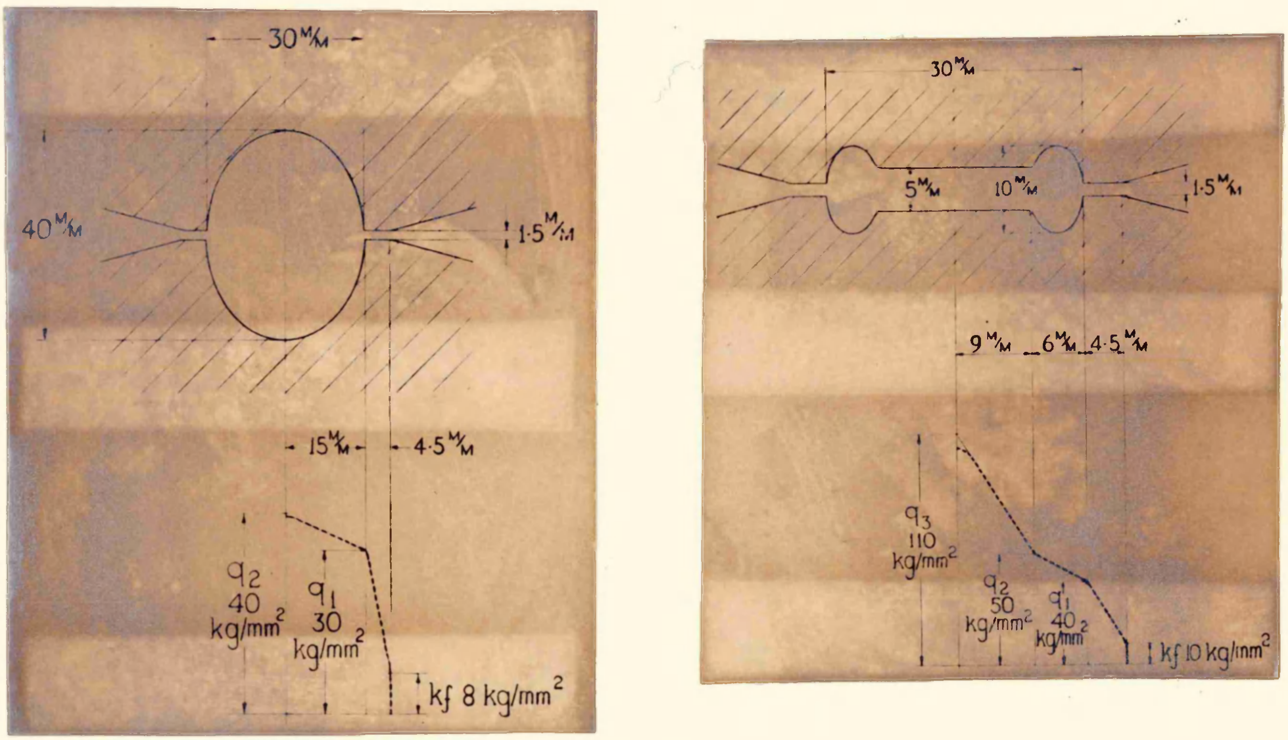


Figure 11

Typical Die Stresses in Drop - Forging Dies (After Foster[18])

2.2 General Work on Die Steels

Several investigators have attempted to develop improved die steels by considering the conventional mechanical properties of steels at elevated temperatures, usually in the region 500 - 700°C.

Prominent among these investigations have been those of the Russian workers Khasim and Parabina[20], Shishlakov[21], Livshits[22], and Gulyaev et al[23]. All these workers sought alternative materials to the commonly used Russian die steels listed in Table 6.

Table 6

Commonly Used Russian Die Steels

Material	Application	Composition								
		C	Si	Mn	Ni	Cr	Mo	W	V	Ti
5KhNV	Hammer dies	.5 .6	.15 .35	.5 .8	1.4 1.8	.5 .8	.6 1.0	-	-	--
5KhNT	Hammer dies	.5 .6	.15 .35	.5 .8	1.4 1.8	1.0 1.4	--	-	-	.08 .15
3Kh2V8	Press dies	.3 .4	.35 max.	.2 .4	--	2.2 2.7	--	7.5 9.0	.2 .5	--
4Kh2V5FM	Press dies	.35 .45	.35 max.	.40 max.	--	2.0 3.0	.4 .6	4.5 5.5	.8 1.2	--

On the basis of their tests, the various authors recommended the use of the steels shown in Table 7.

Table 7

Die Steels Recommended by Russian Investigators

Investigator	Identification of Material	Recommended Steel Composition								Application	Tested in Works Trials
		C	Si	Mn	Ni	Cr	Mo	W	V		
Khasim and Parabina	(A	.4	.3	.5	--	3.0	.6	.8	.8	Hammer	No
	(B	.5	.3	.5	--	4.5	.5	4.5	.7	Press	No
Shishlakov	(C	.5	1.3	1.0	--	1.8	--	-	-	Hammer	Yes
	(D	.5	1.0	--	.6	4.5	--	3.6	.7	Press	Yes
Gulyaev et al	(E	.4	1.0	.5	--	6.5	--	6.4	.6	Press	No
	(F	.4	.3	.6	--	3.25	1.0	8.0	.5	Press	No
	(G	.4	.5	.5	--	3.5	2.1	2.2	1.6	Press	No
	(H	.4	.2	.6	1.4	3.25	5.5	.5	.5	Press	No
Livshits	I	.5	1.5	.25	.3	.7	--	1.2	-	Press	Yes

Only Shishlakov reported data on works trials of the recommended materials. He compared the performance of material D in Table 7 with that of steel 5KhNT for the forging of three different flanges, and reported reductions in die costs between 10 and 38%.

Hopage et al[24] investigated the influence of tungsten content on the properties of chromium-tungsten steels, and considered the possible replacement of tungsten by molybdenum and/or vanadium. They found that vanadium was the most effective element in promoting hot strength (0.2% P.S. of 50 tsi at 400°C), followed by molybdenum and finally tungsten. The relative effects of the elements were found to be 0.5 V ≅ 0.9 Mo ≅ 4.4 W. However, they noted a considerable reduction in impact properties with increasing vanadium additions.

Molybdenum and vanadium were found to reduce thermal fatigue resistance more than tungsten, due to reducing the thermal conductivity of the steels as shown in Figure 12 (p. 25).

Corbett et al[25] investigated the properties of low carbon, 3% molybdenum steels to assess their suitability for use as die steels. They found that a nickel addition of 3% was necessary to produce a material of adequate hardenability and the composition finally recommended was 0.2 C, 3.0 Ni, 3.0 Mo. The authors reported that trials of the material on horizontal upsetting machines and crank presses showed good results with the steel having adequate wear resistance and particularly good resistance to heat checking. Subsequent trials in England[26] have confirmed the high resistance of this material to thermal fatigue.

In a review article ("The Present State of the Development of Cold and Hot Working Steels") Dorrenberg and Mulders[27] mention the use of Cr-Mo/W steels essentially similar to those suggested by the Russian investigators. In addition, they mention the use of nickel based alloys as die materials. Such materials have been shown to give high die lives when used as extrusion dies and brass stamping dies[28]. In the latter application, the nickel-based alloy outperformed dies made from high tungsten steel.

2.3 Specific Investigations of Die Wear

In spite of the importance of wear as a mechanism of failure in forging dies, little work has been devoted to the topic.

Livshits[22] made wear tests on experimental die steels by rotating a 13 in. diameter x 0.040 in. thick disc of cold rolled steel against the ground face of a 0.63 in. wide x 2.35 in. long test specimen. The wear resistance was judged by the length and depth of the wear scar produced. Beyond mentioning that the highest wear resistance was shown by a .5 C, 8 Cr, 3 W, .6 Ti steel, Livshits gives no details of the test results, except for photographs of the test specimens. It is by no means clear that the test conditions used simulate those in forging dies, and little value can be placed on the test without further validation of the results.

Smith et al[29] used a radio-activated insert in a production forging die to try to assess wear. They found that most of the radio-activity transferred from the insert occurred in the scale of the forgings, and thus it was necessary to collect scale from around the hammer and determine its activity to assess how much wear the insert had suffered. The fact that the radio-activity occurred

/in the scale

in the scale was thought to be due to oxidation of metallic particles removed from the insert. Although the mechanism of wear was not definitely established, Smith et al suggested that the most likely mechanism was abrasion by oxide particles derived from the forging stock.

Lange and Meinert[30], during an investigation into the effect of hard-chromium plating of dies, made wear tests by a method essentially similar to that developed by the present author. They compared the wear which occurred on plated and unplated dies by upsetting slugs 1 in. diameter x 1¼ in. long to discs ⅜ in. thick. The volume of metal worn from the dies was used as a measure of wear. The wear traces shown in Figure 13 (p. 25) are taken from Meinert's paper.

Wetter[31] studied the influence of tungsten and molybdenum additions to a 0.4 C, 2.5 Cr and 0.5 V by measuring the wear on small inserts placed in a production die, as shown in Figure 14 (p. 26). Wetter showed that wear was a function of what he termed the "tungsten equivalent" of the die, the tungsten equivalent W_E being given by (W. wt %) + (2 x Mo wt %).

Up to a tungsten equivalent of 6%, Wetter showed that improved wear resistance could be attributed to the progressive increase in the tempering resistance of the steels investigated. The Larson-Miller tempering parameter P to soften as quenched steels to a tensile strength of 160 Kp/mm^2 (100 $tonf/in^2$) was used as a measure of tempering resistance. The parameter P is given by the expression $P = T\ (20 + \log_{10} t)$, where T is the tempering temperature in degrees Kelvin and t is the tempering time in hours.

Beyond a tungsten equivalent of 6%, no increase in tempering resistance was observed although a further slight increase in wear resistance occurred. Figures 15 and 16 (p. 26 and 28) show the effect of tungsten equivalent and tempering parameter on wear.

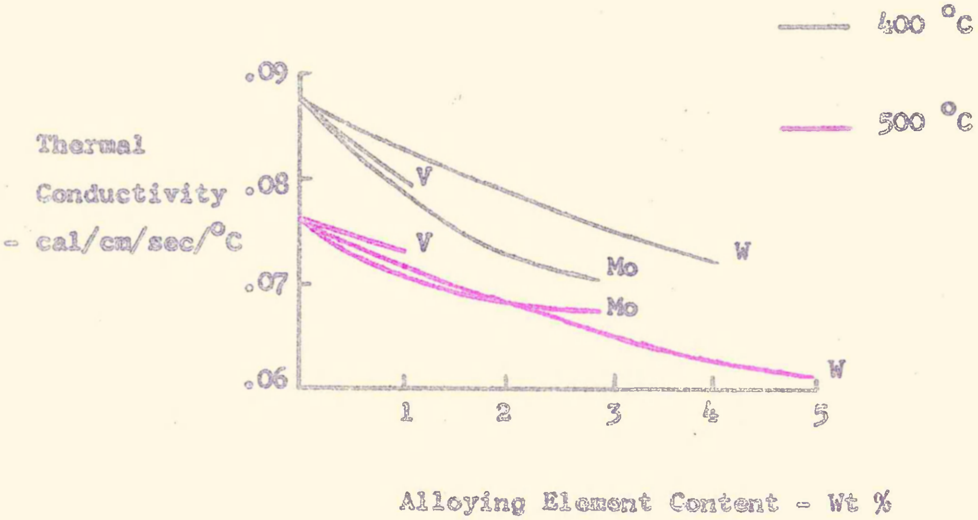


Figure 12

Influence of Mo, W and V on the Thermal Conductivity of Steels. (After Hopage et al[23])

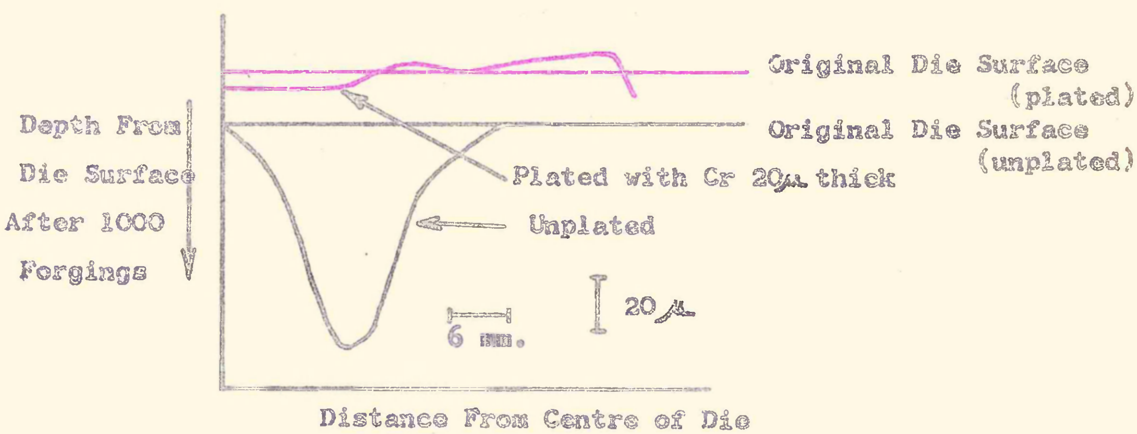
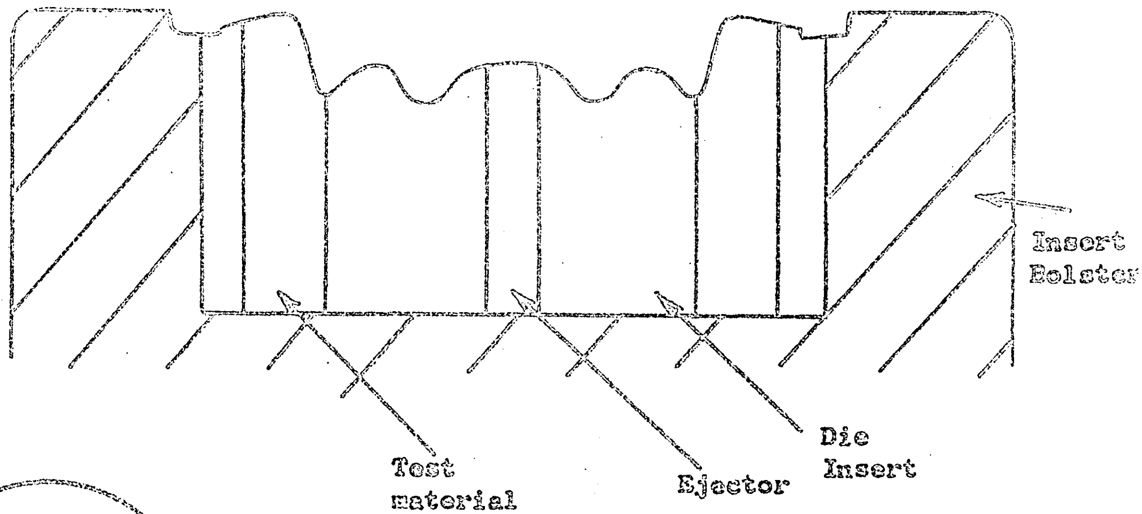


Figure 13

Wear Contour Formed on Press Dies After Upsetting of Cylinders (After Lange & Meinert[29])



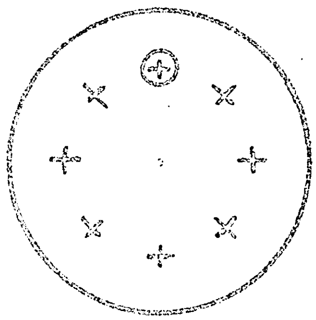


Figure 14

Inserts in a Drop - Forging Die Used by Wetter[30] to Study Die Wear

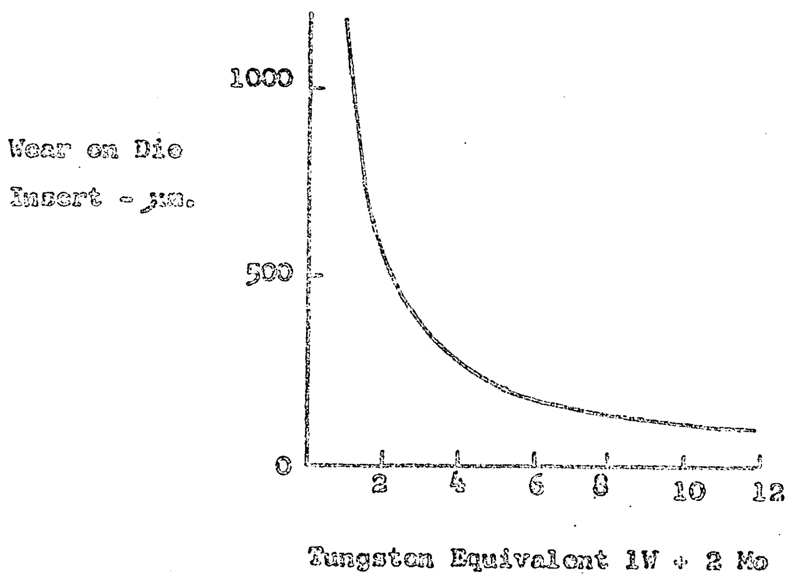


Figure 15

Influence of Tungsten Equivalent on Die Wear (After Wetter[30]).

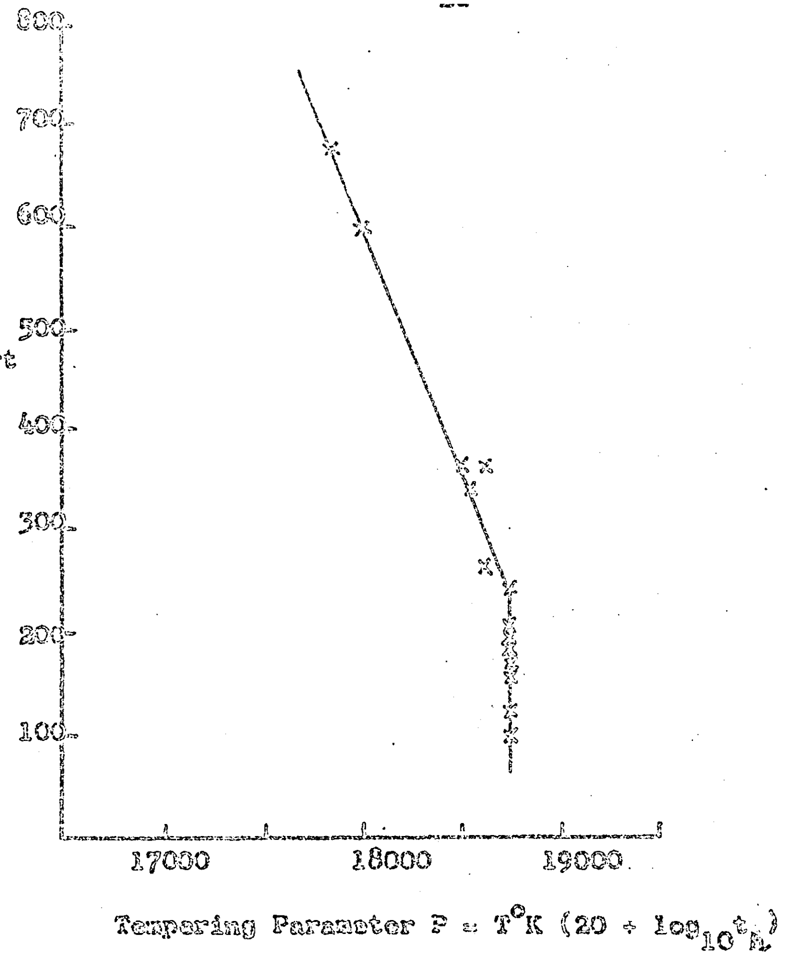
The improved wear resistance in steels of constant tempering resistance was shown to be due to the influence of the equivalent tungsten content on the stability of carbides in re-austenitised steels. Thus, Wetter was able to correlate wear resistance with the amount of carbide remaining undissolved in steels after rehardening, as shown in Figure 17 (p. 28).

Tholander[32] has studied the influence of die hardness on wear by measuring dimensional changes in dies during a forging run. The dimensions measured are indicated in Figure 18 (p. 29), whilst Figures 19 and 20 (p. 29 and 30) summarise some of the results obtained. It is clear from Figure 20 (p. 30) that Tholander's results are complicated to interpret, since deformation and wear are occuring simultaneously, but a high die hardness is obviously beneficial.

Kirkham[33], using an improved version of the wear test equipment developed by the author, has investigated the influence of initial die hardness and die preheat temperature on the wear of No. 5 Die Steel.

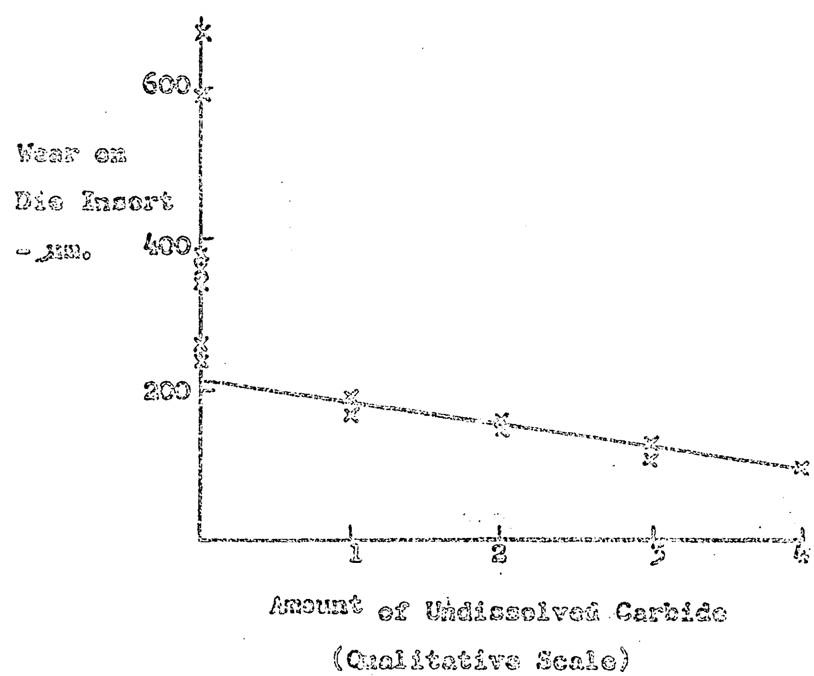
Kirkham found that wear increased (1) as the initial hardness of the die decreased, and (2) as the preheat temperature of the die increased. Once the die preheat temperature was high enough to cause re-austenitisation of the die surface during forging, the amount of wear occuring remained almost constant as shown in Figure 21 (p. 30).

Attempts by Kirkham to study structural changes at the test die surface by optical metallography were largely unsuccessful, and surface changes were more readily followed by hardness measurements. Metallographic examination did, however, confirm that re-austenitisation of the die surface did occur for dies initially preheated to 150°C or above.



<u>Figure 16</u>

Influence of Larson - Miller Tempering Parameter on Die Wear (After Wetter[30])



<u>Figure 17</u>

Influence of Carbide Stability on Die Wear (After Wetter[30])

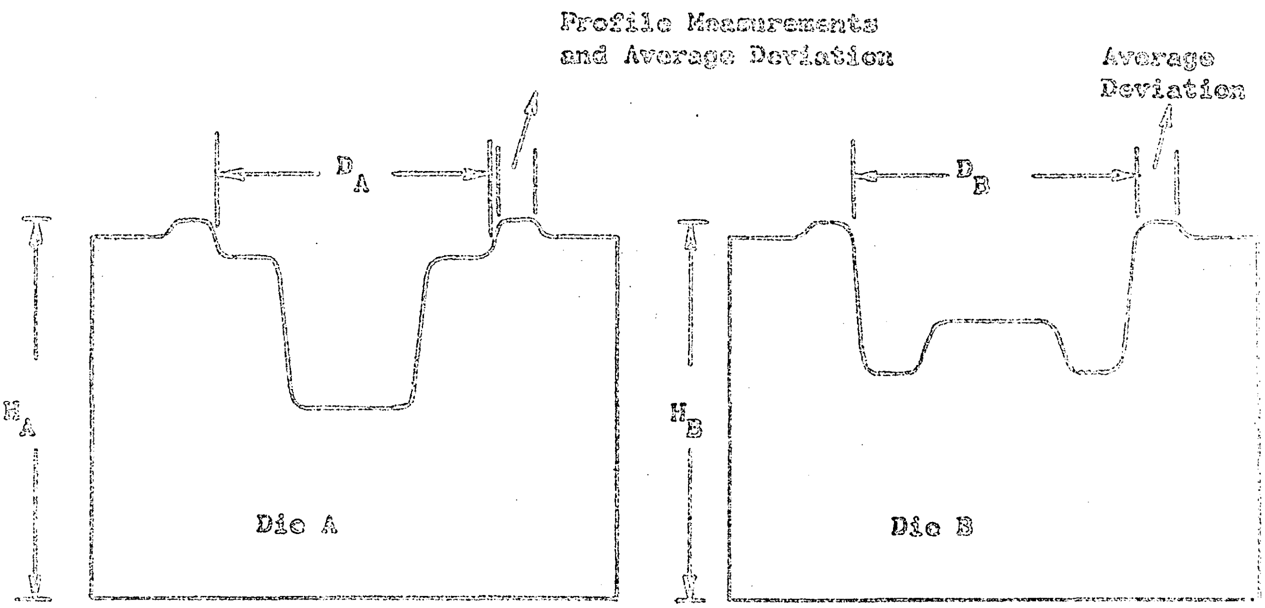


Figure 18

Dimensions Measured by Tholander[31] to Assess Die Wear

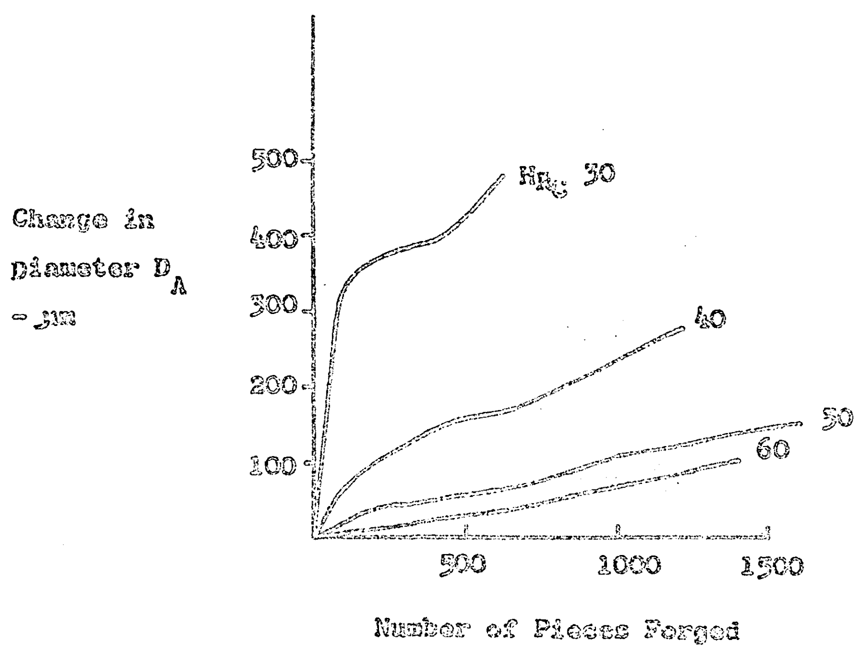


Figure 19

Change in Flange Diameter (D_A) of Die A
Above During Use (After Tholander[31])

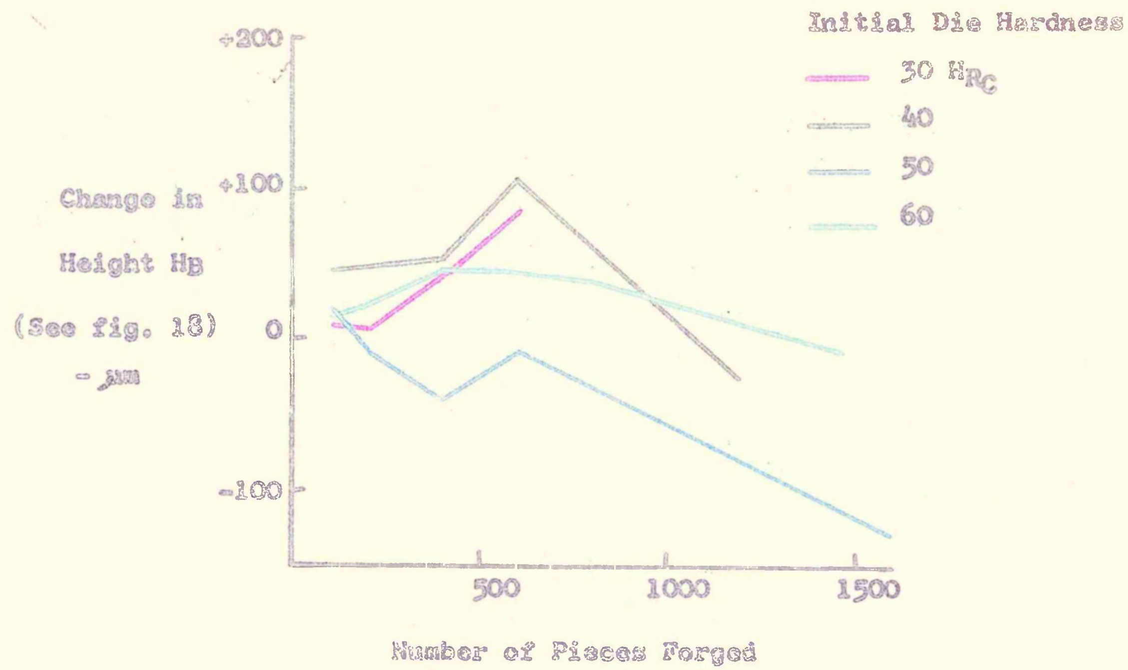


Figure 20

Change in Height of Flash Land in a Die During Use
(After Tholander[31])

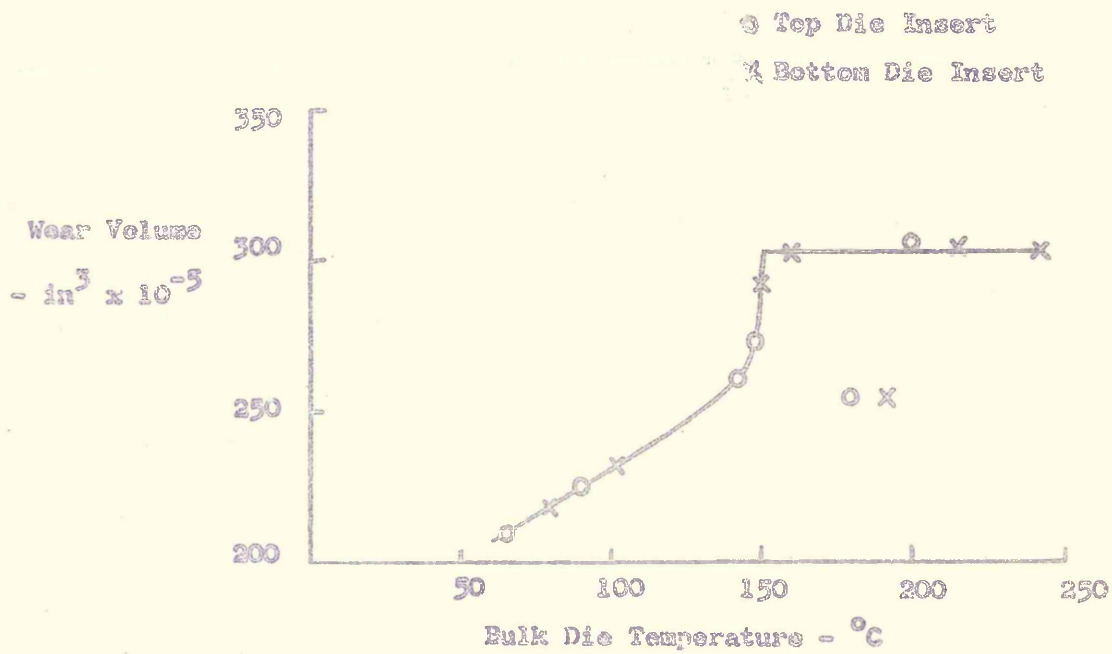


Figure 21

Variation in Wear as a Function of Die Preheat Temperature
(After Kirkham[32])

2.4 Discussion of Previous Work

It is clear from the literature review that maximum die surface temperatures of 700 - 800°C can be reached in some places in both hammer and press dies. The fact that surface temperatures are similar in both forging machines is surprising, since the contact times vary by an order of magnitude. Previous work has failed to account for this apparent anomaly.

Acknowledging the fact of high die temperatures, most attempts to develop improved die materials have involved selection on the basis of tensile strength at some arbitrarily determined high temperature, usually in the region 500 - 650°C.

As a quantitative method of comparing the performance of die steels, this approach is open to question, since a change in the composition of a die steel will influence not only its hot strength but also its thermal properties, as shown by Hopage et al[24].

Thus, whilst alloy additions will increase strength, they will also increase the operating surface temperature of the die, and it will not be possible to determine which effect will have the greatest influence on performance.

Wetter's work[31] has shown that an arbitrarily defined measure of tempering resistance could be used to explain, in part, the wear resistance of die steels over a limited range of composition. It is not clear, however, how widely this approach to selection of die materials can be applied.

So far as wear resistance is concerned, there is as yet no physical parameter which can be used to predict the behaviour of different die materials in a quantitative manner.

This suggests that, until more knowledge of the working conditions of die steels is obtained, a comparison of wear resistance must be made on the basis of wear tests which simulate forging conditions as closely as possible.

A large number of die materials has been suggested for use in all types of forging machines. There has, however, been, in nearly all cases, no economic justification for the materials recommended. There is, therefore, at the moment still no information to guide the drop forger in the selection of improved die materials and their field of application. This is likely to remain the case until there is some common basis on which die materials can be compared in a quantitative manner which correlates with service performance.

To remedy this situation, so far as wear resistance is concerned, the present investigations were undertaken to study the wear of die steels in isolation from conditions which induce other forms of die failure, such as deformation or cracking.

As already mentioned, it is clear that any form of test for wear resistance must correlate closely with service performance and, in addition, economic considerations must be studied to indicate the fields of application of materials investigated.

Furthermore, attention must be paid to other die steel requirements than their wear resistance and hot strength, since it is important that improvements in these properties do not impair toughness.

3. EXPERIMENTAL WORK - LABORATORY TESTS

3.1 Service Conditions in Forging Dies

Before designing a wear test apparatus, it was felt that more information was needed regarding the loads under which metal movement occurred at various stages of die filling, rather than a mere knowledge of maximum loads at the end of a forging cycle.

Preliminary investigations were made, therefore, to study metal flow and load variations during forging in simple die cavities.

In addition, some temperature measurements were made on hammer dies to establish the temperature fluctuations which occurred during forging, since published indications of maximum temperatures, derived from hardness measurements,[10, 11, 12] appeared incompatible with the very low contact times encountered in hammers compared with presses.

3.1.1 Metal flow and die stresses during forging

Metal flow and die stresses during forging were studied initially by using the simple apparatus shown in Figure 22 (p. 34). During the forging of lead slugs under a 35 ton capacity hydraulic press, the stress at selected points of the die surface was measured as follows.

The load at the surface was transmitted by hardened steel pins, with hemispherical ends, to rigidly supported brass measuring discs. Periodically, forging was stopped and the part-forged slug and the brass measuring discs were removed from the die. Examination of the slug enabled the pattern and extent

/of metal flow

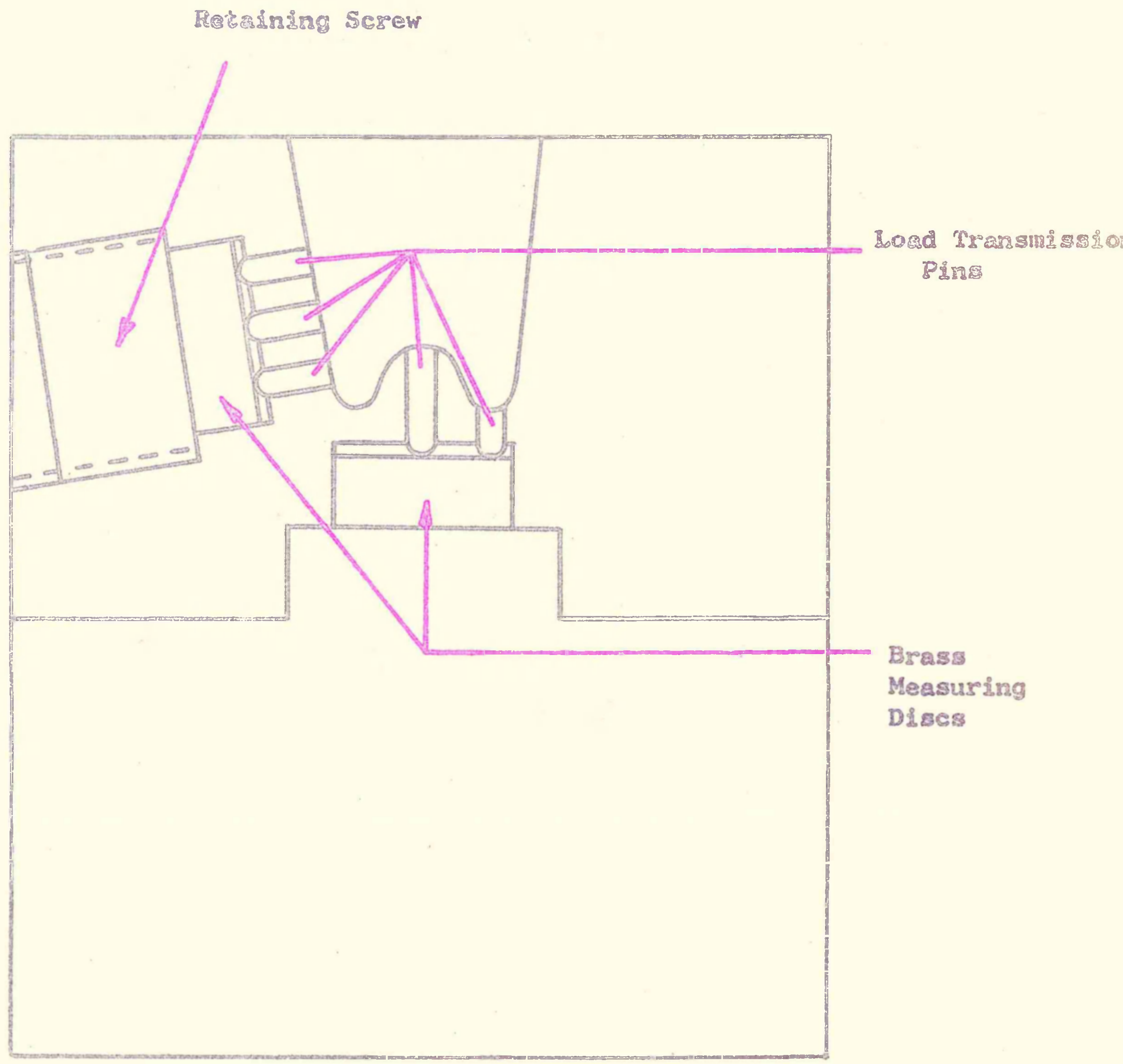


Figure 22

Die Used to Study Metal Flow and
Die Stress Distribution

of metal flow to be determined whilst the maximum load which had occurred was determined by measuring the diameter of the impressions made by the load transmission pins in the brass discs, and comparing them with a previously determined calibration curve of impression diameter versus load.

The results obtained from these tests are shown in Figures 23 and 24 (p. 36 and 37), the former showing the extent of metal flow and the latter the stress at the die surface as a function of the total applied load. These figures show that sliding of metal over the central peg has ceased when the total load is about 15 tons and the stress at the centre of the peg is about 5 tons/in^2.

Horizontal stresses acting on the "vertical" die wall when metal sliding is occuring are below about 4 tons/in^2, and are always low compared with stresses acting in the vertical direction.

These tests show that, although high die loads may occur in the later stages of forging when the flash is being thinned, during the period when metal movement is occuring within the die cavity, die stresses are relatively low. During the last stages of forging, metal flow will occur only in the region of the flash land of a die. To investigate stresses in this region during the final stages of forging, a further die was made as shown in Figure 25 (p. 38). A loose peg was incorporated in this die so that measurements of forging stresses would be made in the die with and without a peg.

Using the same measuring procedure as already described, measurements of die stresses were made after pressing lead slugs to a maximum load of 30 tonf. in dies with and without the peg. Typical results are shown in Figure 26 (p. 38) in which the stress in the die has been plotted as a function of the distance of the measuring pin from the centre of the die.

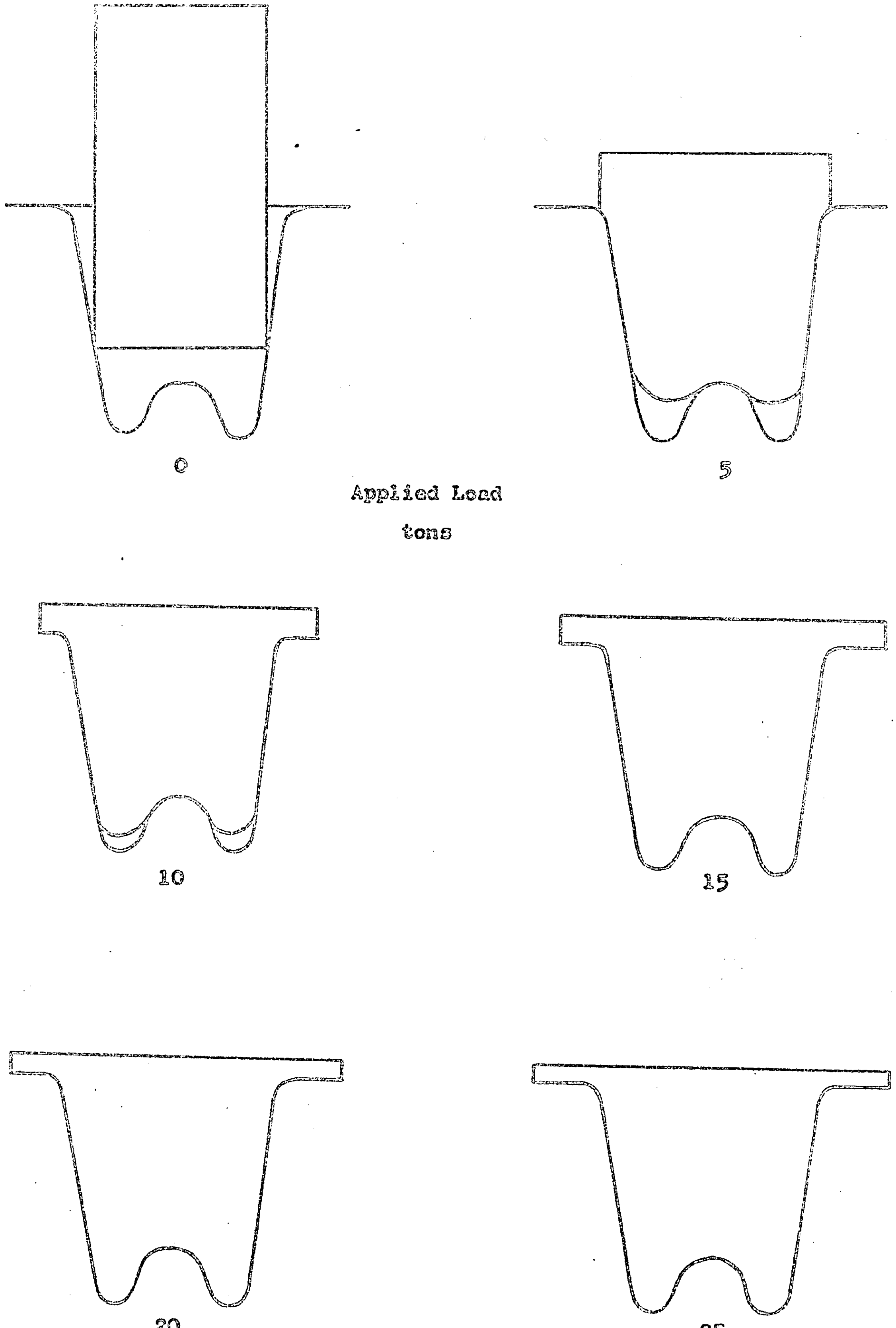


Figure 23

Metal Flow at Various Stages During Forging in the Die.

Shown in Figure 22.

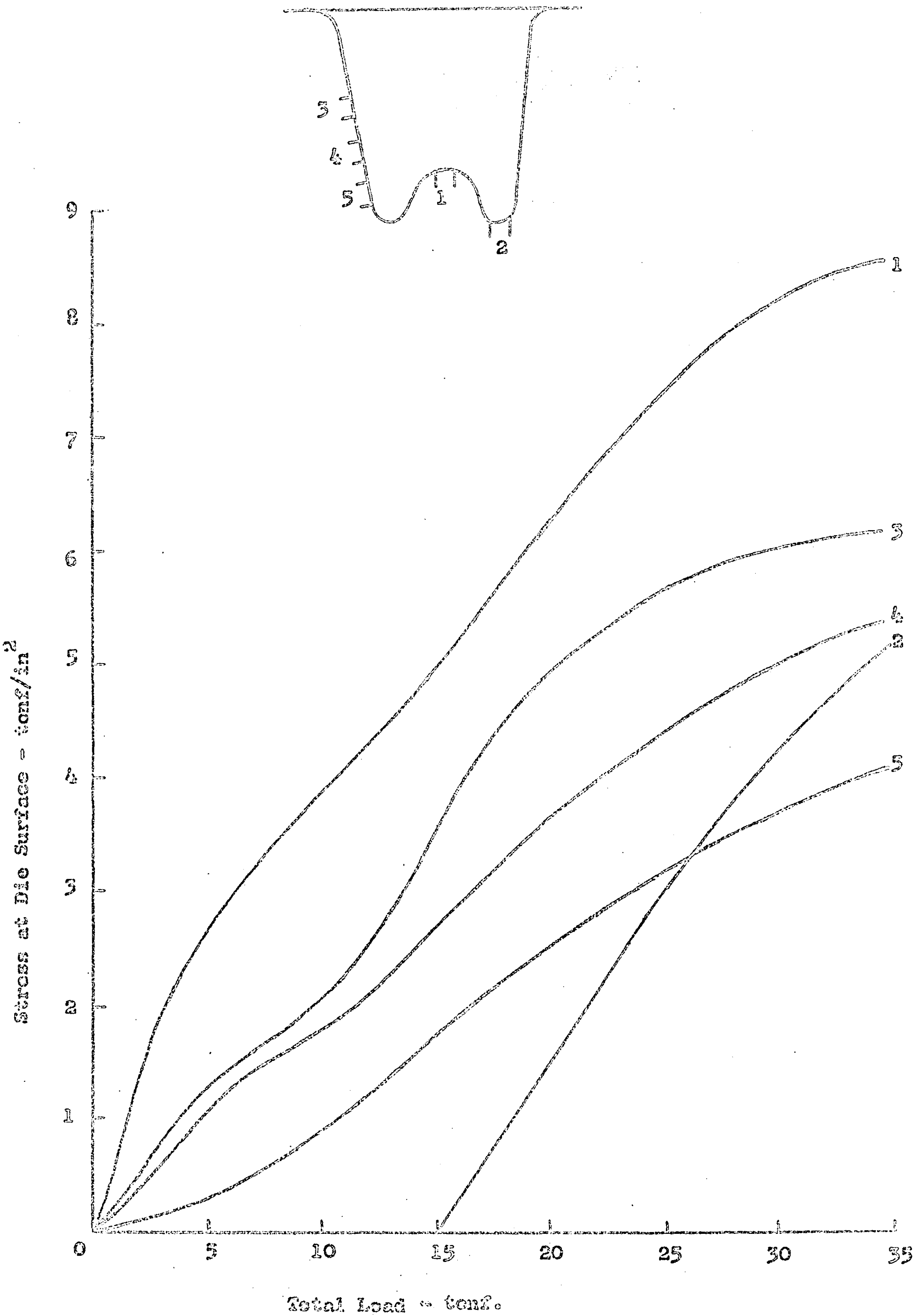


Figure 24

Stress Distribution at Various Stages During Forging in Die Shown in Figure 22.

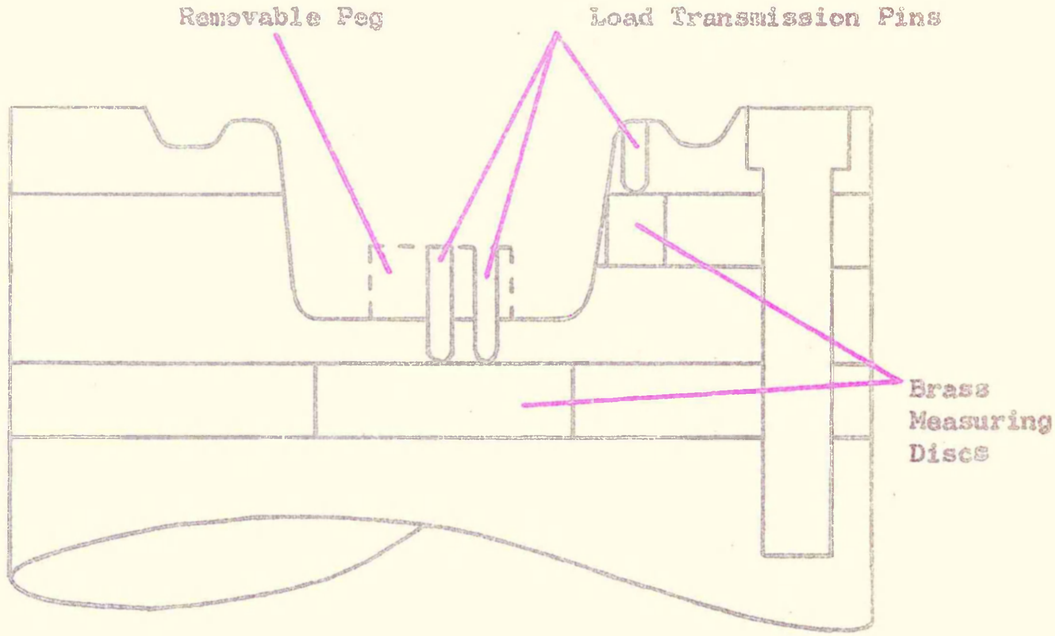


Figure 25

Die Used to Investigate Stresses in Body of Die and in Flash Land

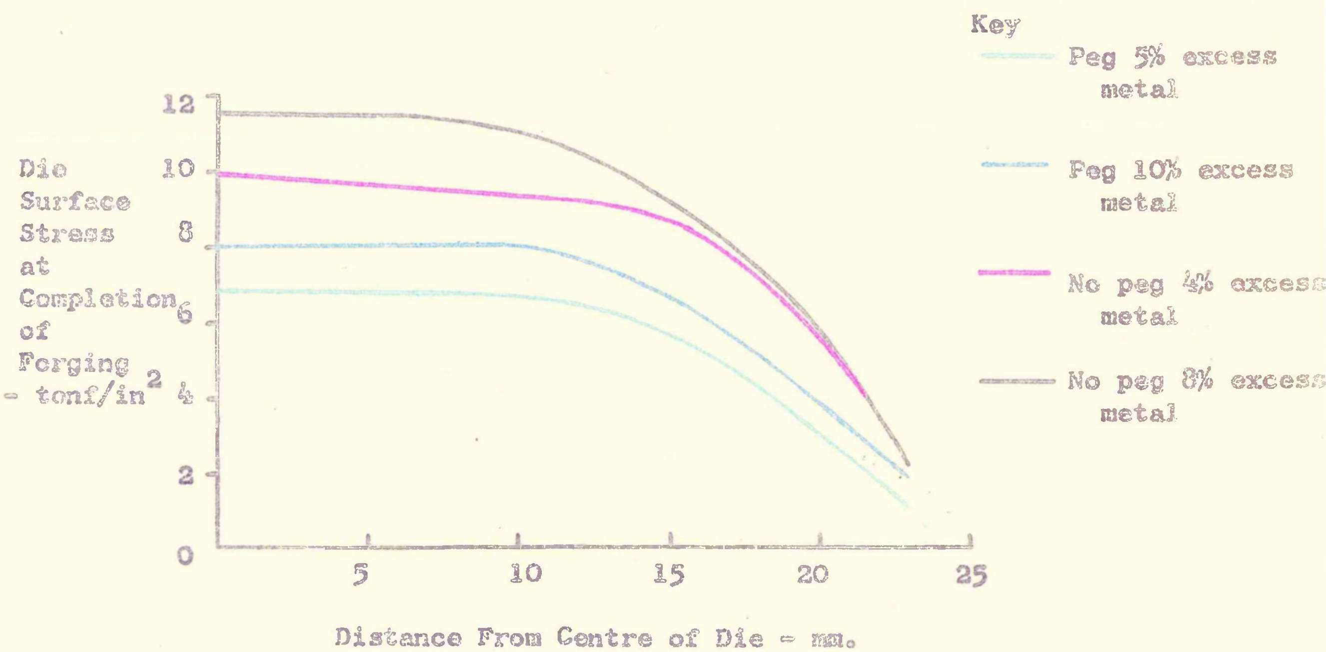


Figure 26

Stress Distribution at End of Forging Period in Die Shown Above

In all cases, the stress in the flash land was always low compared with the stress at the centre of the die at the completion of forging.

The experiments described show that metal sliding at any point in a die will occur under relatively small loads, even though the stress at certain points in the die may ultimately reach high values. However, the tests did not give any quantitative information on the level of stresses during the hot forging of steel. To obtain this information, the die shown in Figure 27 (p. 40) was made.

In this die, the stress at the centre of the die was measured by a compression load cell, whilst the stress in the flash land was measured by a beam-type load cell.

Both load cells utilised electrical resistance strain gauges connected into a Wheatstone bridge network to measure the loads. The bridge outputs during forging were recorded by a dual-beam oscilloscope; the traces being photographed by means of a polaroid camera. A typical load-time trace obtained during the forging of mild steel at 1100°C under a friction screw press is shown in Figure 28 (p. 43).

A screw press was chosen as the forging machine in preference to a hammer, since Stöter[34] has shown that the stress levels which occur when making a given forging are higher in the former machine than in the latter.

Figure 28 (p. 43) shows that during the initial period of forging when the slug is being upset to fill the die cavity, stresses are quite low. Only when metal meets the vertical wall of the die, and sliding on the base of the die ceases, does the stress at the centre of the die rise sharply. The stresses measured in the flash land were always low compared with stresses at the centre of the die.

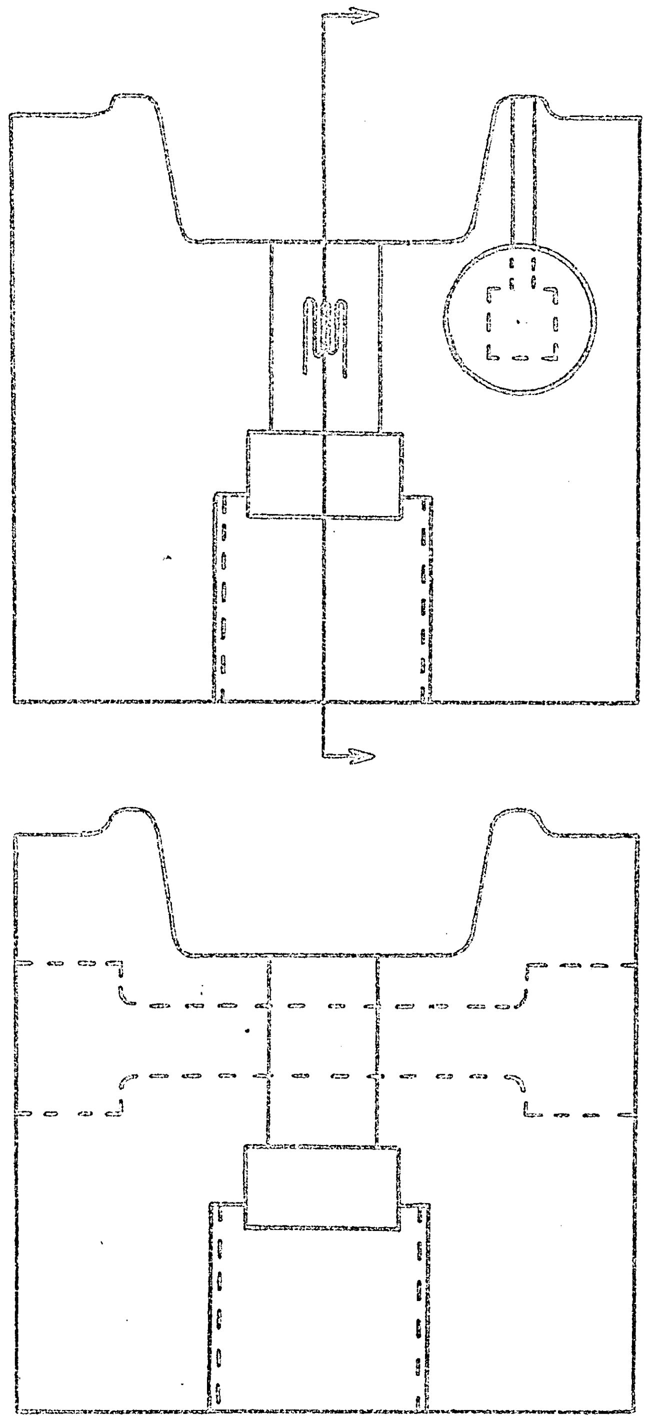


Figure 27

Die used for Measuring Stresses at Centre of Die Cavity and in the Flash Land

It has been claimed[34] that the flash land ratio (flash width/thickness ratio) has an important effect in determining die stresses. Tests were made, therefore, with different values of this ratio, the results obtained being shown in Table 8. In this table, the term "yield stress" has been used to denote the stress level at the centre of the die during the initial free-upsetting period.

Table 8

Stresses in a Die During Forging of M.S. at 1200°C

Test No.	Flash Land Dimensions		$\frac{\text{Width}}{\text{Thickness}}$ Ratio	"Yield Stress" tonf/in^2	Die Stress - tonf/in^2	
	Width inches	Thickness inches			Centre	Land
1	.197	.120	1.64	6.7	35.4	20.5
2	.197	.120	1.64	5.7	37.8	20.0
3	.197	.080	2.46	6.5	34.3	12.7
4	.197	.080	2.46	8.0	34.9	15.6
5	.197	.040	4.92	7.7	27.0	16.3
6	.197	.040	4.92	7.6	27.4	16.1

Table 8 shows that sliding at the centre of the die occurs at stress levels of 6 - 8 tonf/in^2 (i.e. the stress level during the free upsetting period). Sliding over the flash land occurs at higher stress levels between about 13 - 20 tonf/in^2

The stress under which sliding takes place is not influenced very much by the flash land ratio within the limits investigated.

3.1.2 Temperature measurements in dies

Although it was appreciated that true measurements of die surface temperature were unlikely to be obtained, temperature measurements in dies were undertaken for the following reasons:

(1) If the same method was used for temperature determinations in forging dies and dies used in a wear test apparatus, similar readings would assure similar thermal conditions even though absolute values of temperature were not determined.

(2) As already stated, more information was needed to determine why temperatures for hammer and press dies, deduced from hardness measurements, were similar when contact times differed by an order of magnitude.

The thermocouple arrangement used for all die temperature measurements is shown in Figure 29 (p. 43). A 1/16 in. diameter hole was drilled from the back of a die insert to within 1/16 in. of the die surface. A 0.020 in. diameter hole was then drilled from the die surface to meet the larger hole. A Constantan wire with a bead on the end was passed through the smaller hole and the bead peened into the hole and polished flush with the insert surface. An iron wire was then attached to the die to form an Iron-Constantan thermocouple.

The output from the thermocouple was connected to one beam of a double beam oscilloscope, the other beam being used during a test to record a time trace derived from a signal generator.

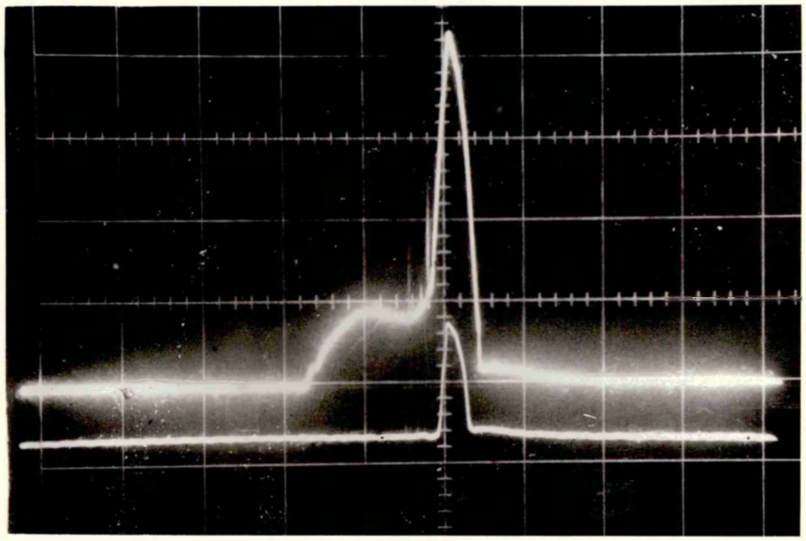


Figure 28

Typical Oscilloscope Trace Obtained When Forging in Die Shown in Figure 27

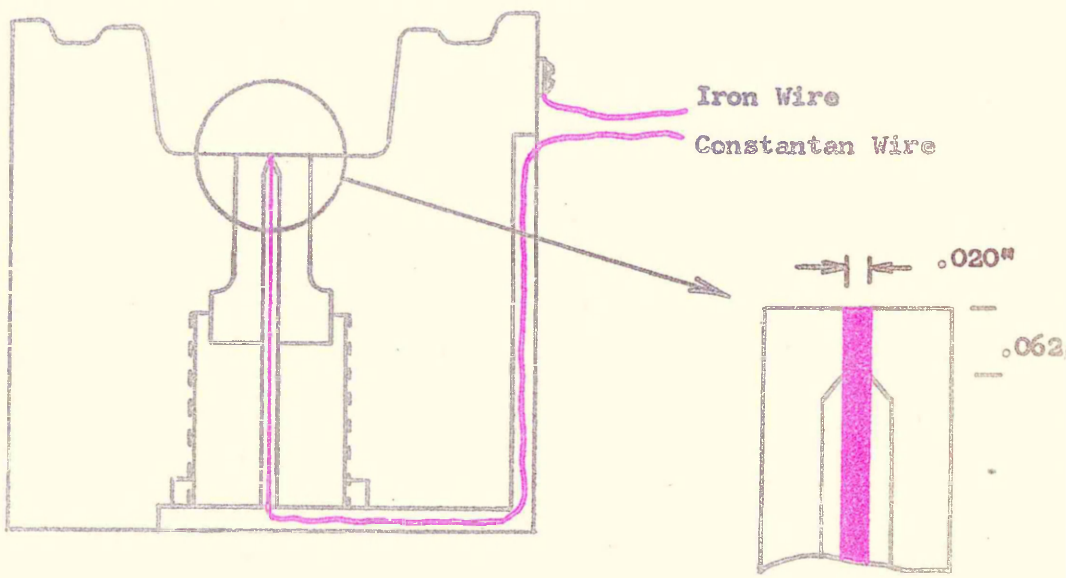


Figure 29

Thermocouple Arrangement Used for Die Surface Temperature Measurement

Using the die shown in Figure 29 (p. 43) a run of eleven forgings was made from 1 in. diameter x 2 in. long slugs forged at 1200°C in three blows on a 10 cwt. drop hammer. A typical time-temperature trace for the forging of one slug is shown in Figure 30 (p. 45).

The various time intervals into which the forging process may be divided are indicated in Figure 30 (p. 45). At the beginning of interval t_1 the slug is placed on the die which is at temperature θ_0 and a very slight temperature rise occurs to θ_1 at the end of t_1. The heat transfer during this period is very low, due to the presence of scale on the slug and the poor thermal contact between slug and die.

At the beginning of time intervals t_2, t_4, and t_6 the forging blows are struck, and during these periods improved thermal contact between the forging and the die leads to the temperature increases shown. Table 9 (p. 46) shows the time intervals and temperatures recorded during forging of eleven slugs.

Load-time traces were also recorded for forgings produced in the same way as those used for temperature measurements. Typical traces for each of the three blows used to produce the forging are plotted in Figure 31 (p. 45).

Table 9 shows a number of interesting features. Firstly, the recorded values of θ_0 show that a quasi-equilibrium state is soon established at the die surface with the steady die temperature settling down at about 130°C after only five forgings, as shown in Figure 32 (p. 51).

A second point of particular interest concerns the duration of the temperature rise during the three forging blows, as shown by the time intervals t_2, t_4, and t_6.

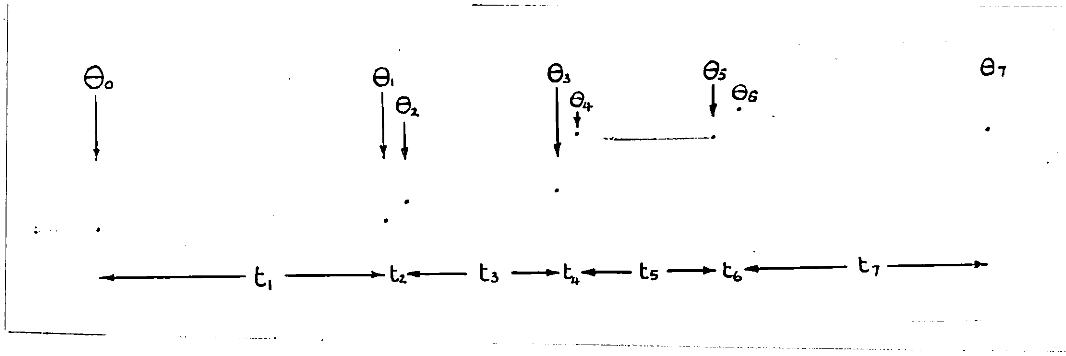


Figure 30

Typical Time-Temperature Record During Forging

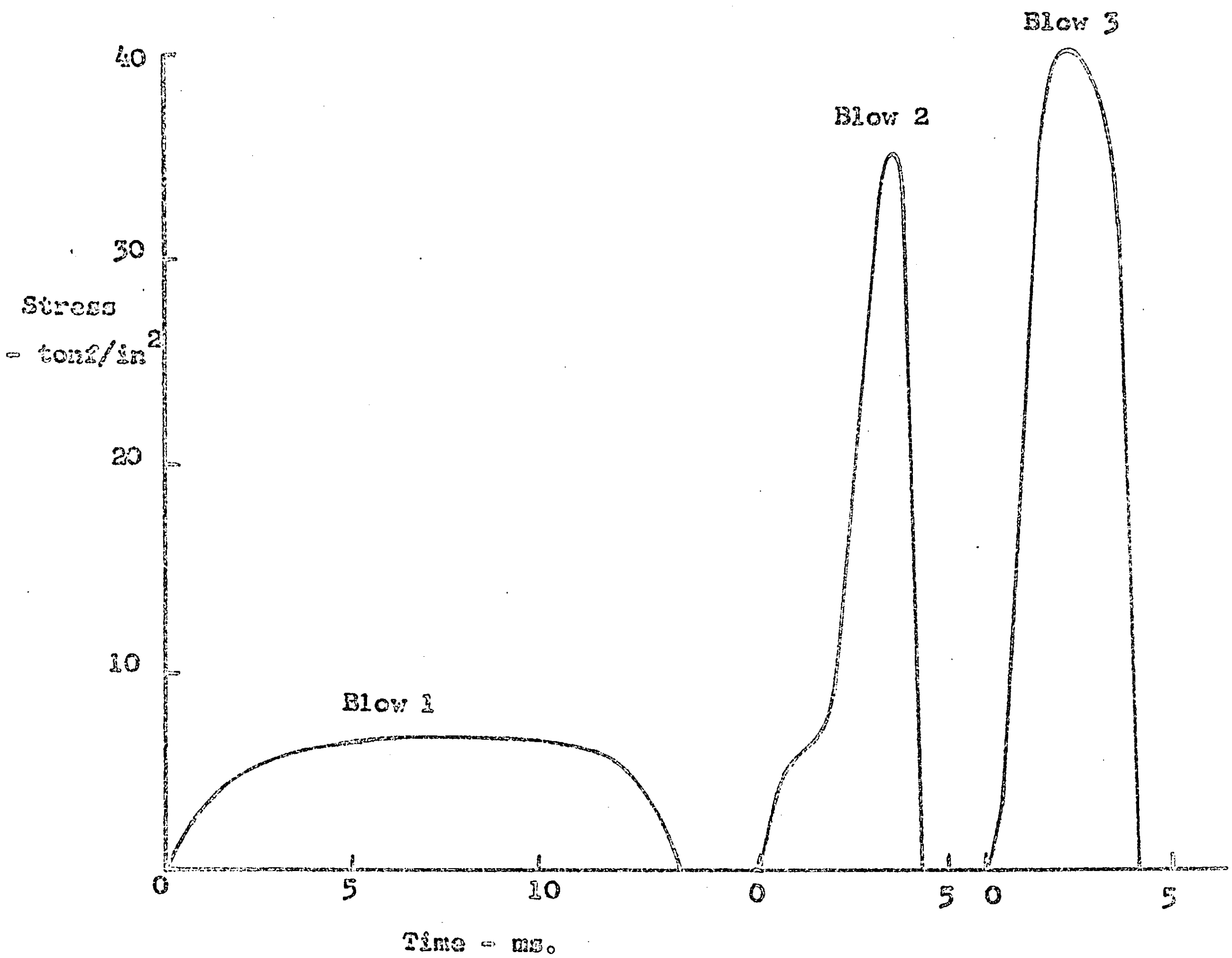


Figure 31

Stress at Centre of a Die During Manufacture of a Forging in Three Blows.

Table 9

Time Intervals and Corresponding Temperatures during Forging

Forging No.	Temperature - ^{o}C								Time Interval - Seconds						
	θ_0	θ_1	θ_2	θ_3	θ_4	θ_5	θ_6	θ_7	t_1	t_2	t_3	t_4	t_5	t_6	t_7
1	20	72	115	126	345	328	440	389	3.98	0.23	1.01	0.17	1.12	0.17	2.32
2	87	142	241	148	367	323	440	374	3.16	0.02	1.18	0.13	1.03	0.17	2.47
3	108	148	250	172	378	330	404	340	2.22	0.13	1.18	0.13	1.15	0.13	1.77
4	116	151	237	187	382	345	454	389	7.26	0.01	1.27	0.13	1.13	0.17	0.93
5	123	162	208	234	396	367	431	360	2.15	0.01	1.27	0.16	1.05	0.17	1.36
6	130	132	215	184	408	222	368	330	3.26	0.01	1.27	0.15	1.06	0.16	0.35
7	120	148	220	222	396	244	389	367	2.10	0.15	1.16	0.13	1.07	0.15	1.99
8	144	144	215	215	396	244	352	330	2.58	0.10	1.15	0.14	1.09	0.13	0.33
9	130	130	244	230	372	330	418	372	2.68	0.37	1.07	0.13	1.09	0.17	1.54
10	112	127	172	194	342	323	397	338	2.23	0.17	1.18	0.17	1.05	0.22	1.92
11	123	130	184	206	360	345	421	367	1.74	0.08	1.10	0.17	1.11	0.20	1.44

Table 10 below compares the load duration for each blow (from Figure 31 p. 45) with the duration of the temperature rise.

Table 10

Load and Temperature Rise Durations

Blow Number	Duration of Temperature Rise - seconds*	Duration of Load - seconds
1	0.12	0.015
2	0.15	0.005
3	0.17	0.004

*mean values from Table 9

The table shows that heat transfer from the slug to the die occurs over a much longer period than that for which the forging load is sustained. A probable explanation of this apparent anomaly is as follows.

During the period when the forging load is maintained, good thermal contact is established between the forging and the die. After the energy of the blow has been dissipated in forging, the tup probably rests on the die for a short period before the hammer driver lifts it to deliver the next blow. During this period, the only load on the die is that due to the weight of the tup, which is too small to be registered by the load cell. It is probable, however, that this tup weight is sufficient to maintain the good heat transfer between stock and die which was established during the period of a high forging load.

Thus, the duration of heat transfer in hammers and presses is probably similar in spite of the large differences in forging load duration. This would explain the similar thermal effects noted on the two forging machines by previous investigators.

The maximum die temperatures shown in Table 9 (θ_6) were between 350 and 450°C, and this indicates that similar temperatures should be achieved, if possible, in dies used in a simulative wear test.

3.2 Development of a Wear Test

3.2.1 Conditions necessary to simulate practical forging

In any form of simulative wear test, the forging conditions in the test should approach as closely as possible those occuring in practical forging if the test results are to be applicable in the forge.

Knowledge gained from the literature survey, and the experiments described in sections 3.1.1 and 3.1.2 of this thesis, give a good indication of the forging conditions to be reproduced.

Briefly summarised, the requirements are as follows:

(1) During a wear test, hot scaled forging stock must slide over the surface of the test die.

(2) Contact between the forging stock and the die surface must be intermittent with a contact period of about .1 to .2 seconds as indicated by the time intervals t_2, t_4, and t_6 in Table 9. The non-contact period should be representative of the time interval between the completion of one forging and the start of the next one under practical forging conditions. This

/period will

period will vary widely according to the type and size of the forging, but for small to medium sized forgings, which account for the majority made, will be typically of the order of 10 seconds.

(3) The loads under which sliding of the stock material over the test die occurs must be representative of those found in commercial forging. The experiments described in section 3.1.1 suggest that sliding should take place under a stress of 6 - 20 tonf/in^2.

The adaptation of conventional methods of wear testing, such as pin and disc tests, crossed cylinder tests, and rotating ball tests, was considered to be difficult due to the need to heat one component to such high temperatures that plastic deformation would ensue.

The inevitability of plastic deformation suggested that the wear test should take the form of a simple forging operation. Preliminary investigations were made, therefore, to investigate whether the required test conditions could be met by the simplest of all forging operations, that of upsetting small cylinders between flat dies.

3.2.2 Load and temperature measurements during upset forging

The first step taken in the development of an erosive wear test was to determine die loads and temperatures during upset forging.

An eccentric press of the C-frame type, with a stroke length of 1 in. and a nominal capacity of 20 tonf, was selected as the forging machine for the tests.

To study the loads and temperatures when forging under this press, the die shown in Figure 27 (p. 40) was used.

Mild steel slugs $\frac{3}{4}$ in. long x $\frac{1}{2}$ in. diameter were upset to various final thicknesses by forging at 1100°C. Loads and temperatures were recorded on a double beam oscilloscope. A typical trace showing load and temperature during forging is shown in Figure 33 (p. 51). The second deflections on this trace were due to a second forging blow being struck before the press could be stopped or the slug removed from the die. Only the loads and temperatures indicated by the first deflection are considered.

The results of these preliminary tests are presented in Table 11 and Figure 34 (p. 52).

Table 11

Die Loads and Temperatures during Upsetting of Mild Steel Slugs $\frac{3}{4}$ in. long x $\frac{1}{2}$ in. diameter

Test No.	Original Slug Length - in.	Final Slug Length - in.	Maximum Temperature - °C	Maximum Stress - tonf/in^2*
1	0.750	0.728	56	0
2	0.750	0.748	42	0
3	0.750	0.660	232	5.4
4	0.750	0.665	297	3.6
5	0.750	0.538	287	8.1
6	0.750	0.537	305	7.5
7	0.750	0.416	282	9.7
8	0.750	0.414	278	8.2
9	0.750	0.302	342	12.5
10	0.750	0.300	360	13.0
11	0.750	0.203	406	21.0
12	0.750	0.211	360	20.0

*calculated from maximum load and final cross section of forged slug

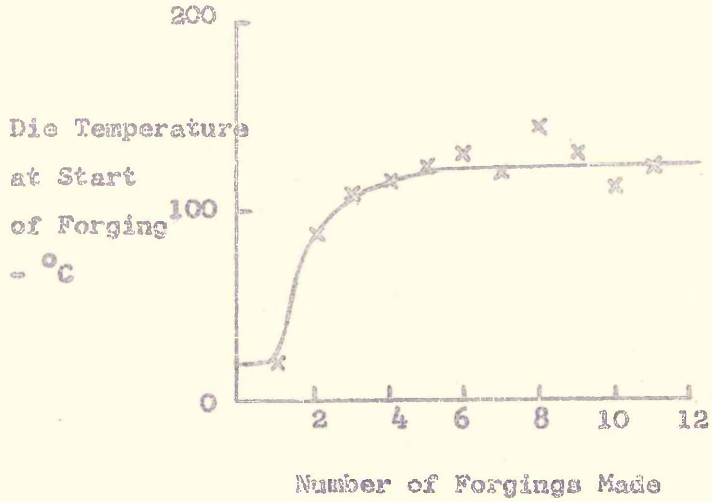


Figure 32

Die Surface Temperature at Start of Forging as a Function of Number of Forgings Made.

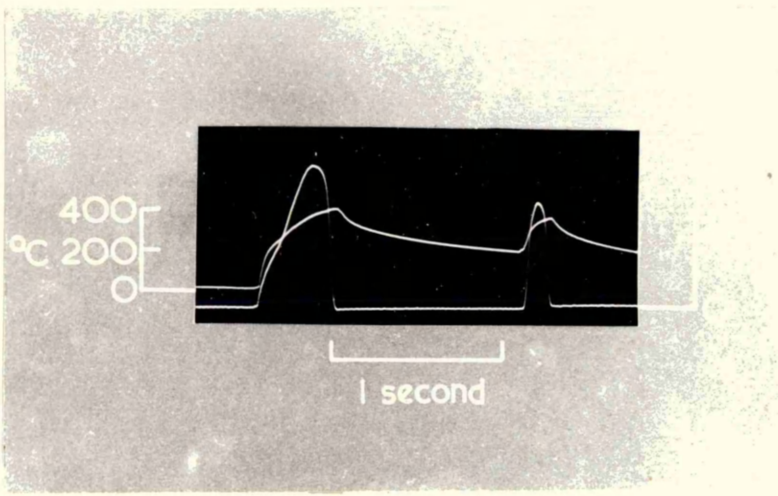


Figure 33

Load and Temperature Traces During Upset Forging of Slugs.

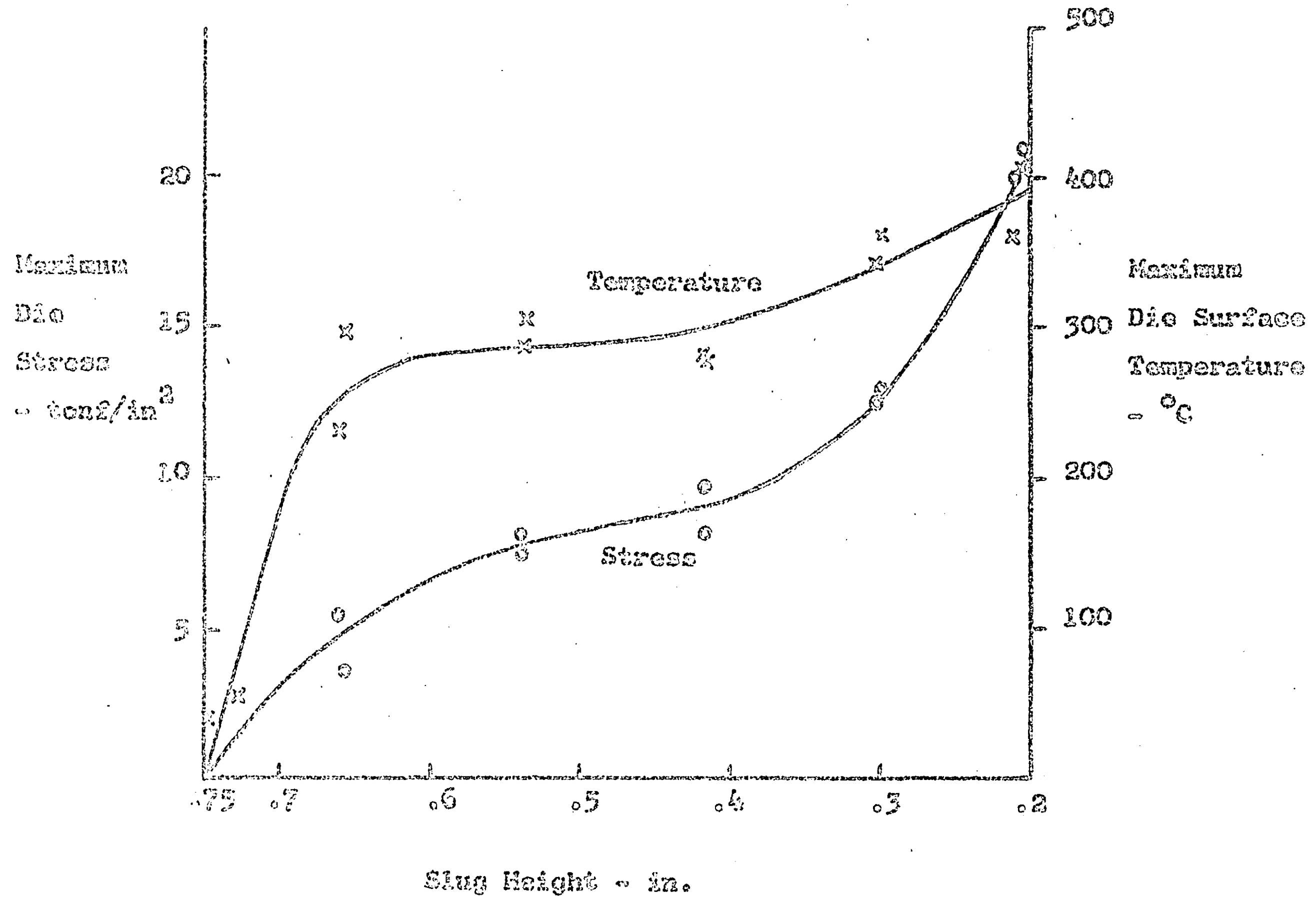


Figure 34

Maximum Stress and Die Surface Temperature During Upset Forging as a Function of Final Slug Height

Table 11 and Figure 34 (p. 52) show that for forgings of a final thickness of 0.3 in. or less the stresses and temperatures are very close to those required to simulate forging in closed dies. The measured die temperatures (340 - 400°C) agree well with those previously determined, by the same thermocouple arrangement, under practical hammer forging conditions (350 - 450°C).

The results of these preliminary tests, therefore, suggested that forging conditions could be simulated by simple upsetting, and that this method could be used as the basis of a wear test.

The work of Smith et al[28], previously described, suggested that the rate of die wear during forging would be of the order of .001 in. per 1000 forgings. This indicated that, to produce a measurable amount of die wear by upset forging, several thousand forgings would have to be made. Hand feeding of such large numbers would be time consuming and tedious, and it was decided, therefore, that feeding of slugs to the press must be done automatically. The next section of this thesis describes the development of an automatic feeding system.

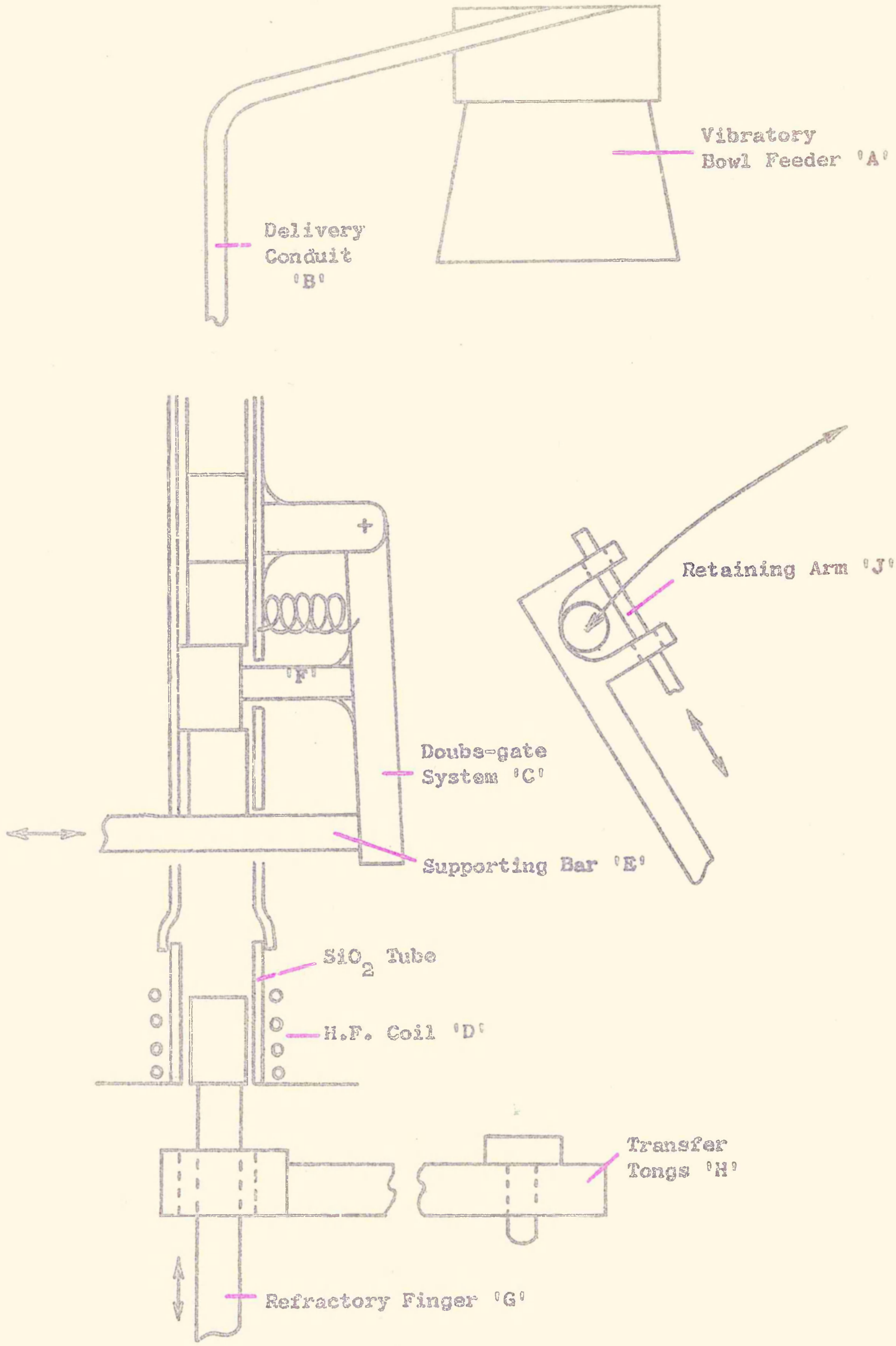
3.2.3 Development of an automatic forging press

The automatic feeding mechanism developed for the forging press is shown schematically in Figure 35 (p. 54).

Sheared and barrelled slugs were placed in a vibratory bowl feeder "A" which fed them, correctly oriented, to a conduit "B". The slugs were led by the conduit to a double-gate system "C" which allowed one slug at a time to pass into an induction heating coil "D".

The operation of the double-gate system was as follows. The column of slugs was supported by a bar "E" which was retracted by an air cylinder when a slug was to be passed into the induction coil. Retraction of the bar "E"

/allowed the



<u>Figure 35</u>

Schematic Diagram of Automatic Feeding Arrangement allowed the bottom slug in the column to drop into the coil whilst the remainder of the column of slugs was held in position by the spring-loaded bar "F" holding a slug against the conduit wall.

When the bar "E" returned to its forward position, the bar "F" released the column of slugs which were again supported on the bar "E".

The slug was supported in the heating coil by a refractory finger "G". When heating was completed, the finger "G" was lowered by an air cylinder transferring the slug into the tongs "H". Operation of another air cylinder moved the tongs to the forging station under the press. When this position was reached, a further cylinder withdrew the retaining arm "J" of the tongs, which then returned under the heating coil leaving the slug in position on the die ready for forging.

The clutch of the press was then engaged and the slug forged. After forging, the slug was ejected by a cylinder operated ejector arm.

All the air cylinders, except that operating the ejector, were activated by cam operated air valves. The cams to operate the valves were carried on a drum driven by a synchronous motor. The positions of the cam on the drum and their length determined the timing of operation of the cylinders.

The ejector cylinder was operated by air valves which were triggered by cams mounted on the ram of the press. The ejector cylinder was timed to operate before the ram pressure on the forged slug was released. In this way, the ejector pushed hard against the forging and ejected it forcibly as soon as the forging pressure was released. This was necessary to ensure ejection of the slug, even if sticking of the slug to the top die tended to occur.

The air circuit which operated the press clutch cylinder was designed in such a way that the clutch could not be engaged until the tongs had returned to a safe position under the heating coil, thus obviating any possibility of forging the tongs. Details of the circuit are given in Appendix I to this thesis, together with details of an electro-pneumatic circuit which replaced the original one during the period of the tests.

Figure 36 (p. 57) shows a photograph of the automatic feeding apparatus fitted to a 20 tonf. capacity crank press.

The sequence of operations for a complete forging cycle together with the approximate time interval for each operation is shown in Table 12 below.

Table 12

Sequence of Operations of Automatic Forging Press

Operation	Time Occupied - sec.
1. Slug dropped into heating coil	-
2. Slug heated to forging temperature	7
3. Slug transferred to tongs	1
4. Slug transferred to forging station	2
5. Tongs returned under coil	1
6. Slug forged) 7. Slug ejected)	1
8. Next slug into heating coil	

The heating time of 7 seconds was the minimum time in which uniform heating of the slug could be achieved.

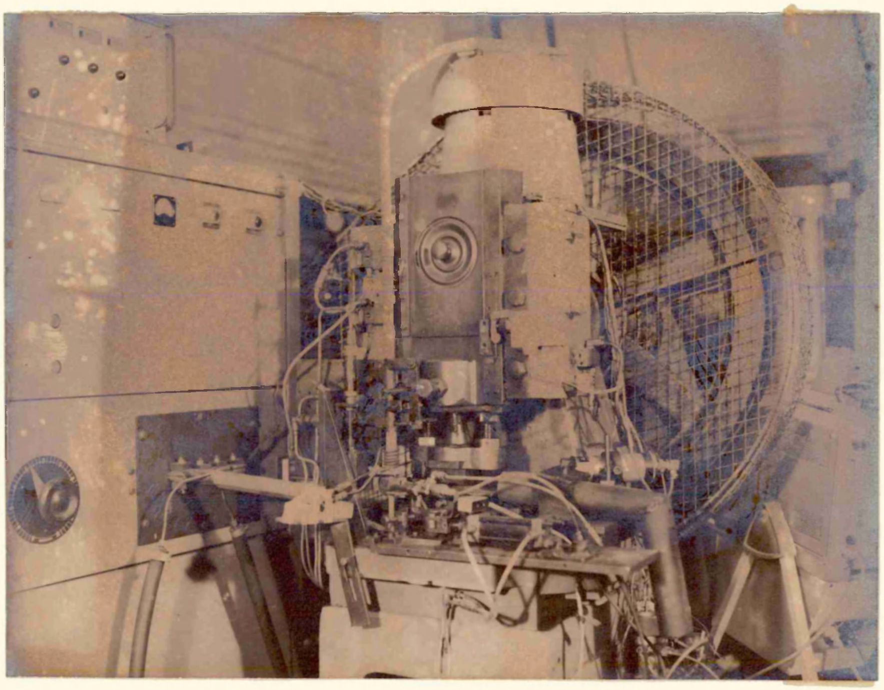


Figure 36

Automatic Feeding Apparatus Fitted to a
20 tonf. Capacity Eccentric Press

The total cycle time of 12 seconds allowed 300 forgings an hour to be made, which is typical of production rates in commercial forging, so that thermal cycles in the test dies were similar to those in industrial dies.

The temperature of each slug immediately prior to forging was monitored by a Land continuous optical pyrometer. Equipment was developed which allowed the number of slugs falling within three pre-selected temperature ranges to be recorded on counters. Details of this equipment are given in Appendix II.

Table 13, which shows the percentage of 2000 slugs falling within the indicated temperature ranges, illustrates the degree of temperature uniformity achieved.

Table 13

Percentage of Slugs Within Indicated Temperature Ranges

Test No.	≤ 1075°C	1075 - 1095°C	1095 - 1115°C	>1115°C
27	1.4	10.0	72.6	16.0
28	0.0	3.0	41.0	56.0
29	1.4	6.9	54.1	37.6
31	0.0	19.0	60.0	21.7
32	0.2	18.2	55.8	24.2
33	14.1	18.7	50.1	17.1
34	1.8	16.4	52.0	19.7
Average for 7 tests	4.0	13.0	55.0	28.0

Table 13 shows that about 70% of all slugs forged were within $\pm$ 20°C of the required forging temperature of 1100°C. Almost all those falling outside this range were too hot.

The reason for this was eventually traced to an increase in mains voltage when local demand fell during lunch breaks and in the late afternoon when nearby factories closed for the day.

Suitable adjustment of the output transformer of the H.F. heating set at the appropriate time eventually allowed over 90% of slugs to be forged within $\pm$ 20°C of the required temperature.

3.2.4 Test procedure

Steel for the test dies was obtained in the form of $2\frac{1}{4}$ in. round bars. These were machined to 2 in. diameter, and slices $\frac{5}{8}$ in. thick were cut from them. These slices were then hardened and tempered to the required hardness level before grinding 1/16 in. from each face to remove any decarburisation.

The test dies were then placed in the press and 1000 mild steel slugs were forged at 1100°C. The top and bottom dies were then reversed, and a further 1000 slugs forged. The reason for reversing the dies half way through each test was as follows.

In early tests, a temperature gradient existed in the slugs with the top of the slug being hotter than the bottom. This led to more spreading of the slug occuring at the top than at the bottom.

This effect can easily be shown to have a pronounced effect on the relative wear occuring on top and bottom dies as follows.

Evenly heated slugs deform uniformly as shown in Figure 37a (p. 61), producing equal areas of sliding on both top and bottom dies. Unevenly heated slugs, however, deform as shown in Figure 37b (p. 61), producing unequal areas of sliding. A limiting case is shown in Figure 37c (p. 61) where no sliding has occurred on the bottom die.

Figure 38 (p. 61) shows the calculated ratio of the sliding areas on top and bottom dies as a function of the ratio $^{Dt}/Db$, where Dt is the final diameter of the top of the slug and Db the final diameter of the bottom of the slug. This figure shows that the ratio of the two sliding areas is very sensitive to non-uniform deformation.

The sum of the two sliding areas is, however, much less sensitive to non-uniform deformation, as shown in Figure 39 (p. 62).

To obviate any errors in die wear assessment due to such an effect, the dies were reversed half way through a test cycle.

Figure 40 (p. 62) shows a typical test die after making 2000 forgings. The wear pattern takes the form of an annular groove surrounding a central unworn plateau, as illustrated schematically in Figure 41 (p. 64).

In the worn region of the die, radial grooves appeared indicating the direction of metal flow during upsetting. These grooves are similar to those found in production forging dies in locations where erosive wear takes place. Grooves were not found in the central unworn plateau, which was marked only by a fine network of cracks indicative of heat-checking.

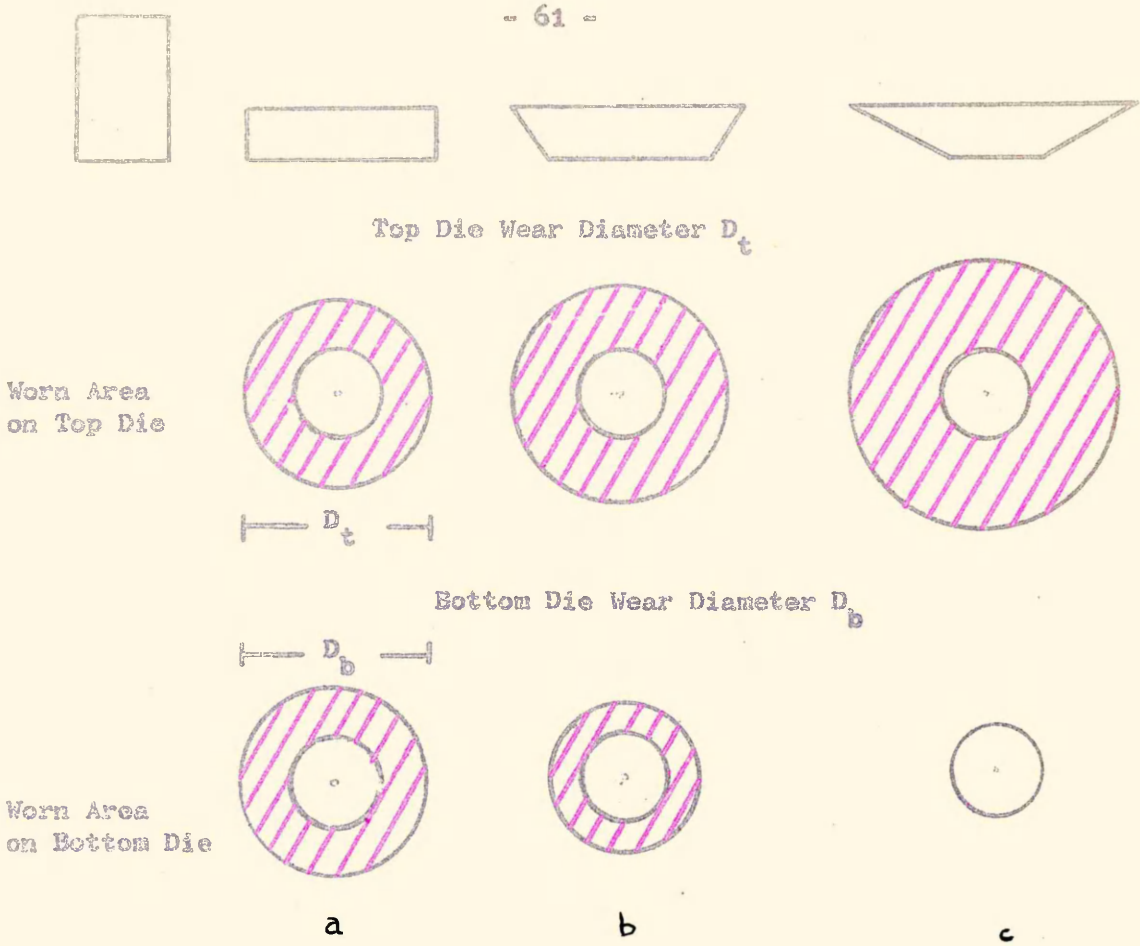


Figure 37

Influence of Non-Uniform Deformation on Wear Areas of Top and Bottom Dies.

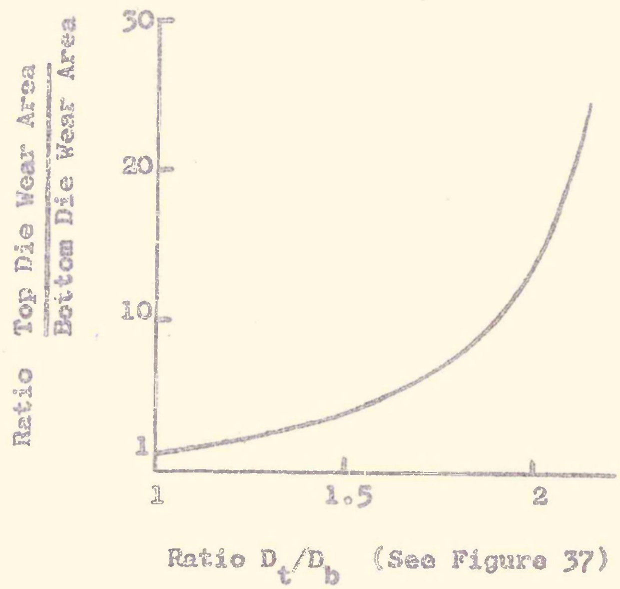


Figure 38

Influence of Non-Uniform Deformation on Ratio of Wear Areas of Top and Bottom Dies

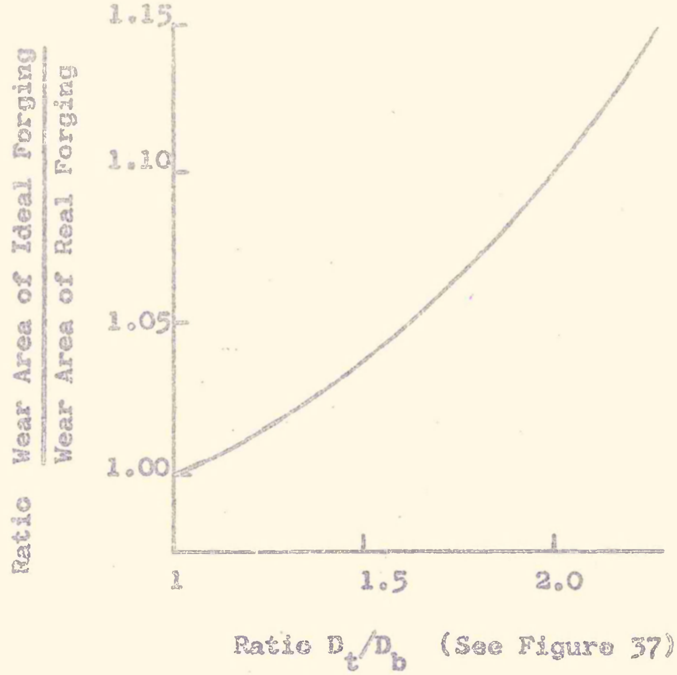


Figure 39

Influence of Non-Uniform Deformation on Sum of Top and Bottom Die Wear Areas

Figure 40

Typical Test Die After 2000 Forgings

3.2.5 Assessment of die wear

An apparatus developed to allow autographic recording of wear contours on test dies is shown schematically in Figure 42 (p. 64), whilst Figure 43 (p. 65) shows a photograph of the equipment.

Depth measurements along a diametral contour of the worn die were made by a differential transformer type displacement transducer, the output of which was fed via a potential divider, to a high speed strip chart recorder. The die was moved past the transducer on a carriage which was driven, through a gear system, by the chart drive motor of the recorder. This arrangement ensured that die movement and chart movement were strictly proportional.

The carriage on which the die was placed ran level on guide rails to within $\pm$ 0.0001 in. over the full length of travel.

A typical diametral contour trace from a worn die is shown in Figure 44 (p. 65).

To assess the wear on a die, eight such traces were made and the average of sixteen radial wear areas (shaded in Figure 44 p. 65) was used as an index of die wear. The units of wear index used were $in^2 \times 10^{-6}$, the wear areas being measured by a planimeter.

3.2.5.1 Reproducibility of wear measurement

The reproducibility of wear measurement was assessed as follows. Two dies were selected, one having a wear index of about 150 $in^2 \times 10^{-6}$ and the other a wear index of about 550 $in^2 \times 10^{-6}$.

Four diameters at 45° intervals round the die were selected independently by three different investigators. Each investigator then made three auto-

/graphic recordings

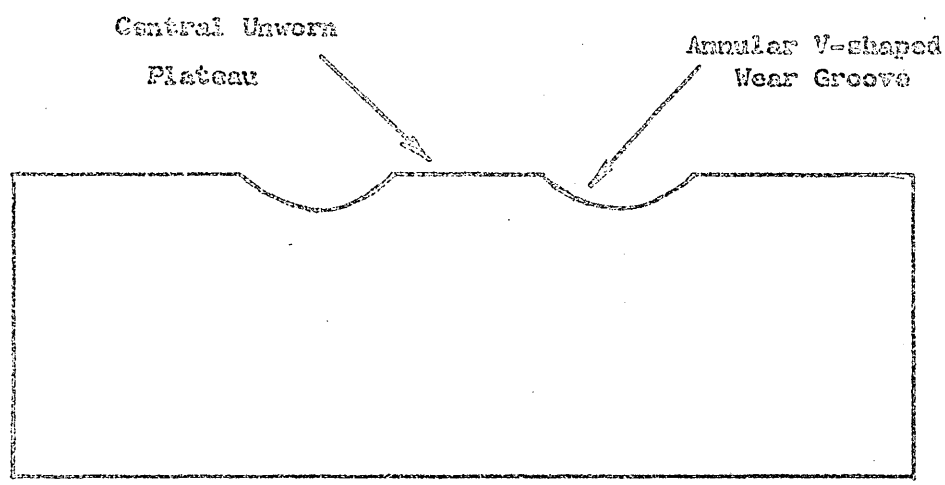


Figure 41

Schematic Illustration of Die Wear Contour

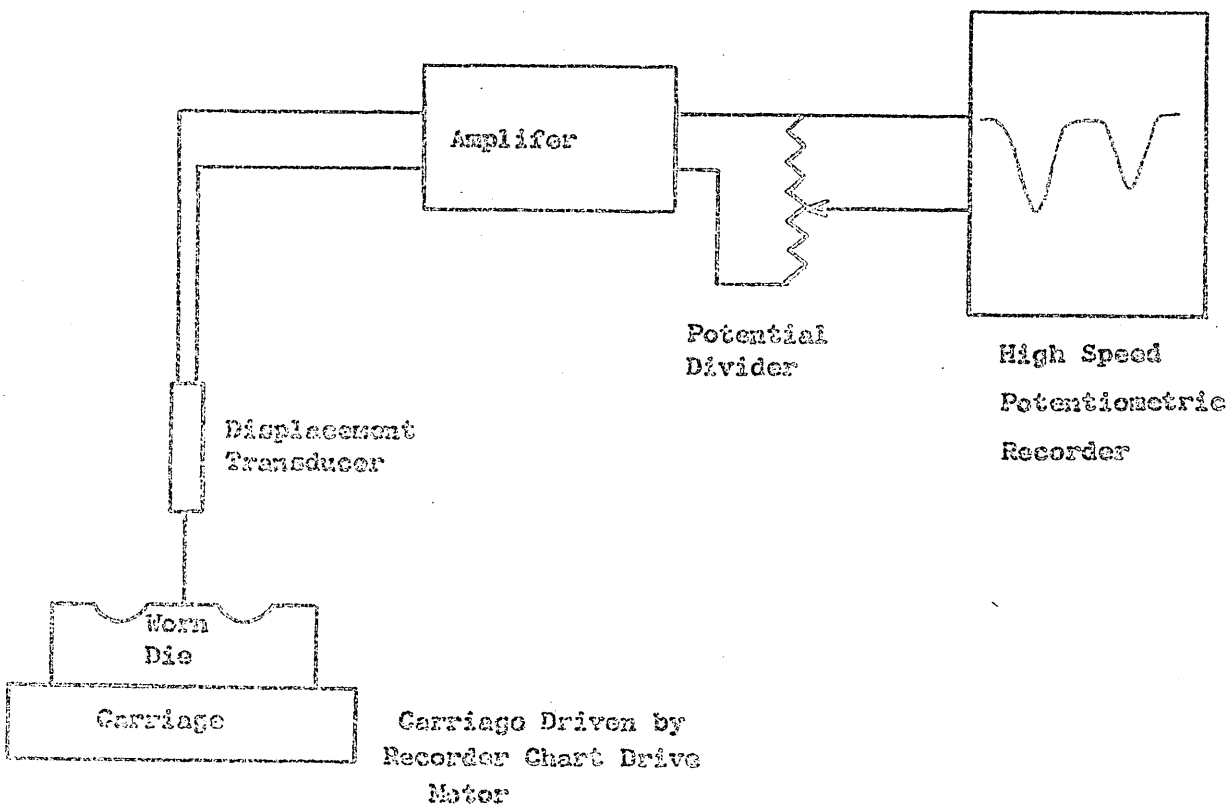


Figure 42

Schematic Diagram of Apparatus for Wear Measurement

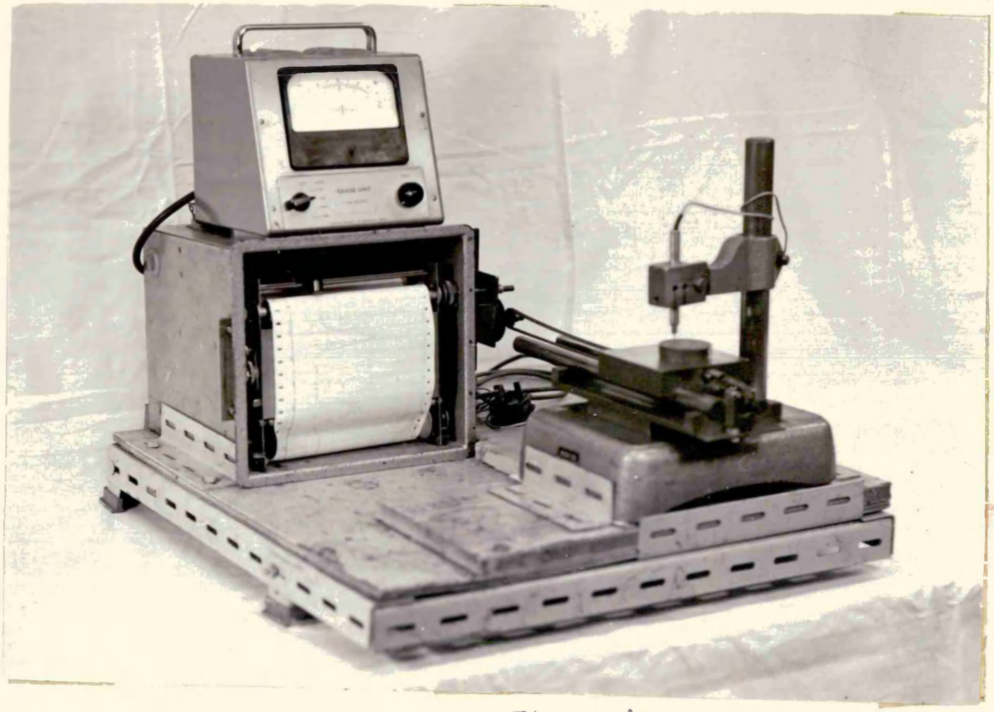


Figure 43

Photograph of Wear Measuring Equipment

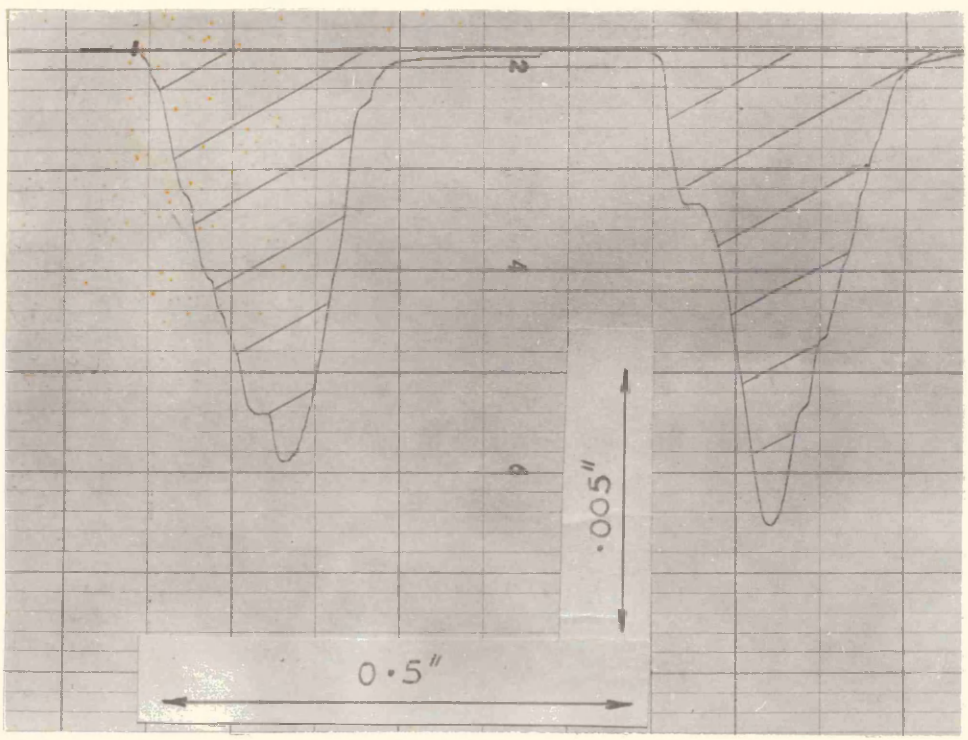


Figure 44

Typical Diametral Wear Contour

graphic recordings of each diametral wear contour to determine how accurately the measuring equipment would reproduce a given trace. The area under each radial trace was then determined in triplicate by each investigator on the diameters which he had traced.

Each investigator thus made nine assessments of the wear index of each die. The results obtained are shown in Table 14 below.

Table 14

Reproducibility of Die Wear Measurement

Die	Investigator	Wear Index									Mean Wear Index	Standard Deviation
		1	2	3	4	5	6	7	8	9		
1	A	147	147	151	152	151	153	150	155	159	152	4
1	B	148	154	150	146	148	153	153	154	156	151	4
1	C	152	151	149	145	148	147	150	143	148	148	3
2	A	563	561	559	566	566	567	539	534	532	554	15
2	B	562	562	559	569	565	565	535	536	534	554	15
2	C	546	578	564	548	558	563	534	555	543	554	11

All the values for die 1 show very close agreement with the highest and lowest values differing by only 9%, whilst the highest and lowest values for die 2 differ by 12%.

It was concluded from these measurements that die wear indices could be determined to an accuracy of about ± 5%.

3.3 Wear Tests

3.3.1 Materials selected for wear tests

As already stated, the objective of the present work was the introduction into the forging industry of new die materials which would reduce the contribution of die costs to the total production costs of forgings.

To achieve this objective two approaches were possible. The wear resistance of specially melted alloys could be studied in the hope of obtaining a relationship between alloy composition and wear resistance. Although some investigations were made along these lines, the approach has one serious practical drawback. Special steels for which only a small initial demand would exist are very expensive and it was felt, therefore, that such steels would be uneconomic until a large demand for them was created, which could take a considerable time.

To provide more immediate help to the drop forger, it was considered preferable, therefore, to study the wear resistance of a number of readily available potential die steels. Most of the materials tested had been suggested as suitable for use as hot work die steels.

Table 15 below, gives the chemical composition of all materials selected for testing. The identification number allotted to each alloy in Table 15 has been retained throughout the remainder of this thesis.

Table 15

Materials Selected for Die Wear Tests

Material and Identification Number	Composition															
	C	Si	Mn	Ni	Cr	Mo	W	V	Ti	Al	Co	Fe	Nb	N	B	Zr
1. Plain C Steel	.65	.3	.6													
2. No. 5 Die Steel	.6	.3	.6	1.5	.6	.25										
3. Cr Mn Ni Mo Steel	.5	.3	1.0	1.0	1.2	.6		.1								
4. En 40C	.4	.3	.6	.4	3.0	1.0		.2								
5. Mo Ni Cr V Steel	.2	.3	.6	2.5	1.0	3.0		.25								
6. Cr Mo W V Steel	.33	1.0	.3		5.0	1.5	1.5	.5								
7. Ni Cr Mo Steel	.1	.6	.6	4.8	3.9	3.0										
8. Cr Co Mo V W Steel	.33	.9	.6		3.0	3.0	1.0	1.0			3.0					
9. Cr Ni Mo V Steel	.1	.3	.7	2.4	12.0	1.8		.35						.05		
10. W Cr V Steel	.35	.2	.3		3.0		10.0	.5								
11. Cr Mn Ni N Steel	.5	.1	9.8	3.9	21.7									.5		
12. Cr Steel	.32	.3	.6		4.6											
13. Cr Mo Steel	.35	.3	.6		4.9	.57										
14. Cr Mo Steel	.35	.3	.6		9.5	.59										
15. Cr Mo Steel	.25	.3	.6		9.6	.56										
16. Nimonic 90	.1 max.	1.0 max.	1.0 max.	bal.	18 - 21				1.8 - 2.7	.5 - 1.8	2.0 max.	5.0				
17. Nimocast 713	.08 - .20	1.0 max.	1.0 max.	bal.	11 - 14	3.5 - 5.5			.25 - 1.85	5.5 - 6.5	bal.	5.0				
18. Inco 901	.02	.04	.2	43.4	12.4	5.7			2.9	.13		bal.				

Materials 1, 2, and 6 were selected for test because they were already used as forging dies, as described previously in section 1.2. Similarly, material 10 was selected since it is occasionally used for small inserts in the body of larger dies.

Although they are not extensively used, materials 3 and 5 are offered as die steels by a manufacturer of die blocks[3], and were, therefore, investigated.

Material 4 was selected for test because it is a cheap, readily available material, intermediate in alloy content between No. 5 Die Steel and the 5% Cr Mo W steel (No. 6) used for press dies.

Claims have been made[35] that material 8 has superior hot strength and better wear resistance than material 6, and the former steel was, therefore, included in the tests.

The results of early wear tests suggested a possible correlation between the chromium content of a die steel and its wear resistance. Because of this, it was decided to investigate the wear resistance of high chromium steels, and materials 9 and 11 were selected as commercially available steels.

Material 7 was tested to investigate whether low carbon, chromium-molybdenum steels would be sufficiently wear resistant for use as forging dies. The advantage of low carbon content is the improvement obtained in toughness.

The nickel based alloys (materials 16 to 18) were included because such alloys are commonly used as extrusion dies, in which application they have been shown to outperform high alloy steels. These alloys have

/also been

also been reported to be superior to chromium and tungsten hot work die steels for producing brass stampings[28].

Materials 12 to 15 inclusive were experimentally melted steels included specifically to investigate the influence of carbon, molybdenum, and chromium contents on wear resistance in the absence of other elements.

In addition to the materials listed in Table 15, a series of carbon free iron-chromium alloys was tested to investigate the influence of chromium on wear resistance in the absence of carbon. These alloys contained chromium contents up to 13% chromium.

3.3.2 Wear test results

Wear tests were made on the materials listed in Table 15 by forging 2000 mild steel slugs $\frac{1}{2}$ in. diameter x $\frac{3}{4}$ in. long down to discs 0.200 in. thick at 1100°C.

Figures 45 to 60 inclusive (pp. 71 to 74) show, for each material, wear index plotted as a function of the initial die hardness. The lines drawn through the points in these figures are lines showing the regression of wear index upon initial die hardness, determined by the method of least squares.

Table 16 shows the regression equations obtained for each material tested.

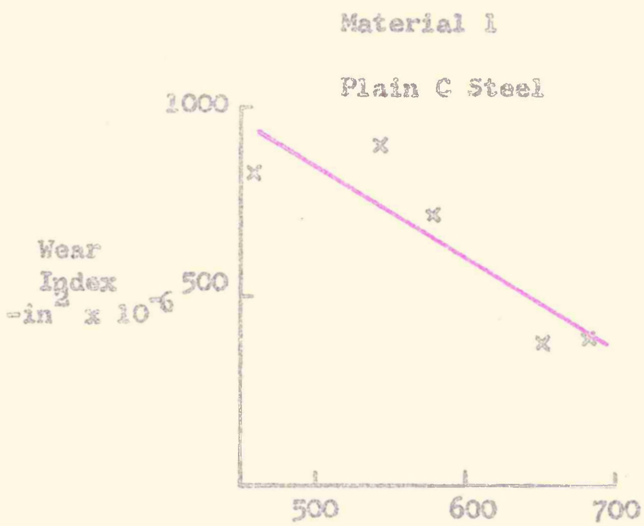


Figure 45

Figure 46

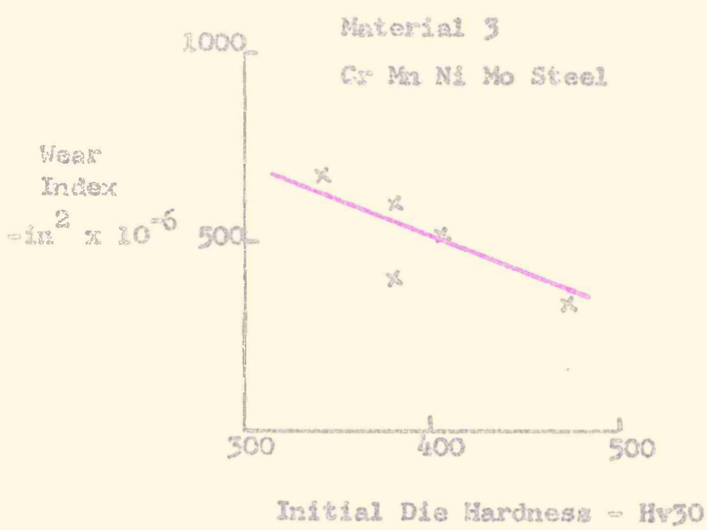


Figure 47

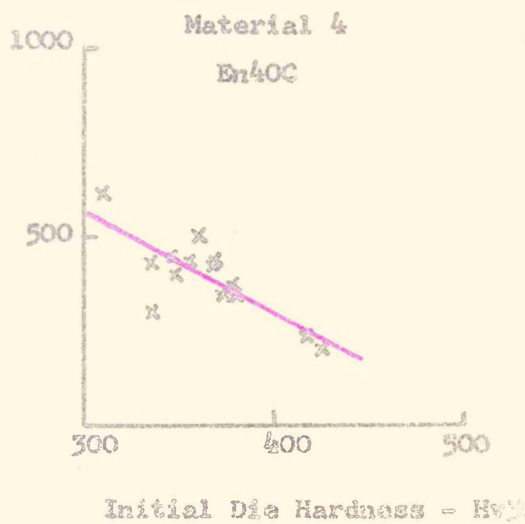


Figure 48

Wear Test Results

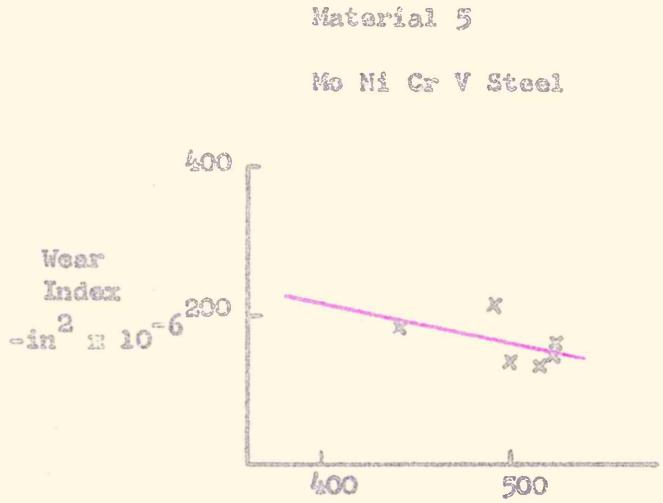


Figure 49

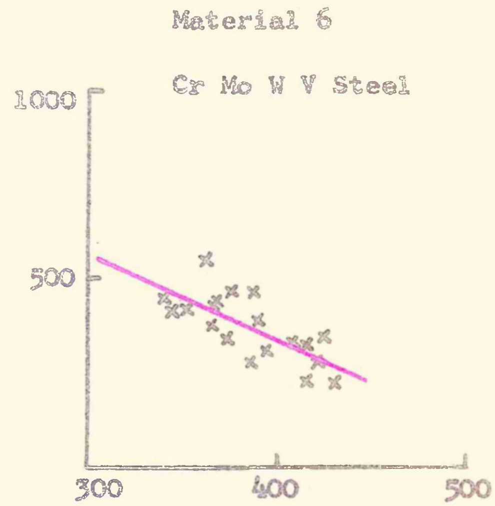


Figure 50

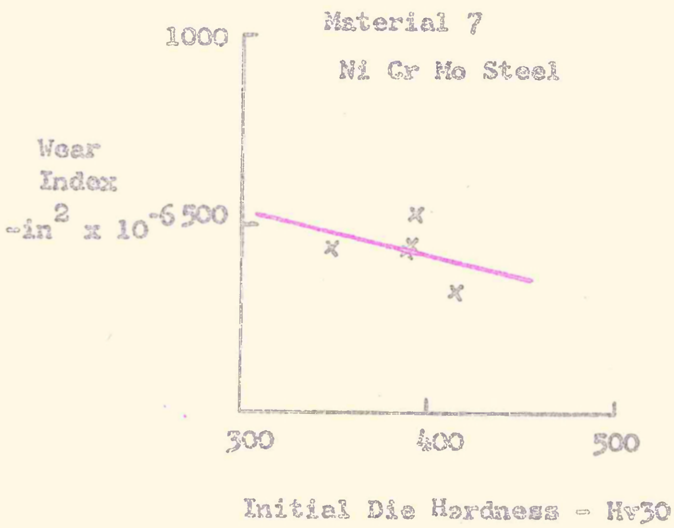


Figure 51

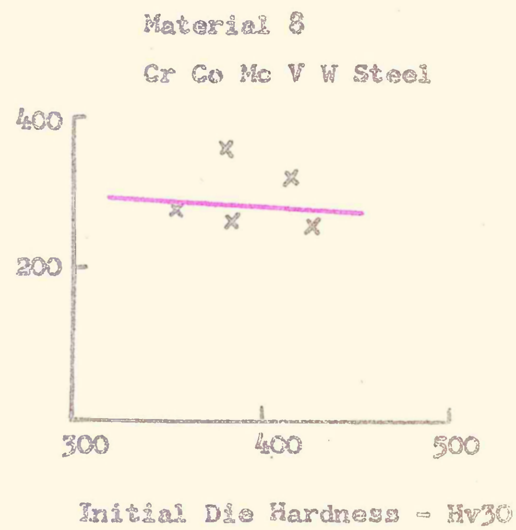


Figure 52

Wear Test Results

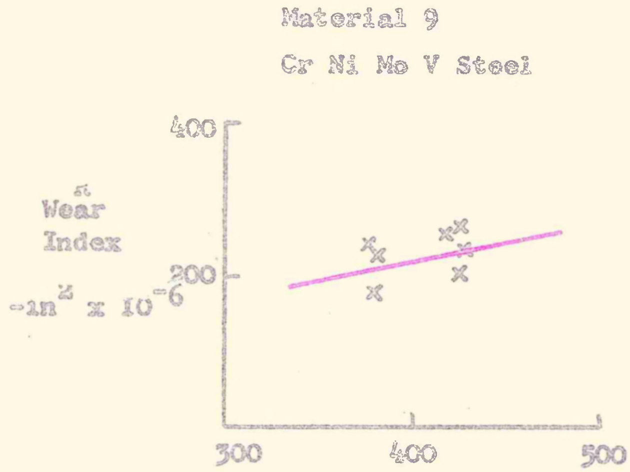


Figure 53

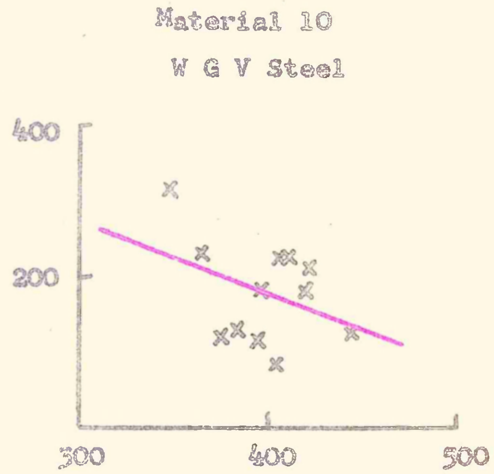


Figure 54

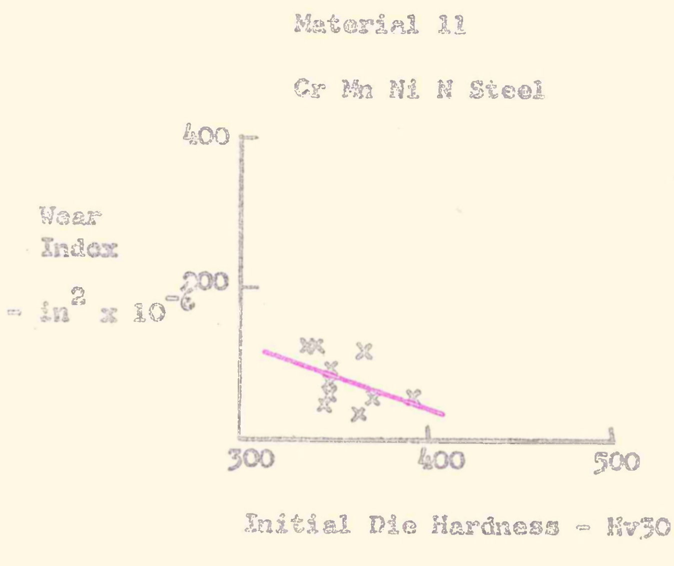


Figure 55

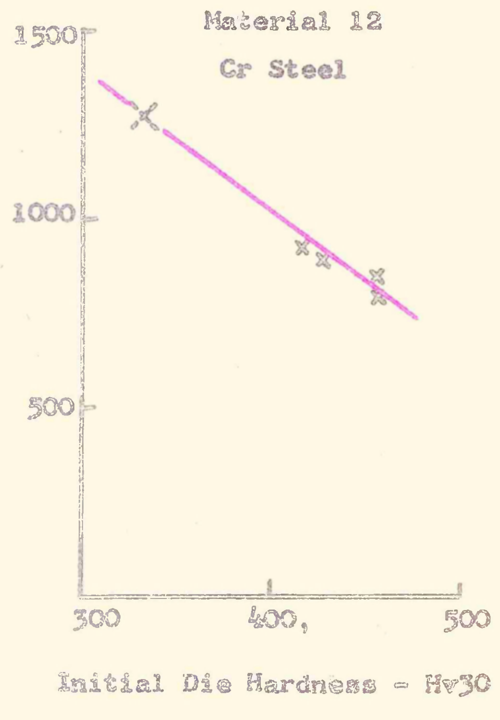


Figure 56

Wear Test Results

Material 13
Cr Mo Steel

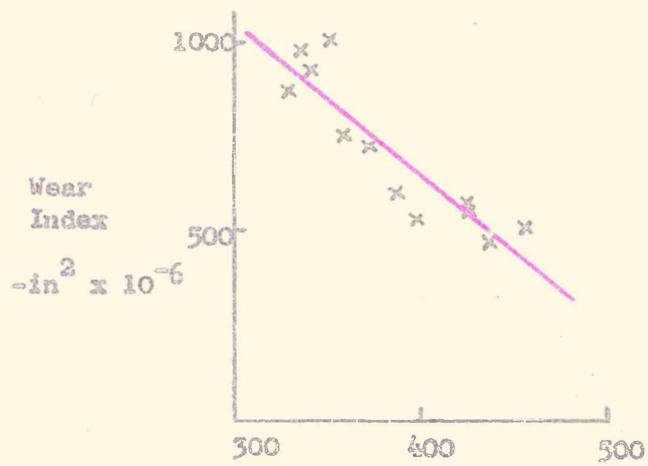


Figure 57

Material 14
Cr Mo Steel

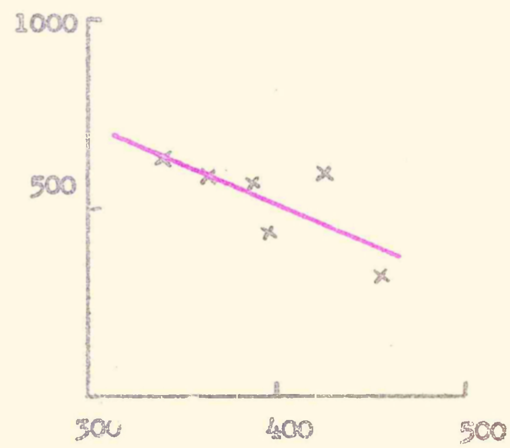
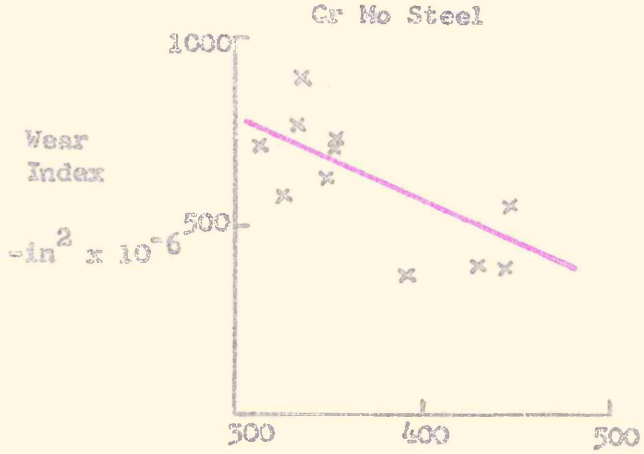


Figure 58

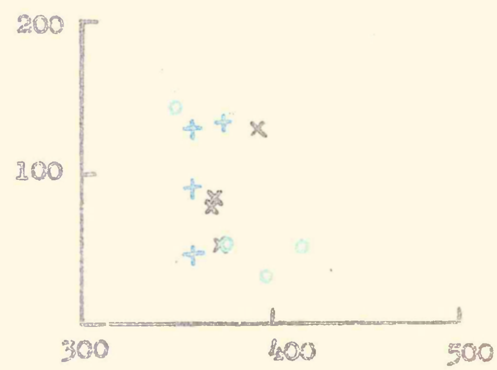
Material 15
Cr Mo Steel



Initial Die Hardness - Hv30

Figure 59

Materials 16, 17, and 18
Nimonic 90 x
Nimocast 713 o
Inco 901 +



Initial Die Hardness - Hv30

Figure 60

Wear Test Results

Table 16

Equations for Regression of Wear Index on Initial Die Hardness

Material	Regression Equation (W = wear index H = initial die hardness - Hv30)
1	W = 2033 - 9.44 H
2	W = 3073 - 6.03 H
3	W = 1380 - 2.17 H
4	W = 1391 - 2.73 H
5	W = 421 - 0.51 H
6	W = 1249 - 2.27 H
7	W = 882 - 1.16 H
8	W = 337 - 0.14 H
9	W = 21 + 0.51 H
10	W = 558 - 0.95 H
11	W = 380 - 0.85 H
12	W = 2510 - 3.74 H
13	W = 2291 - 4.11 H
14	W = 1358 - 2.12 H
15	W = 1442 - 2.17 H

The wear tests made on the iron-chromium alloys were performed on as forged material without any heat treatment.

The results obtained on these alloys are shown in Table 17 below.

Table 17

Wear Test Results on Iron-Chromium Alloys

Composition	Initial Hardness Hv30	Wear Index
Fe - 5% Cr	181/185	1018, 956
Fe - 9% Cr	241/292	577, 639, 645
Fe - 13% Cr	124/153	338, 406

The forging conditions for these tests were non-standard, as described in section 5.1.10

3.3.3 The influence of stock temperature on die wear

All the wear tests described in section 3.3.2 were made with a forging temperature of 1100°C. This temperature was selected as being typical of the temperature of a forging when it is presented to the final impression in a forging die.

Tests were made to investigate the influence of forging temperature on die wear for the following reasons:

(1) Stock temperature is a forging variable which can be controlled under production forging conditions, and therefore, a knowledge of its influence on die wear will indicate whether improvements in die life are possible by control of the forging temperature.

(2) There is widespread interest in the possibility of producing components by "warm forging" and a knowledge of the likely effects on die

/life

life of changing from "hot forging" to "warm forging" would be valuable in assessing the likely economics of "warm forging".

Wear tests were made, therefore, using No. 5 Die Steel dies, with forging temperatures between 900°C and 1250°C. 1250°C was selected as the upper limit since drop forgings are not heated beyond this temperature due to the possibility of overheating occuring.

It was initially hoped to carry out trials below 900°C, but experience showed that, with the press used for the tests, insufficient load was available to allow forging at lower temperatures.

The results of two series of tests are shown below in Table 18, and also in Figure 61 (p. 78).

Table 18

Influence of Stock Temperature on Die Wear

Material	Die Hardness Hv30	Forging Temperature °C	Wear Index in^2 x 10^{-6}	Wear Index Corrected to Hv = 334
1. No. 5 Die Steel	332	950	1172	-
2. No. 5 Die Steel	310	950	907	-
3. No. 5 Die Steel	343	1050	1461	-
4. No. 5 Die Steel	333	1050	1107	-
5. No. 5 Die Steel	340	1150	531	-
6. No. 5 Die Steel	335	1150	526	-
7. No. 5 Die Steel	336	1250	481	-
8. No. 5 Die Steel	339	1250	356	-
9. No. 5 Die Steel	355	900	789	918
10. No. 5 Die Steel	359	900	584	680
11. No. 5 Die Steel	355	1200	443	516
12. No. 5 Die Steel	359	1200	247	287

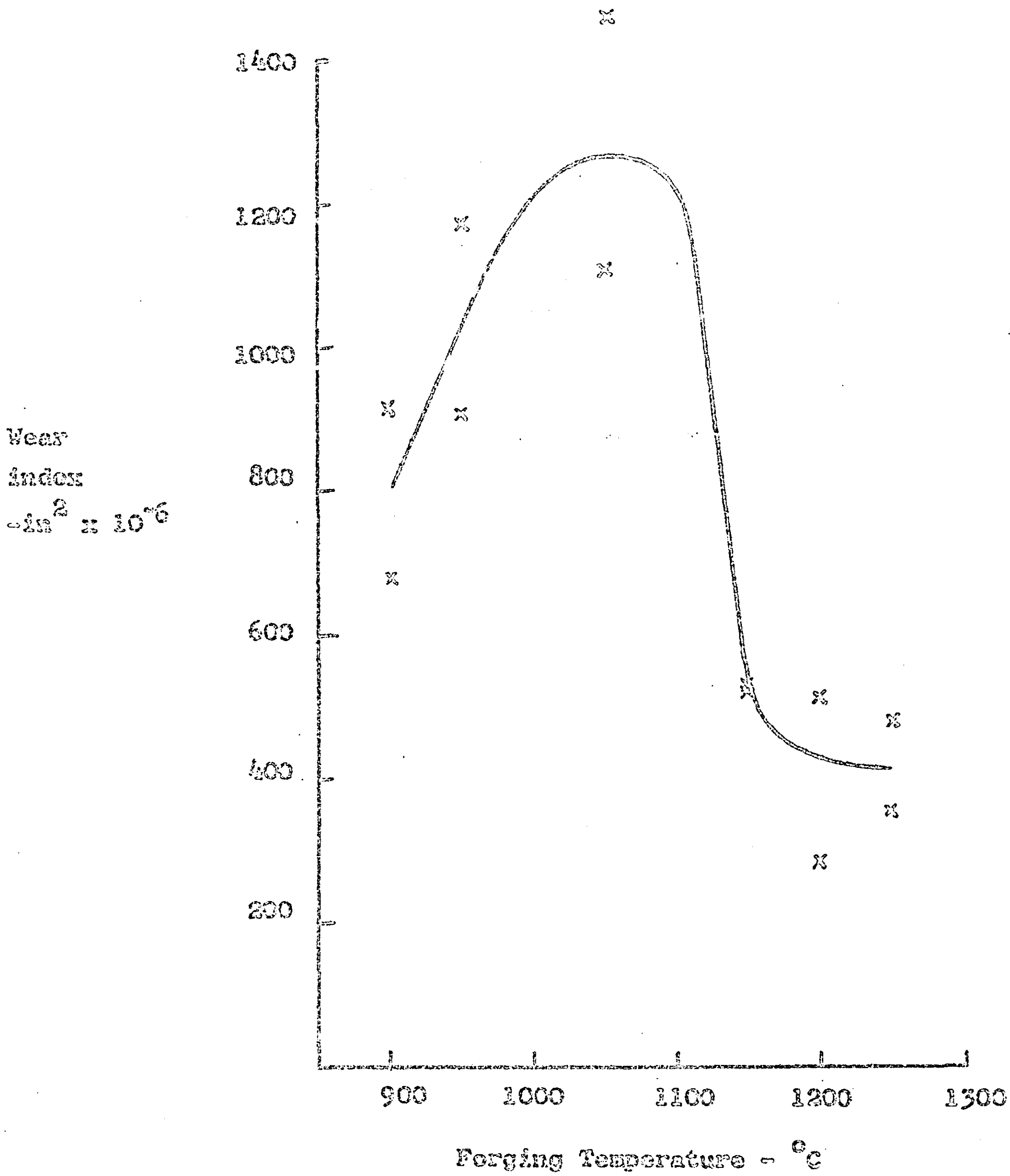


Figure 61

Influence of Stock Temperature on Die Wear

Test dies 9 - 12 were harder than dies 1 - 8, and since hardness has a considerable influence on the wear of No. 5 Die Steel (see Figure 46 p. 71) a correction has been made to the wear index of dies 9 - 12 so that they can be compared with dies 1 - 8. The mean hardness of dies 1 - 8 was 334 Hv30 and that of dies 9 - 12 was 357 Hv30.

Figure 46 (p. 71) shows that the wear index for No. 5 Die Steel at 334 Hv30 is 16% higher than at 357 Hv30.

The wear index of dies 9 - 12 has been increased, therefore, by 16%, and this corrected wear index is plotted in Figure 61 (p. 78).

3.3.4 Influence of stock material on die wear

All the tests described so far were made using mild steel as the forging stock. This material was selected as the standard stock, since the majority of drop forgings are made from either mild steel or plain carbon steels.

In addition, the low price of mild steel commended its use, since a lot of steel was used during the tests.

Drop forgings are, however, made from both alloy and stainless steels, and it was considered necessary, therefore, to investigate the influence of forging stock on die wear.

Wear tests were made on three die materials (numbers 4, 6, and 9 in Table 15), using En 24 and En 57 as the forging stock.

The chemical composition of En 24 and En 57 is shown in Table 19.

Table 19

Composition of En 24 and En 57

Material	Composition					
	C	Si	Mn	Ni	Cr	Mo
En 24	.35/.45	.1/.35	.45/.75	1.3/1.8	.9/1.4	.2/.35
En 57	≤ .25	.1/1.0	≤ 1.0	1.0/3.0	15.5/20.0	-

The results of these tests are shown in Figure 62 (p. 81).

3.3.5 The influence of surface treatment on die wear

3.3.5.1 The influence of nitriding on die wear

Dies for forging presses are sometimes nitrided to increase their wear resistance. Invariably, the die material which is nitrided is the 5% Cr Mo W die steel (material 6) and there appear to have been no investigations into the suitability of other materials.

In the present investigations, the influence of nitriding on the wear resistance of three die materials (materials 4, 6, and 9) was investigated. Material 4 was investigated because it is cheaper than material 6 (£222 per ton compared with £365 per ton). Material 9 was investigated to see whether its improved wear resistance compared with materials 4 and 6 was maintained in the nitrided condition.

Hardened and tempered dies were nitrided for 80 hours in a cracked ammonia atmosphere. Figure 63 (p. 84) shows the case depth produced in each material.

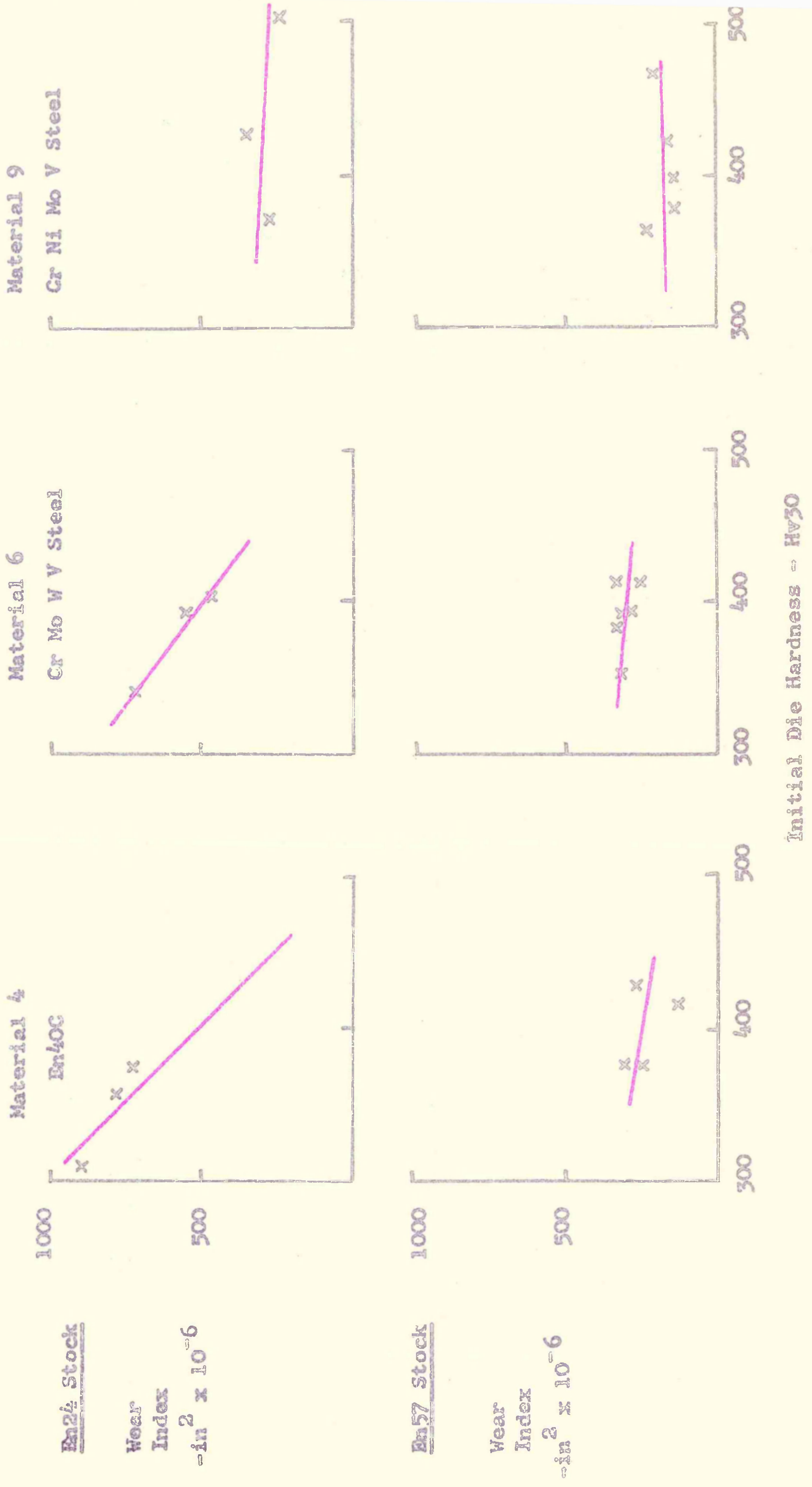


Figure 62

Influence of Forging Stock on Die Wear

Table 20 shows the surface hardness produced by nitriding, and also the surface hardness at the centre of the dies after testing.

Table 20

Hardness of Nitrided Dies Before and After Testing

Material	Surface Hardness - Hv30		
	Before Nitriding	After Nitriding	After Testing
4. En 40C	396 - 424	779 - 950	780 - 790
6. Cr Mo W V Steel	364 - 388	1063 - 1108	763 - 883
9. Cr Ni Mo V Steel	390 - 419	861 - 966	635 - 810

After the normal test procedure of forging 2000 mild steel slugs, no wear could be detected on the nitrided dies. A further 2000 slugs were forged, therefore, but even then no wear was measurable.

3.3.5.2 Influence of Sulfinuz treatment on die wear

The Sulfinuz treatment process is a surface treatment method in which the surface of the treated material is simultaneously impregnated with nitrogen and sulphur.

Parts to be treated are immersed in a molten bath containing cyanides and sulphides. The treatment is carried out at about 570°C, and lasts for periods up to 3 hours.

Three No. 5 Die Steel dies were subjected to the Sulfinuz treatment after hardening and tempering. The dies were then tested in pairs with untreated dies of the same initial hardness as possessed by the treated dies.

The results of these tests are shown in Table 21.

Table 21

Wear Tests on Sulfinuz Treated Dies

Die No.	Treated	Original Hardness Hv30	Hardness After Treatment Hv30	Wear Index $in^2 \times 10^{-6}$
193	Yes	358	400	101
177	No	359	-	632
192	Yes	356	390	200
220	No	363	-	603
226	Yes	372	436	51
238	No	411	-	379

After testing, the Sulfinuz treated dies showed a pronounced network of thermal fatigue cracks not normally encountered in No. 5 Die Steel. Figure 64 (p. 84) shows treated and untreated dies after testing.

3.3.6 The influence of lubrication on die wear

A possible method of improving the life of forging dies is by the use of die lubricants.

Most investigations of forging lubricants[36, 37, 38] under hot forging conditions have been concerned with measurements of the coefficient of friction and the influence of lubrication on the load and energy requirements when making a forging.

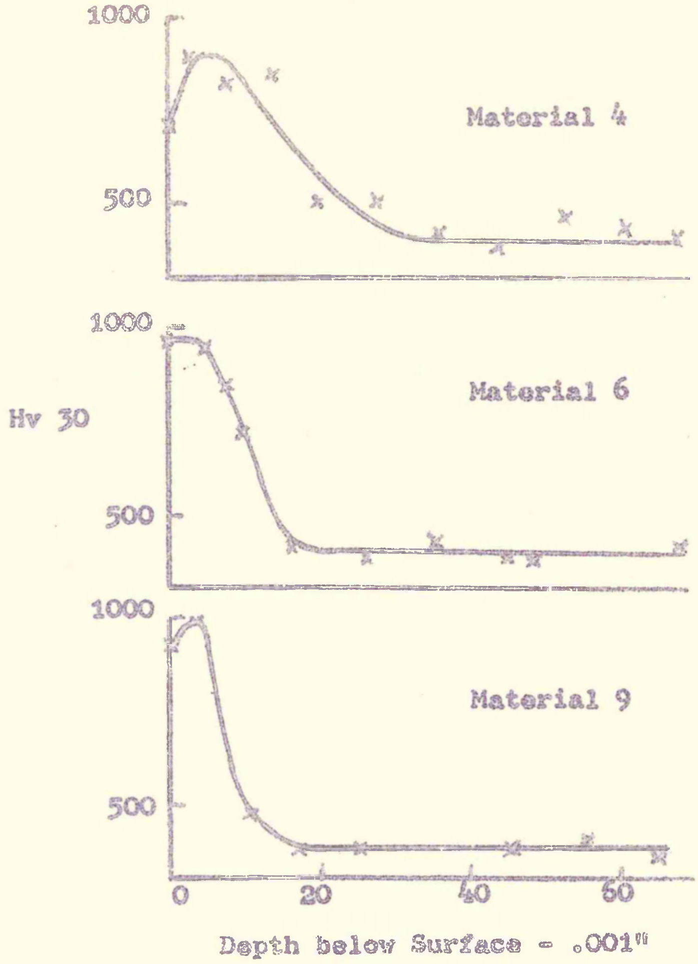


Figure 63

Case Depth in Nitrided Dies

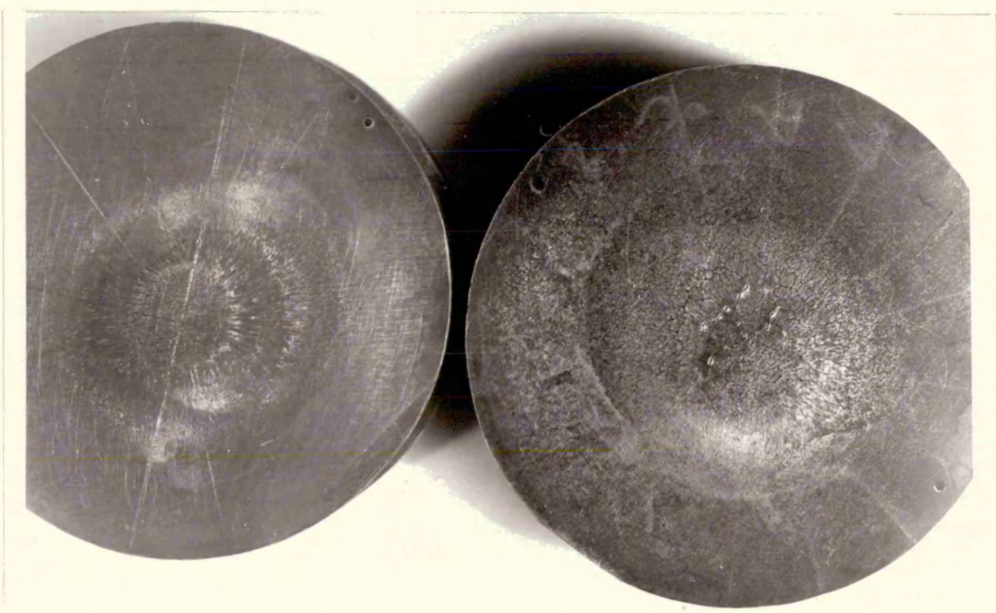


Figure 64

Untreated (left) and Sulfinuz Treated Dies After Test

Only one report[39] has been found which describes the influence of lubrication on die life. In this investigation, the life of two dies, used to produce connecting rods, one unlubricated and the other lubricated with colloidal graphite, were compared.

The unlubricated die produced 11,900 forgings, whilst the lubricated die produced 22,500.

Reference to section 1.3 of this thesis will show that such a small amount of testing of the effect of lubrication is inconclusive. In addition it was not clear in the reported investigation whether the use of a lubricant reduced erosive wear or deformation of the die.

It was decided, therefore, to carry out tests aimed specifically at studying the effect of lubrication on die wear.

In drop forging, the term "lubricant" is used rather loosely to refer to any material thrown, swabbed, or sprayed on to dies to assist the forging process. The most common lubricants used are sawdust, oil, and dispersions of colloidal graphite. It is generally agreed that only graphite exerts a true lubricating effect; the function of oil and sawdust being to aid descaling of the forging and release of the forging from the die by an explosive action.

In the present investigations, only colloidal graphite was used. The effect of lubrication on die wear was studied as follows.

A test die of No. 5 Die Steel was placed in a special die holder which allowed the die to be preheated to 150°C by means of a mineral-insulated heating coil wound in the die holder.

After preheating to 150°C, 1000 mild steel slugs were forged with wear measurements being made after 500 and 1000 forgings.

The dies were then coated with graphite by swabbing at 150°C and subsequently polished with rag. A further 1000 forgings were then made with the dies preheated to 150°C, and sprayed with graphite in water after the completion of every forging. Wear measurements were made after 1500 and 2000 forgings.

All traces of graphite were then removed from the die by scrubbing in water, and a further 1000 forgings made without lubrication. A further 1000 forgings were then made using lubrication.

The results of two tests made in this manner are shown in Table 22 and Figure 65 (p. 87). Because of difficulties in spraying the top die, the test dies remained in the bottom die position throughout the tests, the top dies being blanks on which no wear measurements were made. Due to a mistake in the setting of the press, all forgings made in the investigations of lubrication were forged to a thickness of 0.210 in. instead of the usual 0.200 in.

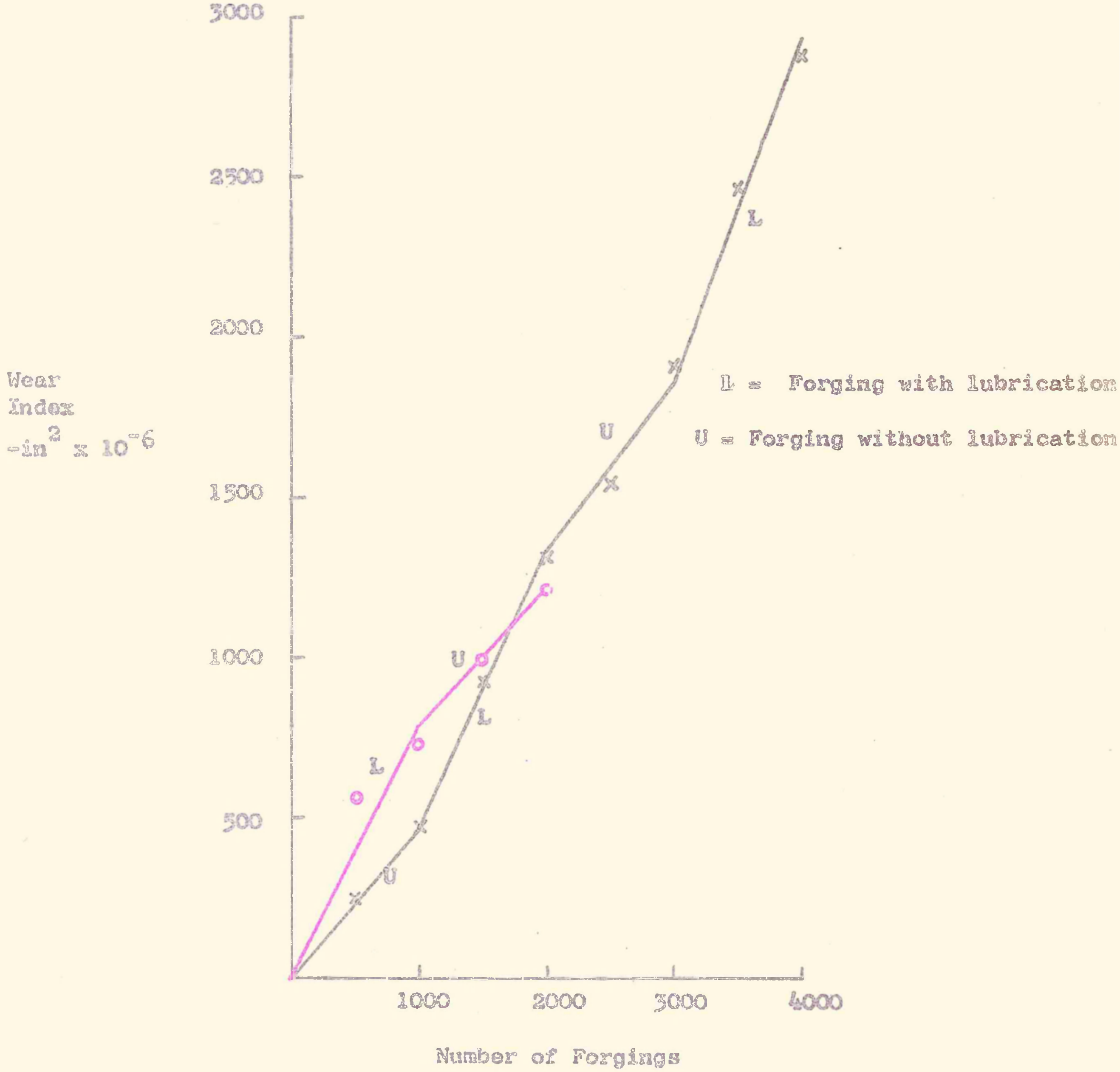


Figure 65

Wear v Number of Forgings with and without Lubrication

Table 22

Influence of Lubrication on Die Wear

Test No.	Die Hardness Hv30	No. of Forgings	Lubricated During Last 500 Forgings	Wear Index $in^2 \times 10^{-6}$
1	379	500	No	252
		1000	No	474
		1500	Yes	928
		2000	Yes	1309
		2500	No	1543
		3000	No	1905
		3500	Yes	2458
		4000	Yes	2880
2	384	500	Yes	565
		1000	Yes	730
		1500	No	994
		2000	No	1216

3.4 Tempering Resistance of Materials Investigated

Tempering curves were determined for the die steels investigated, and are shown in Figures 66 - 68 (pp. 89 - 91). No curves are shown for the Nickel based alloys or material 11, since these are age-hardening materials. All the tempering curves shown in Figures 66 - 68 were determined for a tempering time of two hours.

Since, in use, an already tempered die is subjected to further tempering by heat transferred from the forging, additional tempering tests were made on materials 2, 4, 6, and 9. These materials were chosen as representing steels with poor, intermediate, and good wear resistance. Samples of these materials were initially hardened and tempered to 450, 400, and 350 Hv30 and then subjected

/to further

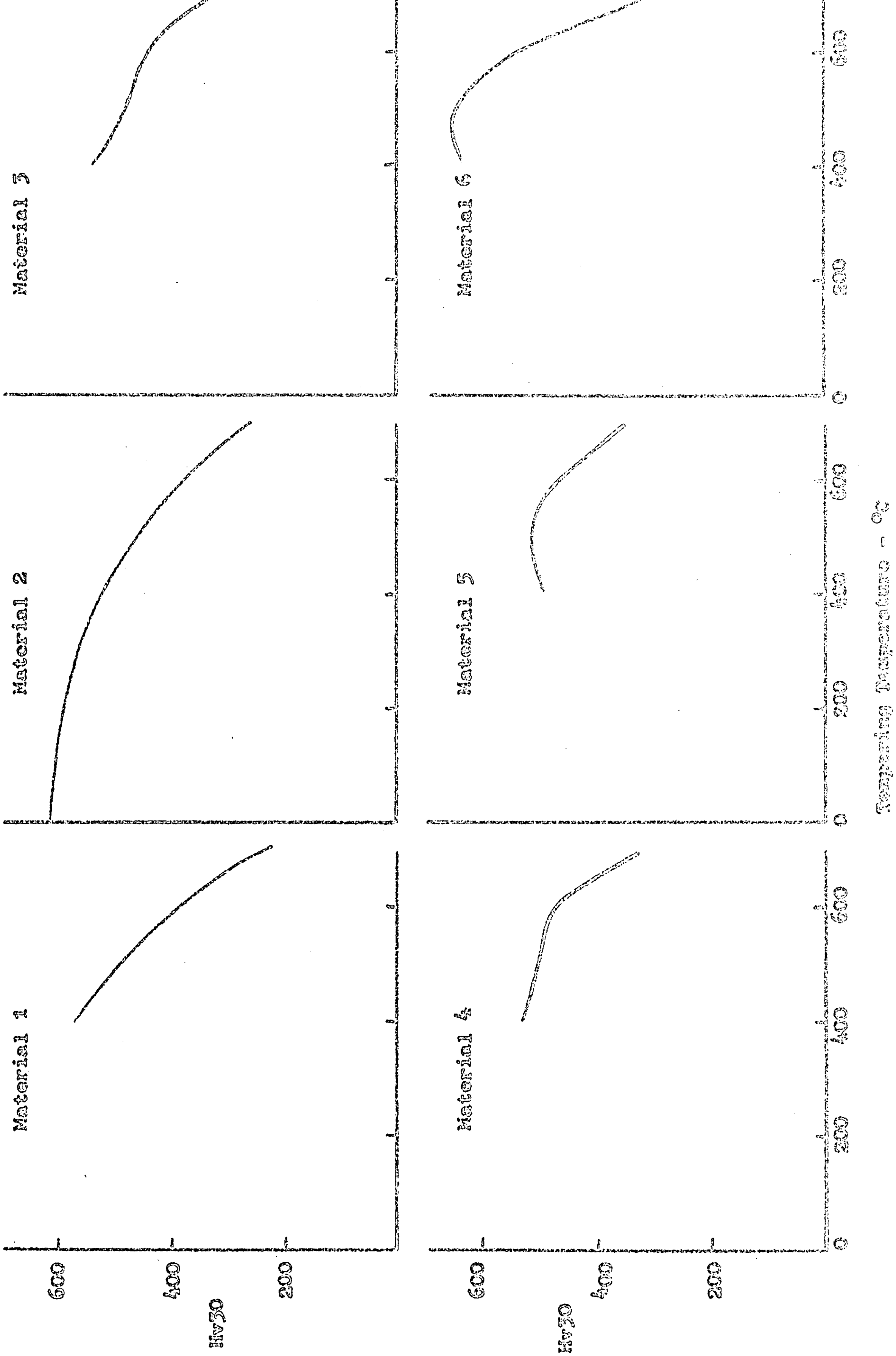


__Figure 66__

Tempering Curves for Die Materials

<u>Figure 67</u>

Tempering Curves for Die Materials

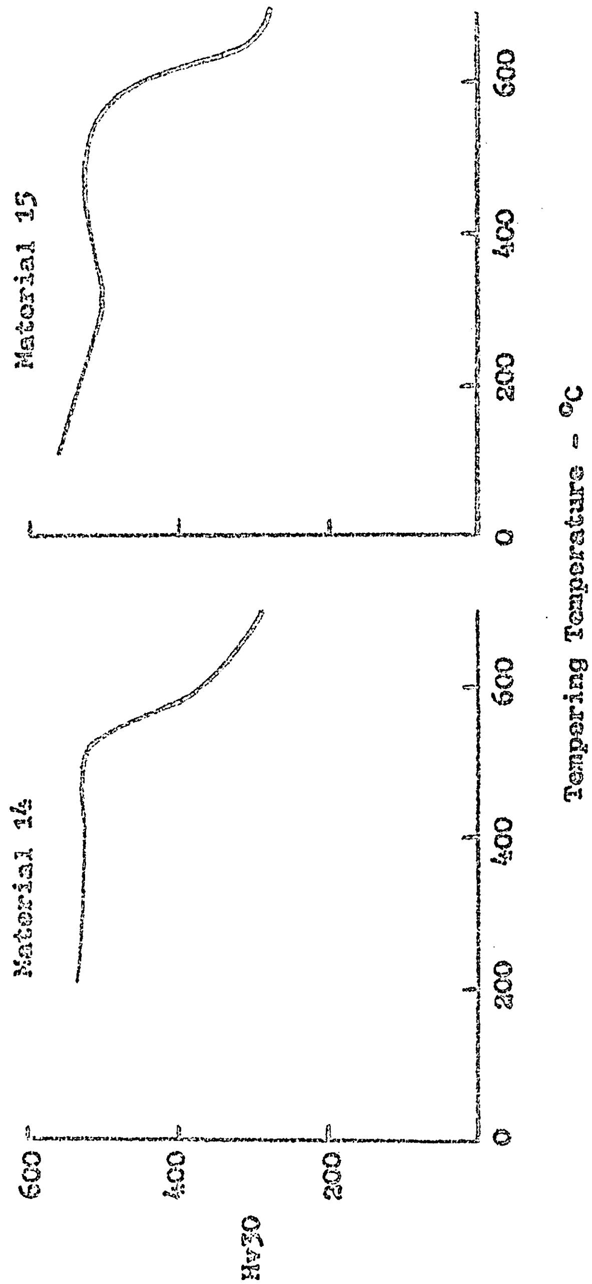


Figure 68

Tempering Curves for Die Materials

to further tempering treatments at 650, 700, and 750°C for periods between ten minutes and two hours.

The results of these tests are shown in Table 23 and Figures 69 - 72 (pp. 94 - 97).

Table 23

Hardness of Die Materials After Retempering

Material	Initial Hardness Hv30	Tempering Temperature °C	Hardness After Tempering for Indicated Time - mins.			
			10	30	60	120
2	447	650	441	424	400	387
2	458	700	366	344	320	291
2	451	750	495	422	536	826
2	396	650	375	388	370	361
2	397	700	355	342	327	301
2	395	750	811	464	635	833
2	357	650	359	366	362	351
2	355	700	345	324	315	304
2	352	750	632	404	595	801
4	445	650	429	427	423	397
4	441	700	365	340	316	291
4	450	750	299	286	271	259
4	398	650	400	396	388	377
4	403	700	365	343	321	296
4	393	750	278	271	270	247
4	329	650	321	318	319	315
4	324	700	300	299	295	281
4	329	750	300	287	270	274

Table 23 continued

Material	Initial Hardness Hv30	Tempering Temperature °C	Hardness After Tempering for Indicated Time - mins.			
			10	30	60	120
6	453	650	441	448	433	393
6	443	700	403	375	347	310
6	445	750	317	307	291	285
6	411	650	420	428	408	387
6	406	700	388	371	341	307
6	407	750	320	302	295	286
6	341	650	337	345	341	327
6	353	700	364	353	329	320
6	350	750	300	287	273	287
9	460	650	457	419	379	364
9	459	700	352	346	334	320
9	450	750	326	316	292	293
9	393	650	397	397	372	352
9	399	700	351	341	339	326
9	393	750	343	316	303	300
9	364	650	371	369	361	367
9	355	700	351	341	331	325
9	367	750	341	325	308	309

3.5 Changes in Die Hardness during Wear Testing

A number of investigations was made to study the tempering of dies which occurred during testing.

Material 2 (No. 5 Die Steel)

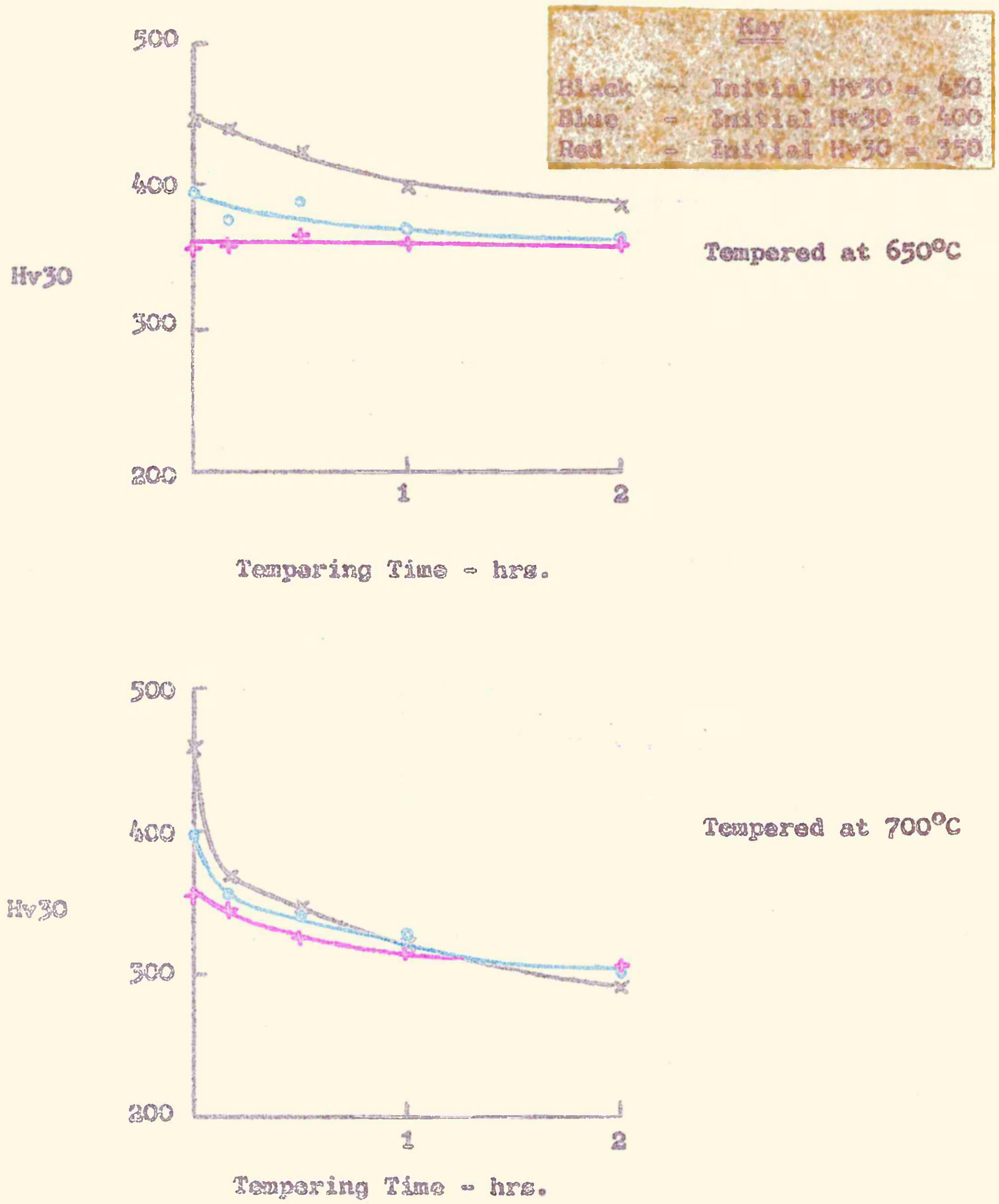


Figure 69

Retempering Curves for Material 2

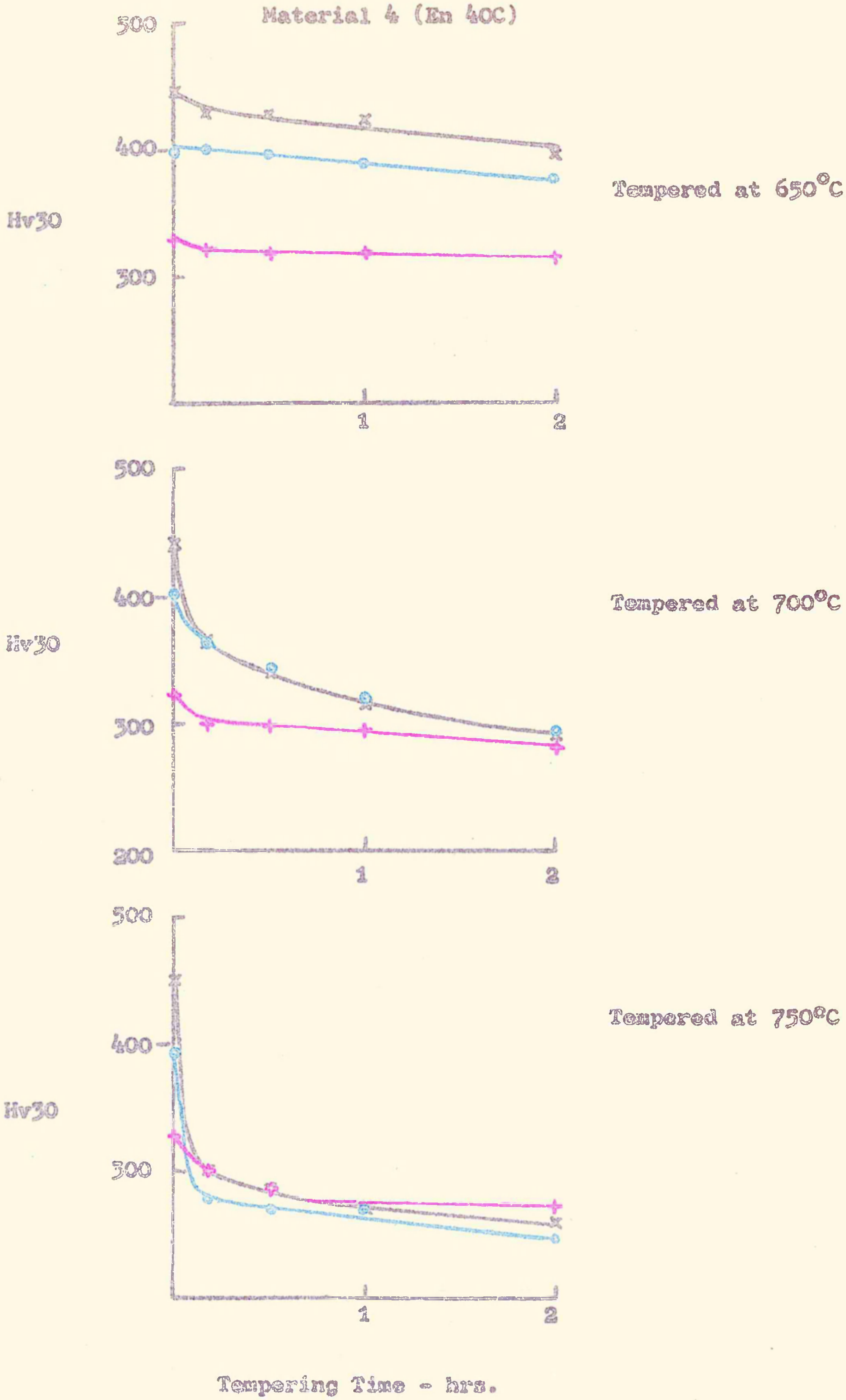


Figure 70

Retempering Curves for Material 4

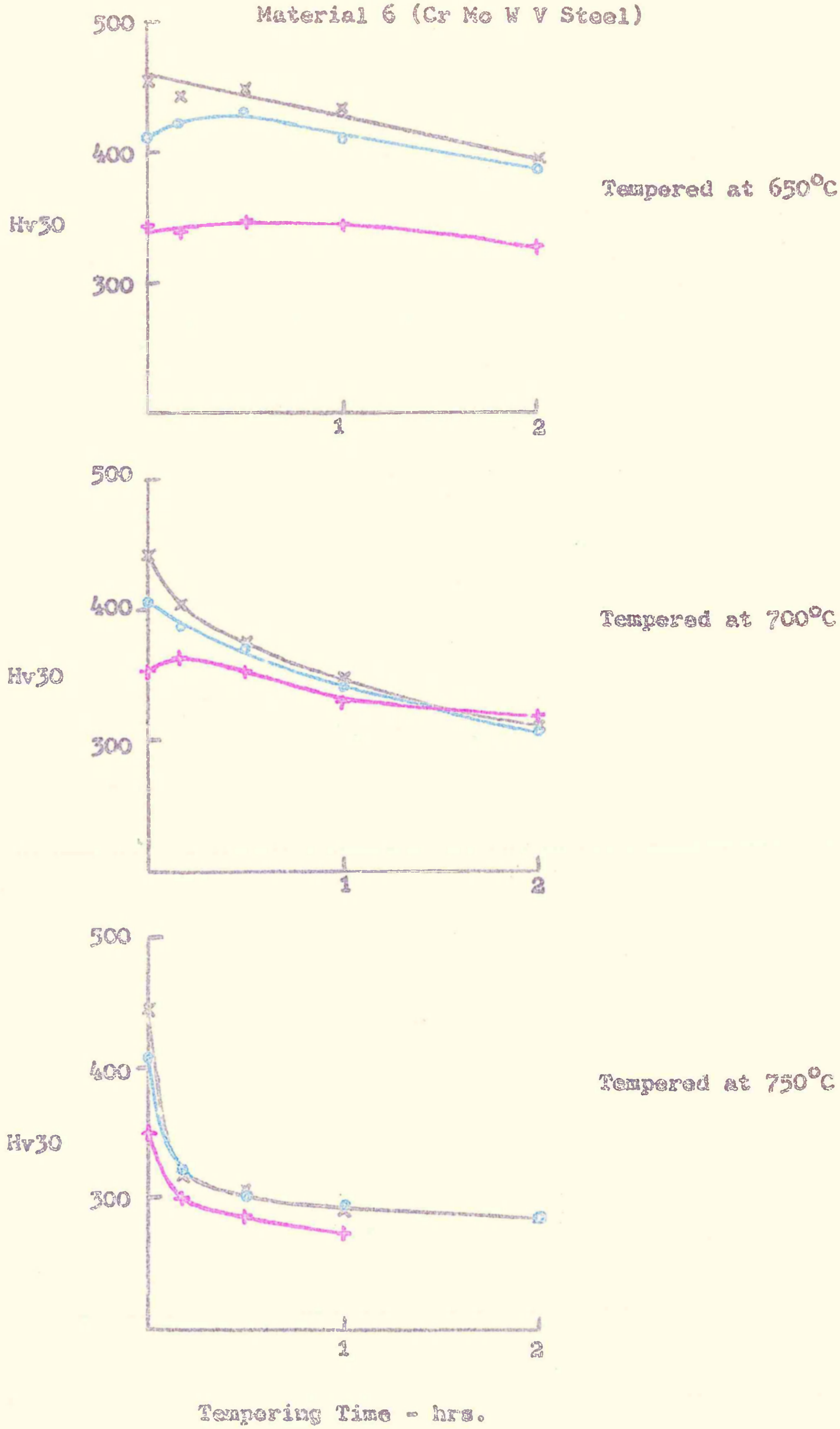


Figure 71

Retempering Curves for Material 6

Material 9 (Cr Ni Mo V Steel)

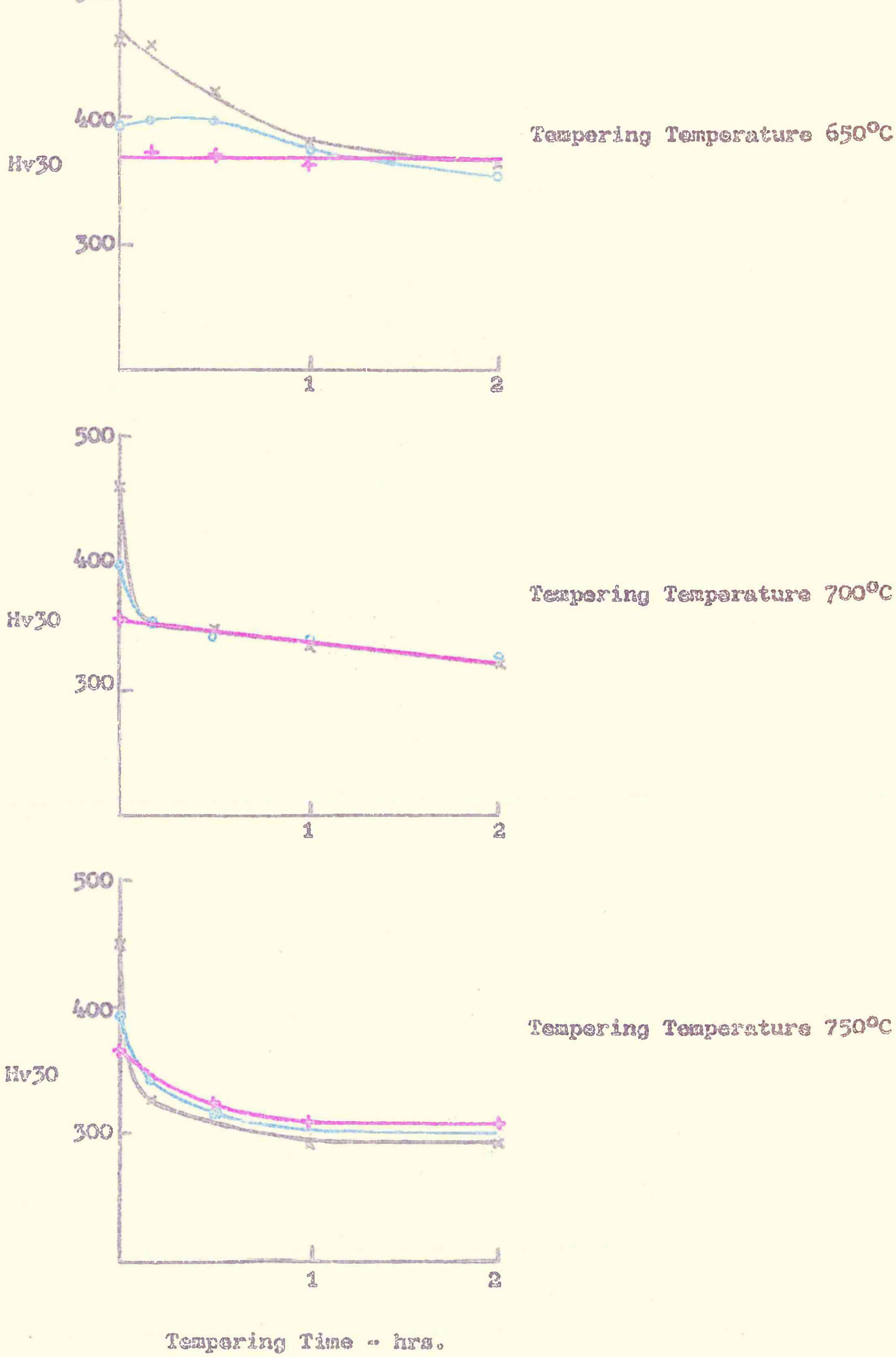


Figure 72

Retempering Curves for Material 9

To investigate the extent and rate of tempering, the hardness changes in dies were followed during the early stages of testing. Hardness measurements were made in the central unworn plateau, in the wear annulus, and at the periphery of dies. Figure 73 (p. 99) shows the results of such hardness measurements on two typical dies.

Figure 74 (p. 100) shows the hardness distribution across a radius of a die after the completion of 50 forgings.

Hardness tests were also made after the completion of testing on some dies. Figure 75 (p. 100) shows the relationship between initial and final die hardness for these dies, whilst Figure 76 (p. 101) gives a more detailed picture of the same relationship for materials 2, 4, 6, and 9.

3.6 Mechanical Properties of Materials Tested

The mechanical properties of greatest interest in connection with hot work die steels are hot strength and toughness. Hot tensile tests and Charpy V-notch impact tests were made, therefore, on the four materials. selected for detailed investigation (i.e. materials 2, 4, 6, and 9).

The hot tensile tests were made on a Hounsfield Tensometer machine using the specimens shown in Figure 77 (p.102).

The results of the mechanical tests are given in Table 24, and Figures 78 and 79 (pp.105 and 106).

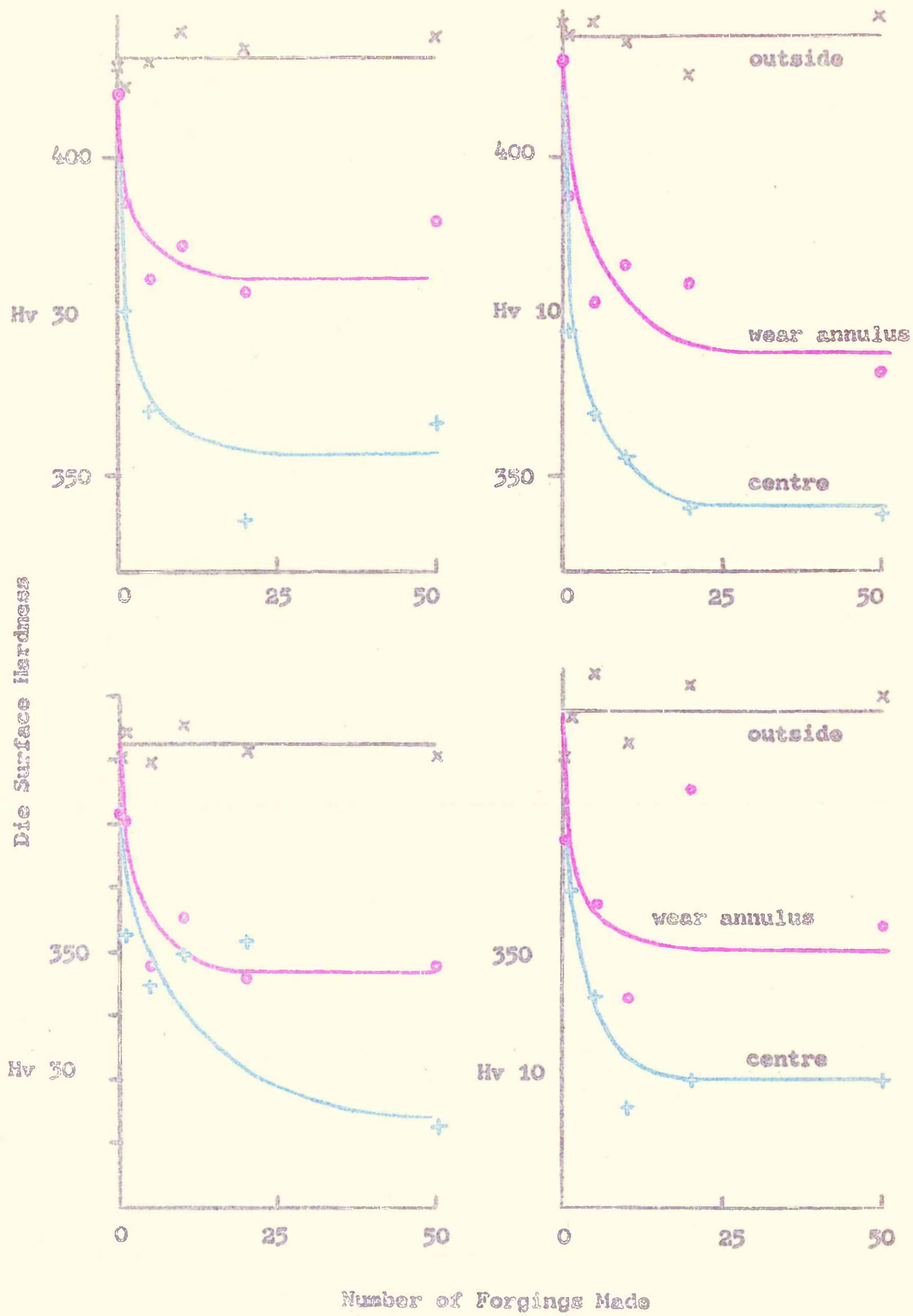


Figure 73

Change in Surface Hardness of No. 5 Die Steel Dies
with Number of Forgings Made

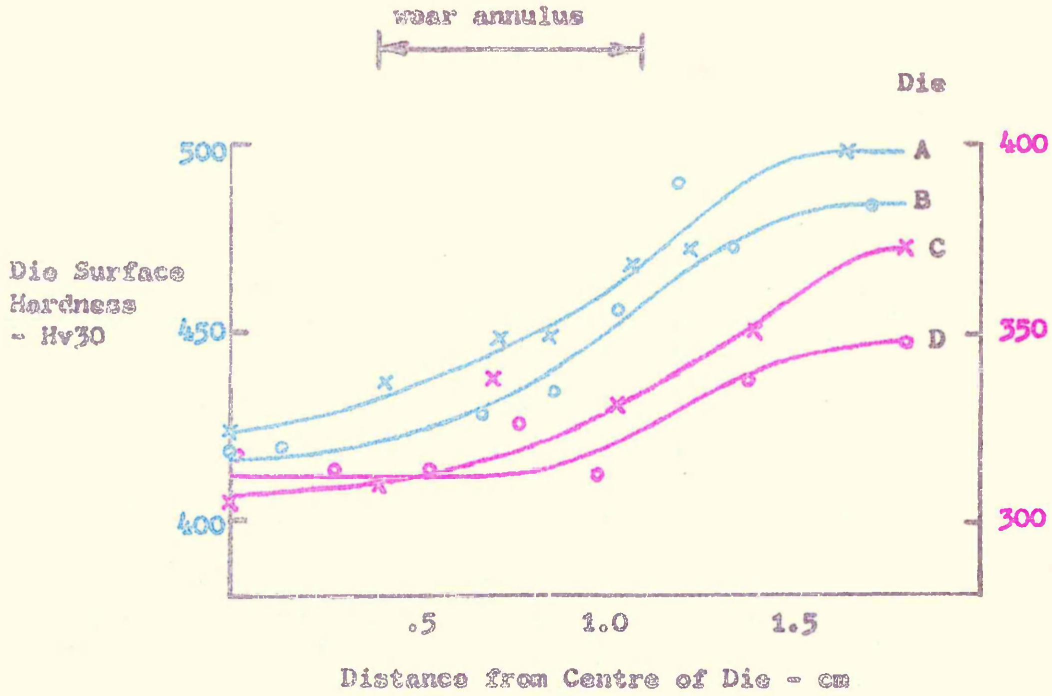


Figure 74

Typical Radial Hardness Contours on Dies after Making 50 Forgings (Die Material No. 6)

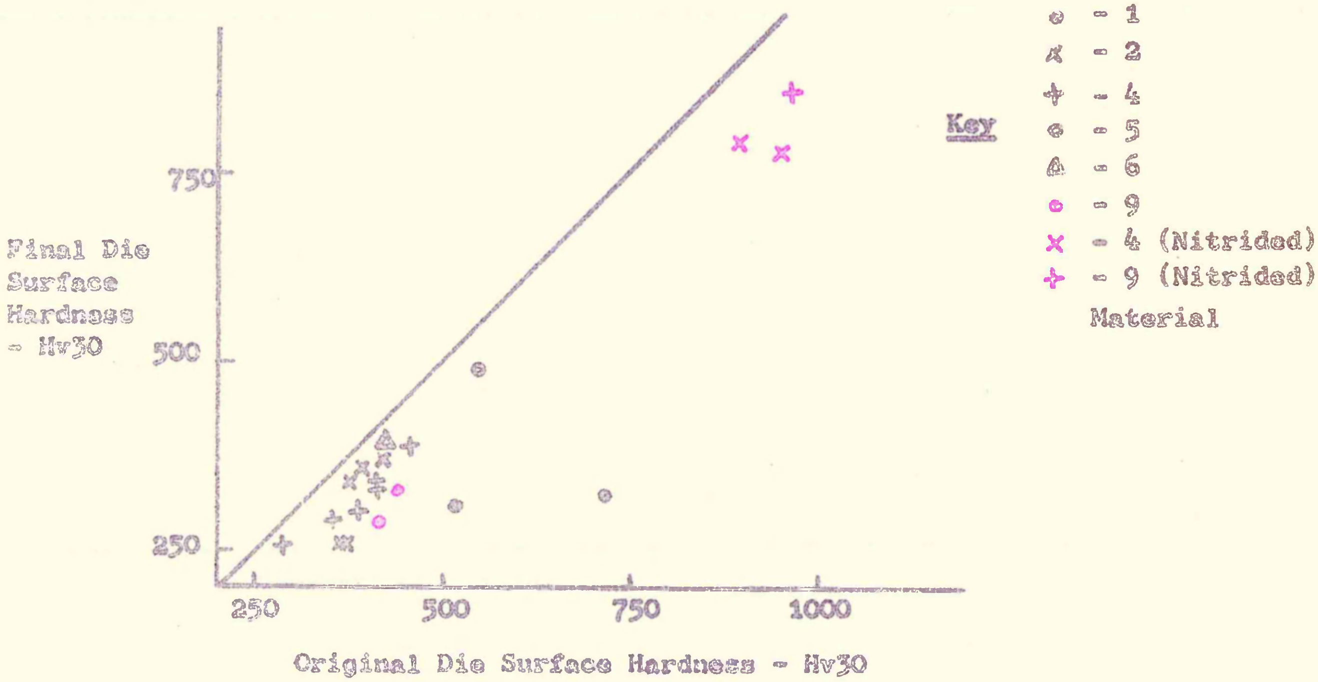


Figure 75

Final Die Hardness (at Centre) v. Original Die Hardness

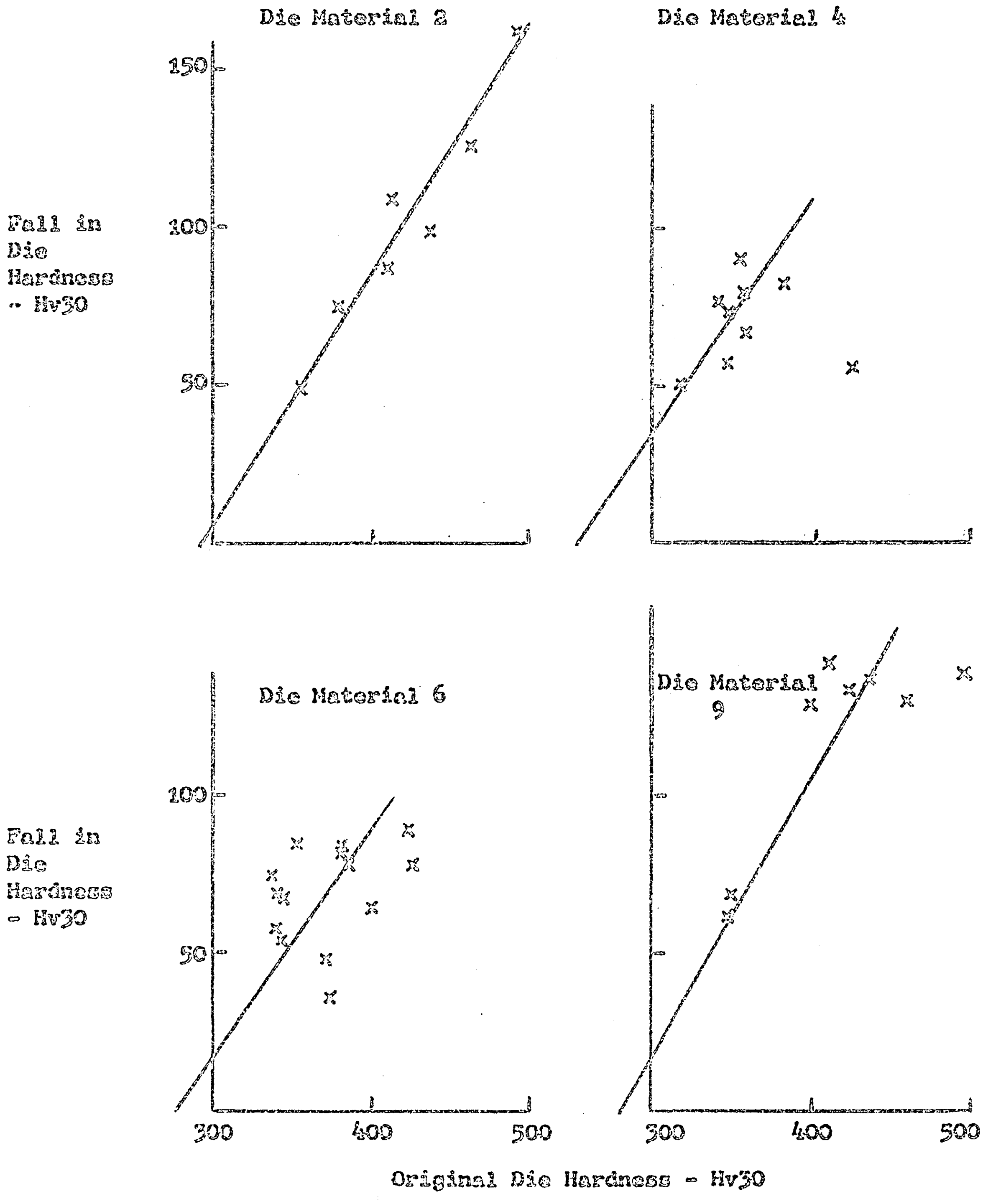


Figure 76

Change in Die Hardness during Testing

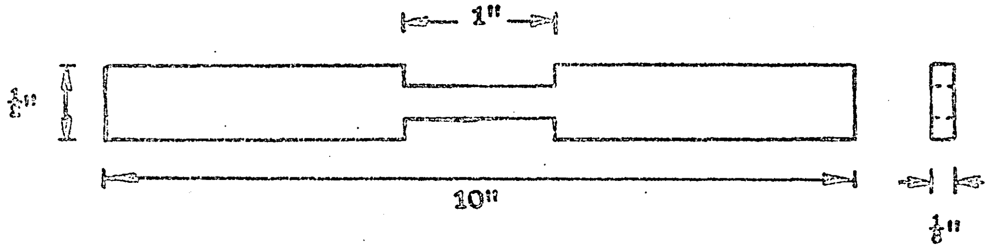


Figure 77

Specimen Used for Hot Tensile Tests

Table 24

Hot Tensile Strength of Die Materials

Material	Hv30	Test Temperature °C	U.T.S. tonf/in^2
2	404	400	64.5
2	411	450	65.5
2	402	500	53.0
2	398	550	37.8
2	406	600	33.2
2	405	650	15.0
2	390	700	8.0
2	407	750	7.0
2	401	750	7.0
2	400	800	5.5
2	403	800	5.4
4	415	400	68.0
4	409	450	65.0
4	410	500	56.0
4	402	550	39.0
4	406	600	41.2
4	412	650	21.5
4	411	700	11.5
4	414	750	6.6
4	408	750	5.5
4	412	800	8.5
4	408	800	8.2

Table 24 continued

Material	Hv30	Test Temperature °C	U.T.S. tonf/in^2
6	404	400	72.0
6	402	450	64.0
6	400	500	55.2
6	403	550	44.0
6	399	600	38.5
6	402	650	26.2
6	401	700	14.0
6	405	750	9.7
6	403	800	7.1
9	410	400	69.5
9	413	500	60.0
9	411	600	48.5
9	418	650	27.0
9	415	700	20.5
9	413	750	13.5
9	415	800	11.5

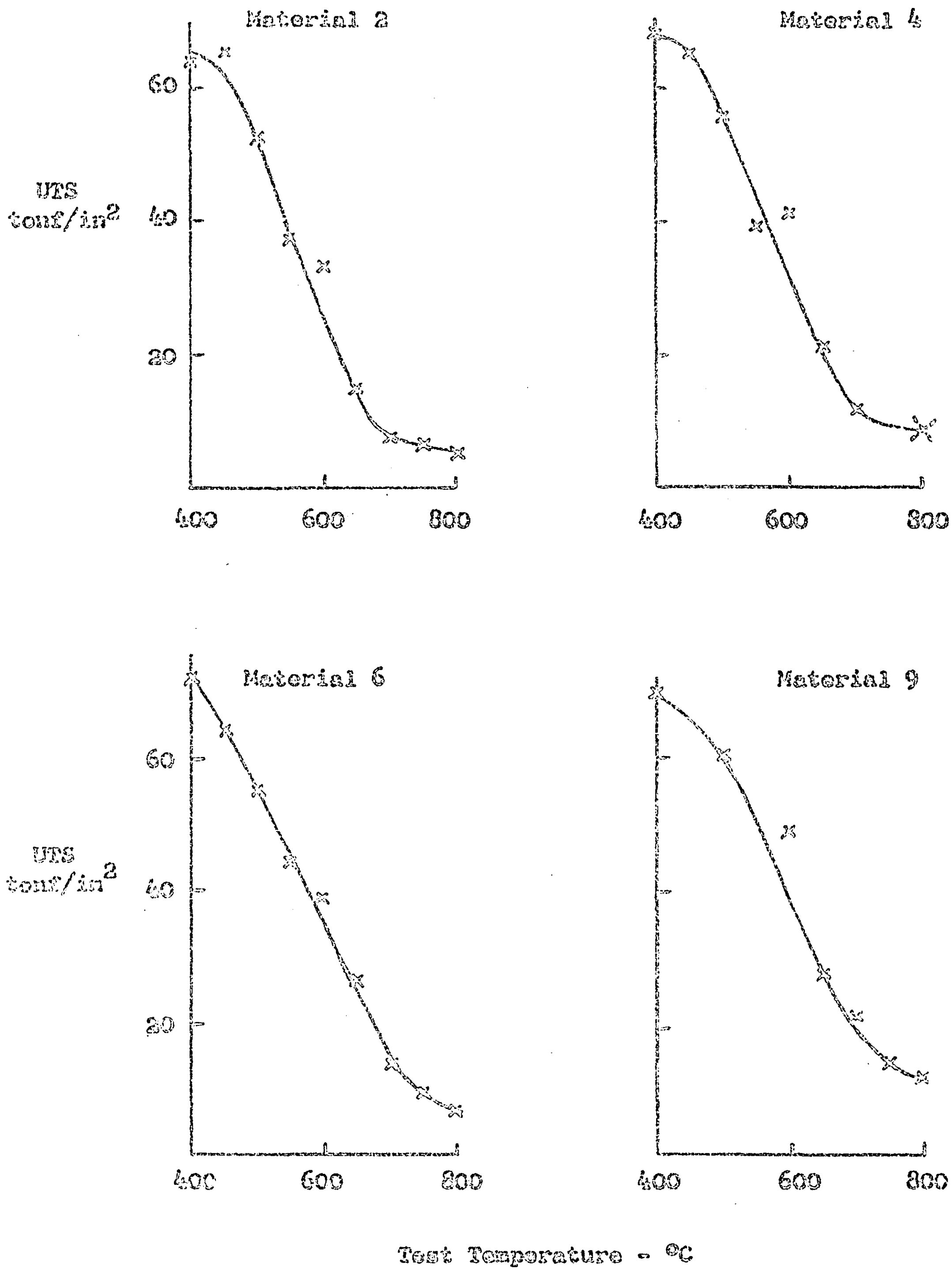


Figure 78

Hot Tensile Strength of Materials 2, 4, 6 and 9

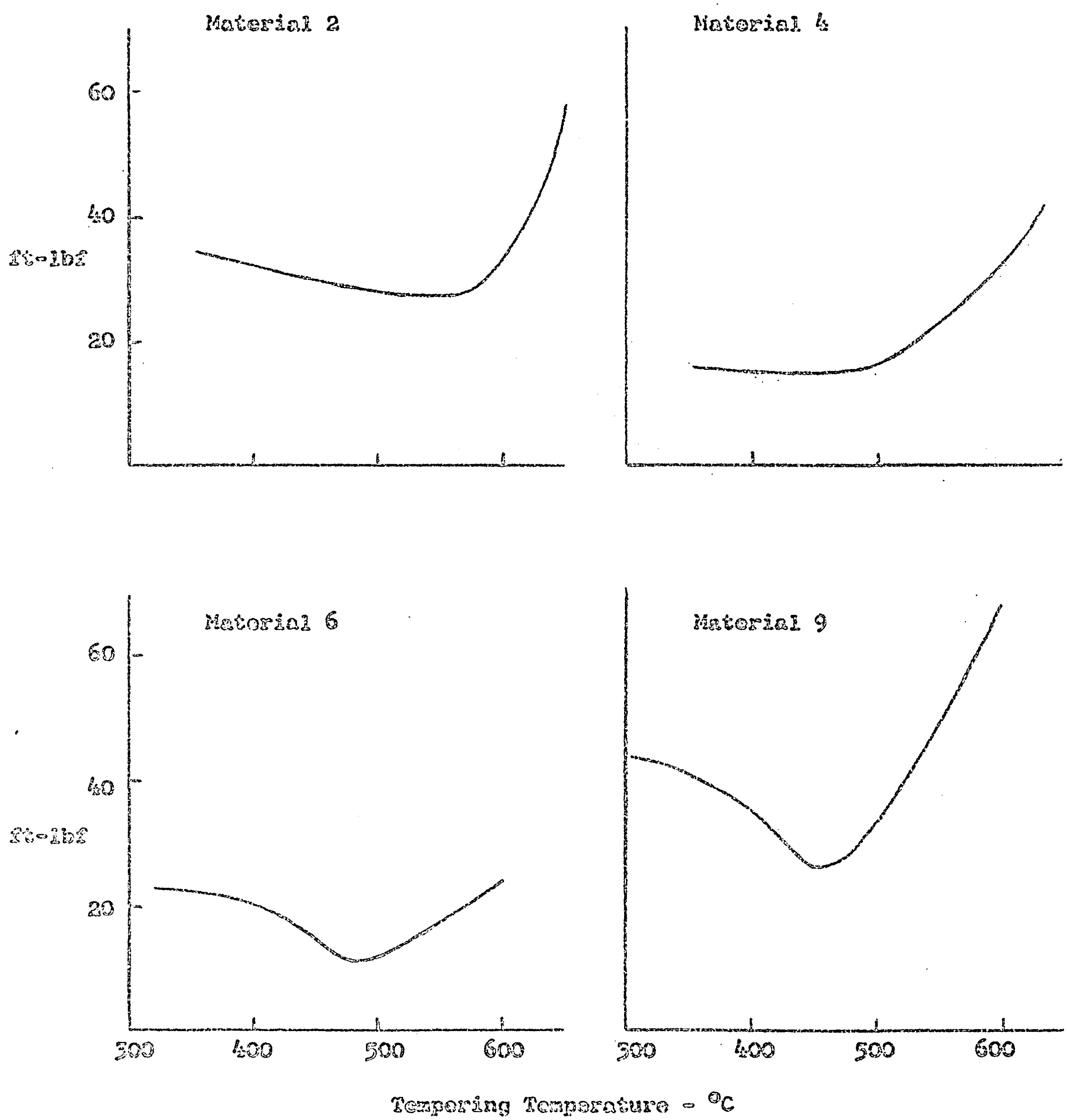


Figure 79

Impact Properties of Materials 2, 4, 6 and 9

4. EXPERIMENTAL WORK - WORKS TRIALS

4.1 Materials Selected for Works Trials

Even though great care has been taken during the development of the wear test, to ensure that test conditions simulated practical forging conditions as closely as possible, it was, nevertheless, considered essential to validate the laboratory test results by works trials.

Of the eighteen materials tested, four were not intended as materials suitable for use as dies (materials 12 - 15 inclusive) and these steels were not considered, therefore, for works trials. Material 1 gave such poor results in laboratory tests that it was excluded from subsequent trials. Material 7 was excluded because the low carbon content prevented the production of dies of sufficient hardness to avoid the risk of deformation. No opportunity was found for testing materials 8 and 10. Since the Nickel based alloys were very expensive, only one of the three alloys investigated was used in works investigations.

The following materials were, therefore, used in works trials; materials 2, 3, 4, 5, 6, 9, 11, and 17.

The approximate price of these materials at the time of commencing the trials is shown in Table 26 below.

Table 26

Material	Price per Ton £	Price per lb. s. d.	Price Ratio Compared with Material 2
2	166	1. 6.	1
3	187	--	1.13
4	222	--	1.34
5	373	--	2.25
6	364	--	2.19
9	476	--	2.87
11	?	--	--
17	-	24. 0.	16.00

4.2 Results of Works Trials

The essential results of the works trials are shown in Table 27. In each trial, the cost of a pair of dies in the standard material (No. 5 Die Steel) has been taken as ten units, and all other costs have been related to this. It is possible, therefore, to compare costs only within one trial and not from one trial to another. Full details of the works trials' data used to draw up Table 27 are given in Appendix III to the thesis.

In row eight of Table 27, the figure before the oblique stroke is the mean die life determined from the number of sinkings shown after the oblique stroke. Where the letter "Q" appears, the die life is that quoted by the forge.

Table 27

Results of Works Die Wear Trials

1. Trial Number	1	2	3	4	5	6	7	8	9
2. Component	Lever	Link	Con-rod	Spanner	Protractor	Combination Square	Sleeve	Hinge	Gear
3. Weight of Component in lb.	3	4	5	$\frac{1}{4}$	$1\frac{1}{2}$	2	4	$1\frac{1}{2}$	6
4. Hammer or Press	H	H	H	H	H	H	P	H	P
5. Labour/Material Cost for Normal Material	?	?	5.87	?	5.15	?	3.66	11.53	3.60
6. Normal Material	2	2	2	2	2	2	2	2	2
7. Trial Material	3	4	4	4	4	4	5	6	6
8. Die Life - Normal Material Trial Material	3280/3 2470/3	7800/Q 3900/1	4318/2 5778/2	15,047/21 19,335/3	1582/2 2571/2	4029/2 4439/2	1200/Q 1900/Q	132,958/4 159,661/4	6232/10 7999/7
9. Die Life Ratio Trial/Normal Material	1.33	-	1.34	1.29	1.63	1.10	1.58	1.20	1.28
10. Cost of Trial Material / Cost of Normal Material	1.13	-	1.30	1.30	1.30	1.30	2.10	2.24	2.24
11. Die Cost per Forging Trial Material Normal Material	15.9 18.2	- -	3.77 or 3.60 3.98	- -	3.50 5.55	- -	3.03 3.88	.120 .108	1.46 1.90
12. % Reduction in Die Costs	14	increase	5 or 10	not established	36	-	22	10	23

Table 27 continued

Results of Works Die Wear Trials

1. Trial Number	10	11	12	13	14	15	16	17	18	19
2. Component	Gear	Gear	Gear	Con-rod	Con-rod	Hook	Protractor Frame	Flange	Gear	Disc
3. Weight of Component in lb.	6	10	10	4	$1\frac{3}{4}$	29	$1\frac{1}{2}$	$3\frac{1}{2}$	1	12
4. Hammer or Press	P	P	P	H	H	H	H	H	P	P
5. Labour/Material Cost for Normal Material	3.60	7.48	4.63	4.83	6.91	-	5.15	-	-	-
6. Normal Material	2	2	2	2	2	1	2	2	6	4
7. Trial Material	6	9	9	9	9	9	9	9	17	11
8. Die Life - Normal Material Trial Material	5,918/5 13,546/5	3730/13 6469/7	5064/13 8849/6	5,219/25 13,547/1	21,197/12 40,564/1	3-4000/Q 4218/1	1582/2 4535/2	3000/Q 4230/3	15,000/Q 26,700/2	4000/Q 198/1
9. Die Life Ratio Trial/Normal Material	2.29	1.73	1.75	2.59	1.91	-	2.86	1.41	-	-
10. Cost of Trial Material / Cost of Normal Material	2.24	2.88	2.88	2.50	2.60	-	2.70	2.87	-	-
11. Die Cost per Forging Trial Material Normal Material	 1.02 1.96	 2.70 3.50	 1.84 1.73	 4.97 11.17	 2.52 3.73	 - -	 2.47 5.55	 3.58 2.24	 - -	 - -
12. % Reduction in Die Costs	48	30	increase	55	32	increase	45	increase	-	-

5. DISCUSSION OF RESULTS

5.1 Laboratory Test Results

5.1.1 Possible mechanisms of wear during forging

Braithwaite[40] has defined wear as "the progressive loss of substance from the surface of a body brought about by mechanical action". He suggested that wear may occur by one or more of the following mechanisms.

(1) Adhesion or galling.

(2) Abrasion.

(3) Corrosion, including oxidation.

(4) Surface fatigue.

(5) Other miscellaneous mechanisms.

The possibility of these mechanisms being responsible for die wear is discussed below.

(a) Adhesive Wear

When two surfaces come into contact they touch only at high points on the surface as shown in figure 80a (p 114). As the load on the surfaces is increased the real areas of contact increase until the total real area of contact A_r can support the applied load W, as shown in figure 80 b (p 114). If H is the indentation hardness of the softer material the real area of contact is given by,

$$A_r = W/H$$

If sliding of the surface takes place metal removal occurs from the softer material by shearing as shown in figure 80c (p 114). If L is the sliding distance the volume of metal removed, V, is given by

$$V = KA_rL = \frac{KWL}{H}$$ where K is a constant characteristic of the softer material.

It is difficult to envisage metal removal from a die surface by adhesive wear since the die is always much harder than the forging stock. If adhesive wear of dies did occur it should be most severe at the centre of the test die, since at this possition the die load is highest (see figure 26 p 38) and the die hardness is at a minimum (see figure 74 p100).

The die wear contour shown in figure 44 (p 65) however shows that no metal removal occurs at the centre of the die. Thus the pattern of die wear confirms that appreciable metal removal cannot occur by an adhesive wear mechanism.

(b) Oxidation

A second possible mechanism of metal removal from the die is by direct oxidation of the die surface.

To investigate whether this is a likely mechanism it is necessary to consider the time which is available for scaling of the die surface to occur during a test.

Figure 33 (p 51) shows that the contact time between the slug and die during forging is about half a second. Thus the total contact time when forging 2,000 slugs is approximately 2,000 x $\frac{1}{2}$ seconds, or about 17 minutes.

Published scaling curves[41] for materials of the following compositions .4C, 1.0Cr, .7Mo and .16C, 11.6Cr, .6Mo, .25Nb, .3V, which are similar to some of the die materials studied, give scaling rates of about 4×10^{-5} and 2×10^{-6} inches per hour respectively at 800°C.

Thus the depth of metal removal to be expected by oxidation in a wear test, even if the die surface temperature was at 800°C for the entire contact period, would be about 1×10^{-5} to 0.5×10^{-6} inches.

Figure 44 (p 65) shows that the actual depth of metal removal in a wear test is of the order of 5×10^{-3} inches i.e. two orders of magnitude greater than could be accounted for by an oxidative wear mechanism.

Again the shape of the wear contour also indicates that oxidation is not responsible for metal loss since the unworn central plateau is subjected to the highest temperatures, so that oxidative wear, if it was significant, would be greater in this region.

(c) Surface Fatigue

In the hot forging of slugs the mechanical stresses in the die surface would be low so that pitting caused by mechanical fatigue would not be expected to occur.

It is possible however that thermal fatigue could cause cracking of the die surface and lead to small fragments of metal being removed from the die. This has in fact been proposed as a mechanism of die wear[42].

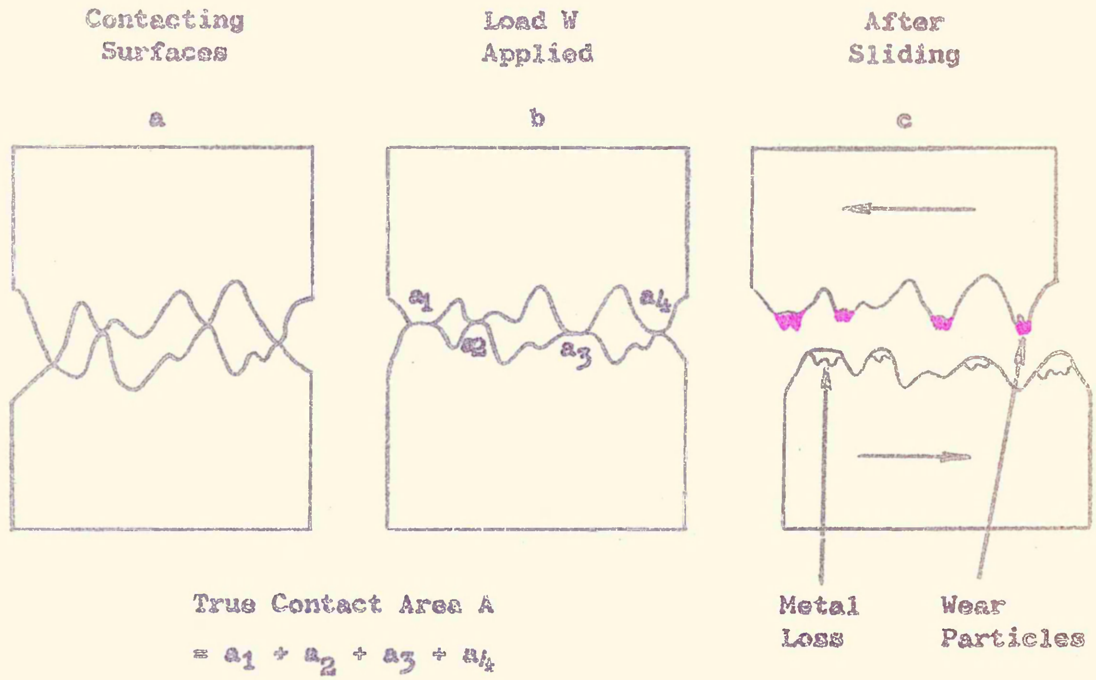
Generally the only evidence of thermal fatigue cracks in test dies was a fine network of cracks in the central unworn region. Except for dies subjected to Sulfinuz treatment and the carbon steel dies, heat-treated to very high hardness levels, this cracking was never severe.

Since the greatest metal removal occurs in a region where the thermal stresses are not most severe it seems unlikely that thermal fatigue of the die surface can play an important part in the mechanism of metal removal.

(d) Erosive Wear

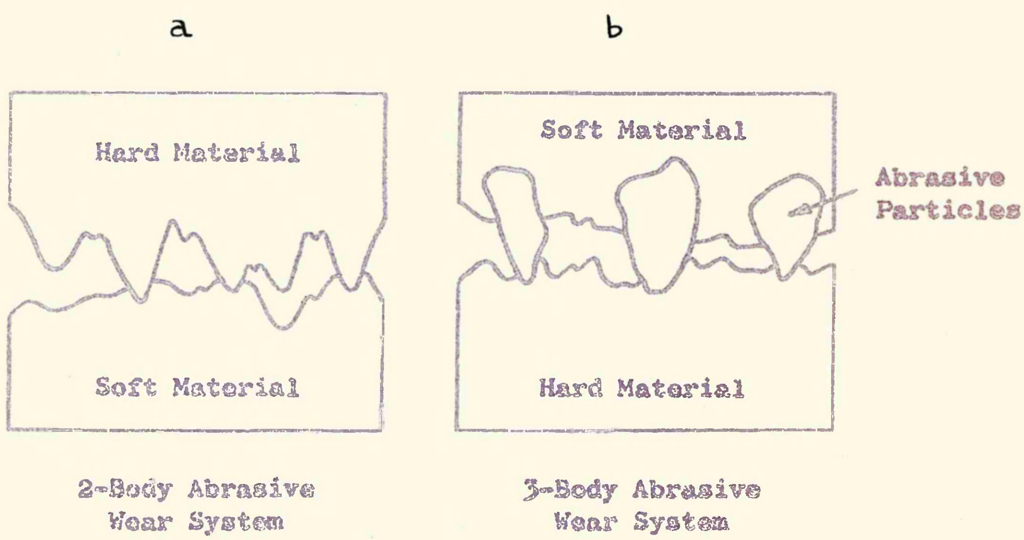
The most likely form of wear in the die wear tests is erosive wear. This form of wear can occur under two-body or three-body conditions as illustrated in figure 81 (p114).

In a two-body wear system asperities on the harder material penetrate the softer body and, when sliding occurs, score grooves in the soft material so that metal is removed by a micro-machining process. Depending on the metal pair under consideration the wear debris formed can oxidise to form hard abrasive particles which embed in the softer material and



<u>Figure 80</u>

Mechanism of Adhesive Wear



<u>Figure 81</u>

<u>2 and 3-Body Abrasive Wear Systems</u>

abrade the harder one.

In a three-body wear system hard abrasive particles are present between the two surfaces from the start of the wear process. In the case of hot-forging of steel the components of the system will be the forging stock, the die steel and scale particles formed on the forging stock.

The factors which are likely to influence the amount of wear occuring in such a system are discussed in the next section.

5.1.2 Factors affecting die wear under three-body erosive wear conditions.

Figure 81b (p114) shows schematically the situation which will exist at the die-forging interface. As this figure shows scale particles will be embedded partly in the die surface and partly in the forging stock. The relative degree of penetration into each surface will depend on the hardness of the respective surfaces. Thus the wear occuring at the die surface will depend on the die hardness at the time when penetration and sliding of the scale occurs and also on the hot strength of the forging stock.

The amount of scale formed on the stock material will influence the amount of wear, since heavily scaled stock will provide more abrasive particles than lightly scaled stock. The degree of scaling of the forging stock will be determined by the stock composition and the heating conditions of time and temperature.

It is also possible that the forging temperature will control not only the amount of scale formed, but also the nature of the oxide. This point is discussed in more detail in section 5.1.6.

The amount of wear taking place at any point on the die surface will depend on how much sliding of stock occurs at that point. This factor will depend on initial and final slug geometry and the coefficient of friction at the stock-die interface as will be shown in section 5.1.3.

Table 28 below summarises the factors which should influence die wear if erosion by scale derived from the forging stock is the mechanism of wear.

Table 28

Factors Influencing Die Wear in Laboratory Tests

	Factor	Property Affecting Die Wear
1.	Die Material	Initial Hardness Strength at Working Temperature
2.	Stock Material	Amount and Type of Scale Formed Strength at Forging Temperature
3.	Stock Temperature	Amount and Type of Scale Formed
4.	Slug Geometry	Amount of Sliding Die Load
5.	Lubrication	Amount of Sliding Die Load

The experimental results obtained in the laboratory wear tests will be examined and interpreted in terms of the parameters listed in table 28. Before this can be done however it is necessary to consider the mechanics of deformation of the slug during forging.

5.1.3 Mechanics of deformation in upsetting of cylinders

Figure 82 (p 118) shows a segment of a cylinder at some stage during an upsetting process, together with the stresses acting on an element of infinitesimal width dr.

Static equilibruim in the radial direction at any instant demands that

$$\sum F_r = -\frac{\partial}{\partial r}(p_r \delta\theta r h)\, dr + 2 p_\theta h\, dr \sin\frac{\delta\theta}{2}$$

$$- 2 \mu p_z \delta\theta r\, dr = 0 \qquad \ldots\ldots\ldots\ldots (1)$$

The first term in this expression represents the radial force acting on the element, the second term represents the force due to the circumferential stresses resolved in a radial direction, and the last term represents the frictional force resisting movement in a radial direction.

Equation 1 above can be reduced[15] to the differential equation

$$r \frac{dp_r}{dr} + p_r - p_\theta + 2\mu p_z r = 0 \qquad \text{.............(2)}$$

or using stresses instead of forces

$$r \frac{d\sigma_r}{dr} + \sigma_r - \sigma_\theta + \frac{2\mu\sigma_z r}{h} = 0 \qquad \text{.............(3)}$$

It can be shown[15] that $\sigma_r = \sigma_\theta$ and consequently equation 3 becomes

$$\frac{d\sigma_r}{dr} + \frac{2\mu\sigma_z}{h} = 0 \qquad \text{.............(4)}$$

It can further be shown[15] that by replacing σ_r by σ_z through the plasticity condition that equation (4) can be written in the form

$$\frac{d\sigma_z}{\sigma_z} + \frac{2\mu}{h} dr = 0 \qquad \text{.............(5)}$$

During the upsetting of cylinders sliding does not occur over the entire surface of the end face but only in an outer annulus between a radius r_o and the outside of the cylinder as shown in figure 83 (p119).

In this outer annulus, or sliding region, equation (5) may be integrated to give the normal stress σ_z at any point on the radius r ($r_o \leq r \leq r_f$). The solution obtained is

$$-\sigma_z = \bar{\sigma} \exp \left[\frac{2\mu}{h} (r_f - r) \right] \qquad \text{.............(6)}$$

Where $\bar{\sigma}$ is the stress at the free edge of the cylinder, which is equal to the yield strength of the material.

Sticking occurs in the central region up to a radius r_o given by the expression

$$r_o = r_f - \frac{h}{2\mu} \ln\left(\frac{1}{\mu\sqrt{3}}\right) \qquad \text{.............(7)}$$

Since there are shear stresses in the direction of the principal stress, the above approach is not strictly valid under present experimental conditions, but similar assumptions are frequently made in the literature.

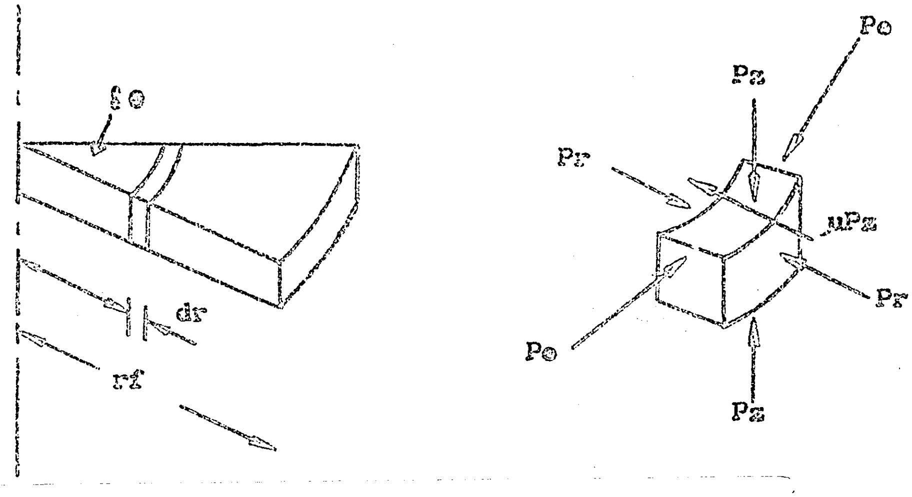
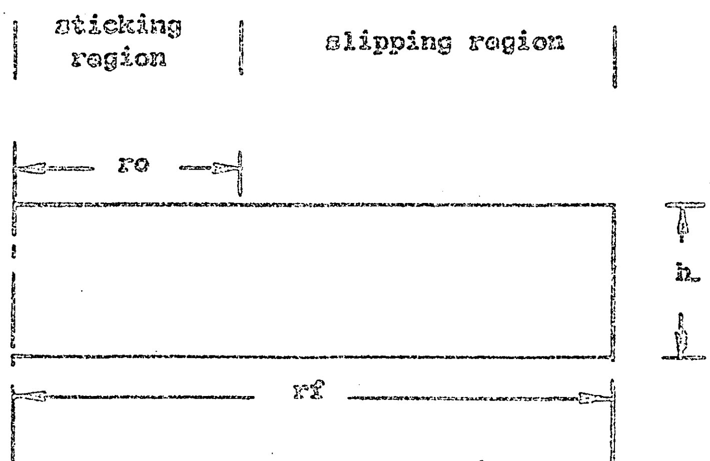


Figure 82

Stress System in Element of Upset Cylinder



$$r_o = rf - \frac{h}{2\mu} \ln \frac{1}{\mu\sqrt{3}}$$

Figure 83

Sticking and Slipping Regions in Upset Slug

Within the sticking region the normal stress at any point r is given by[15]

$$\sigma_z = \bar{\sigma}\left[\frac{2}{\sqrt{3}h}(r_o - r) + \frac{1}{\sqrt{3}\mu}\right] \qquad \ldots\ldots\ldots\ldots\ldots(8)$$

Equations 6, 7 and 8 will be used later to calculate the extent of the sticking zone and the die stresses during forging.

5.1.4 Development of wear pattern on test dies

All the dies tested showed a characteristic wear pattern consisting of a central unworn plateau surrounded by an annular V-shaped groove.

The shape and extent of the wear pattern can be shown to depend on the initial slug geometry, the degree of upsetting and the coefficient of friction between the stock and die as follows.

As upsetting of a slug occurs sliding of metal with impregnated scale takes place in the outer annulus of the deformed slug, beyond the region of sticking. The extent of the sticking region at any instant depends on the degree of upsetting.

Equation 7 in section 5.1.3 can be used to calculate the size of the sticking region (r_o) at any instant for any given value of coefficient of friction (μ) between die and stock.

Figure 84 (p128) shows how the sticking region grows during upsetting, for μ values between 0.25 and 0.56, as a slug initially ¾" diameter x ¾" long is upset. Also plotted in figure 84 is the maximum radius of the slug (r_g) at any instant during the upsetting process.

Assuming that the depth of wear at any point on the die will be proportional to the amount of metal which slides past the point, the theoretical wear contour on the die can be calculated as follows.

In figure 85 (p 121), which shows r_o and r_f as a function of the instantaneous slug height h_i (assuming $\mu = 0.5$), consider three points A, B and C on the radius of a die.

Point A lies within the sticking region from the onset of forging so that no sliding of the slug over the die occurs at this point. Since no sliding occurs at point A no wear will occur if erosion is responsible for wear. At point B sliding of metal starts at the beginning of forging and continues until the sticking region (r_o) reaches point B. Figure 85 (p 121) shows that this occurs when the slug has been upset to height h_{iP}. At this slug height the maximum radius of the slug has grown to r_{fQ} as shown in figure 84 i.e. the slug radius has grown by an amount RQ. Thus the distance of sliding at point B is given by the length of the line RQ and the amount of wear occuring at this point will be proportional to the distance RQ. At point C no metal sliding occurs until the maximum radius r_f reaches point C, sliding then continues until the sticking radius r_o reaches point C. Reference to figure 85 will show that during the period when metal is sliding past point C the outer radius r_f has grown from r_fS to r_fT. Thus the sliding distance and the depth of wear will be proportional to the length ST.

In this manner the theoretical depth of wear at any point on the die can be calculated for any given initial slug geometry, degree of upsetting and μ value.

In making this analysis it has been assumed that the strain in the slug in a radial direction is uniform throughout the sliding region. This is not so, but has been assumed for simplicity since no analytical solution of the strain distribution during upsetting exists. For high values of μ where the sliding region is small, errors due to the assumption of uniform strain are likely to be small.

Theoretical wear contours calculated for the slug geometry and degree of upsetting used in the present investigations are shown in figure 86a (p 123) for various μ values.

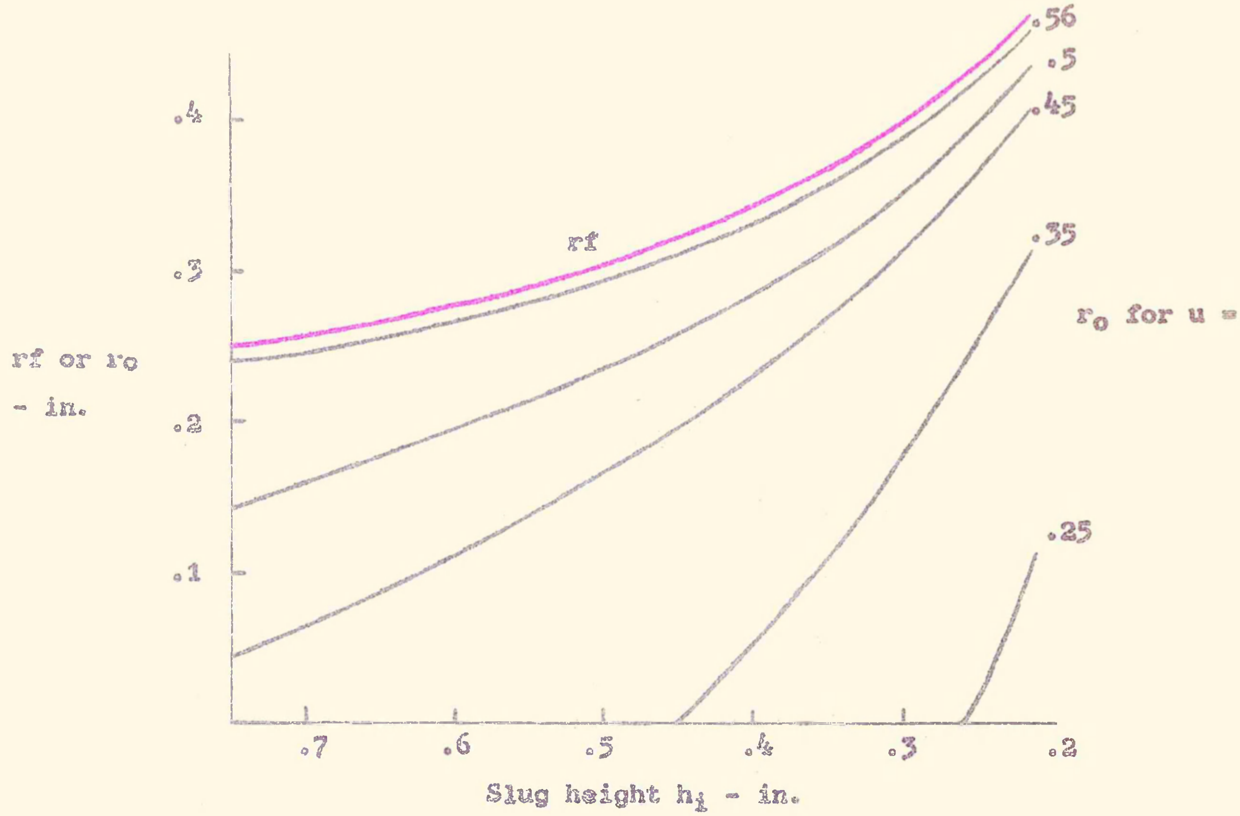


Figure 84

Variation of rf and r_o during Upsetting

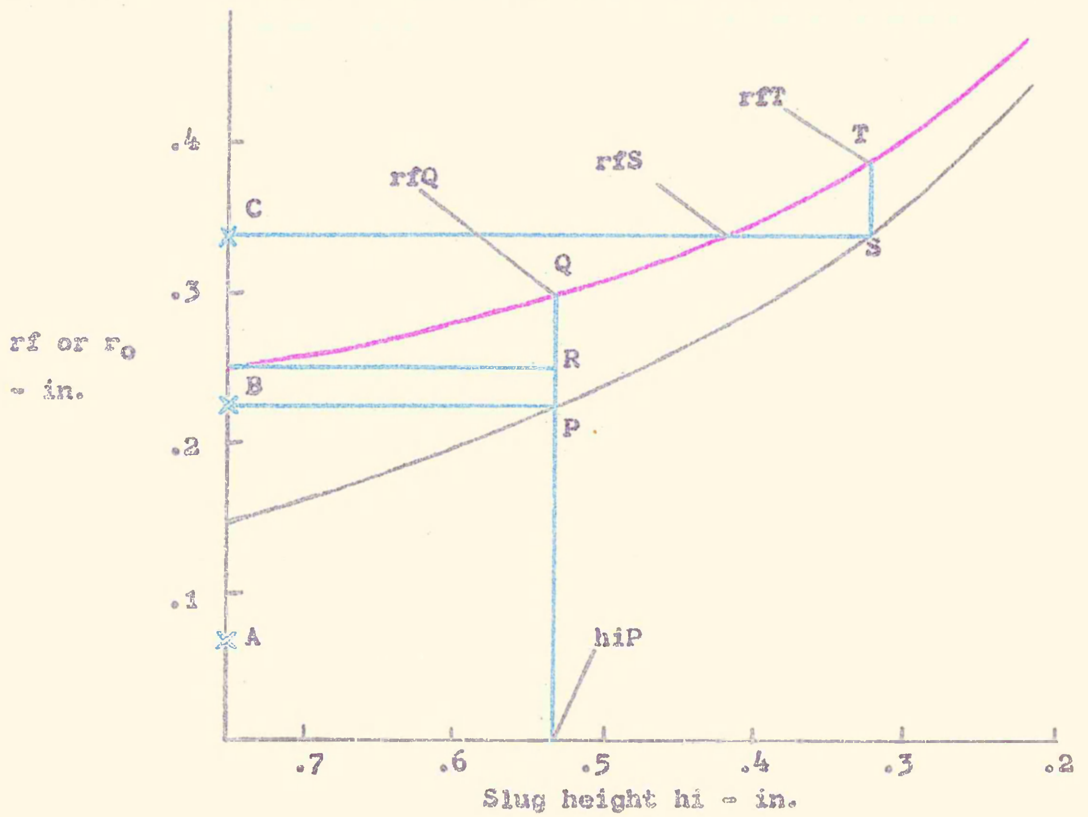


Figure 85

Curves for Use in Calculating Theoretical Die Wear Contours

In all the wear tests described in this thesis a central plateau was formed, but Ali[43] using a similar method of wear testing in which slugs were forged on a Petroforge machine[44], using colloidal graphite spray lubrication, has observed wear contours extending to the centre of the die, confirming that the theoretically predicted wear patterns do occur.

Figure 86 shows that the exact shape of the wear pattern is critically dependent on the μ value but the fact that the observed wear contours agree well with those predicted is strong evidence that the mechanism of wear in the tests is erosive wear which occurs only at places where there is relative movement between stock and die. In figure 86b (p 123) a wear contour has been superimposed on the observed wear contour previously shown in figure 44 (p 65). The vertical scale of the theoretically predicted contour has been arbitrarily chosen so that the maximum wear depths of both curves coincide approximately.

Whilst the general shape of both curves agrees closely there are differences in detail. The measured wear curve tends to lie to the left of the predicted curve.

The failure to place all slugs in exactly the same position on the die will tend to cause an apparent reduction in the size of the unworn central plateau and this explanation may account for the discrepancy in the two curves near the centre of the die.

Near the outside of the wear region less wear occurs on dies than the theoretical pattern predicts. This may be due to plastic deformation of the die surface pushing metal towards the outside of the wear region and thus compensating for metal loss in this region. This suggestion is supported by the appearance of circumferential ridge markings towards the outside of the wear annulus. These can be seen in the photograph of a worn die shown in figure 40 (p 62) and also in the contour trace shown in figure 44 (p 65). An alternative explanation may be the "dilution" of scale as the surface area of the slug increases. Thus the density of scale particles per unit area of slug surface may be less during the later stages of forging.

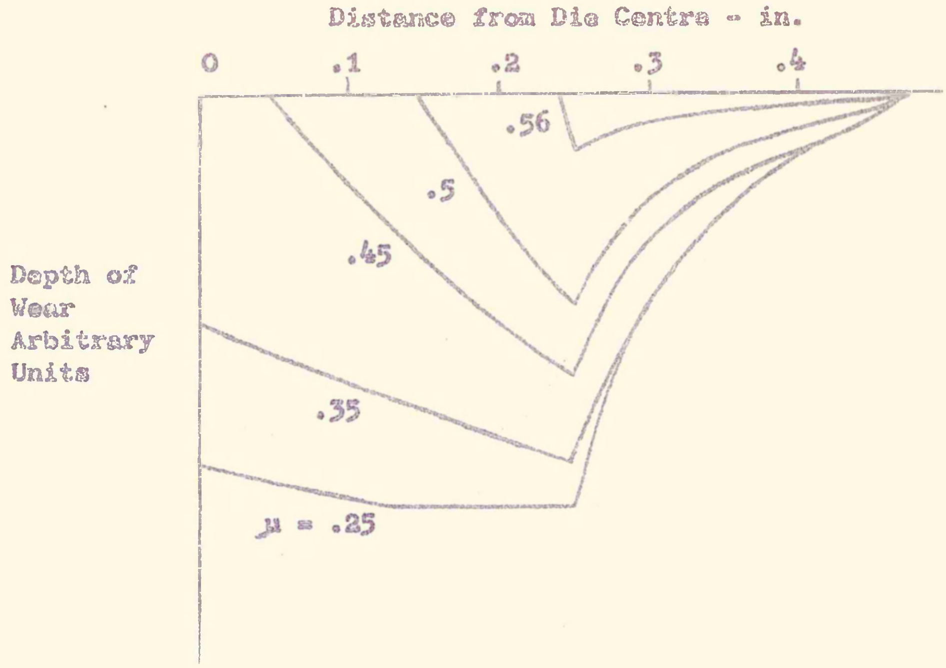


Figure 86a

Theoretical Die Wear Contours

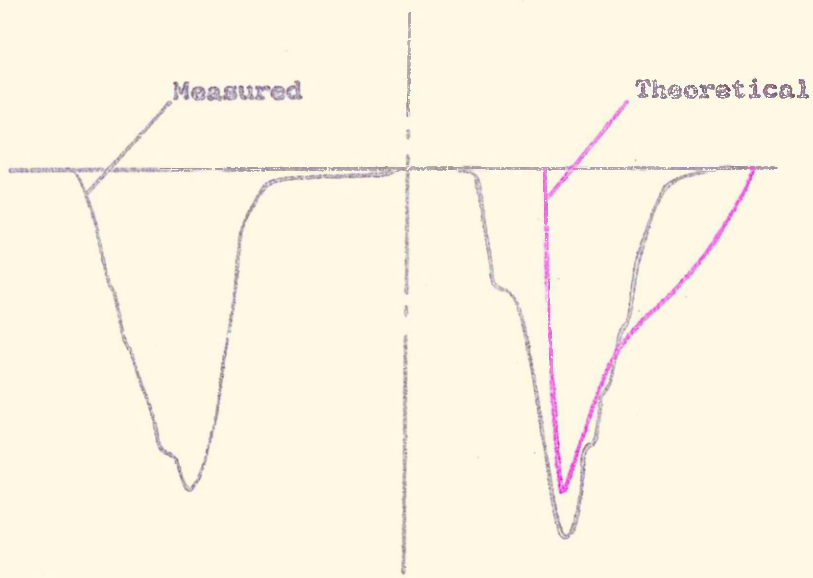


Figure 86b

Theoretical Wear Contour Superimposed
on Measured Wear Contour

In spite of these detailed differences in predicted and observed wear contours the agreement is close enough to suggest that the theory proposed for the shape of the wear contour is correct.

It is convenient at this stage of the discussion to examine how far the results of the wear tests under lubricated conditions can be explained in terms of the wear theory developed. This is done in the next section.

5.1.5 The influence of lubrication on die wear

The first impression created by the results of the die wear test on lubricated dies is that lubrication actually increases the rate of die wear, since, as figure 65 (p 87) and table 22 show, more metal removal occurs from a die for a given number of forgings produced with lubricant than without lubricant.

The situation is complicated however by the fact that lubrication will influence not only the depth of wear at any point, for a given amount of metal sliding, but also the area over which sliding occurs, as shown in section 5.1.3. A more detailed analysis of the effect of lubrication is required therefore.

An indication of the coefficient of friction between the stock and the die during lubricated and unlubricated forging practice can be obtained by considering the radius of the central unworn plateau of the die. This plateau corresponds to the initial sticking zone at the commencement of deformation. Thus by measuring the size of the plateau r_o in equation 7 can be found and since h and r_f (the initial slug dimensions) are known the value of μ can be calculated from equation 7. Measurements of the radius of the unworn plateau on lubricated and unlubricated dies gave average values of 0.184" and 0.230" respectively. These measurements correspond to μ values of 0.525 for lubricated dies and 0.560 for unlubricated dies.

The latter value agrees well with other determinations[45] of μ under similar conditions of dry forging but the value for lubricated forging is much higher than is usually found. It is not known whether the high μ value in the present investigations was due to inefficient lubrication or some degradation of the lubricant during the period when the hot slug rested on the lubricated die.

Using the calculated μ values theoretical wear contours have been drawn using the method described in section 5.1.3. These theoretical contours are shown in figure 87 (p126). The area of each contour has been measured by a planimeter and the wear area for lubricated forging was found to be five times as great as that for unlubricated forging.

Using the data presented in table 22 the observed wear areas after forging 1,000 slugs under dry and lubricated conditions have been calculated and are shown below in table 29.

Table 29

Wear Area After Forging 1,000 Slugs

	With Lubrication	Without Lubrication
	835	474
	975	596
	730	486
mean	847	519

Thus the observed ratio of wear under lubricated conditions to wear under dry conditions was 847/519 = 1.63, compared with the theoretically calculated ratio of 5.

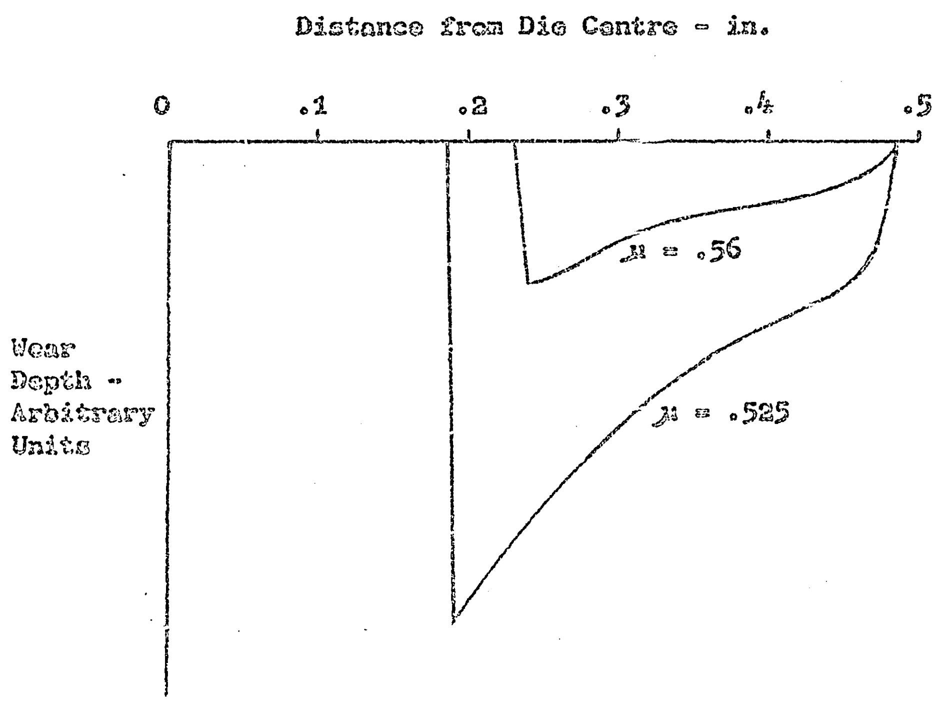


Figure 87

Theoretical Die Wear Contours for μ = .56 and .525

The calculated wear areas were determined on the assumption that the loads during sliding, and hence scale penetration, were the same with and without lubricant. It was thought therefore that the discrepancy between the calculated and observed wear ratios may be explained by metal sliding taking place under lower loads when a lubricant was used.

Using the observed values of μ (.525 and .56) in equations 6 and 8 (section 5.1.3) the stress at various points on the die radius was calculated throughout the upsetting process. The results of these calculations are shown in figure 88 (p128).

The blue part of each curve indicates the stress when sliding is taking place at any point, whilst the red part of each curve shows the stress when the point on the die under consideration is within the sticking region.

These curves show that during the period when sliding is occuring the stress at any point on the die is very slightly higher when a lubricant is used than when forging is done under dry conditions. Thus the discrepancy between the calculated and observed wear ratios cannot be attributed to reduced sliding loads when a lubricant is used.

If the analysis above is correct the observed wear when a lubricant is used is less than that calculated. This implies that for a given amount of metal sliding past any point on a die less metal removal occurs under lubricated conditions than under dry conditions. Since this cannot be attributed to a reduced load causing reduced scale penetration the reduction must be due to the lubricant physically reducing scale penetration as indicated in figure 89 (p 131).

Since the observed increase in wear when using a lubricant was by a factor of 1.63 compared with an expected factor of 5 the physical protection of the die surface by graphite must have reduced the wear at any point on the die by a factor of $5/1.63 \simeq 3$.

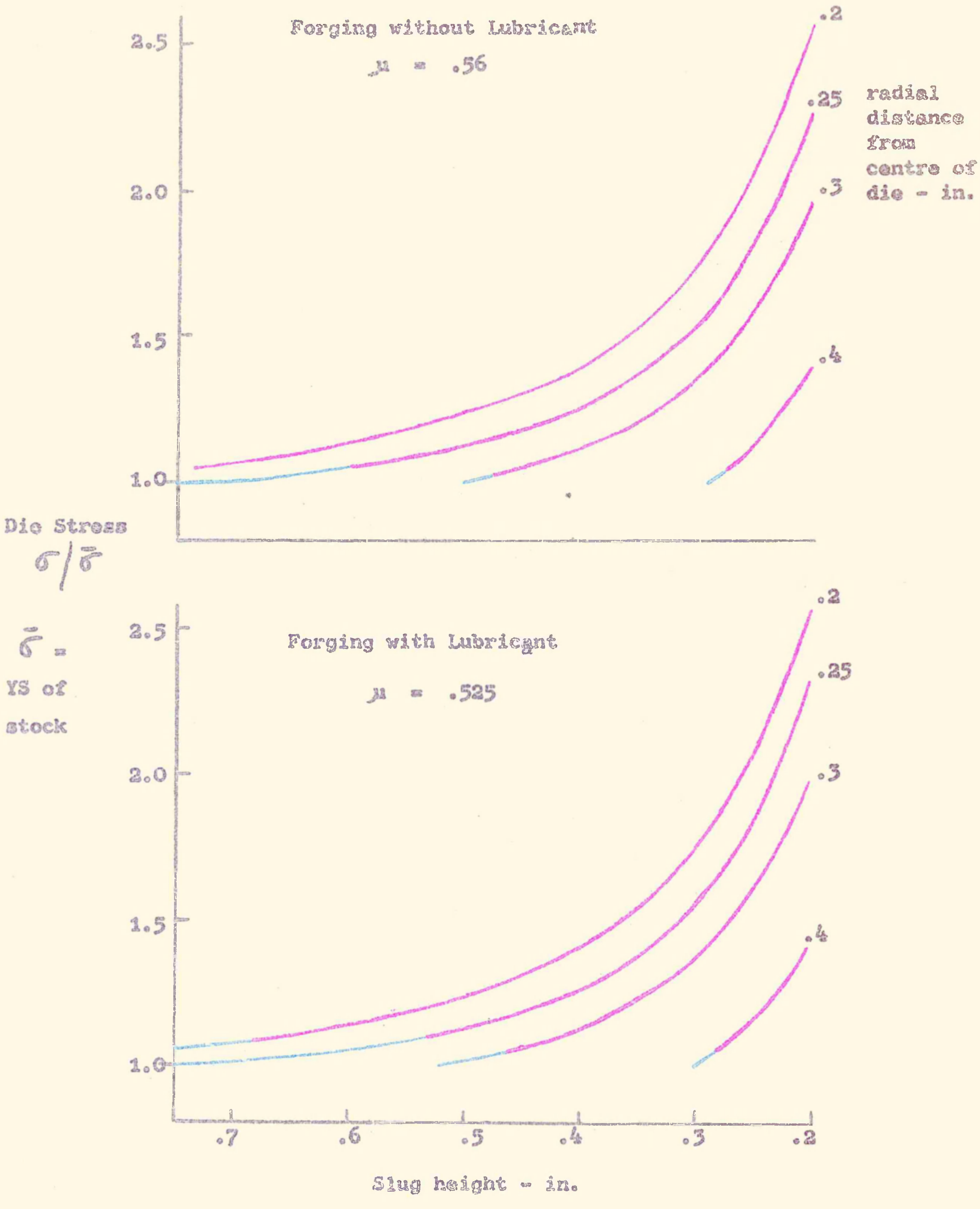


Figure 88

Die Surface Stress during Upset Forging With and Without Lubricant

In other words for a given amount of metal sliding at any point the wear taking place when graphite lubrication is used is only one third of that which would occur if an equivalent amount of sliding occurred without a lubricant.

The results of the tests on lubricated forging lead to interesting conclusions regarding the use of colloidal graphite in practical forging conditions.

Where the forging shape is rather flat, so that forging approximates to the upsetting conditions used in the wear test, the use of colloidal graphite could actually increase the total metal removed from a die by increasing the area over which sliding occurred.

Where however the forging has a deep impression so that the vertical die walls can restrict lateral flow at an early stage, wear will be concentrated on the flash lands at the periphery of the forging. At this point sliding rather than sticking will occur whether a lubricant is used or not. Under such conditions the amount of metal sliding over the land will depend only on the amount of flash thrown and not on the lubrication conditions. In such a situation the reduced wear per unit of metal sliding afforded by lubrication will be beneficial in reducing wear on the land.

Discussions with a supplier of graphite lubricants[46] indicated that improvements in die life were in fact not usually achieved by the use of lubrication for flat forgings. Improvements in life have however been noted for other forging shapes[59].

5.1.6 The influence of stock temperature on die wear

Figure 61 (p 78) shows the variation of die wear with stock temperature. It was suggested in section 5.1.2 that if erosive wear by scale was responsible for die wear then the wear occurring under any forging conditions should be proportional to the amount of scale on the forging stock.

Also it was suggested that the penetration of scale, and hence the amount of wear, should be proportional to the yield strength of the forging stock. In addition to the above factors wear will also be influenced by the strength of the die surface which, in turn, will depend on the die surface temperature. Using the curves shown in figure 6, (p 13), an indication of the influence of stock temperature on die surface temperature can be obtained. Knowing the surface temperature the strength ($\hat{\sigma}$) of the die surface for any forging temperature can be found from figure 78 (p 105). The amount of wear occuring will be **inversely** proportional to the strength of the die surface.

Thus when forging at any given temperature it might be expected that the die wear occuring (W) would be proportional to the function $S \times Y/\hat{\sigma}$ where S is the amount of scale formed on the stock, Y the yield strength of the stock, and $\hat{\sigma}$ is the strength of the die surface.

When considering the yield strength of the stock not only temperature but also strain rate must be taken into account, since at high temperatures the strength of steels is strain-rate sensitive.

Cook[47] has published curves showing the variation in yield strength of mild steel with temperature for natural strain rates between 1.5 and 100 seconds^{-1}, and some of his results have been replotted in figure 90 (p131).

During the forging of slugs in the wear test the slug height was reduced from 0.75" to 0.200" so that the natural, or logarithmic strain, is given by $\log\left(\frac{0.75}{0.20}\right) = \log 3.25 = 1.18$.

Since the stroking rate of the press was 80 strokes per minute and the stroke length was 1" the press ram would accomplish the 1" forging stroke in $\frac{1}{2} \times \frac{60}{80}$ seconds or $\frac{3}{8}$ seconds. However forging occurred for only a fraction

/of this period

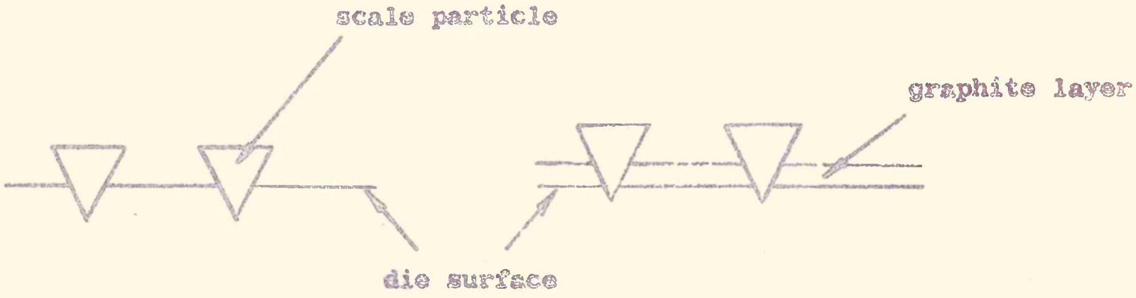


Figure 89

Possible Mechanism of Reduced Scale Penetration
when Using Graphite Lubrication

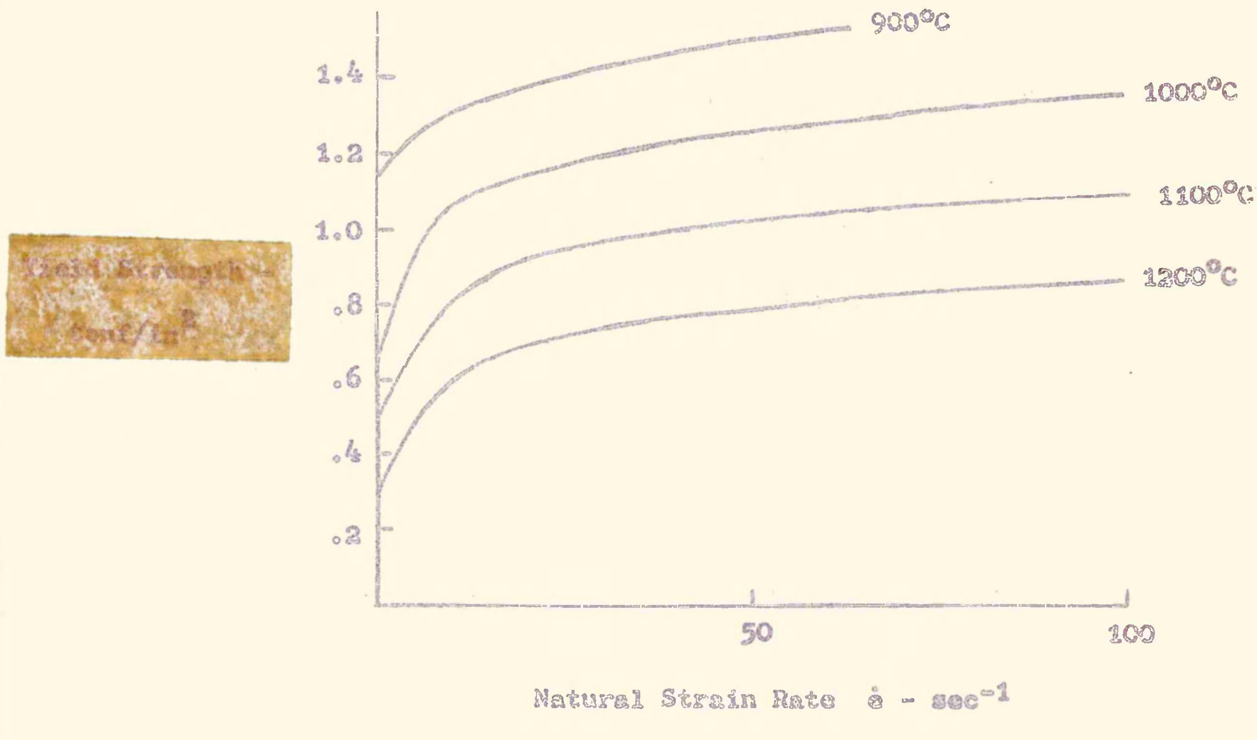


Figure 90

Variation of YS of Mild Steel with Strain Rate
and Temperature - After Cooke[44]

of this period given by $0.55 \times \frac{3}{8} = 0.206$ seconds [see figure 91 (p133)]. Thus a natural strain of 1.18 was achieved in 0.206 seconds so that the average strain rate was $1.18/0.206 = 5.75$ seconds^{-1}. A similar correction for strain rate effects should also be made to the strength value allotted to the die surface. This was not possible however since the relevant data were not available. The errors introduced however are likely to be small since at the temperatures under consideration (500 - 650°C) the influence of strain rate on strength is much lower than at temperatures in excess of 900°C.

Table 30 shows the amount of scale (S) formed on mild steel at temperatures between 900 - 1200°C together with the yield stress Y at a strain rate of 5.75 seconds^{-1} and the strength σ of the die surface. The scaling figures are taken from data published in reference[48], the value of Y has been obtained from figure 90 (p131) and σ has been obtained in the manner already explained.

Also shown in table 30 is the function $Q = S \times Y/\sigma$ to which, as already stated, wear at any temperature should be proportional.

Table 30

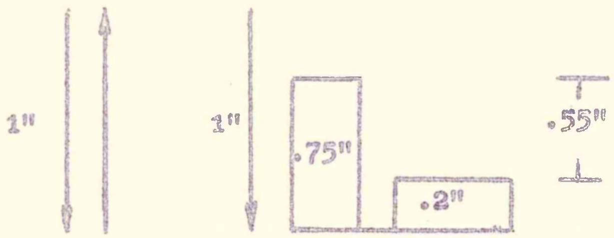
Strength and Scaling Susceptibility of Mild Steel and Die Surface Strength

	900	1000	1100	1200
Amount of Scale Formed (S) .001"/hr.	2.27	5.28	7.0	9.1
Yield Strength (Y) tonf/in^2	12.60	9.55	6.95	5.10
Strength of Die Surface (σ) tonf/in^2	56	41	29	16
Function $Q = \frac{(S \times Y)}{\sigma}$	.51	1.23	1.67	2.90

Figure 92 (p133) shows both the wear index and the function P in table 30 plotted against forging temperature. It can be seen that the general shape

/of the two

Press Stroking
Rate = 80/min



1" downward
stroke takes
$\frac{1}{2} \times \frac{60}{80}$ sec.

compression accomplished in

$$\frac{1}{2} \times \frac{60}{80} \times \frac{.55}{1} \text{ sec.} = .206 \text{ sec.}$$

$$\text{strain} = \ln \frac{.75}{.2} = 1.18$$

$$\text{strain rate} = \frac{1.18}{.206} = 5.75 \text{ sec}^{-1}$$

Figure 91

Calculation of Strain Rate in Upsetting Slugs

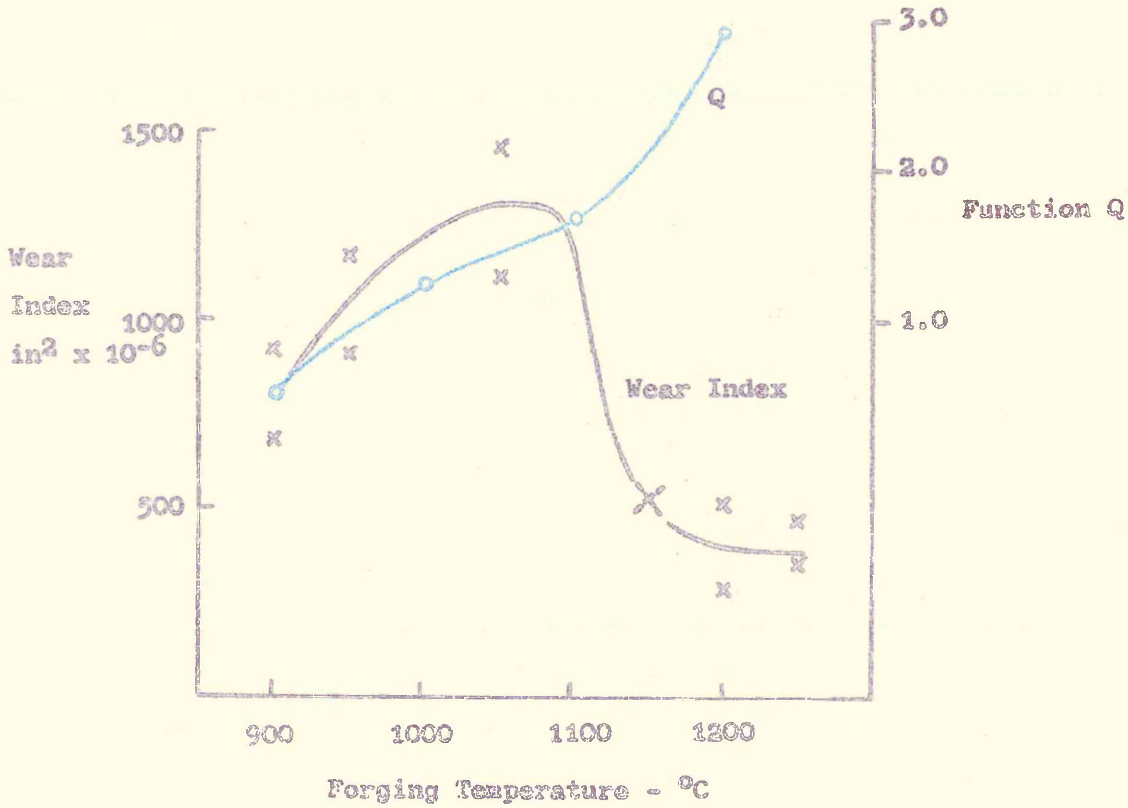


Figure 92

Wear Index and Function Q in Table 30
v. Stock Temperature

of the two curves is very similar up to 1100°C thus supporting the suggestion that the stock temperature influences die wear by its effect on the degree of stock scaling and the strength of the stock and die. Above 1100°C however the observed amount of die wear falls rapidly with increased temperature although the theoretical curve predicts that wear should continue to increase.

A possible explanation of this is that above 1100°C the nature of the scale may change so that it becomes softer and less abrasive.

Tholander and Blomgren[49] have studied the oxidation of mild steel in air and state that at temperatures below about 1100°C the main constituent of the scale is haematite (Fe_2O_3). Above 1100°C however they claim that a molten phase is formed and the oxides formed are magnetite (Fe_3O_4) and wüstite (FeO).

Garber and Sturgeon[50] have measured the hardness of oxides of iron and report the following figures.

Haematite	Fe_2O_3	1030 Hv.
Magnetite	Fe_3O_4	420-500 Hv.
Wüstite	FeO	270-350 Hv.

It appears therefore that the rapid reduction in die wear as the forging stock temperature exceeds 1100°C might be associated with a change in the nature of the scale formed on the stock. It is interesting to note in this connection that Tholander[51] has predicted reduced die wear at high forging temperatures on the basis of his studies of scale composition.

Figure 92 (p 133) shows that a reduction in forging temperature from 1250°C to 1050°C causes a threefold increase in die wear. It is possible that the wide variation in die life in forges mentioned in section 1.3 could, to a large extent, be due to variations in forging temperature. This seems quite feasible when it is remembered that many forge furnaces are not fitted with a means of temperature control.

5.1.7 The influence of forging stock on die wear.

Using arguments similar to those in the previous section it should be possible to predict the influence of forging stock on die wear in terms of the amount of scale (S) formed on the stock and the stock strength (Y) at the forging temperature.

Table 31 below shows this data for mild steel, En24 and En57 which were the three forging stocks investigated.

The ratio of wear (R) for any forging stock compared with that for mild steel should be given by the ratio $\frac{S \times Y \text{ for any stock}}{S \times Y \text{ for mild steel}} = R$

Table 31 shows the predicted values of R for En24 and En57 together with the average values observed for the three die materials (4, 6 and 9) tested using the different forging stocks. The wear of the three die materials has been compared at an initial die hardness of 400 Hv30.

Table 31

Influence of forging stock on die wear

Material	Scale formation S Cg/cm^2	Yield Strength Y at 1100°C-Kg/mm^2	Product Q = S x Y	Wear Ratio R	
				Rp Predicted	Ro Observed
Mild Steel	16.6	2.4	38.2		
En24	12.0	3.0	36.0	$\frac{36.0}{38.2} = 0.94$	1.50
En57	6.6	4.5	29.7	$\frac{29.7}{38.2} = 0.80$	0.80

The scaling figures used in table 31 are those published by Willingham & Williams[52] whilst the yield stress values have been taken from figures published by Unksov.[14]

Table 31 shows that whilst the agreement between the predicted and observed wear ratios for En57 and Mild Steel is very good this is not the case with the figures for En24 and Mild Steel. A practical observation made during many wear tests was that during upsetting some of the scale flaked off the hot slug and was ejected clear of the die area. It is likely therefore that the scale responsible for erosion is not the total amount of scale formed on the slug but only that proportion which adheres strongly during upsetting.

Willingham & Williams[52] have measured the "adhesion index" of scaled slugs in terms of the percentage of the total scale adhering to a 1" x 1" x 2" prism upset by 25% in height. The values quoted for Mild Steel, En24 and En57 were 31, 56 and 36% respectively.

If the product Q in table 31 is corrected for this scale adhesion factor the agreement between predicted and observed wear ratios is good for both En24 and En57, the values being,

$$R_p \text{ for En24} = \frac{36.0 \times 56}{38.2 \times 31} = 1.7 \text{ (cf observed value of 1.5)}$$

$$R_p \text{ for En57} = \frac{29.7 \times 36}{38.2 \times 31} = 0.9 \text{ (cf observed value of 0.8)}$$

5.1.8 Summary of the influence of forging variables on die wear

Before discussing the influence of die materials on die wear it is useful to summarise the extent to which the influence of forging variables on die wear has been explained.

A simple theory of wear, based on erosion of the die by scale particles derived from the forging stock and carried across the die face by stock, has been proposed.

Taking into account the mechanics of deformation during the upsetting of cylinders it has been possible on the basis of the proposed theory to predict the wear pattern formed on dies and the influence which lubrication has on both the extent and pattern of die wear.

It has also been possible to predict the effect of stock temperature on die wear in quantitative terms at least up to 1100°C.

In addition it has been shown that the influence of stock material on die wear can be predicted quantitatively to a fair degree of accuracy.

All the results discussed so far confirm that the mechanism of die wear is by erosion with any other wear mechanism playing a negligible role.

5.1.9 <u>The influence of die material on wear</u>

5.1.9.1 <u>Method of comparing wear resistance for different die materials</u>

<u>Method of comparing wear resistance for different die materials</u>

Since No 5 Die Steel (material 2) is so widely used in the drop forging industry it is useful to compare the behaviour of other die materials directly with that of No. 5 Die Steel.

For this reason when comparing the wear of the die materials investigated this has been done in terms of a relative wear index (RWI). The RWI for any material is defined as follows:-

$$RWI = \frac{\text{wear index of material at initial hardnes H}}{\text{wear index of No. 5 Die Steel at the same Hardness}} \times 100$$

Table 32 below shows the RWI for the materials investigated at initial die hardness of 300, 350 and 395 Hv30.

Table 32

RWI for Die Materials

	Material	RWI at Hv30 = 300	350	395
1	Plain C Steel	104	123	158
2	No 5 Die Steel	100	100	100
3	Cr Mn Ni Mo Steel	43	52	73
4	En40C	45	45	45
5	Mo Ni Cr V Steel	21	25	32
6	Cr Mo W V Steel	45	47	51
7	Ni Cr Mo Steel	42	49	61
8	Cr Co Mo V W Steel	23	30	41
9	Cr Ni Mo V Steel	14	21	32
10	W Cr V Steel	21	23	26
11	Cr Mn Ni N Steel	10	9	6
12	Cr Steel	110	123	149
13	Cr Mo Steel	84	88	97
14	Cr Mo Steel	57	64	75
15	Cr Mo Steel	63	71	85
16	Nimonic 90	-	-	20
17	Nimocast 713	-	-	11
18	Inco 901	-	-	18

Figure 93 (p139) shows the RWI for all materials plotted as a function of initial die hardness.

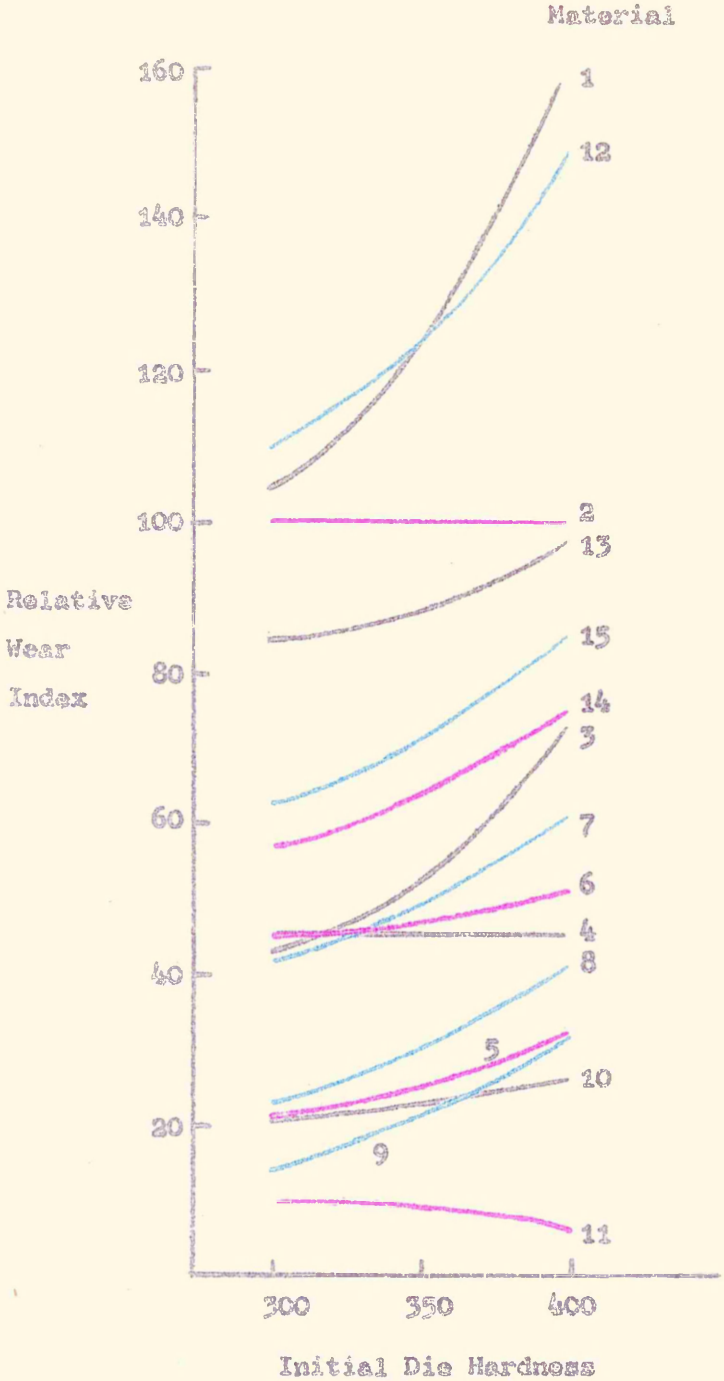


Figure 93

Relative Wear Index v. Initial Die Hardness

5.1.9.2 Influence of tempering resistance on wear resistance

Wetter[31] showed that for the range of die materials which he investigated there was a close correlation between wear resistance and tempering resistance as shown in figure 16 (p 28).

Wetter used as a measure of tempering resistance the Larson - Miller parameter necessary to produce a tensile strength of 160 Kgf/mm^2 (100 $tonf/in^2$). In the British forging industry such hard materials would only be used for small press dies and inserts machined before heat-treatment. The majority of dies are used at a tensile level of 80-85 $tonf/in^2$.

Following Wetter's approach figure 94 (p141) has been plotted to show the RWI for materials at an initial die hardness of 395 Hv30 as a function of the tempering temperature necessary to produce a die hardness of 395 Hv30 ($\equiv 82\frac{1}{2}$ $tonf/in^2$).

Whilst there is clearly a correlation between wear resistance and tempering resistance in figure 94 it is not strong enough to make the measure of tempering resistance used a useful indication of wear resistance.

It is significant that three materials (5, 7 and 9) which fall at the bottom of the scatter band have very low ($\frac{0.1}{0.2}$%) carbon contents. These materials thus have a low as quenched hardness and relatively low tempering temperatures will reduce the hardness to 395 Hv30. Thus for certain materials it appears that the method of assessing tempering resistance used by Wetter does not in fact give a proper measure of the potential high temperature stability of the die material.

Since it has been shown that dies are subjected to high surface temperatures during use it would seem more logical to use as a measure of tempering resistance the hardness of the die material after exposure to temperatures similar to those encountered during service.

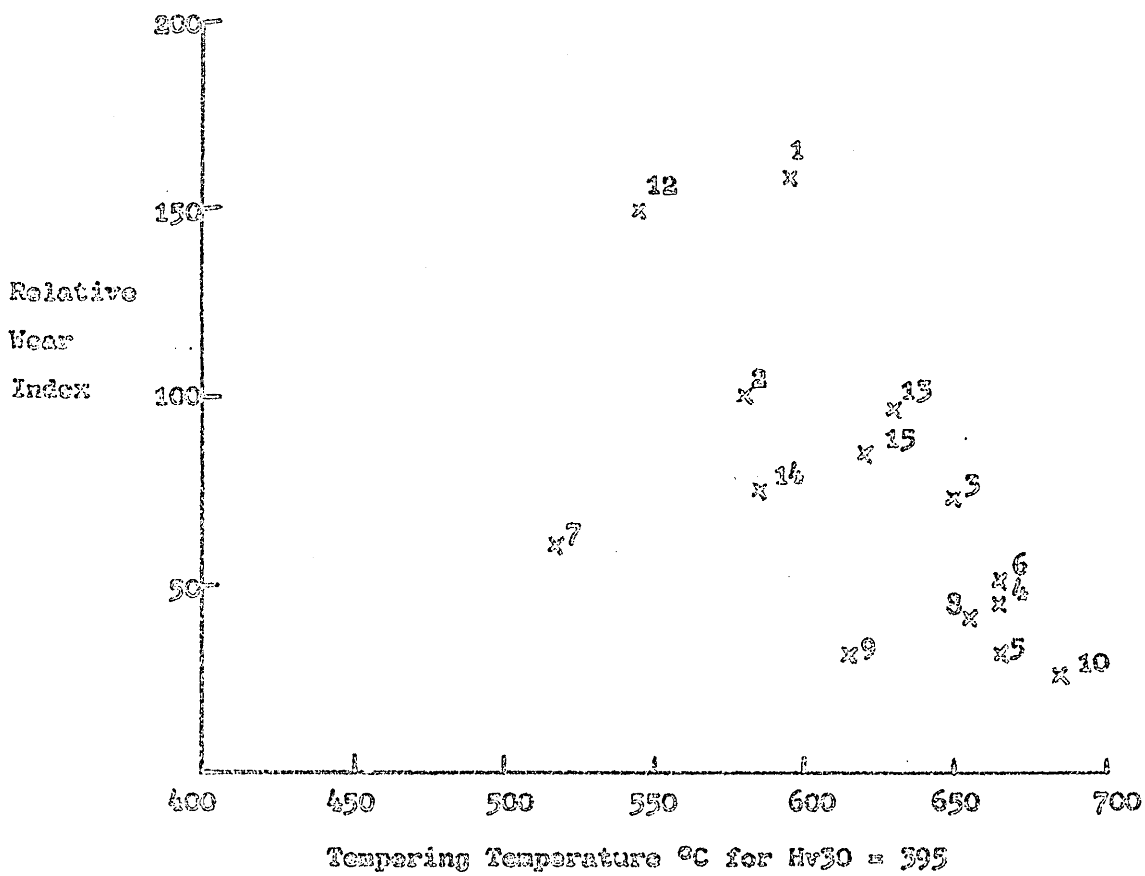


Figure 94

Influence of Tempering Resistance on Wear Resistance

Relating the hardness of dies after use (figures 74, 75 and 76) to tempering curves suggests that effective surface temperatures between 650-700°C are encountered.

Figures 95 and 96 (pp143&144) have been plotted to show the RWI at three initial die hardness levels as a function of the material hardness after tempering for two hours at 650 or 700°C.

The correlation between wear resistance and hardness after tempering at 650°C is poor. Again it is noticeable that low carbon materials (5, 7 and 9) lie at the lower end of the scatter band, presumably for the reason already explained.

Using the hardness after tempering at 700°C as a measure of tempering resistance produces a good correlation with wear resistance, only one material (7) falling very far from the curve drawn.

Considering the mechanism of wear proposed it would seem that the fundamental mechanical property which controls wear resistance is strength at working temperature. The fact that wear resistance correlates closely with hardness after tempering at 700°C suggests that hot strength at 700°C correlates closely with hardness after tempering. Figure 97 (p145) shows that this is indeed the case for the four die materials whose hot strength has been determined.

Figure 98 (p145) shows a plot of RWI against UTS at 700°C for the four materials already considered plus material 16 (Nimonic 90). The point for the nickel based alloy lies well off the curve through the other points.

This may be explained as follows. The nickel based alloys are poor conductors of heat compared with steels and consequently the surface temperature reached in these alloys during forging will be much higher than for steels. Thus the use of the tensile strength at 700°C as a measure of the

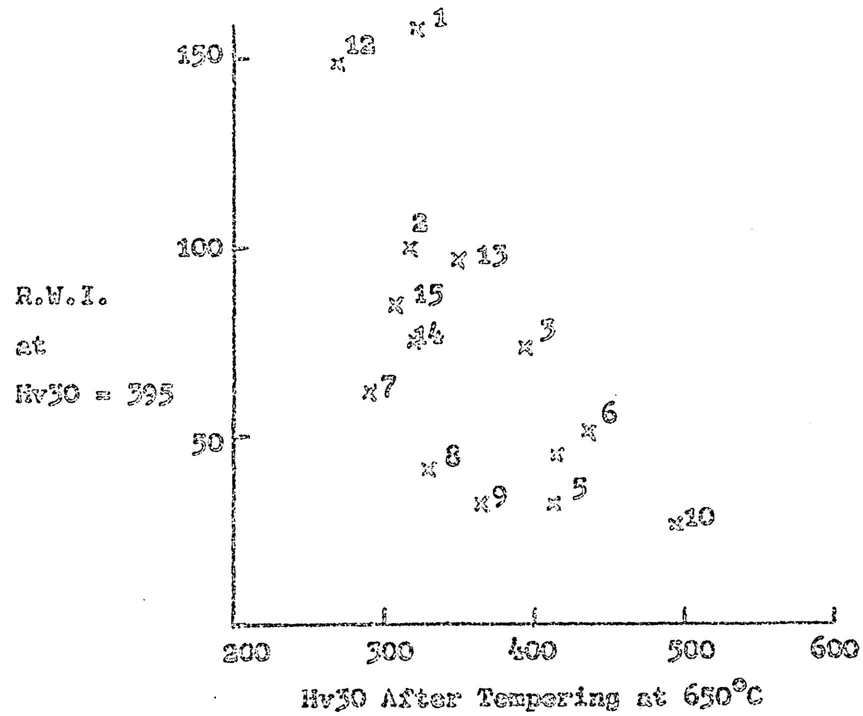


Figure 95

Wear Resistance v. Tempering Resistance

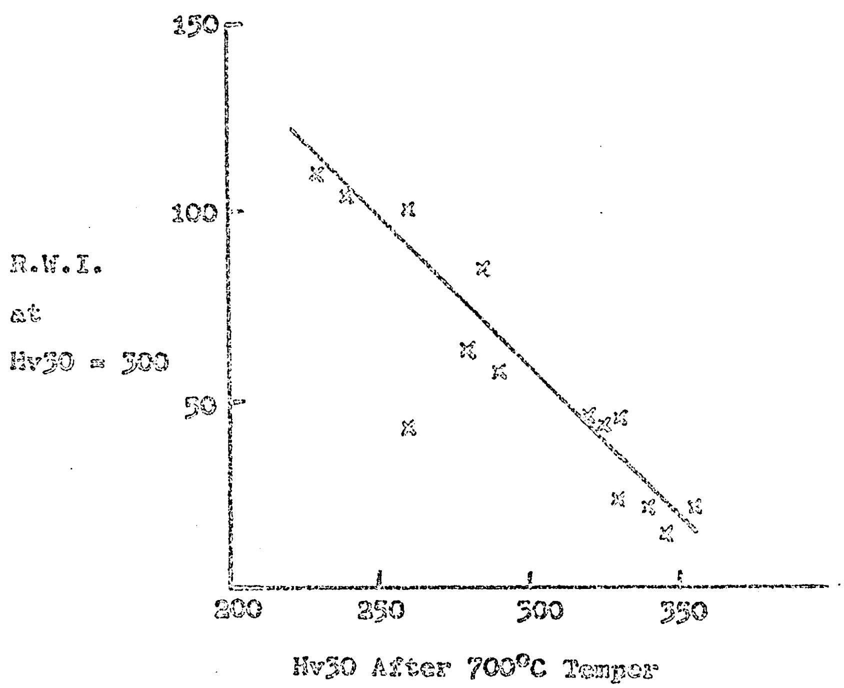


Figure 96a

Influence of Tempering Resistance on Wear Resistance

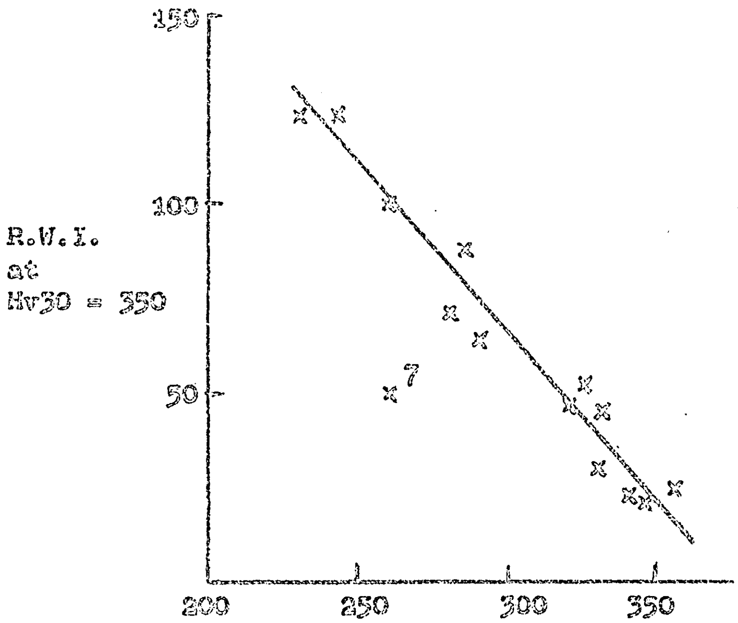


Figure 96b

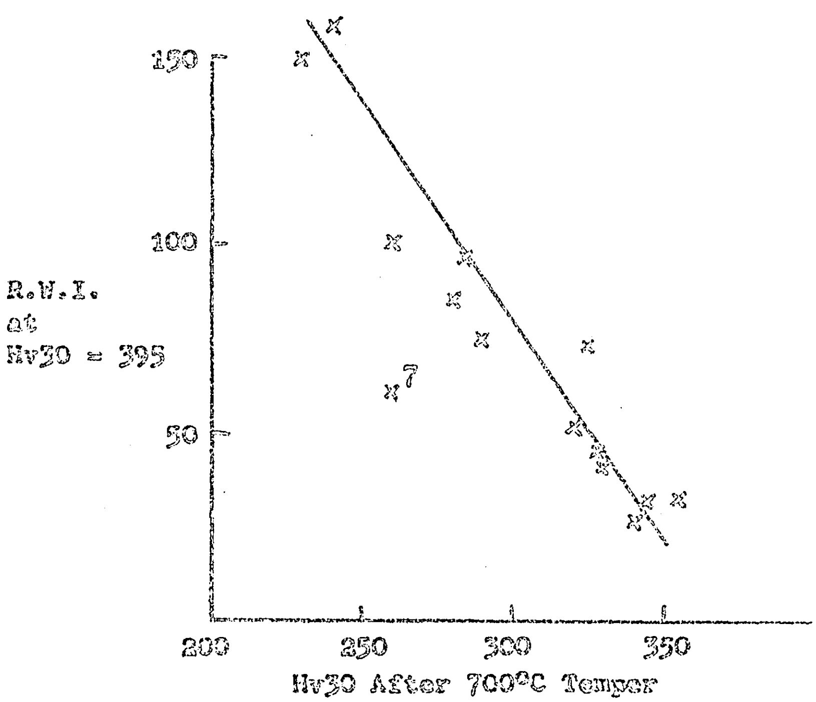


Figure 96c

Influence of Tempering Resistance on Wear Resistance

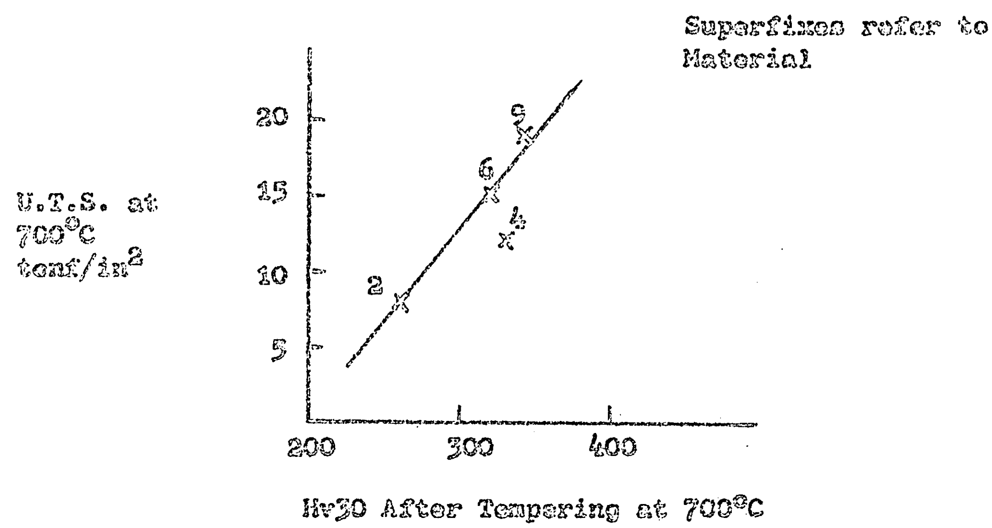


Figure 97

Relationship Between Hot Strength at 700°C and Hardness After Tempering at 700°C

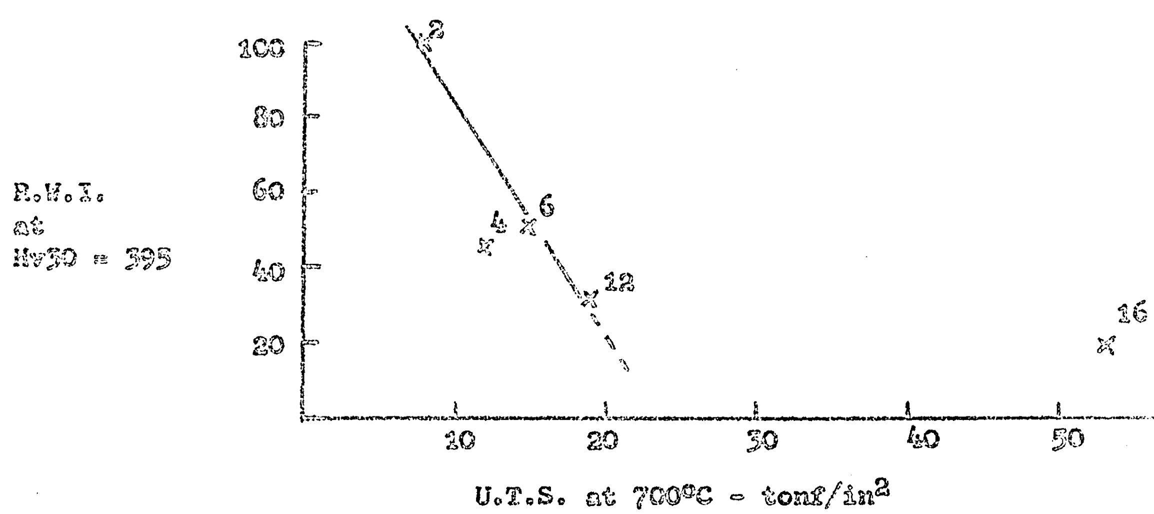


Figure 98

R.W.I. as a Function of U.T.S. at 700°C

wear resistance of Nimonic 90 is unrealistic and the value used in figure 98 should be the tensile strength at the surface temperature reached by this alloy.

An approximate calculation of the likely surface temperature for Nimonic 90, assuming that the surface of steel dies reaches 700°C, indicates that a temperature as high as 1000°C may be reached. Details of the calculation are given in appendix 4.

From the above discussion it is apparent that even though wear resistance is almost certainly controlled by the hot strength of the die material, the latter cannot be used as a comparative measure of wear resistance unless the working temperature at the die surface is known.

There is still therefore a need for comparative wear tests which automatically overcome the difficulty mentioned.

5.1.10 The influence of composition on wear resistance

Although it has been shown in the preceding discussion that wear resistance is determined by the hot strength of die materials it would be valuable for die steel development purposes if a direct relationship between wear resistance and composition could be established.

The wear test data have been analysed therefore to see whether such a relationship could be developed.

Before discussing the method of analysis used the possible manner in which alloying elements could influence wear resistance is considered.

5.1.10.1 Possible functions of alloying elements in promoting wear resistance

Since alloying elements appear to exert an influence on die wear through their effect on hot strength the manner in which they affect the latter property must be considered.

Basically the strength of an alloy steel, in the hardened and tempered condition, at any temperature will depend on the intrinsic strength of the matrix and the extent to which the matrix is hardened by precipitated phases. In the case of most of the steels considered the precipitated phases will be carbides.

Thus the strength of a die steel may be expected to depend on the amount and type of carbide formers present, the carbon content and the amount of alloying element in solid solution in the matrix.

Materials 12-15 were included in the test programme to investigate the relative effect of carbon, chromium and molybdenum on wear resistance.

Table 33 shows the composition and RWI for these alloys.

Table 33

Wear Resistance of Alloys 12-15

Material	Composition			RWI at 395 Hv30
	C	Cr	Mo	
12	.32	4.6	-	149
13	.35	4.9	.57	97
14	.35	9.5	.59	75
15	.25	9.6	.56	85

An estimate of the influence of carbon on wear resistance may be made from the above wear test results for materials 14 and 15. Thus an increase in carbon content of 0.1% reduces the RWI by 10 units, so that 1% C is equivalent to 100 units reduction in RWI.

Similarly by comparing materials 12 and 13 the effect of 0.57 molybdenum is 52 units

i.e. 1% molybdenum ≡ 91 units

Comparing materials 13 and 14 the influence of 4.6% chromium is 22 units

i.e. 1% chromium ≡ 5 units approximately.

From this simple preliminary analysis it appears that carbon and molybdenum have a strong influence on wear resistance, almost certainly through formation of carbides, whilst the influence of chromium is much weaker. Since the chromium additions in the alloys considered were fairly large it was considered that the influence of chromium on wear resistance could be due to either carbide formation or a solid solution hardening effect. The wear tests made on the carbon free iron-chromium alloys were included in the programme of tests to investigate the influence of solid solution hardening on wear resistance.

Table 34 shows the wear index for the iron-chromium alloys.

Table 34

Wear Test Results for Iron-Chromium Alloys

Alloy Composition	Wear Index	Mean Wear Index
Fe - 5Cr	1018, 956	987
Fe - 9Cr	577, 639, 645	620
Fe - 13Cr	338, 406	372

Due to the fact that these alloys were very soft (180/280 Hv30) the wear tests were made by forging slugs to a thickness of 0.300" to reduce the load on the die and avoid deformation, therefore the above test results cannot be compared directly with any other wear tests.

Table 34 shows that even in the absence of carbon chromium exerts an effect on wear resistance, presumably through its solid solution hardening effect. This assumption is supported by plotting the wear resistance of the

/alloys

alloys against their hot-hardness as shown in figure 99 (p 150). The hot hardness data have been taken from figures published by Tedman and Westbrook[53].

It is clear from the above discussion that wear resistance will be affected both by the amount and type of carbide forming elements present in an alloy and by the alloying elements present in solid solution.

Data on all the 15 steels investigated have therefore been analysed to establish the relationship between wear resistance and composition. The Nickel based alloys, since they are hardened by the precipitation of inter-metallic compounds in addition to carbides, have been excluded.

A statistical technique has been employed and the method of analysis is discussed in the next section.

5.1.11 Method of analysing wear test data

The statistical technique used to analyse the wear data was that of linear regression analysis.

This technique may be used to fit a straight line relationship to a set of values of x and y by determining the slope b and the intercept a on the y axis of a line whose equation is $y = a + bx$.

The method minimises the sum of the squares of the differences between observed y values and those given by the line $y = a + bx$.

When more than one variable is involved, as in the present case, the equation to be determined takes the form

$$y = a + b_1x_1 + b_2x_2 \quad \ldots\ldots\ldots\ldots \quad + b_nx_n$$

The analysis was carried out on an Elliott 803 Computer using programme LS-17 for Correlation Matrix and Regression Analysis.

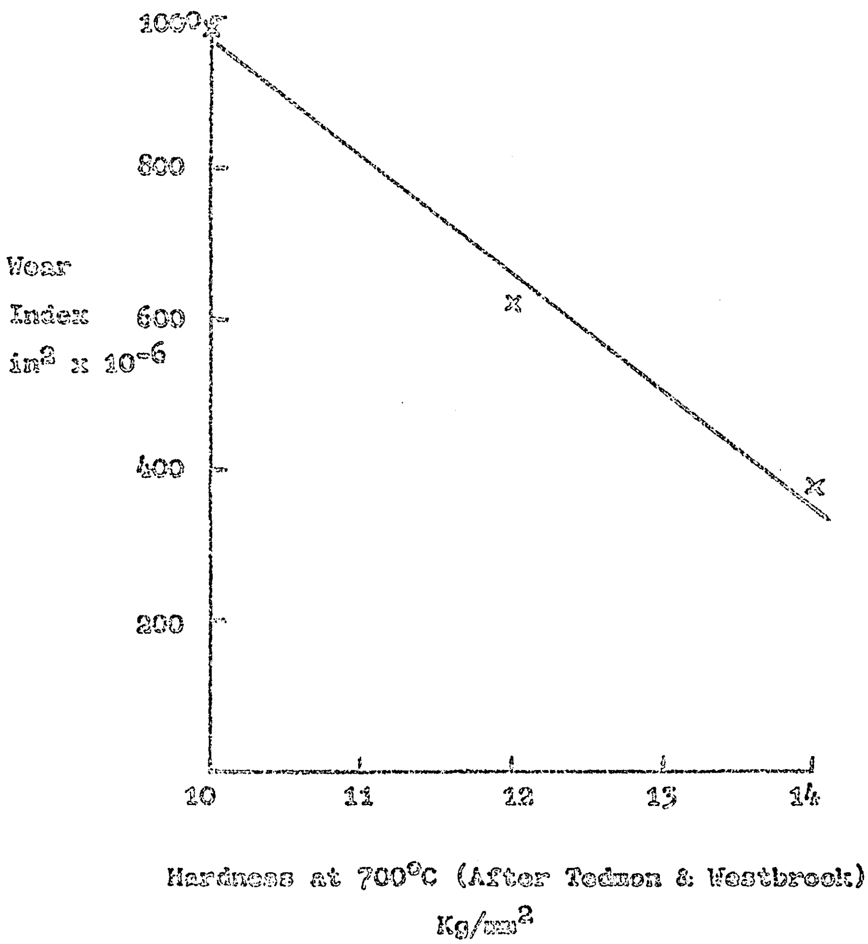


Figure 99

Wear Resistance of Fe-Cr Alloys as a Function of Hot Hardness

Correlation coefficients[54] are obtained for the mean values of each pair of variables entered and are presented in the form of a correlation matrix.

In the regression analysis part of the programme all the variables entered are considered and the regression equation is determined. An 'F' test[54] is then conducted on the variables collectively and if this is not significant at the 10% level the programme is ended.

If the 'F' test is significant each variable is subjected individually to a 't' - test[54] and those variables which are not found to be significant at the 10% level are rejected from the analysis.

Having removed any non-significant variables the 'F' test is repeated on the remaining ones. If there is no significant change in the residual the regression equation is printed and the analysis carried out as before at the 5% 1% and 0.1% levels.

If however there is a significant change in the residual the variables removed from the analysis are reconsidered and the one having the greatest effect on the residual sum of squares is reinserted in the analysis. This procedure is repeated until the 'F' - test holds and the regression equation is printed and the next significance level is considered.

The computer output designates each variable and for each one shows the coefficient (b_n), the standard error[54] of b_n and the ratio of these quantities, i.e. the 't' value. Also printed are the constant a and the number of degrees of freedom at each stage.

The final two columns of the output show the residual sum of squares and the residual mean square.

At each significance level the percentage sum of squares accounted for by the regression equation is shown.

5.4.12 Variables considered in regression analysis

Four variables were initially considered in the regression analysis. These were

(1) The iron content of the alloy present as iron-carbide designated [Fe]

(2) The chromium content of the alloy present as chromium carbide designated [Cr]

(3) The content of secondary hardening elements Mo, W, V and Nb present as carbide expressed in terms of a molybdenum equivalent and designated [Mo]

(4) The total alloy content present in solid solution designated [SS]

The regression analysis was then carried out to express the RWI at a given hardness in the form

$$RWI = a + b[Fe] + c[Cr] + d[Mo] + e[SS]$$

The values of the expressions in the square brackets were determined as follows.

Wetter's work[30] showed that the effect of tungsten and molybdenum on die wear could be expressed in terms of a tungsten equivalent. It was decided therefore in the present analysis to express the tungsten, molybdenum vanadium and niobium contents of any steel in the form of an equivalent molybdenum content. It was first assumed that the carbides formed by these elements were W_2C, Mo_2C, V_4C_3 and NbC. A similar assumption was made by Crafts and Lamont[55] to calculate the as tempered hardness of alloy steels from their composition.

The molybdenum equivalent of an element was then expressed as the weight percentage of molybdenum which would combine with the same amount of carbon as that in combination with the element under consideration. The total molybdenum equivalent of an alloy was given by the sum of the individual equivalents.

It was further assumed that carbon in any steel would first combine with molybdenum. Thus the weight percentage of the molybdenum equivalent in combination with carbon [Mo] was calculated. In most alloys sufficient carbon was present for [Mo] to have the same value as the total equivalent molybdenum content.

In those alloys with carbon remaining after the equivalent molybdenum content was satisfied it was assumed that carbon was next taken up by chromium to form the carbide Cr_7C_3. In this way the value of [Cr] was calculated.

In the event of any carbon remaining after the chromium demand was satisfied it was assumed that Fe_3C was formed and hence [Fe] was calculated.

This method of allocating the carbon to the various alloying elements is similar to that used by Crafts and Lamont[55]. Since these authors showed that the hardness of steels after tempering at 700°C could be expressed in terms of composition it is reasonable to expect that a similar approach will predict wear resistance since this has been shown to be correlated with hardness after tempering at 700°C

The solid solution variable [SS] was then determined as the sum of all alloying elements present, except iron, which were not combined with carbon.

Figures 100 and 101 (p 154) show the RWI at 395Hv30 plotted as a function of the total chromium content and total equivalent molybdenum content for all the steels investigated.

The trend line through the points in both figures suggests that the relationship between wear resistance and the elements concerned is not a linear one but is related to some fractional power of the element.

Because of this fractional powers of the variables [Cr] and [Mo] were investigated in the regression analysis performed.

Tables 35, 36, and 37 show typical computer outputs obtained for one particular regression analysis carried out.

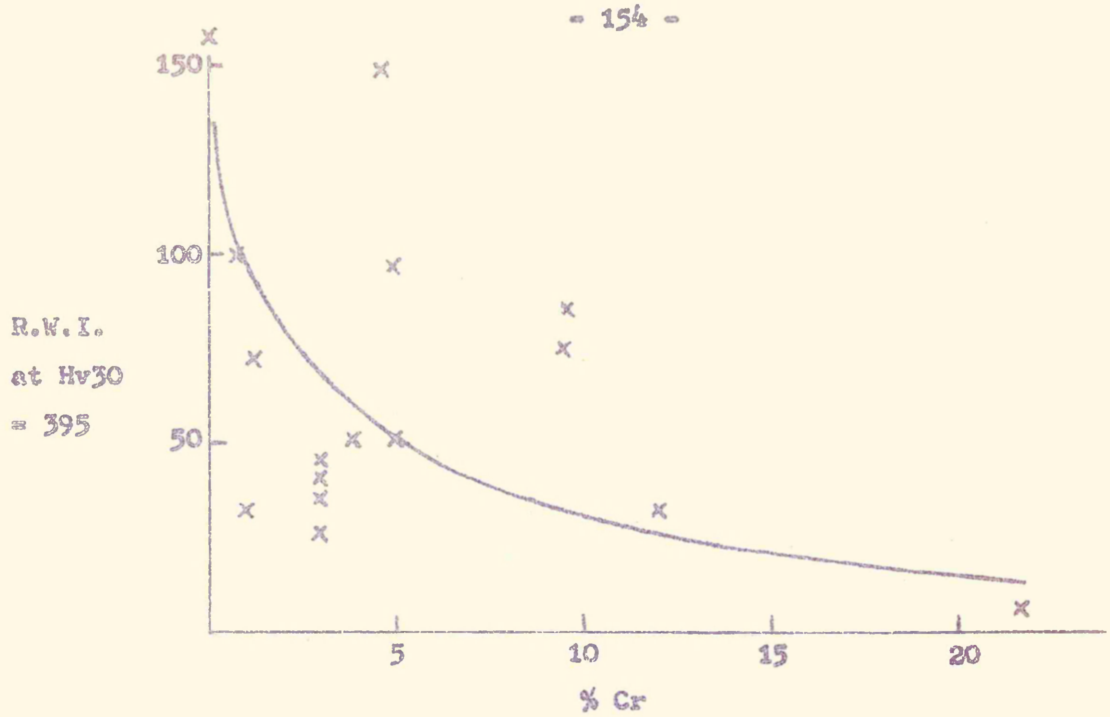


Figure 100

R.W.I. v. Chromium Content

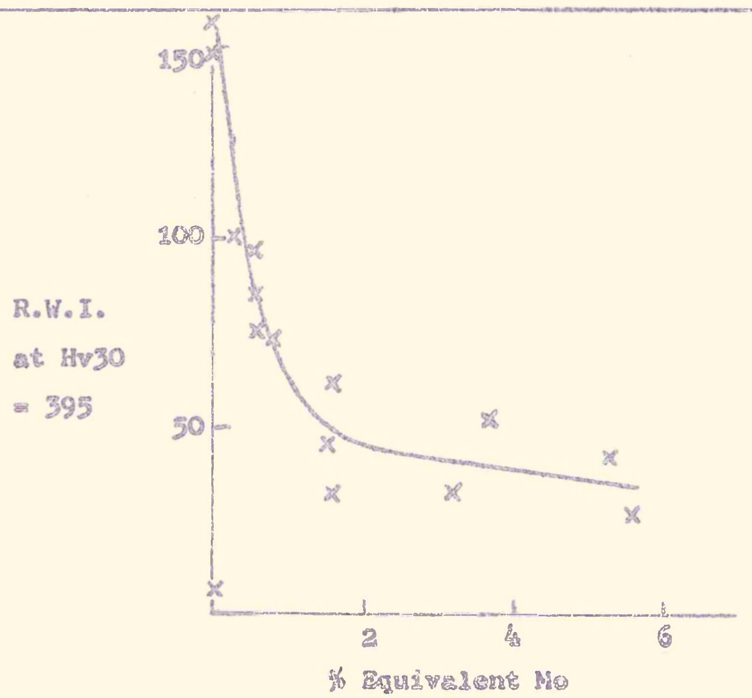


Figure 101

R.W.I. v. Equivalent Molybdenum Content

Table 35

	Var	Means	Minimum	Maximum	Sigma	P-Means
C	1	.3682/ 00	.1000/ 00	.6500/00	.1542/00	
Si	2	.4029/ 00	.1000/ 00	.1000/01	.2407/00	
Mn	3	.1103/ 01	.2500/ 00	.9800/01	.2247/01	
Ni	4	.9706/ 00	.0000/ 00	.4800/01	.1534/01	
Cr	5	.5235/ 01	.0000/ 00	.2170/02	.5388/01	
Mo	6	.9729/ 00	.0000/ 00	.3000/01	.1088/01	
W	7	.7647/ 00	.0000/ 00	.1000/02	.2418/01	
V	8	.2218/ 00	.0000/ 00	.1000/01	.3263/00	
Nb	9	.5882/-01	.0000/ 00	.1000/01	.2425/00	
Co	10	.1765/ 00	.0000/ 00	.3000/01	.7276/00	
RWI (300)	11	.5065/ 02	.1000/ 02	.1100/03	.3178/02	
" (350)	12	.5641/ 02	.9000/ 01	.1230/03	.3462/02	
" (395)	13	.6788/ 02	.6000/ 01	.1580/03	.4196/02	
(C)	14	.3682/ 00	.1000/ 00	.6500/00	.1542/00	
(SS)	15	.6078/ 01	.8500/ 00	.2900/02	.7272/01	
(Mo)	16	.1681/ 01	.0000/ 00	.5600/01	.1827/01	
(Cr)	17	.2005/ 01	.0000/ 00	.6500/01	.2000/01	
(Mö)	18	.1826/ 01	.1000/-03	.5600/01	.2008/01	
(Cr)	19	.1931/ 01	.1000/-03	.6500/01	.1990/01	
(Fe)	20	.1719/ 01	.0000/ 00	.1060/02	.3362/01	
(SS)	21	.6268/ 01	.8500/ 00	.2900/02	.7258/01	
3✓ (Mö)	22	.9835/ 00	.4643/-01	.1776/01	.5769/00	
2✓ (Cr)	23	.1075/ 01	.1000/-01	.2550/01	.9072/00	
3✓ (Cr)	24	.9128/ 00	.4643/-01	.1866/01	.7000/00	
3✓ $(Cr)^2$	25	.1295/ 01	.2154/-02	.3483/01	.1170/01	

Table 36

CORRELATION MATRIX N = 17

1	2	3	4	5	6	7	8	9	10	11	12	13	
-300													
230	-323												
-361	-196	512											
-191	-167	773	390										
-678	550	-213	423	-146									
-035	-128	-115	-213	-125	-170								
-086	373	-171	-255	-174	375	342							
220	-110	-057	-163	-107	-112	-028	536						
-063	532	-057	-163	-107	480	025	615	-062					
399	036	-344	-382	-377	-451	-278	-501	-143	-224				
372	044	-365	-385	-386	-425	-288	-507	-174	-197	991			
340	032	-386	-394	-404	-394	-300	-509	-202	-165	940	976		
[illegible]	-300	230	-361	-191	-678	-035	-086	220	-063	399	372	340	14
-265	-116	813	699	882	158	-090	-071	-184	057	-536	-533	-537	15
-272	331	-247	-147	-261	465	652	865	264	509	-574	-580	-580	16
227	-209	558	-090	606	-520	-286	-321	128	-258	106	063	017	17
-201	308	-242	-180	-266	404	589	915	454	445	-558	-571	-580	18
208	-228	569	-065	621	-516	-288	-390	025	-250	124	085	043	19
715	-244	-103	-111	-442	-370	-172	-333	-132	-132	547	563	576	20
-269	-105	808	689	877	171	-070	-030	-160	081	-558	-555	-559	21
-398	362	-416	-114	-313	553	444	777	342	339	-602	-622	-631	22
272	-155	403	-214	467	-568	-326	-366	109	-303	212	163	112	23
305	-123	339	-258	397	-580	-336	-351	138	-319	244	190	133	24
244	-183	463	-165	528	-551	-314	-378	079	-285	181	136	089	25

14	15	16	17	18	19	20	21	22	23	24	25
-265											
-272	-092										
227	266	-489									
-201	-131	978	-417								
208	288	-529	994	-479							
715	-333	-407	-314	-405	-295						
-269	999	-054	252	-091	272	-347					
-398	-190	883	-482	893	-532	-447	-158				
272	091	-541	965	-464	958	-225	075	-459			
305	017	-537	929	-451	916	-177	001	-424	992		
244	162	-540	986	-473	984	-260	146	-489	994	972	

Table 37

Regression Analysis

Var	B	SE(B)	T	Const	DF	RES SS	RES MS
13					16	.2917/05	.1761/04
13					13	.3304/04	.2541/03
21	-.3992/01	.5575/00	7.16				
22	-.6060/02	.7747/01	7.82				
24	-.1314/02	.6304/01	2.08	.1645/03			88.27%
	10.0% SIGNIFICANCE LEVEL						
	No Change						
	5.0% SIGNIFICANCE LEVEL						
13					14	.4408/04	.3149/03
21	-.3907/01	.6190/00	6.31				
22	-.5366/02	.7788/01	6.89	.1451/03			84.35%
	1.0% SIGNIFICANCE LEVEL						
	No Change						
	0.1% SIGNIFICANCE LEVEL						
	No Change						
	END OF ELIMINATION						

Some of the variables shown (14-18) in table 35 are not discussed here since they formed part of an analysis not reported.

5.1.13 Results of the regression analysis

In most of the regression equations obtained the two most significant variables were $[SS]$ and $[Mo]^{\frac{1}{3}}$. Whilst $[Cr]$ to the fractional power of $\frac{1}{2}$ or $\frac{2}{3}$ appeared in the preliminary regression equation it was invariably not significant at the 5% level. The iron carbide term $[Fe]$ was in all cases not significant at the 10% level.

This indicates that cementite, under the forging conditions used in the wear test, is not a stable enough carbide to impart a high degree of wear resistance. This is in line with the very poor performance of the plain carbon steel (material 1) in the wear tests.

The position with regard to chromium as a carbide former is not absolutely clear but it appears that it plays only a minor role in conferring wear resistance.

Table 38 below shows the regression equations obtained for RWI at hardness levels of 300, 350 and 395 Hv30, together with the sum of squares accounted for.

Table 38

Regression Equations for RWI

Hardness Level Hv30	Regression Equation	Sum of Squares Accounted For. -%
300	$RWI = 107.0 - 39\,[Mo]^{\frac{1}{3}} - 2.9\,[SS]$	79.95
350	$RWI = 119.4 - 44\,[Mo]^{\frac{1}{3}} - 3.2\,[SS]$	82.45
395	$RWI = 145.0 - 54\,[Mo]^{\frac{1}{3}} - 3.9\,[SS]$	84.35

Separate regression equations were determined for each hardness level to investigate whether the coefficients of the terms $[Mo]^{\frac{1}{3}}$ and $[SS]$ changed significantly from one hardness to another. This was thought to be

/possible

possible if high initial tempering temperatures (low initial hardness) dispersed the carbides thus making them less effective in promoting wear resistance whilst the contribution of solid solution hardening to wear resistance would be expected to remain constant irrespective of the initial tempering treatment.

Reference is made to this effect later in section 5.1.14.

The success of the regression equations in predicting wear resistance from composition is shown in figure 102 (p160) in which the observed wear resistance for the die steels investigated is plotted against the predicted wear resistance. The regression analysis technique was also employed to predict the wear to be expected in any die steels in terms of composition and initial die hardness.

The following expression accounted for 81.34% of the sums of squares.

$$\text{Wear} = 1798 - 428\,[\text{Mo}]^{\frac{1}{3}} - 31.6\,[\text{SS}] - 1.8\,H$$

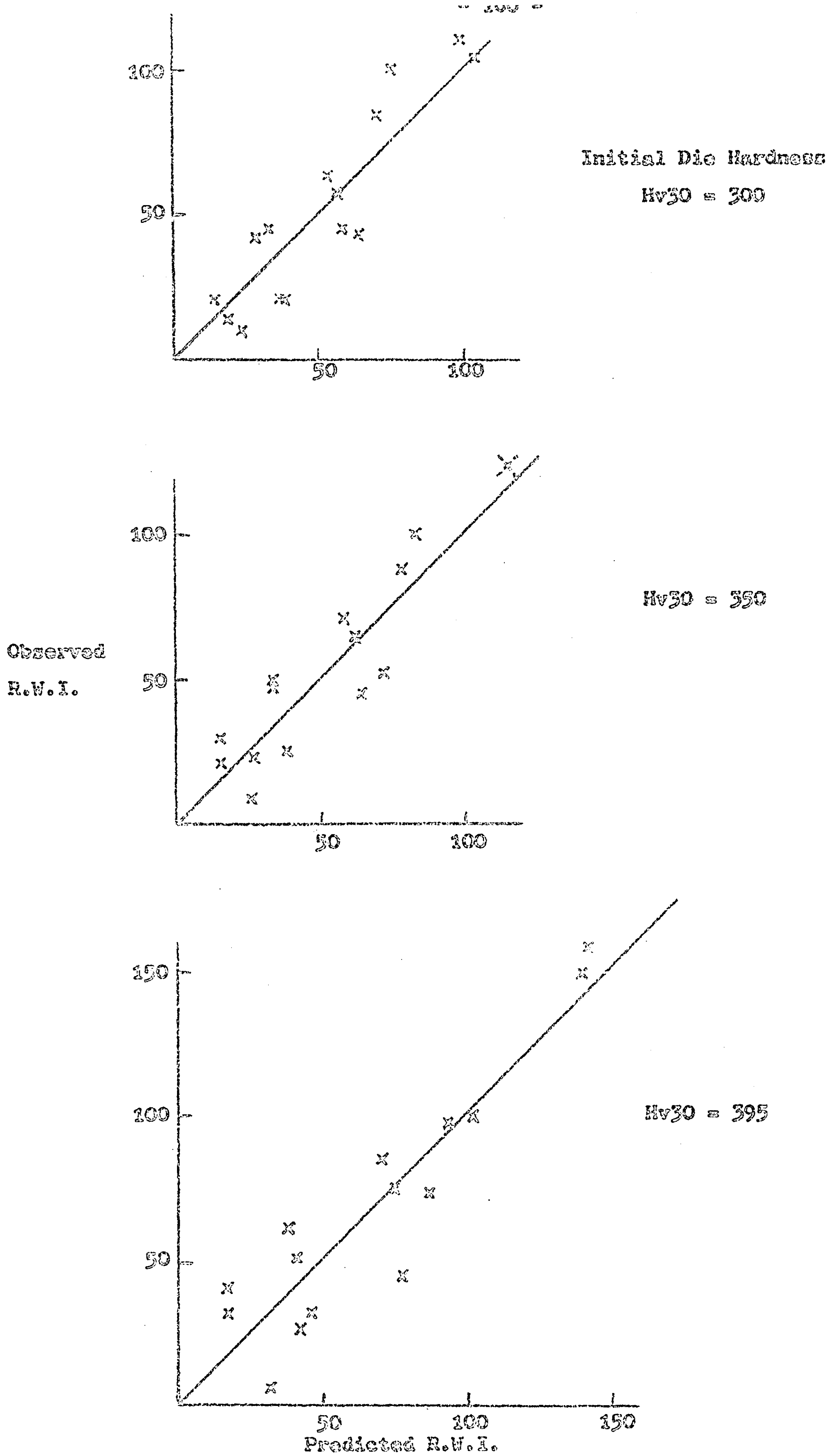
Where H is the hardness on the Vickers scale.

All the terms in the above expression were significant at the 1% level.

The relationship between the predicted and observed wear is shown in figure 103 (p161).

Some of the assumptions made to derive the regression equations were necessarily of an arbitrary nature.

In principle it would have been better to include all the elements present in the materials investigated in the regression analysis. Since however only 15 materials were used to provide the wear data, the inclusion of a large number of variables, each of which represents the loss of one degree of freedom in testing significance, would have made testing for significance more difficult. It was considered better therefore to group the elements as far as possible.



<u>Figure 102</u>

Agreement Between Observed and Predicted R.W.I. Values

Superfixes refer to die material

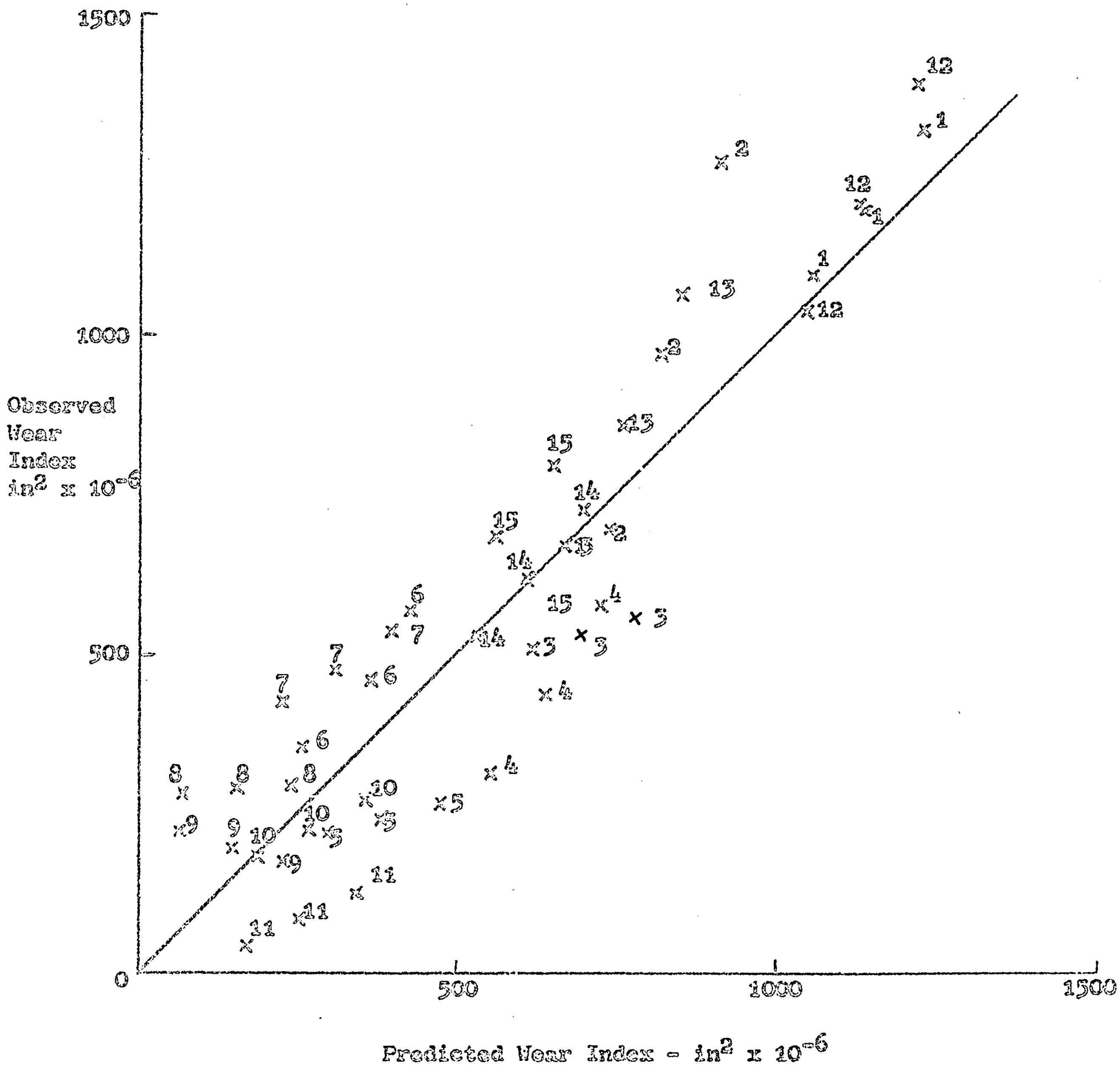


Figure 103

Agreement Between Observed and Predicted Wear Index

It is in this grouping that the arbitrary nature of the variables arises. The carbide forming elements were grouped according to the stability of the carbides as already explained[55]. In practice the elements considered would not be present in only one carbide since a good deal of substitution of one element for another is possible in carbides.

Thus the iron carbides present in the alloys would also contain chromium and molybdenum for instance, so that the influence of these elements should really be expressed by two coefficients, one representing the strength of the influence in stabilising the cementite and the other representing the influence of the carbide formed by the element in question. To separate the two effects however would require far more data than were available as well as a knowledge of the distribution of various elements in the carbides present.

Similarly some error will be introduced into the analysis by assuming that all elements present in solid solution have the same strengthening effect.

In spite of these difficulties, and the known errors introduced, the equations developed were quite successful in explaining the manner in which various elements influence wear resistance and the magnitude of the effects.

A more accurate assessment of the effect of individual elements could only be obtained by a comprehensive series of experiments on alloys specifically designed for the purpose.

Although the present analysis of composition on wear resistance is admittedly incomplete and therefore only approximate it does allow a good prediction of wear resistance to be made from composition alone. It also shows conclusively that stable carbide forming elements are of paramount importance in developing wear resistance. In addition the contribution of solid solution strengthening to wear resistance has been demonstrated.

The dependance of wear resistance on the cube root of the equivalent molybdenum content is interesting to note since Nordberg and Aronsson[57] have shown that the hardness of a 0.2C, 1.2Mn steel with niobium additions is linearly related to the cube root of the niobium content. A possible reason for this is discussed later in section 5.1.16.

5.1.14 <u>The influence of initial die hardness on wear resistance</u>

Figures 45 to 60 (pp 71-74) show that in general the wear resistance of the materials investigated increased with increasing initial die hardness.

The extent to which wear resistance depends on initial hardness is indicated by the slope of the regression lines given in table 16.

The slopes vary from - 9.44 for the plain carbon steel (material 1) to + 0.51 for material 9, and broadly speaking the dependance of wear resistance on hardness diminishes with increasing alloy content.

There would appear to be two possible explanations for this effect.

Firstly as the alloy content of a material increases the thermal conductivity of the material will decrease. This will lead to higher surface temperatures being reached during forging. Figures 69 - 72 (pp94 -97) show that as the tempering temperature of a die material is increased the initial die hardness has a diminishing influence on the hardness after tempering, which has been shown to correlate with high temperature strength.

Thus it is possible that as the alloy content of the materials increases the tempering of the surface during forging will increase and the effect of the initial hardness will be diminished.

A second possible explanation is that in addition to the above effect the relative contribution made to wear resistance in high alloy materials by solution hardening is greater than in low alloy materials. The influence of the solution hardening effect will be independent of the initial tempering conditions of the material which influence only the carbide type, size and distribution. Thus for alloys with a high solid solution hardening contribution to wear resistance it is possible that the influence of initial hardness is reduced.

This second possibility is not supported by the regression equation developed to show how wear is influenced by composition and hardness nor by the separate regression equations at different hardness levels.

In the latter equations (table 38) the relative contributions made to wear resistance by carbide formers and solid solution hardening elements remain in the same proportion irrespective of the initial die hardness.

It appears therefore that the relationship between wear resistance and initial die hardness is more likely to be governed by the thermal conductivity of the die material.

5.1.15 <u>Wear of surface treated dies</u>

The nitrided dies tested proved to have the most wear resistant surface of all materials. This is undoubtedly due to the stability of the nitrided surface at the working temperature. Figure 75 (p 100) shows that the surface hardness of nitrided dies was still above 750 Hv30 after use. Norén and Kindbom[56] have published curves which show that the hardness of nitrided surfaces is about 270 Hv30 at 700°C, which is about 2½ times the hardness of most alloy die steels at this temperature.

It is almost certain therefore that nitrided surfaces resist wear by the prevention of penetration of scale particles into the die surface.

Whilst Sulfinuz treatment of dies reduced the wear occuring the effect was less pronounced than with nitrided dies. This reduction in wear is again probably attributable to the production of a stable surface layer which reduces scale penetration.

The susceptibility of Sulfinuz treated surfaces to heat-checking and the fact that nitrided surfaces are much more wear resistant suggests that Sulfinuz treatment of dies is not likely to prove an attractive proposition to drop forgers.

5.1.16 Influence of microstructure on wear resistance

The regression equations relating wear to composition showed that wear resistance was governed principally by the amount of strong carbide forming elements present in a die material.

Such elements will influence mainly the type, amount and size of carbides present, and also the stability of carbides during the tempering which occurs in a wear test.

An attempt has been made therefore to investigate whether a quantitative relationship exists between wear resistance and microstructure.

The microstructures present in dies before and after testing were too fine to be resolved under an optical microscope. Attempts to obtain replicas, for examination under an electron microscope, from the worn region of dies were unsuccessful due to the irregular nature of the surface.

Since it has been shown that wear resistance correlates closely with the hardness of dies after tempering at 700°C [see figure 96 (p143)] the following procedure was adopted to investigate the relationship between microstructure and wear resistance.

Samples of materials 2, 4 and 9 were hardened to 350 or 400 Hv30 and retempered for ½ an hour at 700°C to simulate the tempering effects which occur during testing.

Figures 104 -106 (pp.167-169) show typical examples of optical and electron micrographs obtained from such samples.

Ansell[59] has shown on theoretical grounds and Greday and Lutts[60] have confirmed experimentally that the strength of steels containing a precipitated phase is proportional to the reciprocal of the square root of the inter-particle spacing.

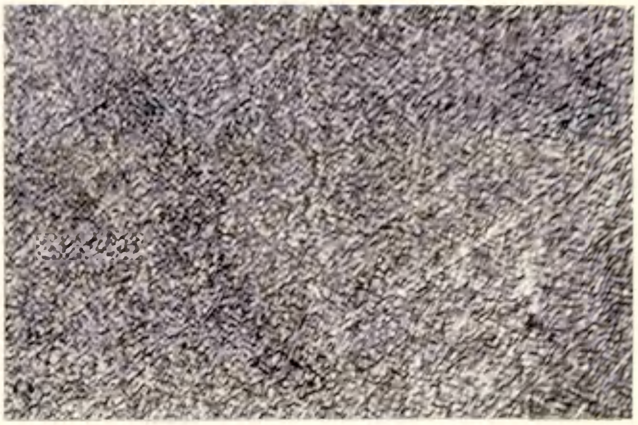
Since wear resistance has been shown to depend on material strength it is interesting to investigate whether wear resistance is a function of inter-particle spacing. To measure the inter-carbide spacing an image of a photographic negative was projected onto a screen on which was drawn a square grid pattern. The interparticle spacing of carbides which intersected the grid lines was measured and converted to a true spacing from a knowledge of the magnification of the negative and the projected image.

Table 39 shows the spacings measured together with the wear index for the samples examined.

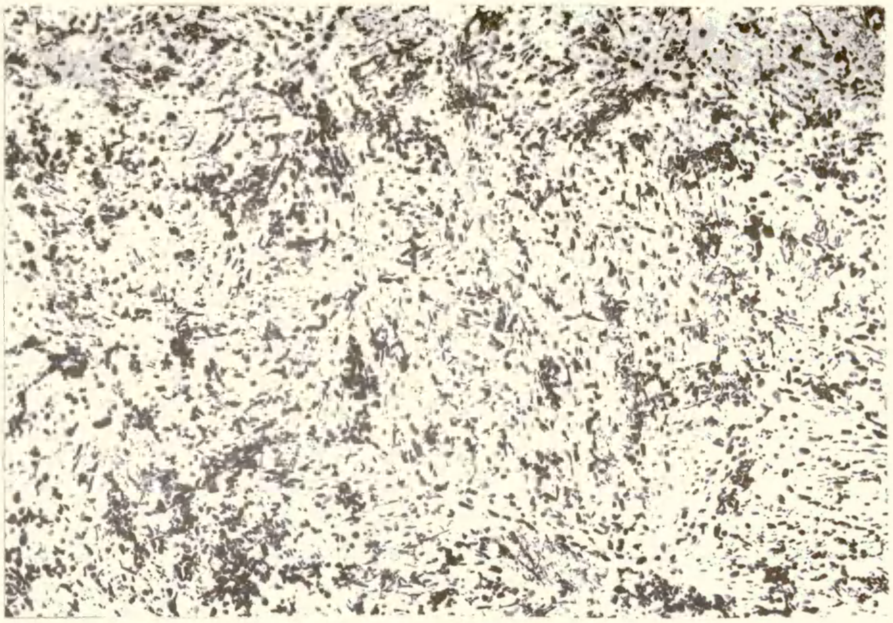
Table 39

Inter-carbide Spacing and RWI for Three Die Steels

Material	Initial Hardness -Hv30	S = Inter-carbide Spacing-microns	$S^{-\frac{1}{2}}$ Microns$^{-\frac{1}{2}}$	Wear Index -in^2 x 10^{-6}
2	402	0.40	1.58	648
4	402	0.33	1.74	291
4	354	0.36	1.67	422
9	355	0.31	1.80	202



x 750
optical

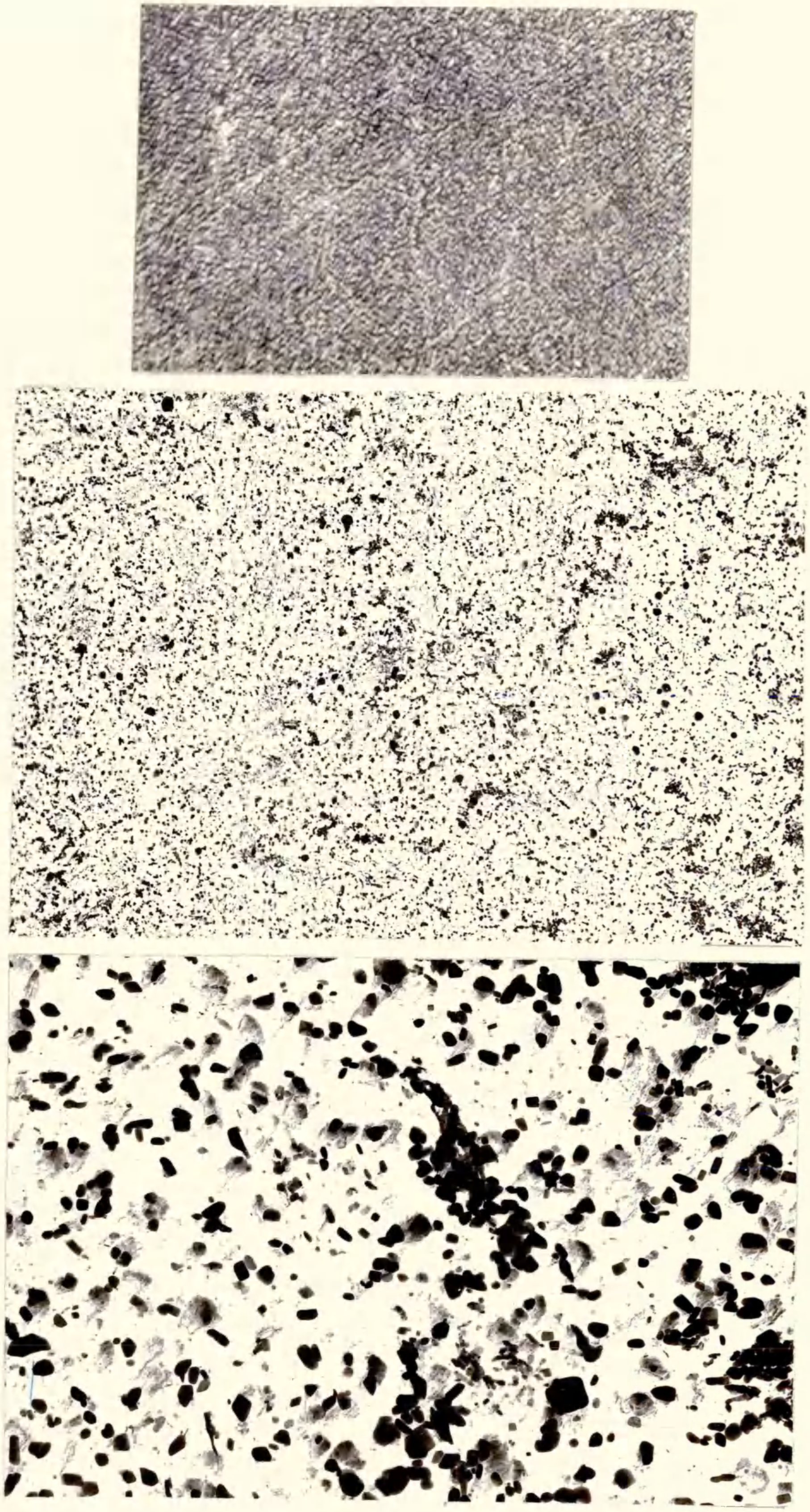


x 2000
electron

x 10,000
electron

Figure 104

Micrographs of No. 5 Die Steel H & T to 402 Hv30
and retempered $\frac{1}{2}$ hr. at 700°C.



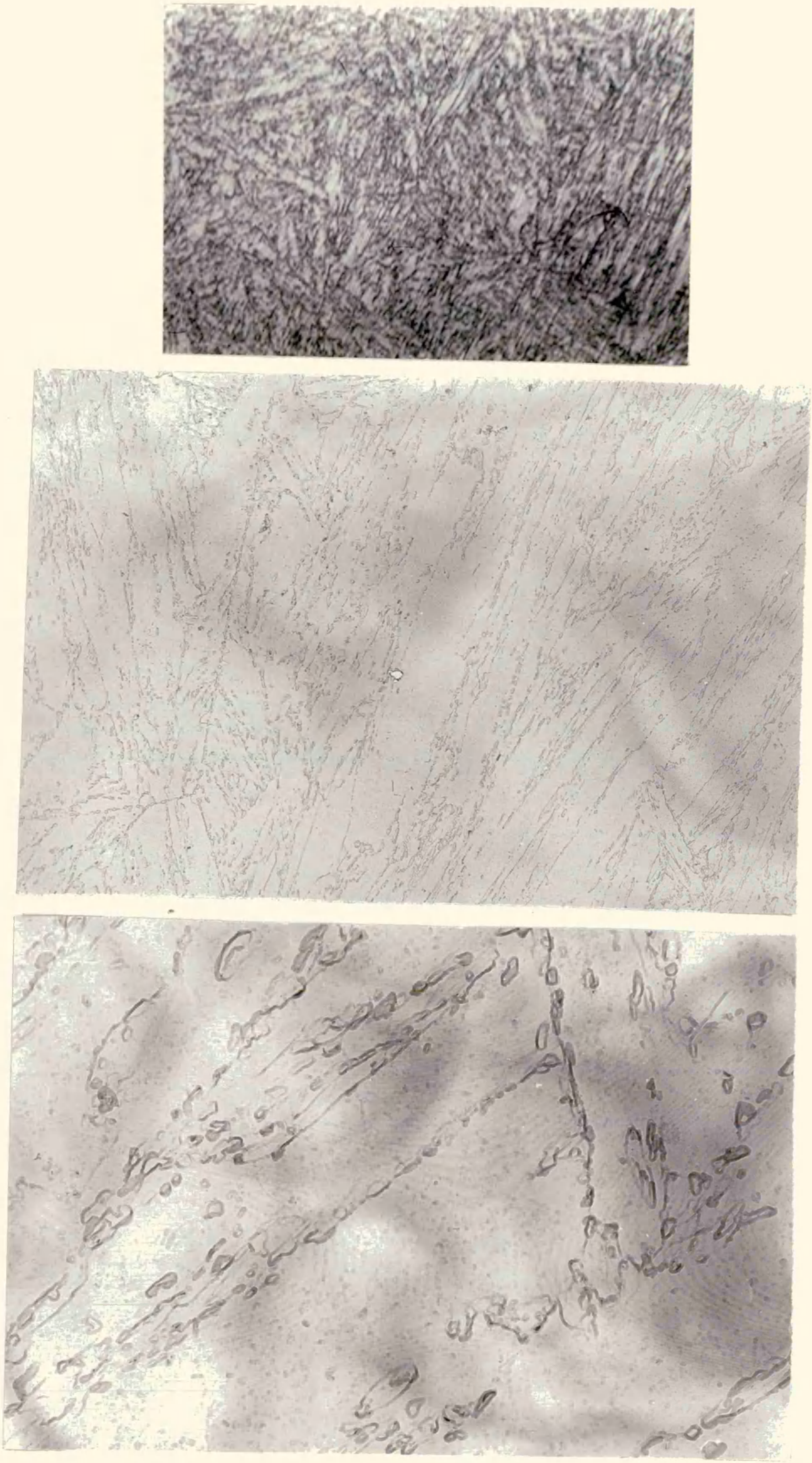
x 750
optical

x 2000
electron

x 10,000
electron

Figure 105

Micrographs of En40C H & T to 402 Hv30
and retempered ½ hr. at 700°C.



x 750
optical

x 2000
electron

x 10,000
electron

<u>Figure 106</u>

Micrographs of Material 9 H & T to 402 Hv30
and retempered ½ hr. at 700°C.

Figure 107 (p 171) shows wear index as a function of the reciprocal of the square root of the intercarbide spacing.

The agreement between the predicted and observed effects is very good the correlation coefficient between the reciprocal of the square root of the intercarbide spacing and the wear index being -0.99. There is a further interesting observation regarding the close agreement between wear resistance and the theory of strengthening by a dispersed phase proposed by Ansell[59]. Ansell has shown that in the case of an alloy containing spherical particles the strength of the alloy is proportional to the cube root of the volume fraction of the second phase particles.

Since the volume fraction of carbides will be proportional to the amount of carbide forming elements present, expressed in the regression equations as the equivalent molybdenum content $[\overline{Mo}]$, Ansell's theory predicts that wear resistance should be proportional to $[\overline{Mo}]^{\frac{1}{3}}$ as was shown in section 5.1.13

5.2 Results of Works Trials

5.2.1. Agreement between observed die life in the forge and life predicted from laboratory tests.

Provided that erosive wear is the criterion of die failure in a production die the comparative data provided by the laboratory tests should be capable of predicting the relative lives of different die materials.

As an example the relative wear indices for No.5 Die Steel and En40C at 395 Hv30 are respectively 100 and 45. Thus the laboratory tests suggest that the life of En40C dies should be $\frac{100}{45}$ i.e. 2.2 times that of No. 5 Die Steel dies.

Figure 108 (p174) shows the die life ratios obtained in the works trials as a function of the predicted life ratios. The observed life ratios plotted are taken from the data presented in table 27.

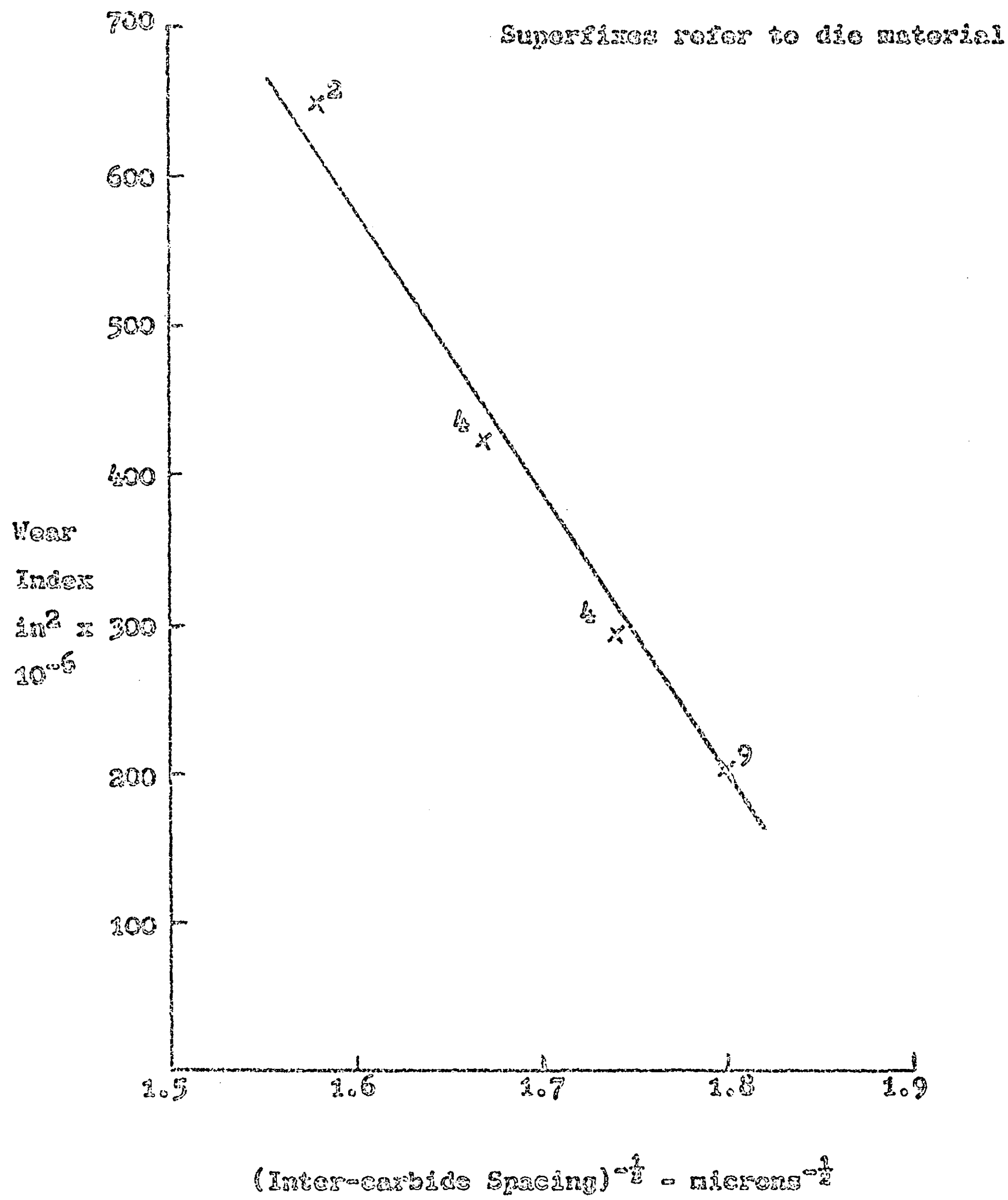


Figure 107

Influence of Inter-Carbide Spacing on Wear Resistance

In general the observed die life ratio in works trials fell below the value predicted from laboratory tests. It is noticable however that the highest life ratios obtained in works trials fell very close to the predicted values.

An analysis of die life data by Aston and Muir[58] has shown that die life tends to be a maximum when erosive wear is the cause of die failure rather than deformation, thermal cracking or mechanical cracking. It is possible that in only those cases showing the highest life ratio did the die fail purely due to erosive wear.

Although the works trials data are not extensive they serve to indicate that the laboratory wear test developed does correlate with service experience and is therefore useful in assessing potential die materials quickly and cheaply.

A criticism of previous investigations into potential new die steels was made in section 2.4 where it was pointed out that economic considerations were invariably neglected. The next section therefore deals with economic considerations.

5.2.2 Economic assessment of die materials studied

The total costs involved in producing a die are composed of material cost and machining cost. For the majority of dies the machining costs are much greater, usually 5-10 times, than the material costs. This is true of both die blocks and die inserts.

The only reliable way of comparing the economic performance of different die materials is on the basis of die cost per forging. Thus the important parameters necessary to compare die materials are

(1) the cost ratio of the two materials.

(2) the machining to material cost ratio for each die material

(3) the life ratio of the materials.

assuming for the time being that the machining costs for different materials are the same. Figure 109 (p 174) shows how the required life for economic use of a potential die material, compared with that of the normal material, varies with the machining to material cost ratio for the standard material.

It is clear from figure 109 that when the machining to material cost ratio is high quite moderate improvements in die life will justify the use of relatively expensive materials.

Unfortunately figure 109 over simplifies the problem involved in selecting new die materials since the work on die wear trials showed that usually die materials more resistant to wear than No 5 Die Steel are also more difficult to machine.

This is to be expected since the mechanism of wear proposed is, in effect, a micro-machining process. This suggests that the very properties of a die steel which confer wear resistance will reduce machinability. The great importance of changes in machining costs in selecting die materials is illustrated in figure 110 (p 175).

This figure shows the increase in machining costs which can be tolerated, compared with the costs of machining No. 5 Die Steel, for die materials 4, 6 and 9 as a function of the machining to material cost ratio for any die made from No. 5 Die Steel.

In drawing the curves shown in figure 110 the following values have been used for life and cost ratios.

Material	Cost ratio compared with No. 5 Die Steel	Life ratio compared with No. 5 Die Steel
4	1.34	1.3
6	2.19	1.2
9	2.87	1.65

The cost ratios are taken from the data given in table 26 whilst the life ratios are those indicated by the dashed line in figure 108.

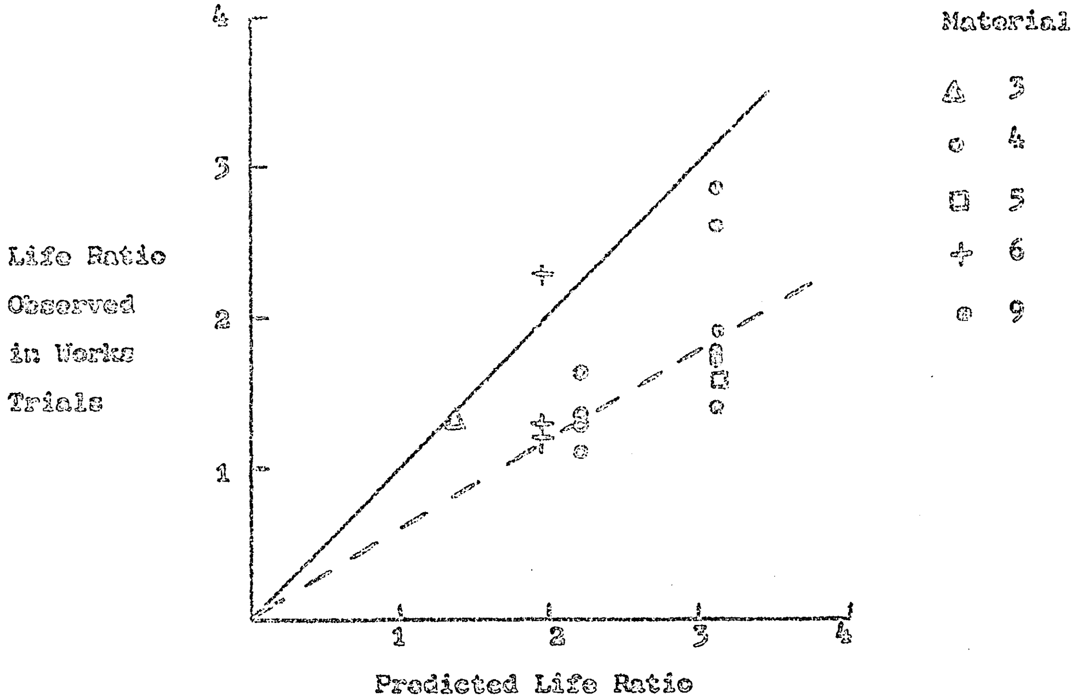


Figure 108

Relationship Between Predicted and Observed Die Life

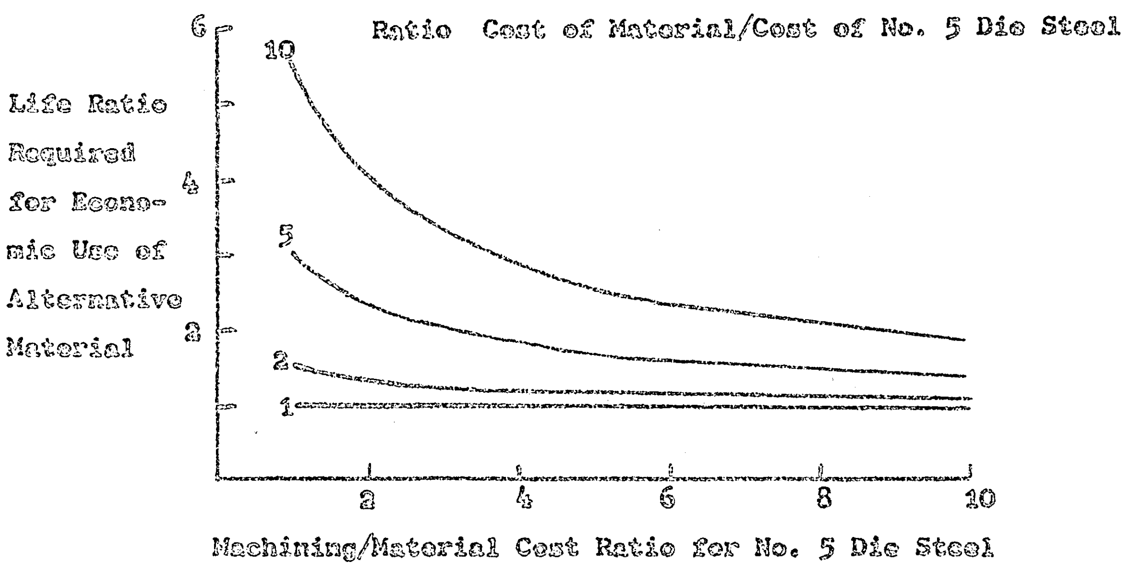


Figure 109

Influence of Machining to Material Cost Ratio
for No. 5 Die Steel on Life Ratio Required
for Economic Use of Other Materials

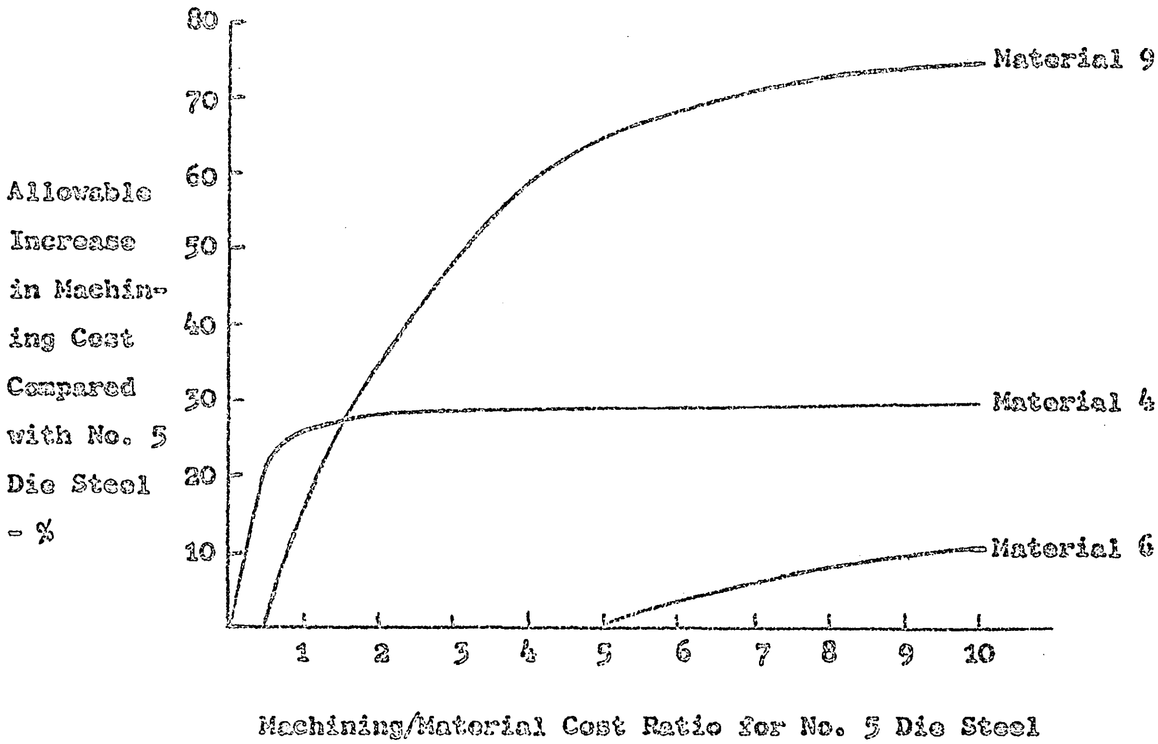


Figure 110

Influence of Machining to Material Cost Ratio for No. 5 Die Steel on Allowable Increase in Machining Costs for Economic Use of Other Materials

Figure 110 shows very clearly the situations in which the various die materials can be used more economically than No. 5 Die Steel. All situations which results in points lying above a given curve represent an increase in die cost per forging whilst points lying below a curve represent a reduction in die cost per forging.

There are several points of particular interest in connection with the curves shown in figure 110.

Firstly it can be seen that the curves becomes asymptotic to some value of increased machining costs. Thus material 4 clearly can never be an economic replacement for No. 5 Die Steel if the machining costs increase by more than 30% when material 4 is used. It follows from this that the selection of die steels by an individual forger will be influenced to a considerable extent by the methods used for die production. Forgers with facilities for electro-spark machining of dies will find the problems associated with poorer machinability of the new die materials easier to overcome than forgers who must rely entirely on milling techniques for die production.

This point is highlighted by the results of the works trials numbered 14 and 17 in table 27 (pp109 & 110).

In the former trial the dies were sunk by spark-erosion and the sinking costs of the new material compared with No. 5 Die Steel showed no increase, so that the economic advantages occuring from the increased life of the new material were fully realised.

In the latter trial however, where die sinking was done by milling, the improved life obtained from the new material was insufficient to offset the increased die production costs.

A further important point shown by figure 110 is that reductions in die costs can only be achieved by careful selection of die steels suited to specific applications. This conclusion contrasts with the present industrial practice of selecting an "all-round" material to be applied to all jobs.

5.2.3 Performance of alloys investigated

In all, fourteen potential die materials have been investigated. These comprised the steels 1-11 inclusive and the three nickel based alloys 16-18 inclusive.

In considering the application of any material the factors to be considered are,

(a) cost of the material

(b) wear resistance of the material

(c) machinability of the material

(d) other properties of the material such as room temperature strength and toughness

Although the hot strength and hence wear resistance of materials 11, 16, 17 and 18 is high the room temperature strength is relatively low compared with hardened and tempered martensitic steels as shown in table 40

Table 40

Mechanical Properties of Some Die Materials Investigated

Material	UTS at 20°C tonf/in^2	.2% PS at 20°C tonf/in^2	Ratio $\frac{.2\%\ PS}{UTS}$
2 No. 5 Die Steel	82.4	76.0	0.92
16 Nimonic 90	80	52.0	0.65
17 Nimocast 713	55	48	0.87
18 Inco 901	78	58	0.74

Table 40 shows that the yield strength of the Nickel based alloys is low compared with mantensitic steels. This means that such alloys will be limited in use to applications where die stresses are relatively low.

A further drawback to the application of these alloys is their high cost and very poor machinability. Figure 109 (p174) showed that expensive die materials were best justified, where the machining costs of the present die materials were high compared with material costs. In such situations however the introduction of Nickel based alloys is likely to increase machining costs considerably and thus invalidate the use of such materials.

Calculations such as those used to produce figure 109 indicate that the use of Nickel based alloys as die materials will be very limited. Possible applications are as loose pegs in dies where loads are relatively low.

Similar arguments to those outlined apply to material 11. The one application in which this was tried as a die (works trial number 19) confirmed that its low room temperature strength prohibits its use as a die material, since the die collapsed after producing only a few forgings. So far as the martensitic alloys are concerned all can develop sufficient strength for use as die materials.

Figure 79 (p106) indicates that some of the materials will prove too brittle for general application in hammer dies, as was confirmed in some of the works trials.

However they may be suitable as press dies. In the latter case their value must be assessed according to the relative cost and wear resistance, as indicated in section 5.2.2.

In those cases where erosive wear dictates die life in press dies the results of the present investigations suggest that the maximum reduction in die costs will be achieved by the use of nitrided dies, since the cost

/of nitriding

of nitriding dies ($\approx$1/- per pound) is very small compared with the improvement in life. The present practice of using material 6 as the base material for nitriding is open to question since material 4 is much cheaper and appears to behave just as well in the nitrided condition.

In the case of hammer dies the brittleness of many of the materials investigated (see figure 79) will limit their application as already stated. One material however, material 9, has been shown to possess very good wear resistance coupled with good impact properties, and should find widespread application in the drop forging industry.

6. CONCLUSIONS

(1) A method of wear testing of die steels has been developed which closely simulates the stress and temperature cycles to which production forging dies are subjected.

In the test, cylindrical slugs are upset forged between flat test dies. The wear occurring during a test has been expressed as a wear index (W.I.) which is proportioned to the amount of metal removed from a die, or a relative wear index (RWI), which is equivalent to the amount of wear on any material expressed as a percentage of that occurring on a reference material, No. 5 Die Steel, under the same conditions of test.

The wear resistance of the die steels assessed by the test has been shown to correlate closely with the performance of the steels under production forging conditions.

(2) Wear of dies has been shown to occur by abrasion of the die surface by scale particles derived from the forging stock.

When forging mild steel the amount of wear occurring is sensitive to forging temperature since the latter affects the amount of scale formed on the stock (S), the yield strength of the stock (Y) and the die surface temperature, which in turn affects the yield strength of the die surface (σ).

Between 900 - 1050°C the amount of wear on a die is quantitatively related to the function $Q = S \times Y/\sigma$. Above 1050°C however the amount of wear falls rapidly below that predicted, due to a change in the nature of the scale. The wear at 1200°C is only about one third of that at 1100°C.

(3) The influence of forging stock on die wear depends on the amount of scale formed (S), the adhesion index of the scale (A) and the yield strength of the stock (Y).
The amount of wear caused by a given stock is proportional to the product S x A x Y for that stock.

(4) The influence of colloidal graphite lubrication on die wear has been shown to be more complicated than was hitherto assumed.
In dies which are predominantly flat and where the forging operation approximates to free upsetting, as in the wear test developed, lubrication can increase the total wear on the die due to an increase in the area over which sliding and hence wear occurs.
However in dies in which lateral movement of metal is restricted by vertical die walls lubrication can reduce the wear on the flash lands by mechanically protecting them from the abrasive action of scale particles.

(5) Multiple regression analysis has been used to establish a relationship between the wear resistance and composition of die steels.
The wear resistance depends on the amount of strong carbide forming elements W, Mo, V and Nb present as carbides and on the amount of other elements which contribute to solid solution hardening of the die steel matrix. The total amount of carbide forming elements has been expressed as an equivalent molybdenum content $[\overline{Mo}]$.
The following regression equations have been shown to predict closely the behaviour of a wide variety of steels.

(a) RWI at 300 Hv30 = $107 - 39[\overline{Mo}]^{\frac{1}{3}} - 2.9[SS]$.

(b) RWI at 350 Hv30 = 119 - $44[\mathrm{Mo}]^{\frac{1}{3}}$ - 3.2 [SS].

(c) RWI at 395 Hv30 = 145 - $54[\mathrm{Mo}]^{\frac{1}{3}}$ - 3.9 [SS].

where [SS] is the sum of all elements present in solid solution.

A further regression equation was developed to include the effect of initial die hardness on die wear. The equation derived was

$$W.I. = 1798 - 428[\mathrm{Mo}]^{\frac{1}{3}} - 31.6[\mathrm{SS}] - 1.8\,H$$

where H is the initial die hardness on the Vickers scale.

(6) Wear resistance has been shown to be correlated with microstructure. The amount of wear occurring is inversely proportional to the function $1/d^{\frac{1}{2}}$ where d is the average intercarbide spacing in a die material after reheating to 700°C for 10 minutes.

(7) The economic factors which govern the selection of die materials have been analysed and a method of calculating the effects on die costs of changing the die material has been presented. The economic factors involved are:-

(1) the die material cost

(2) the machining cost

(3) the die life

By using the method of assessment developed it is possible to predict the likely economic effects of changing from one die material to another.

(8) It has been shown that there are already in existence commercially available steels which if substituted for die steels presently in use will lead to substantial reductions in die costs under a wide range of forging corrections. The nickel base alloys whilst showing excellent wear resistance are likely to be limited in their application as die materials due to their low room temperature yield strength.

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APPENDIX I

Clutch Circuits Used in Automatic Forging Press

During the early period of development of the automatic feeding mechanism for the forging press, occasional forging strokes were made whilst the tongs were still under the press.

To obviate this, an air circuit was designed which allowed the press clutch to engage only if the tongs were in a safe position under the heating coil. The circuit used initially is shown in Figure A1 (p. A2) and operated as follows.

When the tongs moved under the press, the lever L_2 of valve 5 was released, opening the line OM by air pressure applied through the path RQV. When the transfer tongs returned to the safe position under the coil, lever L_2 was closed again and air passed through the path RPOML operating valve V_3 to allow air through path WHI to bring in the clutch by operating the cylinder C. At the same time, air operating at point T through the path RPOMT operated valve V_2 allowing the clutch cylinder to exhaust through path XFE.

When the clutch was engaged, the press ram moved down for the forging stroke and released lever L_1 allowing air to pass through path ACS to change over valve V_2 to open line DF. At the same time, air applied along path ACU changed over valve V_4 to open line MN.

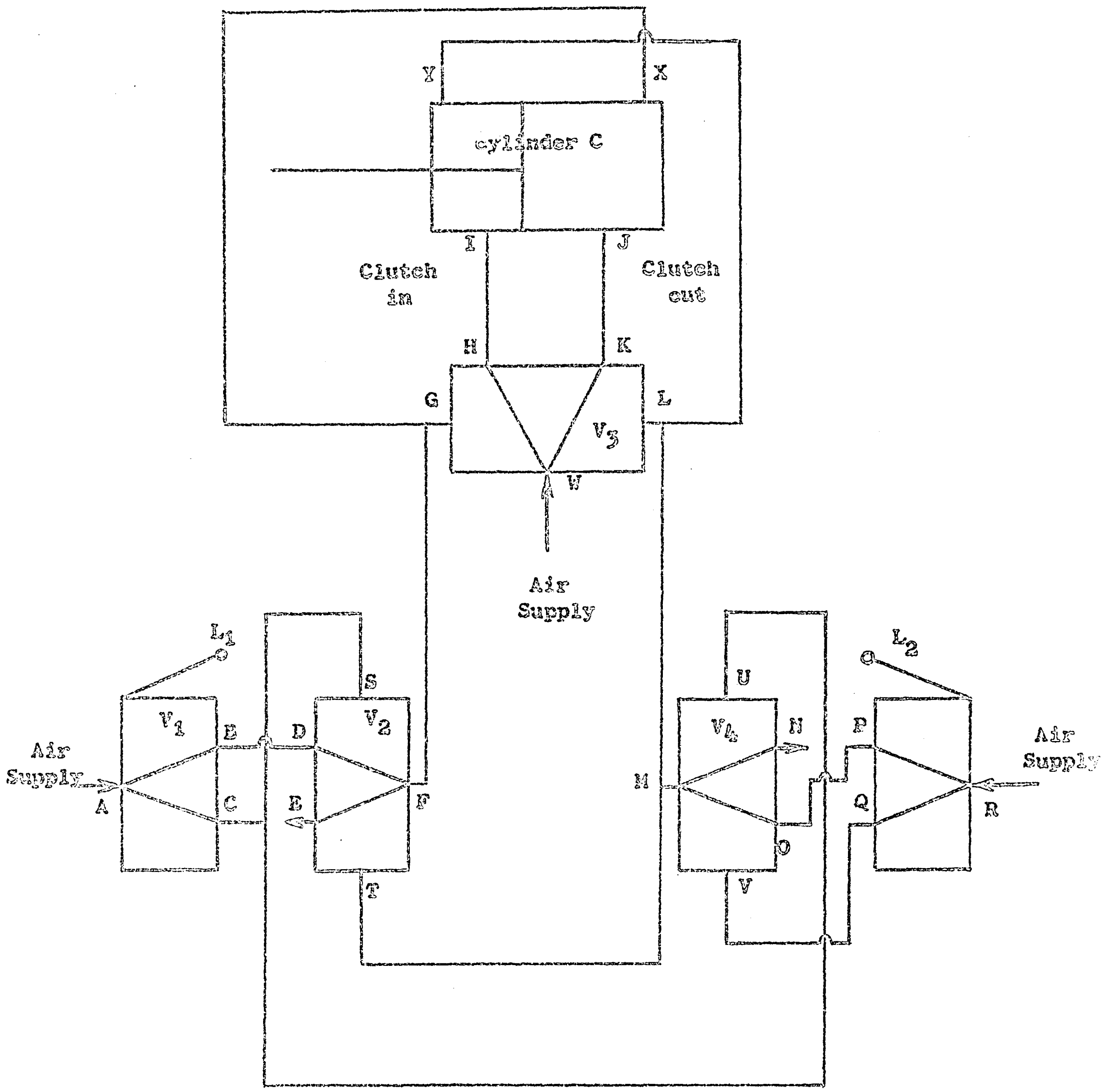

Figure A1

Pneumatic Circuit Used to Engage Press Clutch

When the press ram moved upwards after forging, L_1 was again closed allowing air to pass along ABDFG, thus causing cylinder C to disengage the clutch when air passed through the path WKJ, the clutch cylinder exhausting through the path YMN.

This circuit worked reasonably well with only infrequent breakdowns due to failure of air valves. However, when these breakdowns did occur, the cause of the fault was generally difficult to locate. To improve the reliability of this part of the circuit and to ease fault finding, the electro-pneumatic circuit shown in Figure A2 (p. A5) was designed. The operation of this circuit was as follows.

The normal position of microswitches mounted on the transfer tongs and press ram was as shown in Figure A2 (p. A5).

When the tongs moved under the press, the relay coil S_1 was energised closing contacts C_1 and C_2 and opening C_3. This relay coil was then held in the energised condition by the circuit through the ram microswitch and the closed contacts C_1.

When the tongs returned under the heating coil, solenoid S_3 was energised through contacts C_2 and actuated a solenoid-operated air valve to bring in the press clutch allowing the ram to move down for the forging stroke.

When the ram moved the microswitch M_2 opened, de-energising coil S_1, thus opening C_1 and C_2 and closing C_3. When the ram returned to the "up" position, the air valve solenoid S_2 was operated by the circuit through microswitch M_2 and contacts C_3, thus causing the clutch to disengage.

This circuit operated in a trouble-free fashion throughout the tests.

APPENDIX II

Temperature Monitoring Circuit

Since the wear of test dies was very sensitive to forging temperature, particularly in the region of 1100°C, which was chosen as the forging temperature to maximise wear, it was necessary to monitor the temperature distribution achieved throughout a test.

Equipment was developed, therefore, which automatically recorded the number of slugs in each test which fell within three pre-selected temperature ranges. The circuit developed is shown in Figure A3 (p. A5).

The output from a Land Continuous Optical Pyrometer, focussed on the hot slug, was fed to a Pye Scalamp Galvanometer, the light beam of which was masked to a narrow slit of light.

On to the galvanometer scale, four photo-resistive light cells were fixed, one at the zero position on the scale and the other three at scale positions corresponding to the three pre-selected temperatures.

To illustrate the operation of the circuit, it will be assumed that the signal from the pyrometer deflects the galvanometer beam to a position between the second and third photocells (P2 and P3 in Figure A3 p. A5).

As the beam passes photocell P2, the contacts close and energise the operating coil on the counting relay, closing the first pair of contacts C_1. As the light beam passes P3 on the upward swing, a further pulse closes contacts C_2 and opens C_1.

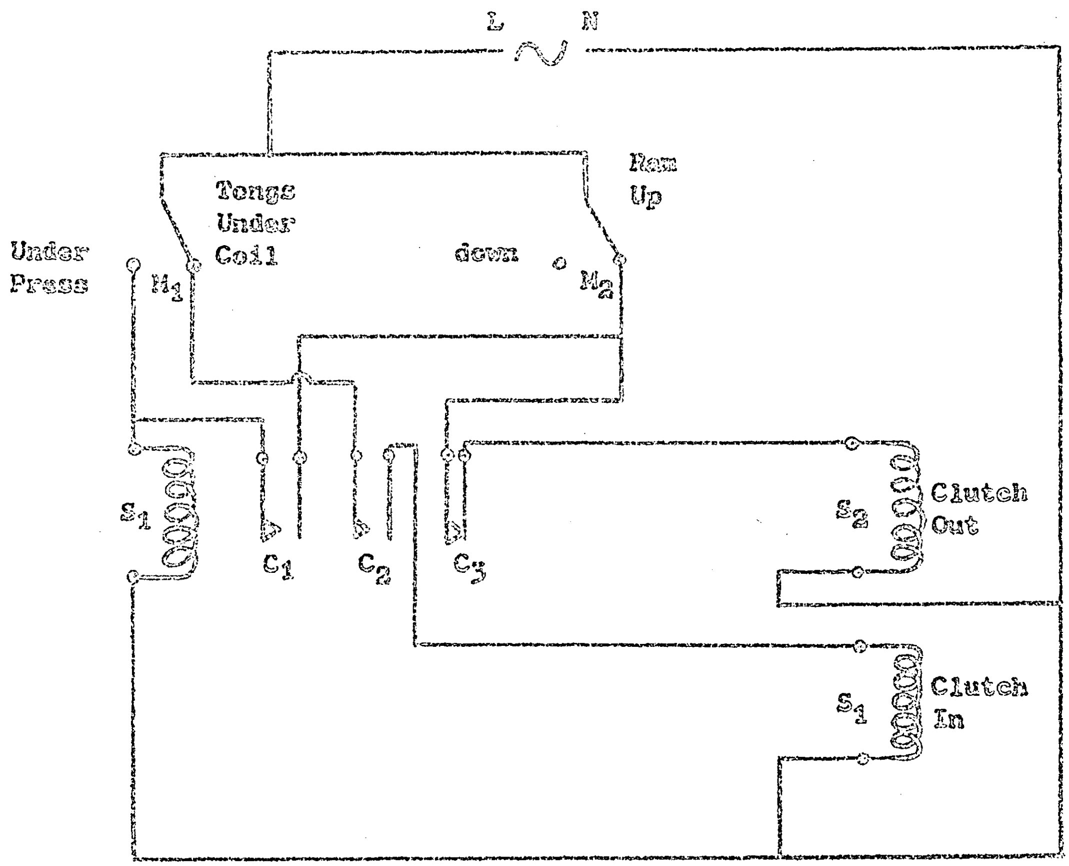

Figure A2

Electro-Pneumatic Circuit Used to Engage Press Clutch

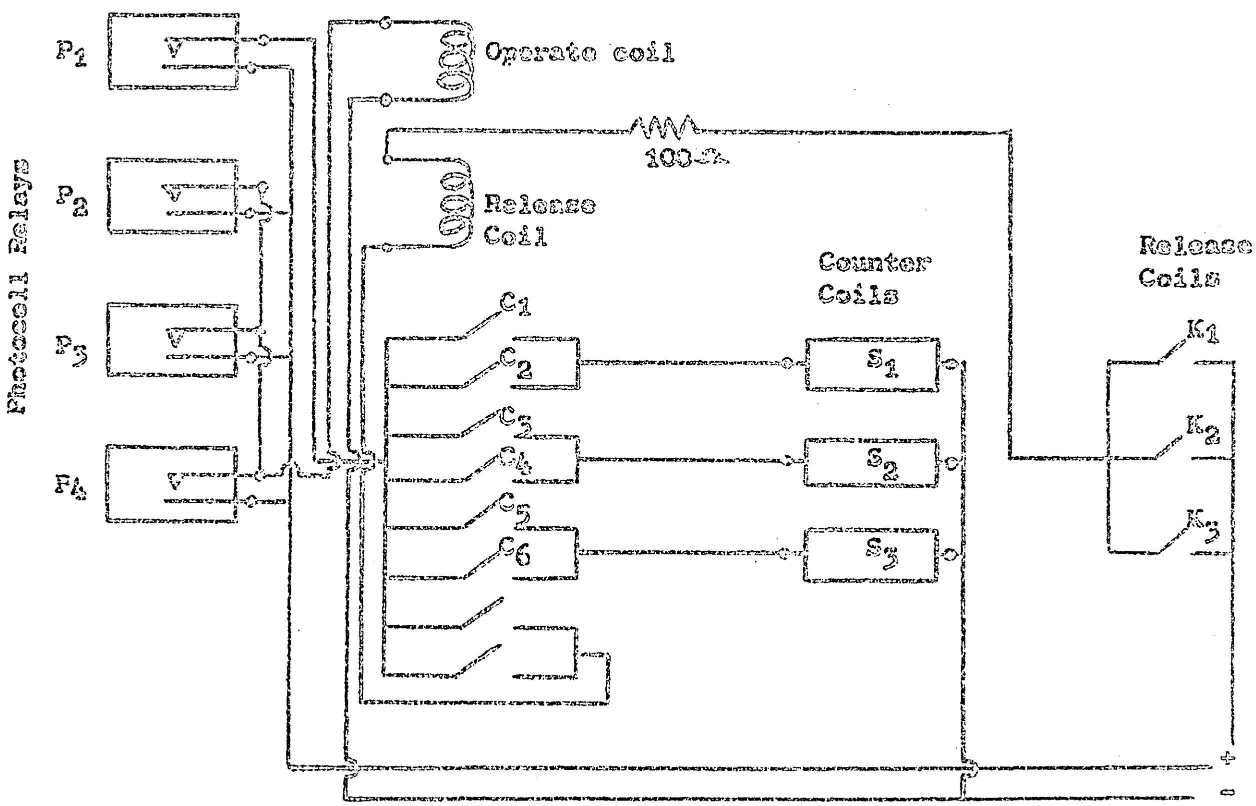

Figure A3

Temperature Monitoring Circuit

When the slug is removed from the view of the pyrometer, the galvanometer beam swings back to zero and, in so doing, operates P3 and P2 successively, thus leading to contacts C_4 on the counting relay being closed when the light beam reaches P_1. When the beam passes P_1, a circuit is made through contacts C_4 on the counting relay and the second counter coil S_2.

When S_2 is energised, the corresponding counter operates and the contacts K_2 close. Closing of contacts K_2 energises the release coil of the counting relay opening the contacts C_4 and leaving the circuit ready for further operation on the next forging cycle.

APPENDIX III

A.III.1. Materials Selected for Trials

On the basis of laboratory tests, seven materials were selected as being worthy of investigation in works trials. The materials selected, and their compositions, are given in Table A1, which also indicates the approximate price of each material.

The prices quoted in the table call for some comment. The price of die blocks varies to some extent, with the size of the block and the quantity ordered. Furthermore, some of the steels listed in Table A1 are not standard materials and, therefore, their price is increased due to the need to produce small quantities of special compositions for die blocks.

In making economic comparisons between materials, it was felt that the die steel price used in cost calculations should be that which the drop forger would have to pay if he ordered dies to any of the specifications in Table A1. The prices quoted are, therefore, the current commercial prices at the time of the investigation.

Table A1

	Material	Composition										Approx. Price £/ton.
		C	Si	Mn	Cr	Ni	Mo	W	V	Nb	N	
2	No. 5 Die Steel	.6	.3	.6	.6	1.5	.25	-	-	-	-	166
3	Cr Mn Ni Mo Steel	.5	.3	1.0	1.2	1.0	.6	-	.1	-	-	
4	En40C	.4	.3	.6	3.0	.4	1.0	-	.2	-	-	222
5	Mo Ni Cr V Steel	.2	.3	.6	1.0	2.5	3.0	-	.25	-	-	373
6	Cr Mo W V Steel	.33	1.0	.3	5.0	-	1.5	1.5	.5	-	-	364
9	Cr Ni Mo V Steel	.1	.3	.7	12.0	2.4	1.8	-	.35	-	-	476
11	Cr Mn Ni N Steel	.5	.1	9.8	21.7	3.9	-	-	-	-	.5	? high
		C	Si	Mn	Ni	Cr	Mo	Ti	Al	Co	Fe	
17	Nimocast 713	.08 / .20	1.0m	1.0m	bal	11 / 14	3.5 / 5.5	.25 / 1.25	5.5 / 6.5	bal	5.0	30/- per lb.

A.III.2 Method of Conducting Trials

Each forger who participated in the trials agreed to try one pair of die blocks or inserts in the material suggested. It was further agreed that, where the use of the trial material did not lead to any production difficulties or undue reduction in die life, the forger would use further blocks until a reliable average die life figure was established. It was suggested that, in most cases, at least six sinkings would be needed to obtain a reliable indication of die life.

In those firms where up-to-date die life records were available, the most recent performances of the normal die material were used to establish the average die life. Where such records did not exist, data on the normal material were collected concurrently with data on the trial material.

A.III.3 Details of Individual Trials

Trial 1 Material 3

In this trial Material 3 was used for the production of a lever forging under a hammer. A detailed breakdown of die costs was not obtained, only the average die life and cost per forging as shown in table 27 being provided.

Trial 2 Material 4

In this trial the experimental material 4 was used to make a vertical link by hammer forging. Only one impression was used, since only by careful attention to the dies were 4,000 forgings produced at an abnormally low production rate. This figure should be compared with about 7,5000 for No. 5 Die Steel.

Due to the trouble caused by cracking of the test dies, no more trials were made and no cost analysis of the trial has been made.

Trial 3 Material 4

The experimental material used in this trial was again material 4. Analysis of the trial results available is made difficult because connecting rod forgings were made in two different materials (EN16 and EN18) which were known from previous experience to give very different die lives.

The data supplied are set out in Table A2 below.

Table A2

Performance of No. 5 Die Steel Dies

Impression No.	Life	Material forged
1	8571	EN16
2	3411	EN18
3	7900	EN16
4	6808	"
5	8247	"
6	8023	"
7	8135	"
8	5226	EN18
9	6646	EN16
10	8511	"
11	8343	"
12	9309	"
13	8293	"
mean life for EN18	4318	

Performance of Material 4

Impression No.	Life	Material forged
1	5305	EN18
2	6251	"
3	8127	EN16
mean life for EN18	5778	

Normally four sinks are obtained from each die, but the trial die was found to be cracked around the dovetail after the third sink, and was therefore scrapped.

If data relating only to EN18 forgings are considered the following cost analyses shown in Table A3 can be made from details supplied.

Table A3

(a) Assuming dies in Material 4 will make only 3 sinks

	No. 5 Die Steel	Material 4
Cost of die blocks	10.0 units	13.0
Sinking Cost	58.7 (= 4 x cost of one impression)	52.7 (= 3 x cost of one impression)
Total die costs	68.7	65.7
Average die life	4318	5778
Die cost per forging x 1000	$\frac{68.7 \times 1000}{4318 \times 4}$ = 3.98	$\frac{65.7 \times 1000}{5778 \times 3}$ = 3.77
Ratio of labour/material cost	$\frac{58.7}{10}$ = 5.78	

(b) Assuming dies in Material 4 will make 4 sinks

	No. 5 Die Steel	Material 4
Cost of die blocks	10.0 units	13.0
Sinking cost (= 4 x cost of one impression)	58.7	70.2
Total die costs	68.7	83.2
Average die life	4318	5778
Die cost per forging x 1000	$\frac{68.7 \times 1000}{4318 \times 4}$	$\frac{83.2 \times 1000}{5778 \times 4}$
	= 3.98	= 3.60

The results of both the above analyses are shown in Table 27.

Many more trials are required before it is certain to what extent the use of material 4 would affect the die cost per forging. This trial illustrates very well how difficult it is to detect potential cost reductions of the order of 10%.

The increased sinking cost for material 4 should be noted, the relative costs of sinking one impression being for No. 5 Die Steel 14.7 units, and for material 4 17.6 units.

Trial 4 Material 4

In this trial material 4 was used to produce small spanners by hammer forging. The average die life for No. 5 Die Steel was established as 15,047 forgings from records of the performance of 21 previous sinkings. The first three sinks in a pair of blocks in material 4 produced 19,385, 20,098 and 18,522 respectively. Trials are continuing and no cost analysis has been attempted.

Trial 5 Material 4

Material 4 in this trial was used for a protractor frame forging. In this case the machining costs for both No. 5 Die Steel and material 4 were said to be almost identical. Table A4 shows the data supplied on this trial.

Table A4

	No. 5 Die Steel	Material 4
Cost of dies	10.0 units	11.4
Cost of 1st sink	12.1	12.1
Cost of 6 resinks	39.4	39.4
Total die costs	61.5	62.9
Average die life	1582 (2 sinks)	2571 (2 sinks)
Die cost per forging x 1000 (based on 7 sinks)	$\frac{61.5 \times 1000}{1582 \times 7}$	$\frac{62.9 \times 1000}{2571 \times 7}$
	= 5.55	= 3.50
Ratio of labour/material cost	$\frac{51.5}{10}$	
	= 5.15	

Trial 6 Material 4

In this trial material 4 was used to produce a combination square body. No cost details were obtained.

Trial 7 Material 5

This was a trial carried out by a forger on his own initiative and the results were kindly made available to the author. The component was a press forged sleeve. Although No. 5 Die Steel was not the standard material for this die, trials were run on it to provide comparative data. Details of the trial are given in Table A5.

Table A5

	No. 5 Die Steel	Material 5
Cost of inserts	10.0 units	21.0
Cost of sinking	36.6	36.6
Total die cost	46.6	57.6
Mean die life	12,000	19,000
Mean die cost per forging x 1000	$\frac{46.6 \times 1000}{12,000}$	$\frac{57.6 \times 1000}{19,000}$
	= 3.88	= 3.03
Ratio of labour/material cost	$\frac{36.6}{10}$	
	= 3.66	

Trial 8 Material 6

In this trial small hinge forgings were made three at a time on a hammer. Details were provided for six sinkings of a standard No. 5 Die Steel block, and six sinkings of a trial block in material 6.

Details of the trial are given below in Table A6.

Table A6

Sinking	Material 6	No. 5 Die Steel
1	240,090	130,221
2	158,025	130,260
3	110,815	133,415
4	165,575	181,270
5	142,995	137,935
6	172,070	184,790

Sinkings 1 and 3 in the material 6 blocks were not considered representative, the latter due to a manufacturing fault in die sinking, and the former because it was run beyond the normal limit in spite of difficulty in maintaining normal tolerances on the forging. For a similar reason sinkings 4 and 6 in No. 5 Die Steel were not considered representative. The remaining 4 sinkings in each material give average lives of 132,958 for No. 5 Die Steel and 159,661 for material 6.

A cost analysis of the two materials based on figures given by the forger is given in Table A7.

Table A7

	No. 5 Die Steel	Material 6
Cost of die blocks	10.0 units	22.3 units
Cost of sinking 8 impressions	116.0	116.0
Total die costs	126.0	138.3
Average die life	132,958	159,661
Forgings made in 8 impressions = 8 x average life	1,063,664	1,277,288
Die cost per forging x 1000	$\frac{126 \times 1000}{1,063,664}$ = 0.120	$\frac{138.3 \times 1000}{1,277,288}$ = 0.108
Ratio of labour/material cost	$\frac{116.0}{10}$ = 11.6	

The cost analysis presented here is a modified version of that used by the forger. These trials were initiated by the forger before any approach by the author and the results were kindly made available to him. The sinking cost quoted is based on the assumption that a total of eight sinkings would be possible in both materials.

The results obtained in this trial call for some comment. The reduction in die costs must be regarded as only very tentatively established. As figure 2 (p. 7) shows, at high die lives such as occured in this trial, large variations in life are to be expected. Since the forgings are made in threes the 'die life' for use with Figure 2 is one third of that quoted above, i.e. about 50,000. Much more data on die life is needed before any confident pronouncement on the relative performance of material 6 can be made.

In addition, the sinking costs for both materials have been taken as identical, because the dies were spark eroded. Had the impressions been milled, increased machining costs for material 6 would probably have led to a lower reduction in die costs, or even possibly an increase. This trial again illustrates very graphically how difficult it is to discover reductions in die costs of the order of 10%, although at first sight it may seem that such reductions could easily be determined.

Trials 9 and 10 Material 6

In these trials gear blank forgings of about $4\frac{1}{2}$" diameter were made on a 1500 ton press. The forging sequence used was one blow each in forming, moulding and finishing dies. In the trials only the finishing dies were replaced by the experimental material.

The top die was a more complex shape than the bottom die and had a shorter life. Since top and bottom dies were not worked in pairs, each die half has been considered as a separate trial. Trial 9 refers to the top die and trial 10 to the bottom die. Details of the data collected are given below.

Trial 9 Top Die

Data were collected on two standard No. 5 Die Steel inserts, each of which produced five sinkings before being scrapped. Details of the cost and performance of these inserts are given in Table A8 below, together with details of the inserts in the experimental material.

Table A8

No. 5 Die Steel

	Insert 1	Insert 2	Value quoted in Table 27
Cost of No. 5 Die Steel insert	10.0 units	11.1	
Cost of 1st sink	17.2	14.1	
" " 1st resink	7.2	9.2	
" " 2nd "	6.5	9.2	
" " 3rd "	6.4	9.4	
" " 4th "	6.5	8.9	
Cost of repairs	2.8		
Total die costs	56.6	61.9	
Total forgings made	29,415	32,906	
Mean die life per sinking	5,833	6,581	6,232
Mean die cost per forging x 1000	$\frac{56.6 \times 1000}{29,415}$	$\frac{61.9 \times 1000}{32,906}$	
	= 1.92	= 1.88	1.90
Ratio of labour/material cost	$\frac{46.6}{10}$	$\frac{50.8}{11.1}$	
	= 4.66	= 4.57	4.63

Trial material 6

Full cost details for this material are not available. The information provided was that the insert made 55,990 forgings in 7 sinkings at an average cost per forging of 1.46 (units as Table A8).

The following figures are, therefore, given in Table 27

Mean die life = 7,999 per sinking

Mean die cost per forging = 1.46

Trial 10 Bottom Die

Data similar to that given above was collected for the bottom dies and is given in Table A9 below.

Table A9

No. 5 Die Steel Bottom Dies

	Insert 1	Insert 2	Value quoted in Table 27
Cost of No. 5 Die Steel insert	10.0 units	10.5	
Cost of 1st sink	12.1	13.7	
" " 1st resink	6.5	9.0	
" " 2nd "	6.4	9.0	
" " 3rd "	-	9.2	
" " 4th "	-	8.6	
Total die costs	35.0	60.0	
Total forgings made	20,585	26,756	
Mean die life per sinking	6,861	5,351	5,918
Mean die cost per forgings x 1000	$\frac{35.0 \times 1000}{20,585}$	$\frac{60 \times 1000}{26,756}$	
	= 1,70	= 2.22	1.96
Ratio of labour/ material cost	$\frac{25}{10}$	$\frac{49.5}{10.5}$	
	= 2.50	= 4.71	3.60

Trial material 6

The trial material for the bottom die insert was still in use at the time of writing. After five sinkings it has produced 67,733 forgings at an average cost of 1.02 units per forging.

The figures used in Table 27 therefore, are an average die life of 13,546 and a die cost per forging of 1.02 units.

For both top and bottom dies further testing would probably have only a slight effect on the figures given above. The % reductions in die costs quoted in Table 27 can, therefore, be taken as reasonably accurate.

Trials 11 and 12 Material 9

In these trials similar gear blank forgings to those in Trials 9 and 10 were produced. The forgings were about $4\frac{1}{4}$" diameter and were forged under identical conditions to those of Trials 9 and 10.

Trial 11 Top Dies

Details of the trial are given in Table A10.

Table A10

	Insert 1	Insert 2	Value quoted in Table 27
Cost of No. 5 Die Steel insert	10.0 units	10.1	
Cost of 1st sink	16.1	17.1	
" " 1st resink	10.4	11.9	
" " 2nd "	12.0	10.4	
" " 3rd "	9.4	11.1	
" " 4th "	10.8	9.4	
" " 5th "	10.8	10.7	
" " 6th "	9.5	-	
Total die costs	89.0	80.7	
Total forgings made	25,343	23,146	
Mean die life per sinking	3,620	3,857	3,730
Mean die cost per forging x 1000	$\frac{89.0 \times 1000}{25,343}$	$\frac{80.7 \times 1000}{23,146}$	
	= 3.52	= 3.49	3.50
Ratio of labour/material cost	$\frac{79.0}{10}$	$\frac{70.6}{10}$	
	= 7.90	= 7.06	7.48

Experimental material 9

The top die insert in this material made 45,282 forgings in seven sinkings at an average cost per forging of 2.70 units. The average die life per sinking was, therefore, 6,469

Trial 12 **Bottom Dies**

Details of this trial are given in Table A11.

Table A11

No. 5 Die Steel

	Insert 1	Insert 2	Value quoted in Table 27
Cost of No. 5 Die Steel insert	10.0 units	10.2	
Cost of 1st sink	12.0	12.6	
" " 1st resink	5.9	7.2	
" " 2nd "	6.1	6.3	
" " 3rd "	6.5	5.0	
" " 4th "	6.5	5.9	
" " 5th "	6.0	7.7	
" " 6th "	5.9	-	
Total die costs	58.9	54.9	
Total forgings made	32,746	33,088	
Mean die life per sinking	4,678	5,514	5,064
Mean die cost per forging x 1000	$\frac{58.9 \times 1000}{32,746}$	$\frac{54.9 \times 1000}{33,088}$	
	= 1.80	= 1.66	1.73
Ratio of labour/material cost	$\frac{48.9}{10}$	$\frac{44.7}{10.2}$	
	= 4.89	= 4.37	4.63

Experimental material 9

This insert made 53,097 forgings in six sinkings for a mean life of 8,849 forgings per sinking. The die cost per forging was quoted as 1.84 units.

Trial 13 Material 9

In this trial inserts were used to make a medium-sized connecting rod by hammer forging. The trial insert in material 9 was used only for the finishing impression. Die life data for 25 No. 5 Die Steel inserts was provided. The average value for the 25 inserts has been taken as the die life for No. 5 Die Steel.

Only one trial insert had been tested at the time of writing. A cost analysis for the two material is given in Table A12.

Although only one result is available for this material the analysis made in section A.III.4 of this appendix shows that a substantial reduction in die costs can confidently be expected on further testing.

Table A12

	No. 5 Die Steel	Material 9
Cost of die inserts	10.0 units	25.0
Cost of sinking	36.6	36.6
Cost of repairs	11.7	5.8
Total die costs	58.3	67.4
Average die life	5,219	13,547
Mean die cost per forging x 1000	$\frac{58.3 \times 1000}{5,219}$ = 11.17	$\frac{67.4 \times 1000}{13,547}$ = 4.97
Ratio of labour/material cost	$\frac{48.3}{10}$ = 4.83	

Trial 14 Material 9

In this trial inserts were used to produce rather larger connecting-rods than in the previous trial. The forgings were again made under a hammer. Die life data, from which the mean life has been obtained, were provided for twelve pairs of inserts in No. 5 Die Steel. Table A13 below gives the cost analysis for this trial.

As explained in section A.III.4 it is possible that much of the cost reduction could disappear if the single value for die life obtained on material 9 is an anomalously high one.

Table A13

	No. 5. Die Steel	Material 9
Cost of inserts	10.0 units	25.9
Cost of sinking	54.6	54.6
Cost of repairs	14.5	21.8
Total die costs	79.1	102.3
Average die life	21,197	40,564
Die cost per forging x 1000	$\frac{79.1 \times 1000}{21,197}$	$\frac{102.3 \times 1000}{40,564}$
	= 3.73	= 2.52
Ratio of labour/material cost	$\frac{69.1}{10}$	
	= 6.91	

Trial 15 Material 9

In this trial a large hook-shaped forging was produced by hammer forging, using normalised and tempered carbon steel dies (No. 1 Die Steel). The experimental material was material 9. Very surprisingly, in view of other results with material 9, the top die deformed very badly after producing only slightly more forgings than the carbon steel dies. The bottom die, however, had retained its shape very well and showed almost no evidence of wear.

The only explanation which can be offered at the moment for this behaviour is the fact that large quantities of oil were used to lubricate the top die. It is possible that ignition of this oil caused the surface of the top die to reach very high temperatures, and thus caused it to deform badly.

This trial was the only one in which material 9 failed to show a substantial increase in die life.

Trial 16 Material 9

The forging selected for this trial was the same protractor frame as that used in trial 5. The data given for the performance of No. 5 Die Steel have been used again in this trial. Details of the trial are given in Table A14 below. The cost analysis has been based on the assumption that a die block produces a total of 7 sinkings.

Table A14

	No. 5 Die Steel	Material 9
Cost of dies	10.0 units	27.0
Cost of 1st sink	12.1	12.1
Cost of 6 resinks	39.4	39.4
Total die costs	61.5	78.5
Average die life	2,310 (4 sinks)	4,480 (1 sink)
Die cost per forging x 1000	$\frac{61.5 \times 1000}{2,310 \times 7}$ = 3.81	$\frac{78.5 \times 1000}{4,480 \times 7}$ = 2.50
Ratio labour/material cost	$\frac{51.5}{10}$ = 5.15	

Trial 17 Material 9

In this trial a gear end casing was made by hammer forging. The die life for No. 5 Die Steel dies was quoted by the forger as 3,000 forgings per sink. At the time of writing 3 sinkings of the trial material 9 had produced 4,055, 4,113 and 4,532 forgings for an average life of 4,230.

Full details of machining costs could not be obtained and, therefore, the cost analysis shown in Table A15 has been based on machining times which were supplied.

Table A15

	No. 5 Die Steel	Material 9
Cost of dies	10.0 units	28.7
Cost of 1st sink	9.4	15.4
Cost of 5 resinks	21.0	36.8
Total die costs	40.4	90.9
Average die life	3,000 (quoted)	4,230 (3 sinks)
Die cost per forging x 1000	$\frac{40.4 \times 1000}{6 \times 3,000} = 2.24$	$\frac{90.0 \times 1000}{6 \times 4,230} = 3.58$
Ratio of labour/ material cost	$\frac{30.4}{10.0} = 3.04$	

As Table A15 shows, the increased life of material 9 failed to compensate for increased machining costs. The forger involved in this trial reported that the top die in material 9 was far more difficult to machine than No. 5 Die Steel, whilst the bottom die was not much different even though the two dies had the same Brinnel figure. This was the only trial where adverse reports on the machinability of material 9 were made.

Trial 18 Material 17

In this trial a small gear blank forging was made on a press, using material 17 as the trial material.

Only estimated machining costs and material costs were available, so that the cost analysis given in Table A16 must only be regarded as an approximate one.

Table A16

	Normal Material 6	Material 17
Cost of dies	10.0 units	82.7
Machining cost	65.5	65.5
Total die cost	76.5	148.2
Average die life	15,000 (quoted)	26,700 (2 inserts)
Die cost per forging x 1000	$\frac{76.5 \times 1000}{15,000} = 5.10$	$\frac{148.2 \times 1000}{26,7000} = 5.50$
Ratio of labour/ material cost	$\frac{66.5}{10} = 6.65$	

In this trial the increased die life has not quite compensated for the large increase in material cost.

Trial 19   Material 11

In this trial material 11 was used to make a disc by press forging. After only about 200 forgings the dies had opened out so much that they were producing out of tolerance forgings.

A.III.4   Reliability of Test Data

In a previous section of this thesis it was pointed out that it is dangerous to rely on limited data to draw conclusions regarding die performance. Since the percentage reduction in die costs quoted in the last row of Table 27 is based in some instances on the result of a single trial, it is necessary to examine the reliability of this data.

An indication of the reliability can be obtained in the following way.

For a given forging, die life is usually distributed normally about a mean value as shown in Figure 1. A measure of the 'spread' of life about the mean value is the standard deviation σ. It is a property of the normal distribution curve that 70% of all observations lie in the region between the mean value $\pm \sigma$.

During the works trials, collection of data allowed an approximate relationship between mean die life and standard deviation to be established as shown in Figure 2.

The effect of this so far as two trials are concerned, in which only a single figure is available for die life of the trial material will be considered. Taking as examples, Trials 14 and 16, and neglecting the 15% chance that the observed life in each of these trials could be above the true mean plus the standard deviation, then the most pessimistic view that can be placed on the trials is that the observed life lies at point A in Figure A3 i.e. at the mean value plus one standard deviation. Now, from Figure 2 the approximate value of σ in each trial is know so that the lowest likely mean value of die life (B in Figure A3) in each trial can be calculated.

Thus, for Trial 14 :-

$$\begin{aligned}
\text{observed life} &= 40{,}564 \\
\text{approx. } \sigma \text{ (from Fig. 3)} &= 12{,}000 \\
\text{lowest likely mean life} &= \text{observed life} - \sigma \\
&= 40{,}564 - 12{,}000 \\
&= 28{,}564
\end{aligned}$$

Thus, it is reasonably certain that the mean die life in this trial will not be less than about 28,500. Similarly, for Trial 16:-

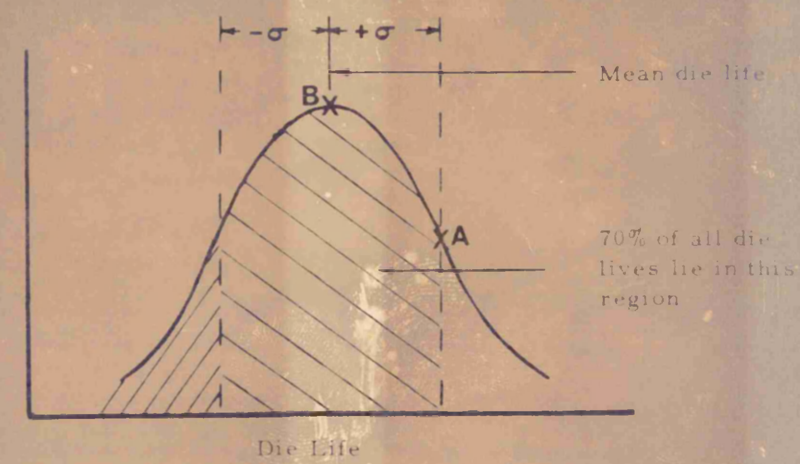

Illustration of method of calculating lowest likely mean life from a single observation of die life

Figure A4

observed life = 4,480

approx. = 500

lowest likely mean life = 4,480 - 500

= about 4,000

Using this method, the lowest likely value for mean die life has been calculated for the trials in which a reduction in die costs is indicated, but where only single observations of die life are available. Using this value of mean die life, the percentage reduction in die costs per forging has been recalculated, and is shown in column 3 of Table A17 below. For comparison, the values given in Table 27 are also included in Table A17.

Table A17

Trial No.	% Reduction in Die Costs	
	From Table 27	Recalculated
13	55	40
14	32	4
16	32	24

Table A17 thus shows that substantial reductions are almost certain to occur in Trials 13 and 16 on further testing. However, reductions in costs in Trial 14 could conceivably be quite small if the only available figure for die life is much higher than will normally be encountered.

APPENDIX IV

Calculation of Die Surface Temperatures

Carslaw and Jaegar[A1] have examined the problem of a semi-infinite solid, subjected to a constant heat flux Fo per unit time per unit area at the surface, whose initial temperature is zero.

They derived the following expression for the die surface temperature after exposure for t seconds to the heat flux.

$$T_{x=0} = \frac{2\,Fo}{K} \left(\frac{vt}{\pi}\right)^{\frac{1}{2}}$$

where $T_{x=0}$ = the temperature at the surface of the semi-infinite solid.

Fo = the heat flux per unit area per unit time.

v = the thermal diffusivity of the solid.

t = time of heating.

K = the thermal conductivity of the solid

π = 3.142

The temperature reached by the surface of a Nimonic 90 die was estimated as follows.

Assuming that the temperature of steel dies reaches 700°C for a contact time (t) of 0.5 secs. (see Figure 33 p. 51), the value of the heat flux Fo can be calculated using the following values for the thermal constants of steel.

$v = .12, \quad K = .1$ both in cgs units.

Using the values indicated:

$$700 = \frac{2\,Fo}{.1} \left(\frac{.12 \times .5}{3.142}\right)^{\frac{1}{2}}$$ which gives $Fo = 254$

During forging using Nimonic 90 dies, it is reasonable to assume that the heat flux is the same as when forging with steel dies. Thus, knowing Fo and the thermal constants for Nimonic 90, $T_{x=o}$ can be calculated.

Reference A2 gives the following data for Nimonic 90

$K = .045$ (average value from 50 - 800°C)

$$v = \frac{K}{Sp} = \frac{.045}{.11 \times 8.18} = .0495$$

where S = specific heat

p = density

Using the above data, the die surface temperature is given by

$$T_{x=o} = \frac{2 \times 254}{.045} \left(\frac{.0495 \times .5}{3.142}\right)^{\frac{1}{2}}$$

$$= 1000^{\circ}C$$