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The Influence of Die Temperature and Hardness on the Wear of No. 5. Die Steel Under Hot Forging Conditions

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Summary

A study has been made of the influence of die hardness and temperature on the wear of No. 5. Die Steel, under hot forging conditions.

Tests were performed under controlled conditions in an automated experimental press, the material, in the form of flat die inserts, being worn by the regular upsetting of cylinders of hot stock between the insert faces, and the die temperature being simulated by means of heaters within the main die blocks. The wear so produced was in the form of an annulus and the volume of metal removed was computed from data obtained by tracing across four diameters of the worn insert using a surface measuring instrument. All inserts were also sectioned, applying a modification of conventional taper sectioning techniques, and investigated metallographically, in order that the wear might be related to the structural condition of the near surface regions of the material.

It was found that abrasion was largely responsible for the total amount of wear. Wear increased with increased temperature mainly as a result of the consequent decrease in strength and hardness of the material, but became disproportionately severe once the surface temperature exceeded Ac_1. Wear decreased with increased hardness

in a discontinuous manner, the discontinuities being related to the structural state of the steel. An acicular structure containing well dispersed carbide particles was found to be particularly wear resistant.

SECTION A. A Survey of the Relevant Literature

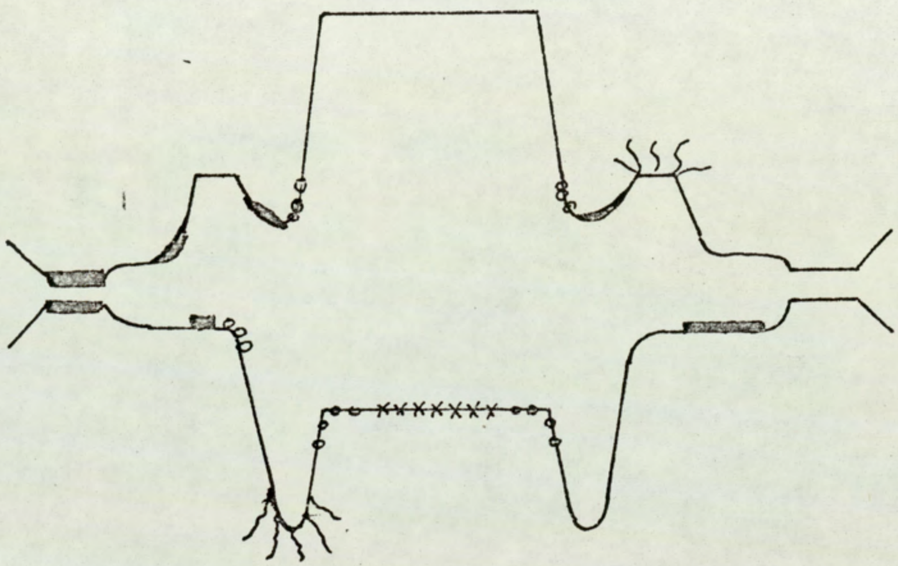
A.1. Introduction

In a forge dies will be taken out of service if they become intolerably oversize or if they crack sufficiently severely to cause repeated sticking of the forging in the die cavity. In either case the die may be resunk and reused, or, if its thickness is insufficient for the retention of mechanical strength after a sinking, it must be scrapped. Since die material and sinking costs account for some 10-20% of the cost of a forging, and down-time on the forge for changing dies means loss of productivity as well, it is in the interests of economy to obtain maximum die life from one sinking.

In a shaped die there are believed to be four primary causes of damage, namely cracking as a result of thermal fatigue, mechanical fatigue failure, deformation and erosion[1]. Fig. 1 shows schematically the positions where each is most likely to occur. Wear can be said to take place when there is a real metal loss from the die, and can result from erosion and/or pitting, which is the removal of small portions of the surface which have been separated from the bulk by thermal fatigue cracks.

This investigation is primarily concerned with the severity of such wear as it is affected by die material

Fig 1



Legend

xxxx Thermal Fatigue

Mechanical Fatigue

ooo Deformation

Erosion

Modes of Die Failure

properties and forging conditions, in particular the die material's hardness and structural condition, and the temperature conditions of forging. In order to investigate the effects of a particular factor, it has been necessary to simplify the forging operation to permit elimination or control of the many variables that can, in the uncontrolled industrial forging situation, affect the wear of the die. To this end material has been tested in the form of flat inserts in the main die blocks of a press, automated to upset a hot cylinder of stock every ten seconds, and the amount of wear has been measured after 1000 such upsets, and related to the known conditions of forging and of the die material. An attempt has been made to analyse the result in terms of the type of wear occurring and the effects on such wear of the parameters under test. The opening section of this thesis is therefore largely concerned with wear in general, and the particular conditions of forging that necessitate modifications to conventional wear data.

A.2. Wear

A.2.1. Classification of Types of Wear

Wear can be defined as the progressive loss of substance from the surface of a body, brought about by mechanical action.

There exist many forms of wear and these have, in the past, been classified rather haphazardly, usually relating the classification to the appearance of the surfacces after wearing. A classification relating the type of wear to the factors causing the surface damage has more physical significance, and every type of wear observed can be accounted for in this way. It is this classification, therefore, that will be used throughout the text, and wear will be referred to as being adhesive or abrasive in nature, or as resulting from thermal fatigue.

Adhesive wear may be defined as that occurring when two smooth bodies are slid over each other, and fragments are pulled off one to adhere to the other. These fragments may later come off the surface on which they were formed, and be transferred back to the original surface, or form as loose wear particles, often oxidised.

Abrasive wear is a form of wear which occurs when a relatively hard surface is slid over a softer one, and ploughs a series of grooves in it, either by deformation or by cutting.

In the latter case the material in the grooves is displaced as wear particles, often loose and oxidised. Abrasive wear can also occur when hard particles are introduced between softer sliding members, and then it is thought that the particles adhere to or embed in one surface, and, so held, plough grooves in the other. This latter system is known as a three body system, the former as a two body system.

Wear resulting from surface fatigue demands the initiation of surface or subsurface cracks by alternating stresses, which may be mechanical or thermal in origin, or a combination of both. These cracks will break up the surface into relatively large pieces, which are subsequently dragged away by the wearing member, leaving pits in the surface. This type of wear is therefore often referred to as pitting.

Adhesive wear is mild in character, and although it occurs to some degree whenever there is mechanical interaction of surfaces, it leads only to a slow rate of deterioration of the interacting materials. It is the other forms of wear which can give rise to excessively high wear rates.

There are two other sources of dimensional loss at a surface which cannot strictly be classified as wear, but which will be discussed in this section for the sake of coherency. Firstly a surface may deform plastically as the result of over-high loading, and secondly the surface may react chemically with its environment and the reaction products, usually oxide, can be broken up and/or rubbed away by any of the above wear mechanisms. This second case is sometimes referred to as corrosive wear, but the loss is more accurately described in terms of the real wear processes as they are modified to the special case of the wearing members being covered in oxide.

In all therefore there are five classifications of surface damage that must be considered as possibly contributing to any recorded loss in dimensions of a worn surface.

A.2.2 Factors Influencing Wear Rate

Many types of wear test have been developed in an attempt to understand the nature of the wear processes and so relate the severity of wear to material properties and environmental conditions. A conventional test is designed to study adhesive or abrasive wear, and the many types of test generally differ from each other only in the points of design of specific machines constructed to permit the rubbing together of the materials of interest under controlled conditions of load, time, speed, etc. No tests exist to study wear resulting from surface fatigue, though conventional mechanical and thermal fatigue tests are instrumental in the understanding of at least part of the process.

Radioactive tracers have been used[2] to measure the amount of wear, but more commonly the volume of metal removed is deduced from a profile of the worn surface. Small test pieces are sometimes weighed before and after a test.

The data obtained from such wear tests has permitted the formulation of some simple fundamental relationships between the type and amount of wear, and material properties and conditions of sliding. Because of the complex nature of the wear process, these relationships are by no means complete. Nevertheless they provide a basis from which deductions can be made as to the probable effects on wear rate of the forging variables.

Adhesive Wear

It is generally agreed that adhesive wear occurs because bonds form at the tiny points of contact of two interacting surfaces, and are broken as sliding continues. It is the surface and near surface properties of the materials that determine whether the junction shall break along the original line of contact or within one or other of the materials, and it is only in the second case that a fragment will be transferred, that is that wear will take place. The liability of junctions breaking away from the original line of contact may most simply be envisaged as a question of whether the bond is stronger or weaker than the underlying material[2,3]. The bond strength is determined by the mutual attraction between atoms of the two wearing materials, and depends on their surface energies; the hardness of the underlying material will give an indication of its strength, though variations in strength on a molecular level are possible because of imperfections in the lattice.

For a particular sliding pair under constant conditions, it can be seen that there will be a statistical probability of a junction breaking away from its original line, and over a period of time the volume of wear will therefore be proportional to the number of junctions made and broken, that is to the load (determining area and number of points of contact[4]) and to the distance slid.

In general therefore the following laws of adhesive wear can be stated:-

(1) The amount of wear is proportional to the load, L.

(2) The amount of wear is proportional to the distance slid, x.

(3) The amount of wear is generally inversely proportional to hardness, p.

(4) The amount of wear will be altered by any factor that alters the degree of adhesion between the two surfaces, such as temperature, cleanliness etc.

These laws can more conveniently be accumulated in the form of Archard's equation[5]

$$V = K_{adh}\frac{Lx}{P}$$

When V is the volume of wear and K_{adh} is a dimensionless constant that expresses qualitatively the factors included in law 4 above, and so represents the proportion of junctions breaking to give transferred fragments. The equation makes no provision for back transfer of the fragments, but this is unimportant in the experimental forging situation where fragments transferred to the forging are lost from the system, the die being worn by fresh stock each blow.

Abrasive Wear

During forging it is the scale, mainly emanating from the stock, that provides abrasive particles for this type of wear. The scale would tend to embed in the relatively soft stock material and be moved along with it, abrading the die surface. The system is therefore a three body one.

For such a three body system the laws of abrasive wear can be stated as follows:

(1) The amount of wear is proportional to the load, L[6].

(2) The amount of wear is proportional to the distance slid, x[7].

(3) The amount of wear of pure metals is inversely proportional to hardness, p[8].

(4) The amount of wear is dependent on the size, shape, orientation and number of the abrasive particles.

Thes laws can be expressed in an equation similar to Archard's above, viz.

$$V = K_{abr} \frac{Lx}{P}$$

where K_{abr} encompasses the factors mentioned in law 4, and so represents the cutting ability of the abrasive particles.

However, law 3 does not hold true for steels where the amount of wear has been found[9] to be inversely proportional not to p, but to $\sqrt[3]{p}$. This effect was attributed mainly to the brittleness of the hardened steel, making possible the formation of a groove of greater dimensions that the cutting particle. It would seem to be more likely, however, that it is the inhomogenous structure of a quenched and tempered steel that is basically responsible for the effect. Popov and Brykov[10] investigated the influence of structure on the abrasion resistance of a steel and concluded that the presence of carbide in the structure tended to reduce the wear by abrasion. It follows therefore, that a soft steel will not wear as much as an homogenous material of the same hardness because of the high percentage of carbide it contains, and that at higher hardness levels the wear rate will not drop as quickly as would be predicted by law 3 because although its hardness has increased, the amount of carbide it contains has decreased. However, the efficacy of carbide particles in reducing wear by abrasion also depends on their size, distribution and type[10,11,12], so it seems unlikely that even the amended relationship of wear to hardness will hold true for every type of steel over its complete range of hardness. In general, therefore, it would seem that the structure of the steel might be of more significance than its actual hardness in determining its abrasion resistance.

Wear Resulting from Surface Fatigue

No wear can possibly occur by this method unless the levels of alternating mechanical and/or thermal stress are sufficiently high to cause plastic strain in the surface leading to the initiation and propagation of cracks. After fatigue failure has occurred it is unclear how wear volume relates to the extent of the cracking. It seems reasonable to postulate, however, the more severe the cracking the greater will be the number and/or size of the particles that will be loosened, so the amount of wear will be governed by the same factors as govern the extent of the fatigue.

Further, if some proportion of these loosened particles are swept away each time the surfaces interact the wear, like other forms of wear, will be proportional to the distance slid.

In this particular investigation using an experimental press under controlled conditions, the level of the mechanically induced stress is never permitted to vary, so it will oppose or reinforce the thermally induced stresses to a constant degree. Further, the mechanical stress levels have been shown[13] to be insufficiently high to initiate cracking within the 1000 cycles of a test. A discussion of the factors influencing failure by mechanical fatigue will therefore be omitted from this report.

A fairly comprehensive review of the results from many thermal fatigue tests has been compiled by Glenny[14] and forms a basis for the formulation of some general rules.

(1) No fatigue can occur unless the stress level induced by the thermal gradients exceeds the yield stress of the material at temperature.

(2) Any factor that increases the stress level will increase the severity of cracking, hence the liability of failure will increase if

a) the difference between the upper and lower levels of temperature, ΔT, increases,

b) the coefficient of expansion, α, of the material increases,

c) the thermal conductivity, K, decreases,

d) the heat transfer coefficient, h, increases,

e) the modulus of elasticity, E, increases,

f) a notch or defect near the surface acts as a stress raiser.

(3) Any factor that increases the amount of plastic strain will increase the severity of cracking, hence the liability of failure will increase as

a) the mean temperature, T_m, of the cycle increases,

b) the yield stress of the material decreases.

The ductility of the material is also important in determining the likelihood of its cracking if the yield stress is exceeded.

Baron and Bloomfield[15] observed an increase in thermal fatigue in steels when the upper cycle temperature exceeded the lower critical point of the steel (Ac_1), and the surface layers became austenitic; further, the life was decreased by a factor of 8 when the lower cycle temperature was dropped below the martensite start temperature (M_s), permitting decomposition of austenite to martensite during the cycle. The decreased life was attributed to the volume increase attendant on the phase change from martensite to austenite, which caused rumpling of the surface and induced stress concentrations that initiated thermal fatigue cracks. Kindbom[16] pointed out that the brittleness of the transformation products would also contribute to the material's inability to resist thermal shock.

The level and sign of the strain can also be influenced by the cycle itself, rapid heating and slow cooling producing residual tensile strain being the most detrimental to life.

In general, therefore, the likelihood of cracking will be increased by any factor that increases the stress level, and the most important single criterion for a fatigue resistant material is a high yield stress at temperature.

Deformation

For deformation to occur the load must exceed the yield stress in compression of the material, so again there is a threshold level below which there will be no damage. Above this level deformation will increase with load according to the plastic properties of the material. It should be noted that sliding is not a necessary condition for loss of dimensions by this method.

Loss of Dimensions Due to Scaling

The amount of corrosion product available to be removed will depend on the material and its rate of reaction with the environment. Its removal may be effected by any of the wear processes, and its rate of removal will be governed by the laws outlined when the properties of the interacting materials are considered as those of a metal/metal oxide pair. The adherence of the scale will also affect its rate of removal, a brittle, non-adherent scale being chipped away more rapidly than a thin, adherent layer. It will be appreciated therefore that the actual loss in dimensions will not necessarily be related to the rate of scaling, and that the rate of removal will be substantially independent of the mechanical properties of the underlying material.

In general the amount of wear will always be proportional to the distance slid. Abrasive and adhesive wear will be proportional to the load on the interacting surfaces, but wear resulting from thermal fatigue will be influenced by external load only inasmuch as it opposes or reinforces the thermal stresses. Abrasive and adhesive wear will also tend to decrease as the hardness of the material being worn increases, but wear by thermal fatigue may increase in a harder material if the increase in hardness has been at the expense of ductility.

The presence of an oxide film will tend to lower the rate of adhesive wear by reducing the mutual attraction between the surfaces; it may reduce abrasive wear if its hardness is greater than that of the underlying material. Its effect on thermal fatigue is somewhat questionable; it has been reported both as detrimental[18] and as of little consequence[19].

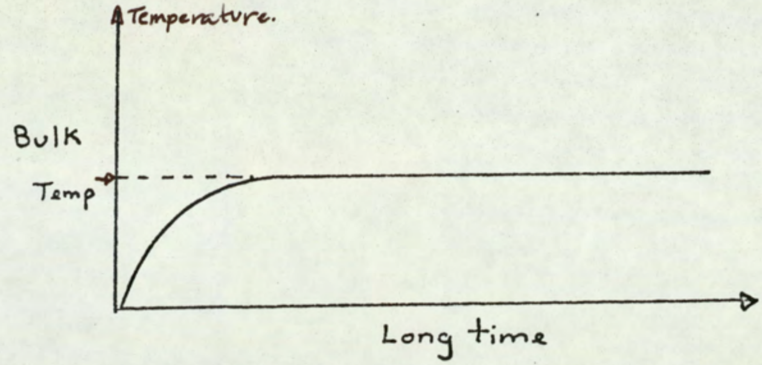
A 3. Wear During Forging

A.3.1. Effect of Load and Temperature

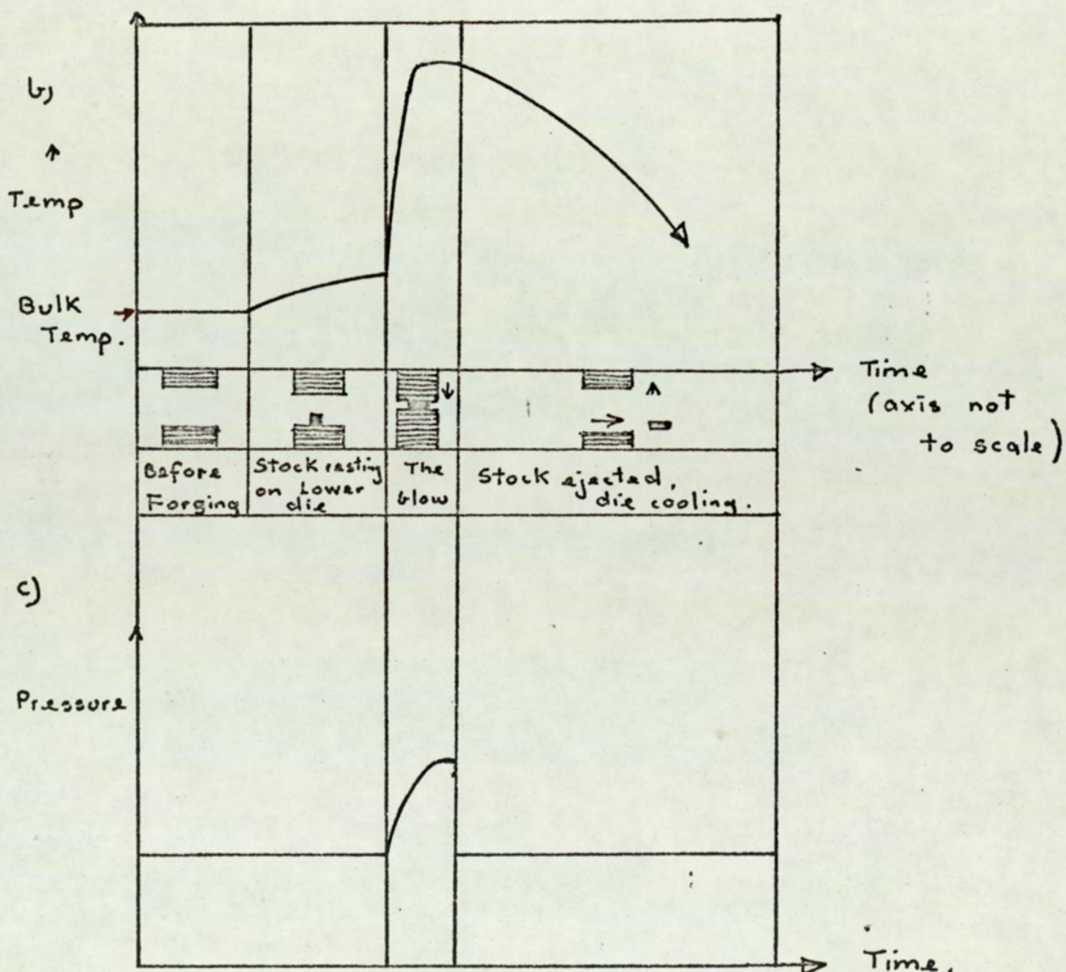
The relationships outlined above have been proved for a variety of materials and environmental conditions, but their validity in the forging situation has never been fully verified. Conditions favouring any or all of the types of wear can exist during forging, and it is uncertain how the proposed relationships for one type of wear are modified in the presence of another. Furthermore forging dies are worn at temperature, a temperature superimposed on the wearing system, not resulting from frictional effects as in the case of the wear of machine components, and under impact conditions of load. The situation is further complicated by the fact that both these factors fluctuate during the cycle.

A cold die being used in a hot forging process will warm up until a stable bulk temperature is reached, when the heat input from the hot stock being forged is balanced by the heat losses by conduction and radiation from the die. This temperature will depend on the temperature of the stock, the amount of deformation, the timing of the working cycle, the mass of the die and its surroundings, and the thermal properties of the stock and die. Fig. 2a) represents schematically the change in bulk temperature of a die over

Fig 2



a) Temperature variates over a long time



b) Temperature variation over a cycle
c, pressure variation over a cycle

Temperature and Load Relationships with Time.

a long time.

In addition, at each blow the die's surface will be considerably raised in temperature, setting up steep temperature gradients in the top layers of the die. When the forging is removed the surface will quickly cool back to the bulk temperature by conduction of heat to the mass of die material at a lower temperature, and possibly by radiation and conduction at the surface of the die, to the air. Fig. 2b) shows schematically the temperature fluctuations with time over a cycle[20].

The mode of fluctuation of load during a blow will be dependent on the type of forging equipment being used. In a press the stored energy of the flywheel is transmitted to an eccentric driven ram which then makes its stroke and rises again The pressure/time curve in this case can be represented as in Fig. 2c)[20]. It will be seen that the load rises from zero to a maximum during the blow, and drops back to zero immediately the ram starts to rise.

During the actual period of the blow, therefore, when metal is sliding and hence wear occurring, neither temperature nor load is a constant. The time taken for the surface to reach its maximum temperature is of the order of 0.02 seconds[21], which represents about 15% of the total blow time in the experimental press, but an approximation to the actual condition of the surface layers during the blow will be given if they are assumed to be at the maximum temperature throughout the stroke. No such simple approximation is possible for the load variation, but since, during the course of the tests in this investigation, load was kept constant, the effects of its variation with time will be consistent. The strain rate, which can quite markedly affect levels of strength and flow stress at temperature, was also kept constant.

Since strength and flow stress fall off approximately exponentially with increasing temperature the most important single factor affecting the properties of the surface layers and hence the wear, was the die temperature, in particular the maximum surface temperature and the severity of the temperature gradients. The levels of these factors are not directly measurable, but some indication of their likely values is possible.

Surface Temperature

Tholander[22] has reviewed most of the work done in the estimation of surface temperatures during forging and concludes that maximum die temperatures may be as low as 500°C or as high as 875°C. The temperature attained will partly depend on the bulk die temperature, recorded values for which range from 50°C[23] to 320°C[24], although it is believed[22] that in fact even higher temperatures may occur. Beck[20] supplies a useful set of curves relating stock and bulk die temperatures to the maximum surface temperature. The graphs are deduced from calculations assuming perfect heat transfer between stock and die, but in practice the heat transfer coefficient has been shown[25] to be reduced by a factor of 2 in the presence of light secondary scale, and by a factor of 5 if heavy primary scale separates the surfaces. Under such circumstances, therefore, the surface temperatures would be expected to be lower than those calculated. Alternatively, the effects of a lower heat transfer coefficient may be more than compensated by extra heat supplied to the system as a result of work done in overcoming the stock's resistance to deformation and the frictional resistance between stock and die. In this case temperatures would be higher than those calculated. The general trend of increase in surface temperature with bulk temperature would be maintained however, that is, an increase in bulk temperature leads to a correspondingly smaller increase in surface temperature at the same stock temperature level.

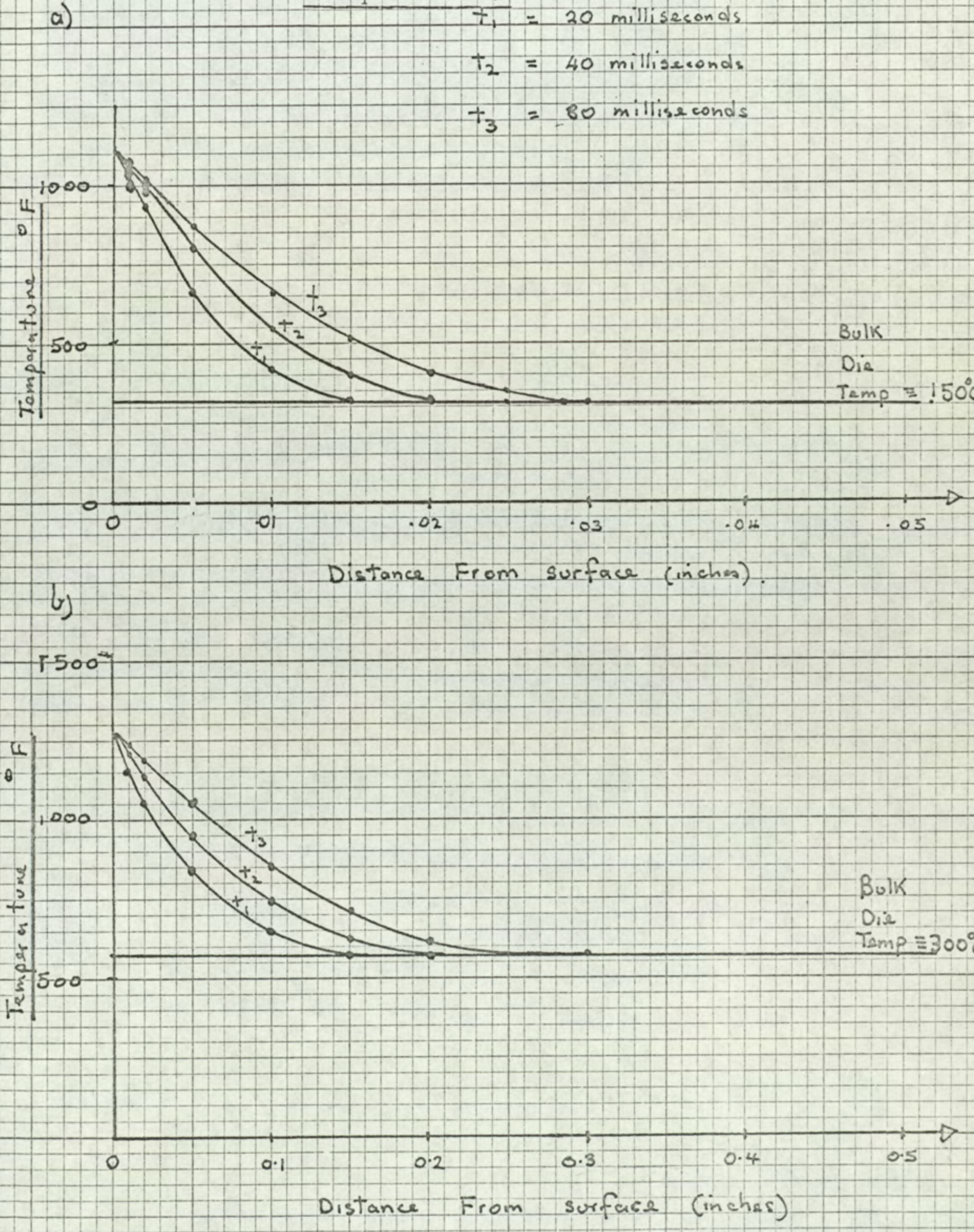
Temperature Gradients

There appears to be no literature concerning the severity of temperature gradients in dies, and no numerical values can be quoted. It is possible, however, to indicate the relative severity of the gradients from a consideration of heat flow during the heating and cooling portions of the cycle, in a simple case.

The simple case chosen was that of the heat flow along the axis of heat symmetry perpendicular to the faces of a flat plate of thickness d, with its back face maintained at a constant temperature equal to the bulk die temperature, and its front face subjected to a sudden heat input. This approximately represents the conditions at the centre of the insert in the experimental press. Ideal contact between stock and die was assumed, under which conditions a maximum temperature is immediately established at the interface which does not vary over a period of time. It was also assumed that the thermal properties of the steel were unaltered by temperature. Two bulk die temperatures were chosen, 150°C and 300°C, and the corresponding surface temperatures read off the graphs supplied by Beck, for a stock temperature of 1100°C. Values of temperature were calculated for points below the surface after 20, 40 and 80 milliseconds, for both systems, and the results are plotted graphically in Fig. 3. The calculations were made by reference to a relevant temperature response chart[26], and details appear in Appendix 1.

Fig. 3.

The temperature distribution with time for a die at 2 bulk die temperatures.



From the graphs it will be seen that the actual temperature gradient at the surface will be much greater than the average gradient over the die, and the maximum gradient will be attained immediately on contact, or very soon afterwards if a build up to maximum die temperature is envisaged. It is also apparent that the gradients existing in the die of the lower bulk temperature, at a particular time, are more severe than those in the die of higher bulk temperature, at the same instant in time. A further point of interest is that after 80 milliseconds a point 0.03 inches from the surface has still been unaffected by the heat input, implying that any structural changes consequent upon heat rise will be limited to very small zones near the surface. However, a specific temperature will always be attained to a greater depth in the die of higher bulk temperature than in the die of lower bulk temperature, so zones of transformation products or overtempered structure will be thicker on this die.

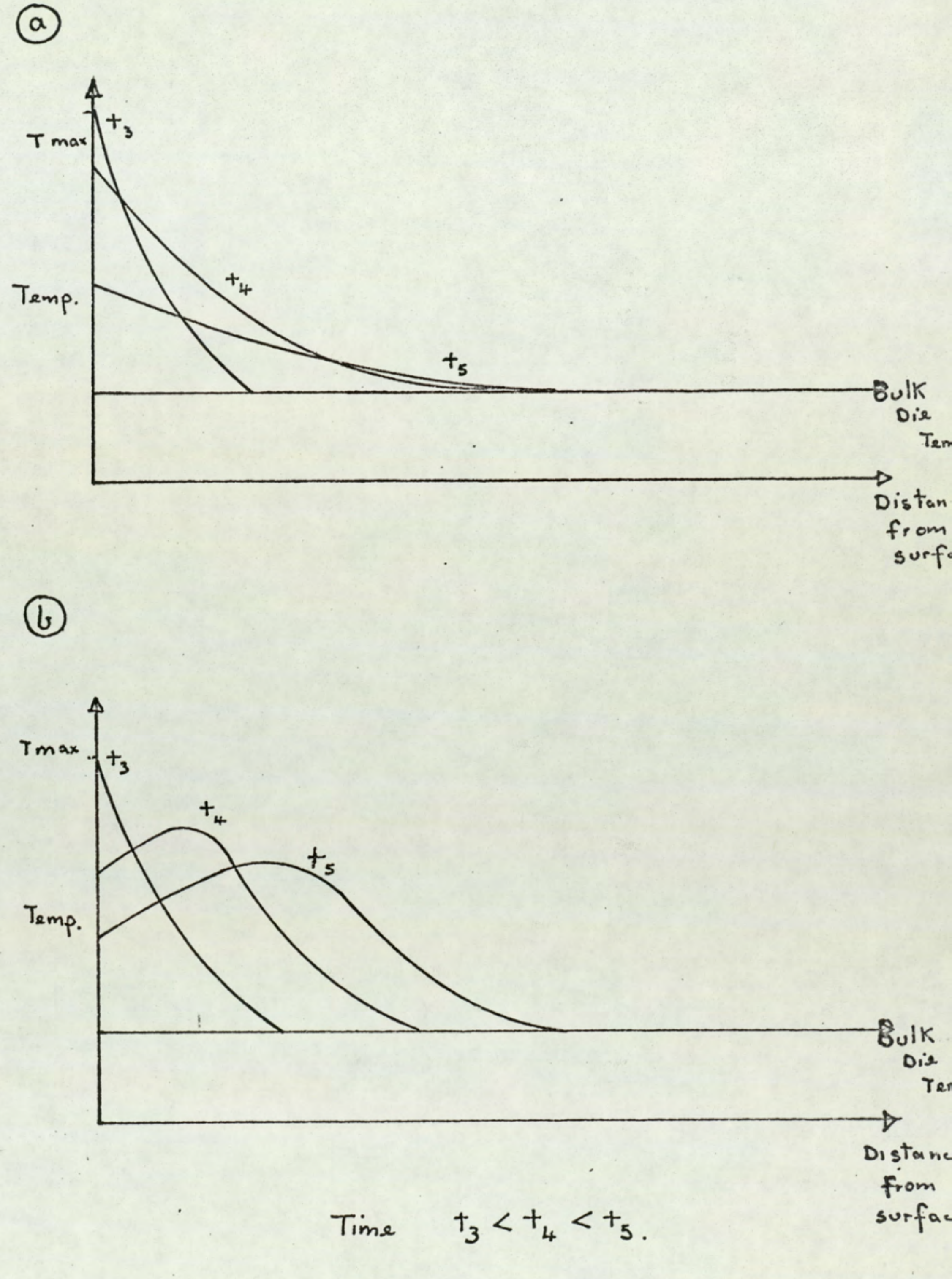
Such simple calculations cannot be applied to the cooling cycle because the temperature distribution at the start of the cycle, unlike that at the start of the heating cycle, is non-uniform. The situation has an analogue in electrical systems as a potential field theory problem, but the extreme complexity of the mathematical solution is beyond the scope of this investigation; the situation can therefore only be represented schematically. There are two possible modes of cooling:

(1) The surface cools solely by virtue of the heat flow into the bulk material, (Fig. 4a)), in which case the gradients remain negative, that is, stress is compressive, and decrease in severity with time.

(2) The surface is cooled by conduction and radiation to the surroundings as well as by heat flow into the bulk material, (Fig. 4b)), in which case positive gradients will be set up in the surface, but gradients of less severity than those induced during heating.

In general, therefore, it can be said that the temperature gradients set up in the die will depend upon the limits of the temperature cycle, and will be most severe at the beginning of the heating part of the cycle. Furthermore it will be the properties at the maximum surface temperature that will control the rate of wear, in particular the values of yield stress and hot hardness. Such properties for No. 5 die steel are available in the literature[27,28] for temperatures up to 700°C, and fall off very rapidly at the higher temperatures. A strong dependence of wear rate on temperature would therefore be expected.

Fig 4



Possible Modes of Heat Flow During the Cooling Cycle.

A.3.2. <u>The Scaling of Dies</u>

The possible influence on wear rate of the presence of an oxide layer has already been discussed. However, even if it lessens the severity of attack on the metal itself by being worn preferentially, loss of dimensions of the die will still occur as a result of its removal. It is desirable therefore to be able to assess the possible extent of dimensional loss as a result of scaling.

The rate at which a die scales will depend on a number of factors including its composition, temperature, time at temperature and the availability of oxygen. In a forging die time will be available for scaling between blows whilst the surface is cooling over a range of temperatures from maximum surface temperature to bulk die temperature. The composition of the scale produced at these temperatures is FeO[(40)], of hardness about 350 V.P.N., and the time available will only be sufficient for a thin, continuous film to be formed. Under these conditions the log. of the scale thickness at any temperature can be said to be proportional to that temperature (in degrees Absolute)[41]. The amount of scale forming between blows will therefore increase as the bulk die temperature increases, because the level of temperatures at which it is forming will be raised. However, the difficulties in attributing an actual figure to scale thickness, in view of the non-uniformity of the temperature, will be appreciated.

An indication of the thickness, however, can be obtained from figures quoted for the oxidation of iron and low alloy steels in air[41]. After exposure to air for 10 seconds at 380°C the scale thickness is 0.8×10^{-6} inches, and after a similar time at 700°C, 1.1×10^{-6} inches. The corresponding figures for 500 and 600°C are about $.9 \times 10^{-6}$ and 1.0×10^{-6}. If the worst conditions are envisaged, where all the scale formed between blows is removed by the sliding action of the hot metal, then the maximum total loss of dimensions after 1000 blows will be in the region of 0.001 inches and will increase some 10% per 100°C increase in "mean" scaling temperature.

Although these figures only provide a rough guide, it is obvious that scaling could account for an appreciable portion of the dimensional loss of a die during forging, and is a factor that needs to be taken into account when the causes of wear of die steels is investigated.

A.3.3. Results from Wear Tests on Die Materials

For a wear test to be of significance in assessing the wear of a die material it must simulate the conditions of forging, in particular those of temperature, temperature gradients and impact loading. No conventional wear testing machine can accomodate these variables, so wear tests on die materials must be carried out in a real hammer or press, either in a forge shop or on a smaller scale in the laboratory. Other environmental conditions that may influence the results of wear tests have been tabulated by Tholander[29] and Liotard[1], and some workers claim no experimental test rig can ever genuinely simulate these conditions. They have therefore made their tests on an industrial forge, usually testing materials as inserts in large dies. However, it can equally well be claimed that no one industrial environment is like any other. By testing materials in a real press under otherwise laboratory type conditions, as Lange and Meinert[30] did, some of the variables can be measured and most controlled; results from such tests would seem to be more meaningful and more widely applicable.

Strict correlation between results of work done on different forges or even on different test rigs is impossible, and the situation is further complicated by the lack of unanimity in the measured value taken to represent "amount of wear". In a fairly unsuccessful experiment[31] using a radioactive insert in a die the volume of metal removed was derived from the radioactivity of the transferred fragments and the scale. Wetter[32] measured the depth to which an insert was worn below the level of the main die block, and Tholander and his co-workers[22] are measuring the "difference in profile across a die", which is one way of indicating the depth of wear. They have also weighed dies before and after forging. Lange and Meinert[30] produced an anular wear pattern in a test rig similar to the one used in this investigation. They measured profiles of the worn surface across a diameter and estimated the radial area between the profile curve and the line of the original surface, and the mean radius of the anulus. A crude estimate of volume was obtained by rotating the area through 360° about the mean radius, and this value was recorded as the amount of wear. In the investigations performed at the laboratories of the Drop Forging Research Association[33,34] again using similar apparatus, the radial area was taken as a measure of wear. A measure of volume may be related to one of weight, although discrepancies will occur if deformation has taken place, but there is unlikely to be any consistent relation between either volume or weight and depth or area. At best therefore, results from die wear tests can only be viewed on a comparative basis.

The inconsistency apparent in the measurement of wear is reflected in the diversity of factors chosen to be studied in wear tests by different workers, and the general picture presented by the results is by no means complete.

No specific data on adhesive wear is available, although Smith et al[31] showed that metal/metal contact did in fact occur despite the high level of scaling they experienced during their test. When scale was prevented from forming during an otherwise identical test the wear rate was considerably lowered, indicating that the test material was losing dimensions because of the rubbing away of scale and/or that the scale was acting as hard cutting particles and producing wear by abrasion. It also implies indirectly that the amount of adhesive wear was small. The presence of an oxide film generally lowers the adhesive wear rate, so when scale was eliminated adhesive wear would have increased in severity, but this increase was masked by the much greater decrease in other forms of damage. This sort of result is consistent with the mild nature of adhesive wear in the non-forging environment.

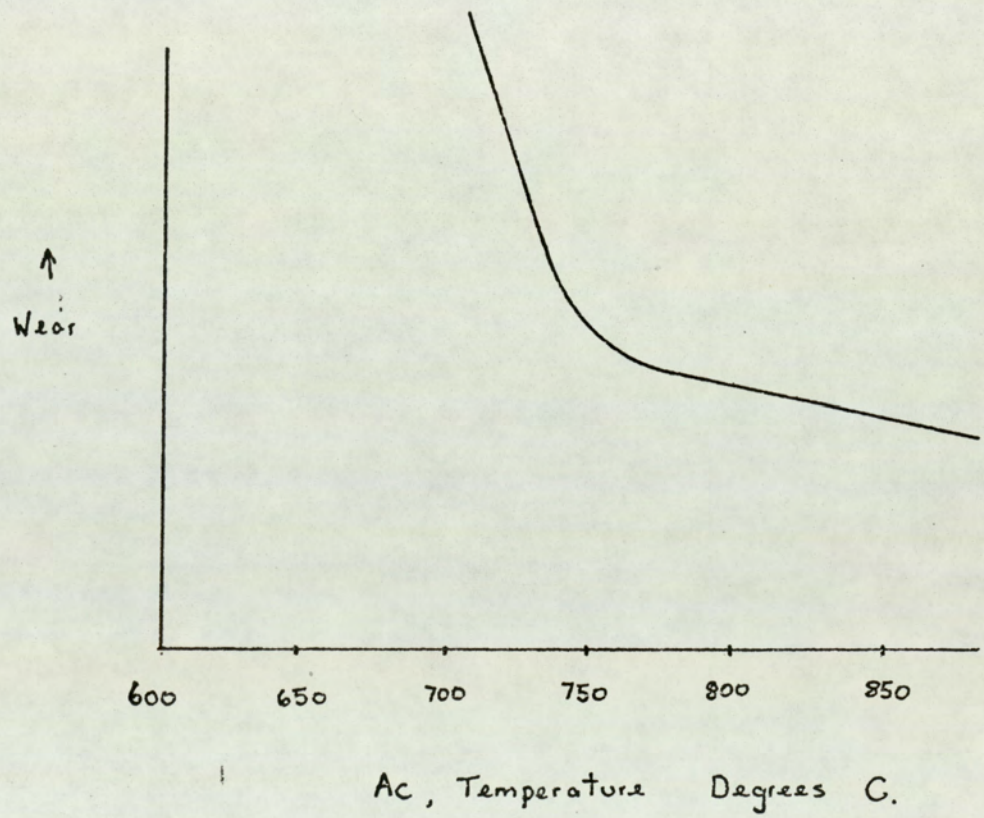
Wetter[32] attempted to relate wear to the structure of W/V/Mo steels, and found that wear decreased as the number of carbides left undissolved after tempering at the working temperature increased. It is unclear what kinds of wear were occurring during his test but in the light of other results it seems likely that abrasive wear predominated. The dependence of the abrasion resistance of steels on their structure and carbide content has already been discussed for the non-forging environment (Section A.2.2.). In a paper on the abrasion resistance of hot work tool steels[11] it was also suggested that an even distribution of carbide imparts good abrasion resistance at temperature, and further that the abrasion resistance of steels follow their high temperature strength retention abilities. In this case it would be thought that it is the stable and bulky carbides (of the type Wetter was investigating, in fact) producing secondary hardening and resistance to tempering that are most beneficial in reducing wear by abrasion of die steels.

Drabing[35] and Thomas[34] have both observed that the bottom die tends to wear faster than the top die, and two possible reasons for this behaviour are suggested. Firstly the phenomenon has been attributed to an increase in abrasive wear on the bottom die consequent upon scale flaking from the hot stock falling onto it. Secondly an alternative explanation may lie in the fact that the stock rests on the bottom die for a time before being forged, and during this time some heat is transferred to the die. If there is insufficient time for equilisation of temperature within the stock before the blow, on compression the bottom of the stock will be cooler than the top. Therefore during sliding the top die is cooler and being worn by hot, easily deformed stock whilst the warmer bottom die is being attacked more vigorously by cooler, more resistant stock. Under these conditions the bottom die would be expected to wear faster than the top even in the absence of scale.

There have been no direct tests relating wear by thermal fatigue to die properties, although heat checks have been frequently reported in used dies. Transformation products have also been observed in the surface layers of dies after forging[22,23,36], and the detrimental effect on forging life of these products, in particular if the cycle is such as to permit transformation to martensite between blows, has already been discussed (Section A.2.2.). Tholander[36] investigated several dies and discovered a variety of transformation products, most areas of which were associated with cracks in the surface. He concluded that the presence of transformation products was undesirable, and that it was the Ac_1 value of the steel that determined whether austenite was formed or not. Thomas[34] has tested a range of materials in an experimental press and plotted the severity of their wear as a function of their Ac_1 temperature. His results are reproduced graphically in Fig. 5. The shape of the graph implies that the surface temperature attained during the tests was in the region of $730^{o}C$, although a record of die temperature was not made. Since he believed the time between blows was insufficiently long to permit transformations between blows, the increase in wear of steels of low Ac_1 was attributed to their surfaces being austenitic during the blow, and in particular to the decrease in carbide in the surface layers consequent upon its greater solubility in austenite than in ferrite, that is, to a decrease in the abrasion resistance of the surface layers. No thermal fatigue cracking during these tests was reported.

When thermal fatigue does occur, in the industrial situation, the recommended procedure is to preheat the dies to reduce the severity of the temperature gradients, and Tholander[22] suggested that the consequent rise in surface temperature be offset by lowering the heat transfer between stock and die by the use of a suitable lubricant. However, at least in the non-forging environment, a lubricant is reported[2] to increase the rate of abrasive wear by flushing away debris clogging the abrasive particles, thus permitting

Fig.5.



The Dependence of wear on AC_1 Temperatures them to cut more efficiently. Its usefulness in reducing wear may therefore depend on the relative severity of the wear resulting from abrasion and from thermal fatigue in the particular situation.

The relationships between wear and hardness in mechanical systems has already been discussed, but no such simple relationships appear to hold true for all steels under forging conditions. As a result of many tests carried out on different materials at the laboratories of the Drop Forging Research Association[33] it was concluded that, in general, wear tended to decrease as the room temperature hardness of the steel increased. This would be the expected behaviour in the absence of thermal fatigue cracking, since hot hardness has been shown to change in the same way as cold hardness[37] and both adhesive and abrasive wear are inversely proportional to some function of hardness. If wear occurs as a result of thermal fatigue, as hardness is increased it may decrease because of the improved strength of the material or, if the strength improvement has been at the expense of ductility, increase. No cracking was reported during these tests however.

The decrease of wear with hardness was not consistent for all steels. Further, the wear rate of No. 5 Die Steel was found to reach a maximum at 320-340 V.P.N. but no reasons have been suggested for this anomalous behaviour. Because of the lack of consistency in results one must assume that factors other than hardness exert an influence on wear rate, and these, for some reason, are particularly dominant in No. 5 Die Steel.

Another area of uncertainty concerns the way in which wear rate changes during a long forging run. Drabing[35] states that "it is a well known fact that dies wear more during the initial stages of a run" and this is supported by evidence supplied by Tholander[22] and Smith, Southan and Whitely[31]. Wetter's[32] curves however, show an initially low wear rate, rising after about 400 blows.

The case of an initially high wear rate is the classic case of "running in" where adhesive wear rate in particular is high whilst bumps and asperities are being smoothed away or deformed, and whilst a protective film is being built up on the surface of the material being worn. An initially low wear rate can be attributed to two factors. Firstly, if the dies are not preheated, they are warming up during the initial stages of a run, bulk and surface temperatures are rising and strength and hardness of the surface layers consequently decreasing; the wear rate would therefore be expected to be initially low, rising until a stable bulk temperature is attained. Secondly, if wear is going to occur as a result of thermal fatigue there will be no wear of this kind during the incubation period before cracks are initiated; the overall wear rate would then be expected to start rising some time after forging is commenced. It would seem therefore that the type of wear which is predominant and the way in which its rate is altered by changes at the surface with time can account for any observed changes in wear rate during the course of a run.

Other wear tests have been performed on specific materials, or cast dies, but the results obtained have little bearing on the present study. The reported increase in wear resistance of surface conditioned materials[38,39], however, confirms the intuitive reasoning that it is properties of the surface layers of the die which govern its wear rate.

In general, the assessments that can be made concerning wear during forging are by no means conclusive. All types of wear have been observed, but the type of wear occurring is not always recorded, and only the most qualitative correlations have been made between forging conditions and the predominant wear mechanism. The general line of thought seems to be that it should be possible to eliminate thermal fatigue by correct choice of material, degrees of preheat and lubrication for the particular forging conditions, and that, if cracking does not occur, abrasive wear is responsible for most of the damage. However, the factors controlling thermal fatigue in dies are not known with sufficient certainty to enable the prediction of conditions under which cracks can be guaranteed to be absent.

It appears that the structural state of the die steel, and in particular its stable carbide content, is an important criterion in determining its wear resistance, and in most steels the austenitising of the surface layers would seem to be detrimental to wear resistance even in the absence of transformations between blows. No very satisfactory explanation can be given for the variation of wear rate with hardness, however, and the general picture of the wear process during forging lacks coherency.

A.4. The Present Investigation

The present investigation is a study of the influence of die temperature and hardness on the wear of No. 5 Die Steel, under hot forging conditions.

No. 5 Die Steel was the material chosen to be investigated because of its wide use throughout the British forging industry. Also, in similar investigations at the laboratories of the Drop Forging Research Association[33,34] this material was used as a standard and, since it was envisaged that work would be continued on the experimental press after this particular investigation were completed, the setting up of a standard, whilst investigating the problem in hand, was desirable.

The factors under investigation were chosen because of their undoubtedly significant effects on wear and because of their relevance, as directly measureable parameters, to the forging industry.

Many observed wear effects on industrial dies had been attributed to the influence of temperature, but no sytematic investigation of the variance of wear with die temperature had ever been made, and in view of the wide range of bulk die temperatures reported in the literature, it seemed desirable that such an investigation should be performed.

Hardness is so commonly used in the forging industry to describe the state of a die block that, although some data on the wear/hardness relationship was available for No. 5 Die Steel[33], an independent investigation did not seem superfluous. Further, it was hoped, by relating the wear to observations of structure and the extent of thermal fatigue cracking, it would be possible to explain the unexpected shape of the published curve.

Two series of tests were therefore run, one on inserts of the same initial hardness, at various levels of bulk die temperature, and the other on inserts of various initial hardnesses run at the same level of temperature. After measuring the wear, all worn inserts were sectioned examined metallographically, and results were analysed in such a way as to make possible the correlation of the amount and type of wear with both the original parameters and the structural state of the material during forging.

SECTION B. The Wear Test and the Evaluation of Worn Dies

Wear tests in general, and wear tests under forging conditions in particular, are notorious for their lack of reproducibility. During the tests performed in this investigation, therefore, it was deemed desirable to eliminate as many extraneous variables as possible in order to reduce the scatter of results. To this end the tests were conducted under the simplest of conditions consistent with the retention of the essential elements of a forging process.

Such variables as rate and amount of deformation of stock, the timing sequence and die shape can be kept constant by forging consistent stock to a consistent reduction, at predetermined intervals of time, between flat dies or die inserts. This idealised situation was realised in practice by performing tests on an experimental press similar in design to that used by Lange and Meinert[30] and at present in use in the laboratories of the Drop Forging Research Association in Sheffield[33]. The automation of the press permitted a constant timing sequence so that stock in the form of identical cylindrical slugs was forged between flat die inserts once every ten seconds, to a reduction set by the positioning of the tools.

Other factors that could possibly affect results, such as material composition and hardness, surface finish of the dies and squareness of the tools could be kept within known limits by careful control of die preparation and tool setting before forging. The remaining important variable, that of die temperature, was one of the factors chosen to investigate, so this too was controlled within known limits for each test.

To ensure that these variables were in fact controlled, and in order that direct correlation between all test results should be possible, a standard procedure for wearing and evaluating all the die sets was set up, and rigorously adhered to throughout.

The details of these procedures, together with a description of the functioning of the experimental press, are contained in the following sub-sections. Wherever possible information not directly necessary to an appreciation of the experimental work has been appended to permit more ready assimiliation of the principles involved.

B.1 The Wear Test

B.1.1. The Experimental Press

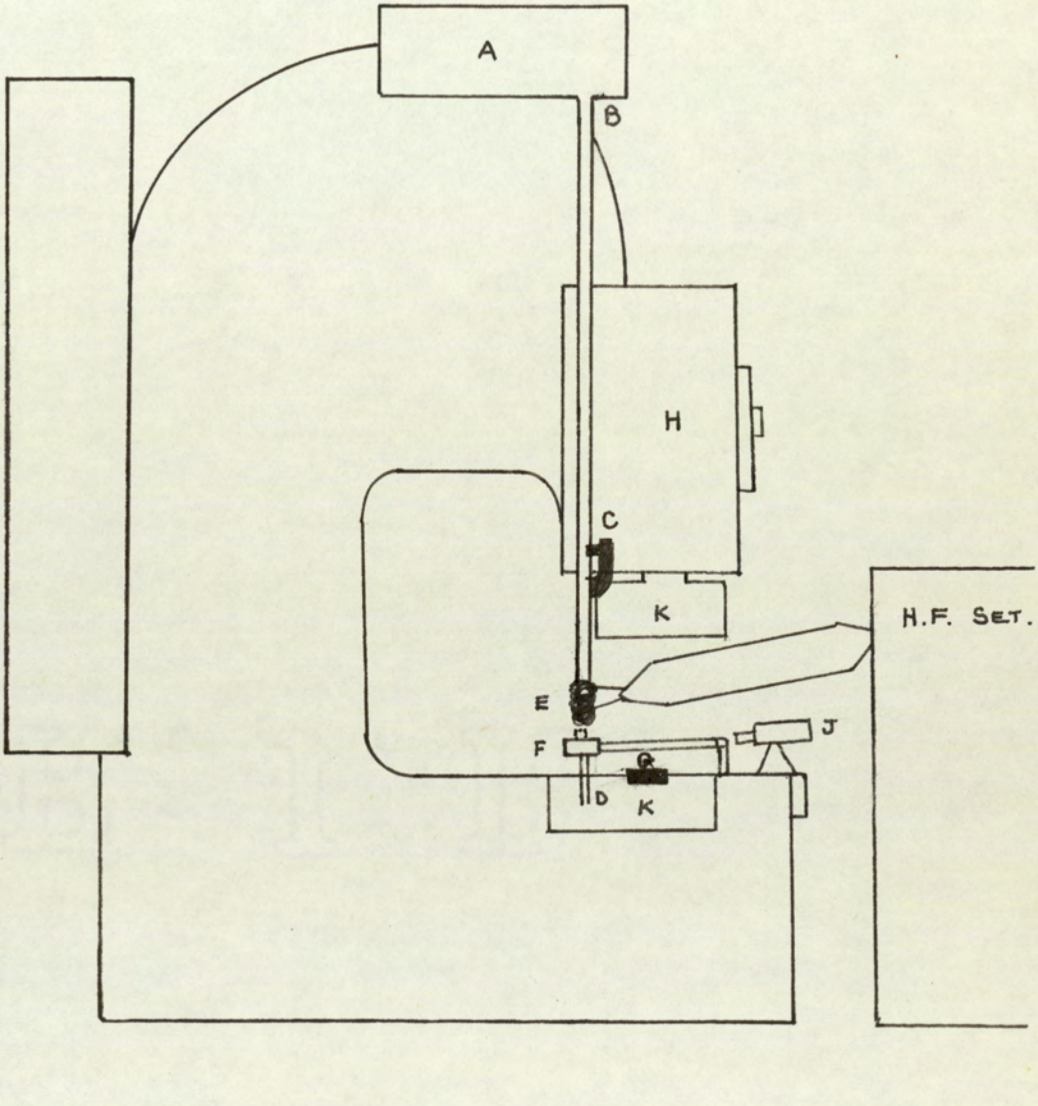
The press used was an old Sheffield cutlery press, modified, with considerable help from the Drop Forging Research Association, whose previous experience with a similar system proved invaluable, to run continuously, automatically feeding and upsetting the slugs between the inserts. The feeding cycle was controlled by a cam timing device, started and stopped by a toggle switch mounted on the front of the press. Each operation was controlled by a pair of cams which tripped micro switch contacts at the appropriate time, and each pair of contacts controlled a solenoid valve to switch compressed air to the appropriate cylinder to activate the corresponding part of the feed mechanism. A high frequency induction heater heated slugs before upsetting to a temperature which was determined by the power output of the set, since the time of heating was controlled by the cam timing device that controlled the whole of the automatic feeding cycle. The forging stroke was initiated electrically at the end of the feed cycle, when a pneumatic cylinder engaged the fly wheel to the crank and completed the forging stroke. Ejection of the forged slugs was also automatic. The whole cycle of operations took ten seconds, and was repeated continuously. The blow time of the press was calculated to be 0.13 seconds, which is somewhat longer than that found in large industrial presses (about 0.05 seconds) but was governed by the speed of the motor and hence was inflexible.

The Press Cycle

The component parts of the press are identified with reference to the accompanying annotated diagram (Fig.6) and the photograph of the press (Fig. 7).

A rotary feeder (A) mounted on top of the press discharged stock in the form of mild steel slugs $\frac{1}{2}$ inch diameter and $\frac{3}{4}$ inch long into the vertical feed pipe (B) where they fell into a pneumatically operated escapement mechanism (C). A ceramic peg (D) was raised into the silica tube surrounded by the high frequency coil (E) and the escapement mechanism dropped one slug onto it. The H.F. set was left on throughout the cycle, so heating began immediately. After the predetermined time, the ceramic peg was lowered, to leave the hot slug between the jaws of a pair of feed tongs (F). These tongs pivoted through 90° to bring the slug to rest on the centre of the bottom die insert face (G). The finger of the tongs was withdrawn, and they returned to their original position beneath the H.F. coil and tube, leaving the slug in position. As they came to rest the finger returned to its gripping position and the tongs

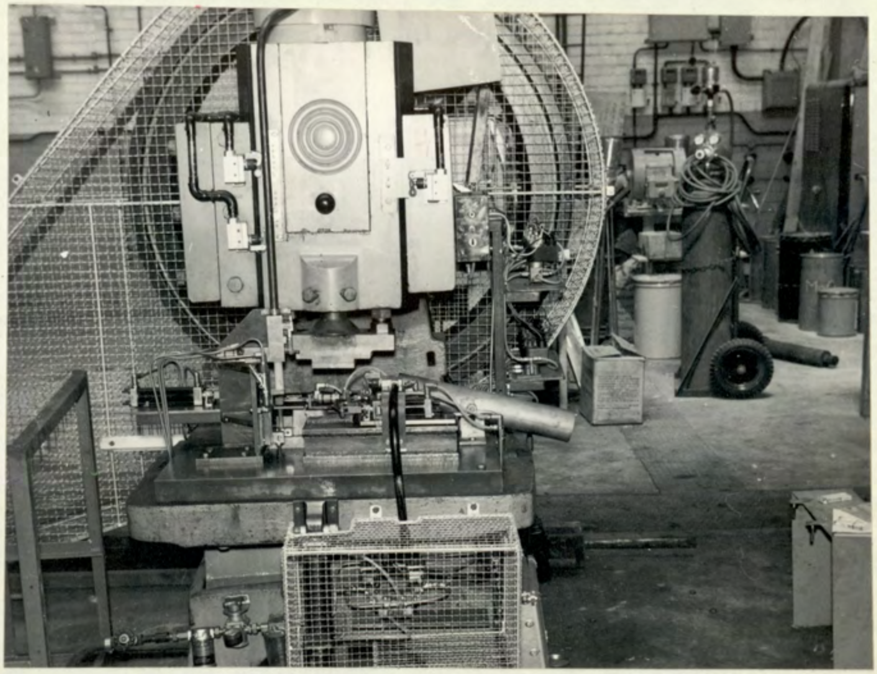
FIG 6



A Schematic Diagram of The Experimental Press.

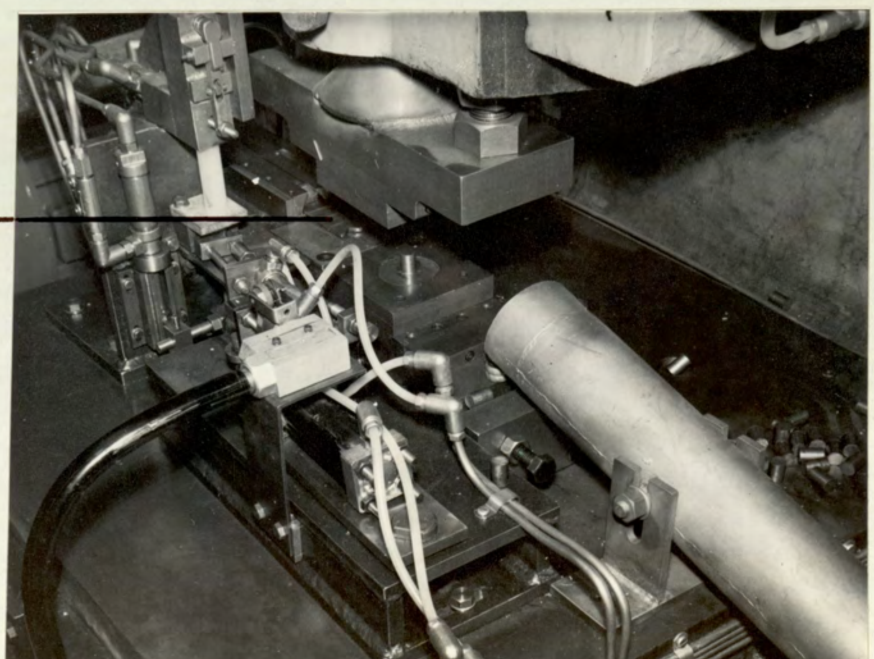
Fig. 7.

a) General View.



b) The Feeding Mechanism.

I



The Experimental Press.

tripped a microswitch which operated a solenoid valve allowing high pressure air to enter the clutch cylinder, so engaging the clutch. The ram (H) therefore moved down and compressed the slug. In so doing it activated a microswitch on the side of the ram and opened an air inlet to the ejector (I) which moved forward until stopped by the presence of the ram. In this position air pressure built up behind the ejector so that, when the ram started to rise, it rapidly moved between the die faces and ejected the compressed slug into a bin. As the ram neared the top of its stroke it activated two microswitches. One switched air to retract the ejector; the other switched air to the other side of the clutch cylinder, disengaging the clutch and allowing the ram to halt at top dead centre.

Safety Factors

The position described at the end of the cycle was the "safe" position for starting and finishing an operation. The press was manually brought to this position after stopping for any reason, to preclude the possibility of the ram descending out of sequence. In addition, the feed mechanism was always checked independently at the beginning of a run, with the press motor switched off.

To increase safety of operation of the press and to preclude the possibility of forging a cold slug a photocell relay (J) was introduced, which viewed the hot slug and if this were not present at the correct time (as determined by the cam timing device) cut off both the H.F. heater and cam control, thus stopping both the heating and the forging processes. The device was also fixed with an override switch to bypass it for restarting after such an incident, or for testing. A red warning light showed when the bypass switch was on, to protect against the press running continuously with the safety circuit disconnected.

B.1.2. The Manufacture of Die Inserts

For the sake of economy of material and convenience in handling, the die steel under investigation was used in the form of 2 inch diameter, $\frac{1}{2}$ inch thick disc shaped inserts, held in the main die blocks (K) of the experimental press.

The incoming stock was in the form of about 2 inch diameter rolled bar, in the annealed condition. The inserts were made by turning the bar down to size and slicing it into discs 0.530 inches thick. Discs were paired randomly and identified with a set number and a T. or B. to designate top or bottom die. They were then heat treated to specification together with test pieces of the same material, which were later sectioned to permit comparison of the worn die's structure with its original state. The heat treated dies were surface ground to final dimensions and checked for hardness to ensure any decarburised layer had been ground away. Thermocouples were then inserted (see section B.1.4. and Appendix 2.1.) and the inserts were ready for use.

The inserts fitted into close tolerance holes in the main die blocks and were secured by long screw-threaded pegs through the blocks, butting against the curved portion of the die insert. The inserts were checked for squareness to each other before a test was run.

B.1.3. <u>Stock Material and Preparation</u>

In order to eliminate extraneous variables, consistency of the slug's surface condition, dimensions and squareness were felt to be important. Bright mild steel bar was chosen in preference to black bar because of its more consistent dimensions and its lack of heavy secondary scale, and from this slugs were cut accurately to length on a six spindle automatic lathe. The "pip" produced on the end of the slug as a result of this operation was removed as a secondary operation on the same machine.

At first slugs were counted by hand, then later assessed by weight, but more recently a counting device has been attached to the front of the press and is activated every time the ram falls to compress a slug. The number of slugs forged can therefore be accurately recorded.

B.1.4. <u>Heating and Temperature Control of Dies</u>

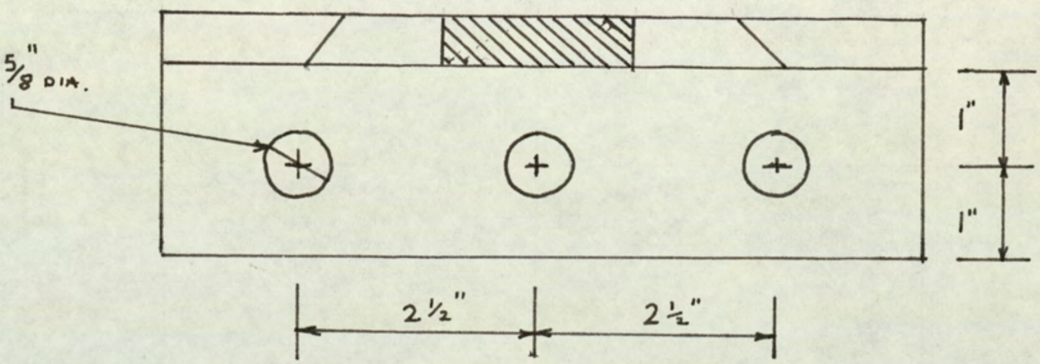
The size of the stock relative to the mass of the die blocks is much smaller than that found in an industrial press, and without auxilary heating the bulk temperature only rose to about 60°C. To simulate more realistic bulk temperatures, therefore, heaters were introduced into the main die blocks, and the temperature attained by the inserts was recorded and controlled by a thermocouple in the insert itself.

B.1.4. a) <u>Heating</u>

Heating was effected by the introduction of three Cressal cartridge type heaters, each supplying 400 watts at 250 volts A.C., into both main die blocks. The heaters were arranged transversely and positioned as close to each other as was commensurate with the retention of structural strength in the blocks (Fig. 8.).

Close tolerance holes were drilled and reamed so that the heaters, when cold, were a tight push fit; when heating they tended to expand to even more securely fit the holes, thus providing more efficient contact for heating by

Fig 8



The Position ofThe Heaters in the Die Block conduction.

Insulation around the die blocks was necessary both for more efficient heating and for safety, to localise the areas of dangerously hot metal. Boxes were constructed from $\frac{1}{4}$ inch thick sindanyo sheet fastened together with screws, to fit loosely around the base and sides of the blocks, and spaces between the block and the insulating box were filled with asbestos wool. The insulation of the top block was less efficient than that of the bottom, since because of its shape and the necessity of its being held in the ram it could not be completely encased in sindanyo.

Nevertheless, utilising this system, it was found that both die inserts could be raised to about 350°C in an hour, and that heat was sufficiently localised for safety.

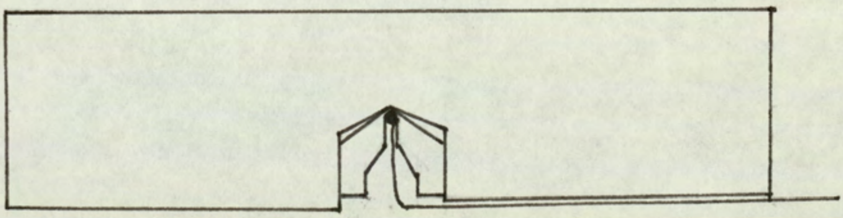
B.1.4. b) Temperature Recording and Control

Since it is the bulk temperature of the insert that is of interest, it was decided to use a thermocouple in each insert to both record and control its temperature. The thermocouple was to be placed directly below the centre of the insert face, in order not to upset the heat flow in the die, and at sufficient depth to be uninfluenced by the temperature fluctuations near the surface of the insert consequent upon the forging process.

At first a fine guage couple was simply sparked into a dimple produced in the centre of the back face of the insert. Its efficacy in recording true temperature was ascertained by subjecting the front face of the insert to heat input from a large mass of heated steel, and recording the temperatures registered by the couple. These correlated well with temperatures measured on the back of the insert, using Tempilsticks. However, the joint between the thermocouple bead and the insert proved insufficiently strong to withstand the impact conditions of load in the press itself. It was decided therefore that, for reliability, the thermocouples should be sunk into the inserts and given backing support.

This was achieved in principle by sparking the thermocouple wires to the top of a small heat sink machined from the die material, which was fitted into a close tolerance hole drilled centrally in the back of the insert. The bead thus butted against the insert itself at $\frac{1}{4}$ inch below the surface, and the leads were led down through the heat sink and out along a groove machined radially in the insert's back face. A cross section of the completed assembly is shown diagrammatically in Fig. 9. and fuller details and dimensions appear in Appendix 2.1. The accuracy of

Fig. 9



The Thermocuple Assembly

temperature measurement was proved in a way similar to that used before.

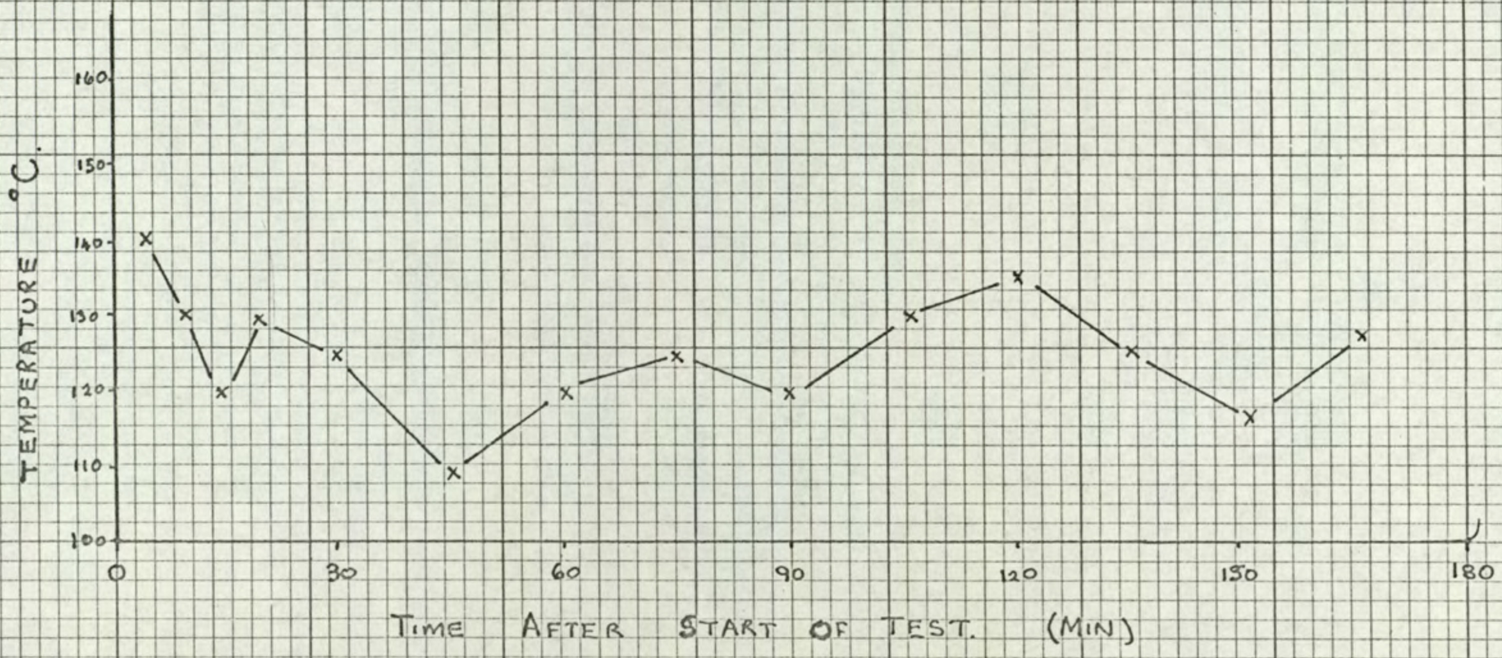
Before a test run in the press the leads from each thermocouple were connected to a Transitrol, set to the required temperature, which recorded the temperature of the inserts and, at the same time, controlled the power input to the heaters. During a test run temperatures were recorded at least every 15 minutes and Fig. 10. shows a typical curve of the temperature fluctuation with time. The fluctuations were consequent upon the lack of sensitivity of the controllers and were unavoidable with the equipment available. The difference between the lowest and highest temperatures recorded was usually about 30^{o}C, and the "bulk die temperature" was taken as the average temperature during a run.

B.1.5. Heating and Temperature Control of Slugs

As has been mentioned before, slugs were heated by induction for a set time, the temperature attained being governed by the output of the H.F. unit. Unfortunately this output was responsive to mains voltage fluctuations, and the cost of a mains voltage stabiliser was considered prohibitive. For this reason it was necessary to make periodic checks of slug temperature just before compression,

FIG 10

A typical curve of temperature fluctation with time during a test run.



using an optical pyrometer, and to ajust the H.F. setting by hand should it prove necessary. Although fairly crude, this method was refined by experience of when severe voltage fluctuations were most likely, and in practice slug temperature variations of greater than 20°C were never recorded during a test run.

Since there was a time lag of about 3 seconds between the slug descending from the coil and its being upset, the slug temperature just before the blow was lower than that attained in the coil. The delay was necessary from a mechanical point of view and also ensured temperature equalisation within the slug, but it was found that, when the power output of the H.F. unit was just less than that necessary to cause incipient melting of the slug in the coil, the maximum temperature obtainable just before compression was 1090°C. In practice it was undesirable to run so near the melting point because, if slight variations in voltage or slug characteristics should cause one slug to just start to melt, the feed mechanism would be jammed and shut down of the press would be necessary. In practice, therefore, test runs were performed with an "on die" slug temperature of 1060 - 1080°C.

B.1.6. The Recording of Test Conditions

For every test a standard sheet was made as a record of the conditions under which the test was performed. The die set identification number, its heat treatment and hardness were entered before starting. A complete test record showed the time heaters were switched on, room temperature, the top and bottom die temperature at intervals in time of not more than 15 minutes, the temperature of the slugs, the reduction of the slugs and the number of slugs forged. Any stoppages during a test were also recorded, as were any unusual observations or comments.

B.2. Evaluation of the Worn Die Insert

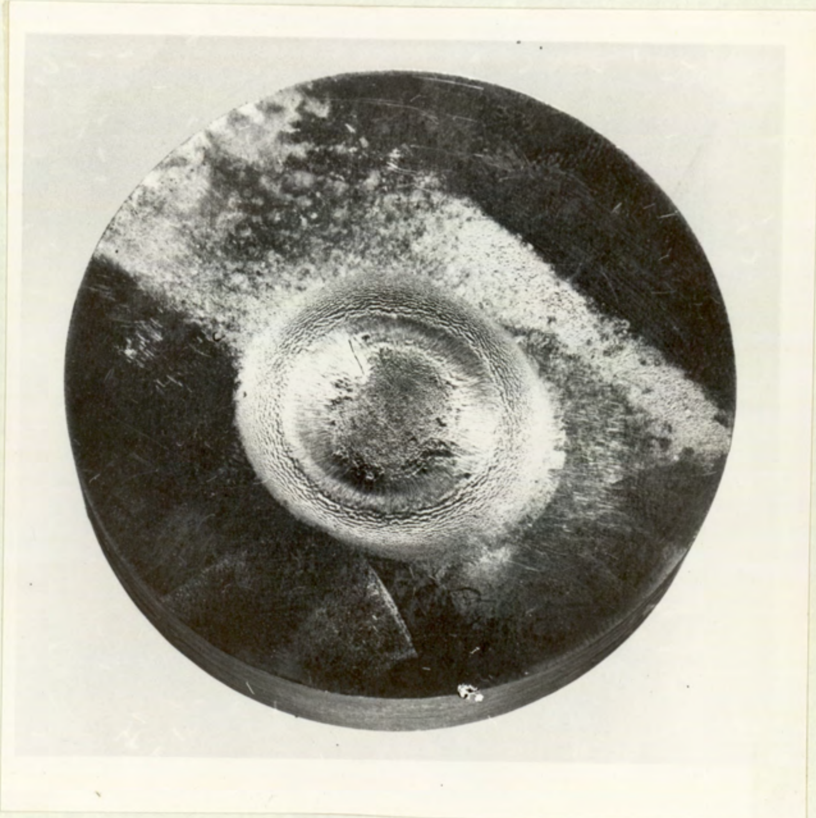
B.2.1. Measurement of Wear

A typically worn die insert is shown in Fig. 11. and it will be noted that the region where metal has been removed is in the form of an anular ring around a central plateau where the slug first rested.

In accordance with other evaluations of similarly worn inserts[30,33] profile traces were made across the die surface to determine the deviation from its initial flat state. Four traces were taken across each insert always in the same sequence (Fig. 12.a)), to allow direct comparison between them should it prove desirable. The traces were made using a commercially produced surface measuring machine known as Talylin I, connected to a chart recorder to give a graphical representation of the profile. This machine provided a choice of three horizontal magnifications, x2, x5, and x10, and six vertical magnifications, x200, x400, x1,000, x2,000, x4,000 and x10,000, covering a range of depths to give full scale deflection on the trace from 0.01 inches to 0.0002 inches. It was also possible to obtain, simultaneously with the graphical trace, the same output in digital form on punched tape, one point being recorded every $^1/10$ second.

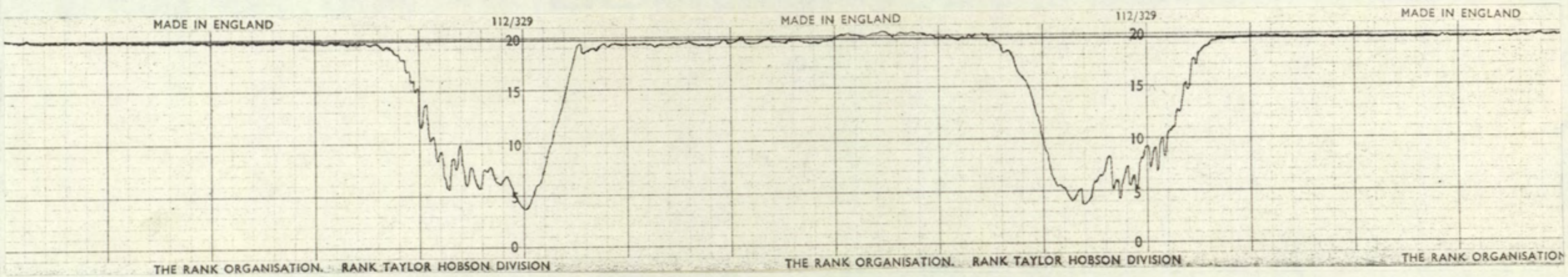
The wedge-ended stylus standard on the equipment could

Fig. 11a).



The Worn Insert.

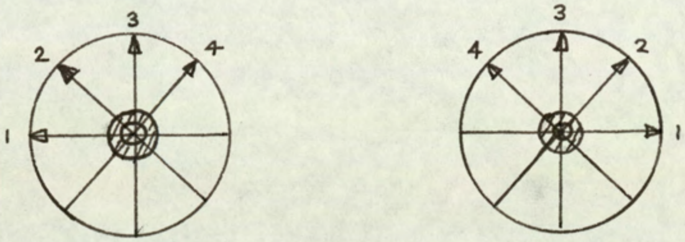
Fig. 11b).



A Typical Profile Trace. Approx. $\frac{1}{2}$ size.

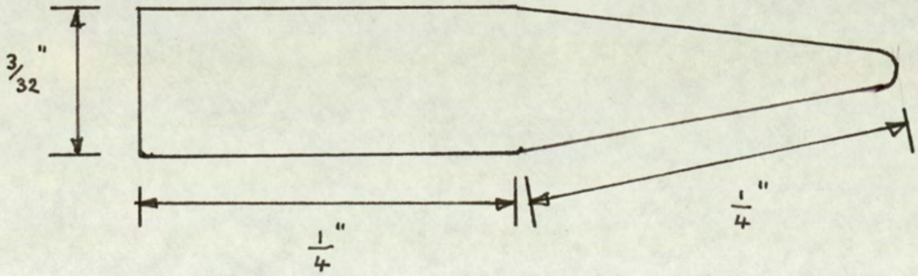
Fig 12

a)



The Order of Tracing The Inserts

b)

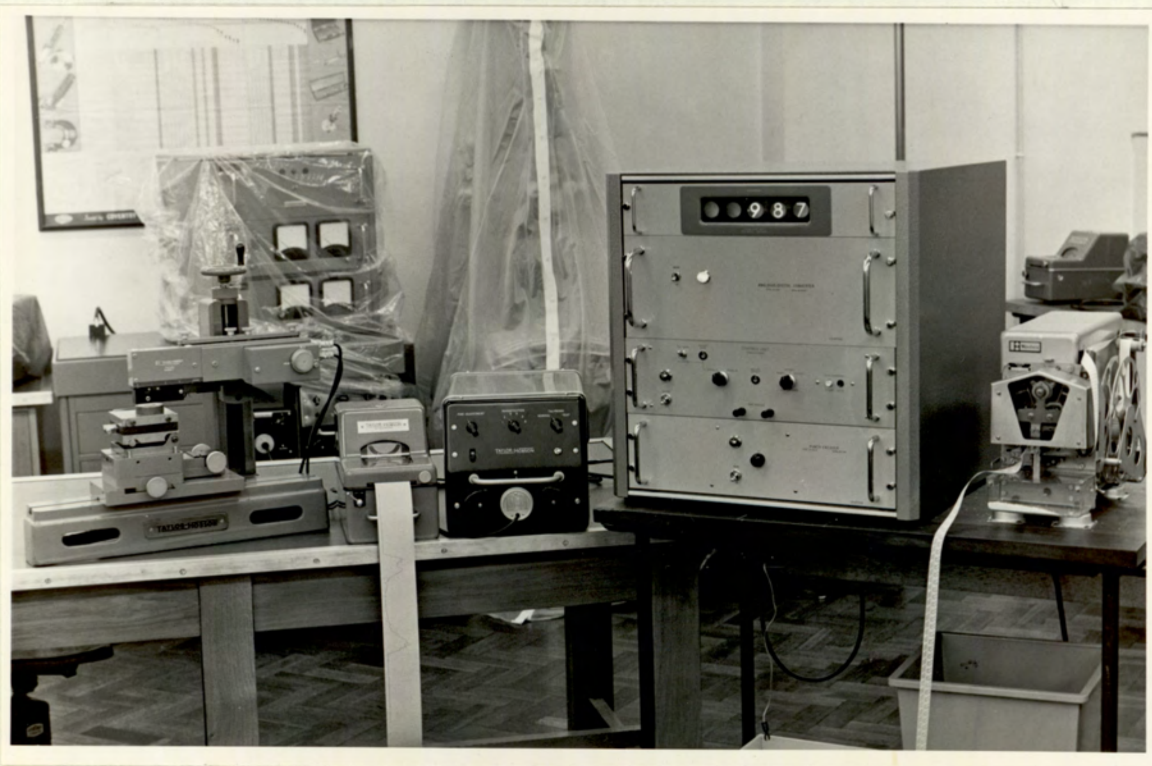


The Stylus

not, because of the annular nature of the wear patterns, penetrate to the full depth of the impression. More pointed styli were therefore manufactured and tested on previously evaluated worn dies obtained from the Drop Forging Research Association, and on blocks containing grooves machined to known dimensions. The sharpest styli scratched the surface and tore at the material, but a stylus of oil hardened silver steel, as shown in Fig. 12.b), proved sufficiently pointed to provide accuracy in tracing without harming the surface. This stylus was therefore adopted for all measurements.

A photograph of the surface measuring equipment is shown in Fig. 13. The insert to be traced was held on a magnetic table and approximately levelled by means of screws on the arm of the instrument. The insert was moved so that the stylus ran along the trace line designated 1, and was set to zero at its edge. The horizontal magnification was set at x10, and the vertical magnification adjusted so that the trace recorded as near filled the chart as was possible. The stylus was reset to zero and the traversing motor switched on. When the stylus started to move, as indicated by a click as the cam, rotated by the traverse motor, engaged with the driving arm, the chart recorder and digital output unit were started up simultaneously, and left

Fig. 13.



The Surface Measuring Equipment.

running until the stylus reached the opposite edge of the insert. They were then stopped simultaneously and the traverse motor switched off. In this way the two forms of output were made to correspond exactly. The trace and the tape were identified with the insert identification number, that is a set number and a T or B, and with a number between 1 and 4 corresponding to the particular trace.

The electromagnet holding the insert was then switched off and the insert rotated through 45° in preparation for setting up for the next trace.

Evaluation of the Trace

The problems concerning the diversity of wear evaluation have already been discussed (Section A. 3. 3.). In this investigation an evaluation of the radial wear was deemed to be desirable in order that results could be compared with those obtained in the laboratories of the Drop Forging Research Association. However, it was felt that although such a value is in some way representative of the amount of metal removed, it could not easily be related to physical reality. In the practical forging situation it is the loss of dimensions that renders a die unusable, so for this reason a measure of the maximum depth of wear was made from each trace and the average value for each insert recorded. More theoretically, wear is defined as the volume of metal removed and this value was felt to have the greatest physical significance. Since the volume is too small to be assessed accurately by direct methods, such as the weighing of the insert before and after use, it was necessary it be calculated from the data provided by the contour trace. A computer programme (see Appendix. 2.2.) was written to perform this otherwise monumental task, advantage being taken of the digital output facilities on the Talylin. The same programme can also compute the radial wear area. Results recorded as volume and area for a particular insert were the average of the values obtained from all four traces across the insert.

B.2.2. Descaling of Inserts

It was found that when inserts were run at bulk die temperatures greater than 50°C scale formed on the surface, particularly at the rim of the central plateau where, when the slug was resting on the insert before compression, the temperature was faily high and a plentiful supply of oxygen was available. This scale was sufficiently thick to cause inaccuracies when tracing the surface. It was therefore necessary to descale most of the inserts before tracing, but in so doing not to alter the contours of the wear impression.

Several descaling solutions were considered (Appendix. 2.3.) and the following standard procedure was set up using the most efficient and least harmful one. All but the front face of the insert was stopped off by painting with "Fortolac". 300 ccs. of 5% sulphuric acid was prepared in a beaker and heated to 78 - 80°C, and 8 drops of Quinoline stirred in. A carbon anode attached to the electropolishing unit was lowered into the beaker and when the temperature had dropped to 74°C the prepared insert, connected as cathode to the electropolishing unit, was lowered in. The controls of the unit were set to give a current of 4.2 amps. When descaling appeared to be complete, after treatment for $\frac{1}{2}$ to $1\frac{1}{2}$ minutes usually, the current was switched off, the insert raised, swilled in clean water and brushed with a toothbrush. If any scale remained adherent the insert was retreated in the same manner until scale-free. The total treatment time for any die was not longer than 3 minutes.

After descaling the "Fortolac" was carefully removed from the back of the insert so as not to cause any difficulties in levelling it when tracing.

B.2.3. Metallography

B.2.3.a) Preparation of Specimens

Sections were to be cut from the worn inserts and examined metallographically in order that the evaluations of the amount of wear could be related to structural properties.

It has already been shown that there is good reason to believe that it is the very near surface layers of the die which have the most profound influence on wear, and calculations of temperature performed in Section A.3.1. indicated that temperature fluctuations and consequently areas of temperature affected structure are likely to be confined to such layers. Regions of transformation products in particular, should they occur, would be expected to be very thin. Since the zone of interest is therefore likely to be very small, it was felt that the conventional method of sectioning, perpendicular to the worn face, could lead to errors in assessment; effects very near the surface could be missed or masked if the edge had been in any way damaged by the sectioning operation, or if polishing had been less than perfect.

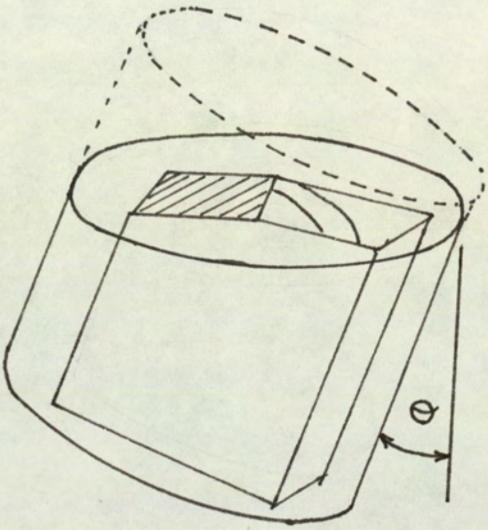
It was decided therefore, for the purposes of this investigation, to adopt a taper sectioning technique providing apparent magnification of the surface layers. Details of the standard procedure set up for the preparation of the taper sections are contained in Appendix 2.4. but in principle an insert is sectioned in such a way that the edge of the polished surface lies along a radius, and the apparent magnification of the near surface layers is about 10 times (Fig. 14.). It will be noted that the edge of interest is about central in the mount, and has metal backing on both its sides. These two factors contributed to the ease with which the edge was retained during polishing.

Interpretation of the Tapered Surfaces

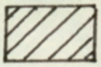
Care must always be exercised in the interpretation of results from taper sections, particularly when assessing the path of cracks or directional structural effects, and this was particularly true in the case of these sections taken from the inserts. Because of the anular nature of the wear pattern there was a limit to the depth within the polished face at which "true" effects could be observed. Fig. 15. will clarify the problems involved.

The plane ABCD represents the original surface before the grinding of the taper, and the plane ABC_1D_1 the polished surface on which observations were being made. When viewing the "below plateau" region along AD_1 any effects genuinely below the plateau cannot be seen at a section depth greater

Fig 14

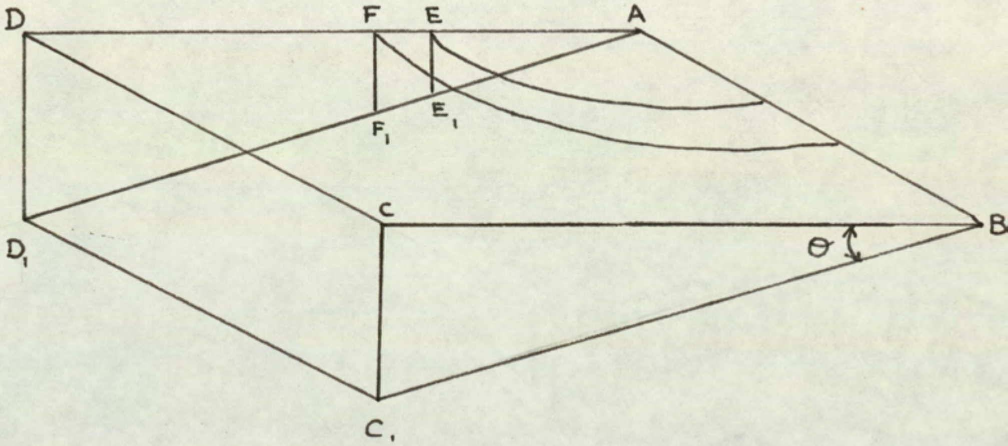


θ - Taper Angle

 - Polished Surface

The Taper Section

Fig 15.

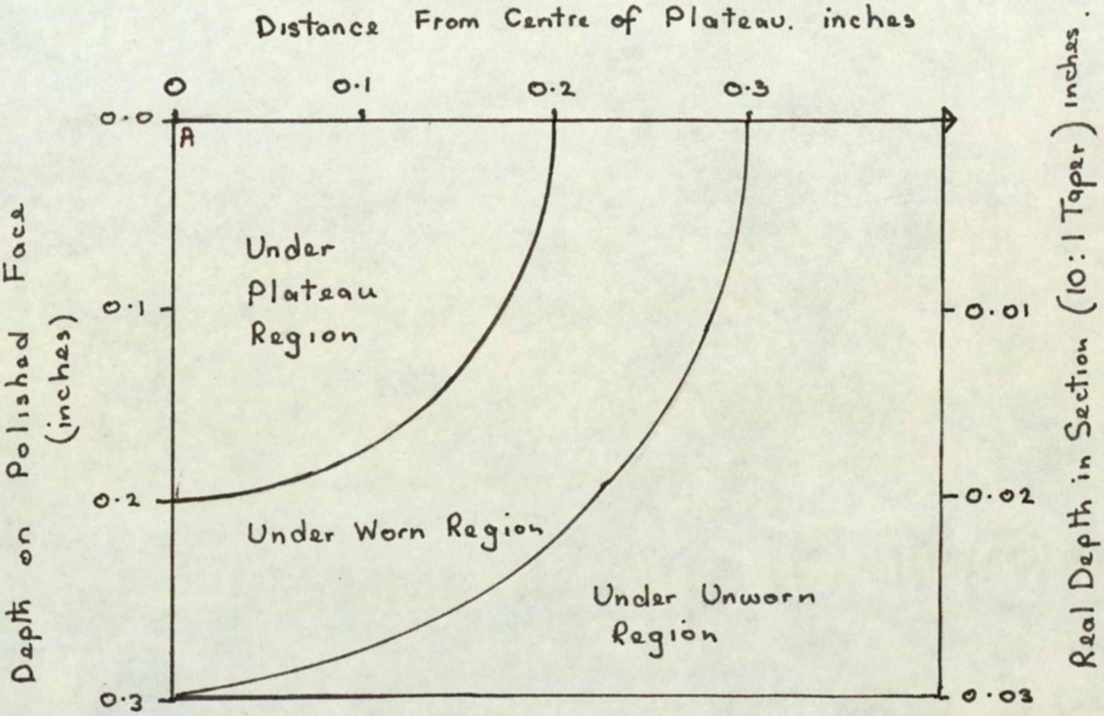


The Limitations of the Taper Section.

than AE_1, because between E_1 and F_1 the region being observed is actually below the wear anulus, and the region F_1 to D_1 is under the unworn part of the insert. Therefore any effect extending to a real depth greater than EE_1 beneath the plateau cannot be observed on this section. Furthermore this maximum depth of view of the below plateau region decreases towards zero as the worn area on the free edge is approached. The "below worn area" structures are similarly affected by sectioning, although effects to a greater depth can be observed. Also the apparent thickness of any scale layer observed on an insert will be subject to the same limitations.

The plateau region of the worn surfaces was about 0.4 inches diameter, and the anular groove about 0.1 inches wide. Since θ is very small ($5^\circ 44'$, the taper angle) AE_1 can be assumed to equal AE. The polished surface can therefore easily be mapped out as in Fig. 16. to correspond to the real regions it represents. If true depth contours are plotted on this figure it can be seen that, by careful observation, effects below the plateau could be observed to a real depth of 0.02 inches, and below the worn area up to 0.03 inches, but the curvilinear nature of the representative areas had to be adhered to. In practice this was achieved fairly simply by using a calibration table under a microscope.

Fig 16



The Limits of the Taper Section.

The section was set square with respect to the two directions of movement of the table and the calibrations zeroed when cross wires in the microscope eyepiece were set on the corner designated A in Fig. 16. Any point within the section could therefore be given co-ordinates provided by the readings of the two movement micrometers, and related to its true position and depth from a figure like Fig. 16. where the true depth contours related to the particular taper of the section. Similarly any line of particular interest, say the plateau/wear junction, could be traced by setting up on co-ordinates along it.

All the inserts were taper sectioned and investigated in this way, and in general surface effects were found to be confined to the top 0.015 inches of the insert. In any case where it seemed likely that such effects extended beyond the limits observable, however, conventional vertical sections were also examined.

B.2.3.b) The Examination of the Sections

All specimens were examined in the as polished condition, in particular to assess the extent and depth of any cracks.

To reveal general structure the specimens were etched either in saturated Picral to which was added a couple of drops of hydrochloric acid to increase its activity, or in 2% Nital. The Picral solution will normally give an etch, on fine structured alloy steels, that permits greater resolution of carbides under the microscope than will a Nital etch. This increased detail proved desirable in the samples under consideration, and most observations of structure were made on specimens etched in Picral. However, problems were encountered with the staining of the section edge resulting from seep-back of the etchant from the metal/mount interface, even after prolonged swilling and drying. Sections, therefore, had to be viewed very soon after etching. When specimens were etched in Nital, however, the undesirable staining could be eliminated, so this etch was utilised whenever it was desired to look at several specimens together for comparison purposes, and when photographs were to be taken.

The microexamination of etched structures revealed any transformation products near their surfaces, the depth of which could be measured and related to real depth in terms of the particular taper of the section. The extent of overtempering was less easy to assess however, because there was little apparent structural difference between the overtempered and the original regions of the material. Etching in Groesbeck's reagent[42] to reveal only the carbides gave no more useful information and tended to destroy the mounting material, making repolishing difficult, so its use was abandoned, and as a more sensitive method of assessing the degree and extent of overtempering, microhardness measurements were made on the sections.

Microhardness Measurements

The instrument used was a Leitz Miniload Hardness Tester, permitting loads between 25 and 300 grams. to be applied to the diamond.

The change in hardness with depth under any region of specimen could be mapped out by setting up co-ordinates on the calibrated table, as was described in Section B.2.3.a); this method provided the desired information concerning the change of properties within the insert.

A measure of the surface hardness of the worn region was also found to be necessary, but because of the irregular nature of the surface of this region its position could not be assessed to any degree of accuracy. However, hardness assessments on several specimens indicated hardness levels under the worn regions of the wear anulus to be comparable to those at equal depth below the plateau region. Since the plateau is flat, its edge was plane on the section, and its position exactly specified. Further, measurements could be made along a straight line perpendicular to the plateau surface, for a distance, without infringing regional boundaries, thus simplifying the setting-up operations. Measurements were therefore made at increments along a line perpendicular to the plateau edge, and the results plotted against the distance from the edge. Translation into real depth was not necessary for this assessment. Hardness often dropped off quite severely towards the edge of the specimen, so, in order to obtain the best estimate of its level at the surface, the hardness axis of the plot was made as large as possible and the point where the extrapolated curve through the measured hardness values met the position of the edge was estimated to the nearest 2.5 V.P.N. Values obtained in this way are those quoted in the results.

All inserts were tested and evaluated in accordance with the procedures propounded in this section, and the following section enumerates the levels of test variables and the results obtained from the evaluation of the worn inserts.

SECTION C. Tests Performed and Results

Each major portion of this section relates to an aspect of the experimental work and is divided into two sub-sections, the first outlining the actual work done, the second giving the results obtained together with a certain amount of particularly pertinent discussion.

C.1. The Incoming Stock and its Heat Treatment

C.1.1.

The first stage of the experimental work involved the evaluation of the incoming bar stock. A chemical analysis of the material was obtained and longitudinal and vertical sections taken from the bar for metallographic examination.

In order that levels of time and temperature could be specified for the heat treatment of the material, specimens of the steel were heat treated according to the specifications laid down[43] and tempered at several temperatures for different times. From the results a master tempering curve, relating hardness to tempering parameter, could be drawn, and the tempered specimens were also sectioned to ascertain the uniformity of structure after heat treatment.

C.1.2.

The analysis of the steel was found to be 0.59% C, 1.52% Ni, 0.72% Cr, 0.24% Mo, 0.59% Mn and 0.25% Si which was 0.01% low in molybdenum, but otherwise within specification.

The longitudinal and transverse sections examined in the unetched condition showed inclusions elongated in the direction of rolling of the bar (Fig. 17 a) and b)), which would eventually lie perpendicular to the face of the insert. Such inclusions were considered undesirable, because they could act as stress raisers precipitating the initiation of thermal fatigue cracks, and because they were probably associated with segregation in the material. The microstructure of the longitudinal section is shown in Fig. 17 c), and indicates that the material was in fact segregated.

The master tempering curve obtained for the steel is shown in Fig. 18. and the microstructures of the tempered specimens showed no decarburisation effects at the edges, although evidence of segregation was again apparent.

Despite the material's deficiencies there was no choice but to use the stock available, and to bear in mind its defects when evaluating results.

C.2. Control Die Sets

C.2.1.

It is reasonably certain that every variable that could affect wear during a test was not being controlled, and in particular it was thought that press characteristics

Fig. 17.

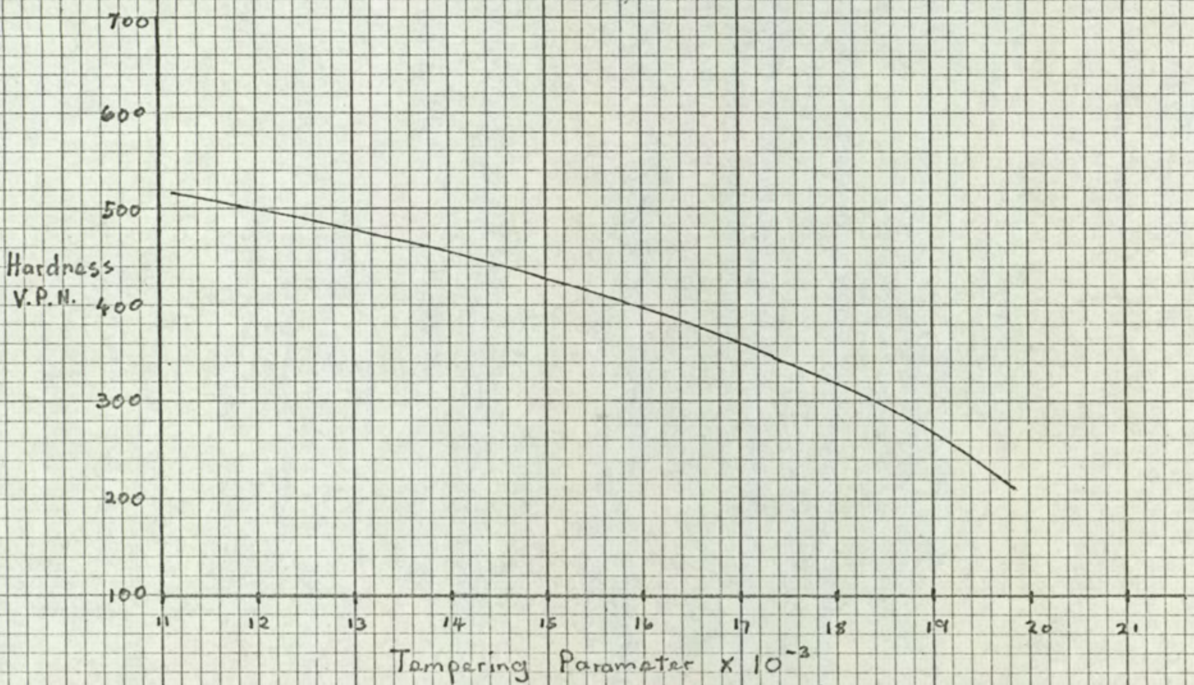
a) Longitudinal
Section.
Unetched.
x 400.

b) Transverse
Section.
Unetched.
x 400.

c) Longitudinal
Section.
Etched - Picral.
x 400.

The Incoming Stock.

FIG 18



Parameter $= T\,(20 + \log t)$ Where T is the tempering temperature in degrees absolute and t is the tempering time in hours.

The Master Tempering Curve.

might alter over a period of time, and affect results. It was therefore felt necessary that identical tests be run periodically to determine whether or not trends were occurring that influenced results.

Five sets of inserts, designated S.1 to S.5 were therefore heat treated together to 350 V.P.N. (oil quench and temper 1 hour at 600°C), and run under identical test conditions. In fact only three sets were used, S.1 being tested before the first series of tests, S.2 between the two series, and S.3 as the last set run.

The first set was run before the heating and temperature control systems had been perfected, so no auxilary heating was applied and neither were die temperatures recorded. Further, the maximum slug temperature that could be attained at this stage was 980°C. These rather unusual test conditions were simulated for the other two sets, in that direct correlation of results was essential to show up the type of trends feared possible.

C.2.2.

The test conditions and results obtained from the three tests are tabulated in Fig. 19. The results indicate that no trend was occurring and, in fact, the values of wear recorded are amazingly consistent considering the reputed

Fig. 19

Insert Set	Slug Temp. °C	Final Slug height inches	Number of slugs forged	Wear Volume cu. ins. x 10^5
S.1.T.	960 - 980	0.25	1000	457
S.1.B.	960 - 980	0.25	1000	810
S.2.T.	960 - 980	0.25	1000	432
S.2.B.	960 - 980	0.25	1000	823
S.3.T.	960 - 980	0.25	1000	465
S.3.B.	960 - 980	0.25	1000	831

Conditions and Results for Control Die Sets degree of scatter for forging tests. The worst variation in result was in the top insert, where the highest value recorded was some 8% greater than the lowest.

Although three tests is an insufficient number on which to make a statistical analysis of error, the results from these tests indicate that, in general, the values of wear volume recorded can be taken to be correct to within about 10%.

C.3. and C.4. The Series of Tests

As has already been indicated it was chosen to run two series of tests, one on inserts heat treated to the same level of hardness, but tested at different bulk die temperatures, called Series 1 tests, and the second at a constant bulk die temperature, but with inserts heat treated to various levels of hardness, called Series 2 tests. In all other respects the tests were to have been identical.

However, there were in fact two major differences between them. Firstly, during Series 1 tests an air blast was employed between blows to blow away scale that had flaked off the stock on compression and was left on the insert face after a blow. It was felt, however, that this blast might be contributing to the thermal fatigue cracking, since bottom inserts were found to be cracking more than top ones, and so its use was continued for all Series 1 tests, for the sake of consistency, but abandoned for Series 2 tests.

A brush arrangement for removing the scale was tried, working from the same pneumatic system as had activated the blast, but could not be perfected in the time available. For Series 2 tests, therefore, the excess scale was permitted to remain on the insert face.

Secondly, modifications were made to the H.F. coil during the interval between the two series of tests, in order to improve uniformity of heating of the slugs. Whilst this improvement was achieved, it resulted in an inability to reach the same level of stock temperature before incipient melting of the slug occurred in the coil. The stock temperature of the Series 2 tests was therefore some 20°C lower than that of Series 1, averaging about 1050°C as opposed to 1070°C.

At the start of Series 1 tests very little was known about the temperature levels at which inserts would actually run as a result of the heat input from the stock over and above the heat input from the heaters, so the original criterion was one of trial and error to obtain the desired temperature levels. When more experience had been obtained, it was possible to fill in the gaps in results. Die insert sets therefore were not necessarily run in numerical order, and the system of numbering was further upset by the fact that some sets failed to complete a test and were later repeated, and that some set numbers were assigned to special material dies run for other investigations. In order to make the discussion more coherent therefore, new set numbers have been assigned to the inserts, those for Series 1 being prefixed by a figure 1, and numbered in order of ascending bulk temperature, and those for Series 2 being prefixed by a figure 2, and numbered in order of decreasing hardness. In the tabulation of test conditions both the original and the revised set numbers will be given, but thereafter reference will only be made to the revised numbers.

C.3. Series 1 Tests

C.3.1.

The hardness level of Series 1 tests was chosen to be 350 V.P.N., near the bottom of the "C range" for die blocks, and heat treatment was specified as oil quench and temper 1 hour at 600°C. All the sets were heat treated together to ensure their uniformity, and the actual hardnesses after heat treatment were 320-325 V.P.N. The discrepancy between predicted and attained hardness was attributed to inadequate control of either tempering temperature or time. At a later stage, when the test pieces that had been tempered with the dies were sectioned to compare the original structures with the heat affected structures in the worn inserts it was found that the material had suffered a slack quench and contained bands of retained austenite, particularly associated with inclusions, that had partially decomposed to martensite on tempering (Fig. 20). Since several inserts had already been run when this was discovered it was decided preferable to continue to run sets of similar structure, even if inhomogenous, rather than to re-heat treat the remaining sets.

Series 1 tests were therefore carried out on rather inhomogenous material, at bulk die temperatures between 65 and 430°C. The actual test conditions are indicated in Fig. 21.

It will be noted that the bulk temperature of the bottom inserts ran some 10-20°C higher than the top, and this effect is accounted for by a combination of factors. Some heat was transferred to the bottom die during the time the hot slug was resting on it before the blow. Also the slug was scaling during this time, but the face resting on the bottom die was deprived of oxygen, therefore, at the beginning of compression, the heat transfer coefficient between slug and bottom die was probably greater than between slug and top die. Further, the insulation of the bottom die block was more efficient than that of the top. The incremental temperature rise consequent on heat input from the stock was therefore greater for the bottom die.

Fig. 20.

The Inhomogeneity of the Heat Treated Material.

Etched 2% Nital. x200.

Fig. 21

Original Insert Number	Revised Insert Number	Bulk Die Temperature
14.T.	1.1.T.	65
14.B.	1.1.B.	80
23.T.	1.2.T.	90
23.B.	1.2.B.	100
15.T.	1.3.T.	140
15.B.	1.3.B.	160
20.T.	1.4.T.	145
20.B.	1.4.B.	150
16.T	1.5.T.	180
16.B.	1.5.B.	190
21.T	1.6.T.	200
21.B.	1.6.B.	215
17.T.	1.7.T.	230
17.B.	1.7.B.	240
19.T.	1.8.T.	275
19.B.	1.8.B.	290
18.T.	1.9.T.	320
18.B.	1.9.B.	320
13.T.	1.10.T.	370
13.B.	1.10.B.	430

For All Sets: Hardness - 320 - 325 V.P.N.

Slug Temperature - 1060 - 1080°C

Slug Reduction to 0.26 ± 0.01 inches

Number of Slugs Forged - 1000

The Conditions of Test for Series 1 Inserts

C.3.2.

Die sets 1.10, 1.9. and 1.8 were inevaluable in terms of wear volume because scaling and bulk deformation had occurred to such a degree as to make the base line of the traces no longer horizontal, so the original surface line could not be assessed with any degree of accuracy. Similar data is missing for insert 1.7.T, in this case because the data tape output was faulty and the traces could not be repeated because the insert had already been sectioned when the fault was discovered. All these inserts, however, were sectioned to provide additional metallographic data and indicate trends in thermal fatigue cracking at higher levels of temperature. These metallographic results, and the full results for other sets are tabulated in Fig. 22.

The Variation of Wear with Temperature

The results for wear volume, plotted as a function of bulk die temperature are shown in Fig. 23. It will be observed that the amount of wear increased with bulk die temperature up to 150°C, above which temperature the rate of increase dropped to almost zero.

Surface Structure

Insert sets 1.1, 1.2 and insert 1.3.T were softened at the surface to an increasing degree as the bulk temperature

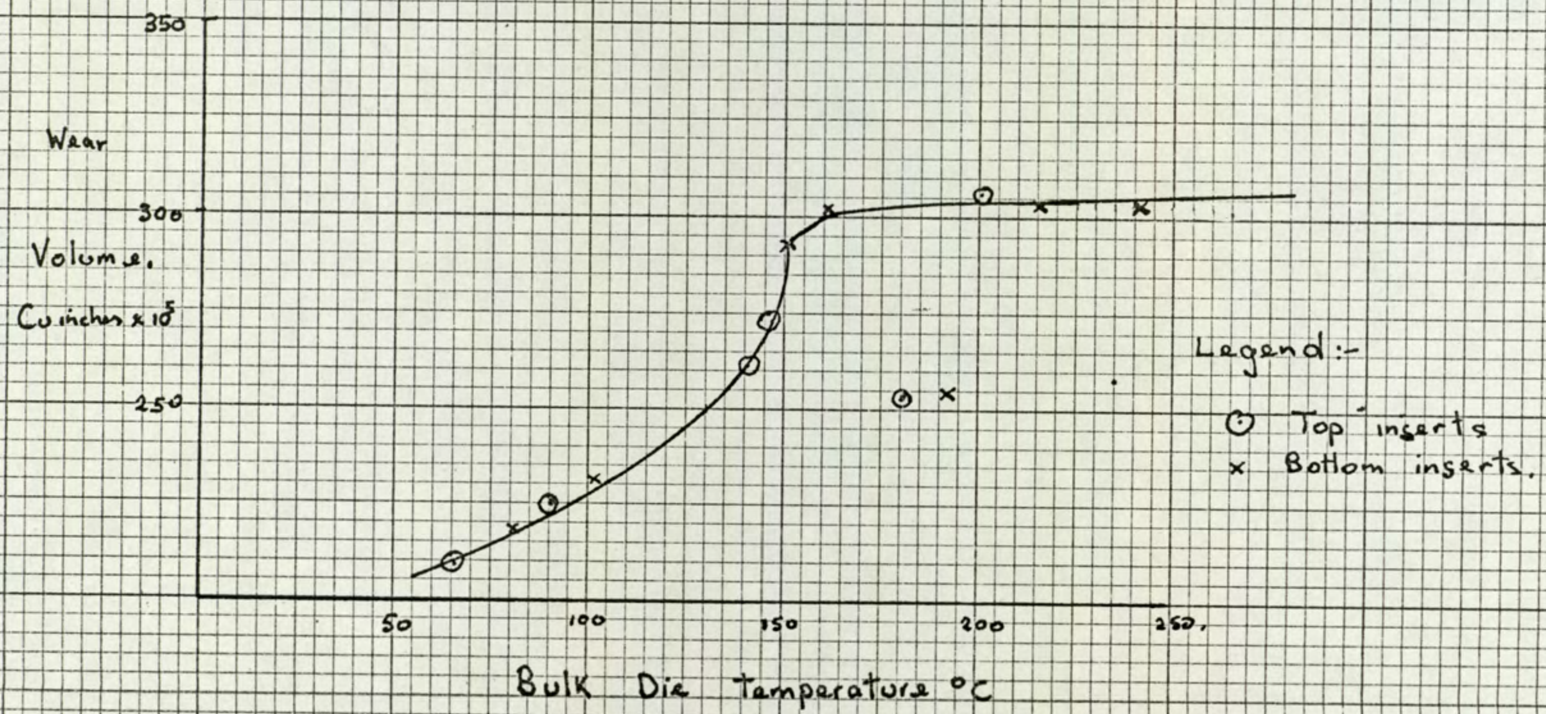
Fig. 22

Insert Number	Wear Vol. cu. ins. x 10^5	Wear Area sq. ins. x 10^5	Wear Depth ins. x 10^5	Surface Hardness V.P.N.	Real Depth of Checking ins. x 10^4	Real depth of transformation product ins. x 10^4
1.1.T.	210	124	140	310	-	-
1.1.B.	219	125	150	300	-	-
1.2.T.	225	140	130	297	-	-
1.2.B.	232	150	150	295	-	-
1.3.T.	262	172	155	290	-	-
1.3.B.	301	180	165	320	17	95
1.4.T.	273	170	170	285	-	-
1.4.B.	294	180	135	320	-	50
1.5.T.	254	157	145	290	-	-
1.5.B.	254	146	135	290	-	-
1.6.T.	306	198	140	320	10	110
1.6.B.	303	181	145	320	20	110
1.7.T.	-	-	-	320	20	130
1.7.B.	303	177	144	320	25	160
1.8.T.	-	-	-	320	25	170
1.8.B.	-	-	-	320	33	170
1.9.T.	-	-	-	320	30	-
1.9.B.	-	-	-	320	35	-
1.10.T.	-	-	-	320	30	-
1.10.B.	-	-	-	320	40	-

Results for Series 1 Inserts

FIG 23

The relationship of wear and bulk temperature.



rose. In the insert 1.4.T. small, discrete regions of transformation product were observed, mainly under the plateau, and the remainder of the surface was in a softened state. In inserts run between 150°C and 430°C a continuous layer of transformation product existed under the plateau and worn regions (Fig. 24). The exception to this case is insert set 1.5, whose level of wear was also inconsistent with the general trend of results. It must be assumed that some error was made in temperature measurement for this die and that it did run at a surface temperature lower than the recorded conditions indicate. How this error was made, however, is inexplicable. The hardness of the transformed layer did not vary, although its thickness increased with increased temperature, (Fig. 25). The thickness for sets 1.1 and 1.2 was not measured because the transformation product extended beyond the legitimate field of view on a taper section, and since wear volume could not be evaluated for these sets, it did not seem to be worthwhile to produce vertical sections.

A plot of hardness versus depth was made on two vertical sections, 1.3B and 1.7B to assess the actual extent of the softening and to be able to deduce the maximum temperature at the surface during forging, (Fig. 26a)). It was found that softening did in fact occur to a small degree up to

Fig. 24.

Vertical Section, Showing Transformed Edge.

Etched 2% Nital. x200.

The thickness of the Transformation Product.

FIG 25

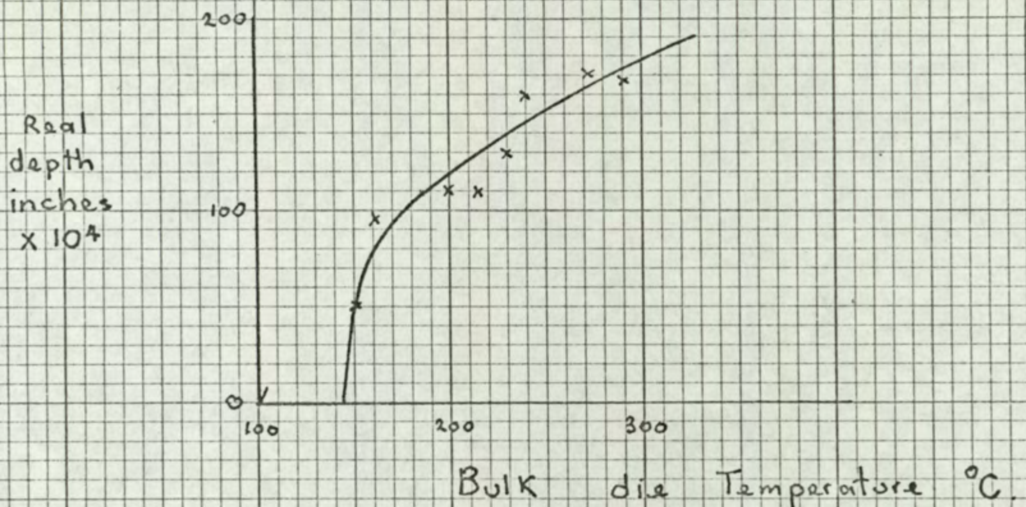
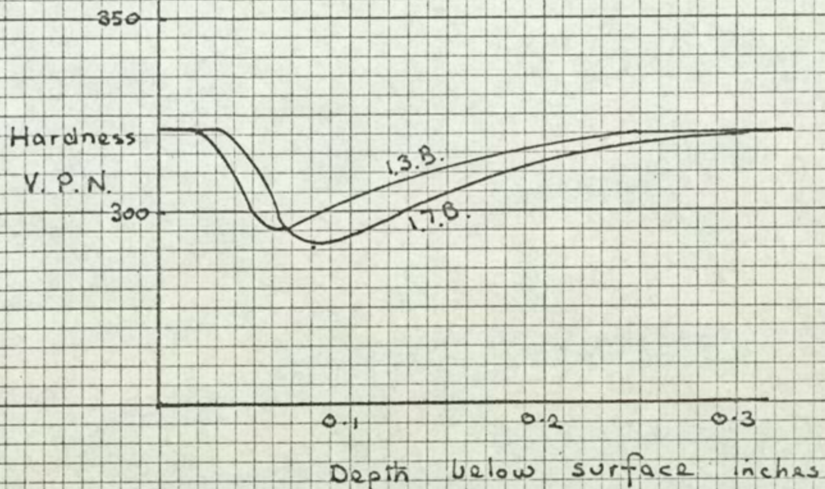


FIG 26

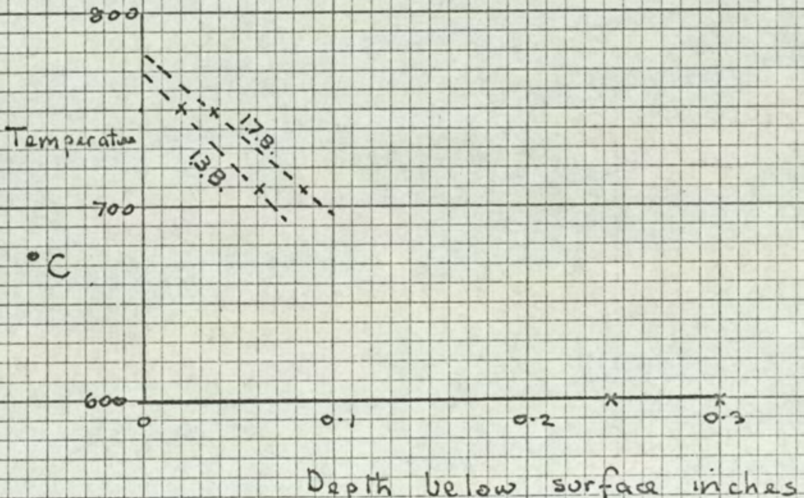
A) Hardness at depth below the surface.

a)



B) Corresponding temperature at depth below the surface.

b)



C is the limit of the softened material; the maximum temperature attained at C is insufficiently high to cause appreciable tempering and is designated the original tempering temperature.

B is the point of minimum hardness. The hardness drops between C and B because of the increasing degree of tempering and rises between B and A because of the contribution to hardness of an increasing percentage of transformation product. The maximum temperature attained at B, therefore, corresponds to the Ac_1 of the steel.

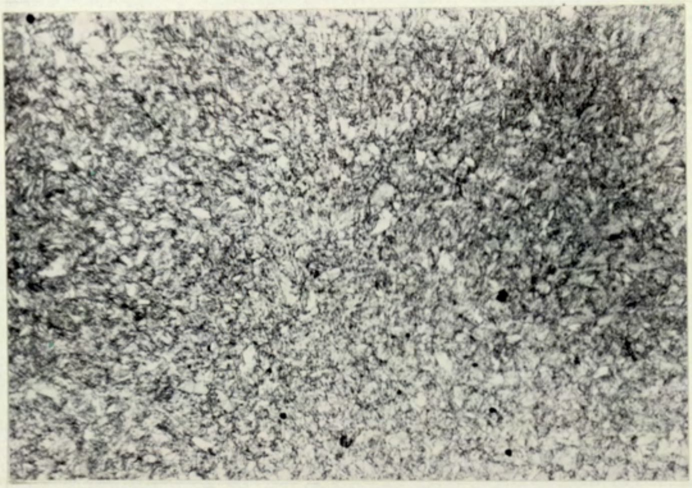
A is the limit of complete transformation; material closer to the surface than point A has completely transformed. The maximum temperature attained at A, therefore, corresponds to the Ac_3 of the steel.

0.15 inches from the surface. When the points A, B, C on the hardness plots were related to the temperatures normally assigned them[33], viz. Ac_3, Ac_1, and the original tempering temperature, it was found that for neither insert did they lie on a straight line, (Fig. 26b)). It would seem that the value assigned to point C is the questionable one, since the material would be expected to continue to soften if subjected to further time at or near its original tempering temperature of 600°C, though to what extent would depend on the heat flow and temperature cycle. Nevertheless it is very simple, if of unsound validity, to draw straight lines through the two points remaining for each insert, to indicate a surface temperature in the region of 765°C for the insert of bulk temperature 150°C, and in the region of 778°C for the insert whose bulk temperature was 240°C. This is a disproportionately small change in surface temperature for a bulk temperature change of 90°C, but the validity of the extrapolation is questionable.

The nature of the transformation product was somewhat unexpected. Its appearance was generally acicular, but the structure contained a large amount of carbide, (Fig. 27), and was fairly soft.

The uniformity of depth of the product implies that the heat transfer coefficient between stock and insert was

Fig. 27.



The Transformation Product.

Etched 2% Nital. x400.

consistent over the area of contact, so it would be thought that at each blow the surface would be austenitic irrespective of whether transformations occurred between blows,

The structure observed was therefore that produced by the final cooling of the die, which was cooling from maximum surface temperature to bulk die temperature in 10 seconds, and then fairly slowly down to a temperature where the inserts were cool enough to be handled and removed from the die blocks. The continuous cooling curve for No. 5 Die Steel indicates that martensite should have formed on cooling in 10 seconds to the bulk die temperature, and even an hour or two at temperatures below this level could not have tempered such martensite to the degree observed. Further, one insert was quenched out with water ten seconds after the final blow, and no change in structure was observed. It must be assumed, therefore, that the structure was bainitic, which implies the austenitising times at the surface were insufficient to completely dissolve the original carbides, and hence the transformation was that of a steel depleted in carbide forming elements and carbon. However, no evidence of the original carbide network was apparent in the transformed structure. It is a little uncertain, therefore, exactly what was occurring during the thermal cycle, although it would seem, from the evidence of the insert quenched out, that transformations were taking place between blows.

Cracking

A net of thermal fatigue cracks, Fig. 28a), was observed under the plateau region of all inserts run above 145°C. The bottom inserts cracked more severely than the top, and the extent of the cracks increased as the temperature increased, (Fig. 28b)), but in all cases cracks were confined within the layer of transformation product.

C.4. Series 2 Tests

C.4.1.

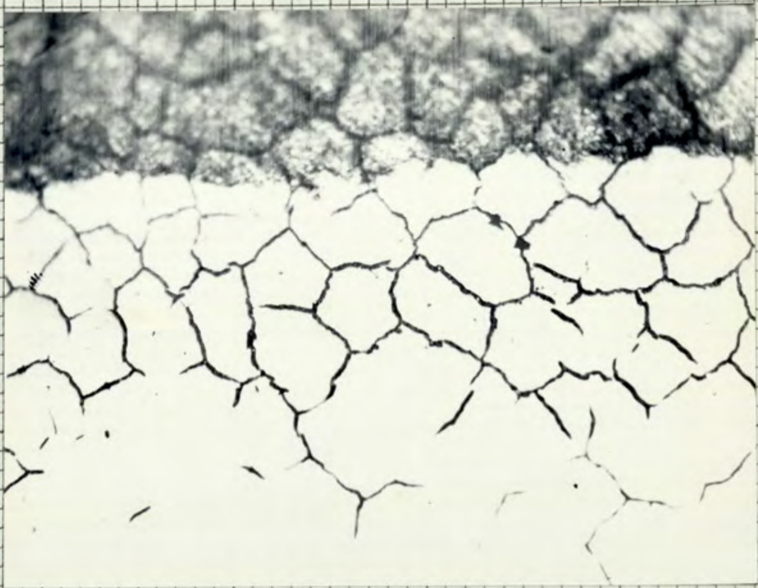
The control of heat treatment for Series 2 tests was improved over that for Series 1 but it still proved impossible to remove all segregation and slack quench effects. The times and temperatures of tempering were chosen to bring inserts to range of hardness levels in steps of about 60 V.P.N. between fully annealed and as quenched. Inconsistencies in heat treatment rendered these values inexact but, since heat treatments were carried out consecutively, the specified levels of hardness could be altered in the light of the hardnesses already obtained and the increments thus approximately maintained,

From the results for the first series of tests the bulk

Fig 28.

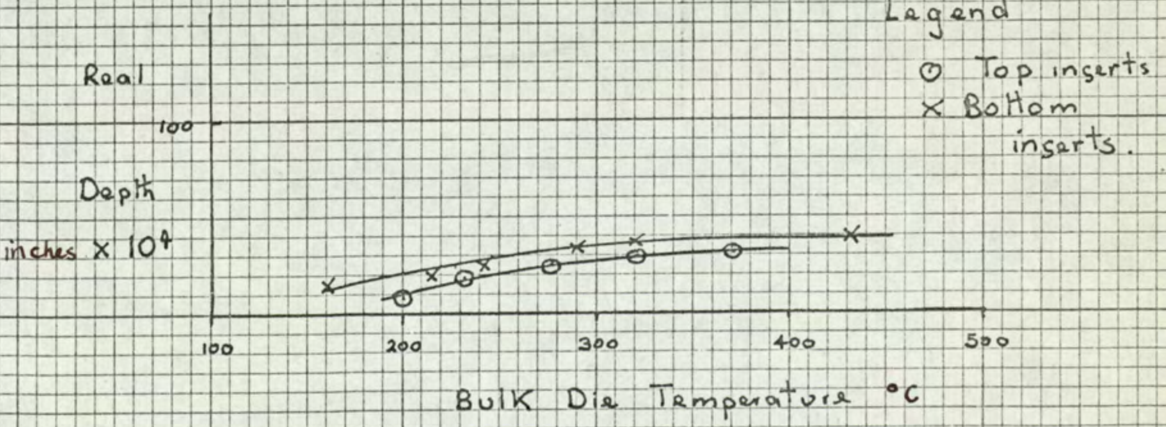
A) Thermal fatigue cracks at the surface of the insert.

a)



B) Extent of cracking at different bulkt temperatures.

b)



die temperature at which to run the second was to be chosen. The original intention was to run the second series at a temperature where transformations were occurring in the surface layers, but since the wear rate was little affected by temperature in the surface transformed inserts of Series 1, it was felt possible that the wear rate might be similarly unaffected by the hardness of the underlying material, and, if so, tests at a series of hardness levels would be relatively meaningless. Further, it was known that the stock temperature would be lower, and consequently, to obtain transformations at the surface, the bulk die temperature would have to be raised in compensation, probably to the region of 200°C to ensure surface transformation occurred. At these levels of bulk temperature the reliability of the feeding mechanism of the press tended to decrease because of expansion effects and the drying up of lubricant on the moving parts. Also, because the resistance to deformation of the slugs and hence the load was going to be greater than in Series 1 tests, it was feared that the inserts of lower hardness levels might have insufficient strength, at a high bulk temperature, to withstand gross deformation which would render them inevaluable. It was decided therefore to run tests at 130-140°C, in the upper regions of the range where softening was known to occur.

An attempt was made to bring top and bottom die temperatures to the same level, by using differential settings on the controllers. In the first test (insert 2.1) the general levels chosen were too high and the differential made too pronounced, such that the sets actually ran at 150-155°C, and the top insert ran hotter than the bottom. More success was obtained with subsequent sets although it was not always possible to control levels of temperature to exactly the desired value. The actual conditions under which sets were tested, together with their original hardness levels and heat treatments, are shown in Fig. 29.

C.4.2.

The results obtained from Series 2 tests are tabulated in Fig. 30.

The Variation of Wear with Hardness

At first sight there seemed little sense to the array of points obtained from a plot of wear against original hardness (Fig. 31), the only consistencies being that, except for the pearlitic insert, the bottom insert wore more than the top, and the amount of wear of the sets tended to decrease as cold hardness increased, but rose for the untempered insert. The points for top and bottom inserts were therefore joined separately, by a series of straight lines.

Fig. 29

Original Insert Number	Revised Insert Number	Tempering Treatment hrs/degrees C	Hardness V.P.N.	Bulk Die Temperature ^{o}C
31.T.	2.1.T.	-	650	155
31.B.	2.1.B.	-	650	150
32.T.	2.2.T.	$\frac{1}{2}$/300	525	130
32.B.	2.2.B.	$\frac{1}{2}$/300	525	140
33.T.	2.3.T.	$^{1}/_{3}$ /400	485	135
33.B.	2.3.B.	$^{1}/_{3}$ /400	485	135
34.T.	2.4.T.	$3\frac{1}{2}$/400	429	130
34.B.	2.4.B.	$3\frac{1}{2}$/400	429	135
35.T.	2.5.T.	1/500	381	130
35.B.	2.5.B.	1/500	381	135
36.T.	2.6.T.	1/600	321	130
36.B.	2.6.B.	1/600	321	130
38.T.	2.7.T.	4/650	285	130
38.B.	2.7.B.	4/650	285	140
40.T.	2.8.T.	Annealed	220	130
40.B.	2.8.B.	Annealed	220	140

For All Sets:- Slug Temperature - 1040 - 1060^{o}C

Slug Reduction to 0.26 $\pm$ 0.01 inches

Number of Slugs Forged - 1000

The Conditions of Test for Series 2 Inserts

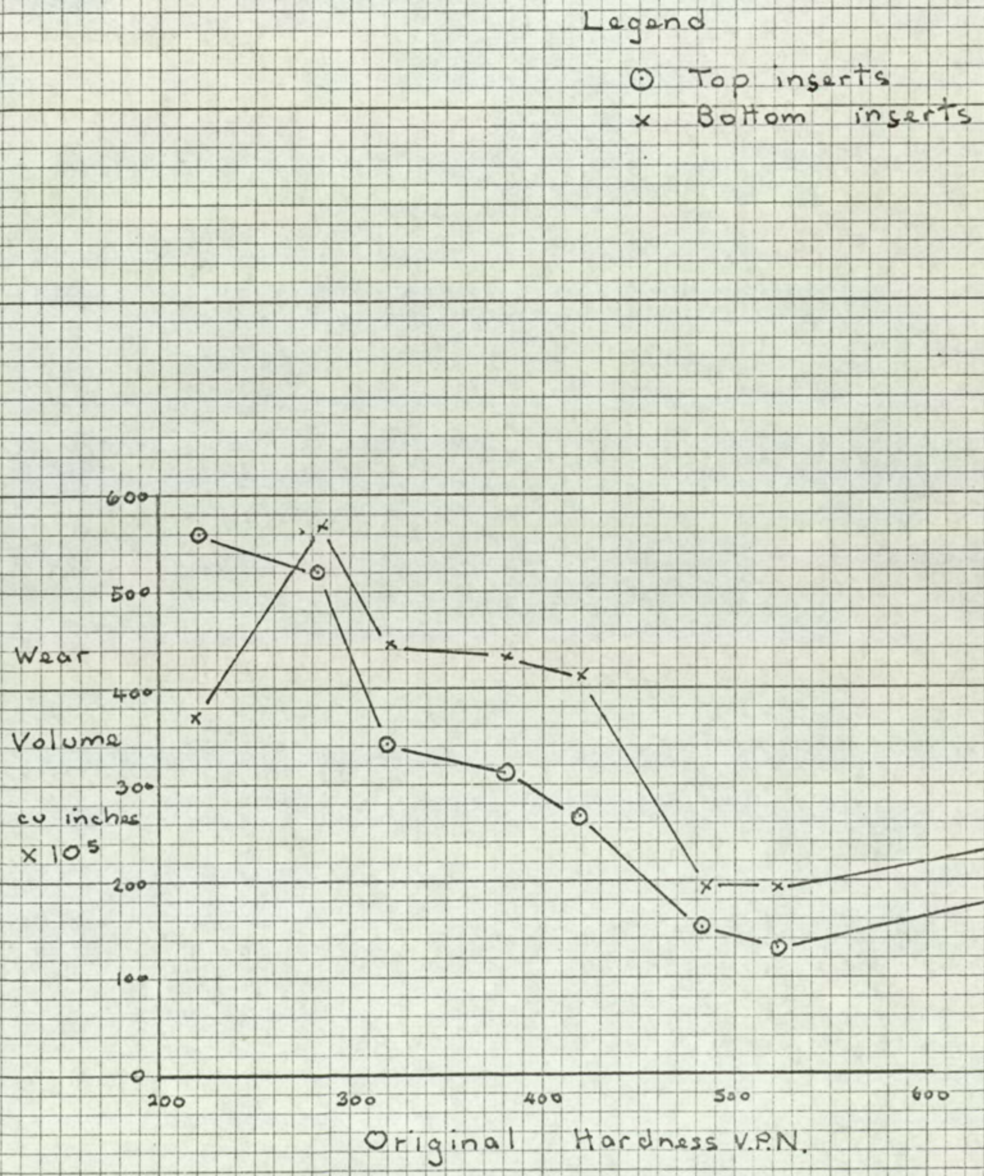
Fig. 30

Insert Number	Wear Vol. cu. ins. x 10^5	Wear Area sq. ins. x 10^5	Wear Depth ins. x 10^5	Surface Hardness V.P.N.	Real Depth of Checking ins. x 10^4
2.1.T.	183	120	109	335	30
2.1.B.	240	162	117	335	60
2.2.T.	113	82	83	355	50
2.2.B.	194	116	105	347	80
2.3.T.	155	104	100	345	80
2.3.B.	194	114	108	345	200
2.4.T.	268	177	222	320	200
2.4.B.	416	218	173	310	270
2.5.T.	315	211	180	297	100
2.5.B.	437	235	172	295	250
2.6.T.	342	206	189	287	80
2.6.B.	446	254	203	285	250
2.7.T.	520	320	318	260	50
2.7.B.	567	323	275	250	150
2.8.T.	560	370	325	220	25
2.8.B.	369	242	230	220	30

Results for Series 2 Inserts

FIG 31

The relation of wear and hardness.



Surface Structure

All the inserts had softened at the surface, the fall off in hardness being more pronounced in the originally harder inserts, although in general, their level of surface hardness at the end of a run still remained superior to that of an originally softer material. The structures observed beneath the inner region of the wear annulus for the top insert of each set are shown in Fig. 32. The structures were similar to the original quenched and tempered structures; as the hardness decreased, so the acicularity of the structure was lost, and greater carbide coalescence was observed.

Cracking

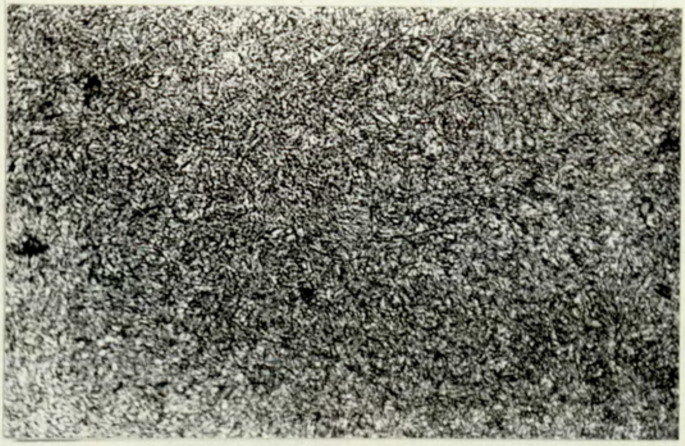
Sets 2.1, 2.2, and 2.3 were unable to withstand the impact conditions of the forging process and after the test were found to contain large cracks, often originating within the wear annulus, and extending both around the annulus and out to the edges of the insert, and to a depth of up to 0.3 inches below the surface. Set 2.1 was the most severely affected, and the crack distribution at the surface can be seen in Fig. 33, as shown up by dye penetrant techniques.

Thermal fatigue cracks were also observed in the plateau region of inserts, to varying degrees, the bottom

Fig. 32.

The Structure of the Near Surface Regions of the Series 2 Inserts.

All Specimens:- Etched 2% Nital. x500.

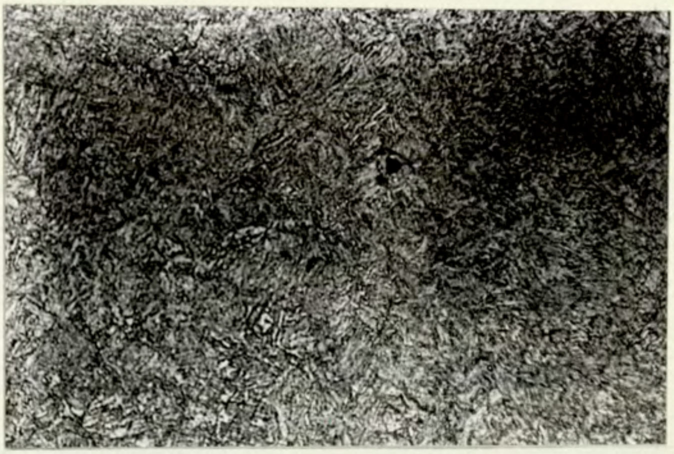


2.1.

2.2.

contd.

Fig. 32. Contd.



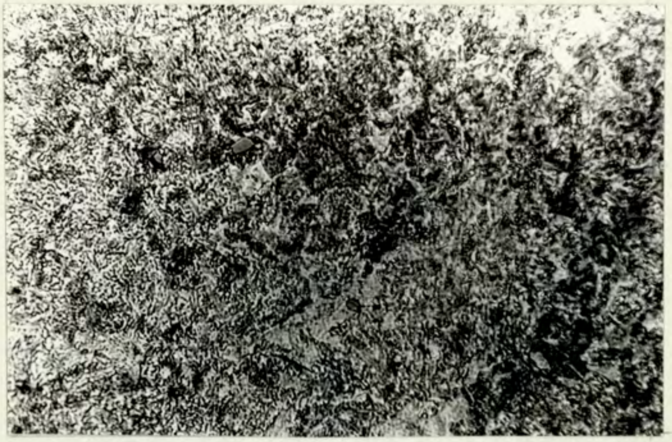
2.3.

2.4.

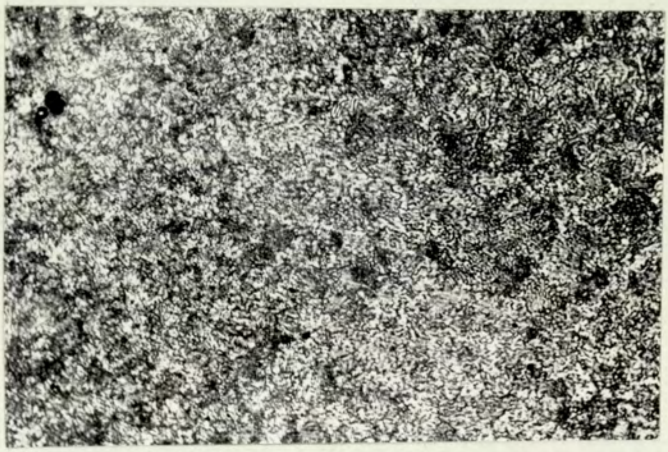
2.5.

contd.

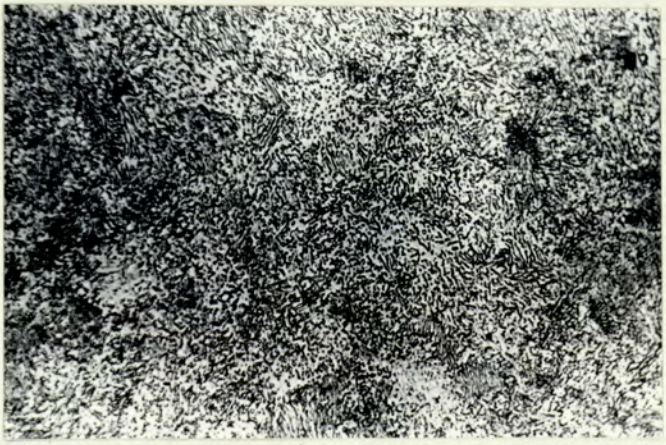
Fig.32. Concld.



2.6.

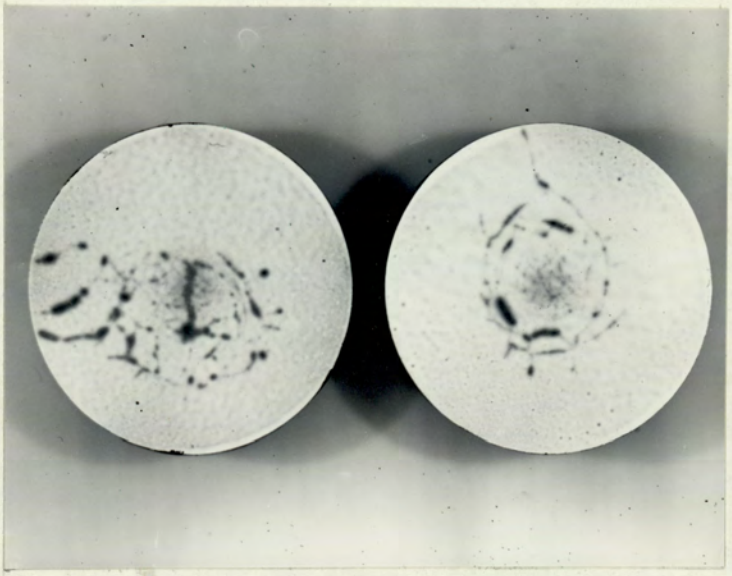


2.7.



2.8.

Fig 33.



Top. Bottom.

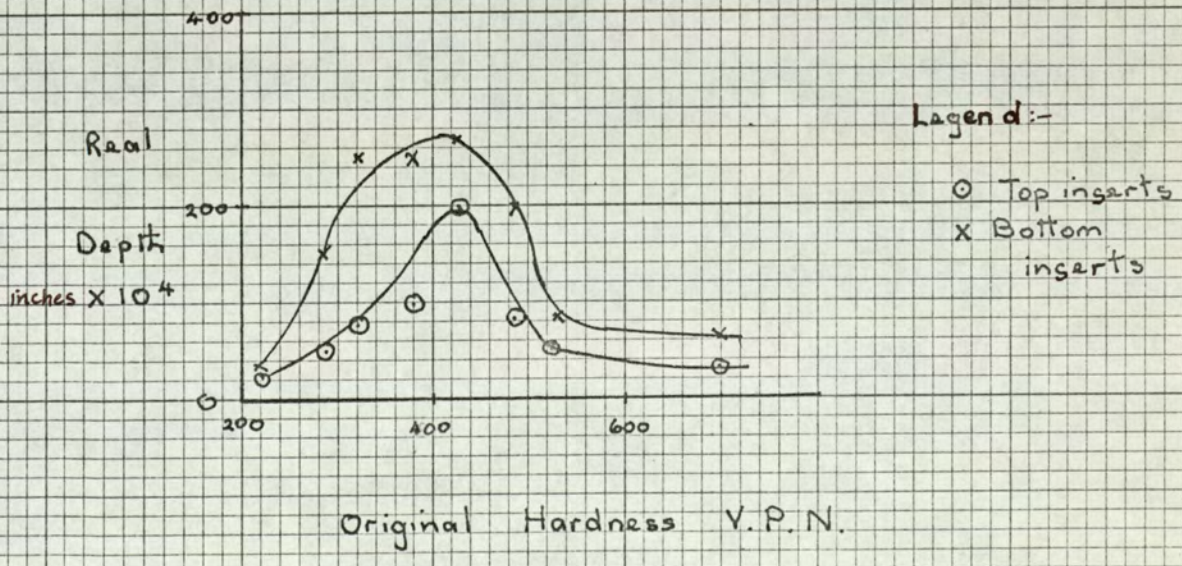
Gross Cracking in Insert 2.1.

Approx. $\frac{3}{4}$ size.

inserts always cracking more than the top (Fig. 34).

Some of the results do not appear to be particularly consistent with expected behaviour. It was thought that the inhomogeneity of the structure might be affecting results, but no evidence was found of wear or cracking being associated with any specific region of the material, although cracks were occasionally associated with inclusions. In the next section therefore an attempt has been made to look at the results in a different way and relate effects to structure in order to obtain more positive correlations. Certain side issues will first be discussed however, in that they have a bearing on the evaluation of results in general.

FIG 34.



The depth of thermal fatigue cracks at different hardnesses.

SECTION D. Analysis of Result and Discussion

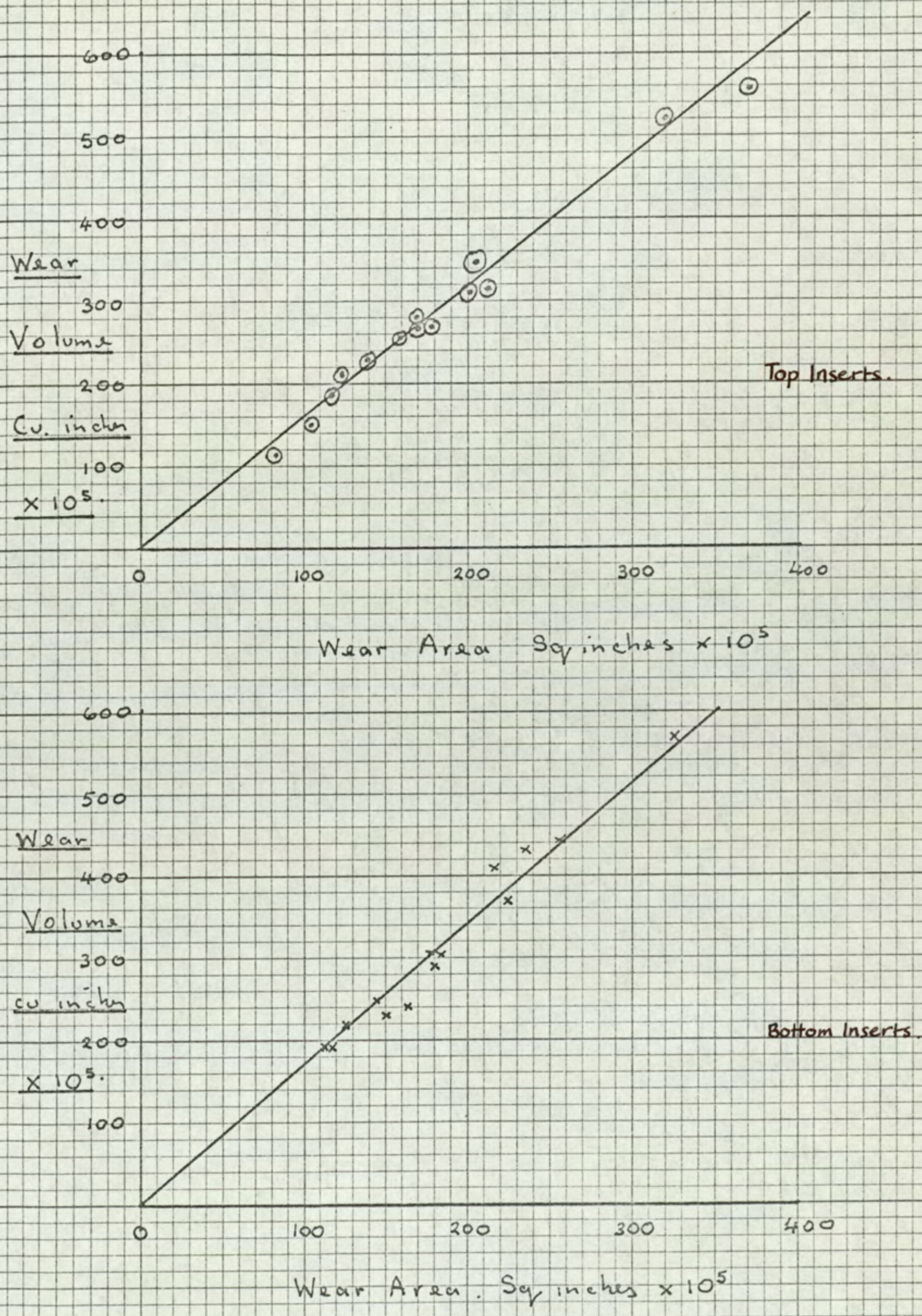
D.1. A Comparison of Measurements of Amounts of Wear

Although volume was considered the only physically significant measure of wear which could be used in absolute theoretical considerations of wear rate, for the representation of relative effects the more simply measured areas or depths may well be useful. To assess their validity as comparative measures of wear, therefore, a study of the relationships between the three values was made.

D.1.1. Wear Measured as Volume and Area of the Profile Trace

To evaluate the proportionality of the two measurements their corresponding values for all inserts were plotted against each other and it was observed that separate plots for top and bottom inserts permitted a more ready appreciation of the proportionality variance, (Fig. 35). As would be expected, the two measurements tended to vary in the same direction, although there was a certain amount of scatter of results, particularly for the bottom inserts. When the best straight lines were drawn through the points from the origin it was found that the volume to area ratio for the top inserts was less than that for bottom inserts. This effect arose because of the differences in shape of the profiles for top and bottom inserts. Typical top and

FIG 35



The relation of wear area and wear volume.

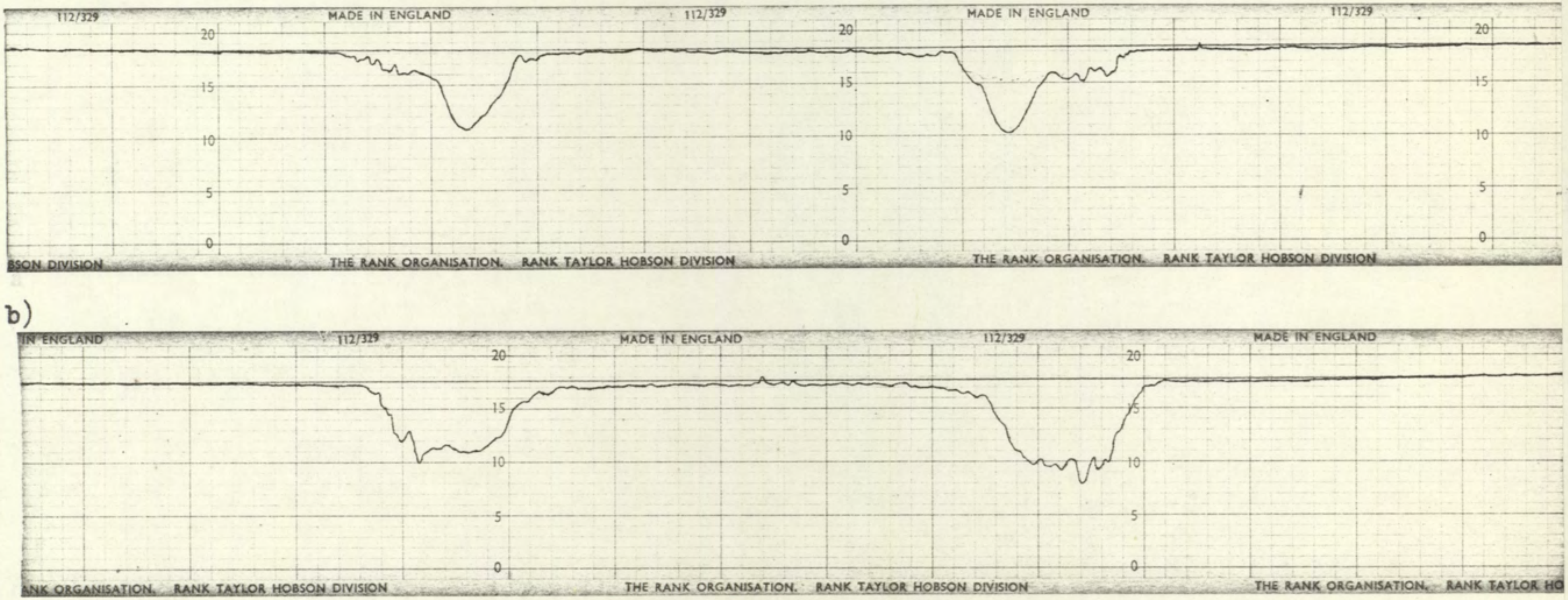
bottom insert traces are shown in Fig. 36, and it will be observed that a top insert trace showed two distinct regions, the outer being more shallowly worn than the inner, whilst a bottom insert trace had a much more uniform appearance, where the outer region was worn to a similar degree to the inner one.

The two regions observed on the profile correspond with regions of different appearance of the insert surface, where the inner ring was scored radially and the outer showed irregular circumferential markings (Fig. 37). The reasons for the change in appearance of the wear pattern, or the differences in profile are not known, and observations made during the course of this investigation have not contributed to the understanding. However, they appear to be fundamental to this kind of upsetting operation, since they were reported on similar tests performed at the laboratories of the Drop Forging Research Association[34] and have been observed during the course of this investigation on all inserts, whatever their test conditions.

Nevertheless, irrespective of the origin of the differences in profile shape, the fact remains that they existed, and since an increment in area near the outside of a trace represents a greater volume than one near the inside, the volume recorded for a bottom insert was greater

Fig. 36.

a)

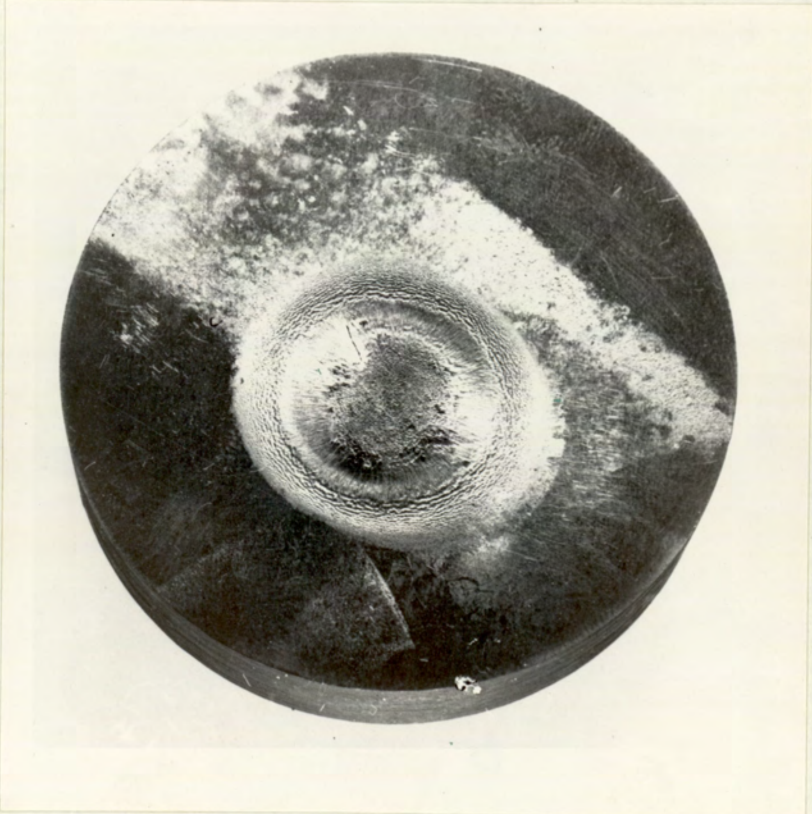


b)

Typical Profile Traces of a) Top and b) Bottom Inserts.

Approx. $\frac{1}{2}$ size.

Fig. 37.



The Worn Insert.

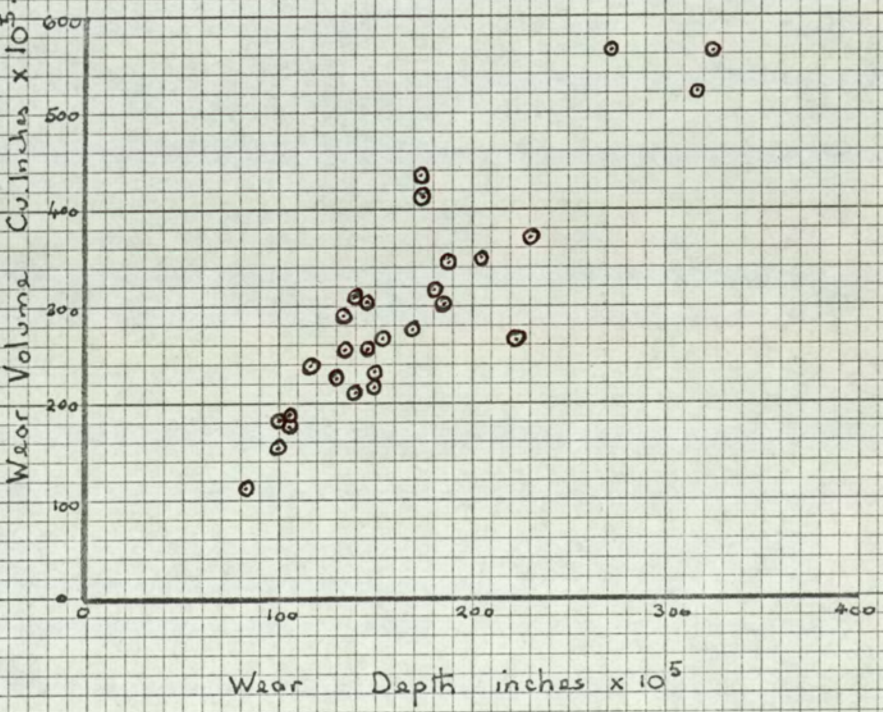
than that for a top insert which had the same wear area. It must therefore be concluded that the use of area as a camparative measure of wear is only strictly valid when the shapes of the profiles of the worn regions are comparable.

An interesting point, should it be desirable to convert measures of area to approximate values of volume, is that the slope of the top insert line indicates wear volume = 1.6 x wear area. If the area is rotated about the average radius of the annulus, then the value calculated for volume would be 1.9 x area, but if the area is rotated about the radius from the centre of the plateau to the point of deepest wear, the value becomes very near the true one of 1.6 x area.

D.1.2. Wear Measured as Volume and Maximum Depth of the Profile Trace

The corresponding measurements of these values were plotted against each other in a similar manner to that adopted above, and the result is shown in Fig. 38. It will be appreciated that the degree of scatter is much more severe and really precludes any meaningful assessment of proportionality because individual values could vary so far from the average. The scatter arises because a measure of depth is unidimensional. It is therefore far less representative of profile changes than a measure of area

FIG 38



The relation of Wear depth and Wear Volume.

and also more sensitive to errors in its assessment. In particular with the highly contoured wear patterns produced in this investigation, it was possible that profile recorded did not actually pass through the point of maximum depth, and even if it did, if the sides of the profile were very steep the stylus might have been physically prevented from recording the maximum depth level. If this occurred the small amount of metal loss unrecorded would be but a tiny proportion of a measurement of area or volume, but could cause significant error in the measurement of maximum depth.

In this sort of investigation, therefore, a measure of depth had little meaning, either as a comparative measure, or as a real physical value. In other situations, for example where a test insert is part of a large die and is worn more or less uniformly over its whole surface, a measure of depth would be more acceptable.

D.1.3. Wear Measured as Area and Maximum Depth of the Profile Trace

Since the measurements of depth made were considered unreliable in this investigation, a comparison of area and depth would be pointless, and so this relationship was not investigated.

In general, therefore, area, being a two dimensional measurement, is superior to depth as a comparative measure of wear.

D.2. Thermal Fatigue

A net of thermal fatigue cracks was observed in the plateau region of all the insert sets except those of Series 1 run below 140°C. In all cases the extent of cracks in the bottom insert was greater than that in the top, and increased as the bulk temperature increased, but the relationship of crack depth with strength was not very conclusive. The cracks were never very deep, the maximum depth recorded being 0.005 inches, real depth.

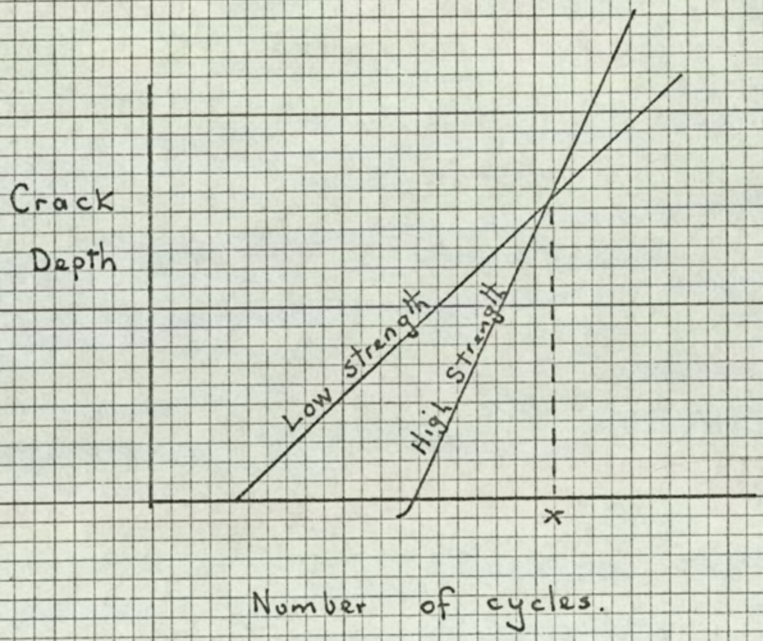
In that thermal fatigue cracks were never observed below the worn annulus, it must be assumed that the surface was being worn away before a crack could form, exposing a fresh surface on which the fatigue cycles had, up to that point, had no effect. The situation is analogous to that of successive grinding of the surface of a conventional fatigue specimen increasing its life almost indefinitely. In these inserts, therefore, the extent of heat checking could have no effect on the amount of wear.

Nevertheless, dies can be removed from service because of severe thermal fatigue cracking and its incidence is always undesirable, so a discussion of the observations of heat checking made during the course of this investigation was felt relevant. The situation is by no means simple, however, and the actual levels of fatigue cracking observed represent some balance between the strength and ductility of the material and the number of cycles of the test, in that crack depth after a period of time depends both on the rate of initiation and on the rate of propagation of the cracks. The lower the yield stress of a material the greater is the plastic strain per cycle, so the fewer are the cycles necessary before cracks initiate. However, a low strength material is often more ductile than a high strength one, so the rate of propagation of a crack is lower. The situation for materials of two different strengths is shown diagrammatically in Fig. 39, and it will be appreciated that if the number of cycles of a test were less than X, then the higher strength material would appear superior to the lower strength one, whereas the opposite conclusion would be reached if the number of cycles of the test were greater than X.

This sort of consideration supplies a possible explanation for the changes in crack depth observed in Series 2 tests since the toughness of acicular structures of the type observed in the hardest inserts in considerably less than that of the lower hardness inserts where the acicularity had been broken down and the carbides were more coalesced. The rate of crack propogation in the harder materials would then be disproportionately greater, with

FIG 39

Thermal fatigue cracking in different strength materials.

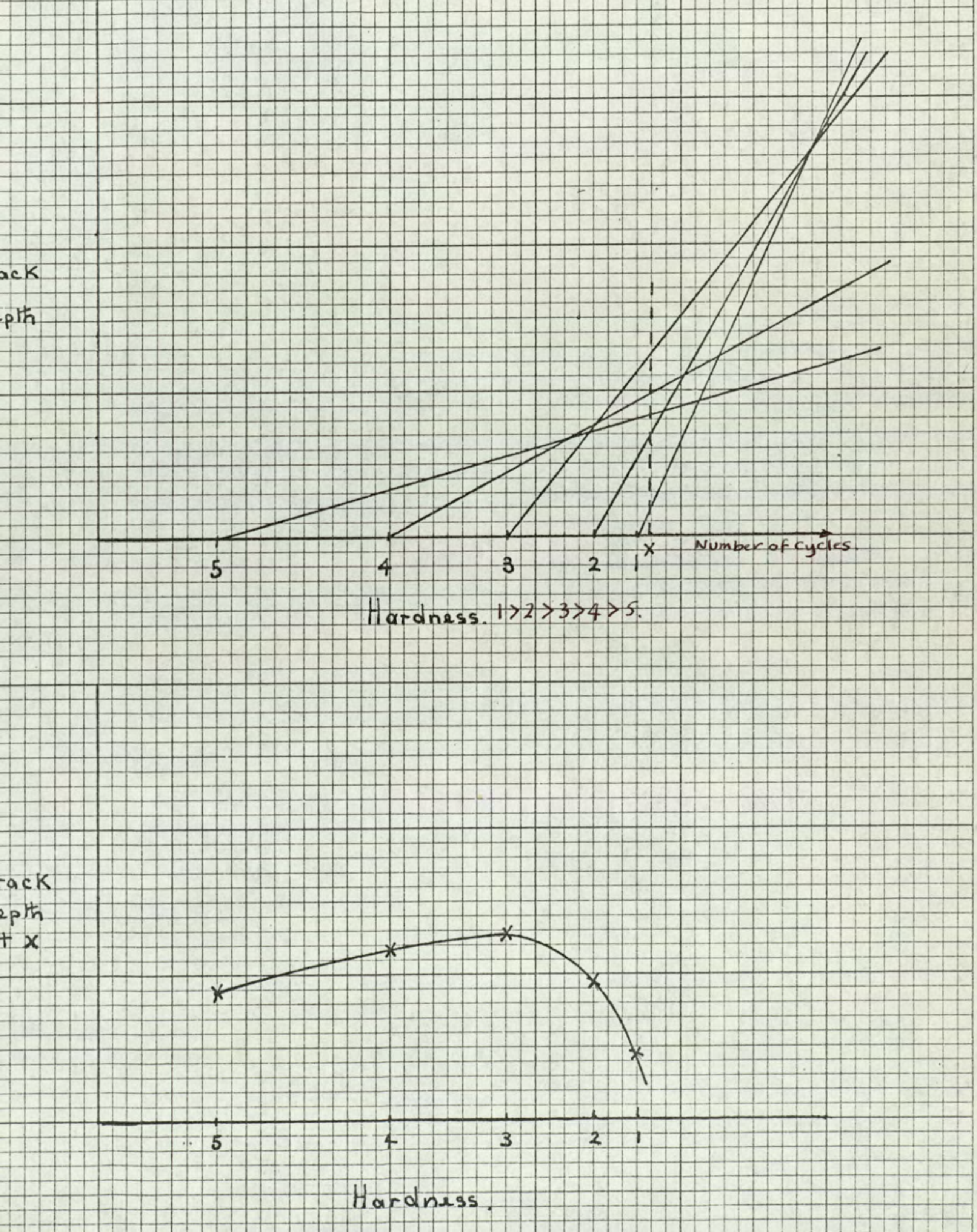


respect to strength, than in the softer materials. A possible representation of the situation is shown diagrammatically in Fig. 40 for five hardness levels numbered 1 to 5 in order of decreasing hardness. The propogation rates have been taken as low for numbers 4 and 5, and high for numbers 1, 2 and 3. It will be seen that if the number of cycles of a test were X, then the extent of the fatigue cracking would appear to increase as hardness was raised, and then to decrease again. It is obvious that by altering the slopes of the lines or the value of X, virtually any combination of final results can be obtained, and the permutations and combinations will be increased by the fact that the relation of crack depth to number of cycles is most probably not linear because of the property changes occurring during the course of a test. Observations of the extent of thermal fatigue in materials of different hardnesses in this sort of test do not therefore really supply any positive information.

However, irrespective of initial conditions, bottom inserts always cracked more severely than top ones, and this would seem to be an effect of the improved heat transfer between the stock and the bottom insert, relative to that to the top insert, and the higher bulk temperature at which the bottom inserts ran. This indicates that an increase

FIG 40

The variation of crack depth with hardness when propogation rates are dissimilar.



in bulk die temperature to decrease thermal gradients is undesirable, for No. 5 Die Steel, at least, if the surface temperature is also permitted to rise. If ideal conditions of heat flow are assumed, the surface temperature will rise by about half the amount by which the bulk temperature is increased, and the criterion on which the desirability or otherwise of reducing temperature gradients in this way should be based will be whether the difference between the stress level induced as a result of the thermal gradient and the yield stress of the material at the surface temperature decreases or increases as bulk temperature is raised. Unfortunately this criterion is not a simple one to evaluate, and temperature gradients will also be affected by the thermal properties of the material at elevated temperatures. For example, the thermal diffusivity of No. 5 Die Steel decreases with increased temperature, and so, on increasing bulk temperature, the gradients will not be reduced to the extent anticipated from the decrease of temperature differential.

In Series 1 tests the severity of the cracking increased with increased bulk temperature, but cracks were always confined within the layer of transformation product. The effect may be attributed to an increased rate of initiation of cracks consequent on the decreasing strength of the austenite, although confirmatory figures are not available. The stress levels may also have been increased as a result of the greater volume of material being transformed to austenite at each blow. The hardness level of the transformation product precludes the concept of easy crack propogation through the brittle product, and it was not possible to raise bulk die temperatures sufficiently to prevent transformations occurring between blows, so the reputed beneficial effects of this measure could not be assessed. However, since no cracks were observed in inserts that did not transform at the surface, in this series of tests, the results would seem to confirm the undesirability of surface temperature being allowed to rise above Ac_1.

In these tests evidence was found that scaling of the inserts was contributing to the initiation of thermal fatigue cracks, although, since the inserts were descaled before being traced and sectioned confirmatory results were not available from the series inserts. Inserts that failed to complete a test run were sectioned without being descaled and a net of cracks was found in the scale layer, which had propogated as scale into the die material itself. In some of these scale crack traces, true cracks were seen to have been formed (Fig. 41). The sequence of events that is envisaged is that the scale cracked because it was brittle

Fig. 41.

Thermal Fatigue Cracks in Scale Penetrations.

Unetched. x400.

and subjected to the most severe temperature gradients, and in so doing exposed fresh metal at the base of the cracks to air, so producing increased oxidation into the metal surface along the lines of the crack paths. On the heating cycle when compressive stresses were highest in the surface layers the presence of wedges of relatively non-ductile scale prevented a uniform stress distribution, and stress concentrations built up around the points of the wedges, sufficient to initiate cracks into the material itself. It is also possible that further oxidation along the true cracks increased their rate of propogation by tending to force the crack faces apart.

A final point is that insert 2.6.T, of original hardness comparable to the Series 1 sets, ran at a bulk temperature of 130^{o}C. Because of the lower slug temperatures in the Series 2 tests, this insert is comparable in terms of surface temperature and hence high temperature strength, to a Series 1 insert run at about 110^{o}C, although the temperature gradients in this hypothetical Series 1 insert would be greater. The Series 1 insert would therefore be expected to crack more severely than the Series 2, whereas in fact the Series 2 insert did crack and it is reasonable to assume the Series 1 insert would not have cracked, since checking was not observed in any insert in the Series 1 tests run at surface temperatures below the transformation point. The only explanation for this phenomenon would seem to be that the levels of imposed external stress were augmenting the stress induced by thermal effects and it was the combined level of stress that determined whether the material cracked or not.

It is appreciated that these observations are rather qualitative in nature, and that, because this was an investigation primarily into wear, the approach has been insufficiently rigorous to ensure their applicability to conditions outside those experienced in the tests. Nevertheless, they do confirm the undesirability of high surface temperatures, and particularly of temperatures in excess of Ac_1, and would seem to indicate that the scaling of dies and the levels of external stress could be contributory factors in the failure of a die as a result of thermal fatigue.

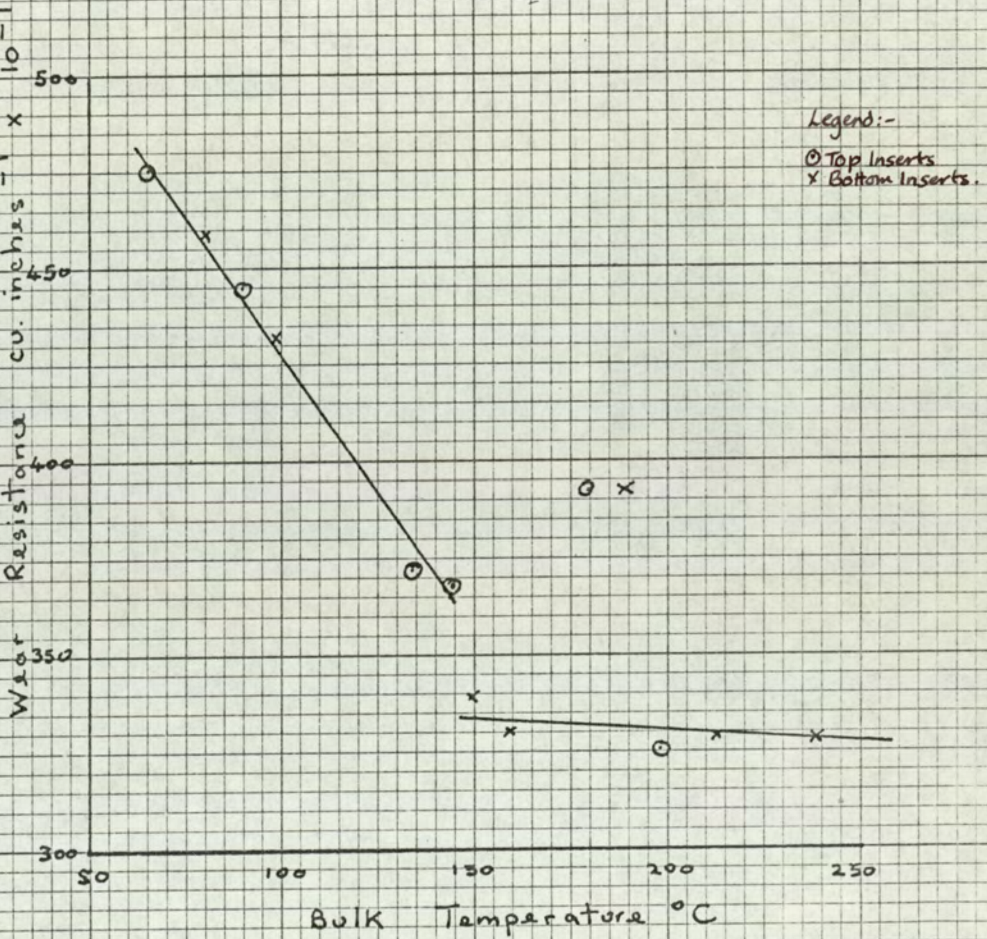
D.3. The Relationship of Wear and Bulk Die Temperature

The wear/bulk die temperature curve shown in Section C.3. indicated an increase in wear as temperature rose, when the inserts were not transforming at the surface, and a levelling off of the rate of increase once the surface had become fully austenitic. Although this curve provides the information that is of practical use, it is not easily analysed and some more critical way of presenting the result was therefore sought.

Since the temperature level would be indicative of the severity of the decrease in strength and hardness of the material being worn and the literature indicates an inverse relationship between wear and hardness, it was felt a more positive correlation would be obtained if the inverse of wear, that is wear resistance, were plotted against temperature. Such a plot (Fig. 42) does in fact permit a far more ready appreciation of the variance, and, in particular, a point that was not obvious from the original curve becomes apparent, that is that there was a discontinuous increase in wear when the surface layers of the insert first became austenitic during a blow. The wear resistance can be seen to have decreased almost linearly with temperature when surface temperatures were below Ac_1, but to have decreased much less, if at all,

Fig 42

The relation of wear resistance and bulk temperature.



once the surface had become fully austenitic. There are therefore three aspects of the curve that merit discussion.

i) <u>The Fall of Wear Resistance of Inserts Whose Surface Temperature Was Below Ac_1</u>

An increase in temperature will decrease the strength and hardness of the surface of an insert both by virtue of the actual value of surface temperature determining its high temperature strength and hot hardness, and also because of the secondary effects of increased softening, further reducing hot hardness and strength levels during the course of a run. An increase in temperature will also increase the surface energy of the material, its rate of scaling and may affect its structure, all of which could influence wear rate.

However the near surface structure of all these inserts was substantially the same, so much so in fact that the softening indicated by measurements of microhardness could not be detected from observations of the microstructure. This factor could therefore be eliminated for these inserts.

Adhesive wear is directly proportional to surface energy, which itself increases proportionally with temperature. If adhesive wear were the controlling wear mechanism therefore, the wear resistance/temperature curve would not be a straight line, but would drop to lower levels of wear resistance at higher temperatures than the linear relationship would indicate. The degree of accuracy of the measurements of wear however is not sufficiently great to rule out the possibility that such an effect did occur, but if it did it would seem to have been small, and it is most likely that abrasive wear, which is unaffected by surface energy, were playing the more significant part in the wear of these inserts.

A similar downward sloping curve, when wear resistance is plotted against increasing temperature, would be expected if the increased rate of scaling were making significant contribution to the total wear volume at higher temperatures. However, the range of temperature between the coolest and hottest insert in this group was less than 100°C, and the incremental increase in amount of scale would have been small, therefore, although removal of metal as scale may have accounted for an appreciable proportion of the wear, it is unlikely that the effects of a change in wear rate consequent upon a change in scaling rate would have been detected within the limits of accuracy of the results.

The decreased wear resistance at increased levels of temperature, therefore, can be attributed almost solely to the deleterious effects of temperature on the strength and hardness of the material being worn.

ii) <u>The Discontinuous Change in Wear Resistance Upon Surface Temperature Exceeding Ac_1</u>

In that the wear resistance/temperature co-ordinate for insert 1.4.T., which contained discrete areas of transformation product, lay on the curve for inserts which only softened at their surfaces, it seems that a high proportion, or a continuous layer, of transformation product was a necessary criterion before its effects on wear were felt.

As has already been discussed, thermal fatigue cracking was not thought to be contributing to wear, so although the onset of the cracking corresponded to the first observations of transformation product at the surface of the inserts, it could not account for the decrease in wear resistance. Further, although only one hot hardness value is available[27] for No. 5 Die Steel in an austenitic condition, that one does not indicate a disproportionate drop in relation to temperature from the figures quoted for quenched and tempered specimens, so the decreased wear resistance could not be accounted for solely on the grounds of hardness. Similarly the rate of scaling could not account for the drop because it retains the same relationship with temperature irrespective of the structural state of the surface.

The discontinuous drop in wear resistance must therefore arise because of the different structure of the austenite as opposed to that of the tempered martensite at the surface of the softened inserts. It is not known exactly in what state the surface of an insert was during a blow, but from the lack of evidence of the original carbide network in the final transformation product it would seem that appreciable carbide solution was occurring. The austenitic layer would then have been depleted in free carbide, and hence its abrasion resistance lowered. Since the surface energy of austenite is greater than that of a tempered martensite structure[2] there would also have been an increase in adhesive wear, and the combination of these two factors could account for the observed drop in wear resistance.

iii) The Change in Wear Resistance with Temperature of Inserts whose Surface Temperature was Greater than Ac_1

The wear resistance of these inserts dropped only slightly, if at all, with increased temperature over a range of nearly 100°C. It is difficult to imagine why this drop were not much more severe since an increase in temperature would be expected to alter the levels of such factors as hardness, carbide solubility, surface energy, degree of scaling, which could influence the wear rate, towards more deleterious effects on wear resistance. The possibility that the recorded data was spurious is unlikely, because points on this portion of the curve represent results from four different insert sets and the probability of their all being in error is very low.

It is possible that the wear rate is in some way specifically dependent upon the surface being austenitic and that other effects of temperature have relatively little influence, but it is hard to visualise such a situation existing. The other possible explanation is that once the surface had become austenitic the surface temperature did not rise to any great extent as bulk temperature was increased. Such an increase in bulk temperature without a proportional increase in surface temperature would be consistent with all the measured data, in that the thickness of the layer of transformation product would still have increased, as would have the degree of softening beneath the transformed region, and the greater depth of thermal fatigue cracks could be explained in terms of the increased stresses imposed by a greater volume of metal being transformed to austenite. The only objection is that there is no recorded data, either physical or thermal, that indicates surface temperatures should not rise proportionally as bulk temperature increases.

In reality therefore, no satisfactory explanation can be given for the near lack of dependence of wear resistance upon temperature in these inserts.

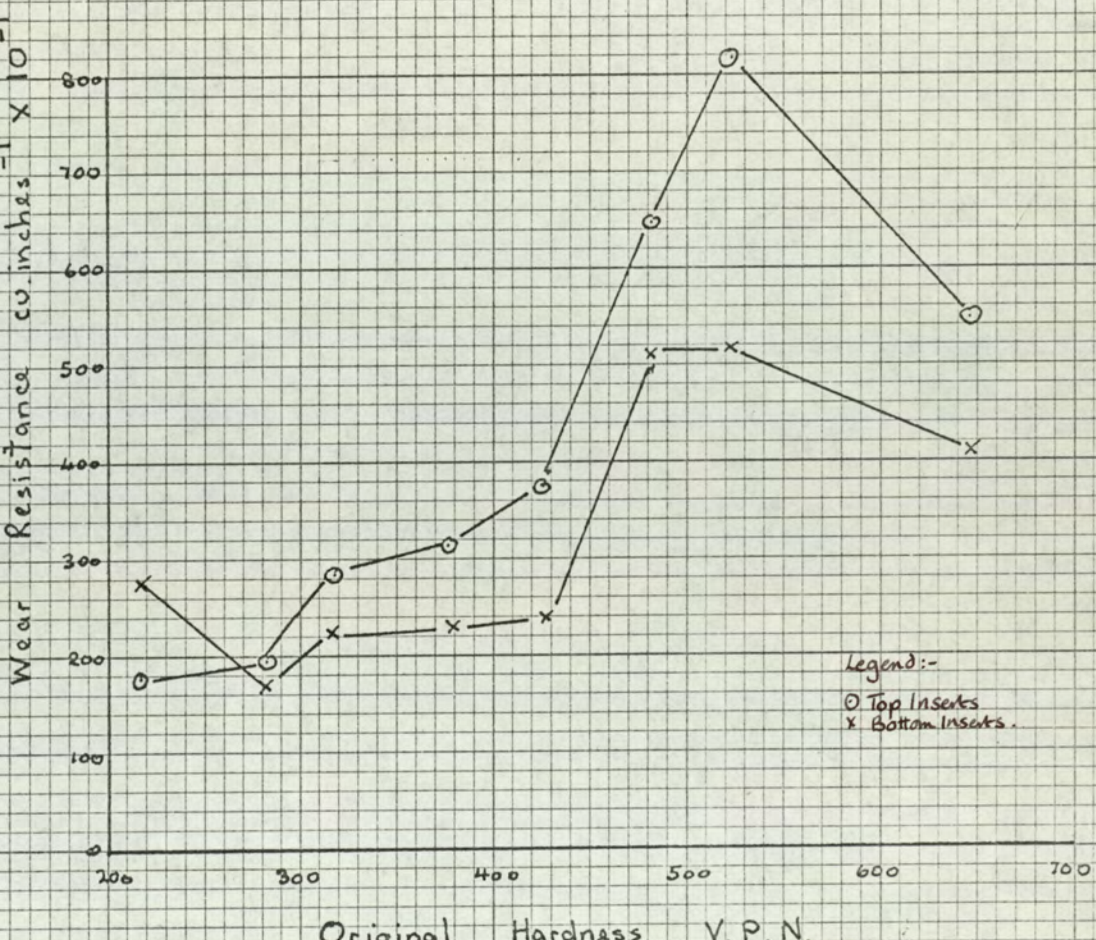
The general conclusions that can be drawn from this series of tests are that it is the level of strength and hardness of the surface layers that largely determines the amount of wear of inserts in which surface transformations do not occur, and that wear of an austenitic structure is more severe than that of a tempered martensite structure. There is also some evidence that abrasive wear is the more important wear mechanism.

D.4. <u>The Relationship of Wear and Hardness</u>

The original wear/hardness curve shown in Section C.4. indicated a general decrease in wear at higher levels of hardness, but either the scatter of result was high or the relationship was not a continuous one. A plot of wear resistance versus hardness, performed for the same reasons as before did not clarify the situation, but merely emphasised the degree of discontinuity (Fig. 43). Since the results of Series 1 tests were fairly self-consistent there seemed no reason why those for Series 2 should show any greater degree of error because, although conditions had altered between the two sets of tests, the control exercised over them had not deteriorated. Again, therefore, although it is the original curve that provides the information that is of practical use, in order to be able to make any more positive statement than that wear tended to decrease as hardness increased it was necessary to find a more critical value of hardness with which to compare wear and wear resistance. The ideal value, that would be expected to bear direct relationship to wear resistance, would be some hardness level that represented an "average" of the range of hardnesses over which the insert was actually worn. Such a value however can obviously not be measured directly and an assessment of it would be

Fig 43.

The relation of wear resistance and original hardness.



of unsound validity. However the surface hardness at the end of a run could be measured directly, and to a fairly high degree of accuracy on the taper sections, and for several reasons it was felt that this value might bear a more direct relationship to the amount of wear occurring than did the original hardness. Firstly the inserts had been tested at slightly different temperatures and a measure of surface hardness would partially compensate for the discrepancies introduced, in that it would reflect the effects of such temperature differences on the structural state of the insert, although not the absolute effects of the temperature level on the hot hardness during the run. Secondly, since the hot hardness at the surface of the inserts bears a relationship to the cold hardness, which is effectively decreasing during the course of a run, and the effects of tempering are such as to cause a higher rate of decrease of hardness during the initial stages of a run than during the final stages, the final hardness more nearly approximates to the state of the insert during the run than does the initial hardness. This is particularly true because wear is greater the lower the hardness, so the proportional contribution to the total amount of wear is also greater at lower levels of hardness. Therefore plots were made of wear and wear resistance against surface hardness (Figs. 44 and 45).

The obviously improved positivity of the relationship indicates that although surface hardness is only a "better approximation" to the hardness at which the insert was being worn, and has no real theoretical significance, in practice it is an acceptable measure in that, although the points on the wear resistance/surface hardness curve almost undoubtedly do not lie on straight lines, they sufficiently closely approximate to this state to permit the drawing of such lines to indicate the general level of the wear resistance, and the trend of its change with hardness.

The wear volume/surface hardness curve bears a resemblance to the original curve as would be expected because at similar levels of temperature the inserts would retain their same order in the scale of ascending hardness. However, one discontinuity, that of the increased wear of the originally as quenched insert, has been removed because the significantly higher temperature at which it ran has made its surface hardness less than that of two originally softer inserts. The wear resistance/surface hardness plot indicates that the remaining discontinuities are in fact genuine effects, that is that the wear resistance of inserts 2.1, 2.2 and 2.3 was considerably

FIG 4 4

The relation of wear and surface hardness.

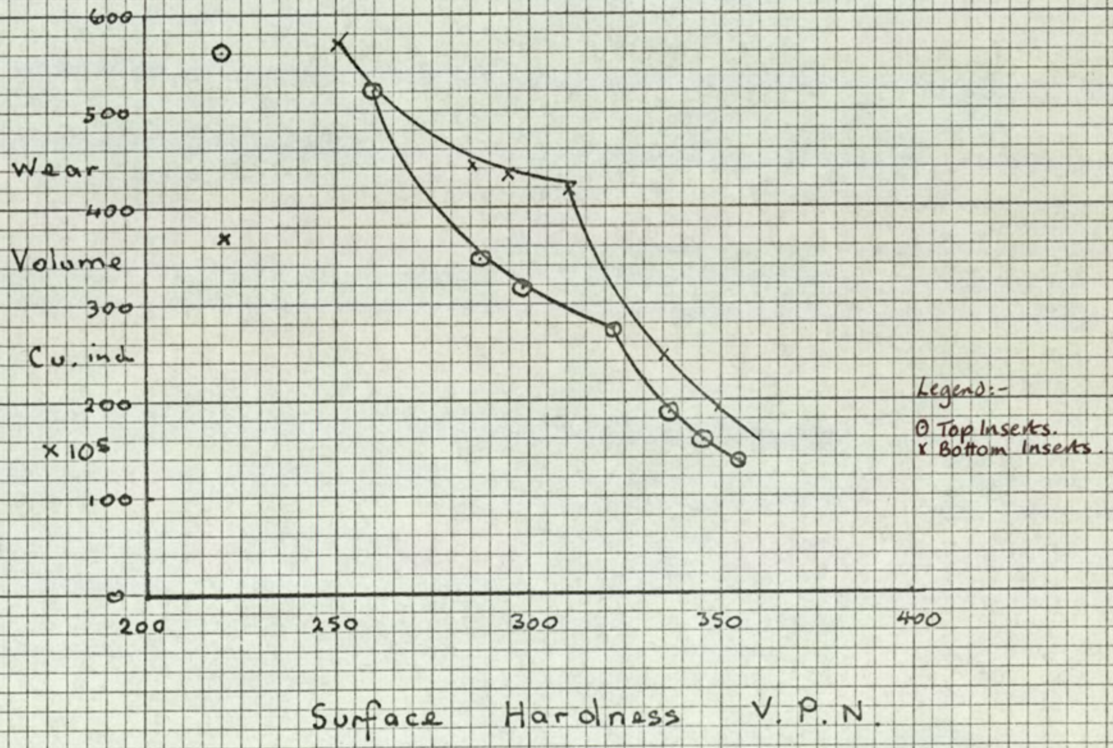
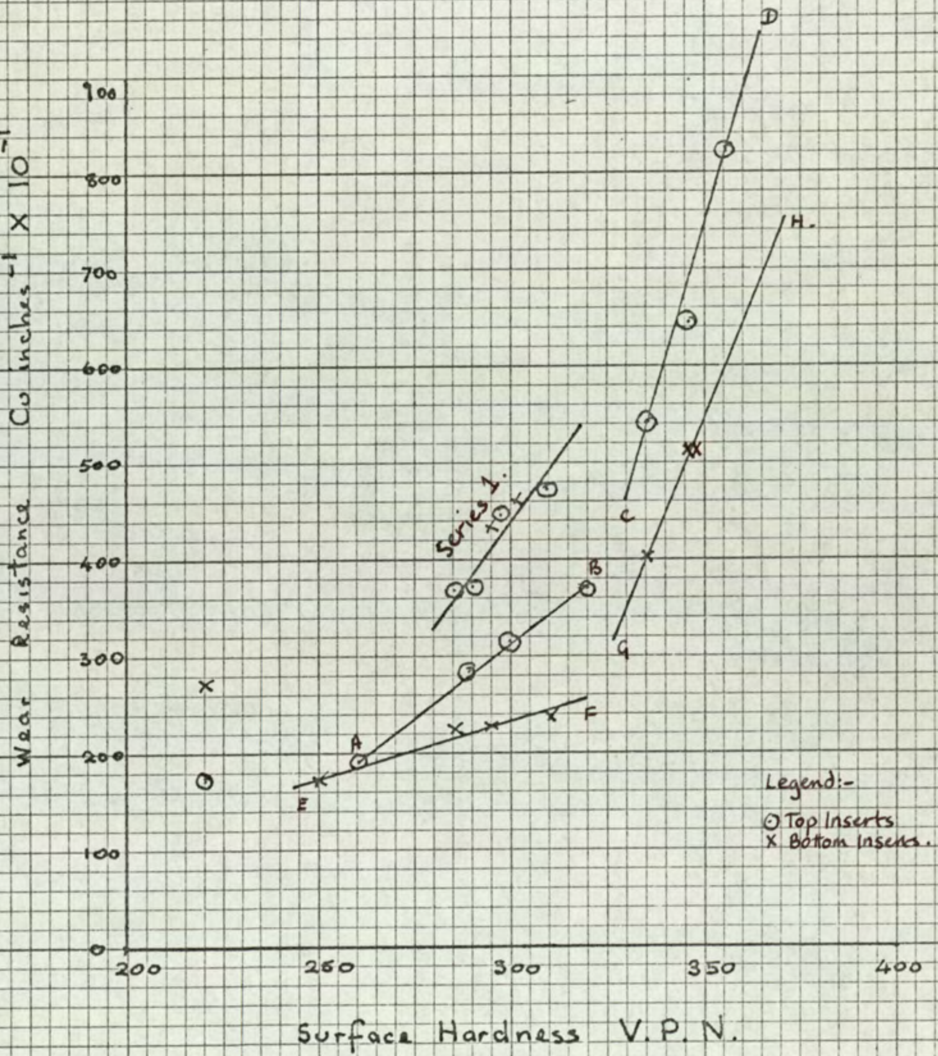


FIG 45.

The relation of wear resistance and surface hardness.



greater than that of the softer inserts, and that the wear resistance of the pearlitic insert 2.8 was relatively greater than would be expected from the trend of the quenched and tempered inserts.

A study of the near surface regions of the microsections of the inserts showed an acicularity of structure in inserts 2.1, 2.2 and 2.3 that was not apparent in inserts of lower hardness (see Fig. 32, Section C.4). An observation of original structures showed the same trend so the effects of temperature had not been so much to alter the type of structure as to cause softening by increasing the level and degree of coalescence of the free carbide, and the observed differences in wear resistance can therefore be correlated to the type of structure. Such a discontinuous change in wear resistance and wear resistance/hardness dependence has been found when a steel, heat treated to different levels of hardness, is abraded[10], and the similarities between the two situations imply the same wear mechanism to be dominant in each case. It would seem therefore that abrasion was an important source of wear in these inserts and that the structure sensitivity of abrasive wear accounts for the greatly improved wear resistance of inserts 2.1, 2.2 and 2.3

The relatively improved wear resistance of the pearlitic insert, however, is at variance with results obtained from conventional adhesive and abrasive wear tests. Adhesive wear has not been reported as being structure sensitive, and the abrasion resistance of a pearlitic structure has been shown to be considerably inferior to that of a tempered martensite structure[10]. The only possibility seems to be that, at high levels of temperature, the ferrite in the structure is so soft as to be worn away preferentially, leaving bands of carbide standing proud of the surface, after which time the wear becomes that of stock on carbide. Such wear would be very low because of the carbide's hardness and low surface energy, and the total amount of metal removed by this two stage process could be less than that which would be removed if the surface were worn as a homogenous structure. However, this is not a very satisfactory explanation in that it cannot be more than a hypothesis, because it cannot be tested.

If this insert is neglected the general conclusions that can be drawn from the remaining inserts are that wear tends to decrease as hardness increases, and that, because abrasive wear is the dominant mechanism, the decrease is more pronounced when the hardness increase is accompanied by a change in structure.

A Cpmparison Between the Results of This Investigation and the Published Wear/Hardness Curve for No. 5. Die Steel

The published wear/original hardness curve for No. 5 Die Steel[33] covered only a range of hardness from 300 V.P.N. to 400 V.P.N. and hence did not show structural discontinuities such as have been observed in Series 2 set. However it did show a maximum of wear at about 330 V.P.N., which is at some variance with the results obtained in this investigation.

Since the amount of wear occurring is so dependent on variables in the forging process it is possible that the discrepancies in the two sets of results is a reflection of the particular conditions of each test. Unfortunately the levels of variables were not recorded in conjunction with the published curve, so only the most tentative explanations can be forwarded to account for the differences in behaviour.

At best it is hard to envisage a situation where wear would decrease with decreasing hardness, unless some effect such as that suggested to account for the increased wear resistance of the pearlitic insert 2.8. were occurring. If this is the case then it is possible that the preferential wearing away of ferrite would only occur once the hardness difference between the ferrite and the carbide exceeded some critical amount, and that this critical state might be attained during the course of a run as the result of softening. The published curve was derived from results where 2000 slugs were forged on each insert set, and it is possible, because of the different length of stroke of the press used and hence the increased blow time, that despite the fact no auxilary heat were applied to the dies the inserts ran at surface temperatures higher than those in this investigation. Such increases in time and temperature could mean that inserts of relatively high initial hardness could be softened to the critical state during the course of their run. Since the total amount of metal removed would be partly that resulting from "homogenous" wear and partly the relatively small amount resulting from wear on carbide, the total wear volume would decrease as hardness decreased because softer inserts would soften below the critical level more quickly.

However, this is but the most hypothetical of explanations, and a comparison of the structures and surface hardnesses after forging of inserts below and above the maximum point, with those of inserts in this investigation might be instrumentative in a better understanding of the disparities in behaviour.

D.5. A Comparison of Results from Both Series of Tests

A comparison of results from both series of tests could be made by considering the original wear/temperature and wear/hardness curves, but the wear resistance/surface hardness curve permits both sets of results to be shown together, so it is to that diagram that reference will be made.

A strict, theoretical analysis of the diagram is not possible because the final surface hardness is not truly representative of the state of an insert during its run, and the error incurred in assuming it is, is different for every insert because of the different hardness range and temperature conditions under which it ran. However, in these particular tests the general levels of values in the various sections of the curves differ so widely from each other that, even allowing for the unknown levels of error in the points, a qualitative, relative analysis can be made.

It can be seen that the over-all wear resistance of Series 1 inserts was superior to that of Series 2 inserts of a similar, non-acicular structure, and this would almost undoubtedly be a result of the higher stock temperature during the Series 1 tests. The resulting decreased resistance to deformation of the stock would have imposed lower levels of load on the insert and have reduced both adhesive and abrasive wear. If the wear of inserts 2.6.T and 1.4.T of the same initial hardness and bulk die temperature is compared, it is obvious that the deleterious effects on wear of raising the surface temperature by increasing the stock temperature have been more than offset by the benefinial effects of the decreased load.

The top inserts of Series 2 tests always wore appreciably less than the bottom inserts, even in the one case where the top die was running hotter than the bottom, whereas the difference in the wear of top and bottom inserts of Series 1 tests was not greater than could be accounted for on the grounds of temperature alone. This discrepancy could arise because of the increased abrasion on the bottom inserts of Series 2 sets, consequent upon the quantities of stock scale that was not removed between blows. However, since the control sets showed an even more marked discrepancy when running at stock temperatures of 960-980^{o}C, the resistance to deformation of the stock would also seem to be a contributing factor, such that the available abrasive particles were put to more efficient use at a lower stock temperature, becuase of their decreased tendency to be effectively removed from the wearing system by embedding in the stock.

A comparison of the slopes of the lines on the wear resistance/surface hardness diagram is unfortunately unjustified, for the reasons already mentioned, but a very general comment on their differences is in order. The high slope of the Series 1 inserts line is a consequence of the effects of temperature, which are not accounted for in the measure of surface hardness, becoming more significant at higher temperatures, and highlights the fact that wear resistance cannot be correlated with surface hardness in any practical way because it will depend on the specific conditions during forging that have contributed to the final level of hardness. The slopes of the Series 2 insert lines ABCD and EFGH reflect the structure sensitivity of the abrasive wear occurring and indicate it is possible that adhesive wear is also contributing to the total wear volume, particularly in the top inserts, where such a contribution would tend to lessen the structure sensitivity of the overall wear rate, making the discontinuity of the slopes less obvious.

In general, therefore, the resistance to deformation of the stock would seem to be an important criterion in determining the wear of the inserts, particularly if substantial amounts of abrasive particles are available in the system. The results also indicate that there is evidence for both theories concerning the observed tendency for bottom dies to wear more than top, and although an increased amount of scale on the bottom die would almost certainly increase its wear, such a differential in amount of scale would not be necessary for increased abrasion of the bottom die to occur if, as has been suggested, the stock moving across the bottom die were cooler, and hence more resistant to deformation, than the stock moving across the top die.

SECTION E. Conclusions and Suggestions for Future Work

E.1. The Wear Test

The level of control exercised over test conditions during this investigation seems to have been sufficient to obtain fairly reproducible results, and so permit conclusions to be drawn with some degree of certainty, even though rigorous analysis on a theoretically sound basis is still impossible because of the complexity of the forging process itself.

The question is to what extent the results obtained from such a simplified forging test can be extrapolated to apply to the forging situation in general. Such a test has already been shown[34] to put different steels in the right order as regards their wear resistance under test and in industrial forges, and the results obtained from this investigation are not at variance with observed behaviour in the industrial situation. Therefore, although the actual levels of wear recorded obviously have no significance outside the limits of the test, it seems reasonable to suppose that the generalised conclusions drawn from test results, will hold true in other forging situations. Further, since it is only relative levels of wear and general conclusions that can be of any significance to the forger, a relative measure of area of the profile traces is probably sufficiently accurate for the purpose, although a measure of maximum depth of the trace would not be acceptable.

The wear test does not lend itself to a genuine investigation of thermal fatigue cracking, but the observations made highlight the difficulties in predicting actual behaviour during forging from a measure of thermal fatigue resistance obtained from a conventional test. In particular the temperature cycle during forging derives from a condition of constant heat input rather than constant maximum temperature, so actual levels of temperature, as well as the strains imposed, will be determined by the thermal properties of the material at temperature. Further, it would seem that the cracking does not emanate from purely thermal sources, but that the level of externally imposed stress can contribute to the deterioration.

E.2. The Wear of the Inserts

Conclusions concerning the wear of the inserts have already been drawn in the course of the discussion, but to sum up, the overall picture is that abrasive wear was the dominant wear mechanism, and that the actual amount of wear depended on the resistance to deformation of the stock and the exact state of the surface layers during forging, as described by their temperature, hardness and structural state. Wear tended to increase with increased die temperature and decreased hardness but changes in temperature and hardness had a much more significant effect on the amount of wear when accompanied by changes in structure.

E.3. The Relevance to the Forging Industry

It would seem that the observations made and the conclusions reached concerning the variation of wear with temperature and hardness can be related, at least in general terms, to the industrial situation. However, in practice, the application of general principles may not be economically sound. For example, the increased machining costs for a die of high hardness must be more than offset by its increased life, to make the change to such a die a viable proposition. Further, many conditions of forging are often predetermined within certain limits by the forge itself, the job in hand, furnace capacity etc., and if the general principles are to be applied to an existing forging situation the criterion is whether the forger can, by a relatively small change in forging conditions, produce significant differences in die life.

The really practical areas of interest to the industry, therefore, correspond to the regions of the wear/temperature and wear/hardness curves where discontinuities are apparent. For example, if die temperatures are running in the region where surface temperatures are just above Ac_1, it may well be worthwhile to bring their temperatures to just below Ac_1, whereas a similar drop in temperature may not be a practicable proposition if the original die temperature were lower. A similar argument holds true concerning the desirability or otherwise of increasing hardness, and it would seem that a better basis for the choice of a steel would be one of structure rather than level of hardness alone.

A further complication is that the apparent importance of factors affecting wear can be altered according to the forging conditions so that, say, a harder die may well show a significantly increased life over that of a softer die when running at fairly low temperature, yet show little improvement in performance when die temperatures are high.

Lastly, other factors such as thermal fatigue resistance and toughness must be taken into consideration because improved wear resistance can be of no benefit if premature die failure occurs because of cracking.

Besides hardness and temperature, the resistance to deformation of the stock and the amount of stock scale have also been shown to be important. The resistance to deformation of the stock is determined by the stock material itself and the temperature at which it is forged and since the former is determined by the job in hand, and the latter usually made as high as possible, this aspect of the situation has little relevance to the forging industry. However, lowering the amount of stock scale is becoming more feasible as furnace technology improves, and the practices of a preliminary upset to flake off excess scale before forging, and the blowing away of scale left on the die face between blows are to be highly recommended, particularly when forging fairly highly resistant materials.

In general therefore care must be taken in applying results of wear tests, pro-rata, to the industrial forging situation, but the general principles do remain true, that is, that superior performance will be obtained from a die of No. 5 Die Steel when it is as hard as is commensurate with the retention of sufficient toughness to withstand the impact conditions of load, is preferably acicular in structure, and is run at as low a temperature as possible in the presence of minimum stock scale.

E.4. Suggestions for Future Work

Despite the fact that results during these tests have been fairly self consistent, a degree of scatter is always inevitable in a wear test, but this could be further reduced and hence positivity of results improved if better control were kept on both stock and die temperatures. It was partly in order to improve stock temperature control that accurately dimensioned slugs were used, and if a mains voltage stabiliser were attached to the H.F. set control of slug temperature could be near ideal.

The control of die temperature is less simple. The cycling of the die temperature during a run partly derives from the maximum/minimum settings of the controlling transistrols, and improved controlling equipment would be helpful in this respect. It is also partly a consequence of the controlling thermocouple being so far removed from the heat source, and if recording couples were retained in the inserts, and controlling couples introduced near the heaters, experience would indicate the control settings necessary to attain the desired insert temperature, and control of this temperature would be improved.

However, even with such improved controls, it is doubtful that any rigorous theoretical analysis of wear during forging would be possible, and so the emphasis in future work must still be on comparative tests of direct relevance to the forging industry.

The logical extensions of the present work are a further investigation into the material showing acicularity of structure, to determine over what range of hardness such a structure can be achieved, and whether it retains its superior wear resistance at all hardness levels and temperatures, and an assessment of the effects of temperature on the wear of other materials, to determine more fully how the wear/temperature variance is related to the structure and tempering characteristics of a material. The implication that structure and high temperature strength are important in determining wear resistance suggests that, in particular, investigation into such factors as structural state and the size, type and distribution of carbide particles, coupled with a consideration of other mechanisms for hardening steels, will be most instrumentative in a more fundamental understanding of wear during forging.

Comparative tests on materials, surface treatments etc. could also have their place and since the experimental press is a fairly adaptable piece of equipment that can be used to test any material in any condition at any reasonable level of die or stock temperature and that could be modified, if required, to use shaped inserts, different stock materials, lubricants etc., the possibilities for future work are almost unlimited.

Appendices.

Appendix 1. The Calculation of Temperature at Points Below the Surface of a Die during the Heating Protion of the Forging Cycle

The Basis of the Calculations

In order to simplify these calculations, use was made of a temperature response chart[26] for the conditions nearest to those of the die insert in the experimental press, which have already been outlined in the body of the text (Section A.2.2). The chart is based on a Fourier analysis of temperature distribution and supplies curves for a number of distances, x, from the surface, as a proportion of the plate thickness, d, in terms of the Fourier number, F_o, and a dimensionless value of temperature, T. F_o is equal to the product of thermal diffusivity, α, and time, θ, divided by the square of the plate thickness. The dimensionless temperature value is a function of the bulk temperature t_o, the surface temperature, t_e, and the temperature of the point in question, t, such that

$$T = (t - t_o) / (t_e - t_o)$$

It can be seen, therefore, that the value of F_o can be calculated for a plate of any chosen steel, of any thickness and at any time after the heat input. From the curve for a chosen value of $x/_d$, the corresponding value of T can be obtained and the temperature of a point at $x/_d$ inches from the surface, after time θ can be calculated, knowing t_o and t_e.

This calculation was first carried out for a plate thickness equal to the insert's depth, that is, 0.5 inches, but after a time of 0.1 seconds, approximately equal to the blow time, F_o was 0.000324, and values of T were unreadable from the chart. After 1 second, however, a plot was possible, and showed that a point 0.1 inches from the surface was still unaffected by the heat flow. Therefore in order to be able to assess the temperature of points very near the surface after very small intervals of time, a plate thickness of 0.1 inches was assumed. This assumption of course is not strictly valid since the plate edge is the barrier to heat flow, but in this case, where times of less than one tenth of one second were being considered, and where the desire was for a general indication of temperature levels, rather than exact numerical values the inaccuracies introduced by the simplification were considered tolerable. Actual numerical values would in any case have been of doubtful veracity because of the errors introduced by the assumption of ideal heat transfer in the assessment of t_e and because the variation in thermal properties of the steel with temperature were neglected.

Numerical Results

The thermal diffusivity of No. 5 Die Steel was calculated to be 0.0203 ft^2/hr, so, for a plate thickness of 0.00834 ft, F_o becomes equal to 293 x θ(hrs).

For θ = 0.02 seconds, F_o becomes 293 x 0.00000555, = 0.00162

For θ = 0.04 seconds, F_o becomes 293 x 0.0000111 = 0.00324

For θ = 0.08 seconds, F_o becomes 293 x 0.0000222 = 0.00648

The value of t_o was chosen to be (a) 150°C = 302°F

(b) 300°C = 572°F

and corresponding surface temperatures were deduced from Becks[20] curves for a stock temperature of 1100°C to be

(a) 600°C = 1112°F

(b) 675°C = 1267°F

The results calculated from these values are tabulated in Fig.46., and appear graphically in Fig. 3 in the main text.

Fig.46

		θ = 0.02 seconds = t_1			θ = 0.04 seconds = t_2			θ = 0.08 seconds = t_3		
$x/_d$	x inches	T	t_{302} °F	t_{572} °F	T	t_{302} °F	t_{572} °F	T	t_{302} °F	t_{572} °F
0.01	0.001	0.85	989	1162	0.89	1002	1190	0.92	1047	1212
0.02	0.002	0.71	877	1065	0.79	942	1122	0.85	989	1162
0.05	0.005	0.36	594	822	0.52	722	934	0.65	827	1024
0.10	0.010	0.08	367	628	0.21	468	714	0.37	602	829
0.15	0.015	0.00	302	572	0.03	326	593	0.28	528	766
0.20	0.020				0.01	310	579	0.08	383	528
0.25	0.025				0.00	302	572	0.03	326	593
0.30	0.030							0.005	306	575
0.40	0.040							0.00	302	572

Calculated Values of Temperature

Appendix 2.2. Aspects of the Experimental Work

2.1. The Method of Thermocouple Insertion

30 S.W.G., cotton covered Chromel/Alumel thermocouples were used. The cotton covering retained its insulation properties at the temperatures envisaged, and the fine guage permitted a small bead for greater accuracy.

A hole $\frac{1}{4}$ inch deep and $^{9}/64$ inch diameter was drilled into the back of the insert, ending in a 60° cone, and a groove $\frac{1}{4}$ inch wide was milled radially from it to the edge of the insert to allow the thermocouple leads to be brought out.

Heat sinks were made to the dimensions shown in Fig. 47.a) by turning them to size and then drilling. The thermocouple wires were led up through the 0.020 inch diameter hole, sparked together, then sparked to the top of the heat sink (Fig. 47.b)). The assembly was pushed into the prepared hole and the edges of the hole gently peened over to ensure its retention (Fig. 47.c)).

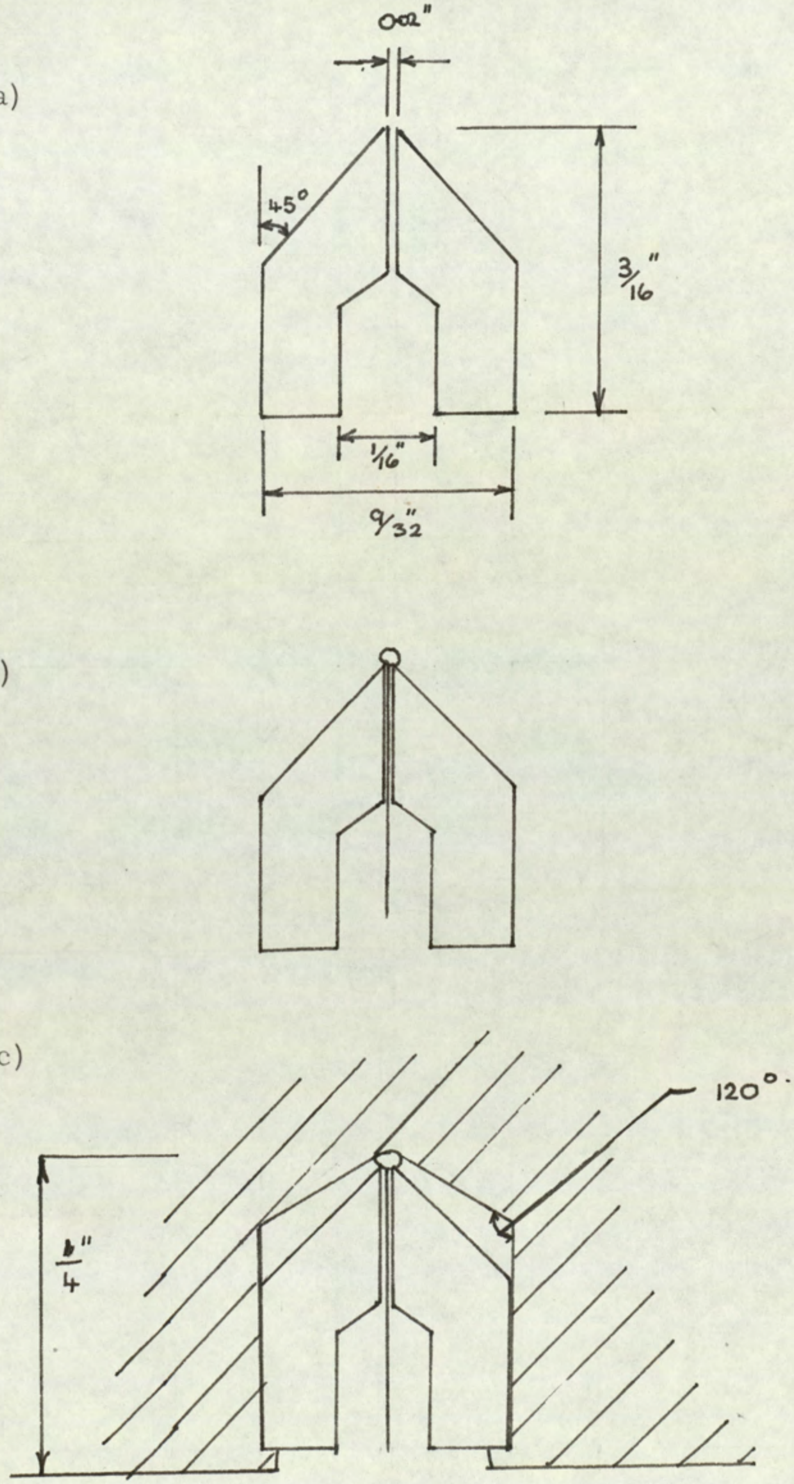
The leads were laid along the groove prepared for them, and the groove and the end of the hole were filled with "Silkaset", a plastic-based insulating compound applied as a gel which hardens on exposure to air. This ensured the leads were not displaced when assembling the inserts in the dies.

Fig 47

a)

b)

c)



The stages in the Production of the Thermocouple Assembly

2.2. The Construction of the Computer Programme to Calculate Wear Volume

It was first assumed, as was necessary for any type of measurement, that the trace was representative of any diametric profile across the insert. The programme had to be capable of dealing with every trace, and a schematic diagram of the "general case" of a trace is shown in Fig. 48.

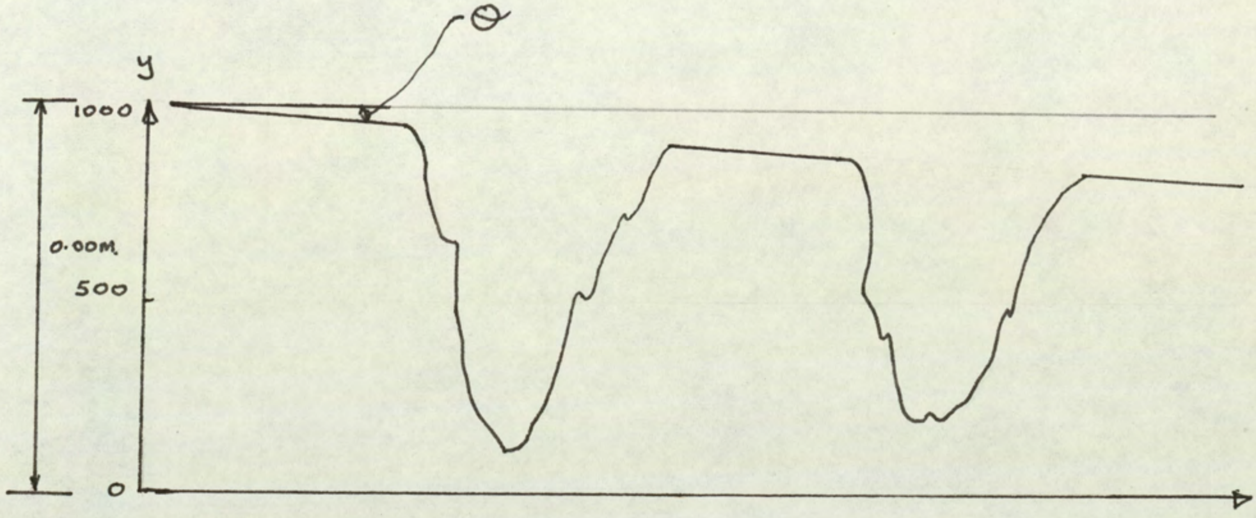
On the digital output the trace was represented by a series of three digit numbers between 000 and 999, whose value corresponded to the y co-ordinate of the point. The position of the number on the tape was representative of its x co-ordinate, since numbers were printed out at equal time intervals which represented equal distance intervals on the traverse.

The prime necessity therefore was that there be facility in the programme for putting the punched values into an array that would allocate a numbered position to each point, thus giving it an x co-ordinate, and further that there be facility for converting these artificial co-ordinates to real values.

The x Co-ordinate

The horizontal magnification was chosen always to be x10, and at this magnification a traverse of 1 inch took

Fig 48



A Typical Wear Trace

49.53 seconds. Points were printed on the data tape at the rate of ten per second, therefore the distance interval between each point was $\frac{1}{10 \times 49.3}$ or 0.00209 inches. On this basis the 100th point on the tape was given an x co-ordinate of 100 x 0.00209 inches or 0.209 inches. The correlation facotr, 0.00209, was called N.

The y Co-ordinate

Various vertical magnifications were used, the aim being to use as large a magnification as was compatible with retaining the trace within the limits of the chart. If the full scale deflection at a chosen magnification were M thousandths of an inch then 0.00M inches was represented on the trace by the figure 1000. Therefore one division represented 0.00M/1000 inches, and for correlation to a real value of depth each figure on the output tape was multiplied by 0.00000M, a factor called in the programme, RATIO.

Identification of Results

To identify results a die set number and trace number was punched onto each tape and an "instring/outstring" section included in the programme. The identification was then punched out before the calculated results.

Establishment of a Base Line

Before any calculations of metal removed could be made it was necessary first to establish the position of the "unworn" die surface as datum. Since the original die was surface ground flat and $\frac{1}{4}$ inch of the original surface existed outside the worn area, a projection of the unworn flats across the contour was taken as the base line from which calculations would be made. From the traces the number of points, B, constituting the shorter of the flats was estimated and fed into the computer. The computer was programmed to add the y co-ordinates of the first B points on the data tape and to divide the result by B, to give the y co-ordinate of the mid point of the flat. The x co-ordinate was designated $(\frac{B}{2} + \frac{1}{2}).N$ since B points represent (B - 1) intervals. The process was repeated working backwards from the end of the tape to give a second set of co-ordinates. From these two sets of co-ordinates the values of SLOPE and CONST in the equation of the straight line y = SLOPE.x + CONST joining the points could be found, hence an equation for the base line established.

Establishment of the Extent of the Impression

It was decided that the impression should be said to have "begun" when the trace deviated from the base line by an amount equal to $^{1}/_{20}$ of the full scale deflection, that is 50 x RATIO, and the "finish" point should be determined in the same way. Since it is conceivable that one odd point could be that far from the base line before the genuine downward trend began, points were averaged five at a time and the average value compared with the base line. If the difference was less than 50 x RATIO the first point of the five was dropped out, and a fresh point brought in, and a new average calculated and compared. When the difference did exceed 50 x RATIO the co-ordinates of the middle point of the five being averaged were designated as the co-ordinates of BEG, the begin point. FIN, the finish point, was found in the same way, working backwards through the data.

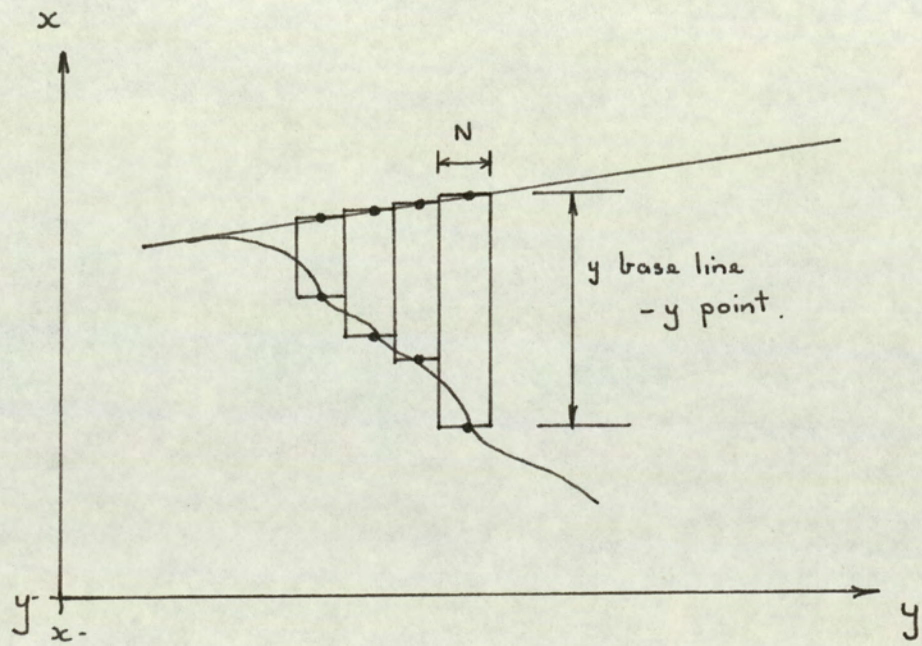
Geometric and Algebraic Aspects of the Calculations

Calculations were made of the area of the radial wear impression and the sum of the two volumes of metal that would be represented by a semicircular sweep of each of the radial impressions about a mid point, taken to have x co-ordinates half way between those of BEG and FIN and to lie on the datum line.

The Area

Each x co-ordinate was taken to represent the mid point of the short side of a tiny block 0.00209 inches wide and of length equal to the difference between the y co-ordinate of the point and the y co-ordinate of the base line at the same x co-ordinate, translated to a real value (Fig. 49).

Fig 49



The Incremental Blocks For The Calculation of Area.

The area of each block was simply calculated as width x length and the summation of all block areas between BEG and FIN, divided by two, gave a value for the area of the radial impression, in square inches.

The Volume

The calculation of volume was less simple, since the blocks had to be swept through a semicircle in the plane of the base line, hence areas of blocks perpendicular to the base line had to be calculated. The geometry of the problem is shown in Fig. 50 .

The volume it was desired to calculate was that swept out by the rotation of a block area $N_1 \times h_1$ through π radians at a radius of d_1 from the midpoint, that is, the volume increment $N_1.h_1.d_1.\pi$.

The value of all distances lettered without suffixes were calculable direct from the data and the tangent of θ was SLOPE, already calculated, so a value of θ was obtainable from the data.

From Fig. 50 :-

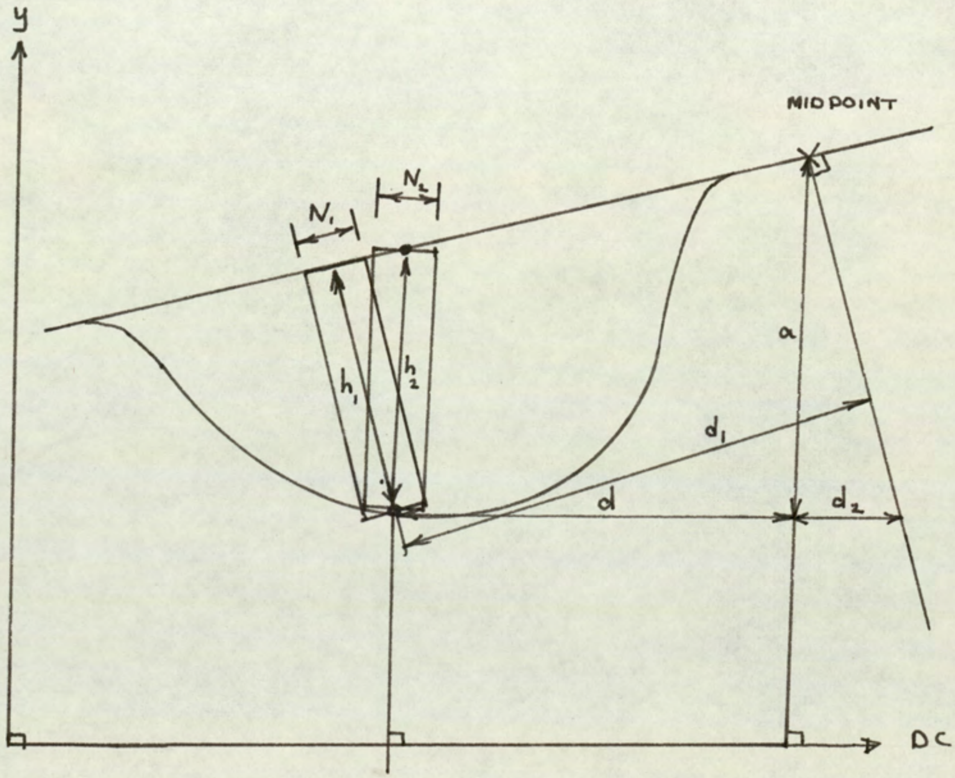
$$N_1 = N/\cos\theta$$

$$h_1 = h.\cos\theta$$

$$d_1 = (d + d_2)\cos\theta \quad \text{and} \quad d_2 = a\tan\theta$$

$$\therefore \quad d_1 = \cos\theta\,(d + a \times \text{SLOPE})$$

Fig 50



The Incremental Blocks For the Calculation of Volume.

The volume increment then became:-

$$VOL = N\ .h\ .\cos\theta.(d + a.SLOPE)$$

in terms of values which could be calculated direct from the data. A summation of all such incremental volumes from BEG to FIN gave a figure for volume of metal removed, in cubic inches.

A printout of the programme is reproduced in Fig. 51. annotated to indicate the function of each part. It will be noted that such values as BEG, FIN, DIAMETER are also printed out, and these provided a check, when referred back to the traces, that the programme is in fact functioning correctly. A practical point to note is that identification and values of N, B and RATIO must be put onto the front end of the tapes, and the figure -999 added after the data to indicate the data is complete.

Fig. 51. <u>The Computer Programme.</u>

```
K.E.SEAL,WEAR VOLUME AND AREA CALCULATIONS'

BEGIN
    REAL N,LEFT,RIGHT,SLOPE,CONST,AV,Z,BEG,FIN,
         MIDX,MIDY,VOL,RATIO,AREA'
    INTEGER B,M,I,POINTS'
    ARRAY Y(1:1500)'
    SWITCH S:=AGAIN,REGO,RETRY'
```

Identification.

```
    PRINT ££L4??'
    M:=1'
    INSTRING (Y,M)'
    M:=1'
    OUTSTRING(Y,M)'

    READ N,B,RATIO'
```

Establishment of Base Line.

```
    I:=0'
AGAIN:I:=I+1'
    READ Y(I)'
    IF Y(I) GR -100 THEN GOTO AGAIN'
    POINTS:=(I-1)'

    LEFT:=0'
    FOR I:=1 STEP 1 UNTIL B DO
    LEFT:=LEFT+Y(I)'
    LEFT:=LEFT/B'
    RIGHT:=0'
    FOR I:=(POINTS-B+1) STEP 1 UNTIL POINTS DO
    RIGHT:=RIGHT+Y(I)'
    RIGHT:=RIGHT/B'
    SLOPE:=(RIGHT-LEFT)*RATIO/((POINTS-B)*N)'
    CONST:=LEFT*RATIO-SLOPE*N*(0.5*B+0.5)'
```

Establishment of Extent of Impression.

```
    I:=B'
REGO: I:=I+1'
    AV:=(Y(I)+Y(I+1)+Y(I+2)+Y(I+3)+Y(I+4))*RATIO/5'
    Z:=SLOPE*(I+2)*N+CONST'
    IF (Z-AV) GR (50*RATIO) THEN BEG:=(I+2) ELSE GOTO REGO'

    I:=POINTS-B+1'
RETRY:I:=I-1'
    AV:=(Y(I)+Y(I-1)+Y(I-2)+Y(I-3)+Y(I-4))*RATIO/5'
    Z:=SLOPE*(I-2)*N+CONST'
    IF (Z-AV) GR (50*RATIO) THEN FIN:=(I-2) ELSE GOTO RETRY'
    PRINT ££L3??, £BEG=     ?,SAMELINE,BEG,££L1??,
                  £FIN=     ?,SAMELINE,FIN,££L1??,
                 £POINTS=?,SAMELINE,POINTS,££L1??,
                  £SLOPE=?,SAMELINE,SLOPE,££L1??,
                  £CONSTANT=?,SAMELINE,CONST,££L2??,
        £DIAMETER=?,SAMELINE,((FIN-BEG)*N/COS(ARCTAN(SLOPE))),
    ££L2??'
```

Mid-Point.

```
    MIDX:=0.5*(FIN+BEG)*N'
    MIDY:=SLOPE*MIDX+CONST'
```

Volume and Area Calculations.

```
    AREA:=0'
    VOL:=0'
    FOR I:=BEG STEP 1 UNTIL FIN DO
        BEGIN
            VOL:=VOL+ABS(((SLOPE*I*N+CONST)-(Y(I)*RATIO))*N*
          ((MIDX-I*N)+((MIDY-Y(I)*RATIO)*SLOPE))*
        COS(ARCTAN(SLOPE)))'
            AREA:=AREA+ABS(((SLOPE*I*N+CONST)-(Y(I)*RATIO))*N)'
        END'
    VOL:=VOL*3.1416'
    AREA:=AREA*0.5'
    PRINT £VOLUME=?,SAMELINE,VOL,££L2??,£AREA=?,
          SAMELINE,AREA,££L3??'
END'
```

2.3. The Descaling of Inserts

Descaling Solutions

Compositions of possible solutions for descaling were obtained from "Metals Handbook", "Corrosion Handbook" and "Corrosion Testing Procedures", and a short list drawn up of the four claiming least metal loss. Pieces of a worn scaled die were subjected to each treatment to assess its efficiency in removing the scale; two of the solutions proved inefficient. Pieces of unscaled die steel were then weighed and subjected to the recommended descaling procedure in the two solutions that had proved efficient, and then reweighed to assess metal loss. Results are tabulated in Fig. 52.

The better solution appeared to be 5% sulphuric acid with a small quantity of quinoline as an inhibitor. This solution was also preferable to the potassium cyanide one on the grounds of safety and speed. However, it was feared that, although the total metal loss was small, an electrolytic descaling method might result in preferential attack on the peaks of the wear impression, again giving false results from the traces. An artificial "worn" insert was therefore machined, traced, "descaled", and retraced. The two traces are shown in Fig. 53. and proved the solution to be acceptable. Its use was thereafter adopted for all scaled die inserts.

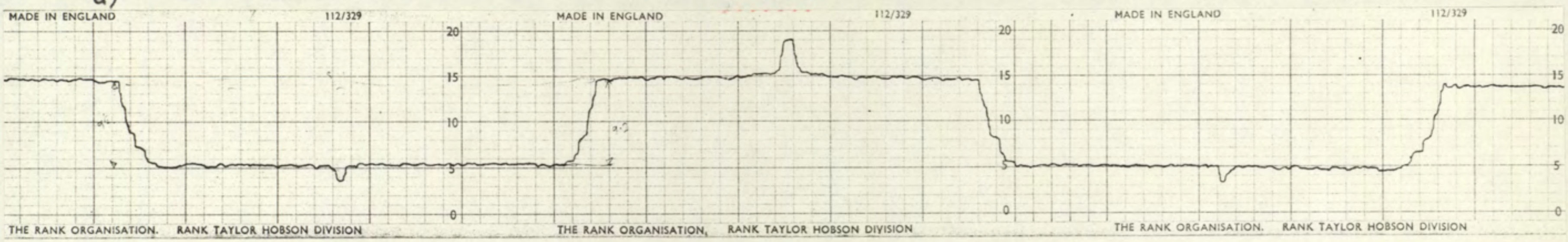
Fig.52.

Composition of Solution	Treatment Conditions			Results				
	Time	Temperature	Current Density	Efficiency	Weight before descaling (gms)	Weight after descaling (gms)	Weight loss (gms)	Percentage Weight loss
10% Ammonium Citrate	10 mins	Cold	1 amp/dm^2	Poor				
5% Sulphuric Acid with Quinoline	5 mins	74°C	20 amp/dm^2	Good	107.835	107.831	.004	.0037%
5% Potassium Cyanide	3 hours	Cold	1.5 amp/dm^2	Good	109.637	109.630	.007	.0064%
8-10% Sodium Hydroxide	3 hours	Cold	-	Poor				

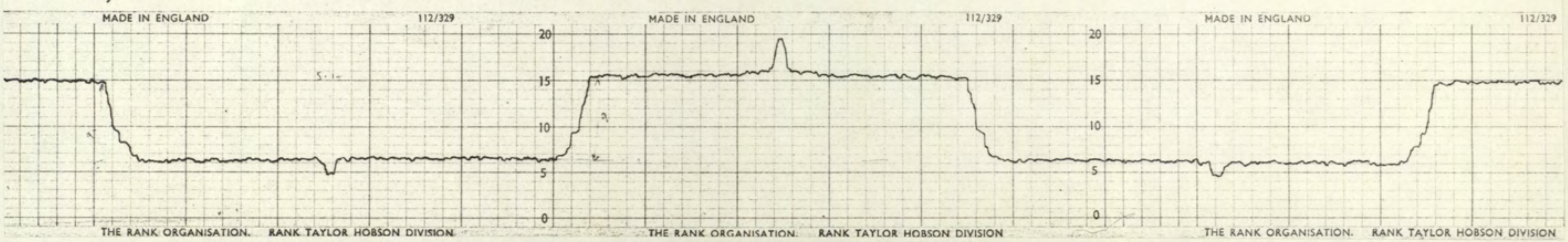
Descaling Procedures Tested

Fig. 53.

a)



b)



Profile Traces of a Specially Machined Insert, a) before and b) after Descaling.
Approx. $\frac{1}{2}$ size.

Current Density

Using the sulphurio acid solution a current density of 20 amps per square decimeter was necessary.

The surface area of an insert 2 inches in diameter and $\frac{1}{2}$ inch thick is

$$2\pi \times (2.54)^2 + 2\pi \times \frac{1}{2} \times (2.54)^2 \text{ sq.cms.}$$

$$= 63 \text{ sq.cms.} \qquad = 0.63 \text{ sq.dm.}$$

To attain the required current density therefore, a current of 12.6 amps would be necessary, which was beyond the 5 amp limit of the available electropolishing equipment.

The sides and back of the insert were stopped off with "Fortolac" and the surface area then became

$$\pi \times (2.54)^2 \text{ sq.cm.}$$

$$= 0.21 \text{ sq.dm.}$$

requiring a current of 4.2 amps, which was obtainable. All surfaces of the insert except its face were therefore stopped off, and a current of 4.2 amps was used.

2.4. The Production of Taper Sections

Again in order that direct correlation should be possible between all results it was deemed desirable to set up a standard procedure for sectioning and polishing, which was adhered to for all inserts.

The method adopted was based on that suggested by Samuels[44], but modified so that only one mounting operation was necessary.

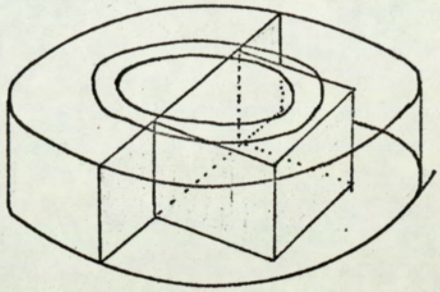
A section was cut from the insert as shown in Fig. 54.a). a full half of the insert always being retained in case metallographic examination of a vertical section should prove necessary. The edge from which grinding was to begin was marked with a Chinagraph pencil, and the distance from the centre of the plateau to that edge measured in order that the free edge of the section could accurately be brought to lie along the radius of the insert after grinding and polishing.

The back and front faces of the section were known to be parallel because of the original mode of die insert manufacture. The specimen could therefore be mounted, worn face upwards, in a standard hot mounting press with the certainty that the worn face would be parallel to the top and bottom of the mount (Fig. 54b)). The amount of mounting compound used was measured to ensure at least 0.05 inches of mount above the specimen.

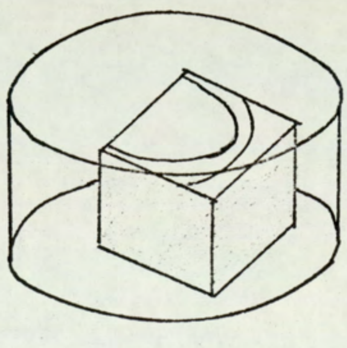
The mount was inserted into a specially made jig (Fig. 55.a)).

Fig 54

a)

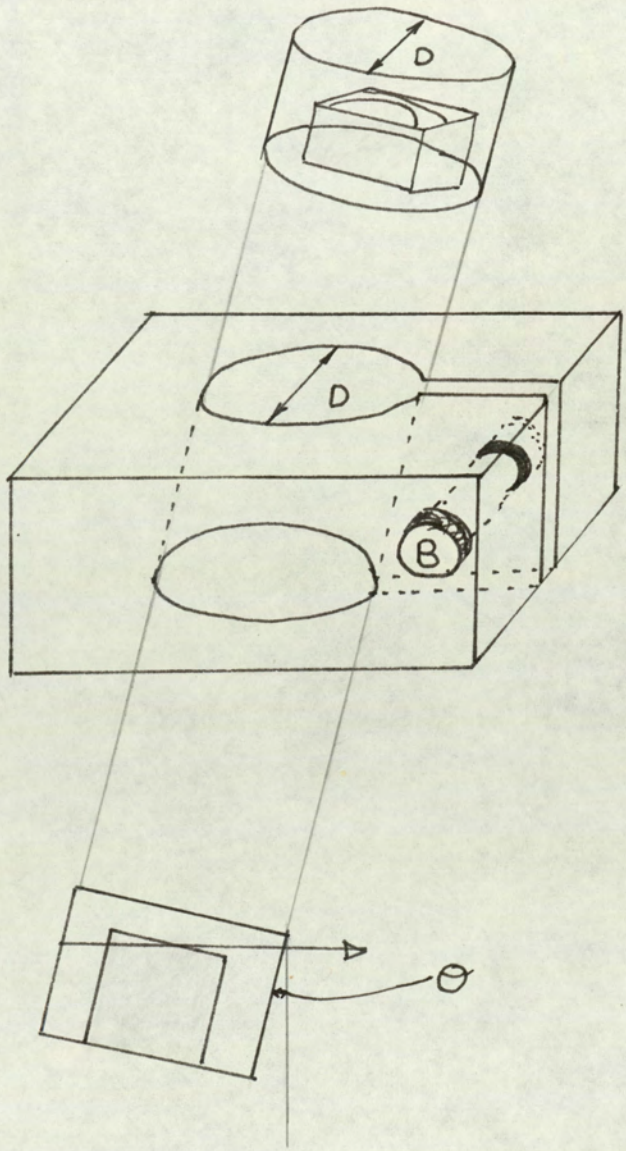


b)



The Sectioning of Inserts and Mounting of Specimens.

Fig 55



Producing the Taper

designed to hold it at the predetermined taper angle, and rotated until the marked edge was at the highest point and perpendicular to the line of greatest slope. The knurled bolt B was tightened up to ensure no movement of the mount occurred during grinding. The mount and specimen were then surface ground until metal had been removed to nearly half way across the specimen. The 0.05 inches of mounting compound left above the level of the worn surface permitted this grinding operation to produce a completely flat surface suitable for subsequent polishing, eliminating the necessity to knock out and remount the specimen (Fig. 55b)). Polishing on 220 emery was carried out until the free edge was brought in line with the radius. Subsequent polishing on finer grades of emery and diamond wheels did not much alter the measured dimension from free edge to back edge, and any slight increases tended to counteract errors introduced by assuming the length measured on the section was the same as that measured on the original surface. The initial fairly heavy polishing also ensured that any deformation layers resulting from the surface grinding operation were removed.

It is possible that the final polished surface is not exactly parallel to the ground surface and a tilt through even a very small angle can have a significant effect on the taper ratio. It was desirable therefore to be able to assess the exact ratio after polishing to be able to accurately translate measurements made on the polished surface to values of real depth. Samuels suggested a fine guage wire should be laid parallel to the surface and perpendicular to the line of sectioning, and retained by a thick electrodeposit. The wire would be ground and polished along with the section. The polished face would be elliptical, the minor axis being equal to the original diameter of the wire, and the ratio of the major to the minor axis providing a measure of the actual taper after polishing. If the wire were not exactly perpendicular to the line of section, the taper ratio would be given by the ratio of the distance between the tangents to the ellipse, parallel to the line of section, to the minor axis.

The principle of the method was adopted for this investigation, but the layer of mounting compound above these particular specimens proved to provide adequate retention of the wire during polishing, obviating the necessity for electrodeposition and its possible effects on the surface layers, and speeding the preparation process. A wire 0.005 inches in diameter was used, and after polishing the taper ratio was measured using the same calibration table and crosswire system as used to measure the depth of structural effects (Section B.2.3).

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