~~SECRET.~~

~~DISCREET.~~

PREFACE.

During the last war, the Ordnance Board set up a Sub-Committee of the Armour Piercing Projectile Committee. It was known as the Armour Piercing Projectile Co-ordinating Sub-Committee, and its functions were to review and co-ordinate the various investigations being carried out in connection with the attack of armour. After the war, this Sub-Committee continued in being with a reduced scale of activity. In order that the results of war-time work might be made more conveniently available to those working in the same field in future, it appointed a Report Sub-Committee, charged with the duty of preparing, in book form, a digest of the knowledge which had been accumulated.

The composition of the Report Sub-Committee, which included the authors of all sections of this volume, is shown below, followed by some relevant information about each of the members.

Dr. R. Beeching (*Chairman*).

Mr. C. A. Adams (*Secretary*).

Dr. J. W. Maccoll.

Dr. D. G. Sopwith.

Dr. C. Sykes.

C. Sykes, Ph.D., D.Sc., F.Inst.P., F.R.S., now Director of Research, Thos. Firth and John Brown Ltd., Brown-Firth Research Laboratories, who wrote the Foreword, was Chairman of the A.P.P. Co-ordinating Sub-Committee from its inception in 1941 to 1946. He held, at the same time, the posts of Superintendent of the Metallurgy Department at the National Physical Laboratory and Superintendent of Terminal Ballistics in the Armaments Research Department. In this dual capacity, he was intimately concerned with the development of solid steel shot, heavy naval A.P. shell, cored projectiles and armour plate. He was primarily responsible for the introduction and use of calibration shot for firing trials, and played an important part in introducing cored projectiles into service during the war.

R. Beeching, A.R.C.S., B.Sc., D.I.C., Ph.D., who prepared Chapters 1 and 5, was first concerned with A.P. shot while in the Research and Development Laboratory of the Mond Nickel Co. In 1943, he transferred to the Armaments Design Department, and was for some time, Superintendent of Shell Design. He was associated with the design of many armour piercing projectiles, with the development of high velocity cored projectiles and with the production of heavy A.P. shell at R.O.F. (Cardonald). He became Chairman of the A.P.P. Co-ordinating Sub-Committee in 1946.

D. G. Sopwith, D.Sc., Wh.Sc., A.M.I.Mech.E., now Superintendent of the Engineering Division, National Physical Laboratory, who wrote Chapter 2, was Secretary of the A.P.P. Co-ordinating Sub-Committee from its inception until 1946. He was responsible for the analysis of an extensive series of firing trials, made under closely controlled conditions in a special range at the N.P.L. and designed to elucidate scale effect and the effect of plate hardness. He also did much to rationalize the use of penetration formulæ.

C. A. Adams, B.Sc., F.Inst.P., who prepared Chapters 3 and 4, was in the Terminal Ballistics Branch of the Armaments Research Department, and did a great deal of investigation by means of small scale trials, in connection with which he developed and employed various high-speed photographic techniques. He contributed greatly to understanding of the penetration of complex targets, and cap stripping. In 1946, he became Secretary of the A.P.P. Co-ordinating Sub-Committee.

Dr. J. W. Maccoll, Superintendent of Theoretical Armaments Research in the Armaments Research Department, was not directly responsible for preparing any one section of this volume, but gave helpful advice and criticism throughout. Much of the theoretical work on the mechanism of penetration was carried out under his supervision. He was a member of the A.P.P. Co-ordinating Sub-Committee from its inception in 1941.

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PROJECTILE.

d = diameter in inches.

M = mass in lbs.
W = weight in lbs. wt.
} With these definitions W and M are numerically equal. Both are given because their dimensions differ, and each symbol is frequently used.

A = projected area.

ρ = density.

l = length of head.

PLATE.

t = thickness in inches.

ρ^1 = density.

$w^1 = \frac{\pi}{4} d^2 t \rho^1$ = weight of displaced plate material.

f = stress associated with resistance offered to penetration.

f_y = yield stress in compression.

f_u = ultimate tensile stress.

q = effective shear strength during penetration.

S_o = yield stress in shear.

S_F = fracture stress in shear.

E_c = critical energy for perforation.

CONDITIONS OF ATTACK.

v_o = striking velocity in f.s.

v_1 = residual velocity in f.s.

v = critical velocity in f.s.

u = instantaneous velocity.

θ = angle of attack = angle between initial direction of motion of projectile and normal to plate.

s = slope of line relating v_0^2 and v_1^2, *i.e.* $v_0^2 = v^2 + s v_1^2$.

FORCES.

F = maximum force opposing penetration.

$\bar{F}$ = mean force opposing penetration.

STRESSES.

p = pressure resisting penetration.

p_o = value of p assumed in "constant pressure" theories.

P = maximum value of p.

$\frac{\gamma \rho^1 u^2}{2g}$ = component of pressure assumed due to dynamic effects.

γ may be regarded as the "drag coefficient" during penetration, giving $p = p_o + \frac{\gamma \rho^1 u^2}{2g}$.

ELASTIC CONSTANTS.

E = Young's modulus.

σ = Poisson's ratio.

FOREWORD

By Dr. C. Sykes, F.R.S.

ARMOUR AND ARMOUR-PIERCING PROJECTILES PRIOR TO WORLD WAR II.

Prior to the Second World War, experience in the use of armour-piercing projectiles was almost exclusively confined to Naval warfare and knowledge of armour and armour-piercing projectiles developed against this background. There was, inevitably, competitive development of armour and shot and supremacy passed back and forth between defence and attack. To understand this competition fully it is necessary to realize that not only did sizes of guns and projectiles increase to keep pace with increases in thickness of armour, but also there was a continuous struggle to improve the quality of armour, to get the greatest protection from a given weight, and to improve the quality of projectiles to give the greatest armour penetration with guns of limited size.

Naval armour started to come into general use around 1860, when the adoption of spherical, cast-iron, shell rendered wooden ships very vulnerable. To counter this type of projectile, ships were fitted with a belt of wrought iron plates, which broke up the spherical shell. As a consequence, spin-stabilized, ogival headed, cylindrical shell or shot of chilled cast-iron were developed.

Following this, ships were armoured with compound armour of wrought iron plates faced with steel, then with homogeneous steel plates, and a corresponding development was the use of hardened steel shell.

To defeat the hardened, forged steel shell, armour plate was carburized and chilled on the outer face, and this led to the fitting of shell with piercing caps. Such caps were, in the first place, made of mild steel and were of various forms. Against early forms of face-hardened armour, and at angles of attack near normal, they prevented break-up of the shell head by the hard face of the plate.

Further improvements in quality of face-hardened armour resulting from the use of high alloy steel plates, together with an increase in the importance of angles of attack other than normal associated with changes in Naval tactics, led to the adoption of various forms of hardened steel penetrating cap and also to a reduction in the length of the shell head. Thus, between the wars, the shell in use by the major Navies of the world had all developed to a rather similar form, the shell bodies being about 3 to $3\frac{1}{2}$ calibres long, with an ogival head of about 1·4 c.r.h. and made from heat-treated, fairly high carbon, high alloy steel. In general, these shell were fitted with a steel piercing cap, having about a tenth of the weight of the shell body, and were also fitted with a long ballistic cap, to improve the external form of the shell from the point of view of air resistance.

The shell bodies were very hard at the point and their hardness was graded down from the point to the base, the precise hardness layout varying quite considerably between different countries and different makers in any one country. This grading of the body hardness was held to be necessary to prevent break-up of the shell during perforation of plate at angles of 30 degrees or so, and was also an aid to manufacture, as it permitted machining of the base end of the shell after heat-treatment.

Between the 1914—18 and 1939—45 wars, increasing importance was attached to the future rôle of the tank and all leading nations developed anti-tank, armour-piercing projectiles to keep pace with the development of more heavily armoured fighting vehicles. Even so, however, the thicknesses of armour involved were relatively small and such armour could be severely overmatched by reasonably small weapons. Therefore, while there was appreciation of the advantages of producing good quality shot to give high efficiency weapons in terms of the ratio of their penetrative performance to their weight, the natural tendency was to develop guns and projectiles which easily outmatched existing armour. For this reason the quality of anti-tank A.P. projectiles was not so severely tested as it was to be later.

The war led to a rapid progressive increase in armour thickness on tanks, with the result that each generation of gun and projectile passed through a phase where it was presented with an increasingly difficult task. Under these conditions it was of vital importance to achieve the highest possible penetrative performance from a given weapon, which necessitated improvements in shot quality and design. Up to that time the developments in both Naval and Land Service armour and armour-piercing projectiles had mainly followed naturally from developments in steel making and treatment and had depended upon only superficial understanding of the mechanism of armour penetration. However, as the problems of the 1939—45 war developed in rapid succession, a better understanding of the process of armour penetration became increasingly necessary. As a consequence, in 1941, a small Committee — the Armour Piercing Projectile Co-ordinating Sub-Committee—was set up by the Ordnance Board, with the following terms of reference :—

> "To review and co-ordinate the investigations being carried out by groups of scientists in connection with the attack of armour: to make recommendations concerning their scope and progress; and to report to the Board."

SUMMARY OF ACTIVITIES OF THE A.P.P. CO-ORDINATING SUB-COMMITTEE.

At the time when the Sub-Committee was formed, a considerable amount of investigational work had been put in hand, both in the various research laboratories associated with the Services and in industrial laboratories. This work was reviewed and the meetings of the Sub-Committee were used as a medium whereby the various investigators could familiarize themselves with the work going on elsewhere.

Although the Sub-Committee was primarily concerned with the attack of armour, *i.e.*, the performance of the projectile, it found immediately that to make any reliable investigation relating to the behaviour of shell required a detailed knowledge of the behaviour of the plate. Consequently, research on both plates and projectiles was co-ordinated by the Sub-Committee practically from its inception.

In 1941 a good deal of empirical knowledge was available on armour and shell. As regards penetration of armour, the De Marre formula, or one of its variants, was used, and Milne very quickly collected together the mass of firing trial data available at the Ordnance Board and reduced it by statistical methods into what is now known as the Milne formula (see Chapter 2). The bulk of Milne's data referred to attack at an angle of 30 degrees.

Similarly, Stockdale, at the Department of Tank Design, reviewed the data accumulated on tank armour plates, generally for "normal attack," and produced a suitable empirical formula to handle the data. This formula is different in form from Milne's, but is similar in accuracy.

While both these formulæ served their purpose in allowing the behaviour of shot, shell and armour to be predicted with reasonable accuracy, they had certain demerits. The empirical constants could not readily be interpreted in terms of any known physical property of the plate, although it was known that within certain ranges of hardness the constants increased with hardness. As regards the shell, the formulæ merely recognized its weight and diameter and did not differentiate between an annealed shell and a hardened shell. Finally, the formulæ took no account of any scale effect and there was no reliable information as to whether such an effect did, in fact, exist.

The resistance of armour varies from plate to plate and from one position on any particular plate to another. Shell vary in quality. The firing test itself is generally a test to destruction, either of plate or shell, and it is well known that scatter in tests of this type is high. Thus, it is not surprising that both the Milne and Stockdale formulæ, applied intelligently, enabled the behaviour of armour plate to be predicted within the limits with which the plate could be produced in bulk.

However, from the point of view of further developments, a much better understanding was required, and it is the purpose of this book to record the progress made in this direction. The major items of development proceeded as follows :—

Shot.

It is generally agreed that the production of shot and shell of a consistent quality is a much simpler proposition than the production of armour. Cleaner steel can be used and the problems of segregation are much easier. At an early stage, then, the manufacture of the so-called calibration shot was instituted. These shot were made under supervision and were subsequently segregated into batches of known uniformity of hardness gradient, by an electrical method. Such shot were reliable projectiles for proof of armour, and considerably reduced the variability in the firing-trial test.

During its passage through the plate the shot has to impose and withstand the very high stresses necessary to deform the plate material. In the case of normal attack, the stresses in the projectile are primarily compressive, although tensile stress waves occur by reflection from the rear end. At angles other than normal the stress distribution in the projectile is more complex, due to the unsymmetrical pressure on the head, and tensile stresses are likely to be produced by the applied bending moment. Therefore, the projectile should be of such a shape and of such a material that it has adequate strength to withstand these stresses without appreciable deformation or fracture. Hardness layouts which ensured this were determined empirically, but better understanding of the stresses set up in a projectile during plate perforation became increasingly necessary, as more severe performance requirements arose.

Stresses calculated from the penetration formulæ are at best "average," and do not give the maximum stress. Two methods were explored to provide such information; the photographic method—by means of which the retardation of the shot can be obtained from high speed films covering the actual penetration, and the static penetration method —in which the force necessary to perforate the plate under static conditions is determined using a standard projectile and a press. This latter method was applied up to 2-pr. scale. The results obtained were used to develop a reliable means of determining minimum hardness gradients for shot and shell. They were particularly useful in providing a quantitative method for dealing with the effects of cavity shape and size on the performance of shell.

At an early stage in the war the phenomenon of "shatter" was encountered. The term is applied when shot failure occurs at high velocities, with complete collapse of the shot, while, at lower velocities on the same plate normal penetration may occur without any shot break-up. This trouble led to the study of the inertia forces on the head of the shot arising from the high velocity induced in the plate material during the initial stages of penetration. The importance of such forces, especially with high velocity carbide shot, is now realized.

Shatter was eliminated as a practical problem on steel shot by the introduction of capped shot. Although much empirical work was carried out with various shapes and weights of cap, understanding of the mechanism of cap action remains limited.

The war saw the introduction of types of target which had received little previous consideration; high angle targets, space-plate targets, etc. Such targets accentuated problems associated with cap-stripping and yaw. Much information of a semi-quantitative type was produced regarding such problems, by means of the high speed spark photography equipment.

Plate penetration formulæ.

As already indicated, variability in normal supplies of armour and shot made it impossible to determine which of the various empirical formulæ was likely to be the most reliable. Consequently a detailed firing programme was worked out using specially manufactured armour plate and shot, in an endeavour to get maximum uniformity. Four sizes of projectile were used, approximately 0·3 inch, 0·5 inch, 1 inch and 1·56-inch (2-pr.), and firing trials were carried out under laboratory conditions, using residual velocity measurements for determination of critical velocity.

Such trials have been carried out under conditions of normal attack and angle attack, and the reproducibility of the results is far higher than that previously achieved in any firing trials, and has justified the care and attention put into the work.

The results give definite evidence for a scale effect, and enable its magnitude to be assessed. In addition, the constants in the penetration formula finally chosen can be estimated, within certain limits, from the physical properties of the plate. Although the reproducibility of the critical velocity in these trials is high, values falling within limits of ± 10 f.s., the various empirical formulæ still give equally good "fits" when the appropriate constants are chosen. For a given scale of attack, in terms of the ratio of shot calibre to plate thickness, plate resistance is found to pass through a maximum at a certain hardness level. This hardness varies with the (shot calibre/plate thickness) ratio.

Plate quality.

Armour plate can fail in a variety of ways, when attacked by a given projectile under standard conditions. It may plug, petal or disc. The discing failure is usually associated with a low penetration velocity, and is also objectionable for other reasons. There was very little authoritative information available as to how the physical properties of the armour plate affected the method of failure. This problem was studied empirically from the data provided by metallurgical examination and physical testing of plates, and the conclusions correlated with the investigation on plate penetration already described.

It was demonstrated that discing depends both on the "quality" of the plate, *i.e.*, freedom from inclusions and marked directional properties, as well as on the actual hardness.

PROBLEMS YET TO BE SOLVED.

Major problems in connection with the penetration of armour by armour-piercing projectiles yet remain to be solved. All of these are involved in a more thorough understanding of the mechanism of penetration, and some of them are of direct practical importance in relation to the improvement of shot and armour performance.

It is necessary to find out much more about the way in which plate material behaves under the peculiar condition of stressing and deformation which are induced by the penetration of shot. The mechanical properties normally measured for steel are of very limited value in this connection, and a very big step forward would be achieved by determination of the behaviour in shear of plate material under conditions where the principal stresses were all compressive, at high rates of strain. The effect of steel quality, *i.e.*, cleanliness and directionality, on this behaviour is of particular importance.

Secondly, more knowledge is required of the stress distribution in projectiles when penetrating armour at angles other than normal. So far, nearly all attempts to measure or to calculate the stresses in a shot have been confined to the case of normal attack. For this case, relatively simple theoretical treatments of the problem give useful results, but the importance of stress data for angle attack is greater, and far more difficult to obtain.

A third outstanding problem, to which reference has already been made, is to determine how a piercing cap serves to prevent shatter. Possible explanations suggested so far are:—

(*a*). That the cap imparts enough energy to the plate, to reduce the effects of the inertia of the plate material immediately in front of the shot, without transmitting great stress to the shot because of its own break-up.

(*b*). That the cap gives radial support to the shot head, by virtue of its own strength or inertia, during the early stages of penetration.

However, trials with various shapes of cap calculated to eliminate the possibility of one or other of these effects have failed to substantiate either explanation. It is possible that both play some part, but this has not been proved.

CHAPTER 1.

THE MODES AND MECHANISM OF PLATE PENETRATION AND OF SHOT FAILURE.

By R. Beeching.

1. Introduction.

When a good armour-piercing shot or shell penetrates a plate, the penetration usually occurs in one of a few quite characteristic ways, the actual mode of failure depending upon the nature of the plate. In the first part of this chapter the various common modes of penetration are described, and an explanation of the way in which plate properties determine the nature of the penetration is offered. For this purpose the shot is considered to be perfect and to suffer no fracture or deformation, while, in the second part of the chapter, typical forms of shot failure are described, and explained so far as is at present possible.

The quality of armour plate must obviously be assessed primarily in terms of its ability to stop piercing projectiles, but an important secondary requirement is that, when defeated, the plate shall not fail in such a way that fragments become detached and so add to the lethality of the attack. These two requirements are not independent, since both depend upon the mode of deformation and fracture of the plate when penetrated, although it does not follow that the type of plate giving the most desirable form of failure offers the greatest resistance to penetration. This inter-relationship between resistance to penetration and mode of failure is considered further when mechanism of penetration is discussed.

When considering the various types of plate failure it may prove helpful to bear in mind the obvious fact that complete perforation of a plate cannot occur by deformation alone. Some form of fracture must also occur, and the type of plate failure which takes place is very largely determined by the nature and position of the first fracture.

The deformation or break-up of piercing shot or shell sets a limit to armour penetration performance. Set-up of a projectile on impact increases the striking energy necessary for success against a given plate, while, in the case of shell, it may also cause a premature, low-order detonation. Break-up of a projectile, if it occurs before perforation is substantially complete, also has the effect of raising the striking energy necessary to defeat any given plate. Further, although break-up of shot on leaving the plate may be advantageous and increase lethality, shell must remain unbroken until detonated if the explosive filling is to be effective. Therefore, a good criterion of quality for armour piercing projectiles is ability to resist deformation or fracture while penetrating thick plate, and it is important to develop the best possible understanding of the mechanism whereby these failures occur.

It is not considered that the ideas suggested to account for the various modes of failure of plate or shot offer a complete explanation of the observed phenomena. Indeed, it appears that, in view of the present lack of knowledge relating to the plastic flow of metals, especially at high rates of shear, a rigorous treatment of the problem is impossible. Nevertheless, incomplete as they are, these attempts to explain the mechanism of various types of plate and shot failure are thought to be of value as a basis for considering the influence of plate and shot properties on performance, and may form a useful step towards more complete treatment of the problem at some future date.

1.1. *Typical plate damage (homogeneous armour) : Normal attack.*

For the sake of simplicity the whole of this section will be limited to consideration of the phenomena associated with normal attack. In a later section, the differences in behaviour resulting from variation of the angle of attack will be described.

When a shot is fired into a plate of about one calibre thickness or more, the front of the plate almost invariably has either the petalled appearance shown in Fig. 1, or that shown in Fig. 2, whether perforation is complete or not.

If the plate is thick enough to stop the shot, the impression in the plate, beyond the crater, is a mould of the shot form. On the other hand, if the shot perforates the plate, the last stages of hole formation may occur in a variety of ways, giving one of four main types of back damage or, sometimes, a combination of two or more of these.

A plug of approximately shot diameter may shear out, giving a roughly cylindrical hole right through the plate and a slight lip on the back face, such as that shown in Fig. 3. Alternatively, penetration may proceed to the stage where the projectile breaks through the back of the plate and forms back petals. These are normally larger and fewer than the front petals, and they may remain attached to the plate or break away as they are bent back by the shot.

A third type of back damage occurs with rolled plate and is caused by the breaking away from the back of the plate of a disc of metal up to several calibres in diameter and usually half a calibre or so in thickness. Typical discs are shown in Figs. 4 and 5.

A fourth type of back damage which may occur is the breaking away of irregular flakes from the back of the plate. This most commonly occurs with cast plate, and Fig. 6 shows the type of flake which may be detached. This is a rather exceptional example, since in the case illustrated only one large, fairly symmetrical flake was formed and this remained in one piece. It would have been more typical had it broken into several irregular shaped pieces.

2. The Mechanism of Plate Penetration and Failure. Normal Attack.

All the phenomena referred to in the previous section can be accounted for in terms of plate properties, in a general way, although no strict quantitative treatment has been found possible so far.

2.1. *Plates of semi-infinite thickness.*

Consider a shot fired at normal against the face of a semi-infinite mass of armour. As the head of the shot forces its way into the plate, any element of the head surface in contact with the plate exerts a compressive force on the plate. This force is roughly normal to the head surface at any part, because, as metallurgical examination of damaged plates shows, a thin surface layer of plate either melts or is raised to such a high temperature that the coefficient of friction between plate and shot is likely to be low. Thus the forces exerted on the plate by the shot may be resolved into a forward, axial component, and equally distributed radial components. As a result of the radial load, the plate material shears over a series of co-axial conical surfaces, cutting the plate face at approximately 45 degrees, as shown by the dotted lines in Fig. 7. The shear stress is greatest over the surface closest to the shot ogive. Plastic displacement occurs there first and then extends to surfaces further and further out as work hardening occurs, and as the shot penetrates to a greater depth. Due to the form of the ogive, and as a result of the radial velocity imparted to the plate material displaced by it, a raised collar builds up round the head, as shown at AA in Fig. 7. This tends to split up into petals under the influence of the resulting tensile stresses, as shown in Fig. 1. If, however, the plate has rather less ductility and is incapable of so much deformation in shear, shear fracture will occur over one of the conical surfaces, giving front damage of the type shown in Fig. 2.

As penetration proceeds, displacement of plate by the process described above obviously becomes more difficult, since there is a rapid increase in the area over which shear must occur, if displacement is to extend to the plate surface. Examination of impressions in thick plates suggests that the process of front petal formation has virtually ceased by the time the shot ogive is completely immersed in the plate.

Under these conditions, when bulging of the plate has ceased, if further penetration is to occur, the volume of material displaced by the shot ogive must be accommodated by elastic deformation. The material immediately around the shot will be deformed plastically, and will be reduced in volume by an amount corresponding to the elastic part of the strain. Therefore, a large zone of plate material surrounding the plastically deformed zone must also be subjected to elastic compression, to account for the full volume of material displaced. Penetration under these conditions has been treated theoretically in refs. A.20, 243 and 289. While it represents an important part of the process of penetration in the case of tungsten carbide cored shot, where the thicknesses of plates penetrated are large in relation to calibre, it will be seen that the conditions are not normally encountered in the attack of plate by steel projectiles, due to the influence of the back face overlapping that of the front face.

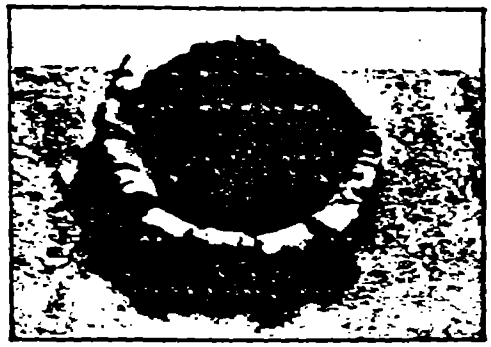


Fig. 1.

Typical front petals and also the beginning of a conical shear fracture below the petals on the left.

Fig. 2.

The appearance of an entrance hole from which the petals have sheared. The polished appearance has been destroyed by rusting.

Fig. 3.

The appearance of the back of a plate from which a plug has been driven.

2. A

FIG. 4.

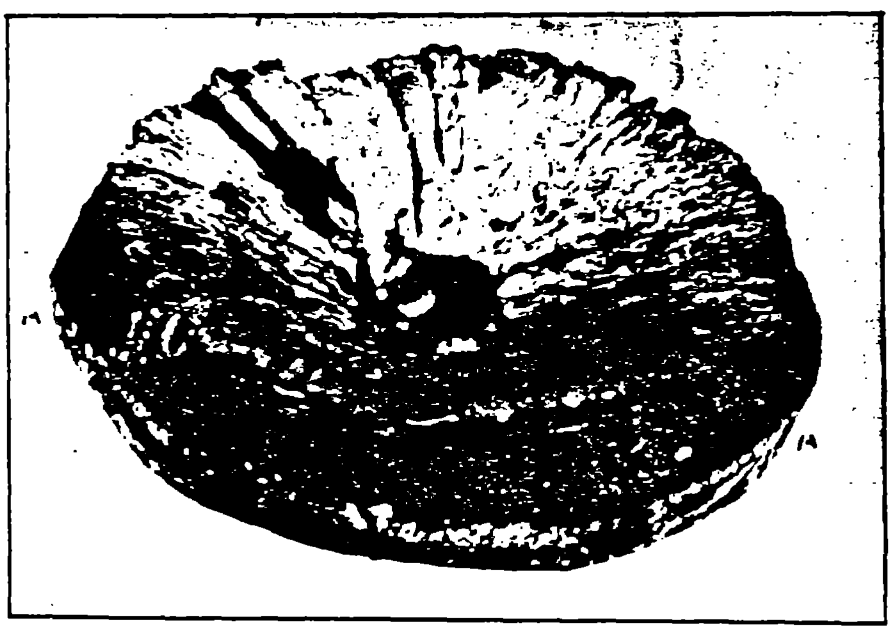


FIG. 5.

Figs. 4 & 5.—Two typical discs.

2-B

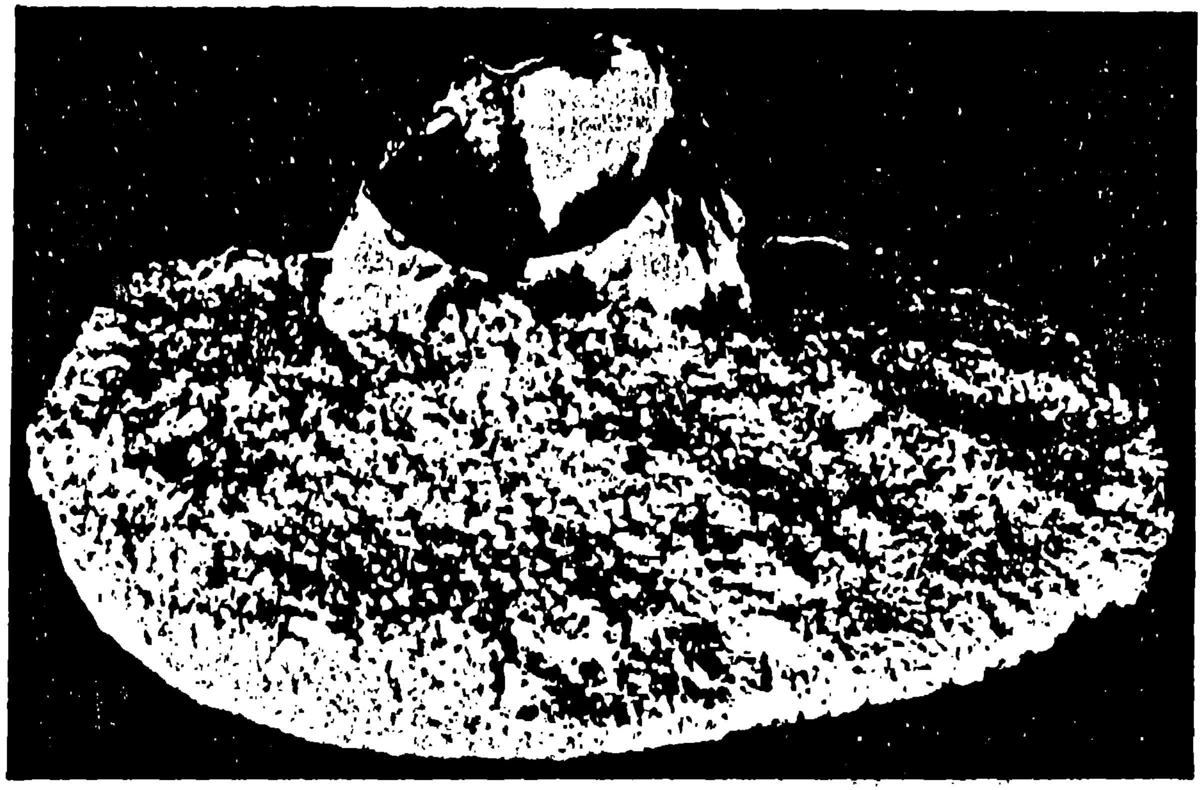
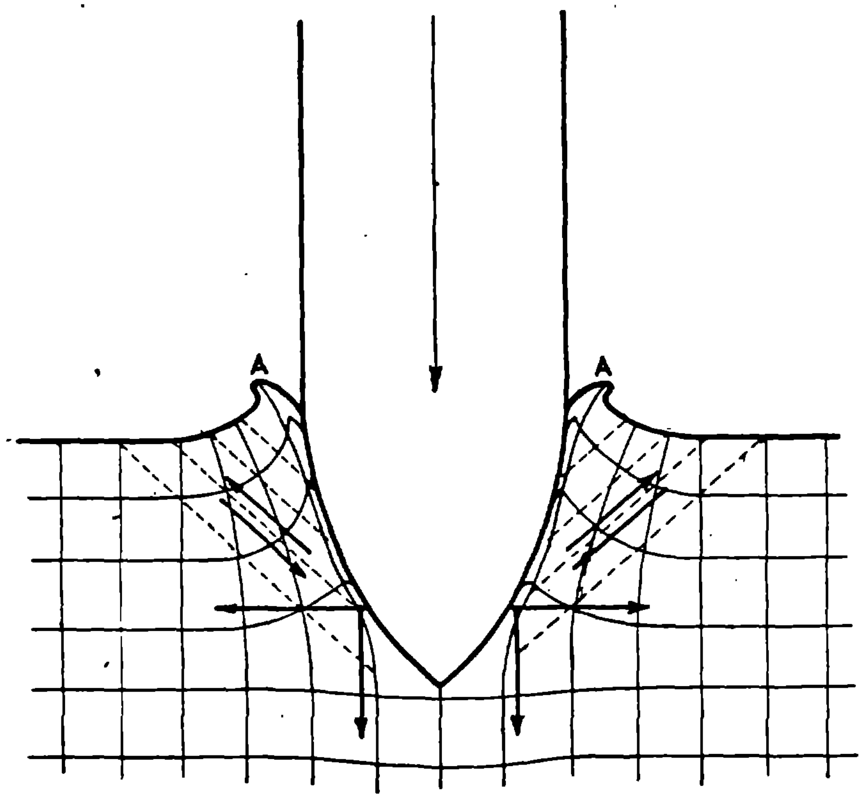


FIG. 6.
A flake from cast plate with part of a plug attached.

2-C

FIG. 7

SHOWING THE MODE OF PLATE DEFORMATION ASSOCIATED WITH THE FORMATION OF FRONT PETALS.



2.2. *Plates of normal thickness.*

For reasons which will become apparent, steel projectiles are seldom employed against plate of more than two calibres thickness, while plates of much less than one calibre are so outmatched that they present a problem of relatively small interest. Therefore, plate thicknesses around one to two calibres thickness are of most general interest.

Consider now what happens if the semi-infinite plate is reduced to a thickness of the normal order. Then, by the time the shot ogive is immersed in the plate, there will be shear stresses over roughly cylindrical surfaces co-axial with the prolongation of the shot, as indicated in Fig. 8†. These shear stresses will be a maximum over the inner surface, where they will have a value :—

$\frac{T}{\pi dt}$ where T is the forward thrust of the shot

d is the shot calibre

t is the thickness of plate forward of the shoulder of the shot,

and the stress may be assumed to fall off approximately inversely as distance from the shot axis.

All the while the plate is so thick that the shear stress $\frac{T}{\pi dt}$ does not cause plastic deformation, the conditions will be similar to those in the semi-infinite plate. It is interesting, therefore, to determine approximately at what plate thickness this condition is no longer satisfied.

Consider the state when the shot ogive is just completely embedded in the plate, so that if t is the full plate thickness, then $\frac{T}{\pi dt} = S_0$ is the condition for plastic shear over the cylindrical surface, where S_0 is the yield point in shear of the plate.

If it is assumed that there is a normal pressure p all over the head surface, then $T = \frac{\pi d^2}{4} p$.

In reference 243 it is shown that the pressure p necessary to cause increase in cavity size in an infinite mass of plate is from four to six times the yield point in compression of the material which, in turn, is approximately twice S_0.

Hence, if the lower value is taken, the condition for the expansion form of penetration is $p = 4\ f_y = 8\ S_0$, while the condition for shear over the cylindrical surface is $T = \frac{\pi d^2}{4} p = \pi d\, t\, S_0$

$$\text{or } p = 4t/d\ S_0 = 8S_0 \text{ when } \frac{t}{d} = 2.$$

Therefore, the influence of the rear face will become important as soon as t is less than about $2d$.

Thus, plates of normal thickness cannot be treated as though of infinite thickness at any stage of penetration. Once the shot head is immersed in the plate, penetration will proceed, to some extent at least, by the formation of a bulge on the back face of the plate.

2.3. *The formation of a bulge on the back face.*

The foregoing picture is useful as a means of showing at what plate thickness the influence of the back face becomes of importance, but it is over simplified by the assumption that there is a uniform pressure all over the shot head. It suggests that in plates of less than two calibres thickness, penetration beyond the front petalling stage would occur by the forward displacement of plate material by shear over cylindrical surfaces only. In practice, it appears that this process is accompanied by some sideways displacement of plate material by the shot ogive, and that penetration proceeds by a combination of forwards and sideways displacement of material by the head of the shot. The manner in which material is assumed to be displaced is illustrated roughly in Fig. 9. It is to be expected, however, that as plate thickness is reduced, or as penetration proceeds, the tendency towards forward rather than sideways displacement of the plate material will increase.

As penetration proceeds under these conditions the bulge on the back face becomes more pronounced and more sharply curved over the apex, while increasing shear strain develops over the cylindrical surface which is a prolongation of the shot body. Under these conditions, fracture of the plate will ultimately occur in one of two ways, depending upon the plate properties. Either a star crack will develop at the apex of the bulge, due to the tensile stresses set up there, or a plug of roughly shot diameter will shear out.

† This is a simplified picture, but represents a reasonable approximation to the true one.

The first of these types of failure will be favoured by relatively good ductility under shear stress in the bulk of the plate, or poor ductility under tensile stresses over the back face, while the second type of failure will be favoured by converse conditions*.

Once a crack has formed at the apex of the bulge on the back face, this is likely to extend through to the head of the shot and so reduce the rigidity of the plate in front of the shot. Further penetration is then likely to proceed by the bending forwards and outwards of petals, as illustrated in Fig. 10.

2.22. *Plug formation.*

A matter of obvious importance in relation to plug formation is the question as to how far the plug will move forward before shear fracture occurs. In the past, this problem has been dealt with by making arbitrary assumptions about the distance which the plug moves through and the shear load necessary to cause the movement. A more reasonable treatment than this is possible, even though simplification is necessary.

If the formation of a plug of a full plate thickness is considered, then the shear stresses parallel with the shot axis would be expected to fall off roughly inversely as distance from the axis, outside a radius $d/2$, since the total forward thrust to be supported remains constant and the area of any cylindrical surface of equal shear stress is directly proportional to its radius. For the present purpose, it is assumed that there is no shear stress inside the surface of radius $d/2$, although this is not likely to be strictly true for normal head shapes.

Unless the shear deformation extends through a shell of finite thickness, fracture would be expected after infinitesimal plug movement, even though the material had high ductility in shear. Therefore, plastic shear cannot be limited to the surface over which shear stress is a maximum, and the question as to how far the plug moves becomes one of deciding how widely plastic deformation extends.

If the plate material work hardens so that the fracture stress in shear divided by the yield stress in shear is λ, then the yield point will just be reached at a radius $\frac{d}{2}\lambda$, when shear failure occurs at a radius $\frac{d}{2}$. Therefore, it is to be expected that a cylindrical zone of internal radius $\frac{d}{2}$ and external radius $\lambda\frac{d}{2}$ will be plastically deformed, the degree of deformation varying from a maximum at a radius $\frac{d}{2}$ to zero at $\lambda\frac{d}{2}$. Hence, the forward movement preceding shearing out of the plug will depend upon the work hardening of the plate material.

2.23. *Failures peculiar to rolled plate.*

So far, the plate has been regarded as isotropic, but rolled plates seldom are, on account of their tendency to have planes of weakness parallel with the surface. If such planes of weakness are sufficiently pronounced, laminar cracks may form in the plate during penetration and lead to discing failures of the type already described, or to a modified form of back petalling failure. The association of such cracks with discing failure and with the modified type of back petalling failure is illustrated by Figs. 11 and 12 respectively.

2.24. *The formation of laminar cracks.*

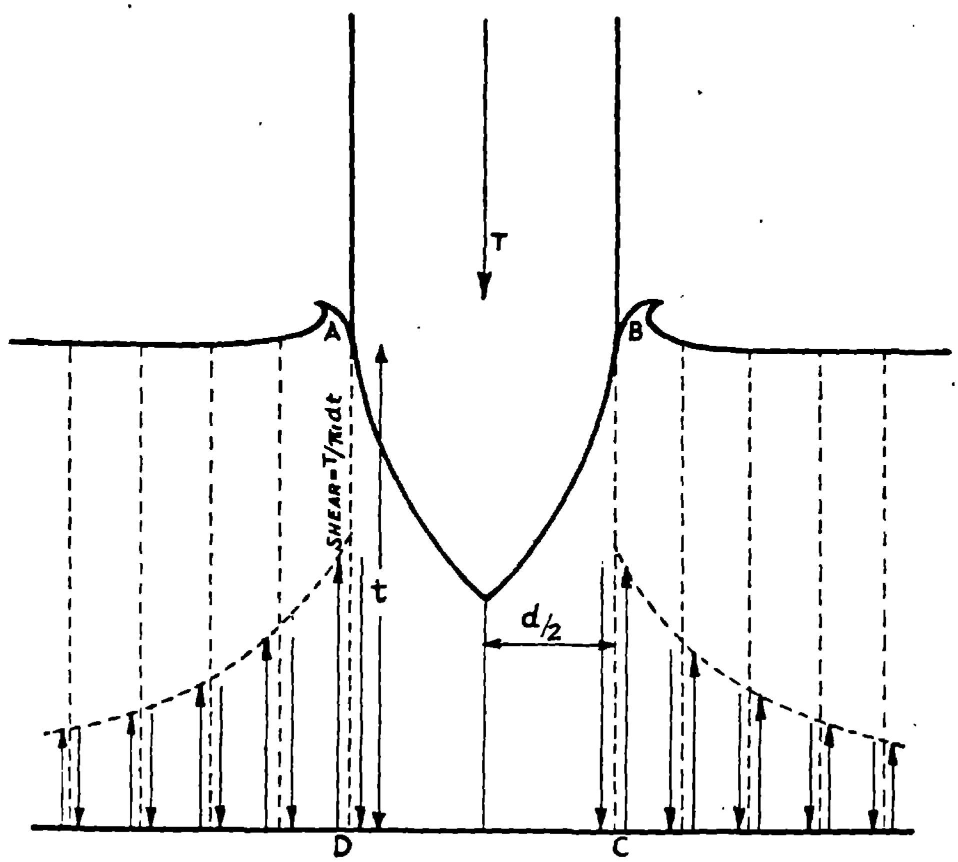
It is evident that the presence of laminar weaknesses in a plate cannot prevent the processes of failure already described by increasing the resistance of the plate to failure in these ways. Since they do affect the mode of failure, however, they must do so by causing the intervention of some other form of breakdown which alters the mode of deformation and affects the stresses which develop. Moreover, it is not uncommon for the same plate to fail by plugging in some parts and by discing or star cracking following lamination in other parts, and since when a plug is driven out it normally has a length of from half to two-thirds the plate thickness, it is evident that the event preventing plugging must occur at an early stage in shot pentration.

* This is borne out by the fact that some plates giving star cracking failures can be made to give plugging failures by machining off the oxide embrittled back face.

FIG. 8.

SIMPLIFIED REPRESENTATION OF THE SHEAR STRESSES ASSOCIATED WITH PLUG FORMATION.

THE LENGTHS OF THE PAIRS OF ARROWS INDICATE THE INTENSITY OF THE SHEAR STRESSES.



4·A

FIG. 9

SHOWING THE PLATE DEFORMATION ASSOCIATED WITH THE FORMATION OF A BACK BULGE.

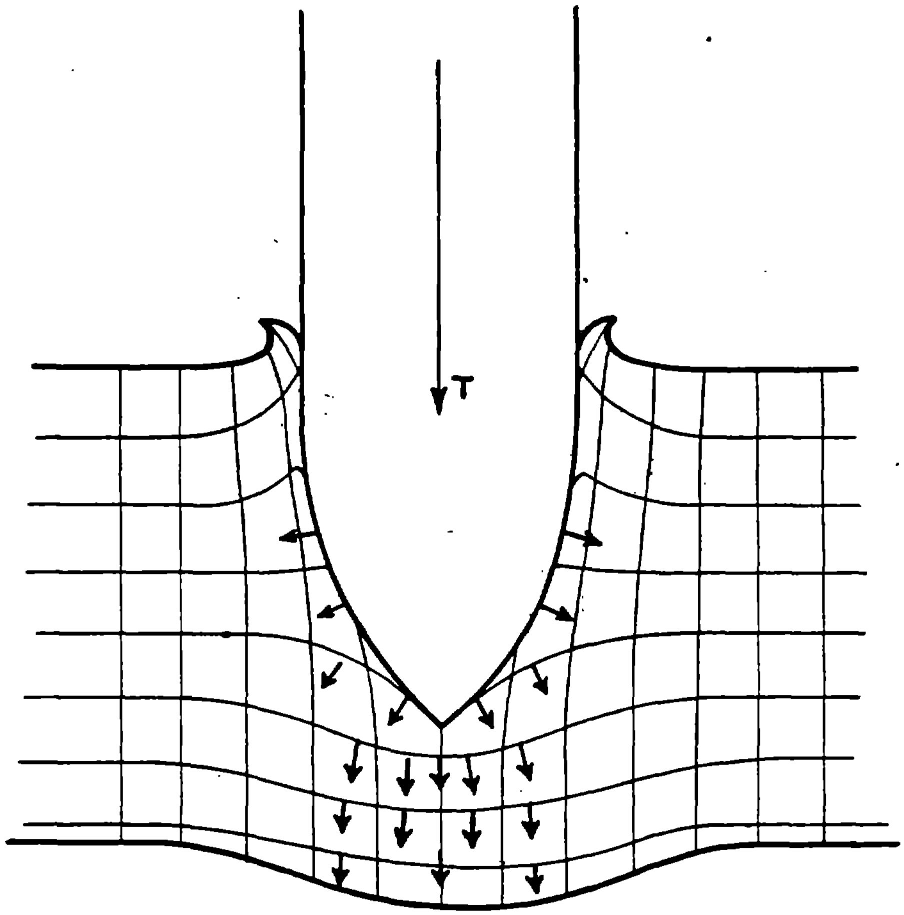
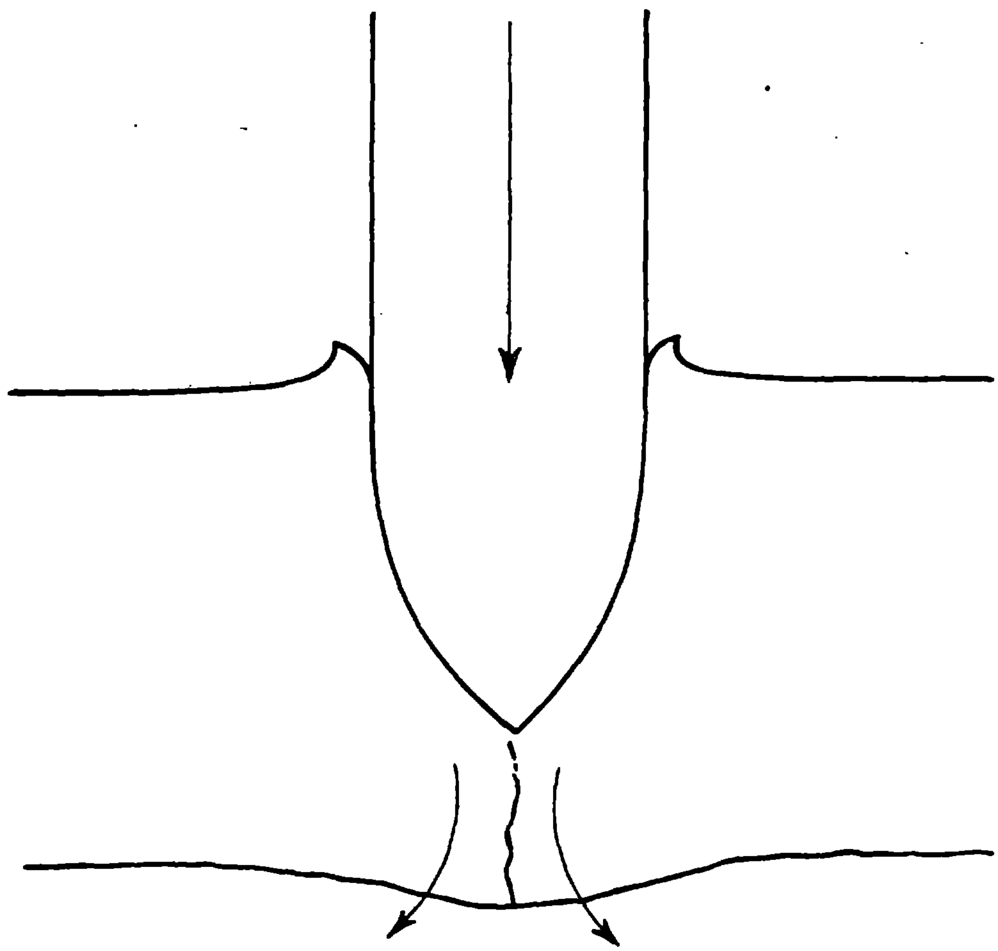


FIG. 10

SHOWING HOW THE METAL REMAINING IN FRONT OF THE SHOT WILL TEND TO BEND WHEN ONCE A STAR CRACK HAS FORMED.



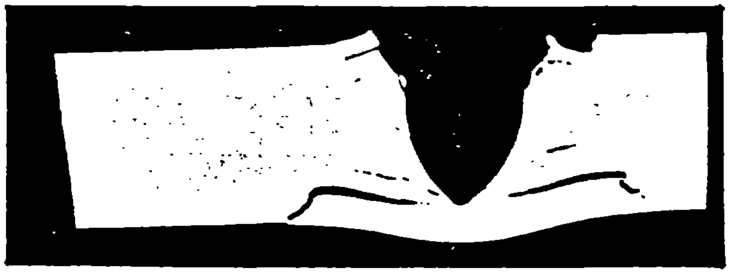


FIG. 11. ($\times \frac{3}{8}$).
A section through a plate from which a disc was about to become detached.

FIG. 12. ($\times \frac{1}{2}$).
A section through a plate which has failed by back petalling after laminating.

4-D

This event is the formation of one or more laminar cracks in the layers of plate in front of the shot, with a consequent reduction in its rigidity. When the plate is laminated in this way it tends to behave as a pile of discs, clamped round their outer edge, and therefore has a lower rigidity than a single diaphragm of the same total thickness. As a result, the layers of material in front of the shot tend to retreat before it more easily by bending, and so the tendency to plug formation is reduced.

The manner in which the laminar cracks form is not fully established, though the fact that they do form in parts of the plate not yet penetrated has been shown by sectioning partially penetrated plates. One view is that they are formed by the tensile stress wave which returns from the back face after the initial compression wave has reached it. This explanation could in any case apply only at a time at which the displacement due to the reflected wave exceeds that due to the continuation of the pressure wave, or in approximate terms, after the peak resistance has been passed. The alternative view that laminar cracks result from the deformations of the plate associated with the formation of the back bulge seems more satisfactory. This second mechanism may be viewed in either of two ways, one of which is represented in Fig. 13 and the other in Fig. 14.

Shear will occur over the surface of the cylinder A B C D shown in Fig. 13 and this cylinder may be regarded as being pushed through the surrounding material. If there are planes of weakness parallel with the plate surface, the forward movement of the cylinder will tend to separate the plate into layers, in much the same way as a rough, tightly fitting plug pushed through a hole in a pile of plates clamped round the edge would cause the back plates to bulge away from the others.

In the neighbourhood of the cylinder A B C D, where the shear stresses are high, there will be correspondingly intense shear stresses parallel with the plate surfaces, and it is not quite clear to what extent these shear stresses contribute to the initial formation of laminar cracks. It does seem certain, however, that once a crack extends as a complete ring round the embryo plug, it extends outwards mainly as a result of the concentration of tensile stress over its outer edges. This extension continues until the crack perimeter is large enough to reduce the tensile stress across it to a value below the strength of the plate in a direction perpendicular to the plate faces. On the other hand, the extension of the crack inwards, into the zone in front of the shot where there are compressive stresses through the plate, may occur at a later stage as a result of shear stresses produced by the stretching of one layer with respect to another as they are bulged forward by the shot.

Fig. 14 represents an alternative way of viewing this process of crack formation. If a shot were fired into a pile of plates clamped round the edges, the plates would be expected to bulge in the manner illustrated, with consequent separation. A single plate would be expected to behave in a similar manner if it had sufficiently pronounced laminar weakness.

2.25. *Star cracking after lamination.*

Reference has already been made to the way in which lamination might increase the tendency to form a rear bulge on a plate, while reducing the tendency towards plugging. As a result, the presence of laminar weakness in a plate may encourage a back petalling type failure.

The various layers of plate will tend to fail in the same way as a pile of thin plates. They will be bulged in the direction of motion of the shot and the metal over the shot nose will be thinned by stretching radially and by compression between the nose of the shot and the next layer. This process may continue for each successive layer, including the back one, and give rise to a star cracking failure and finally to complete back petalling. This stretching and thinning of successive layers is well shown in Figs. 11 and 12.

On the other hand, since the last layer is less adequately supported than the preceding layers, it is not remarkable that it may fail in a different way. Whether it does so or not depends upon its thickness, upon the extent of the laminar cracking, and upon the tensile strength of the main bulk of the plate in directions parallel with the face, as compared with the tensile strength of the surface layer.

2.26. *Discing.**

The last layer of a laminated plate will bend like a circular diaphragm clamped around the edge and loaded at the centre. It will, therefore, bend with a double curvature of the form shown in section in Fig. 15. Consequently, there will be radial tensile stresses set up round the edge of the inner face and over the centre of the outer face. The thinner the layer in relation to the diameter of the laminar crack, the less pronounced will be the curvature round the edge in relation to that at the centre. Consequently, the less severe will be the radial stress at the edge in relation to that at the centre and the more likely is the plate to fail by star cracking, particularly if the back face is embrittled. On the other hand, if the layer is thick in relation to the diameter of the laminar crack, the tensile stresses at the centre will be small compared with the stress at the edge, which results from the combination of the radial tensile stress due to bending and the tensile stress perpendicular to the plate face due to the forward thrust on the disc as a whole, as indicated in Fig. 15. In this case a crack is likely to form first round the periphery of the laminar crack, and, except in-so-far as it is affected by the anisotropy of the plate, it will tend to start towards the back face with a slight outward inclination as shown.

As the crack extends towards the back face and the thickness of metal remaining uncracked is decreased, the local concentration of tensile stress over the edge of the crack and in a direction perpendicular to the plate face will tend to increase, while the radial stress will decrease. Therefore, the direction of the resultant stress, which will remain equal in magnitude to the tensile strength of the steel, will swing progressively towards the normal to the plate face, as cracking proceeds. This process, considered as occurring in stages, is illustrated in Fig. 16.

Suppose the radial component to be x_1, and component of stress perpendicular to the plate face to be y_1 when the crack starts. Then the crack will have a direction perpendicular to R_1. When the crack has reached a, the radial stress will have decreased to x_2 and the other component will have increased to y_2 and the crack will proceed in a direction perpendicular to the new resultant R_2 and so on for further stages c and d. In practice, the process is continuous and the crack takes a curved path of the form shown in Figs. 4 and 5.

As the crack nears the back face, a stage is reached at which the shear stress round the edge of the disc and perpendicular to the plate face exceeds the shear strength of the plate material. Therefore, the final separation of the disc occurs by shear failure and the bright sheared edge, shown at A in Figs. 4 and 5, is a characteristic feature of discs.

Cast armour does not display the laminar weakness which may occur in rolled armour, but may have a general low level of elongation under tensile stress due to intergranular weakness or inclusions. Thus, it is not uncommon to find cast armour which is quite as ductile as rolled armour when subject to shear stresses in the absence of tensile stress, but which is capable of little elongation in tension. This combination of properties tends to favour star cracking and back petalling, but may cause flaking if the internal weakness of the plate is too pronounced.

The way in which flaking occurs is best seen by analogy with discing. As in the case of rolled plate, annular cracks may be started in a cast plate by incipient plugging. Since there are no preferential planes of weakness, however, the cracks do not spread outwards parallel with the plate face. Instead, they proceed towards the back face in the same way as the edge crack on a disc in rolled plate, but with greater initial outward inclination due to the high component of stress parallel with the shot axis. When no secondary break-up occurs, this results in a flake of the form shown in Fig. 6, although, in practice, further break-up usually occurs due to the brittle nature of plates which fail in this way.

* A distinction has been made between discing and flaking (page 2 and Figs. 4, 5, & 6). This distinction has been found useful in practice, but the nomenclature is not uniform in the literature of the subject. The terms may sometimes be found to be interchanged. In particular, in reports of trials on tank armour it will be found that the term "flake" is applied in cases in which "disc" would be used according to the present definitions.

FIG. 13

SHOWING HOW THE FORWARD SHEARING TO FORM A BACK BULGE WILL SET UP TENSILE STRESSES OVER THE EDGE OF LAMINAR DEFECTS

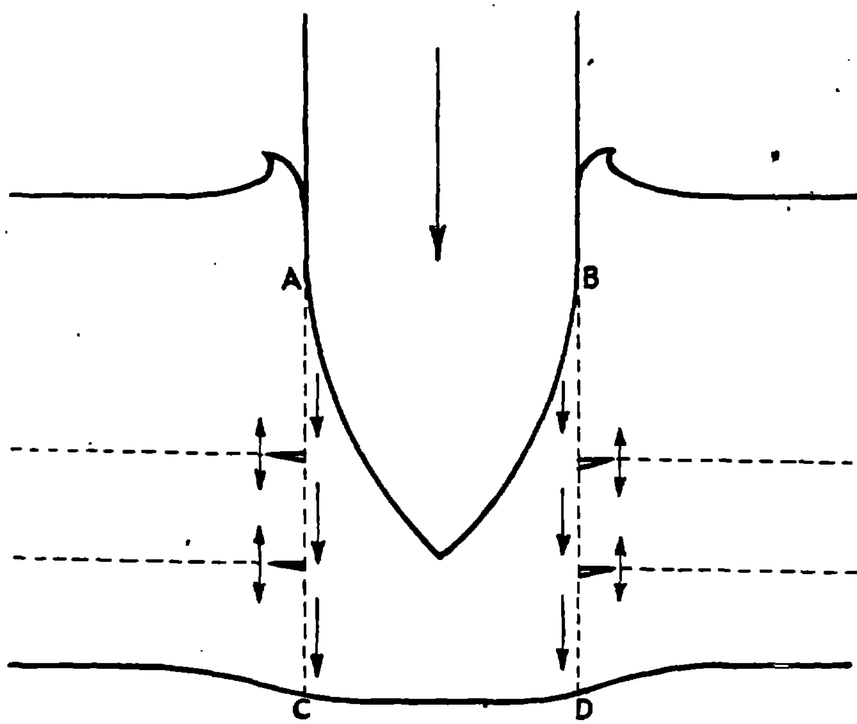
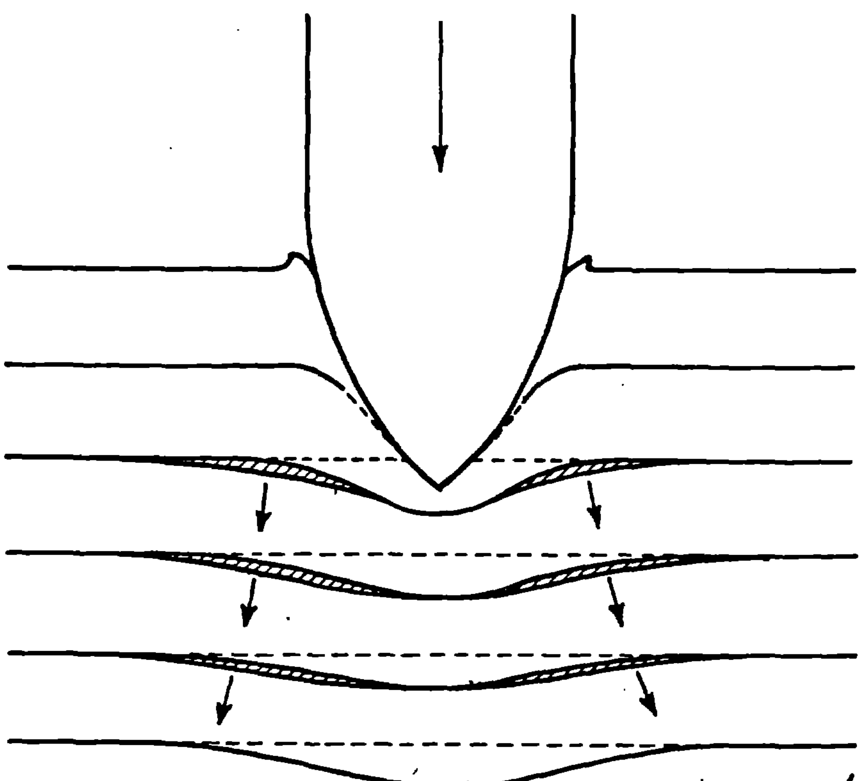


FIG. 14

SHOWING HOW A CLOSE PACKED PILE OF THIN PLATES WOULD DEFORM, TO INDICATE WHERE TENSILE STRESSES MIGHT BE EXPECTED IN A SOLID PLATE.

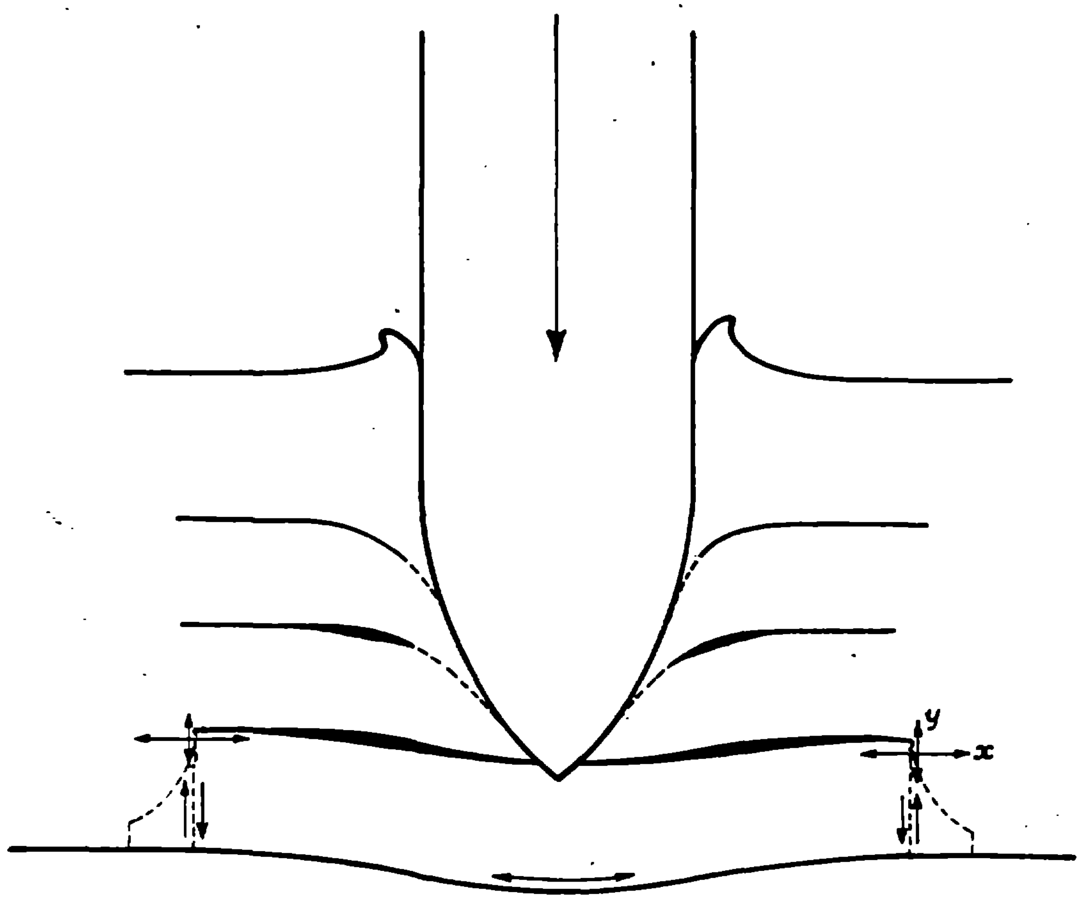


6·A

FIG. 15

SHOWING THE VARIOUS STRESSES WHICH EXIST AT THE EDGE OF A DISC, BEFORE THE CIRCUMFERENTIAL CRACK STARTS.

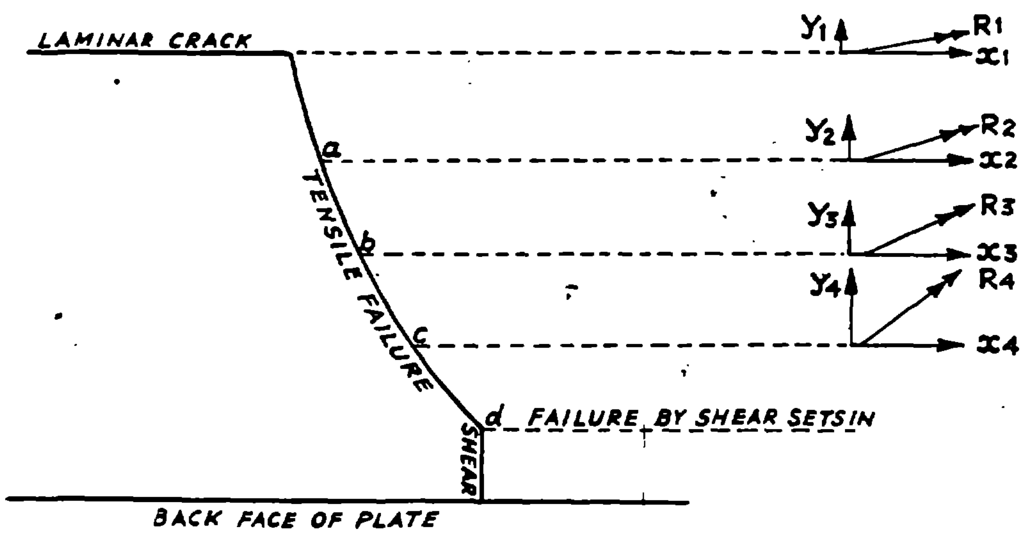
TENSILE STRESS x IS DUE TO BENDING OF THE DISC, AND y IS THE TENSILE STRESS OVER THE PERIPHERY OF THE LAMINAR CRACK.



6-B

FIG. 16.

THE DIRECTION OF THE RESULTANT TENSILE STRESS ACROSS THE CRACK AT ANY STAGE IS REPRESENTED BY R_n AND THE STRESSES DUE TO BENDING AND TO THE TENDENCY OF THE REMAINING UNCRACKED LAYER TO MOVE AWAY FROM THE LAYERS ALREADY CRACKED ARE REPRESENTED BY x AND y RESPECTIVELY. THE CRACK WILL TEND TO PROCEED AT RIGHT ANGLES TO THE RESULTANT STRESS AT EVERY STAGE.



6-C

3. Resistance to Penetration and Energy Absorption : Normal Attack.

There are two different ways in which a plate may defeat a shot. It may offer so much resistance to penetration that the shot is over-stressed and breaks up, or it may deform in such a way that all the shot energy is absorbed before perforation occurs. While these two effects are related, because energy absorption is the space integral of the resisting force, it does not follow that a plate which gives a high peak resistance to penetration gives the highest energy absorption on perforation.

In a later chapter, reference will be made to the determination of the load necessary for penetration, by means of static punching tests, and by retardation measurements on projectiles. It is interesting to note, however, that a useful estimate of the load necessary for penetration may be made by assuming that a plug of shot diameter and of full plate thickness is driven out of the plate. This would be expected to give a high value for the peak load, since some easier form of penetration might prevent the development of the shear stress necessary for plugging, but it is found to give loads corresponding closely with those determined by the the other methods. If this mechanism of penetration is assumed, the mean axial compressive stress P over the cross sectional area of the plug is given by the expression

$$\pi\frac{d^2}{4}P=\pi dtS$$

$$\text{or } P=\frac{4tS}{d}$$

where S is the shear strength of the plate material.

Thus, for a shot penetrating a plate having a shear strength of 40 tons per square inch, corresponding to a hardness around 300 B.H.N., the mean compressive stress over the plug section would be expected to have the following values for plates of the thicknesses shown :—

Plate thickness in calibres	P=Mean compressive stress over the cross sectional area of the plug	Observed values (static penetration)
	tons/sq. in.	
0·75	120	117
1·0	160	149
1·25	200	173
1·5	240	192
1·75	280	209
2·0	320	224

In the last column are shown approximate mean values taken from the static punching test results shown in Chapter 3.

As will be seen, the agreement for plates of moderate thicknesses is very good, while, as might be expected, the estimated values tend to be high for plate thicknesses around 2 calibres. This is due to the fact that appreciable shot penetration occurs before plugging starts in plates of this thickness, so that the early stages of static penetration are easier than they would be if a full thickness plug were formed. In the case of dynamic penetration, however, a considerable increase in plate resistance must be expected during the early stages of penetration, due to the inertia of the material displaced. This is particularly the case with thick plates which must always be attacked at high velocities if perforation is to be achieved. Therefore, the simple method of estimating the maximum thrust between plate and shot appears to be a good basis for design, and has been used for this purpose for some time. From the values of P, the mean compressive stress p^1 over any transverse section of the projectile at and behind the shoulder may be calculated by means of the formula

$$p^1=P\frac{W-w}{W}\bigg/\frac{4A}{\pi d^2}$$

where p^1 is the stress over the section
W is the projectile weight
w is the weight forward of the section
A is the area of the section.

3.1. *Energy for perforation.*

Not only does the assumption that perforation occurs by the formation of a plug of full plate thickness permit the calculation of a good approximation to the maximum compressive stress imposed upon the shot, but it also allows the calculation of the energy expended by the shot in perforating the plate. This leads to a penetration formula bearing some similarity to those already in use, and gives values for energy absorption of the right order. It is considered, however, that its real value is that it demonstrates the dependence of plate performance on a complex of mechanical properties more clearly than other lines of approach adopted so far.

Suppose the stress and strain to be uniform over cylindrical shells concentric with the shot axis, and let S and θ be the shear stress and shear strain, respectively, at a distance r from the axis. Then $S=F(r)$ where the form of F is not necessarily known, but is such that S decreases with increase in r.

If the stress-strain relationship for the plate material is represented by $\theta=f(S)$, if the suffix o relates to the values of S and θ at the yield point, and if the elastic work prior to yield is neglected, the work per unit volume in a thin shell of radius r is given by

$$e=\int_{\theta_0}^{\theta} S\,d\theta$$

$$e=\int_{S_0}^{S} Sf^1(S)dS=I(S)-I(S_0)=I[F(r)]-I(S_0)$$

$$\text{where } I=\int Sf^1(S)dS.$$

Since the volume of an elementary cylindrical shell is $2\pi\ r\ tdr$, the total strain energy E is given by

$$E=2\pi t\left[\int_{r_0}^{r_1} r\,I\,[F(r)]\,dr-\tfrac{1}{2}I(S_0)\,(r_1^2-r_0^2)\right]$$

where r_0 is the shot radius and r_1 is the radius outside which no plastic deformation occurs, provided deformation within the plug is neglected.

If the specific assumptions are made that :—

(i). The stress-strain curve is linear,

(ii). The stress is inversely proportional to the radial distance for values greater than r_0,

then $\theta=f(S)=\phi\dfrac{S-S_0}{S_F-S_0}$ where ϕ is the strain at the stress S_F which causes shear fracture

and $S=F(r)=S_F\dfrac{r_0}{r}$ at the moment of plug separation.

$$\text{Hence } I=\int Sf^1(S)\,dS=\frac{\phi}{S_F-S_0}\left(\frac{S^2}{2}\right)$$

$$\text{and } I\ [F(r)]=\frac{\phi}{2(S_F-S_0)}\left(\frac{S_F r_0}{r}\right)^2$$

$$E=2\pi t\left\{\phi\frac{S_F{}^2 r_0{}^2}{2(S_F-S_0)}\log_e\frac{r_1}{r_0}-\frac{\phi\,S_0{}^2}{4(S_F-S_0)}\,(r_1{}^2-r_0{}^2)\right\}$$

If the yield ratio $\dfrac{S_F}{S_0}=\lambda$, and since $r_0=\dfrac{d}{2}$

$$E=\frac{\pi t\phi d^2 S_0}{4}\left\{\frac{\lambda^2}{\lambda-1}\log\lambda-\frac{\lambda+1}{2}\right\}$$

Therefore, since a shot must have at least this energy for perforation, the relationship for bare perforation becomes

$$\tfrac{1}{2}\, Wv^2 = k\ t\ d^2$$

$$\text{or } \frac{Wv^2}{d^3} = C^1\ \frac{t}{d}$$

$$\text{where } C^1 = \frac{\pi\phi S_0}{2}\left(\frac{\lambda^2}{\lambda-1}\log\lambda - \frac{\lambda+1}{2}\right)\ \frac{2240\times 32{\cdot}2}{12}$$

$$= 9440\ \phi S_0\left(\frac{\lambda^2}{\lambda-1}\log\lambda - \frac{\lambda+1}{2}\right)$$

W is in lb.
v in f.s.
t and d in inches
and S_0 in tons per square inch.

If $\lambda = 1 + \mu$, and if $\mu < 1$ and terms of higher order than μ^2 are neglected, the formula may be written as $C^1 \triangle 9440\,\phi\, S_0\left(\mu + \frac{\mu^2}{3}\right)$

It is of interest to compare this relationship with the modified de Marre formula, which is in general use in this country, namely

$$\frac{Wv^2}{d_3} = C\left(\frac{t}{d}\right)^{1{\cdot}43}$$

The only difference in form is the absence of the index 1·43 in the formula derived from the assumption of perforation by plugging. The appearance of this feature in the empirical formula is, no doubt, accounted for by the fact that as plate thickness increases, there is a progressive increase in shot penetration before plugging starts. Although this has the effect of reducing the load necessary to cause plugging, it has an even greater effect corresponding to an increase of ϕ in equation (4) above, so that $\frac{Wv^2}{d^3}$ will tend to increase more rapidly than $\frac{t}{d}$.

Although no reliable data defining the behaviour of armour plate steels under shear stress are available, reasonable assumptions lead to values of C^1 of the the same order as C.

Thus, if it is assumed that

$$\left.\begin{array}{l} S_0 = 30 \text{ tons per square inch} \\ S_P = 40 \text{ tons per square inch} \end{array}\right\}\lambda = 1{\cdot}33$$

and $\phi = 5$ (corresponding to a reduction of area in tension of 65 per cent.)
then $C^1 \triangle 0{\cdot}52\times 10^6$.

This value of C^1 is calculated on the assumption that the perforation occurs by the formation of a plug through the full plate thickness, without any prior penetration of the shot head. Therefore, it is to be expected that it will agree best with observed values for blunt headed shot attacking plates of 1 calibre or less in thickness, since the assumed conditions are then more closely satisfied. For good ogival headed shot, of 1·4 c.r.h., observed values of C are around 1×10^6, while for flat headed shot values of C around $0{\cdot}5\times 10^6$ are observed.

It is of interest to note also, that if plugging occurs in the manner supposed, the lip formed round the hole, or the back face, should have a width

$$\frac{d}{2}\left(\lambda - 1\right)$$

Thus, if $\lambda = 1{\cdot}33$, the lip width is $0{\cdot}17\ d$, and, as will be seen from Fig. 3, the lip width is of this order with plates of about 1 calibre thickness.

As already mentioned, the value of this derivation of a penetration formula lies, not in its use to predict shot performance, for which existing empirical formulæ are better, but in the demonstration of the way in which plate performance depends directly upon yield stress in shear, and strain at fracture, and also depends in a more complex manner upon yield ratio.

4. Angle Attack.

4.1. *The effect on hole and plug form.*

So far, in this chapter, only normal attack of plates has been described. Under Service conditions, however, normal attack of armour is seldom possible and it has now become customary to carry out development trials, to test plate or shot performance, with angles of attack of 30 degrees or more. This section will be devoted to a description of the way in which the mode of deformation and failure of plates is affected by increase in the angle of attack. In this section, unless otherwise mentioned, shot with a 1·4 c.r.h. form are considered.

At small angles of attack, little change occurs in the mode of plate failure. The hole in the plate usually has a direction intermediate between the direction of attack and the normal, and the other main characteristics such as plugging, discing, back petalling, etc., remain the same. At angles of around 20 degrees to 30 degrees, however, some differences become apparent.

The plate thickness which can be defeated by a given shot decreases with increase in angle of attack. As a result, failure by plugging tends to be more common than back petalling at angles of attack of 30 degrees or more, in plates free from serious laminar weakness, since at these angles the attack is, in practice, likely to be made against relatively thin plates and such plates tend to plug. Moreover, when plugging occurs at these angles it is usually found that the plug is of the shape shown in Fig. 17. It is roughly elliptical in section, with a minor axis equal to the shot calibre, and with a major axis slightly greater and lying in the plane of attack. The manner in which such a plug forms is shown diagrammatically in Fig. 18. As this shows, the plug shears out over surfaces which are roughly perpendicular to the plate face, but which show a curvature due to the tendency of the plug to have a hingeing action about the end furthest from the shot point.

The fact that the plug forms by shearing in a direction roughly perpendicular to the plate face is of interest, since it might therefore be expected that only the kinetic energy associated with the normal component of velocity of the shot would be effective. This does, in fact, appear to be the case, since a penetration formula of the form :—

$$\frac{WV^2 \cos^2 \theta}{d^3} = C\left(\frac{t}{d}\right)^{1\cdot 43}$$

where θ is the angle of attack gives the best agreement with observed results for angles from 0 degree to 30 degrees.

As the angle is increased further, the major axis of the plug tends to increase in length, and it is found that the shot performance falls off even more rapidly with θ than is suggested by the formula above. This may be due to the fact that the sheared surface of the plug is increased in area by the increase in the major axis.

At angles of the order of 45 degrees to 55 degrees, perforation of the plate appears to occur in two stages, as illustrated in Fig. 19. First, a plug is driven out of the plate as shown in Fig. 19(a), and then a wedge shaped section of plate is removed from the side of the hole, as shown in Fig. 19(b). The evidence supporting this conclusion is that shots striking at a velocity slightly below the critical velocity produce only the first stage of damage and do not pass through the plate, while, after the plate has been completely defeated the wedge shaped piece of plate already referred to may often be recovered. Since this two-stage penetration obviously involves uneconomical expenditure of energy, it is not surprising that shot performance at these angles is relatively poor.

At even higher angles, unless the plate is very thin, shots ricochet without perforation. The angle at which this occurs can be altered to some extent by change of head shape, as also can the striking energy necessary for perforation at smaller angles. This will be discussed further in a later section.

4.2. *Reaction on the shot.*

The reaction on the shot is naturally more complex in the case of angle attack than when the shot strikes the plate normally. When the ogive enters the plate, it first of all experiences a greater thrust on the side away from the normal, both because a greater area is in contact with the plate, and because displacement of plate material on that side of the head is more difficult [Fig. 20(a)]. Consequently, the shot experiences a turning moment which causes it to swing away from the normal.

As penetration proceeds, a stage is reached at which the plug begins to shear out, and as a result the thrust of the side of the ogive remote from the normal is decreased, while that on the other side of the head becomes relatively high [Fig. 20 (b)]. Therefore, at this stage the shot starts to swing back towards the normal and continues to do so as the shot moves forward until rotation in stopped by the shot body striking the side of the hole, as illustrated in Fig. 20(c).

It is not at present possible to determine the stress system set up in a shot during angle attack with any certainty, but it may be of interest and of some value to discuss what might be expected.

FIG. 17.
Two views of plug produced by attack at 30 degs.

10-A

FIG. 18.

SHOWING POSITION OF THE SHEAR FRACTURE CAUSING PLUG FORMATION IN 30° ATTACK.

TENSILE FRACTURE FREQUENTLY OCCURS AT A.B. DUE TO HINGING OF THE PLUG.

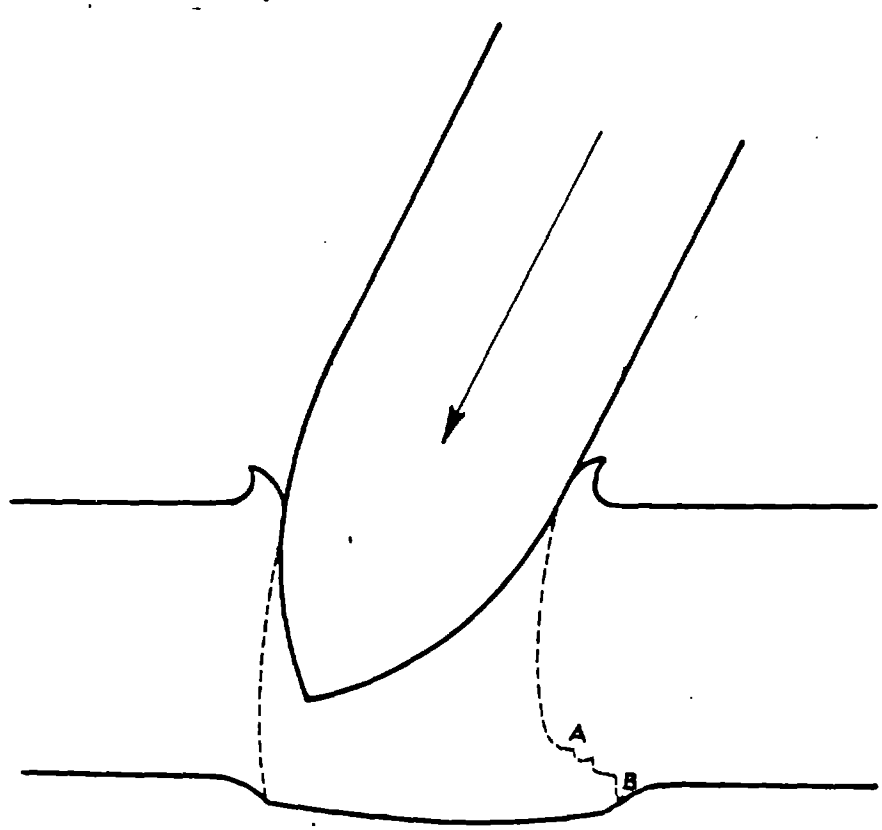
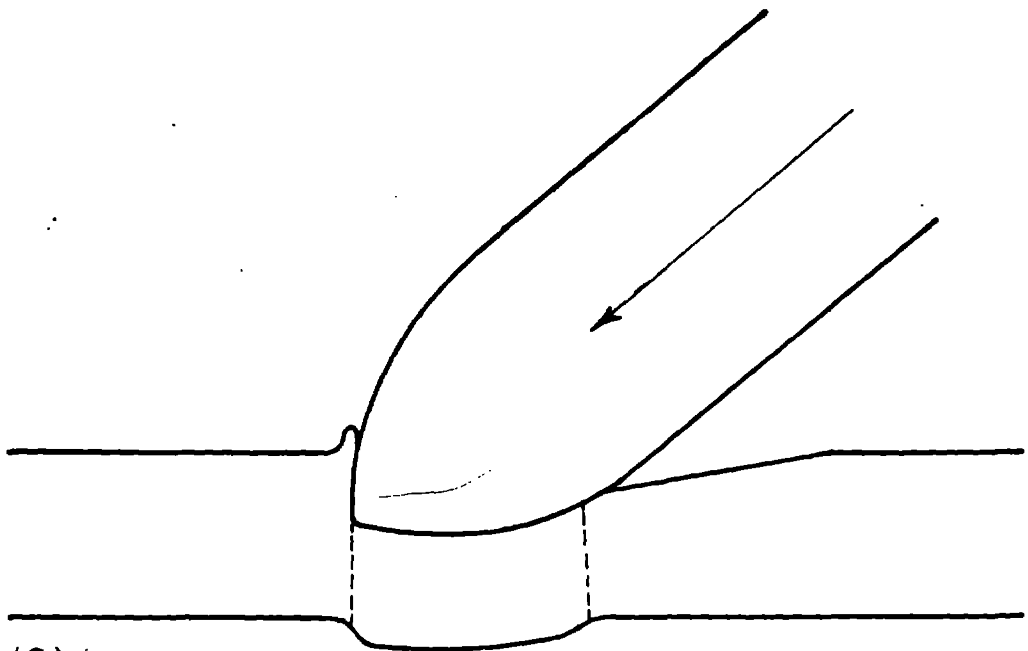
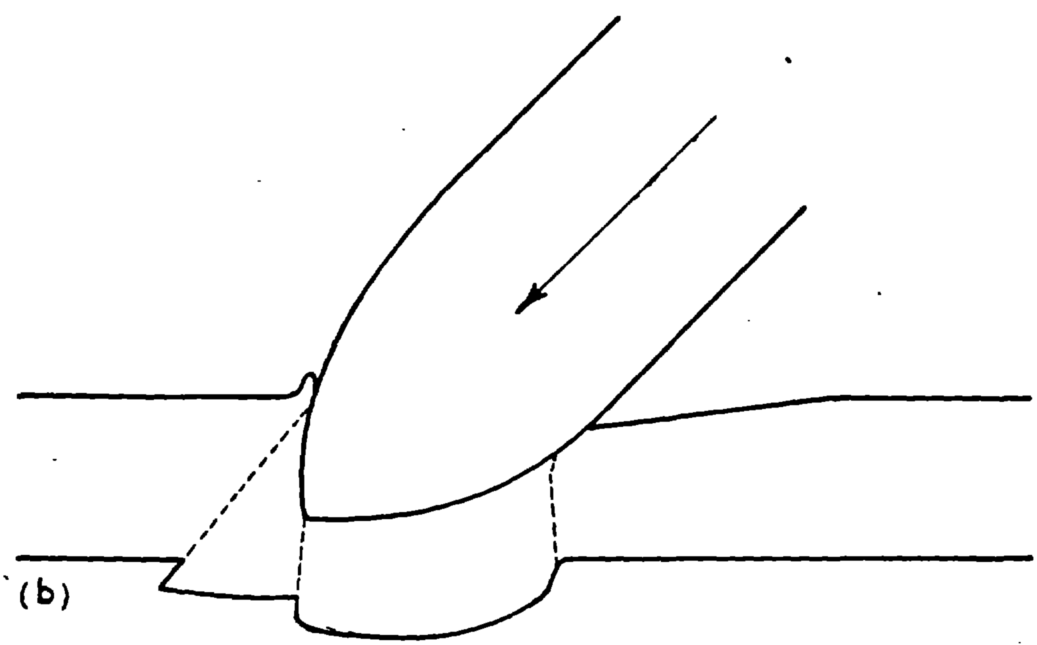


FIG. 19.

TWO STAGES OF PENETRATION AT ANGLES OF 45° TO 55°



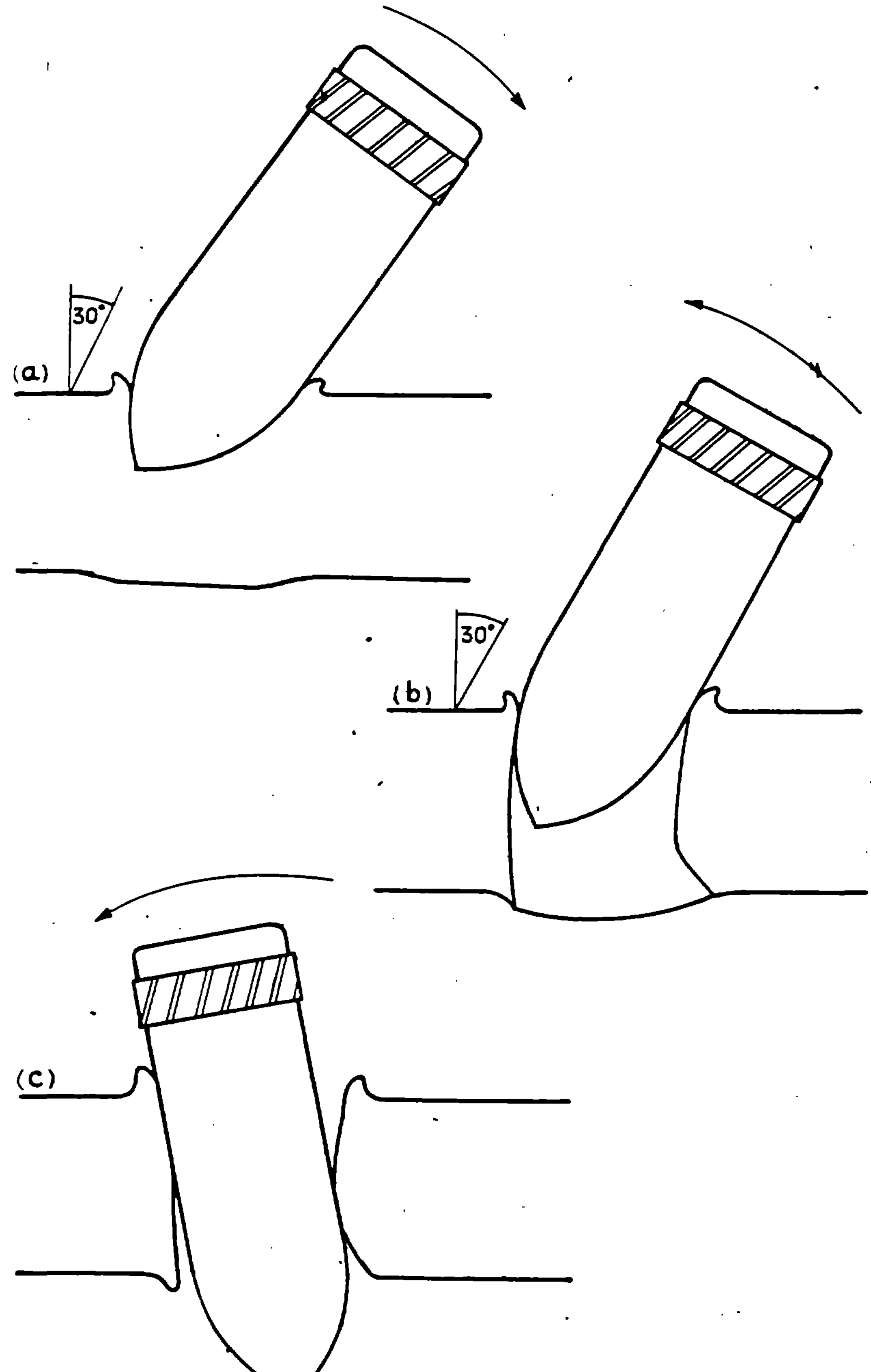
(a)



(b)

FIG. 20.

SHOWING TURNING OF THE SHOT DURING PERFORATION IN ANGLE ATTACK.



Consider the case of a shot which has penetrated a plate at 30 degrees, to the point where shear fracture to form a plug has not quite started, and ignore, for the sake of simplicity, the small turn away from the normal which would already have occurred. Now consider the resultant forces on the two parts of the head on either side of a plane through the shot axis and perpendicular to the plane of attack. Let these be referred to as sides A and B, as in Fig. 21. Then it is to be expected that the resultant force on side A will act approximately along LM, while that on side B will act along PQ. Moreover, since the area of head in contact with the plate is greater on side B, also because the plate on side B is less free to deform, the magnitude of the resultant thrust on side B will be greater than on side A. Hence, the resultant thrust on the head as a whole might be expected to act along OD, which will not coincide with the axis of the shot, or pass through the CG of the shot, nor is it likely to be perpendicular to the plane of the plate face FG. As a result, there will be a turning moment on the shot, of magnitude OR × CD, and there will be a shear stress over the plane FG.

The compressive stress perpendicular to the face FG will not be uniform, but will tend to be highest on side B. Further back in the shot body, however, this state of affairs will be reversed, and the compressive stress on side A will be greater than on side B, due to the added effect of bending stresses. Even so, however, it is doubtful whether this produces a tensile stress on side B anywhere in the forward part of the shot body. It appears, therefore, that at angles of attack of 30 degrees or so, up to the stage of plug formation, the main effect of angle is to give a non-uniform distribution of compressive stress in the forward part of the shot. This may have the effect of increasing the maximum compressive stress set up in the shot, for a given thickness of plate, but it is unlikely that tensile stresses are produced, unless the shot deforms plastically.

When shear fracture occurs, so that the plug is free to move, the resultant force on side B will be greatly reduced. The compressive stress across the face FG will then be small and the shear stress over this face will become relatively large. Also, although the thrust along LM may be small compared with the earlier value of OR, it is likely to have a greater turning moment on the shot, due to the greater magnitude of CE as compared with CD. Therefore, when the plug separates, the shot is likely to suffer a greater angular acceleration towards the normal than the original acceleration away from it. Moreover, since the axial compressive stresses will largely disappear, the bending stresses are likely to give an appreciable tensile stress in the forward part of the shot body on side A, which may possibly cause tensile fracture in some cases.

Finally, when the forward part of the shot passes through the hole formed in the plate, the angular velocity of the shot will be destroyed rapidly by impact of the shot body against the sides of the hole, as shown in Fig. 20 (c). The bending stresses imposed by this sudden retardation are likely to be greater than those associated with either the initial turn away from the normal or subsequent swing back. It is probable, too, that the tensile stresses produced in the shot body on side A by this violent angular retardation are responsible for many of the observed cases of shot break-up at angles of 30 degrees or so.

To reduce the tendency of A.P. projectiles to fail under the influence of tensile stresses produced by the bending moments set up during angle attack, it is usual to reduce the hardness of the body progressively from shoulder to base. As explained in Chapter 3, this can be done in such a way as to match the fall off in axial compressive stress, while giving an increased resistance to failure under tensile stress.

4.3. *The effect of shot head shape.*

Head shape has a pronounced effect on shot performance. So far, in this Chapter, consideration has been limited to shot of around 1·4 c.r.h. form, since this has been generally adopted as the most satisfactory compromise for angles of attack around 30 degrees. However, there is no one head form which is best for all conditions of attack and it is of interest to consider the effect of changes in head shape on shot performance against various thicknesses of plate and at various angles. In general, these effects may be explained quite satisfactorily in the light of the mechanism of penetration already postulated.

The observed facts are that blunt heads perform better against thin plates, while longer heads give better performance against thick plates at normal, but not at angles.

With plates of around one calibre or less in thickness, penetration tends to occur by the formation of a plug of full plate thickness. Therefore, energy expended in driving a pointed head into the plate, before a calibre diameter plug can form, is largely wasted. Hence it would be expected that blunt headed shot would succeed at lower striking velocities. As already stated, this is observed to be the case and flat fronted shot will succeed against thin plates, with striking energy only about half that necessary with ogival headed shot. As plate thickness is increased, however, the load necessary to produce a plug of full plate thickness becomes greater than the force necessary to cause penetration by radial displacement of plate material and, as a result, it is found that more pointed shot, which favour this mode of penetration, perform better than blunt shot. Moreover, as plate thickness increases, the shock loading produced in blunt headed shot becomes so severe that the shot break up. Thus, for normal attack of plates of around two calibres thickness, head forms of 2 c.r.h. or even more pointed forms are found to be better than a 1·4 c.r.h. shot.

As angle of attack is increased, there is a progressive tendency to favour short head forms. This is so for two main reasons. Firstly, the turning moments exerted on a shot during angle attack are increased by increase in head length. Secondly, there must, in practice, be a reduction in plate thickness attacked, as angle is increased, if success is to be achieved.

At angles of 60 degrees or so, ogival headed projectiles fail against quite thin plates, due to the fact that the turning moment on the shot is sufficient to cause ricochet in the manner shown in Figs. 22 and 24. This tendency to ricochet can be reduced and the angle at which it occurs can be increased by the adoption of a suitable flat fronted head form, such as that shown in Fig. 23 (b).

Consider the case of a flat ended cylindrical projectile striking a plate at a large angle as illustrated in Fig. 23 (a). Then, as the edge of the flat front penetrates the plate, it is to be expected that there will be a reaction of the plate on the shot approximately in the direction AB as shown. If the shot length is not more than about 3 to 4 calibres, then the reaction AB is likely to fall between the plate and the C. of G. of the shot, so that there will be an overturning moment rather than a couple tending to turn the base of the shot down on to the plate. Moreover, by suitable adjustment of the size of the flat front, in the manner shown in Fig. 23 (b), it is found possible to prevent either skidding or toppling of the projectile and so make penetration possible at higher angles.

Moreover, such shaping of the head tends to make the shot penetrate in such a manner that it uses all its kinetic energy, rather than only that associated with the normal component, and so improves performance even at rather lower angles where ricochet would not occur.

5. FAILURES OF SHOT.

In this section the failures of armour piercing shot will be dealt with first and the more difficult problem of shell failure will be considered later.

It is very common for A.P. shot to break up on passing through a plate. Provided this break-up does not raise the critical velocity at which the shot is able to defeat a given thickness of plate, and provided a reasonable proportion of the shot passes through the plate, this is not considered to be a disadvantage. In fact, a spray of fragments of shot may be more lethal than one unbroken projectile.

Shot break-up, with no appreciable effect on the critical velocity for penetration, may occur with normal or angle attack, but becomes more common as the angle of attack increases. There seems little doubt that the most usual cause of shot break-up, with good quality shot, is the imposition of bending stresses during penetration at angles other than normal. Since there is usually no appreciable change in critical velocity, it can only be assumed that the shot break-up occurs when the plate has been holed or when hole formation is nearing completion. It has already been suggested that the bending stresses imposed upon the shot by striking the sides of the hole are the most severe of those imposed during attack, at angles up to 30 degrees or so, and it appears that these are the usual causes of break-up.

Another cause of break up, which might be expected to operate during normal attack, as well as during angle attack, is the formation of a tensile stress wave by a sudden release of compressive stresses in the shot at the time when the plug separates. Such a mechanism might be expected to operate even more effectively if a shot were fired into a thick plate at a velocity too low for success, since the compressive stress would be released very suddenly when the shot came to rest. Therefore, the fact that shot often rebound broken from a plate which they will penetrate unbroken at a slightly higher velocity lends support to this explanation of shot break up.

So far, reference has been made only to forms of shot break-up which do not affect performance. There are, however, other forms of shot break-up, and of shot deformation, which occur with poor shot or under severe conditions of attack, and which have an adverse affect on performance. The most serious of these is shatter.

FIG. 21.

SHOWING THE NATURE OF THE FORCES ACTING ON THE HEAD OF A SHOT DURING PENETRATION.

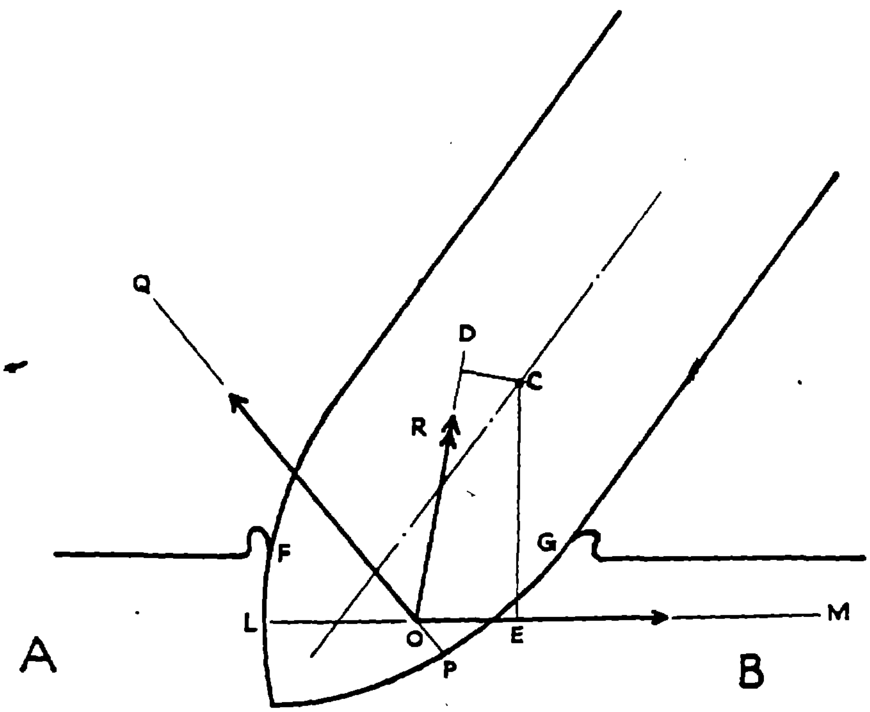


FIG. 22.

STAGES IN RICOCHET.

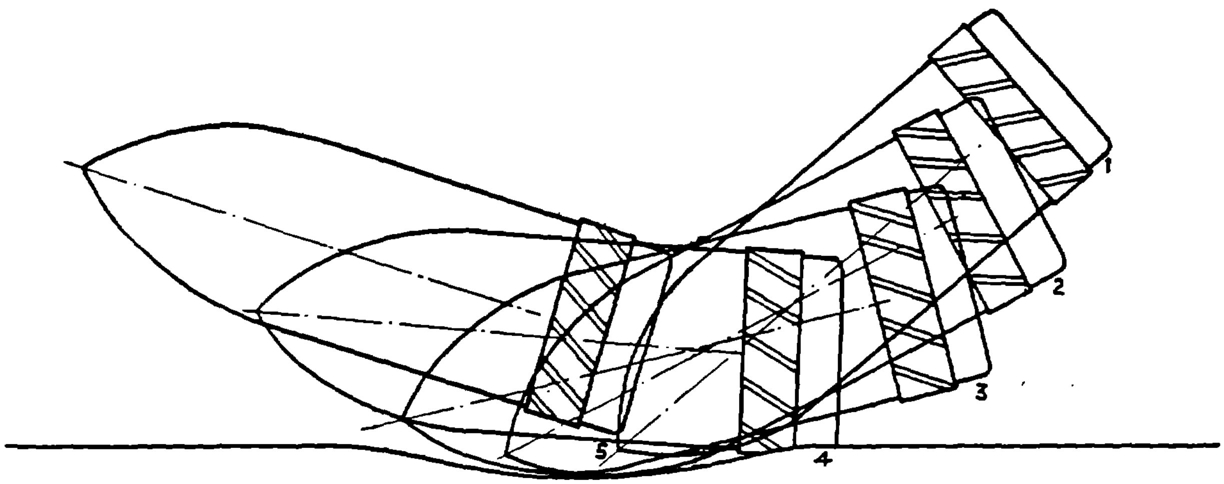
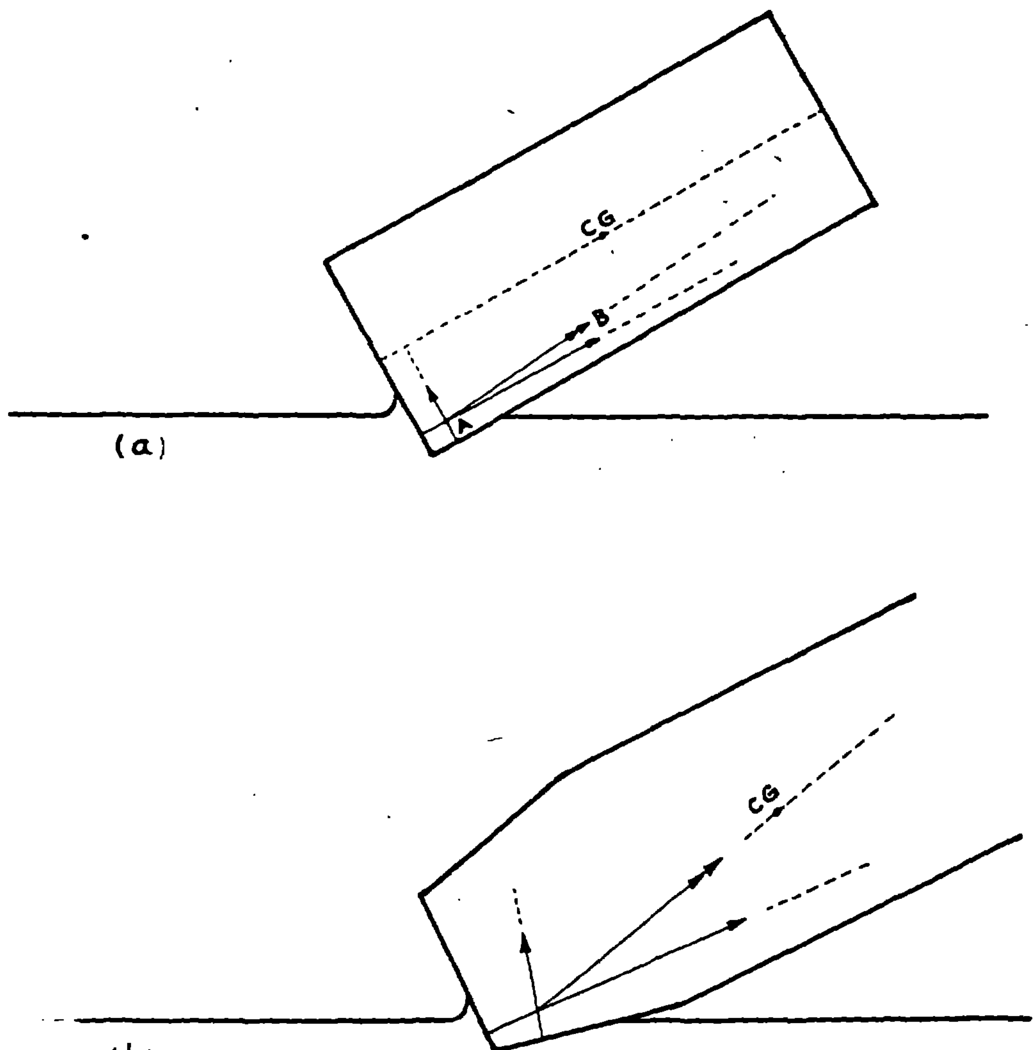


FIG. 23.

EFFECT OF HEAD FORM ON RICOCHET.



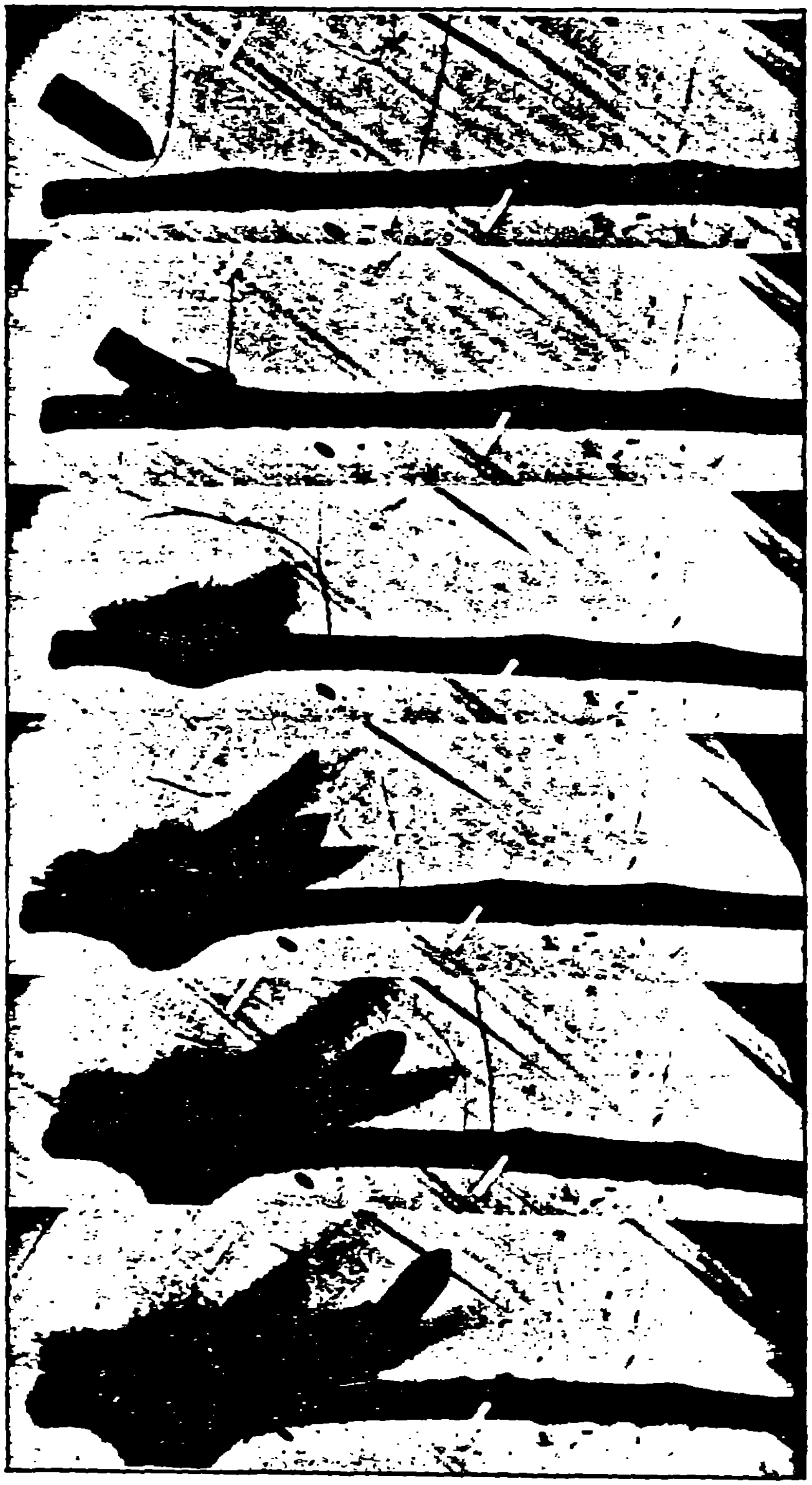


FIG. 24.
Multiple spark photographs of the ricochet of a model shot.

12-0

5.1. *The occurrence of shatter failure.*

In the early part of the last war, shatter of armour piercing shot presented a very serious problem. It was found that there was a limit to the extent to which performance could be increased by increasing shot velocity, because, above some limiting velocity, even good quality shot were found to suffer break up at such an early stage of penetration that there was a marked adverse effect on performance. This form of failure was known as " shatter " and use of the term still persists even though it is now thought that it suggests a false explanation of the phenomenon.

Against thick plate at normal, shot usually start to shatter at velocities around 2600 f.s. As plate thickness is reduced, below about two calibres, the velocity at which shatter occurs tends to increase, while it falls fairly rapidly with increase in angle of attack. At 30 degrees, for example, shatter may occur against thicker plates at velocities as low as 2200—2400 f.s.

Against relatively thin plates, for which the critical velocity of the shot is well below the shatter velocity, shatter does not matter much. All that happens is that, when the shatter velocity is reached, the hole in the plate tends to be larger in size than the shot section and of irregular shape, while the shot emerges from the plate in many small fragments instead of being whole or in a few large pieces. Against thick plates, on the other hand, the shatter velocity is less than that necessary to defeat the plate. It is then found that instead of a shot producing an impression of its own shape in the plate, it produces a shallow impression, smooth round the outside and rough in the middle. These impressions, like most of the forms of plate damage produced by A.P. shot attack, are quite consistent in appearance, and a section of a typical shatter impression is shown diagramatically in Fig. 25. When this type of shatter damage occurs the shot fail to penetrate the plate at velocities at which they would normally be expected to succeed. Only when the shot velocity is increased considerably is it possible to hole the plate, and then the hole has a ragged appearance characteristic of success with shatter.

Against plates of a certain limited range of thickness, for which the shatter velocity is not much greater than the critical velocity for success without shatter, it is found that shot will succeed without shatter over this narrow range of velocities. At the shatter velocity the shot fail to penetrate, and only by a considerable increase in velocity does it again become possible to hole the plate.

Fig. 26 presents the results of a trial carried out with 2-inch A.P. shot against several thicknesses of plate. This shows how shatter affects performance.

5.2. *The mechanism of shatter failure.*

Recovery of fragments of shattered shot shows that shatter is a failure of the forward end of the shot, since the rear half of the shot is quite frequently recovered whole, while the forward end is usually broken into many pieces. Also, the break up of the forward end of the shot results predominantly from brittle tensile fracture.

When a shot fails to perforate a plate, due to shatter, the impression produced in the plate is normally quite shallow, and is usually rather less than the shot head length in depth. This proves that the break up must occur at a very early stage of penetration, which, in turn, confirms the view that a shatter is essentially a head failure.

The tensile stresses which cause shatter of the head might be a direct result of the impact, although it is not obvious how they would arise, or they might result from plastic deformation of the shot by the axial compressive stresses. The second of these possibilities is considered to be the true cause of shatter.

Consider first the case of a shot striking the plate normally. It has already been mentioned that, under these conditions, both retardation observations on shot and static punching tests show that the compressive stresses around the shoulder rise as the head becomes embedded in the plate. If these stresses are higher than the compressive yield stress of the shot in the shoulder region, then set-up of the shot is to be expected. This will establish hoop tensile strains of considerable magnitude, and since hard shot steel has virtually no elongation in tension, longitudinal cracking of the shot will result. After this had happened, complete break-up of the forward end of the shot is likely to follow, with the results illustrated in Fig. 27. Fragments of the shattered head are embedded in the centre of the impression, giving the characteristic jagged surface, while mushrooming of the rest of the shot scoops off the surrounding part of the plate and leaves it with a smooth, sheared appearance.

In order that the dependence of shatter upon striking velocity and plate thickness may be understood, it is necessary to take into account the inertia of the plate material displaced by the shot nose.

The precise magnitude of this effect would be difficult to establish, due to the complex manner in which plate material is displaced, but as a means of showing the order of the effect it is not unreasonable to suppose that a volume of plate equal to the volume of the shot head is given a velocity of the order of half that of the shot during a penetration of one calibre depth. Then, if the shot is assumed to impart this energy uniformly during one calibre of travel, the resulting pressure over the shoulder section would be

$$\frac{v^2 \rho V}{8d} \times \frac{4}{\pi d^2} \times 12 \text{ poundals per square inch,}$$

where V is the volume of the shot head in cubic inches (which is $0 \cdot 48\, d^3$ in the case of a 1·4 c.r.h. shot) and ρ is the density of the plate in lb./cubic inches = 0·283.

Hence, the increase in axial pressure which might be expected to result from inertia is of the order

$$\frac{6 \times 0 \cdot 48 \times 0 \cdot 283\ v^2}{\pi} \text{ poundals per square inch}$$

or

$$\frac{6}{32 \times 2240} \times 0 \cdot 48 \times \frac{0 \cdot 283\ v^2}{\pi} = 3 \cdot 62 \times 10^{-6}\ v^2 \text{ tons per square inch.}$$

Thus, for a striking velocity of 2500 f.s., the pressure due to inertia alone might be expected to be around 25 tons per square inch.

It will be seen, therefore, that the axial pressure set up in a shot will be appreciably affected by the inertia of the plate material, and this effect increases rapidly with increase in striking velocity v, since it varies as v^2. Moreover, it has already been shown that the compressive stress set up in the shot will increase with plate thickness, up to a thickness of 2 calibres or so, above which the initial stages of penetration are not affected by the presence of the back face. Hence, if shatter results from set-up of the shot under the influence of the axial compressive stress, this form of failure would be expected to occur at velocities above some critical value, and this shatter velocity would be expected to fall with increase in plate thickness, up to thicknesses of the order of 2 calibres, and then remain constant. This is, in fact, observed.

At angles other than normal, up to 30 degrees or so at least, the mechanism of shatter is considered to be essentially the same. As has already been pointed out, however, the compressive stresses in the region of the shot shoulder are likely to be non-uniformly distributed and to have a higher maximum value. Hence, shatter tends to occur at lower velocities.

The foregoing hypothesis as to the mechanism of shatter leads to the conclusion that the tendency of shot to shatter would be decreased by increasing their shoulder hardness and compressive strength. On the other hand, if shatter were due to the direct establishment of tensile stresses on impact, without prior set-up of the shot, it would be expected that the tendency to shatter would be reduced by reducing shot hardness and so increasing tensile strength and ductility. It is found that increasing hardness reduces the tendency to shatter, which supports the hypothesis presented.

6. Face-Hardened Plate.

In order to reduce the effectiveness of A.P. projectiles, by causing early break-up, armour is sometimes face-hardened, either by flame hardening or by carburizing. Such armour is very commonly used for the main armour belt of warships, and less often for armoured fighting vehicles.

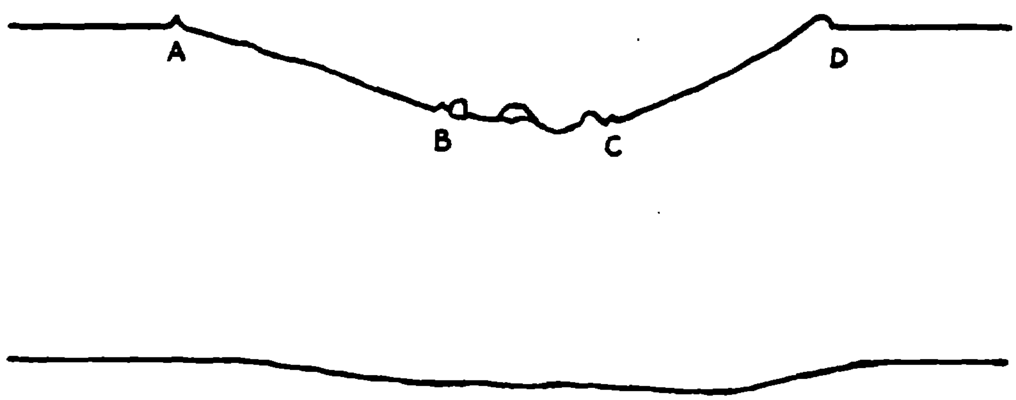
The hardened layer is usually around $\frac{1}{4}$ to $\frac{1}{3}$ of the total plate thickness, and has a hardness of the same order as that of the usual steel armour piercing projectiles. When fired against such armour, steel projectiles fail in much the same way as when they shatter against homogeneous plate, and it is likely that the mechanism is much the same. Due to the very high yield point of the hardened layer of the plate, the compressive stresses in the shot would be expected to reach high values at an earlier stage of penetration and at a point further forward in the head. Hence, it appears probable that the failure occurs at an even earlier stage in penetration than normal shatter, and the effect in shot performance is even more marked.

7. Cap Action.

When face-hardened plate was first used for warships it was found that shell could be prevented from breaking up on the plate face by fitting a steel cap over the shell head. In the first place, for attack of plate at angles near the normal, these caps were of soft steel. Later for attack at larger angles, caps with hardened fronts were used. These were found to offset the effect of the hard face very satisfactorily. Finally, when shatter was experienced with anti-tank A.P. projectiles, this trouble also was overcome by fitting shot with piercing caps.

FIG. 25.

SECTION OF A TYPICAL SHATTER DENT
SMOOTH SHEARED SURFACE AB AND CD
WITH JAGGED AREA AND EMBEDDED
SHOT FRAGMENTS IN CENTRE.



14-A

FIG. 26. A PERFORMANCE CHART SHOWING REGIONS OF SHATTER.

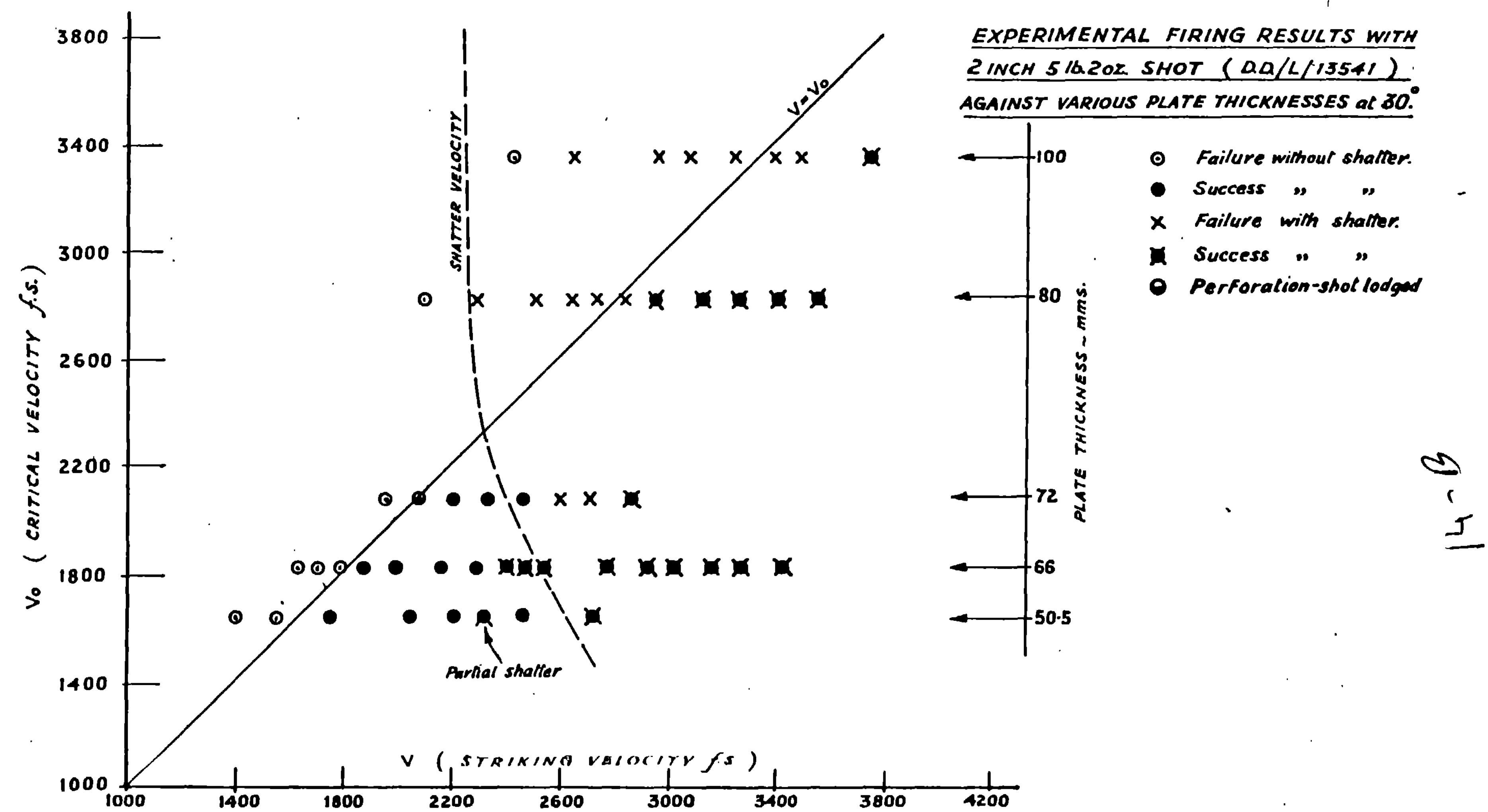
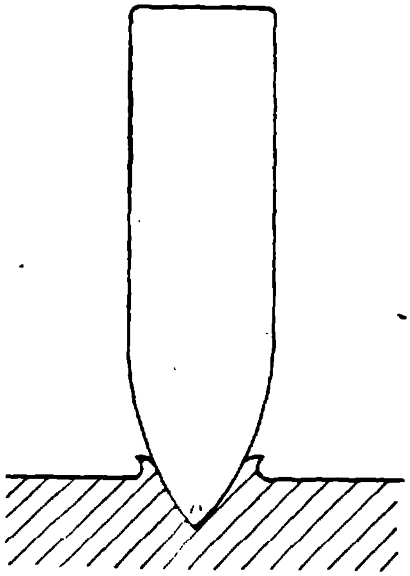
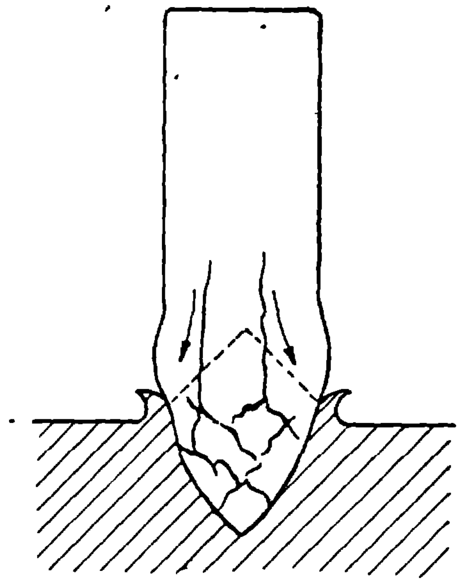


FIG. 27.

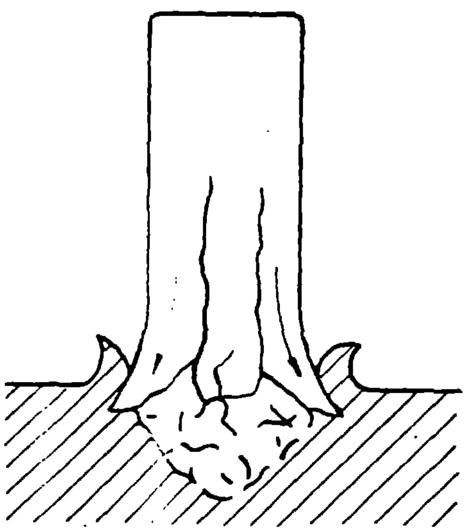
THE MECHANISM OF SHATTER.



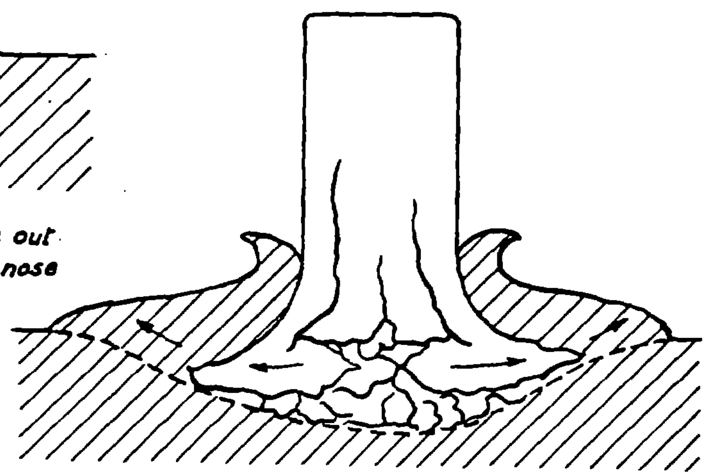
(a) Shot strikes and starts to penetrate.



(b) Shot sets up by shear over conical surfaces, then splits longitudinally. Cracks extend into the head.



(c) Shot bursts and mushrooms out at shoulder. Fragments of nose begin to spread.



(d) Further mushrooming of shot scoops out a shallow impression as indicated by dotted line. Fragments of nose left embedded. Base of shot complete, but with longitudinal splits.

14-C

In spite of the successful use of piercing caps, the manner in which they operate has never been fully explained. When used against face-hardened plate, it appears probable that the cap operates by cracking up the hardened face, by virtue of its own energy, and does not transmit much shock to the shell head due to its own break-up. It may be, also, that the skirt of the cap, which is normally left soft on large shell, gives some support to the head of the projectile during the critical early stages of penetration.

The action of caps used against homogeneous armour to prevent shatter is, perhaps, even more difficult to explain. It has been found that the effectiveness of the cap is not very sensitive to variations in shape or hardness, and it appears probable that the main effect of the cap is to overcome some inertia of the plate material, by expending its own energy while breaking up, and so reducing the load on the projectile itself. This is largely an unsupported supposition, however, and further investigation will be necessary to elucidate the mechanism of cap action.

8. FAILURES OF ARMOUR PIERCING SHELL.

Armour piercing shell suffer much the same types of failure as A.P. shot, with the main difference that forms of break-up and damage which are unimportant with shot, so long as they do not raise the critical velocity, are important in the case of shell because they prevent satisfactory detonation of the filling.

Armour piercing shell, particularly the larger ones, are usually treated to a lower hardness level than shot, although the general form of the hardness gradient from nose to base is much the same. Some reduction in hardness would, in any case, be necessitated by the fact that steels of lower intrinsic hardness are used and by the tendency of fully hardened large shell to crack. Quite apart from this, however, lower hardness levels have been adopted to improve the resistance of the shell to failure under the influence of bending stresses and side blows. As already mentioned, there is some doubt as to whether appreciable tensile stresses are set up in the forward part of a projectile when penetrating plates at angles up to 30 degrees or so unless plastic deformation in compression occurs first. Therefore, there is some doubt as to whether the hardness of large shell has not been lowered too far.

8.1. *Head failures of shell.*

Piercing shell suffer head failures akin to shatter failures of shot, except that, because they generally have lower hardness and greater ductility, more plastic deformation precedes fracture, the break-up is not so complete, and brittle tensile fracture is not as predominant or obvious. Nevertheless, it appears probable that the mechanism of failure is similar and that the basic cause of failure is set-up under the influence of axial compressive stresses.

In this connection, it is of interest to see whether the compressive stresses to be expected are of the right magnitude to cause set-up. Large piercing shell, as at present produced, will normally just defeat a 1 calibre thick face-hardened plate at normal, or a 0·85 calibre thickness plate at 30 degrees. Consider the case of normal attack, and suppose for the present that the shell cap just smashes the hardened face, leaving the shell to defeat the remaining thickness of plate (approximately 0·8 calibre). Then, if the plate has the normal hardness level around 250—260 B.H.N. with a shear strength of around 33 tons per square inch, the pressure set up in the shoulder section of the shell would be expected to be around 80 tons per square inch, since the head is about a quarter of the total shot weight. When attacking the thinner plate at 30 degrees, compressive stresses of the same order would be expected.

Since many heavy shell have shoulder hardnesses around 360 B.H.N., with a corresponding yield in comparison around 65—70 tons per square inch, these must suffer some set-up when fired against the targets considered. This set-up is limited by the fact that the period of overstressing is short and the relative movement of parts of the shell on either side of the overstressed section is limited by their inertia. Therefore, provided the shell has adequate transverse ductility, it will not break up, although some energy will be wasted in setting up the shell with a consequent raising of the critical velocity. Production of successful shell of this type requires a careful balance between shell set-up, controlled by compressive strength, and transverse tensile elongation.

In the light of the foregoing argument, it might be concluded that shell should be made harder in the shoulder region. It does not follow, however, that the likelihood of shell break-up will be reduced progressively by increase in shoulder hardness, since the reduction in the ability of the steel to endure tensile strain may be reduced more rapidly than the set-up of of the shell. On the other hand, if the shell hardness in the shoulder region is increased to a level where the compressive strength is high enough to prevent plastic deformation, no hoop tensile stresses will be produced and low tensile elongation may no longer matter. It would appear that a compressive yield of around 80 to 85 tons per square inch is necessary to ensure this, or a shoulder hardness of around 450 B.H.N. It is, in fact, found that shell of this mean shoulder hardness will perform well, if produced by methods which give a surface hardness rather lower than the hardness over the middle of the transverse section. Shell of the same mean shoulder hardness, but produced by other methods, have not so far been successful. It is not known whether this is fully accounted for by the presence of the softer skin, but it is of interest to note that if there is still some small set-up in such shell, the higher ductility of the softer surface layer would be an advantage, since the greatest transverse elongation occurs in the surface layers.

8.2. *Base damage to shell.*

Piercing shell are also subject to two other common forms of damage. These do not appreciably affect the ability of the shell to perforate the plate, but prevent effective detonation.

When shell are fired at angles of 30 degrees or more, the turning of the shell as it passes through the plate causes it to suffer side blows towards the rear end, as a result of striking the sides of the hole (see Fig. 20). This may sometimes cause the shell to split longitudinally, cause a transverse crack, tear off the whole base of the shell, or cause crushing or ejection of the base adapter which carries the fuse. To reduce the likelihood of splitting, shell are normally made quite soft towards the base, with hardness around 250 B.H.N. This, however, encourages rather than prevents deformation. To reduce the likelihood that the adapter will be squeezed out or crushed enough to damage the fuze, it is usual to fit larger shell with a "relieved adapter." This is roughly cup shaped, with a truncated conical external surface, and a flat base into which the fuze is screwed. The cup has an external thread towards the lip, and is screwed into the shell, forward of the shell base, so that the flat base of the adapter is roughly in the plane of the shell base. Due to the form of the adapter, this leaves a space between the rear part of the adapter and the shell wall, with the result that a side blow on the base of the shell bends in the shell wall without crushing the fuze. Also, because the adapter is screwed in forward of the shell base, it is not necessarily removed even when part or whole of the shell wall is torn away round the driving band groove.

An alternative line of approach would be to attempt to prevent the turning of the shot and so eliminate the damaging side blows on the base. It has already been pointed out that the reduction of head length, or even partial truncation of the head, serves this purpose, and trials with shell of this type show them to suffer little or no crushing of the base. In general, however, the adoption of blunt head form tends to increase incidence of failure by loss of the adapter by a different mechanism.

It is quite common for shell to lose their adapters without suffering any base damage, other than some evidence of shearing of the threads holding the adapter. This form of failure is not fully understood, but is thought to be due to the violent elastic recovery of the shell as the first pressure wave returns from the base as a tensile wave, in the same way as a blow on one end of a bar will throw off a mass in contact with the other end. This view is supported by the fact that the phenomenon is found to occur more frequently with blunt headed shell. No real cure has yet been found.

CHAPTER 2.

PENETRATION FORMULÆ.

By D. G. Sopwith.

1. Introduction.

In order to be able to design an armoured structure to withstand specified conditions of attack, or an armour-piercing projectile to defeat under specified conditions a given armoured structure, formulæ relating the velocity required for perforation to the relevant particulars of the plate and shot are required. Such formulæ are generally referred to as "penetration formulæ," although that term refers more logically to formulæ giving the depth of penetration for velocities insufficient to give complete perforation.

The term "perforation" can be defined in many ways, according to the stage at which defeat of the plate is considered to have occurred. Thus the "ballistic limit" of a plate is that velocity above which a given shot will produce a cracked bulge and below which it will produce an uncracked bulge. The "critical velocity" used in this chapter, however, is that corresponding to exact perforation with no residual velocity after the shot has perforated the plate, *i.e.,* the minimum velocity at which the shot passes clean through the plate.

1.1. *Factors involved in formulæ.*

The factors involved relate to the shot, the plate and the condition of attack, and are as follows:—

(*a*). *Shot.*

Diameter	d.
Mass	M (or weight W).
Form	The length of the shot is given implicitly by W, the ratio W/d^3 (sometimes called the "calibre density") being a convenient index. The head form is usually specified by its c.r.h. (calibre radius head), *i.e.,* the radius of the ogive in terms of the diameter or calibre of the shot. For most A.P. shot this is about 1·4; for small arms it is usually much greater, but in this chapter attention will be confined, unless otherwise stated, to uncapped unjacketed shot or shell having a c.r.h. of about 1·4. $\frac{W}{d^3}$ does not afford any direct indication of the length of a shell, on account of the presence of the cavity. Length in itself, however, has little or no effect on penetration.
Strength	The strength of the shot obviously enters into the problem: as yet no satisfactory method has been devised for predicting the perforation of a shot which breaks up or deforms badly.

(*b*). Plate.

Thickness	t.
Size	Above a reasonable minimum, cases below which are of little interest, area of plate has little or no effect.
Strength	f. For the moment the precise meaning of the term strength will not be defined. It can, however, be specified by a quantity f having the dimensions of a stress.

(c). *Conditions of attack.*

Velocity — Striking velocity v_0.
Residual velocity v_1.
Critical velocity v (for exact perforation, *i.e.*, value of v_0 for which $v_1 = 0$).

Angle — θ (measured between the line of flight and the normal to the plate).

2. Formulæ Based on Theoretical Considerations.

2.1. *The formula* $\dfrac{Wv^2}{d^3} = K\phi\left(\dfrac{t}{d}, \theta\right)$: *dimensional aspect.*

The main variables are thus M, d, t, f, v and θ. Dimensional analysis gives the non-dimensional forms θ, t/d, Mv^2/fd^3, which suggest a formula of the form :—

$$Mv^2/fd^3 = \phi\ (t/d,\ \theta)$$

It will be noted that Mv^2 on the L.H.S. is twice the kinetic energy of the shot. For convenience we may use weight W instead of mass M and take f over to the R.H.S., giving :—

$$Wv^2/d^3 = K\phi\ (t/d,\ \theta) \qquad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \qquad (1)$$

where $K = fg$ (and $W = Mg$).

The function Wv^2/d^3 is referred to in U.S.A. as the "specific limit energy."

The problem now becomes that of defining the form of the function ϕ of t/d and θ, and of relating the coefficient K to the known properties of the plate.

2.2. *Theoretical derivations of the form of* $\phi\,(=\phi_0)$ *for normal attack.*

Considering first the case of normal attack ($\theta = 0$) the function ϕ becomes a function of t/d only :—

$$\phi\,(t/d,\ 0) = \phi_0\ (t/d).$$

Various assumptions may be made as to the resistance offered by the plate to the passage of the shot; each assumption leads in general to a different form of the function ϕ_0.

2.21. *Constant resistive pressure.*

Robins and Euler (A5), about 1742—45, assumed that the resistive pressure p against the shot was constant, equal to p_0. The total resistance is then $p_0A = \frac{\pi}{4}\ d^2p_0$ (A = projected area of shot) and the energy absorbed in perforation $\frac{\pi}{4}d^2tp_0$ whence $Wv^2/2g = \frac{\pi}{4}\ d^2tp_0$ or

$$Wv^2/d^3 = \frac{\pi g p_0}{2}\ (t/d) \qquad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \qquad (2)$$

so that $K = \frac{\pi g}{2}\ p_0$, $\phi_0\ (t/d) = t/d$*.

It may be noted that if p is not constant, $p_0 = \dfrac{2}{\pi g}\ \dfrac{Wv^2}{d^2t}$ is its mean value. This is an index of plate performance which has been used to a considerable extent in U.S.A. Its value is of the same order as the Brinell or diamond pyramid hardness (expressed in the same units†) of the plate, but the ratio of p_0 to hardness rises with t/d and falls with increase of B or H (B = Brinell, H = D.P. hardness number). The former fact shows that p is not constant.

2.22. *Poncelet theory.*

Poncelet (1829) (A5) assumed that $p = a + bu^2$ where a, b are constants and u is the instantaneous velocity of the shot. If a is put equal to p_0 and b in the form $\gamma\rho^1/2g$ (ρ^1 = density of plate material) § so that $p = p_0 + \dfrac{\gamma\rho^1 u^2}{2g}$, this is equivalent to taking an additional drag term, with γ as "drag coefficient." This theory has been elaborated, and recent treatment on similar lines may be found in Refs. A 18, 42 and 264.

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* Care is necessary with regard to units. If, as is usual, W is expressed in lb., v in f.s., t/d in inches and p_0 in tons per square inch, g must be taken as 32·2 (f.s.^{2}) × 2240 (lb./ton) × 1/12 (ft./in.) = 6020, giving $\frac{\pi g}{2}$ = 9450.

† Both Brinell and diamond pyramid hardnesses are expressed in kg./mm.^{2} (1 kg./mm.^{2} = 0·635 ton/in.^{2}).

§ g is inserted to convert the stresses to practical units of weight per unit area.

The equation of motion of the shot is :—

$$W\dot{u}=Wu\,\frac{du}{dx}=-A\,(a+bu^2)g$$

where A = projected area of part of shot inside plate.

x = penetration of nose.

$$\text{Hence } \frac{-M}{g}\int_{v_0}^{v_1}\frac{udu}{a+bu^2}=\int_0^t Adx=\text{volume of plate material displaced.}$$

$$\log_e\left(\frac{a+bv_0^2}{a+bv_1^2}\right)=\frac{2b}{M}\,g\,\frac{\pi}{4}\,d^2t=\gamma W^1/W$$

where W^1 = weight displaced $=\frac{\pi}{4}\,d^2t\rho^1$

For exact perforation $v_1=O$, $v_0=v$, hence :—

$$\log_e\frac{a+bv_0^2}{a+bv_1^2}=\log_e\frac{a+bv^2}{a}=\gamma W^1/W=\log_e s \text{ say} \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad (3)$$

This gives the following relation between the striking and residual velocities :—

$$v_0^2=v^2+sv_1^2 \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad (4)$$

The validity of this relation has been firmly established in the course of a considerable number of residual velocity firing trials both in this country and the U.S.A. (A45, 152, 153, 264 and 361). It forms the basis of the residual velocity method of determining critical velocities (A30, 150, 197 and 341), in which the residual velocity is obtained for two or more values of striking velocity and v_1^2 plotted against v_0^2, a straight line through the points giving v^2 at $v_1^2=0$.

The energy required for perforation is $\frac{M}{2}\,(v_0^2-v_1^2)=\frac{Mv^2}{2}+(s-1)\,\frac{Mv_1^2}{2}$

This increases with v_1 and so with v_0, the term $(s-1)\,\frac{Mv_1^2}{2}=\frac{s-1}{s}\,\frac{M}{2}\,(v_0^2-v^2)$ representing the energy imparted to the plate, partly in moving the plate material laterally and partly in accelerating any petals, discs, flakes, etc., forced off the back. It would thus be expected that γ would increase with increasing amounts of back damage; this has been shown to be the case (362).

Equation (3) gives :—

$$\frac{a+bv^2}{a}=e^{s_1}=1+s_1+\frac{s_1^2}{2!}+\frac{s_1^3}{3!}\cdot\cdot\cdot\cdot$$

where $s_1=\log_e s=\frac{\gamma W_1}{W}$

$$\text{or } v^2=\frac{as_1}{b}\,(1+\frac{s_1}{2!}+\frac{s_1^2}{3!}+\ldots\ldots)$$

$$\text{Since } s_1=\gamma W^1/W=\frac{\gamma\rho^1}{W}\,\frac{\pi}{4}\,d^2t;\ a=p_0;\ b=\frac{\gamma\rho^1}{2g}$$

$$v^2=\frac{2gp_0}{W}\,\frac{\pi}{4}\,d^2t\left[1+c\,\frac{t}{d}+\frac{2}{3}\left(c\,\frac{t}{d}\right)^2\ldots\right]$$

Finally :—

$$\frac{Wv^2}{d^3}=\frac{\pi g}{2}\,p_0\frac{t}{d}\left(1+c\,\frac{t}{d}\cdot\cdot\cdot\cdot\right) \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad (5)$$

where $c=\frac{\pi\gamma\rho^1}{8W/d^3}$

In practice the terms in $(t/d)^2$ and higher in the bracket may be neglected. This equation differs from the constant pressure equation (2) in the introduction of the $c\,t/d$ terms, giving

$$\phi_0=\frac{t}{d}\left(1+c\frac{t}{d}\right),\ K \text{ being } \frac{\pi g}{2}\,p_0 \text{ as before.}$$

As mentioned above, the mean resistive pressure p is found in practice to increase with t/d and equation (5) can, by the correct selection of p_0 and c, be made to fit firing trial results with considerable accuracy.

Thus, the Poncelet theory accounts satisfactorily for the linear relation between striking and residual energy and suggests a reasonable form for the function ϕ_0. Unfortunately, however, the values of γ ("drag coefficient") derived from the slope s in residual velocity trials and from the variation of critical velocity with t/d [*i.e.*, from c in equation (5)] are widely different; those derived from c are of the order of five times those derived from s.

2.23. *Shearing or punching.*

Fairbairn (1861) put forward the following formula, based on the arbitrary assumptions that failure occurred by the shearing out of a plug, and that the load required varied linearly from a maximum on commencement of shearing to zero in a distance equal to the thickness of the plate. The maximum load is πdtq where q = shear strength of plate material, and the energy absorbed is $\frac{t}{d}\pi dtq$, whence :—

$$\frac{Wv^2}{2g}=\frac{\pi}{2}\,q\,dt^2 \text{ or } \frac{Wv^2}{d^3}=\pi gq\,(t/d)^2 \qquad \ldots \quad \ldots \quad \ldots \quad \ldots \qquad (6)$$

so that $K=\pi gq$, $\phi_0\,(t/d)=(t/d)^2$.

It will be shown later that a formula of this type, with v varying as t/d holds for low values of t/d (less than 0·4). Failure does not, however, take place by the simple mechanism assumed, and little significance can be attached to the "shear strength" q.

2.24. *Plastic deformation theories.*

Various attempts have been made to derive penetration formulæ from analysis of the plastic deformation. These necessitate idealizing the conditions so as to render the problem tractable mathematically. Thus Ref. A20 gives a method in which, by considering the radial expansion of a small hole, expressions whose form is identical with that for constant resistive pressure [equation (2)] are derived. The values of p_0 obtained were :—

For thin plates $p_0=2f_y$. Ref. 57 corrects this to $1{\cdot}3f_y$.

$$\text{For thick plates } p_0=\tfrac{1}{2}f_y\left(1+\log_e\frac{2E}{(5-4\sigma)f_y}\right)$$
$$=2{\cdot}7 \text{ to } 3{\cdot}2\,f_y{}^*$$

where f_y = yield stress in compression

E = Young's modulus.

A calculation for thick plates of the value of p_0 required to enlarge radially a cylindrical and a spherical hole, corresponding to limiting cases of very sharp and very blunt shot is given in Ref. 243. For no strain-hardening the results are :—

$$\text{For sharp shot } p_0=\frac{1}{\sqrt{3}}f_y\left(1+\log_e\frac{\sqrt{3}E}{2(1+\sigma)f_y}\right)=3{\cdot}3 \text{ to } 3{\cdot}9\,f_y{}^*$$

$$\text{For blunt shot } p_0=\frac{2}{3}f_y\left(1+\log_e\frac{E}{(1+\sigma)f_y}\right)=3{\cdot}9 \text{ to } 4{\cdot}6\,f_y{}^*$$

The paper gives formulæ for p_0 for any stress-strain curve; at the high rates of strain occurring in armour plate penetration, however, the yield and the maximum stress will probably coincide so that no strain-hardening occurs and f_y will be somewhat higher than the (static) ultimate tensile stress. Thus the values of p_0 given above are of the order of 3 to 4 times the ultimate tensile stress, *i.e.*, of the same order as the Brinell or D.P. hardness (in the appropriate units). This is not very surprising since the firing trial is, in effect, a high speed indentation hardness test carried to much greater depths than is customary.

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* In each case the lower value is for $f_y=90$ tons/in.^{2} and the higher for $f_y=30$ tons/in.^{2}.

A third theory of this type is developed in Chapter 1. This is the only theory so far put forward which indicates the part played by the ductility of the plate material. It leads to

$$p_0 = q_0 \phi \left(\frac{\lambda^2}{\lambda - 1} \log \lambda - \frac{\lambda + 1}{2} \right)$$

where q_0 = shear stress at yield.

λq_0 = shear stress at fracture.

ϕ = shear strain at fracture.

Plastic theory is not yet sufficiently advanced to enable values of λ and ϕ applicable to the complex stress system occurring in plate penetration to be obtained from mechanical tests, *e.g.*, tension or torsion.

The theories in this section, therefore, do not lead to a new penetration formula, but to an explanation of the order of magnitude of the constant resistive pressure p_0 in equation (2).

2.25. *Pseudo-elastic deformation theory.*

Refs. 88 and 121 treat the problem as that of elastic perforation of a plate having a reduced modulus of elasticity to be derived from comparison with firing trials. The analysis is complex, but leads to a formula of the form,

$$\frac{Wv^2}{d^3} = K\left(\frac{t}{d} + k\right)^2 \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad (7)$$

where K and k are to be derived from firing trial results. The theoretical values, however, may be expected to give some idea of their dependence on the properties of the plate. These are $K = \frac{16}{15} gE^1$ and $k = \frac{b - 0{\cdot}33l}{d}$ where :—

E^1 = reduced modulus of elasticity.

b = height of bulge to commencement of cracking.

l = length of ogive of shot.

2.26. *Combinations of above theories.*

The above formulæ can be combined in various ways. Thus, Dahlgren Proving Ground (350, 353) have suggested that the early stages of penetration may occur as in the cylindrical expansion solution above, the final stage (in ductile plate) occurring by the bending back of a series of sector-shaped petals. The analysis indicates that for thin plates the energy varies as t^2d; above a limiting thickness nd this applies to the last nd of the thickness only, the early solution applying to the front part of thickness $(t - nd)$. Hence, the total energy is $\alpha d^2(t - nd) + \beta(nd)^2 d$ where α and β are constants, i.e.,

$$\frac{Wv^2}{2g} = \alpha d^2(t - nd) + \beta n^2 d^3$$

$$\text{and } \frac{Wv^2}{d^3} = H\left(\frac{t}{d} - a\right) \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad (8)$$

$$\text{when } H = 2g\alpha,\ a = \frac{n}{\alpha}(\alpha - \beta n).$$

Ref. 152 derives this formula by using constant resistance [equation (2)] followed by punching [equation (6)]. Here again the energy in the front $(t - nd)$ varies as $d^2(t - nd)$ and that in the rear nd as $(nd)^2d$. In this case, n is obtained by equating the load $\frac{\pi d^2}{4} p_0$ in penetration to that needed to shear the final nd, *i.e.*, $\pi d.ndq$, so that $n = p_0/4q$. The resulting penetration formula is, of course, the same as (8) above.

A method given in Ref. 139 has added to a constant resistance formula a term representing the energy absorbed in elastic vibration of the plate. The resulting equation is too complex for normal use.

3. Empirical Forms for Function ϕ_0.

Instead of deriving the form of the function ϕ_0 theoretically, many attempts have been made to derive it empirically from firing trial results. These may be divided into two classes—those giving a high degree of accuracy over a limited field and those giving a moderate degree of accuracy over a wide range.

Of the former, the linear relation between critical velocity and thickness for a given shot and type of plate—used for many years in this country by Fighting Vehicles (formerly Tank) Design Department (69, 244) is a very convenient and accurate one. This gives :—

$$\frac{Wv^2}{d^3}=K\left(\frac{t}{d}+k\right)^2 \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad (7)$$

as for Refs. 88 and 121 above. Ref. 244 suggests a constant value of k of 0·6, but this will be shown later to be incorrect.

The second class of empirical formulæ, aiming only at giving a moderate accuracy over a wide range of diameter of shot, and thickness and type of plate. usually express firing trial results in the form of a coefficient which varies less with these variables than does $\frac{Wv^2}{d^3}$. The U.S. Navy uses a function F such that :—

$$F^2= Wv^2/d^2t \text{ or } \frac{Wv^2}{d^3}=F\frac{t}{d} \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad (9)$$

As has been shown above, for constant resistive pressure, F would be constant. In certain cases this is nearly true, and in any case F varies much less than Wv^2/d^3.

De Marre, about 1870, introduced formulæ of the following type (expressed in the form now under discussion) :—

$$\frac{Wv^2}{d^3}=C\frac{t^a}{d^b}$$

This form has since that time been largely used, at first in de Marre's original form with $a=1{\cdot}4$, $b=1{\cdot}3$, C being expressed as the value for mild steel, *viz.*, $1{\cdot}044\times10^6$. (W in lb., v in f.s., t, d in inches) multiplied by a coefficient varying with type of armour. More recently, Milne and Hinchliffe (50, 86) have pointed out that for dimensional correctness a and b should be equal and have modified the de Marre equation to :

$$\frac{Wv^2}{d^3}=C\,(t/d)^{1{\cdot}43} \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad (10)$$

In this form the equation has been very extensively used during the late war.

The equations (9) and (10) should be looked upon rather as convenient methods of expressing firing trial results by a single coefficient than of predicting accurately the results of firing a particular shot at a particular plate, unless the appropriate coefficient for a very similar case is known. Thus, in the modified de Marre formula (10) $\log_{10}C$ may vary from 5·5 to 6·5, corresponding to a more than threefold variation in critical velocity. For a more limited range, equations may be derived empirically for the variation of F or C with t/d. Thus, for major calibre Naval projectiles, the U.S. Navy 1931 penetration formula (390) was given in the form (for normal attack) :—

$$F=c_1\left(\frac{t}{d}-0{\cdot}45\right)^2+c_2$$

Again, for 2-pr. shot, Milne and Hinchliffe (86) gave (for normal attack) :

$$\log_{10}C=5{\cdot}998+0{\cdot}227\left(\frac{t}{d}-1\right)=a+\frac{\beta t}{d} \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad (11)$$

The need for such expressions for the "constant" F or C in equations (9) and (10) shows that at least two constants are necessary to represent adequately the results of firing trials. One obvious possibility is to replace the exponent 1.43 in Milne and Hinchliffe's formula by a disposable index, giving :

$$\frac{Wv^2}{d^3}=C_1\,(t/d)^m \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad (12)$$

This form also has been used by many investigators, *e.g.*, O.C. *Memo.* B 23,108 (1931) gives an analysis of bomb firing trials leading to the approximate value $m=1{\cdot}7$.

Further empirical forms are suggested by the theoretical analyses in the previous section (the constant pressure and shearing theories each contain only one constant and so are not suitable) as follows :—

$$\frac{Wv^2}{d^3}=A\,\frac{t}{d}\left(1+c\frac{t}{d}\right) \qquad \ldots \qquad (5)$$

$$=K\left(\frac{t}{d}+k\right)^2 \qquad \ldots \qquad (7)$$

$$=H\left(\frac{t}{d}-a\right) \qquad \ldots \qquad (8)$$

where now all constants A, c, K, k, H, a are to be derived from firing trial results. It has been found (152, 364) that for a given series of results, *i.e.*, for a given shot against plate of a given type for values of t/d from, say, 0·5 to 2, any of the formulæ (5), (7), (8), (11) and (12) can be fitted with a degree of accuracy comparable with the experimental accuracy in the firing trials. Hence, the most useful type of penetration formula will be that in which the constants can be best correlated with the properties of the plate.

3.1. *Correlation of constants with properties of plates.*

The five formulæ suggested above are :—

$$\frac{Wv^2}{d^3}=A\,\frac{t}{d}\left(1+c\frac{t}{d}\right) \qquad \ldots \qquad (5)$$

$$=K\left(\frac{t}{d}+k\right)^2 \qquad \ldots \qquad (7)$$

$$=H\left(\frac{t}{d}-a\right) \qquad \ldots \qquad (8)$$

$$=C\left(\frac{t}{d}\right)^{1\cdot43} \text{ where } \log_{10}C=\alpha+\beta\frac{t}{d} \qquad \ldots \qquad (11)$$

$$=C_1\,(t/d)^m \qquad \ldots \qquad (12)$$

In the first three, the derivation suggests that the constants A, K and H are likely to vary with the hardness or ultimate tensile stress of the plate, whilst c, k and a depend rather on the ductility of the material. A series of firing trials on plates of Brinell hardnesses B of 250, 350 and 450 and $t/d=0{\cdot}75$ to 1·8 was reported in Ref. 152, where an analysis of the results is given according to the above five formulæ. It was found that whilst equations (5), (8), (11) and (12) gave curves of A, H, C and a showing a maximum within the range, with considerable doubt as to interpolation between the hardness values used, K was proportional to B. These trials, carried out with model 2-pr. shot of 0·296 inch diameter (0·189 scale) were later repeated (Ref. 360) with shot 0·540 inch, 0·990 inch and 1·565 inches diameter (0·345, 0·633 and full scale 2-pr.) and with plate of 300 Brinell. These confirmed the above conclusions and indicated the presence of a scale or size effect, which will now be discussed.

4. The National Physical Laboratory Formula for Normal Attack.

4.1. *Effect of size of shot.*

None of the above formulæ indicates specifically the existence of a scale effect, *i.e.*, they suggest that geometrically similar shot would perforate plate of the same ratio t/d at the same critical velocity v. At the beginning of the late war it was not known whether or not this was the case, no systematic trials on this issue having been carried out in the 50 years or so during which armour piercing shot had been used. The series of trials described in the last section was consequently carried out at the National Physical Laboratory for the Ordnance Board, for the dual purpose of clearing up this point (scale effect) and ascertaining the effect of plate hardness.

The results of the trials indicated a definite scale effect, the critical velocity v for a given t/d decreasing with increase of diameter d. For two given diameters of shot the difference in v increased with hardness B, but was independent of t/d. Analysing the results, Sopwith (361) found that they could be represented with a probable error of 21 f.s. or 1·3 per cent. by the following formula :—

$$\frac{Wv^2}{d^3}=\left(43{\cdot}4\sqrt{B}\frac{t}{d}+747-\frac{54000}{B_0-B}\right)^2$$

$$=1630B\left[\frac{t}{d}+\frac{17{\cdot}2-\dfrac{1240}{B_0-B}}{\sqrt{B}}\right]^2 \qquad \ldots \qquad (13)$$

The symbol B_0 in equation (13) denotes a limiting hardness depending on the diameter d of the shot. For the sizes investigated (0·189, 0·345, 0·633 and full scale 2-pr. shot) it was found that it could be represented by the empirical equation :—

$$B_0 = 500 - 160 \log_{10} d/d_2 \qquad \ldots \quad \ldots \quad \ldots \quad \ldots \qquad (14)$$

where $d_2 = 1{\cdot}565$ inch = diameter of 2-pr. shot.

Equation (13) is of the form $\frac{Wv^2}{d^3} = K\left(\frac{t}{d} + k\right)^2$; as stated in the last section K is proportional to B. k, as also stated above, is likely to be dependent on the ductility of the plate material. For a given plate quality, however, the ductility will itself be a function of hardness B, as equation (13) shows; for other qualities of plate, the relation between k and B might be different. It was found, however, that this equation, derived from trials on 3 per cent. chrome-molybdenum steel plate of 250 to 450 Brinell, applied also to mild steel (364) of 110 Brinell, so that the relation between k and B did not appear to depend on composition of plate.

For two shot of the same W/d^3 fired at plates of the same hardness B and thickness ratio t/d the difference in critical velocity due to difference in scale is given by :—

$$v'' - v' = \frac{54000}{\sqrt{W/d^3}} \frac{B_0' - B_0''}{(B_0' - B)(B_0'' - B)} \qquad \ldots \quad \ldots \quad \ldots \quad \ldots \qquad (15)$$

where ′ and ″ refer to the respective sizes of the two shot.

In practice, two shot of different diameters will usually differ somewhat in W/d^3 and the difference in v will consequently vary slightly with t/d, the difference being only in part due to scale effect. The following table shows the predicted difference in v due to scale effect for various sizes of shot, all of $W/d^3 = 0{\cdot}603$, as used in the trials discussed. In the case of the scale model two pounders the experimental values are given in brackets; those for the 6-pr., 17-pr. and 3·7-inch guns are illustrative of that part of the difference due to scale effect only, the values in brackets being derived from trials referred to later.

Difference in critical velocity (f.s.) due to scale effect between various sizes of shot and 2-pr.

Shot	Diameter d	Brinell hardness of plate B			
		200	250	300	350
	ins.				
0·189 scale 2-pr.	0·296	—	+88(90)	+120(145)	+202(110)
0·345 scale 2-pr.	0·540	—	+63(60)	+ 94(105)	+153(170)
0·633 scale 2-pr.	0·990	—	+31(30)	+ 48(70)	+81(105)
2-pr.	1·565	0	0	0	0
6-pr.	2·231	−21	−31(−10)	−50(−20)	−93
17-pr.	2·98	−41	−61(−60)	−101(−90)	−199
3·7-inch	3·685	−53	−80(−40)	−135(−100)	−276

This table shows clearly the danger of using scale model results scaled up without correction.

4.2. *Effect of hardness of plate : Optimum hardness.*

Various trials carried out during the late war (*e.g.*, Refs. 256, 343) have indicated the fact that, in general, there is an optimum hardness of plate to resist a particular attack, due to the fact that increasing hardness is associated with decreasing ductility. Of the penetration formulæ discussed above, equation (13) is the only one which takes this into account. For maximum v, differentiating the R.H.S. of equation (13) and equating to zero, we have :—

$$\frac{t}{d} = \frac{2490\sqrt{B}}{(B_0 - B)^2} \qquad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \qquad (16)$$

Values of optimum hardness calculated from equations (14) and (16) for values of t/d from 0·5 to 2 and for shot of the calibres considered above are shown in Fig. 28. It will be seen that the optimum hardness decreases with increasing diameter of shot and increases with t/d. The fact that the optimum hardness varies with t/d means that the best hardness for a plate of given thickness cannot be settled without reference to the type of attack. Thus, a 3-inch plate to give maximum resistance against 6-pr. attack will be too hard for maximum resistance to 3·7-inch shot; to illustrate this point lines of constant thickness of plate have been included in Fig. 28.

Fig. 29 shows for $t/d=0{\cdot}5$, 1, 1·5 and 2, the values of $v\sqrt{W/d^3}$ for hardness $B=200$ to 450 for 0·189, 0·345, 0·633 and full scale 2-pr. and for 6-pr., 17-pr. and 3·7-inch; on each curve the optimum hardness is indicated by a circle. It will be seen that the curves are fairly flat, especially below the optimum hardness. Increased resistance to small-calibre shot—say less than $d=t/2$—is of little importance, since these shot are unlikely ever to perforate the plate; again, increased resistance to large shot—say over $d=t$—will in general result only in decreasing the residual energy of the shot which will perforate the plate in any case. Hence, it is best to base the hardness of plate on a t/d of say 1·5. This gives an optimum hardness of about 325 for 60 mm. plate/2-pr. and 285 for 4½-inch plate/17-pr.; these may be taken as typical conditions both as regards thickness, diameter and hardness at the beginning and end of the war of 1939—45.

4.3. *Limits of applicability and accuracy of equations.*

Equation (13), the auxiliary equation (14) for B_0, and the derived equations (15) and (16) thus reproduce the main features observed in actual firing trials at normal, *viz.*, the relation between thickness of plate and critical velocity for a given shot [equations (13) and (14)], the effect of size of shot [equation (15)] and the optimum hardness [equation (16)]. They were derived, as stated above, from firing trials with shot 0·296 to 1·565-inch diameter against 3 per cent. Cr.Mo. steel plates from 0·5 to 2 diameters thick and of Brinell hardness 250 to 450; the corresponding critical velocities were from 1000 to 3000 f.s. Subsequent trials with 0·296 inch shot against mild steel showed that the equations also applied to a Brinell hardness of 110 for t/d up to 3·4. In all these trials, the probable error of the critical velocity derived from equations (13) and (14) was 21 f.s. or 1·3 per cent.

The value of W/d^3 in the above trials was 0·603; various trials have shown that the assumption that, for perforation of a given plate, Wv^2 is constant is accurate enough for all practical purposes, at any rate for normal values of W/d^3. The generalization of the equation (13) to a general value of W/d^3 is thus justified. The trials now to be described confirm this.

A very comprehensive series of firing trials has since been carried out by Fighting Vehicles Design Department. These were carried out with 6-pr., 17-pr. and 3·7-inch shot against 3, 3¾, 4½ and 6-inch plate of twelve different types (compositions and steel makers) of ultimate tensile stress from about 45 to 70 tons/in.^{2} (about 220 to 330 Brinell)—about 1000 critical velocity determinations in all. Equations (13) and (14) have been applied to the analysis of these results, using a ratio of ultimate stress to Brinell of 0·21. For 3 per cent. chrome-molybdenum steel from three makers the deviation from the formula was rarely greater than 2 per cent. in v. For other types of steel the deviation on the high side was up to about 60 f.s. over the whole range of ultimate stress and on the low side from 60 f.s. (for 45 tons/in.^{2}) to 100 f.s. (for 70 tons in.^{2}) with an occasional value down to 150 f.s. low at the higher ultimate.

One limitation, as mentioned above, is the lower limit of t/d of about ½. Later trials show that the formula holds down to $t/d=0{\cdot}4$, below which v is proportional to t/d as in Section 2.23. The question of thin plates is considered more fully in Section 6.

5. Effect of Angle of Attack.

The description in Chapter 1 of the very complex mode of perforation of a plate by a projectile striking obliquely suggests that it will be extremely difficult if not impossible to obtain a penetration formula for oblique attack at once accurate and simple. Nevertheless, formulæ have been obtained which give a general idea of the effect of angle.

As a first approximation, the effect of obliquity may be considered as being equivalent to increasing the thickness of the plate from t to t sec. θ, *i.e.*, the thickness in the line of flight; this method has been used by Naval Research Laboratory, U.S.A. (A 13). Alternatively, the velocity v may be resolved into v cos. θ perpendicular and v sin. θ parallel to the plate, the latter component being neglected. This method is used in the modified de Marre formula of Milne and Hinchliffe, equation (10), (Refs. 50, 86) and and in the U.S. Navy F-function, equation (9), (Ref. 390); in both cases the general form replaces v by v cos. θ.

Both these methods are approximate only and tend to give too low a critical velocity for angles over say 20 degrees. This fact is realized in the two formulæ just mentioned (Milne and Hinchliffe and U.S. Navy), where the substitution of $v \cos. \theta$ for v in the formula is not sufficient to express the full variation with angle. Thus, Milne and Hinchliffe's full expression for C for the 2-pr. shot (Ref. 86) is:—

$$\log_{10} C = 5{\cdot}998 + 0{\cdot}227 \left(\frac{t}{d} - 1\right) + 0{\cdot}947 \ (1 - \cos. \theta) \qquad \ldots \quad \ldots \qquad (11a)$$

Various modifications have been tried in order to improve these approximations, *e.g.*,

$v \cos^2 \theta$ (Ref. 164);

$t (\sec \theta + \tan \theta)$ (Ref. 148).

These forms have only a limited range of application. It may be noted that if $\frac{Wv^2}{d^3} = C_1 (t/d)^m$ [equation (12)] the use of $v \cos \theta$ corresponds to that of $t (\sec \theta)^{2/m} \triangleq t (\sec \theta)^{1 \cdot 4}$ for the modified de Marre formula.

The constants in penetration formulæ for normal attack may be regarded as functions of angle in the general case and these functions may be plotted or tabulated, with no attempt to represent the variation with θ analytically. This procedure has been adopted in Refs. 69 and 244, where it is stated that no general formula is possible, and in Ref. 366.

Finally, these functions of angle obtained experimentally may be represented by formulæ frankly empirical. Thus, the F function of Section 5 has been expressed (Ref. 389) as $F = a + b \cos \theta$, where a is a function of t/d and both a and b change their values at $\theta = 44$ degrees. Alternatively, $F = a + b\ \theta^2$ has been used (Ref. 390). A formula of this type, derived from an extension to angle attack of the firing trials described in Section 6 of this Chapter, is given in Ref. 437, this is as follows:—

Typo. Should be d^3

$$\frac{Wv^2}{d_3} = \left[43{\cdot}4\sqrt{B}\ \frac{t \sec \frac{3}{2} \theta}{d} + 916 - \frac{11800}{65 - \theta} - \frac{54000}{B_0 - B}\right]^2 \qquad \ldots \quad \ldots \qquad (13a)$$

For normal attack ($\theta = 0$), equation (13a) reduces to equation (13) except that the middle term becomes 734 instead of 747; this corresponds to a constant difference of 16 f.s. between the batches of plate used in the two series of trials.

The optimum hardness condition becomes:—

$$\frac{t}{d} \sec \frac{3}{2} \theta = \frac{2490\sqrt{B}}{(B_0 - B)^2} \qquad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \qquad (16a)$$

The optimum hardness curves (Fig. 28) are still applicable provided the abcissa is taken as $\frac{t}{d} \sec. \frac{3}{2} \theta$. The optimum hardness is found to rise with angle of attack.

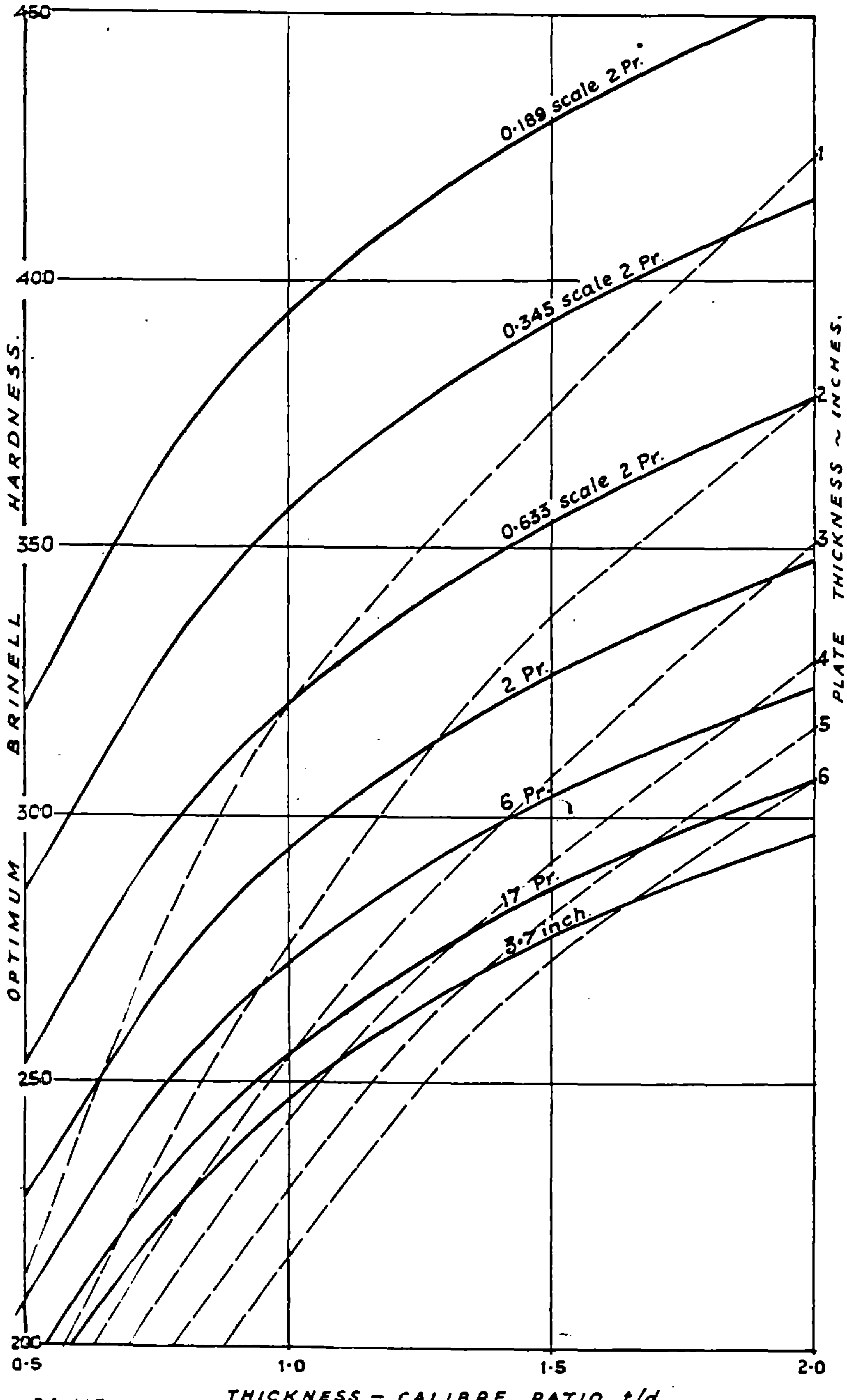
Angles of attack exceeding 45 degrees will be considered in Section 6.

6. Thin Plates $\frac{t}{d} \sec. \theta < 0{\cdot}4$.

There are comparatively little systematic data on the perforation of thin plates (t/d less than about $\frac{1}{2}$); such data are mainly for isolated cases of capped shell against face-hardened plate. If a single formula is to cover all values of t/d it is obviously necessary that v should be zero for t/d zero, *i.e.*, $\phi(0) = O$. Some of the formulæ discussed in the previous sections do not fulfill this condition and so cannot be applied to thin plates; The equations necessarily represent an analytical approximation to the correct value and some disagreement outside the fitted range is to be expected. It is, however, possible that there is a change in mechanism of failure at some critical thickness, necessitating a change of formula. Firing trials against mild steel of low t/d at Road Research Laboratory (82), Princeton University and N.P.L. (A45, 364) indicated that least for this material v was proportional to t/d (for t/d less than about $\frac{1}{2}$); the Princeton trials (A45) and some at N.P.L. (364) showed that this was far from being the case at higher t/d, equation (13) being applicable to the latter. In order to obtain further data, the firing trials at N.P.L. referred to in sections 4 and 5 were extended to as low a value of t/d as was practicable, *viz.*, about 0·2, corresponding to a critical velocity v of 400 to 500 f.s. The results (Ref. 437) on 3 per cent. Cr.Mo. steel plate (Brinell 250 to 350), confirmed the indications on mild steel. At low values of t/d plugging occurred. and v was proportional to t/d, equation (13a) holding for higher t/d; the intersection of the two lines was found to occur at $\frac{t \sec. \theta}{d} \triangleq 0{\cdot}4$.

FIG. 28.

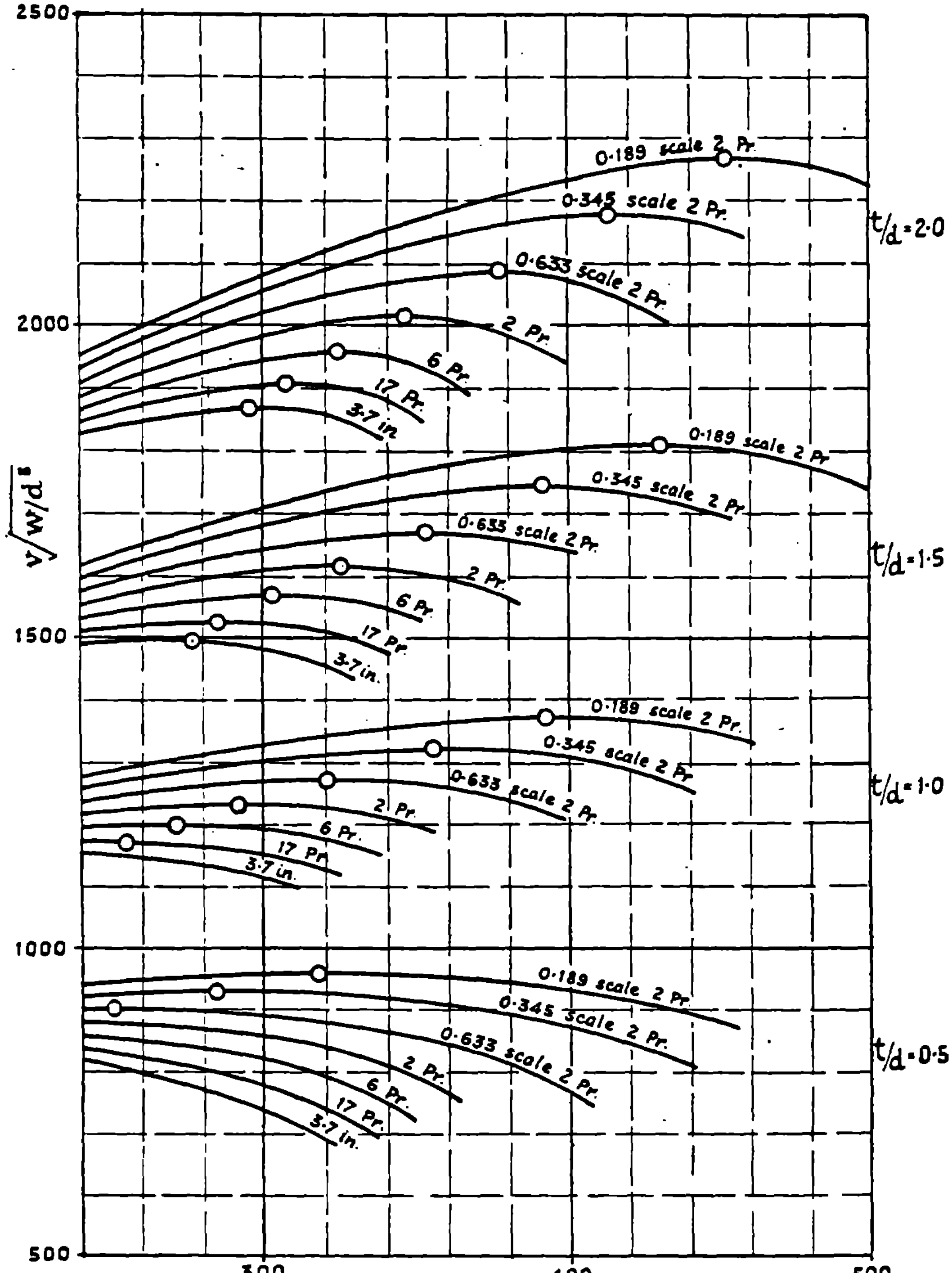
OPTIMUM HARDNESS OF PLATE FOR GIVEN SHOT DIAMETER, PLATE THICKNESS AND THICKNESS-CALIBRE RATIO.



Ref:- Nat. Physical Lab. Eng. Dn. 234/46.

FIG. 29.

— VARIATION WITH HARDNESS OF CRITICAL VELOCITY FOR GIVEN SHOT DIAMETER AND THICKNESS-CALIBRE RATIO.



Full scale and model				2 Pr.	$\sqrt{w/d^3}$	=	0·776
"	"	"	"	6 Pr.	"	=	0·752
"	"	"	"	17 Pr.	"	=	0·801
"	"	"	"	3·7 in.	"	=	0·749

Ref:- Nat. Physical Lab. Eng. Div. 254/46.

26B

Thus, if equation (13a) is expressed in the form :—

$$v=\alpha\frac{t}{d}+\beta \qquad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \qquad (13b)$$

this holds for $\frac{t}{d}>0{\cdot}4 \cos \theta$, below which :—

$$v=(\alpha+2{\cdot}5\,\beta \sec.\theta)\frac{t}{d} \qquad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \qquad (17)$$

The accuracy of equation (17) is not as great as that of equation (13a); near the critical thickness $t/d=0{\cdot}4 \cos \theta$ there is a slight " rounding off " of the transition from one line to the other.

As the angle of attack increases, the intercept term β in equation (13b) decreases, until at about 45 degrees (the probable limit of application of the formula) it vanishes.* In this case equations (13a) and (17) coincide; at 60 degrees the whole of the relation between v and t/d was found to be of the form $v=c\ t/d$, the value of c being about twice that for normal attack.

7. FACE-HARDENED PLATE AND CAPPED PROJECTILES.

The above discussion of penetration formulæ relates almost entirely to homogeneous plates attacked by monobloc projectiles. In Service, face-hardened plate is used occasionally in armoured fighting vehicles and more frequently in ship armour. In order to counteract the shattering effect on the projectile, the latter is provided with a piercing cap. Detailed designs of cap and hardness lay-outs of plate differ so much that few detailed conclusions can be drawn. **In general, it may be said that to a first approximation the cap and the face cancel each other, and the case can be treated as monobloc shot v. homogeneous plate of the hardness of the main (back) portion of the plate.**

Shot fitted with a piercing cap may encounter homogeneous plate; in such cases the critical velocity is increased. Thus, in Ref. 366, it was found that at normal the critical velocity against 2-pr. A.P.C.B.C. was about 100 f.s. higher (for $t/d=0{\cdot}7$ to 2) although the capped shot were 15 per cent. heavier; this difference represents the combined effect of the piercing and ballistic caps.

The case of capped shell is of even greater complexity since, owing to the presence of the cavity, the strength of the shell becomes of great importance. Some typical values of $\log_{10}C$ in the modified de Marre formula [equation (10)] are given below;† these all apply to A.P.C. shell against C plate.

d	t	t/d	θ	$\log_{10}C$	Remarks
ins.	ins.		degs.		
15	12	0·80	30	$<6{\cdot}05$	
15	12	0·80	40	6·21	
15	12	0·80	45	$>6{\cdot}24$	
9·2	8	0·87	40	6·24	
9·2	6	0·65	40	6·06	
9·2	6	0·65	45	6·06	Low c.r.h. (0·57 and 0·7).
9·2	6	0·65	50	$>6{\cdot}25$	
6	12	2·00	0	$6{\cdot}09+0{\cdot}07$	
5·25	4·5	0·86	30	$6{\cdot}13+0{\cdot}06$	Small scale trials.
5·25	4·5	0·86	40	$6{\cdot}22+0{\cdot}05_5$	Small scale trials.
5·25	4	0·76	40	$6{\cdot}18+0{\cdot}01_5$	Small scale trials.
5·25	4	0·76	45	$6{\cdot}25+0{\cdot}01_5$	Small scale trials.
5·25	—	0·36 to 0·61	45 to 65	$6{\cdot}11+0{\cdot}44t/d$ $-0{\cdot}30\cos\theta$	Small scale trials.

* The value of θ at which β vanishes may be seen from formula (13a) to depend to some extent on scale of attack and plate hardness, but $\theta=45$ degrees is a representative value for most practical conditions.

† Information supplied by Ordnance Board.

In the absence of a value of $\log_{10} C$ for a case reasonably similar to the one required, it will be found that the above values for angles from 30 degrees to 45 degrees can be represented by :—

$$\log_{10} C = 6{\cdot}56 + \tfrac{1}{2}\frac{t}{d} - \cos\theta.$$

with an error of $\pm 0{\cdot}04$ (*i.e.*, ± 5 per cent. in v); the error in the case of low c.r.h. is larger.

8. Practical Applications of Formulæ.

8.1. *Formulæ and tables for A.P. shot up to 6-inch against homogeneous armour of 200 to 400 Brinell.*

The basic formula recommended is equation (13a) with the constant 916 adjusted to 929 to agree with the normal attack results on the first batch of plates, which have been taken as standard. That is :—

$$\frac{Wv^2}{d^3} = \left[43{\cdot}4\sqrt{B}\frac{t}{d}\sec.\frac{3}{2}\theta + 747 - \frac{54000}{B_0 - B} - \frac{182\theta}{65-\theta}\right]^2$$

$$\left(N.B.\ 747 - \frac{182\theta}{65-\theta} = 929 - \frac{11800}{65-\theta}\right)$$

This equation has too many variables for plotting to be useful except in particular cases, *e.g.*, particular shot against particular plate or at particular angle (*i.e.*, v function of t and θ or B respectively). The formula and that derived from it for thin plate may be expressed for ease of computation thus :—

$$v = K\left(a\frac{t}{d} + b\right) \qquad \left(\theta \ngtr 45^\circ,\ \frac{t}{d} > h\right)$$

$$= K c\frac{t}{d} \qquad \left(\theta \ngtr 45^\circ.\ \frac{t}{d} < h\right)$$

where $a = a_0\ a_1$

$b = b_0 - b_1$

$c = a + b/h$

$h = 0{\cdot}4\cos\theta$

$K = \sqrt{(W/d^3)_0/(W/d^3)}$

$a_0 = 43{\cdot}4\sqrt{B/\left(\frac{W}{d^3}\right)_0}$

$b_0 = \left(747 - \frac{54000}{B_0 - B}\right)\Big/\sqrt{\left(\frac{W}{d^3}\right)_0}$

$a_1 = \sec.\frac{3}{2}\theta$

$b_1 = 182\theta/(65-\theta)\sqrt{\left(\frac{W}{d^3}\right)_0}$

Values of K, a_0, b_0, a_1, b_1 and h are tabulated in Table 1. K is a function of W/d^3, $(W/d^3)_0$ being standard W/d^3 taken as 0·6, K thus being a correction factor for other values of W/d^3. a_0 is a function of B only, b_0 of B and B_0 (*i.e.*, of d) and a_1, b_1 and h are functions of θ.

In some extreme cases (high angle, hardness and/or calibre) b will be found to be negative; in such cases it will probably be best to take b as zero.

8.2. *Formulæ and tables for other cases.*

For other cases, the modified de Marre formula (10) may be used, provided a value of C or $\log_{10} C$ for a reasonably similar case is known. Some values are given in Section 7. For computation purposes the formula may be expressed :—

$$\log_{10} v = \tfrac{1}{2}\log_{10} C - \tfrac{1}{2}\log_{10}\frac{W}{d^3} + 0{\cdot}715\log_{10}\frac{t}{d} - \log_{10}\cos\theta.$$

Values of $-\tfrac{1}{2}\log_{10}\frac{W}{d^3}$, $-\log_{10}\cos\theta$ and $0{\cdot}715\log_{10}\frac{t}{d}$ are tabulated in Table 2.

Table 1.

Values of constants in perforation formula for A.P. shot v. homogeneous plate.

B (kg. mm.²)	a_0 (f.s.)	b_0 (f.s.) d=1-inch B_0=531	1·5-inch 503	2-inch 483	2·5-inch 467	3-inch 455	3·5-inch 445	4-inch 435	4·5-inch 427	5-inch 419	5·5-inch 413	6-inch 407
200	793	754	734	718	704	691	680	667	657	646	636	627
210	812	747	727	709	693	680	667	655	643	631	621	610
220	831	740	718	700	682	667	655	640	627	615	603	591
230	850	733	709	689	670	655	640	625	610	595	584	571
240	868	724	700	678	657	640	625	607	591	574	562	540
250	886	717	689	664	643	625	607	587	571	551	537	520
260	903	707	678	652	627	607	587	565	540	525	507	491
270	921	697	664	636	610	587	565	542	520	497	476	456
280	937	687	652	621	591	565	542	515	491	463	440	416
290	954	675	636	603	571	542	515	484	456	423	398	368
300	970	662	621	584	540	515	484	448	416	378	347	312
310	987	649	603	562	520	484	448	407	368	325	288	252
320	1002	634	584	537	491	448	407	358	312	261	216	163
330	1018	617	562	507	456	407	358	301	252	181	125	59
340	1033	599	537	476	416	358	301	231	163	81	9	—
350	1048	580	507	440	368	301	231	145	59	—	—	—
360	1063	556	476	398	312	231	145	35	—	—	—	—
370	1078	532	440	347	252	145	35	—	—	—	—	—
380	1092	502	398	288	163	35	—	—	—	—	—	—
390	1107	470	347	216	59	—	—	—	—	—	—	—
400	1121	432	288	125	—	—	—	—	—	—	—	—

W/d^3	K
lb./inch³	
0·50	1·095
0·55	1·044
0·60	1·000
0·65	0·960
0·70	0·926

$\theta°$	a_1	b_1	h
	f.s.	f.s.	
0	1	0	0·400
5	1·009	20	0·398
10	1·035	43	0·394
15	1·082	71	0·386
20	1·155	105	0·376
25	1·260	147	0·362
30	1·414	201	0·346
35	1·643	274	0·328
40	2·000	376	0·306
45	2·613	530	0·283
50	—	—	0·257
55	—	—	0·229
60	—	—	0·200

Table 2.

Values of functions in modified de Marre formula.

t/d	$0{\cdot}715 \log_{10} t/d$	Diff.	W/d^2	$-\frac{1}{2}\log_{10}\frac{W}{d^2}$	Diff.
0·2	$\bar{1}$·500	126	0·50	0·151	21
0·3	$\bar{1}$·626	90	0·55	0·130	19
0·4	$\bar{1}$·716	69	0·60	0·111	17
0·5	$\bar{1}$·785	57	0·65	0·094	17
0·6	$\bar{1}$·842	47	0·70	0·077	—
0·7	$\bar{1}$·889	42	—	—	—
0·8	$\bar{1}$·931	36	—	—	—
0·9	$\bar{1}$·967	33	$\theta°$	$-\log_{10}\cos\theta$	Diff.
1·0	0	30	—	—	—
1·1	0·030	27	0	0	2
1·2	0·057	24	5	0·002	5
1·3	0·081	23	10	0·007	8
1·4	0·104	22	15	0·015	12
1·5	0·126	20	20	0·027	16
1·6	0·146	19	25	0·043	20
1·7	0·165	18	30	0·063	24
1·8	0·183	16	35	0·087	29
1·9	0·199	16	40	0·116	35
2·0	0·215	15	45	0·151	41
2·1	0·230	15	50	0·192	49
2·2	0·245	14	55	0·241	60
2·3	0·259	13	60	0·301	—
2·4	0·272	12	—	—	—
2·5	0·284	—	—	—	—

CHAPTER 3.

THE STRESSES IN PROJECTILES WHEN PENETRATING STEEL.

By C. A. Adams.

1. Introduction.

From Chapters 1 and 2 it is clear that while penetration formulæ give a value for the minimum energy required for penetration of a given armour plate, the success of the projectile depends not only on its possession of the required energy, but also on its ability to withstand the stresses generated by the impact. For normal attack an estimate of the order of the stress can be obtained from indirect experimental evidence and some indications are available from theoretical considerations. For oblique attack, information from either source is very scanty. In practice, more difficulties arise in designing a projectile capable of withstanding the combined stresses arising in oblique attack, than in ensuring that there is sufficient strength to prevent set-up or fracture from the axial compression due to normal attack. The knowledge so far obtained on the stresses to which projectiles are subjected on penetrating armour therefore falls very short of that required. Nevertheless it has a use, since to some extent increase in strength under compression is obtained at the expense of resistance under bending stress. Since it is certainly necessary to ensure that sufficient compressive strength exists, a working knowledge of the limits set by this consideration enables the design to give no more compressive strength than is essential, and so to increase resistance to failure under transverse stresses.

2. Dependence of Projectile Stresses on Head Resistance.

The state of stress in a projectile which is penetrating an armour plate can easily be found to a first approximation, once the magnitude of the reaction at the head is known. Neglecting elastic effects, the instantaneous retardation at any part is the same throughout the projectile. The total force over any cross section is therefore proportional to the product of the reaction at the head and the mass behind the cross section. With the assumption of a perfectly rigid shot the important facts to determine are therefore the magnitudes of the reactions at the head and their dependence on plate thickness and quality, and projectile shape and velocity.

2.1. *Estimate of head resistance from plate hardness.*

An approximate idea of the order of the pressure at the head can be obtained from the following simple considerations. The Brinell hardness number gives a measure of the pressure with which the material resists a surface deformation which is small in relation to its total thickness. If it is assumed that approximately the same pressure would be obtained whatever the size or shape of the indenter, then neglecting any dynamic effect, this is the pressure which would act on the head of a projectile until deformation of the rear of the plate provided a relief of stress. This is certainly an over simplification, but, if the assumption is made, it would imply that the stress until back yielding occurs, *i.e.*, the maximum stress approached in very thick plates, would equal the Brinell number. The fact that the Brinell measurement is obtained by dividing the resisting force by a curved area introduces a small correction which need not be taken into account in the present approximation. Since the units for Brinell numbers are kgs. per square mm. and 1 kg. per square mm. $\equiv 0{\cdot}635$ ton per square inch the maximum pressures thus calculated for the range of hardness levels likely to occur in armour plate are as follows:—

B.H.N. (kgs. per square mm.)	200	300	400
Corresponding pressure in semi-infinite plate, tons per square inch	127	190	254

Alternatively, since the relation between Brinell number B and ultimate tensile strength f_u is very approximately, $B = 4{\cdot}7\, f_u$ over this range of hardness, the maximum pressure P anticipated in a very thick plate on this argument would be:—

$$P = 3{\cdot}0\, f_u \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \qquad (18)$$

Pressures of this order may be expected to act over the tip of the projectile in the initial stages of penetration, and to be augmented by any dynamic term in the resistance. It follows that if the tip of the projectile is to withstand these pressures its yield strength must be at least three times the ultimate strength of the target. The calculation, however gives little guide to the maximum pressures likely to be reached in any cross section of the parallel body of the shot. In the first place, the indentation test allows some backward flow of the material towards the surface, and therefore measures a pressure which is less than that encountered deep in the material (see p. 41). Secondly, the stress gradient in general reduces the stress towards the rear of the projectile. Thirdly, in most practical applications the thickness of the plate will not be sufficiently great to justify the assumption that, at the stage at which the head is completely immersed in the plate, the latter is still offering a resistance unmodified by distortion of the rear face.

2.2. *Estimate of head resistance from penetration formulæ.*

The mean resistance offered by a finite plate can be estimated very roughly by making use of the formulæ discussed in Chapter 2. These formulæ give the energy E_c in ft. pndls. required by a projectile of diameter d for the penetration of a plate of thickness t. If the reaction is regarded as starting when the projectile tip meets the plate and ending when the shoulder emerges, then neglecting bulging, the mean force $\bar{F}$ (poundals) acting over the distance $t+l$ (inches), where l is the length of the head of the shot, is given by :—

$$\frac{\bar{F}(t+l)}{12} = E_c$$

The mean pressure $\bar{p}$ in tons per square inch over a section of diameter d inches is thus :—

$$\bar{p} = \frac{\bar{F}}{2240g} \cdot \frac{4}{\pi d^2} = \frac{48E_c}{2240g.\ \pi d^2 (t+l)}$$

or since $E_c = \frac{1}{2}Mv^2 = \frac{1}{2}d^3 \left(43{\cdot}4\sqrt{B}\frac{t}{d} + 747 - \frac{54000}{B_0 - B}\right)^2$ (Chapter 2, page 23)

$$\bar{p} = \frac{1}{93{\cdot}3\, g\pi} \cdot \frac{d}{t+l}\left(43.4\sqrt{B}\ \frac{t}{d} + 747 - \frac{54000}{B_0 - B}\right)^2$$

This equation embodies the "scale effect" as discussed in Chapter 2. For the present approximate purposes it is sufficient to use the value of B_0 appropriate to the 2-pr. scale. *i.e.*, $B_0 = 500$. The equation then becomes :—

$$\bar{p} = 1{\cdot}064.10^{-4}. \frac{1}{t/d + l/d}\left(43{\cdot}4\sqrt{B}\ \frac{t}{d} + 747 - \frac{54000}{500-B}\right)^2 \quad \dots \quad \dots \quad \dots \quad \dots \quad (19)$$

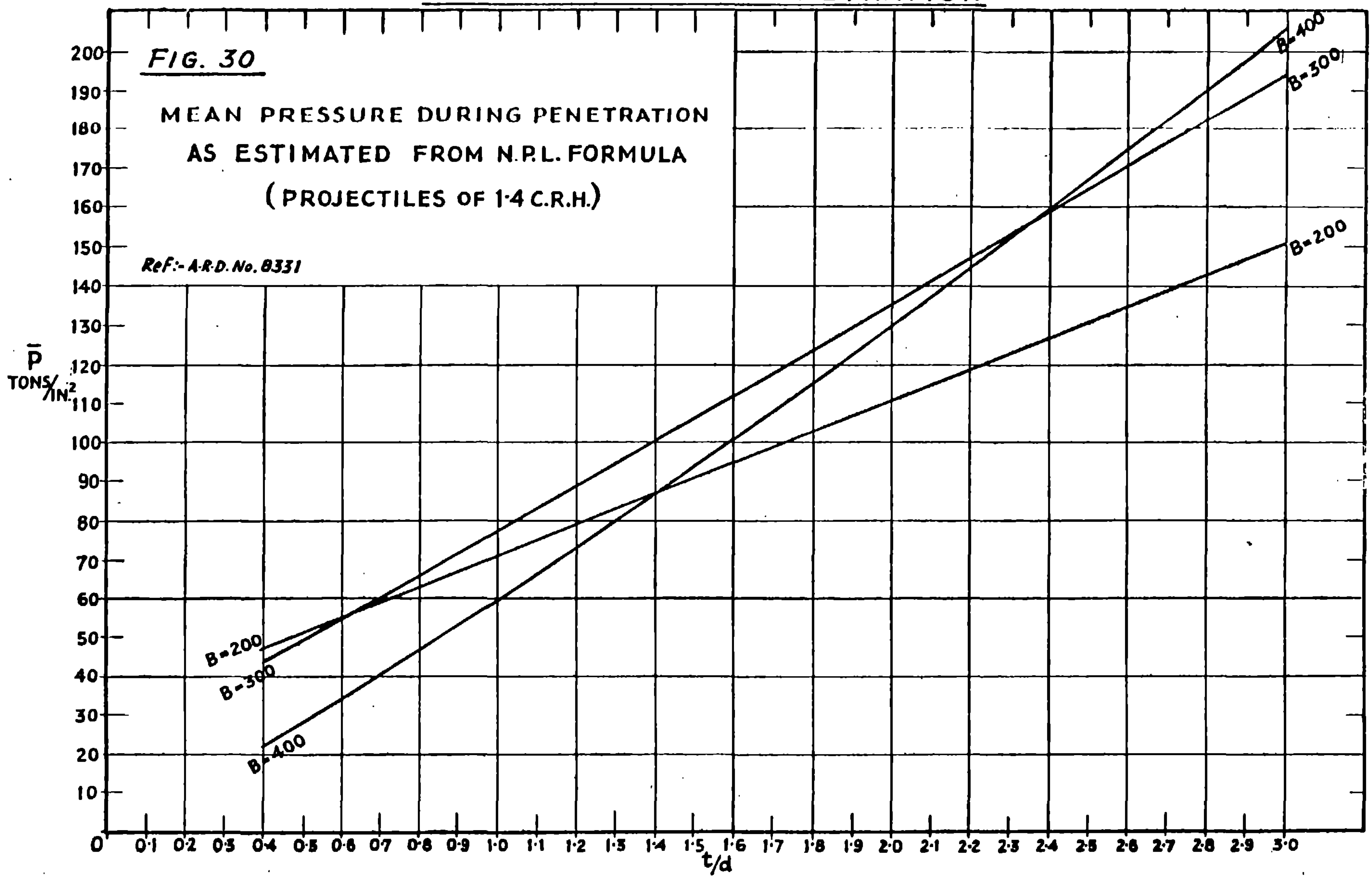
$$= \frac{0{\cdot}20B}{t/d + l/d}\left[\frac{t}{d} + k\right]^2 \text{ where } k \text{ depends on } B.$$

It is obvious that this equation can be written in the form :—

$$\bar{p} = a\left(\frac{t}{d} + \frac{l}{d}\right) + b + \frac{c}{t/d + ld}$$

where a, b and c are independent of t/d. Over the range of t/d of practical importance the effect of the last term, which is negative, is small. $\bar{p}$ is therefore approximately linear in t/d, but the curve bends upwards slightly as t/d increases. Fig. 30 shows $\bar{p}$ plotted against t/d for the values of B of 200, 300 and 400 and with $l/d = 1{\cdot}07$ (corresponding with a 1·4 c.r.h. projectile). It is noticeable that on the basis of this formula the mean pressure at low values of t/d ($< 0{\cdot}65$ approximately) is highest for the softest plate ($B = 200$) and lowest for the hardest ($B = 400$). Further, the hardest plate ($B = 400$) begins to give a larger value for $\bar{p}$ than the intermediate plate ($B = 300$) only at the comparatively high value of $t/d \simeq 2{\cdot}3$. Remembering that formula (19) uses total energy of perforation, and that examination of fired plates shows that the extent of petalling or plugging is greatly dependent on hardness, it would be unjustifiable to assume that the ratio of maximum load to $\bar{p}$ is the same for hard and soft plates. A higher ratio will clearly exist in a harder plate where the reaction is spread over a shorter distance than $t+l$ and a lower in softer plates where the bulge may give a total distance considerably in excess of $t+l$. The relation of peak pressure to mean pressure is so far a matter of speculation, but on the assumption that the factor is not greatly different from 2 (as would be the case in a triangular force-penetration curve) when the plate is

ARMOUR PLATE PENETRATION



moderately hard ($B = 300$), the value of 190 tons per square inch for the peak pressure would be estimated to correspond with a value of t/d of about 1·3. The stress estimated directly from the Brinell number was 190 tons per square inch for a semi-infinite plate. The calculation therefore gives another indication that the Brinell pressure underestimates the resistance of a thick plate. It is not to be expected that estimates of maximum pressure as $2\bar{p}$, when $\bar{p}$ is taken from these curves, will have much significance at high values of t/d, (a) because formula (19) has not been shown to apply to values of t/d much in excess of 2 and (b) because p max. will obviously tend to approach $\bar{p}$ when t/d is large. Nevertheless, in the region of $t/d \simeq$ 1·0 to 1·5, where maximum pressure may be expected fairly near the mid-point of the penetration, the estimate is likely to be of the correct order. For this region it indicates maximum pressures from about 120 tons per square inch to 210 tons per square inch.

3. Measurements of Plate Resistance in Static Punching Experiments.

For closer estimates of the resistance of a plate to penetration it is necessary to consider more direct experimental measurements. The information on which most reliance can be placed is obtained from static experiments in which a punch to the contour of an armour piercing shot is pressed through armour plate, the load and penetration being measured throughout the process. The extent to which load-penetration curves, thus obtained under static conditions, represent the corresponding relation under dynamic conditions cannot be fully known until reliable measurements have been made of the forces acting on a shot when fired through a plate. Attempts at such measurements have been made (see page 39), but the results are not yet sufficiently substantiated to enable them to be used as a check against the static method. The striking fact about the results of the latter method is that measurements of the total energy required for penetration under static conditions agree within the limits of experimental error with measurements of the energy as obtained in firing trials. Although this agreement does not preclude the possibility that differences in the shape of the load-penetration curves may exist in the two cases, it gives some justification for the use of the static curves as working hypotheses until further evidence becomes available.

3.1. *General results established by static punching experiments.*

The results of static punching experiments on armour plate and other steels, with discussions of their significance in relation to armour plate penetration in general, are mainly given in References 20, 64, 101, 123, 156, 158, 190, 293, 297, 325, 330, 376, 377, 438 and 439. From the present aspect, namely that of the stresses induced in the projectile, the interest is primarily in the peak loads found by this method. Before quoting and discussing these results some general facts established by the investigation are presented :—

(1). If the punch is not lubricated a significant proportion of the work performed in static punching is expended in overcoming friction. This frictional effect is relatively unimportant in the early stages of penetration but increases the maximum load by 10 per cent. to 45 per cent. and may increase the total work by an amount up to 50 per cent. The lubricant used was a thin film of soft solder (40 Sn, 40 Pb, 20 Bi). The agreement between energies for complete perforation in static and firing trials refers to the static results in which the lubricant was used. The inference is that friction probably plays only a small part in dynamic penetration. This deduction cannot be made with certainty : (a) because the value of the residual friction when the lubricant is used in the static experiments is not known; and (b) because the agreement between static and dynamic results, though highly suggestive, does not prove identity in the processes. Nevertheless the conclusion is consistent with the following facts : (i). Firings with lubricated shot (Refs. A 38, 240) have failed to show any reduction in critical velocity by lubrication*. (ii). Friction of steel on steel decreases as the speed of the moving parts increases. The empirical formula $\mu = 0{\cdot}27 \left(\dfrac{1 + 0{\cdot}0044V}{1 + 0{\cdot}064\,V}\right)$ has been given (Ref. 440) for the variation of the coefficient of friction μ in the dry sliding of steel on steel where V is the relative velocity in metres

* In Ref. 161 a reduction of about 12 per cent. caused in critical velocity by lubrication was reported, but the firings were in the low velocity range 200 to 300 f.s.

per second. (iii). Metallurgical examination of holes made in armour plate, by firing, show the presence of a thin surface film of metal which has been raised to at least 850° C. and which has probably acted as a lubricant during penetration. (Ref. 74) (iv). Experiments designed to determine the loss of spin of a projectile after its passage through armour indicated that frictional effects were relatively small (Ref. 68).

(2). The qualitative behaviour of the plate is very similar in static and dynamic penetration for a given head shape and calibre. When back petalling occurs under firing conditions, back petals of identical type occur in the static case. Similarly plugging occurs in the same circumstances in the two cases. Limited results on the 2-pr. scale also show that plates which fail by discing under firing conditions, fail similarly under static test. The coronet form of a large number of front petals characteristic of "fired" holes is not reproduced in static trials, but with good lubrication a ridge is raised which approximates to the height and shape of the boundary of the front petals. Another difference between the two cases exists in the volume of metal undergoing plastic strain in the two cases. (Ref. 58). Although the volume affected is much larger in the static case it appears from calculation that the excess work thus expended is relatively small.

(3). The representation of the resistance as a constant pressure equivalent to that given by a Brinell number, for semi-infinite plates or for small penetrations into finite plates, is not valid. For conical punches up to shoulder penetration it is a good approximation. For ogival headed punches the pressure increases with increasing penetration. In any case the equivalence would not apply over a large part of the penetration, since bulging of the rear face causes a departure from semi infinite conditions when the point of the punch reaches a distance of the order of ½ calibre from the rear face.

(4). The position of maximum load varies according to the shape of the punch. For sharp punches it occurs when the point breaks through the rear bulge. This condition corresponds with petalling failure and penetration is not complete until the shoulder has emerged from the bulge. For blunt punches, which cause plugging, maximum resistance occurs very shortly before the plug shears. Shearing commonly occurs in the range investigated. *i.e.*, $0{\cdot}75<t/d<2{\cdot}0$, when the point of the punch is a little over half-way through the plate. Variation of maximum load for a given head shape and plate quality is approximately linear with plate thickness.

(5). A small difference in the work performed at different stages, between the static and dynamic cases, is found by comparisons of the energies required for partial penetrations in the two cases. These comparisons show that more energy is used in the dynamic case in the early stages and less in the later. The differences are not large but are probably beyond the range of experimental error and thus indicate the existence of a dynamic term in the resistance*.

(6). As in firing trials, scale effect is found to be small. Nevertheless it was observable and agreed as regards total work. and mode of failure, with the results found in the N.P.L. firing trials, when the plates used in these trials were penetrated statically.

3.2. *Detailed results of static punching experiments.*

Since the frictional resistance to an unlubricated punch has been found to account for a significant proportion of the energy absorbed in perforation, the numerical results quoted here have been taken only from the observations on lubricated punches. A summary of these results is given in Table 3. The maximum stresses to which the projectile is subjected are largely governed by the maximum load, although the rate at which this maximum is reached must have a secondary effect on the stress distribution. In Table 3, therefore, the values of the maximum load are quoted. The close agreement in nearly

* The existence of a dynamic term is shown independently of static experiments by the fact that in the linear relation $v_s^2=v^2+sv_1^2$ between the squares of the striking, remaining and critical velocities v_s^2, v_1^2, v^2, the constant s departs in general from unity. The partial penetration experiments show that whether or not $s=1$ there is a difference between the static and dynamic forces at corresponding penetrations, but they also indicate that the difference is small.

all cases between energies for perforation in the static and dynamic cases is shown in the columns in which the critical velocity, deduced from the static energy, is compared with the critical velocity determined by firing trials. From the observed maximum load F, a stress P is obtained by dividing by the cross-sectional area of a body of the projectile ($\pi d^2/4$). This value is quoted in the table, but it is not necessarily the value of any stress existing in the shot during dynamic penetration. Neglecting any fundamental differences in the load-penetration curve which may exist between the static and dynamic cases, and also the stress gradient in the latter case, maximum load may be reached before the shoulder of the shot has penetrated the front surface of the plate. In these conditions, which will arise when the plate is thin or the head of the projectile is long, the load will be distributed over a smaller area and the stress at the head will be greater than the quoted value. Presentation of the results in the form of stresses which the load would cause if acting over the full cross-section of the projectile is adopted for two reasons :—

(a). It illustrates that the dependence of the stress on scale is very small.

(b). Because this dependence is small it is convenient in applications to calculate from the stress rather than from a load which varies according to the scale.

Table 3.

Static observations referring to lubricated punches, with comparisons of calculated and observed critical velocities.

F = Maximum load (tons). $P = \frac{4F}{\pi d^2}$ (tons per square inch).

In most cases the quoted figures are means from several observations.

Head shape	d	t/d	B.H.N.	F	P	P/B	Critical velocity Static	Critical velocity Firing
	ins.		kgs./sq. mm.	tons	tons/sq. in.		f.s.	f.s.
1·4 c.r.h.	0·285	0·787	373	9·04	141·7	0·380	1523	1528
"	"	0·825	318	8·60	134·8	0·424	1548	1552
"	"	0·867	307	8·73	136·9	0·446	N.R.	N.R.
"	"	0·877	109	3·98	62·4	0·572	1115*	1225
"	"	1·024	263	9·02	141·4	0·538	1702	1693
"	"	1·028	352	10·86	170·3	0·483	1818	1779
"	"	1·046	302	9·89	155·0	0·513	1761	1757
"	"	1·344	260	10·95	171·7	0·660	2034	2015
"	"	1·491	302	12·13	190·2	0·630	2237	2180
"	"	1·753	109	6·17	96·7	0·887	1745	1778
"	"	1·996	296	13·54	212·3	0·717	2680	2645
"	"	2·011	323	14·55	228·1	0·706	2696	2620
"	"	2·102	112	8·22	128·9	1·151	N.R.	N.R.
1·4 c.r.h.	0·285	2·632	109	7·04	110·4	1·013	2201	2345
"	"	3·505	109	8·34	130·7	1·199	2750	2816
"	0·785	0·721	295	51·03	105·4	0·357	1391	1379
"	"	1·042	288	69·60	143·8	0·499	1675	1683
"	"	1·419	274	79·71	164·7	0·601	1982	2015
"	"	1·419	286	84·73	175·1	0·612	2035	2038
"	"	1·426	291	84·56	174·7	0·600	2043	2051
"	1·565	0·714	271	180·9	94·0	0·347	1248	1314

* Bending and dishing occurred in the static penetration.

Table 3 (contd.)

Head shape	d	t/d	B.H.N.	F	P	$\frac{P}{B}$	Critical velocity	
							Static	Firing
	ins.		kgs./sq. mm.	tons	tons/sq. in.		f.s.	f.s.
1·4 c.r.h.	1.565	0·715	289	197·5	102·7	0·355	1291	1311
"	"	0·760	262	200·0	104·0	0·397	1218	1186
"	"	0·897	248	232·2	120·7	0·487	1406	1440
"	1·656	0·944	257	246·5	128·2	0·499	1445	1440
"	"	0·973	246	241·5	125·6	0·511	1481	1453
"	"	0·976	281	263·2	136·8	0·487	1471	1440
"	"	1·011	284	254·9	132·5	0·467	1604	1591
"	"	1·249	273	276·4	143·7	0·526	1747	1709
"	"	1·358	292	321·8	167·3	0·573	1942	1947
"	"	1·522	290	346·4	180·1	0·655	2044	2053
"	"	2·037	302	424·4	220·6	0·730	N.R.	N.R.
4·0 c.r.h.	0·250	0·788	342	5·58	113·7	0·332	1454	1418
"	"	0·860	212	4·23	86·2	0·407	1312	1350
"	"	0·988	307	5·85	119·2	0·388	1615	1557
"	"	1·532	260	7·02	143·0	0·550	1864	1982
"	"	1·564	112	4·76	97·0	0·866	1611	1572
"	"	1·568	207	7·54	153·6	0·742	1872	1892
"	"	2·220	217	8·39	170·9	0·788	2232	2289
"	"	2·286	321	11·82	240·8	0·750	2606	2584
"	"	2·404	112	5·82	118·6	1·059	2013	1989
0·6 c.r.h.	1·565	1·042	269	313	163	0·605	—	—
1·0 "	"	"	"	277	144	0·535	—	—
2·0 "	"	"	"	265	138	0·512	—	—
0·6 "	"	1·259	273	323	168	0·615	—	—
1·0 "	"	"	"	308	160	0·586	—	—
2·0 "	"	"	"	276	144	0·526	—	—
0·6 "	"	1·518	290	375	195	0·672	—	—
1·0 "	"	"	"	356	185	0·638	—	—

4. Dependence of Static Resistance on Plate Hardness and Thickness.

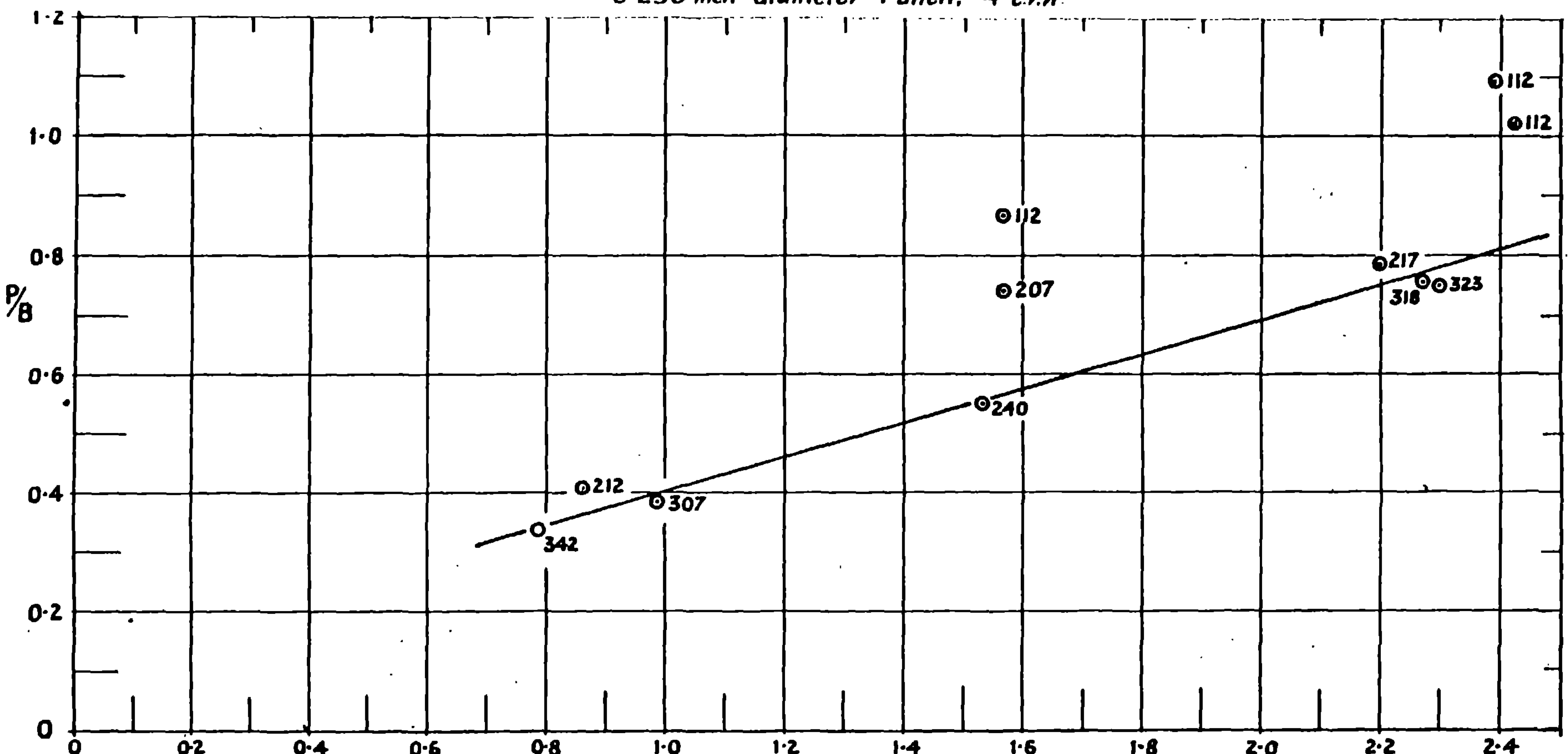
If maximum load were entirely determined by the thickness t of the plate, the diameter d of the punch, and a single stress characteristic of the plate quality, and if further the Brinell number B were a direct measure of this characteristic stress, then it is easily seen from dimensional arguments that P/B would be a function of t/d only. The values of $\frac{P}{B}$ have therefore been tabulated and a plot of P/B against t/d is shown in Figs. 31 and 32.

FIG. 31.

ARMOUR PLATE PENETRATION.

VALUES OF $\dfrac{\text{MAXIMUM PRESSURE}}{\text{BRINELL HARDNESS}}$ FROM STATIC OBSERVATIONS.

0·250 inch diameter Punch, 4 c.r.h.



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36A

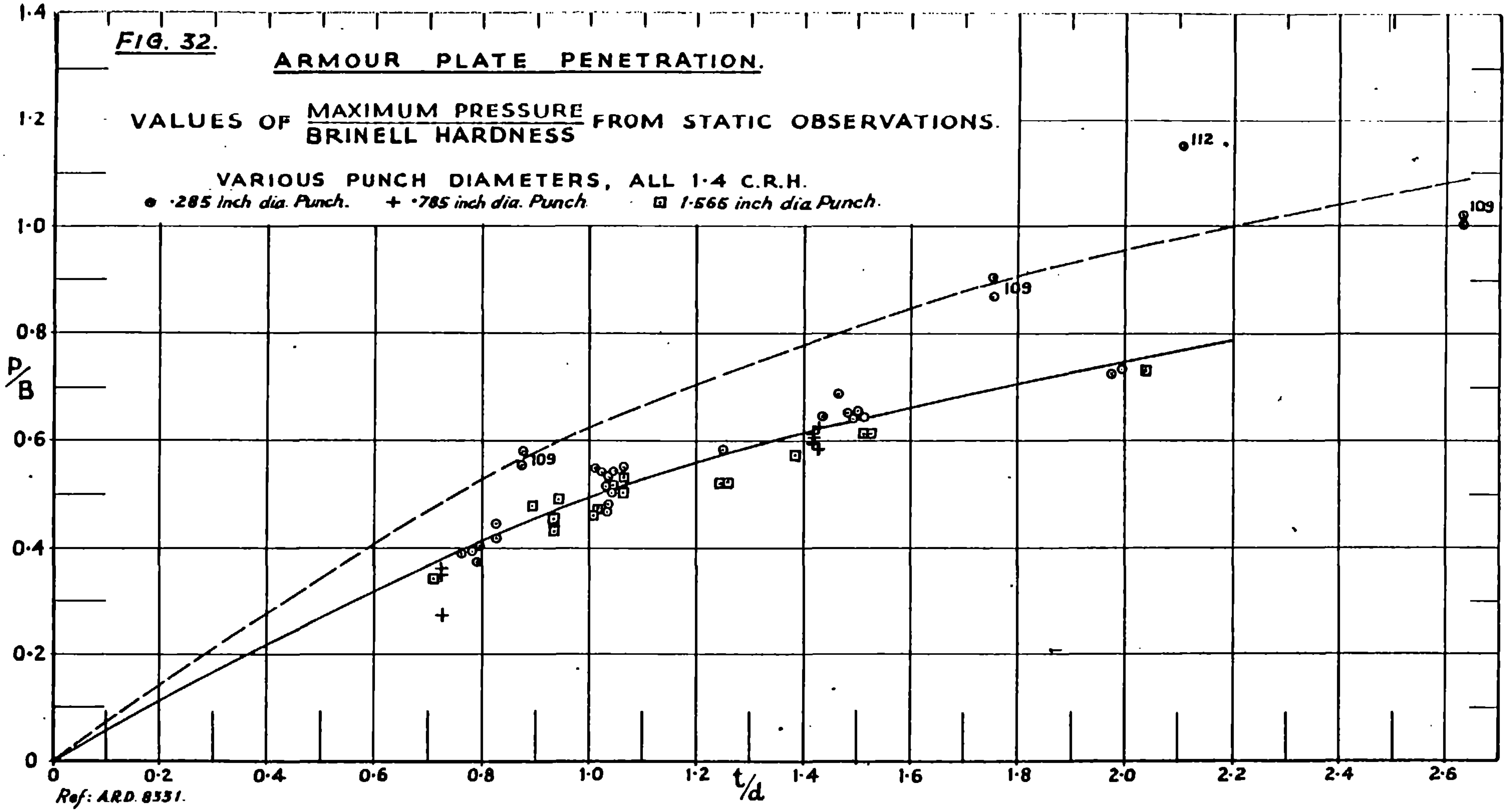


FIG. 32.
ARMOUR PLATE PENETRATION.
VALUES OF MAXIMUM PRESSURE / BRINELL HARDNESS FROM STATIC OBSERVATIONS.
VARIOUS PUNCH DIAMETERS, ALL 1·4 C.R.H.
·285 inch dia. Punch.
+ ·785 inch dia. Punch
1·566 inch dia Punch.
109
112
109
109
P/B
1·4
1·2
1·0
0·8
0·6
0·4
0·2
0
0·2
0·4
0·6
0·8
1·0
1·2
1·4
1·6
1·8
2·0
2·2
2·4
2·6
t/d
Ref: A.R.D. 8331.

36-B

In Fig. 31, which refers to a punch of 4 c.r.h., 0·250 inch diameter, the Brinell numbers of the plate concerned are given against each plotted point. It is clear from this diagram that P/B is not a function of t/d only. The points obtained from observation on mild steel (B.H.N. 112) lie well above those for harder plates. A similar result is observable in Fig. 32. To avoid confusion the Brinell numbers are given in this diagram only for the mild steel results, but it is again clear that for a given value of t/d the corresponding value of P/B is greater for mild steel than for the considerably harder armour steels to which the other results refer. It is further apparent that although there is a tendency for the points referring to armour plate to group about a common curve it would only be an approximation, even in these cases, to regard P/B as determined entirely by t/d. Several possibilities arise from these observations :—

(a). It may still be true that P is a function only of t/d and some single stress f characteristic of plate resistance, which would again imply that P/f is a function of t/d but f is not proportional to B. In this case, if B is a function of f only, it should be possible to find a function $F(B)$ of B such that $P/F(B)$ plotted against t/d would give a uniform curve.

(b). P may depend not only on the stress given by the Brinell reading B but also on some additional stress characteristic of the plate. If the additional stress is capable of variation independently of B, no method of plotting will give the regular curve required, since the plate properties would be insufficiently specified.

(c). t and d may not be the only distances concerned in the determination of P. For example, the bulge height may enter and may vary with the quality of the plate [this possibility is similar to those of (a) or (b) since height ratio can depend on a stress only if the co-efficients in the relation have the dimensions of a stress], or the dimensions concerned in the plate structure may influence its behaviour.

Case (b), namely, the possibility that some property of the plate in addition to its thickness and hardness enters into the determination of the maximum load, is almost certainly true. For example, the maximum resistance might depend on the extent of shear deformation which can occur between yield and fracture. (See Chapter 1, pp. 8 and 9.) At best, the Brinell number can give only an average measure of the stress strain curve, and this average refers to only one type of deformation. In the present connection the possibility that some other "strength" constants influence the results cannot be analysed and the working assumption must be that unspecified plate qualities have only a secondary effect on the phenomena. This assumption is equivalent to supposing that if the results were plotted in three dimensions with P, B, and t/d as ordinates they would all fall on some unique surface. Alternatively, P plotted against t/d or B when the other quality is constant would yield a unique curve. The experimental points do not provide sufficient results at constant t/d or constant B to enable a satisfactory analysis to be made on these lines. A fairly large number of results are available in the region $t/d \simeq 1$ and it is thus possible, as an approximation, to correct them to $t/d = 1$, the correction being obtained from the general trend of the results shown, say, in Fig. 32. Table 4 gives the result of such a calculation, but beyond showing that the mild steel results fall well outside the group for armour plate it gives no indication of the precise relation between P and B. Another presentation of the results is given in Fig. 33 in which P has been plotted against t/d and the Brinell number associated with each result has been given against the plotted point. Although this diagram provides evidence that P tends to increase with B it does not suggest any definite contours for constant B such as would be expected from the assumption $P = f(t/d,\ B)$. In view of these observations it is not profitable to attempt to obtain an approximate analytical relation between the variables. From theoretical considerations it is to be expected that when t/d is large the value of P will approximate to a constant value of $k\ f_y$ where k is about 4·6 and f_y is the yield stress material (see p. 41). It is also obvious that P is zero when $t/d = 0$. A function such as $P = k\ f_y\ (1 - e^{-at/d})$ where a is a function of head shape and Brinell number might give an approximate representation, but, in view of the scatter of the results there is no advantage in presenting them in this form. Comparison of Figs. 32 and 33, however, shows that some gain is made by plotting P/B instead of P against t/d. The curve shown in Fig. 32 is drawn freely with no attempt to fit to an equation. It will be seen that as far as armour plate is concerned the error introduced by using this curve would never be large.

Table 4.

Estimated values of P/B at $t/d=1$, obtained by correcting values observed in the neighbourhood of $t/d=1$.

d	t/d	$\delta\nu$ $0{\cdot}36\,(1-t/d)$	$\nu = \frac{P}{B}$	Y $=\nu+\delta\nu$	B
0·285	0·825	0·063	0·422	0·485	321
	0·828	0·062	0·450	0·512	315
	1·025	−0·009	0·543	0·534	264
	1·011	−0·004	0·552	0·548	261
	1·042	−0·015	0·347	0·532	300
	1·046	−0·017	0·506	0·489	303
	1·032	−0·012	0·483	0·471	357
	1·032	−0·012	0·470	0·458	346
	1.035	−0·013	0·536	0·523	461
	1·032	−0·012	0·518	0·506	462
0·785	1·043	−0·015	0·504	0·489	285
	1·041	−0·015	0·495	0·480	291
1·565	1·013	−0·005	0·471	0·466	283
	1·009	−0·003	0·463	0·460	285
	1·042	−0·015	0·532	0·517	269
	0·944	0·020	0·495	0·515	257
	0·897	0·037	0·483	0·520	248
	1·065	−0·023	0·534	0·511	316
	1·068	−0·024	0·552	0·528	316
	1·061	−0·022	0·503	0·481	311
	0·932	0·024	0·458	0·481	247
	0·933	0·024	0·433	0·457	257
0·285	0·877	0·044	0·585	0·629	109
	0·877	0·044	0·558	0·602	109

The curve of Fig. 32 will give a fairly good estimate of the maximum resistance encountered by a punch of 1·4 c.r.h. in the range $0{\cdot}7<t/d<2{\cdot}0$. Despite the scatter of the points the diagram shows some evidence of a small "scale effect" in the direction which would be expected from the similar effect in energies for perforation. Maximum pressures tend to decrease as calibre increases. This tendency is illustrated in Fig. 33, in which the points corresponding with the plates used in the N.P.L. trials are shown in heavier printing.

Although the tendency can be seen, the variations apparent in Fig. 32 show that no useful purpose would be served by an attempt to express the relation by different curves for each scale. Extrapolation of the curve beyond the range $0{\cdot}7<t/d<2{\cdot}0$ is probably justifiable, if it is guided by the following considerations :—

(i). There may be a discontinuity in the slope of the curve in the region $t/d=0{\cdot}4$ or 0·5 (see Chapter 2, page 26) but there is no reason to suppose that any large curvature exists from the origin to this value.

(ii). Since increase of thickness must increase maximum load except possibly at great thicknesses, the slope is always positive.

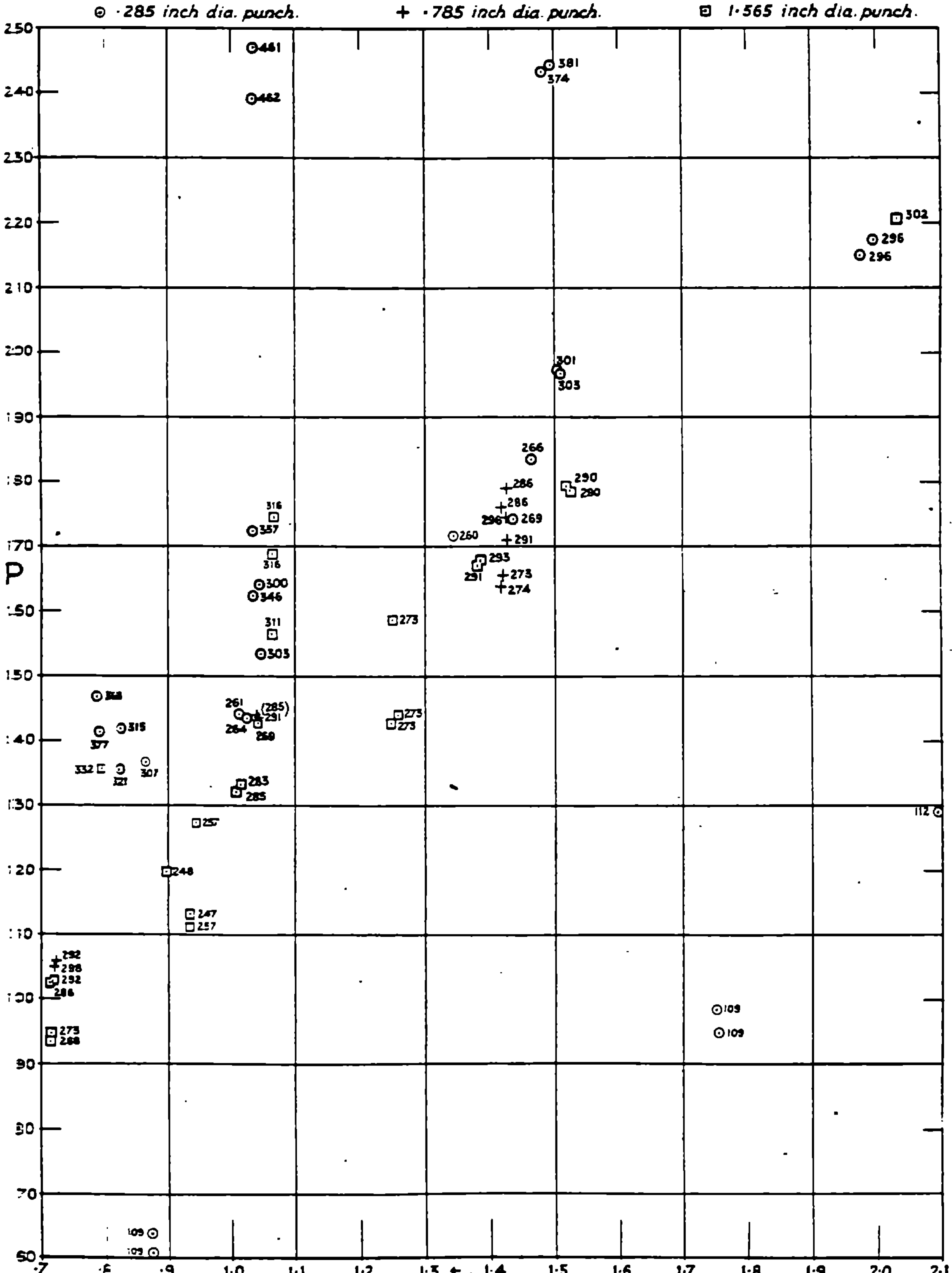
FIG. 33.

ARMOUR PLATE PENETRATION.

VALUES OF MAXIMUM PRESSURE WITH PLATE HARDNESSES SHOWN AGAINST PLOTTED POINTS.

VARIOUS PUNCH DIAMETERS, ALL 1·4 C.R.H.

⊙ ·285 inch dia. punch. + ·785 inch dia. punch. ⊡ 1·565 inch dia. punch.



Ref. A.R.C. 8331.

38-A

(iii). The limiting value of the pressure, probably approached very closely at $t/d \simeq 5$, is likely to be about $p \simeq 4{\cdot}6\, f_y$.*

A tentative curve for hardness in the region of that of mild steel ($B \simeq 100$) is included. Information on the effects of variation in head shape is very scanty, but the available results referring to head shapes other than 1·4 and 4·0 c.r.h. are plotted in Fig. 34. The approximate curves for 1·4 and 4·0 c.r.h. are repeated in this diagram. In the small range of thicknesses in which the additional results lie (approximately $1{\cdot}0 < t/d < 1{\cdot}5$) the departures of the results for 0·6, 1·0 and 2·0 c.r.h. from those for 1·4 c.r.h. are not large. Nevertheless, they indicate, as is to be expected, that a blunter head gives a higher maximum pressure. The differences are likely to be greater when $0 < t/d < 1$ and smaller when $t/d > 1{\cdot}5$.

The values of $\bar{p}/B$ as calculated from the N.P.L. formula have been included in Fig. 34. It is apparent that the approximate estimate of $2\bar{p}$ for maximum stress obtained from the formula was not greatly in error for the middle hardness ($B = 300$) but that, as was to be expected, the estimate is not good for the other hardnesses.

5. Dynamic Measurement of Plate Resistance.

The experimental evidence on the magnitude of the forces to which a projectile is subjected on penetrating armour plate is almost entirely confined to the static punching results which have just been described. Efforts to deduce the retardation (and hence the retarding force) by obtaining space-time records of a shot in the course of normal penetration of armour have been made in England, America and Germany. In the latter country the experimental method used was that of multiple spark photography giving at most a series of 24 associated values of space and time. This method had already been tried in England (Ref. 67) but discontinued because of its insufficient accuracy. The German results (Ref. 441) are also insufficiently accurate to give more than the order of the force which, as shown by Fig. 30 is readily calculable from critical energy. The difficulties associated with the measurement arise primarily from the extreme brevity of the period during which the retardation acts. The order of this time for a projectile of diameter d inches and a head length equal to its calibre when attacking a one calibre plate may be taken at $d/6\bar{v}$ seconds, where $\bar{v}$ is the mean velocity in f.s. over the travel of $2d$ inches. At a mean velocity of 1000 f.s. the total time for a 1-inch projectile is thus of the order $\frac{1}{6}$ millisecond. Within this period a space-time curve is required of sufficient accuracy to give a representation, on a time basis, of the second derivative. The experimental arrangement first used by the Naval Research Laboratory, Washington (Refs. A12a, 347, 348) is shown in Fig. 35.

The principle of the method was to obtain a record on moving film of the motion of the base of the projectile as it uncovered a narrow slit, which was intensely illuminated by a spark of sufficiently long duration to cover the whole time interval concerned. The target plate was so positioned relative to the slit that the motion recorded would occur while the plate was being penetrated. The image motion and film motion being perpendicular, the slope of the trace at any point was proportional to the instantaneous velocity of the base of the projectile, and hence the rate of change of the slope gave the retardation of the base. The possibility of obtaining a measure of this rate of change depends on obtaining a very high film speed. By using an air turbine as illustrated in Fig. 35, the film being mounted on the inside of the cylindrical portion of the turbine, a film speed of about 560 f.s. was obtained. The turbine was small, about 0·9 inch diameter, but capable of a rotational speed of about 2400 revolutions per second.

From analysis of records taken with this apparatus for impacts of 0·2655 inch diameter projectiles against armour steel (S.T.S.) and mild steel the results given in Table 5 were obtained. (Results, not reproduced here, were also obtained in Ref. 348 for face hardened plate and showed a double peak in the force curve).

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* A curve, derived from firing trials, showing estimated mean pressure for values of t/d from 3 to 8, and referring to plate in the range 230 to 280 B.H.N. is given in Ref. 444. It is roughly consistent as an extension of the appropriate curve of Fig. 30, but shows mean pressure continuing to increase slightly with t/d up to $t/d = 8$.

Table 5.

Projectile dimensions.—Diameter 0·2655 inch ± 0·0001 inch; length 1·040 inch ± 0·005 inch; ogival radius 0·910 inch.

Target properties :—

(*a*). S.T.S. 2 ins. × 2 ins. × ¼ in. Hardness 240 Brinell; ultimate tensile strength 125,000 lb. per square inch.

(*b*). Mild steel 2 ins. × 2 ins. × ¼ in. Hardness 103 Brinell; ultimate tensile strength 50,000 lb per square inch.

S.T.S. Armour		
Striking velocity	Remaining velocity	Maximum force
f.s.	f.s.	lb. wt.
1543	765	13,430
1440	490	14,820
1316	0	14,590
1367	0	14,500

Mild steel		
Striking velocity	Remaining velocity	Maximum force
f.s.	f.s.	lb. wt.
1222	475	9,440
1127	349	8,560
1108	0	8,560

The mean figures obtained from these results are :—

	Max. force tons	Max. pressure p, tons/sq. inch	p/B	t/d
S.T.S.	6·40	115·6	0·482	0·942
Mild steel ...	3·95	71·4	0·693	0·942

Since the ogive used was 3·43 c.r.h., these results are not directly comparable with any of those obtained by static punching. The curves of Fig. 34 or Fig. 31 would forecast lower values. It is not, however, to be concluded on these results alone that there is a dynamic effect increasing the maximum forces found by static experiments. It has been pointed out by several authors (Refs. 291, 295, 298, 308) that the assumption of a perfectly rigid shot leads to significant errors in this method of estimating the retarding force. Simple calculation shows that the time duration of the force acting on the projectile is only a fairly small multiple of its period of longitudinal vibration. Such vibrations which have been recorded after impact (Ref. 365) have, therefore, a considerable effect on the motion of the base in the period under measurement. The head reaction is propagated elastically to the base and its motion is the resultant of a series of waves reflected up and down the shot. Allowance was made for this effect in the later report (Ref. 347) where results are given for S.T.S. armour, mild steel and 24 S.T. aluminium. These results are given in Table 6.

Table 6.

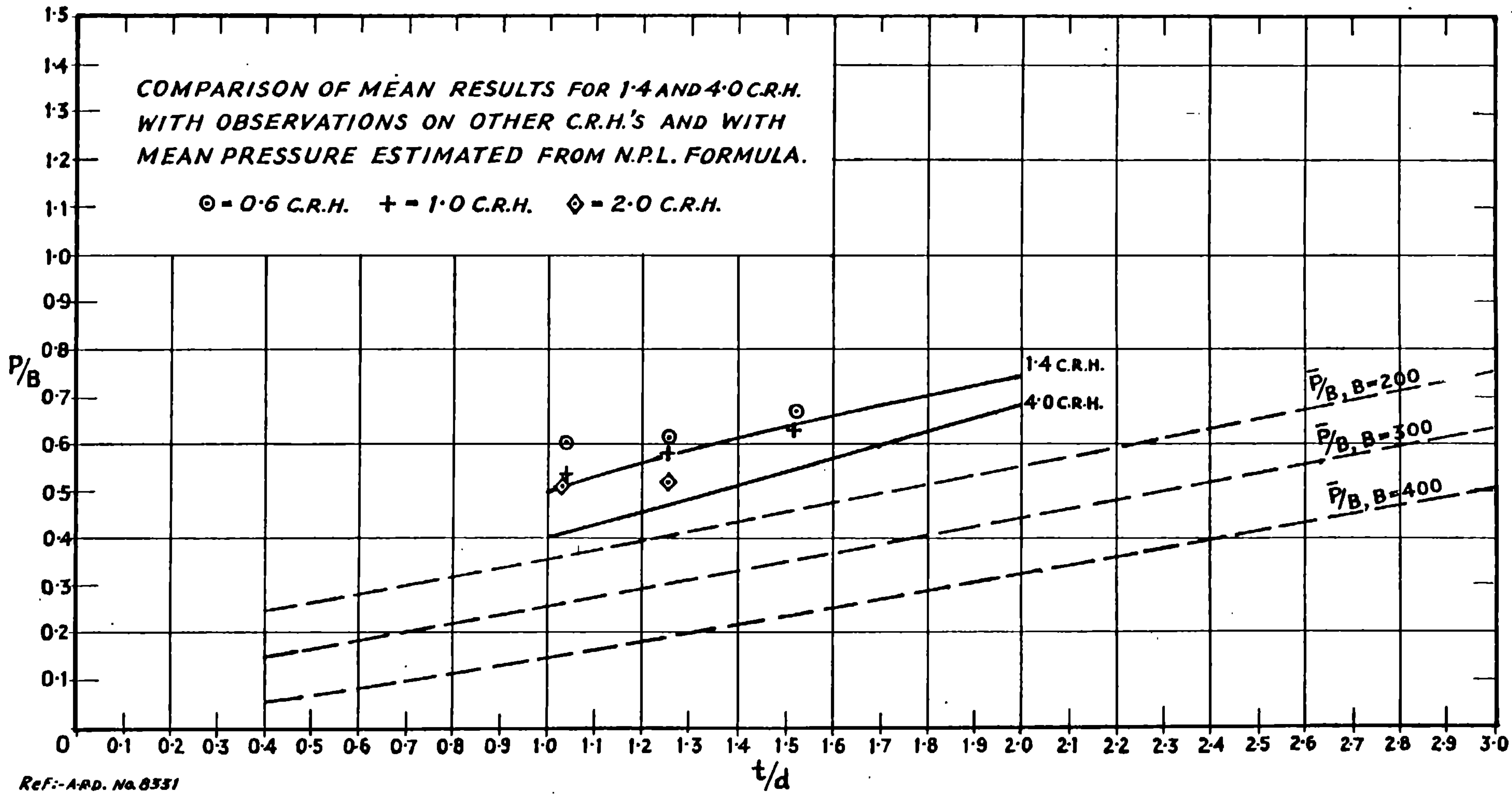
Projectile diameter, $d = 0·1695$ inch. Ogive shape, 2·5 c.r.h.

Material	S.T.S. Armour $B=266$				Mild steel $B=99$				24 S.T. Aluminium
Thickness t ... (inches)	0·500	0·360	0·250	0·1875	0·500	0·350	0·250	0·1875	0·281
Max. force (lb.)	22,800	20,600	14,400	10,400	12,600	11,200	8,600	6,300	8,000
Max. pressure p, (tons/inch^2)	178·4	161·2	112·7	81·4	98·6	87·7	67·3	49·3	62·6
t/d	1·855	1·336	0·928	0·696	1·855	1·299	0·928	0·696	—
p/B	0·671	0·606	0·424	0·306	0·996	0·886	0·680	0·498	—

FIG. 34

ARMOUR PLATE PENETRATION.

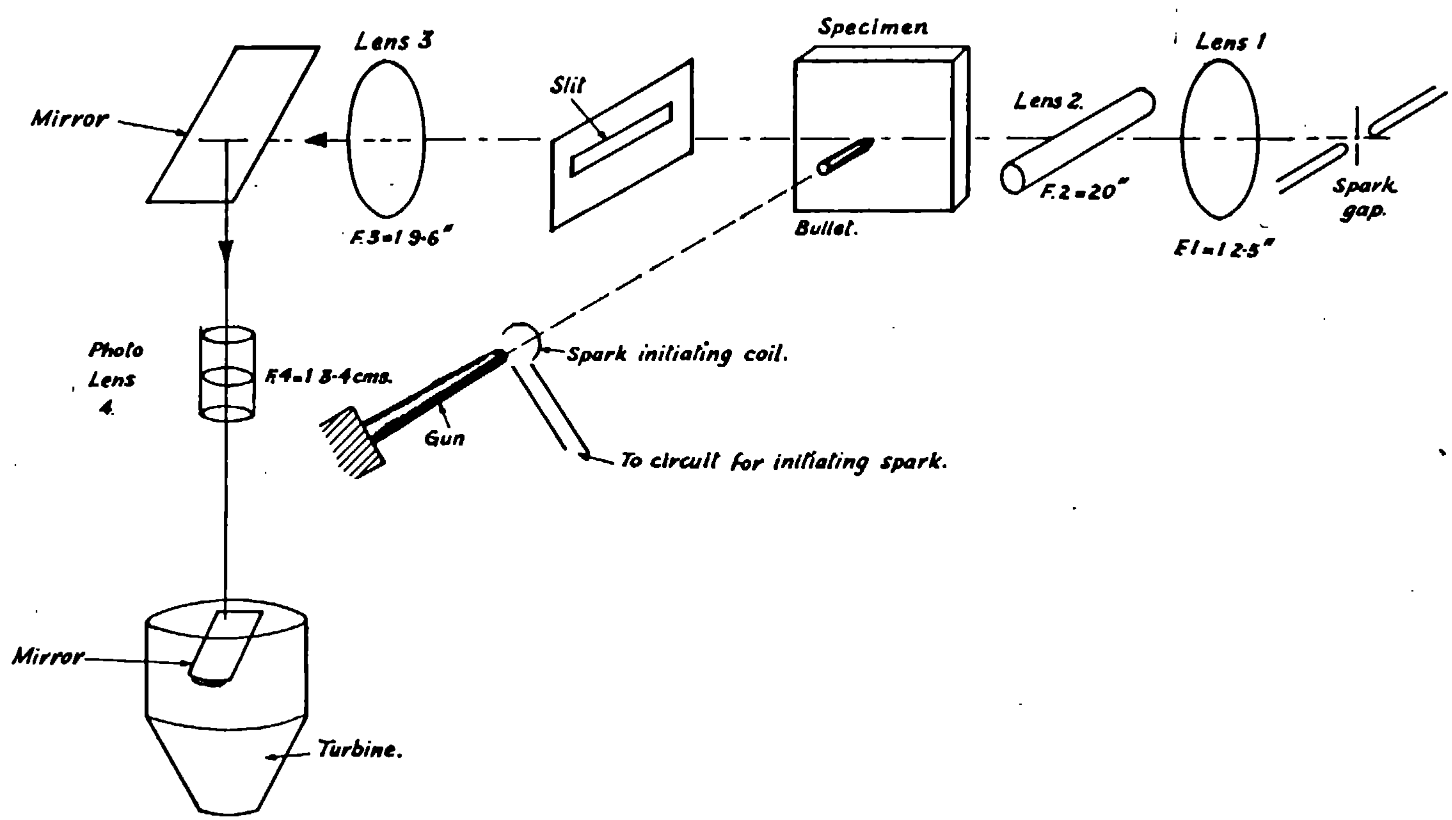
VALUES OF $\dfrac{\text{MAXIMUM PRESSURE}}{\text{BRINELL HARDNESS}}$ FROM STATIC OBSERVATIONS.



Ref:- A.R.D. No. 8331

FIG. 35.

EXPERIMENTAL ARRANGEMENT USED AT NAVAL RESEARCH LABORATORY WASHINGTON TO RECORD THE MOTION OF A PROJECTILE PENETRATING ARMOUR.



An essentially similar method has been used by the Road Research Laboratory (Refs. 283, 285, 406) on the 2-pr. scale. Results are available for only one plate, whose thickness was 41 mm. and hardness 293 Brinell. The peak retardation found when allowance was made for elastic motion was approximately 8.10^6 f.s.^{2} Assuming a shot mass of 2·37 lb., this figure would give $p/B = 0{\cdot}467$ at $t/d = 1{\cdot}031$. These latter values correspond fairly closely with the static results shown in Fig. 32. The American results give pressures which are somewhat higher than those of the nearest head shape used in the static tests. The accuracy of the experimental results needs to be extremely high in order to give even a rough estimate of the variation of the force on the projectile. While, therefore, the results of this experimental technique may be regarded as confirming that there is no change in the order of the peak force between static and dynamic penetrations, it cannot at present be concluded that the small differences found either in the type of force-penetration curve (Refs. 299, 406), or the peak loads are significant.

Other methods of determining the forces exerted during impact have been attempted. The surface markings on shot which rebounded from a normal target were analysed in Ref. 65. and an estimate of the retardation obtained on the assumptions that the spin remained constant and that the marks were caused by material which remained in a plane parallel to the plate surface. Estimates have also been made by housing a free indenter in contact with a copper " target " within a shell and measuring the depth of indentation caused when the shell penetrated an armour plate. An extension of this method is described in Ref. 412. Several steel balls were used as indenters and were located at various initial distances from the copper " target." They thus struck the copper at various times after impact and at various velocities which could be estimated from the depths of indentation. From these velocities a series of mean accelerations was deduced. The difficulties of interpretation in all these experimental methods are considerable and, in consequence, they have so far done no more than to confirm the probable order of the peak force involved.

6. Theoretical Work on the Resistance to Penetration.

6.1. *Pressure required to expand a hole in an infinite medium.*

The experimental evidence on the force to which the projectile is subjected has now been considered and it remains to examine the investigations which have been made from a theoretical aspect. An estimate of the limiting pressure which would be reached in an infinitely thick medium has been given in Ref. A.20 and extended in Ref. 243. In the former case an analysis was made of the work necessary to expand a cylindrical hole in a plastic medium and in the latter the method was extended to a spherical hole. If strain hardening is neglected the first and second methods give respectively the following approximate relations (see Chapter 2, p. 20) :—

$$\text{(i).}\quad p = 3{\cdot}6\, f_y \qquad \text{(ii).}\quad p = 4{\cdot}3\, f_y$$

where f_y is the yield strength of the material. Strain hardening will have the effect of increasing the value of f_y. Using results obtained for large strains under high pressure (Refs. A.42. 178) it is estimated that the value of p will be increased by about 8 per cent. for armour steels and 30 per cent. for mild steel. Hence, for a fairly blunt punch penetrating armour the estimated order of the pressure is about

$$p = 4{\cdot}6\, f_y$$

If the ratio of Brinell number B to yield strength f_y is k

$$p/B = 4{\cdot}6/k$$

or. since $B \doteq 4{\cdot}7\, f_u$ for armour steels, and f_u slightly exceeds f_y.

$$p/B \simeq 0{\cdot}9.$$

This is the limiting ratio of p/B which would be expected for high values of t/d. The curve of Fig. 32 is consistent with a limit of about this value. In the case of mild steel the approximate relation between p and f_y will be

$$p \simeq 5f_y \text{ or } p/B = 5/k.$$

Since k is in the neighbourhood of 2·5 for mild steel the limiting value for p/B in this case is

$$p/B \simeq 2.$$

This result is again consistent with Fig. 32.

Although the static punching results are consistent with the existence of limiting pressures of about the above value, the range of t/d for which observations on lubricated punches are available is not sufficiently large to give direct confirmation of the existence and magnitude of the limit. Experiments on punching in copper, both hardened and annealed, are described in Ref. 404 in which a discussion is also given of the significance hardness tests. Confirmation is obtained of the existence of a limit of the predicted magnitude and it is found that a penetration depth of about 5 diameters is necessary before the limit is reached. It is, however, pointed out that independently of strain hardening the Brinell or other indentation hardness is likely to be less than the pressure at great depths because the constraint on the material is less near the surface. Flow into a lip can, therefore, occur and this provides a relief which is not available deep in the material. This effect has some importance in any attempts to deduce the value of the hardness from the yield strength of the material, but does not affect the arguments given above in which it is discounted by the use of an empirical value for k.

6.2. *Analyses involving the theory of elasticity.*

Other estimates of the resistance and state of stress during penetration have been made by the use of the methods of the theory of elasticity. In Ref. 66 an upper limit is found to the resistance by using the von Mises plasticity condition and considering the resolved forces on conical surfaces axial with the punch. The limits thus found increase as penetration increases and, therefore, give no information on any constant resistance at great depths. The same method applied to finite plates gives results of the order of the static observations. In Refs. 88 and 121 the analytical problem is treated entirely on the basis of elasticity theory. Although the solution is rigorous for the very complex problem of elastic indentation of a solid by a body of revolution its application as an approximation to penetration problems requires an empirical estimate for the appropriate change in "modulus of elasticity" applicable to plastic changes. It does not, therefore, provide an independent estimate for resistance. An investigation of the distribution of stress in a semi-infinite elastic body subjected to a uniform pressure over an internal circular area is given in Ref. 373. The practical application of this analysis is concerned with discing. Again no direct estimate of plate resistance is provided, but the assumption of a pressure equal to 4 or 5 times the yield strength gives consistent values for the stresses under which back failure occurs. The case of wedge indentation into a plastic solid, reducing the problem to two dimensions instead of three, has been considered in Ref. 408. The predicted type of flow was observed when the deformation was made on a lead block scribed with a grid of squares.

7. Modifications to the Forces Acting on the Projectile Due to Dynamic Effects.

Theoretical investigations may be regarded as giving justification for assuming that the static curves shown in Fig. 32 will tend to approach a constant pressure at $t/d = 4$ or 5 and they predict a value for this pressure. They also give an explanation in terms of strain hardening of the divergence of the values of p/B for mild steel compared with armour steel. No very close estimates have yet been given from theoretical considerations of the maximum resistance offered by a finite plate although the total work can be estimated by the methods discussed in Chapter 2. Nevertheless, some indications can be provided from theory of the conditions in which the force is likely to exceed that measured by static methods.

7.1. *Kinetic energy of the target.*

Some increase in resistance in dynamic penetration compared with static might be expected from the necessity in the former case to accelerate the material away from the surface of the advancing projectile. The overall increase in resistance from this cause is known to be small from the fact that, at least up to velocities of about 2500 f.s., the energy required to perforate a plate is not greatly dependent on velocity (see footnote page 34) and that it agrees with the static work. A minimum value of the kinetic energy imparted to the plate is easily calculable from considerations of momentum. If the mass of the projectile is M, and of the plate kM and if the projectile approaches the plate with velocity v_0 and leaves it with velocity v_1, the centre of gravity of the plate must have acquired a velocity $(1/k)\ (v_0 - v_1)$. The ratio of the energy of translation E_p of the plate to the energy E lost by the projectile is thus given by

$$\frac{E_p}{E} = \frac{1}{k}\left(\frac{v_0 - v_1}{v_0^2 - v_1^2}\right)^2 = \frac{1}{k}\left(\frac{v_0 - v_1}{v_0 + v_1}\right) \quad \dots \quad (20)$$

For velocities of attack near the critical velocity, v_1 will be small compared with v_0 and the term in brackets will be near unity. Regarding the target as a disc N times the diameter of the projectile, and the latter as equivalent in volume to a cylinder of diameter d and length $3d$ the value of k is given by

$$k = N^2/3 \; . \; t/d.$$

In the case of a one calibre plate for example the ratio E_p/E could thus be made as great as 5 per cent. only by making the plate less than eight calibres in diameter. The calculation assumes a freely supported target and the effect of constraints on an actual target will clearly be to increase the effective mass and so to decrease the ratio. It is not, however, true that the energy imparted to the plate continues to decrease as its size increases. Equation (20) is correct for translational energy but neglects energy of vibration, and the distribution of energy between vibration and translation depends on the size of the plate. It is obvious that the energy given to the target cannot depend on its size if the time for transmission of waves from the impact position to the boundaries of the plate and back again is greater than the duration of impact. If the velocity of wave propagation is c and the duration of impact T, the energy given to the plate must thus be at least as great as the energy of translation given to a plate of radius approximately $cT/2$. For head length of one calibre T may be taken approximately as $(t+d)\ \dfrac{2}{v_0+v_1}$ leading to

$$\frac{E_p}{E} > \frac{3}{4}\ \frac{v_0^2 - v_1^2}{c^2} \cdot \frac{1}{t/d(1+t/d)^2}$$

If now $v_0^2 - v_1^2$ is taken as v^2, the square of the critical velocity, and the formula $\dfrac{mv^2}{d^3} = C\left(\dfrac{t}{d}\right)^{1.43}$ (10 of Chapter 2) is used, the inequality becomes

$$\frac{E_p}{E} > \frac{3}{4}\ \frac{d^3}{m}\ \frac{C}{c^2}\ \frac{(t/d)^{\cdot 43}}{(1+t/d)^2}$$

With the approximations $\dfrac{3}{4}\ \dfrac{d^3}{m} = 1{\cdot}2$, $C = 10^6$, $c = 10{,}000$ f.s. the formula becomes finally

$$\frac{E_p}{E} > \frac{1{\cdot}2}{100} \; . \; (t/d)^{\cdot 43}\,(1+t/d)^{-2}$$

A one calibre plate of large area relative to the cross section of the projectile thus absorbs at least 0·3 per cent. of the energy as kinetic energy. Its total kinetic energy including the vibrational component, omitted from this calculation, may be much larger. An attempt to calculate the total energy is given in Ref. 163. By approximating for the elasticity of the plate the velocity $\dot{U}$ of the centre of a large thin plate under the action of a normal force F is obtained as

$$\dot{U} = aF$$

$$\text{where } a = \frac{1}{4t^2\rho c}$$

This relation leads to the following value for the ratio E_p/E where E_p is now the total kinetic energy communicated to the plate :—

$$\frac{E_p}{E} = 2am \frac{\int F^2 dt}{\left(\int F dt\right)^2}$$

Making the further approximation $\dfrac{\int F^2 dt}{\left(\int F dt\right)^2} = \dfrac{1}{\tau}$ where τ is the duration of impact

$$\frac{E_p}{E} = \frac{3\pi}{8} \cdot \frac{d}{c\tau} \cdot \left(\frac{d}{t}\right)^2$$

For a one calibre plate, with the same assumptions as above,

$$\tau \simeq \frac{4d}{v_0+v_1} \text{ giving } \frac{E_p}{E} = \frac{3\pi}{32} \cdot \frac{v_0+v_1}{c}$$

where, with the numerical values assumed [$v = \sqrt{(10^6 \, . \, 1{\cdot}6)}$, $c = 10^4$], the ratio at the critical velocity becomes

$$\frac{E_p}{E} = \frac{3\pi}{32}\ \frac{\sqrt{1{\cdot}6}}{10} \simeq {\cdot}037$$

(In Ref. 163, in which the method of analysis is given, an estimate of 18 per cent. is obtained for the energy of the plate. This figure is obtained by assuming a critical velocity of 3000 f.s. for a one calibre plate, the force being assumed to act over the plate thickness only). The ratio obtained above corresponds to a plate energy of about 4 per cent. of the total and is consistent with the estimate of 5 per cent. for a plate eight calibres in diameter. In a plate as small as this the waves will have made about four double journeys to the boundary and back to the centre before the impact is over. The energy is, therefore, likely to have been very largely converted into energy of translation and the proportion remaining as vibrational energy will be relatively small. The reaction between the plate and the projectile will, of course, be modified by the plate motion, but the variation will be small in plates as large as, or larger, than eight calibres in diameter.

It therefore appears that for plate thicknesses in the region of one calibre the kinetic energy of the plate is not likely to exceed about 5 per cent. of the total. Higher proportions may exist in conditions in which the duration of impact is very small, *i.e.*, for considerably thinner plates, or with blunt headed projectiles, or with plates which plug shortly after impact occurs. Analyses of the elastic motion of thin plates from which estimates corresponding with these conditions would be calculable are given in Refs. 102, 139 and 339.

The energy abstracted from the projectile when attack is made at a velocity higher than the critical velocity usually exceeds the critical energy. This fact is sometimes attributed to the energy given to the plug. It is true that the plug is ejected with a velocity at least as great as the remaining velocity of the projectile and that the latter must have supplied the corresponding kinetic energy. As a very rough approximation it is thus possible to attribute the deviation from unity of the coefficient s (pp. 19 and 34) to plug energy. Since, however, the plug must have acquired some velocity to enable the shear fracture strain to be reached, part of this energy is expended whether or not the process extends to fracture. Further, the determination of s must depend on the total energy given to the plate, both vibrational and translational energy being included. Even in the absence of plug formation these effects are reflected in the values of s differing from unity found for petalling plates. From these considerations it is apparent that it is only in a crude way that s can be regarded as calculable from plug energy.

A convention has arisen of expressing trial results, on occasion, in terms of a "Poncelet coefficient" γ (see Chapter 2). It would be possible formally to give the energy due to dynamic effects in terms of γ. The lack of any physical justification for the Poncelet form of the resistance in steel would, however, prevent such calculations from having any significance. The only conditions in which a formula for the resistance of a type $R = kA\left(1 + \frac{1}{2}\gamma\rho\frac{u^2}{p_0}\right)$ can be given a fairly precise physical meaning occur when the size of the hole made in the target increases with velocity. Such conditions arise in steel only at very high velocities of attack and, as a normal consequence, against thick targets. The increase in resistance then occurring through the high velocity is not primarily a result of energy retained in the target as kinetic energy but as plastic deformation in excess of that which would occur at lower velocities. The conditions in which such behaviour will occur are considered in the next section.

7.2. *Cavitation.*

The head shape may be such that the radial velocity imparted to the target material causes it to move away from the projectile axis at a rate which is too high to allow it to be overtaken by the advancing head. Such behaviour is known as "cavitation," and its occurrence is obviously conditioned by the shape of the head. A discussion of the effect is given in Refs. 372, 409. The qualitative effects to be expected are fairly obvious. In the case of a conical head, for example, the flow of the target material at shoulder entry must have a radial component, and even at moderate velocities a displacement must occur in excess of that from which elastic recovery is possible. For ogival heads which meet the parallel body tangentially the existence of the effect will depend on the velocity in relation to the head curvature. With a given head shape there will be a critical velocity below which no cavitation occurs. As this velocity is exceeded the excess diameter of the hole over the projectile diameter will grow, and the region of separation of the target material from the projectile surface will shift towards the nose. Since the projectile velocity decreases as penetration proceeds the excess diameter decreases from the front of the plate and a tapering hole is therefore formed. The effect is well substantiated for ductile materials (Refs. 372, 409), but is within the range of practical importance for armour steels only where very high velocity projectiles are concerned. A discussion of the practical significance in relation to the performance of high velocity tungsten-carbide cores is given in Ref. 372. In considering the implications as regards stresses within the projectile the phase of the motion during which cavitation effects operate becomes important. The largest contribution from the dynamic term in the resistance, regarded as a pressure, must clearly come at the initial stage when the velocity is highest. The local pressure at the tip is therefore augmented even though the target material may not be leaving contact with the head. Maximum pressure in the sense in which it has so far been interpreted will not, however, have been reached. At a penetration of about four or five calibres, at which depth maximum static resistance may be expected, the velocity in most practical cases will have fallen below the cavitation velocity. Where a finite plate is concerned the maximum stress considered as $\frac{4F}{\pi d^2}$, where F is the maximum resistance, is thus likely to be increased by cavitation effects only in those cases where the projectile has a remaining velocity approaching the cavitation velocity. Such cases will be rare, and, in consequence, the practical importance of cavitation effects lies in their concentration of the stress on the nose of the projectile, and the increase they require in total energy rather than in the direct increase of maximum resistance. The cavitation velocity v_c for an ogival head of n c.r.h. penetrating a material which offers a mean resistive pressure p and has a density ρ is given in Ref. 409 as

$$v_c = \sqrt{\frac{2np}{k\rho}}$$

where k is a constant depending on the target material. The approximate value of k for steel is 2·3. A discussion of the value of p as determined from firing trials against thick plates is given in Ref. 444. Using the value $p = 237$ tons per square inch for plate in the range 230—280 B.H.N. the critical cavitation velocity for a 1·4 c.r.h. projectile is thus found as $v_c = 2465$ f.s. Above this velocity an increase in critical energy may be expected through cavitation. This increase is small within the velocity ranges ordinarily used and further, as shown above, does not necessarily cause an increase in maximum resistance. The increase in resistance in the initial stage of penetration is difficult to estimate, because it is mitigated by the surface effect which allows the target material to flow into a lip. If, however, it is taken to be in the same proportion as the increase which would occur deep in the target, a velocity 50 per cent. in excess of the cavitation velocity (*i.e.*, 3700 f.s.) would cause an increase of nearly 20 per cent. in the stress, and a velocity equal to twice the cavitation velocity would give an increase of more than 50 per cent. in the stress. For very high velocity attack a considerable reserve of head strength in the projectile is thus necessary over that which would be calculated from the static load-penetration curves.

8. Compressive Strength Required in Armour Piercing Projectiles.

8.1. *Strength required to withstand the retardation on impact.*

If inertia effects of the target material are neglected the maximum load F encountered during penetration can be closely estimated from Fig. 34. Treating the projectile as a rigid body which is subjected to a maximum retardation of F/M, M being its mass, the maximum stress S_x at any section x not immersed in the plate is thus :—

$$S_x = \frac{M_x F}{A_x M} \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad (21)$$

where M_x = mass of the part of the projectile between x and the base
and A_x = cross-sectional area of the projectile at x.

On these hypotheses the maximum compressive stresses are thus easily calculable for the normal attack of a given target when the dimensions of the projectile are known. As regards compressive stresses it is probable that little error is made in covering the case of oblique attack by taking the plate thickness as $t \sec \theta$ where θ is the angle of attack. This assumption probably overestimates the effective thickness and therefore gives a margin of safety when used to estimate the strength required in the projectile. Calculations on these lines of the stresses anticipated in various standard armour piercing projectiles are given in Ref. 325. In this paper attention is largely directed to the hardness distribution required in the projectile to give it just sufficient strength to withstand the pressures. It is, of course, not necessary to give a hardness gradient to the projectile in order to prevent failure under compression. A uniformly hard shot with sufficient compressive strength in its head would have a greater reserve of body strength under pure compression than a similar shot with its hardness reduced towards the base. The reduced hardness is desirable mainly as a means of conferring greater resistance to fracture under the transverse forces generated in oblique attack.* For a projectile with a solid cylindrical body the calculation will clearly give a hardness falling linearly to zero at the base of the shot. The gradient required is one which ensures that the hardness exceeds the calculated value at every point. A projectile with a cavity will lead to a calculated curve with a form depending on the cavity shape. Again the hardness distribution actually used would not necessarily follow this form, but would conform to the nearest distribution practicable in ensuring that a reserve of hardness existed at every point. In calculations of this type it is not necessary to take hardness distribution as the unknown quantity. Assuming this to be fixed within limits, optimum values for other design parameters may be calculated from the static results. These parameters include shell mass and length, volume and shape of the cavity and the dimensions and mass of the base plug. A discussion of the method by which optimum values of some of these parameters may be determined when the others are specified is given in Ref. 375. The problem to which detailed consideration is given in this paper is the determination of the maximum cavity permissible in a shell whose hardness gradient and proof conditions are specified. The numerical cases for the 6-inch C.P.B.C. shell and the 5·25-inch A.P.C. model of the 15-inch A.P.C. shell are solved.

In these problems, which are soluble from the data of the static punching results the conditions considered are those in the body of the projectile. The load is distributed round the head and in this region equation (21) will therefore not apply. In the case of static penetration, as has already been shown, the pressure deep in a very thick plate tends to a value of about 0·9 B ton per square inch where B is the Brinell hardness in kg./mm.². At the surface, *i.e.*, for the initial stages of static penetration, it must necessarily be approximately B kg./mm.² or 0·635 B ton per square inch. The maximum pressure in regions of the head near the tip if dynamic effects are neglected may thus be taken as kB where

$$0{\cdot}635 < k < 0{\cdot}9.$$

If H is the Vickers Diamond Hardness of the projectile head its static compressive strength S is approximately given (Ref. 97) by

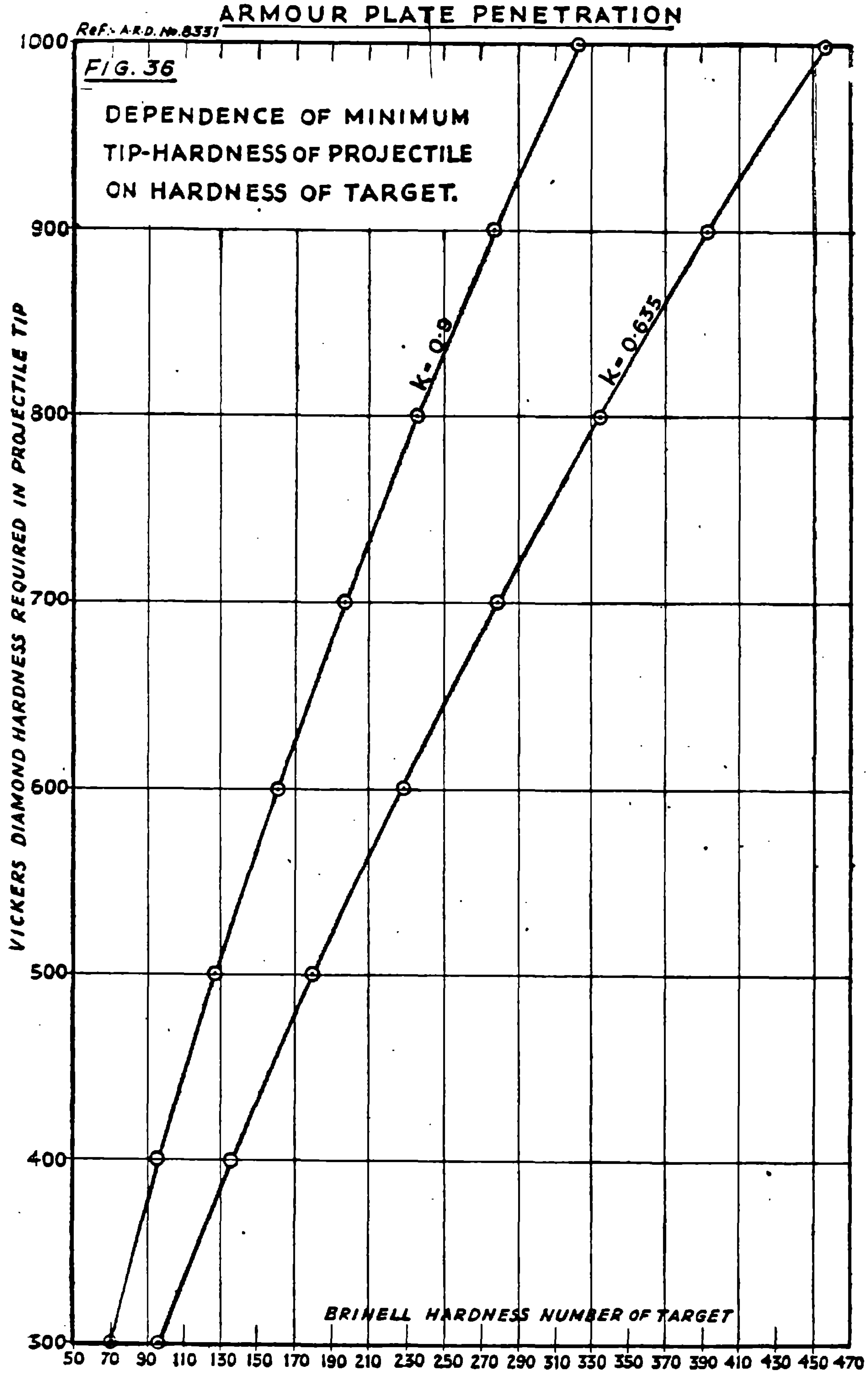
$$S = H(0{\cdot}17 + 0{\cdot}00012H) \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad (22)$$

This formula was deduced from static compression tests on specimens, 0·3 inch diameter and 4 inches in length, of two types of projectile steel (Cr.Mo. as used for the 2-pr. and Ni.Cr. as used for the 25-pr.). The range of hardness investigated was 500 to 900 V.D.H. and equation (22) was fitted to the observed points, which showed a nearly linear relation, by imposing the condition that the curve should pass through the origin. Within the range of validity of equation (22) the minimum hardness H required to prevent compressive failure near the tip of the projectile is thus given by

$$H(0{\cdot}17 + 0{\cdot}00012H) = kB \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad (23)$$

where B is the Brinell hardness of the target. Graphs of this relation for the two values $k = 0{\cdot}635$ and $k = 0{\cdot}9$ are shown in Fig. 36. The danger of compressive failure if the projectile head has inadequate hardness does not strictly apply to the immersed part. Except for conditions in which cavitation effects are significant the surface of the head within the target is, approximately, under hydrostatic pressure. Failure cannot occur under such conditions. The dangerous region is, therefore, in the close neighbourhood of the section immediately outside the plate where full lateral support is not available. Even in this region the front petals of the plate will provide some support and hence, since the cross section of the head is increasing from tip to shoulder, the curves given in Fig. 36 over-estimate the hardness required in the head. The operative curve for most practical cases will be very much closer to that corresponding with $k = 0{\cdot}635$ than that for $k = 0{\cdot}9$. The latter constant assumes a depth of penetration of four of five calibres. Not only will such target thicknesses be rare, but even when they are encountered the head will be immersed, and, except in cavitation conditions, will be receiving support from the plate.

* If impact conditions are such as to give a force of sufficiently short duration at the projectile head, wave propagation will transmit the pressure to the base, and on reflection a tension will be transmitted to the body. In these conditions reduced hardness may prevent tensile failure.



ARMOUR PLATE PENETRATION
Ref:- A.R.D. No. 8331
FIG. 36
DEPENDENCE OF MINIMUM TIP-HARDNESS OF PROJECTILE ON HARDNESS OF TARGET.
k = 0·9
k = 0·635
VICKERS DIAMOND HARDNESS REQUIRED IN PROJECTILE TIP
BRINELL HARDNESS NUMBER OF TARGET
1000
900
800
700
600
500
400
300
50 70 90 110 130 150 170 190 210 230 250 270 290 310 330 350 370 390 410 430 450 470

8.2. *Factors other than retardation affecting strength and hardness requirements.*

There are some factors which modify the conclusions reached above relative to the strength estimated from static results to be necessary in the projectile. These factors are :—

(1). The time occupied by penetration is so short that the assumption that the projectile responds to impressed forces as a rigid body, instead of as an elastic body, is only very approximately true.

(2). The static and dynamic forces are not equal at all stages, especially when cavitation occurs.

(3). The compressive yield strength of the projectile material is not necessarily the same in dynamic as in static conditions.

(4). The assumption that the force over any cross-section is purely normal is only an approximate representation of the actual three dimensional state of stress in the projectile.

8.21. *Elastic propagation of stress in the projectile.*

The effects to be expected as a consequence of (1) are considered in ref. 295. Treating the problem as one dimensional the force-time relation existing at the head will be transmitted down the shot and repeated in the same form at any section until the wave has been reflected at the base and re-transmitted, with change of sign, to the section concerned. There can thus be no immediate adjustment of the stress to the value given by equation (21) and corresponding with " rigid body " treatment. Complete adjustment will never occur, but the error involved in assuming it in comparatively slow penetrations will be very small. Taking the velocity of sound in steel as 17.000 f.s. and the projectile as three calibres in length the head will not receive relief from the reflected wave, *i.e.*, will not begin to receive the mitigation in stress which occurs through its finite length. until the wave has travelled the distance of six calibres from head to base and back. If the projectile velocity is v the penetration before relief arrives at the head is thus $6v/17{,}000$ calibres. At velocities of the order 3000 f.s. more than one calibre penetration will thus have occurred before the relief arrives at the head. The exact consequence of these effects depends on the shape of the force-penetration curve. Qualitatively the result must always be that a greater maximum stress occurs at any section than that given by equation (21). The increase is. however. significant only at moderately high velocities and is more serious near the head. where transmission times are relatively longer. than near the base. Any factor additional to velocity which increases the rate of rise of the force, such as bluntness of the head or hardness of the target will increase the effect. The estimate of requisite head-strength indicated by Fig. 36 is not affected by these considerations since the graphs are based on the assumption that the full head pressure is operative. The body strength must, however, have some reserve over that indicated by equation (21). The amount of this reserve cannot be quoted in general terms. but if the force-penetration curve is known it can be calculated for any specified velocity of attack by the method given in Ref. 295.

8.22. *Dynamic component of resistance.*

The increase in stress due to non-equivalence of static and dynamic penetration has in part been considered (pp. 44, 45). As with the elastic effects in the projectile, the dynamic component of the plate resistance becomes serious only at high velocity. In this case, however, the effect requires an increase in head strength over that shown by Fig. 36 and, in general. no significant increase in body strength compared with that defined by equation (21). The increase in head strength is required to counter the concentration of stress towards the tip as cavitation tends to be, or is established. The smallness of the effect on the body strength as calculated from equation (21) arises as already shown from the fact that when maximum resistance occurs the projectile will usually have lost sufficient velocity to bring the phenomena into the range where the difference between static and dynamic resistance is small.

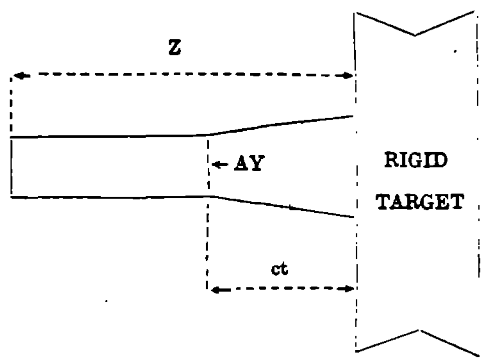
8.23. *Dependence of yield stress on rate of strain.*

The dependence of yield strength on rate of strain mentioned in (3) above has been the subject of many investigations. These show that there is a large dependence in comparatively soft materials, but that the ratio of dynamic to static yield, which is always greater than unity, approaches unity very closely when the material is hard. In one method of investigation (Ref. 1) the test is a tensile one, the specimen being suddenly pulled by the impact of a bullet against a yoke to which it is attached. In a second method (Ref. 41) the specimen is compressed by being itself fired normally against a very hard target. This method of testing is frequently called the " Taylor test " since it, like the first, was originally developed by G. I. Taylor who proposed a simple method of analysis for the problem. In the approximation in which the motion of the target is neglected the two simplifying assumptions are :—

(a). As the plastic deformation, due to the stoppage of the projectile head, travels down the projectile the stress at the boundary of the region not yet plastically deformed is Y, the yield stress which it is required to find;

(b). The plastic boundary travels with a constant velocity c which can be estimated by assuming that the retardation of the rear of the projectile is constant*.

These assumptions are illustrated in Fig. 37.



Let t = time measured from instant of impact of head.
z = distance of base from target at time t.
ρ = density of projectile.
A = cross-sectional area of undeformed projectile.

With assumptions (a) and (b) the equation of motion of the undeformed part of the projectile is

$$A\,\rho\ (z-ct)=AY\ /\ d^2{}_z/dt^2 \qquad \dots \qquad (24)$$

If $y=z-ct$ and x is the value of y when the base comes to rest, *i.e.*, when $\dot{z}=0$, the first integration of (24) gives

$$(dy/dt)^2 - c^2 = 2Y/\rho \log {}^{y}/_{x}$$

If the initial velocity is U and the initial length is L, insertion of these conditions gives :—

$$Y=\tfrac{1}{2}\rho\left(\frac{U^2+2U_c}{\log {}^{L}/x}\right) \qquad \dots \qquad (25)$$

Hence, Y is known in terms of the measurable quantities ρ, U, L, x and the velocity c. The value of the latter obtained from assumption (b) is

$$c=\frac{U}{2}\left(\frac{l_1-x}{L-l_1}\right)\ *$$

where l_1 is the final length of the specimen.

More rigorous analyses of this problem are given in Refs. 181, 238, 242. The similar problem of the sudden propagation of large strains in wires has received considerable attention (Refs. A.23, A.32, A.36, A.37, A.41, A.43, A.44, 113, 168, 173, 227, 357). Many of these papers are concerned with the consequences of the result derived in Ref. A.23 that plastic strains are propagated at a velocity c related to the stress s, strain e, and density ρ by the equation

$$c^2=\frac{1}{\rho}\frac{ds}{de}$$

* Assumption (b) is not consistent with equation (24), but is used only in order to obtain an approximate value for c. If the duration of impact is T, $T=\frac{l_1-x}{c}$, and if the retardation of the base is constant $T=\frac{2(L-l_1)}{U}$. Hence, the quoted value for c is obtained. The approximation tends to underestimate Y.

An elegant method of obtaining this result directly from the one-dimensional equation is given in Ref. 155, and a review of work on the propagation of plastic waves in Ref. 154. Descriptions of practical applications of the Taylor test to the determination of compressive yield stresses are given in the papers from which the results in Table 7 below have been taken :—

Table 7.

Dynamic compressive yield stress, as measured in the Taylor test, compared with static yield.

Reference	Material	Yield tons/sq. in. Static	Yield tons/sq. in. Dynamic	Velocity range f.s.
15	Mild steel	39	68	1300 to 2550
47	" "	24	66	800 to 2450
47	Med. C.	19	60	
47	Ni. Cr.	53	80	
47	Vibrac	74	100	
48	Armour plate	39	77	1400 to 2400
48	" "	37	63	
48	" "	41	77	
48	" "	33	77	
61	Mild steel	(B.H.N. 120)	45	1100 to 2750
61	Armour plate	(B.H.N. 210)	69	
81	Shell steel	20	54	1500 to 2500
81	Armour plate	40	74	
81	" "	60	93	
81	" "	80	112	
83	Shot steel	(V.D.H.218 to 236)	67 to 88	1600 to 2400
136	Mild steel	18	45	600 to 1800
136	Ni. Cr	120	125	

Although the rate of strain in these tests is unknown, it must be high. If the results are regarded as applicable to penetration conditions, they indicate that the ratio of dynamic to static compressive yield decreases from about 3 at a static yield of 20 tons to about 1·3 at 80 tons (Refs. 118 and 135). Since the ratio probably continues to approach unity as the static yield increases, and since the head hardness, as shown by Fig. 36, will almost invariably be required to exceed 400 V.D.H. (87 tons per square inch yield) the conclusions already reached relating to head strength do not require any significant modification to allow for differences in the static and dynamic yield. As regards body hardness, the factor will in many cases permit a degradation in hardness. The amount of the reduction can readily be estimated in any particular case since equation (21) directly specifies the stress the projectile must withstand at any section, and if this is interpreted as dynamic stress the necessary static strength and hence hardness can be found approximately from the results in Table 7.

8.24. *Three-dimensional distribution of stress in the projectile.*

An explicit solution for the complete stress distribution in a body moving under an arbitrary force at one end has not yet been obtained. The approximations so far considered either treat the projectile as rigid or assume one-dimensional propagation of stress along the axis. A closer approximation for the case of a decelerating elastic sphere is given in Ref. 371. The problem considered in this paper is that of a sphere, part of whose surface is subjected to a hydrostatic pressure so that, if it were rigid, it would move with a constant acceleration. The complication of wave effects in the elastic sphere is avoided by assuming a body force acting on each element of mass in the opposite sense to the pressure, thus reducing the problem to a semi-static case. To a close approximation the contours of maximum stress differences in the region of the axis of symmetry are planes perpendicular to the axis, although in their entirety they are necessarily closed surfaces within the sphere. A comparison is given of the axial stress differences thus calculated with the stress which would be calculated, as in equation (4), for a rigid sphere under the same partial hydrostatic pressure. Considerable differences in detail naturally exist and it appears that the "rigid" assumption might sometimes considerably under-estimate the stress. The divergence is, however, likely to be less serious when the calculations are applied to a cylinder instead of a sphere. For the sphere the contours of largest stress difference occur between the stressed area and the centre and enclose a small volume with a nearly plane surface perpendicular to the axis. The necessity for high internal hardness within the projectile head, and the occasional fracture of heads across a plane normal to the axis are probably due to this cause. These considerations will not, however, lead to an estimated hardness requirement exceeding that shown in Fig. 36, since the calculation does not indicate stresses exceeding the applied stress.

9. STRESSES GENERATED IN THE OBLIQUE ATTACK OF ARMOUR.

9.1. *Motion of the projectile through the plate.*

In the field of oblique attack of armour there is at present little exact knowledge of the stresses brought into play within the projectile, but from various experimental observations and simplified theoretical investigations a qualitative idea of the phenomena may be obtained. The initial penetration of an oblique plate by the symmetrical ogival head of a projectile must cause a reaction which is not axial with the projectile and which produces a moment tending to turn the axis further away from the normal to the plate. Further, if the projectile has sufficient energy to perforate, the earlier relief of stress on the part of the head which first breaks the back surface produces a couple of opposite sense and so tends to rotate the projectile towards the normal. The forward progression of the projectile through the plate is thus accompanied by a see-saw motion. This is illustrated in Fig. 38, which shows multiple spark photographs of the penetration at a striking velocity of about 2000 f.s. by a 2-pr. shot of a 17 mm. I.T.80 plate at 30 degrees attack. Other photographs showing the motion of 0·303 inch projectiles penetrating various targets may be seen in Refs. 140 and 142. The extent of the transverse rotational motion clearly depends, amongst other factors, on the velocity of the projectile. At a sufficiently high velocity the time of penetration is so reduced that only a very small angular displacement can develop before the projectile travels beyond the plate. At a velocity in the neighbourhood of the critical velocity the initial turn away from normal may be very large, causing the presentation of a large surface of the projectile to the front face of the plate. At a velocity well below the critical velocity the back surface will not be broken and the rotation due to the first couple will continue, allowing the projectile to skid away from the impact position. Since the resultant magnitude and direction of the reaction in the shot is governed by its disposition relative to the plate there is thus a dependence of stress distribution on velocity of a type which does not occur in normal attack.

9.2. *Bending moment and shearing stress in a rigid rod.*

Although the magnitude of the forces on the head are thus dependent on several factors which cannot easily be estimated it is possible from simple considerations to determine the approximate distribution of stress along the projectile. Let the latter be regarded as a rigid rod subject to forces as illustrated in Fig. 39.

Length of rod $= l$.

Mass per unit length $= s$.

Reaction at head O at time $t = R$ at angle θ to axis of rod.

Forces at section P distant x from O equivalent to :—

(1) a tension T; (2) a shearing force S; (3) a couple Q.

The forces as shown in the larger diagram represent those exerted at P on the section OP. In the inset the forces acting on an element with boundaries at x and $x + \delta x$ are illustrated. The equation for the rotation of this element gives directly the relation :—

$$s = \frac{-\partial Q}{\partial x}$$

FIG. 38.
Multiple spark photographs showing the turning of a 2-pr. projectile during perforation of a thin plate.

FIG. 39.

REACTIONS IN A RIGID ROD UNDER A FORCE AT ONE END.

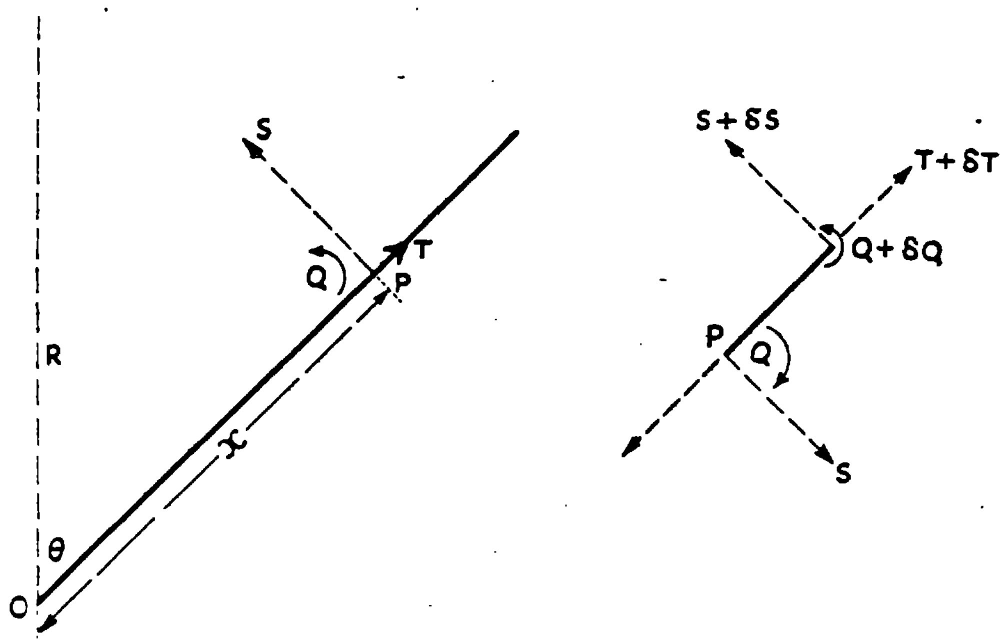
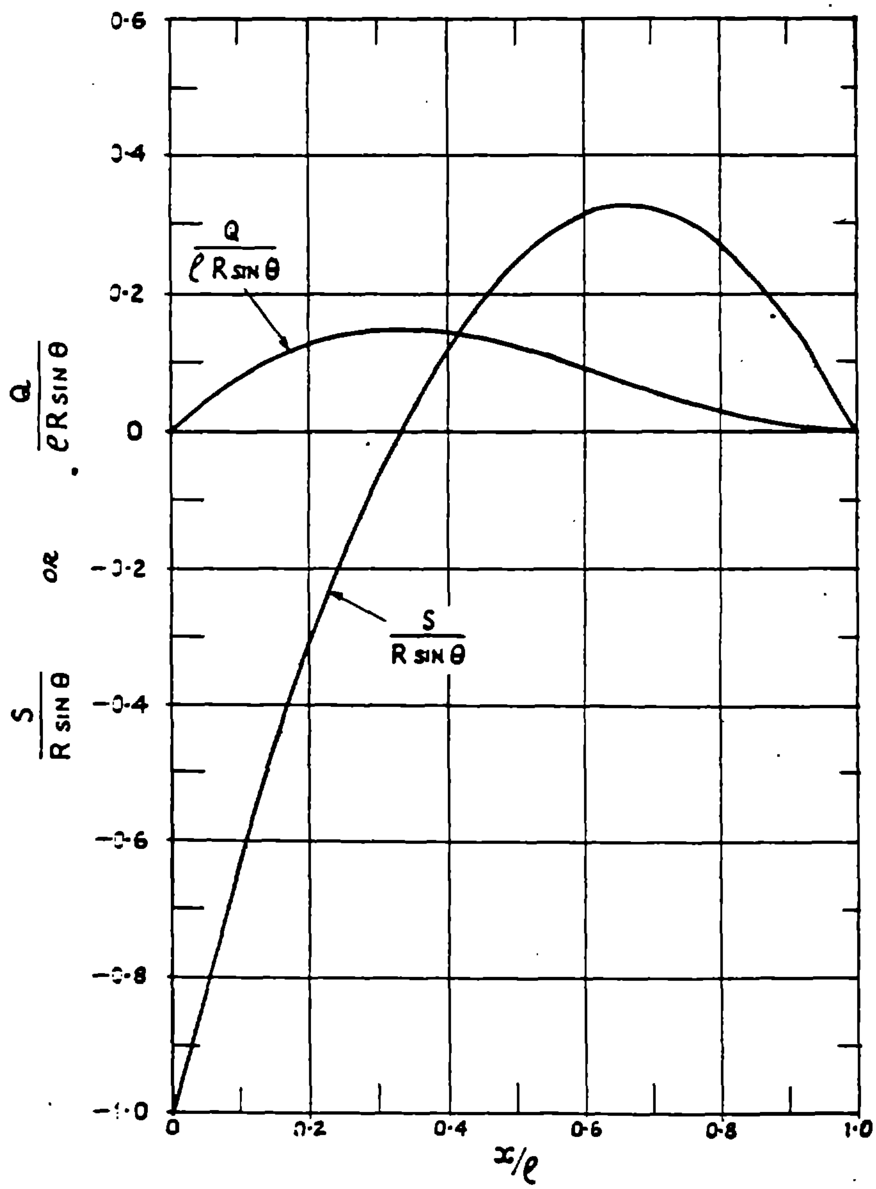


FIG. 40.

VARIATION OF BENDING MOMENT AND SHEARING STRESS ALONG A ROD FREE AT ONE END.

Bending moment $= Q = R \sin\theta \times \left(1 - \frac{x}{\ell}\right)^2$

Shearing stress $= S = R \sin\theta \left(1 - \frac{3x}{\ell}\right)\left(1 - \frac{x}{\ell}\right)$



Ref:- A.R.D. 8831.

The equation for the rotation of the section OP is :—

$$sx \cdot \frac{x^2}{12}\ddot{\theta} = R \sin\theta \cdot \frac{x}{2} - \frac{Sx}{2} - Q$$

and for the whole rod, $sl. \frac{l^2}{12}\ddot{\theta} = R \sin\theta \cdot \frac{l}{2}$

Hence, $x\frac{\partial Q}{\partial x} - 2Q = R \sin\theta \left(\frac{x^3}{l^2} - x\right)$

$$\text{or } Q = R \sin\theta \, . \, x(1-\frac{x}{l})^2$$

The bending moment thus has a maximum at $x=l/3$ and since it is zero at $x=O$ and $x=l$ its greatest value occurs at this point. The shearing stress $S= -\partial Q/\partial x$, is given by :—

$$S= -R\sin\theta(1-4x/l+3x^2/l^2)$$

and thus has a maximum at $x=2l/3$. Its value at this position is $-\frac{R \sin\theta}{3}$ and hence less in absolute value than the corresponding stress at $x=O$. The variation in bending moment and shearing stress along the rod are shown in Fig. 40.

9.3. *Qualitative distribution of stress in a projectile attacking a plate obliquely and its relation to projectile design.*

Although the assumptions that the projectile may be regarded as a uniform rod, and that the equations of a rigid body may be applied, are crude approximations, they are sufficient to indicate that the bending moment is highest in a region set back from the head, and that the shearing stress is highest in the forward part of the projectile. In addition to these forces there will be a compressive stress as already discussed in relation to normal attack. Assuming the projectile to possess sufficient strength to resist failure from the latter stress, the most likely cause of failure arises in the bending moments. To withstand these moments a high "bend strength" is necessary. The bend strength of steel is increased by increasing its ability to undergo plastic deformation after its yield strength has been reached. By giving a hardness gradient to the projectile, ductility is increased from front to rear, and bending moments which would fracture a fully hardened projectile produce only a deformation which still enables most of the energy to be used in perforating the plate. A discussion of the variation of the bend strength of projectile steels with hardness is given in Refs. 416 and 443. Differential hardening provides only a means of increasing resistance to fracture under the stress distribution likely to occur in oblique penetration. It causes no reduction in the applied forces. The magnitude of the latter can, however, be modified by projectile design to a much greater extent in oblique than in normal attack. From the expression for the bending moment it is clear that at a given value of x/l, Q is proportional to l. Hence, the longer the shot the greater is the tendency to fail under the bending stress. This tendency would occur if the reaction R were independent of length. In fact, R will largely be determined by the projected area of the part of the head immersed in the plate and hence by head shape and by the amount of rotation caused by the impact.

9.4. *Theoretical and empirical investigations of oblique attack.*

An estimate from theoretical considerations of the reaction to oblique penetration by a yawed wedge is given in Ref. 445. There is a considerable degree of approximation involved in replacing the three-dimensional problem to one of plane strain by assuming wedge-indentation, but it appears from the solution that it is not legitimate to regard the reaction as a hydrostatic pressure over the immersed part of the head. Static measurements of the reaction in oblique penetration present many experimental difficulties. In any case, static measurements in these conditions would give less direct information than that obtainable in normal penetration because of the modification in mode of penetration due to the transverse rotation of the projectile in actual firing.

In view of the complexity of the conditions, information on the phenomena of oblique penetration is derived almost entirely from empirical investigations. Many trials have been made to determine the best design of projectile for various conditions of oblique attack. Some of these are described in Refs. 351, 352 and 354. In general, these investigations show that blunt nosed projectiles give the better performance at high obliquity. When the conditions are moderately severe, because of hardness, thickness or obliquity of the target, or high velocity of attack it becomes impossible to preserve a monobloc projectile in an integral condition. In these conditions armour piercing caps can greatly improve the performance of the projectile (Ref. 356).

Empirical investigations into problems of oblique attack include those in which multiple targets of special type are concerned. Some of these investigations are described in Chapter 4.

Ref. 110

CHAPTER 4.

COMPLEX TARGETS.

By C. A. Adams.

1. Introduction.

The evidence and conclusions presented in Chapters 1 to 3 relate almost entirely to the effects which occur when an unyawed projectile strikes a single plate. In the present chapter consideration will be given to some of the effects which are introduced when the armour is so situated that the projectile can arrive only after preliminary penetration of structure or auxiliary armour. These conditions will frequently arise in practice. Preliminary armour may be deliberately introduced as part of the defence, as in the skirting plates of some land fighting vehicles and in special dispositions in aircraft. In addition, structural members and ancillary equipment may screen the armour as in armoured bulkheads in ships, submarines and aircraft. If the preliminary targets merely retarded the projectile without causing yaw, deformation, or breakage their effects would in general cause little modification in the conditions of impact, and the effects on striking velocity, and possibly angle of impact, could be estimated. The importance of preliminary impacts arises when the latter modifications are not the only effects introduced. Yaw can have a large effect on the performance of the projectile against armour, and need not necessarily cause a deterioration. Deformation of the projectile, understood in a general sense as including removal of caps, distortion of sheaths, or fracture of the main body, can also produce a large effect and is always disadvantageous to the projectile. When "spaced armour" is used it is, therefore, usually intended to deform or break the projectile, although in special cases the introduction of yaw may be intended when it is known that yaw in the projectile will increase its critical velocity against the target concerned.

2. General Considerations Affecting the Use of Spaced Armour.

If a given quantity of armour is available to protect a given area, and if yaw and deformation are neglected, it would be anticipated that a loss of stopping power would follow from using the armour as two separate plates instead of one integral plate. In the absence of precise information for the critical velocities of thin plates, and of any general formula for the slopes of the lines relating striking and residual energy, no rigorous proof of this effect can be given. A strong indication is, nevertheless, obtainable as follows:—

Consider two plates of thicknesses t_1, t_2 and critical energies E_1, E_2 when under attack by some specified projectile. Let E be the critical energy for a plate of the same material of thickness $t_1 + t_2$. If the critical velocities in each case can be expressed in a modified de Marre type formula then :—

$$E - (E_1 + E_2) = C[(t_1 + t_2)^n - (t_1^n + t_2^n)]$$
$$= C t_2^n[(1 + a)^n - 1 - a^n]$$

where $a = t_1/t_2$ and $n = 1{\cdot}43$.

Since C is a positive constant this expression is positive if $n>1$. This result may be proved, for example, by putting :—

$$f(a) = (1 + a)^n - 1 - a^n$$
$$f^1(a) = n\,(1 + a)^{n-1} - x^{n-1}$$

Thus $f^1(a)$ is positive if $(n - 1)>0$, and since $f(0) = 0$ the difference $E - (E_1 + E_2)$ is positive except at $a = 0$, where it is necessarily zero.

In the particular case $t_1 = t_2$:—

$$\frac{E}{E_1 + E_2} = \frac{(t_1 + t_2)^n}{t_1^n + t_2^n} = 2^{n-1} = 1{\cdot}347 \textit{ if } n = 1{\cdot}43$$

Thus, nearly 35 per cent. more energy can be absorbed by the plate if it is used as a single plate instead of two plates each of half the thickness. The facts that the penetration formula is not exactly true, and that an over-matched plate does not abstract exactly the critical energy from an attacking projectile, do not invalidate the conclusion that a loss of protecting power would be caused by dividing the plate if only energy considerations were involved. The result is not surprising when it is remembered that the shear strength over the interior face of the plate is sacrificed when the division is made.

A further point arising in any application of spaced armour is that supports are required for the additional plate and that where weight is an important consideration they may reduce the weight available for the total armour. The deflection introduced by the first plate may also cause a loss of protection. Under oblique attack the projectile on emergence from the first plate may frequently be deflected towards the normal. Hence, if the two plates are parallel, the projectile will, in this respect, be in a more favourable condition to penetrate the rear plate. All these considerations show that the extra complication involved in installing spaced armour can only be justified by a substantial reduction in the projectile's performance by the effects of yaw or deformation.

3. Yaw Caused by Preliminary Targets and its Effects on Penetration.

Any target other than a uniform plate set normal to the line of flight may be expected to generate yaw in a projectile which passes through it. It has been seen (Chapters 1 and 3) that couples are brought into play when an oblique target is traversed, and that although they change in sign during penetration, their resultant is usually not zero. If the preliminary target is hit on or near an edge a further cause of yaw development exists. Any asymmetrical deformation or fracture of sheath or cap of the attacking projectile will also generate yaw. If yaw is developed it may affect penetration of the main armour in two ways: directly by modifying the application of forces to the plate, or indirectly by causing the projectile to break, when, in an unyawed condition, it would have remained whole. A direct effect may be expected from the increase in the projected area of the projectile on the target. Thus if, as in Fig. 41, the axis of the projectile is inclined at an angle α to its line of flight, and if its diameter is d and body length behind the shoulder is l, its projected length perpendicular to the line of flight is approximately $AB = l \sin \alpha + d \cos \alpha$.

Writing the length AB as :—

$$AB = d\,[\,1 + 2 \sin \alpha/2\,(\,l/d \cos \alpha/2 - \sin \alpha/2\,)\,]$$

it is seen that since $l > d$ for all projectiles of ordinary design the projected area is increased by yaw. If the projectile perforated the plate without alteration in presentation an increased critical velocity would be expected corresponding with the increase in the area of displaced plate.

This direct effect might be the dominating factor in cases in which the transverse rotation of the projectile in the course of penetration is negligible, but such cases will be rare. Unless the striking velocity is well in excess of the critical velocity the projectile will be subjected to couples which will either alter the inclination of the axis or produce breakage. The effects to be expected from the couples, at least in the early stages, can be seen from Fig. 42.

If, as illustrated in A of Fig. 42, the plane of the yaw is such that the axis of the projectile is more inclined to the plate normal than it would have been in unyawed flight, the resultant reaction R, at the beginning of penetration will also be more inclined to the normal. The moment about the centre of gravity G will also be increased and the initial ricochet-type of rotation will be intensified. Even if the projectile remains integral under the increased transverse forces the presentation will thus augment the resisting forces and may cause ricochet instead of penetration. Against a thick or hard target the unbalance of the forces is likely to induce deformation or break-up. B of Fig. 42 illustrates a case in which the direction of yaw diminishes the angle between the plate normal and the projectile axis. The reaction R will now become nearer to the normal than it would have been in unyawed attack. In the Fig., the moment would still have the same sign as in case A, but conditions can arise in which the sign is reversed. The tendency to ricochet type of motion can thus be reduced or reversed. Provided the yaw is not too large, a more favourable presentation for penetration thus results and the transverse stresses in the projectile are also reduced. From these considerations it is to be expected that the influence of yaw will vary with the type of problem concerned, the primary factors being the orientation of the yaw plane relative to the armour, and

ARMOUR PLATE PENETRATION.

FIG. 41.

INCREASE IN PROJECTED AREA DUE TO YAW.

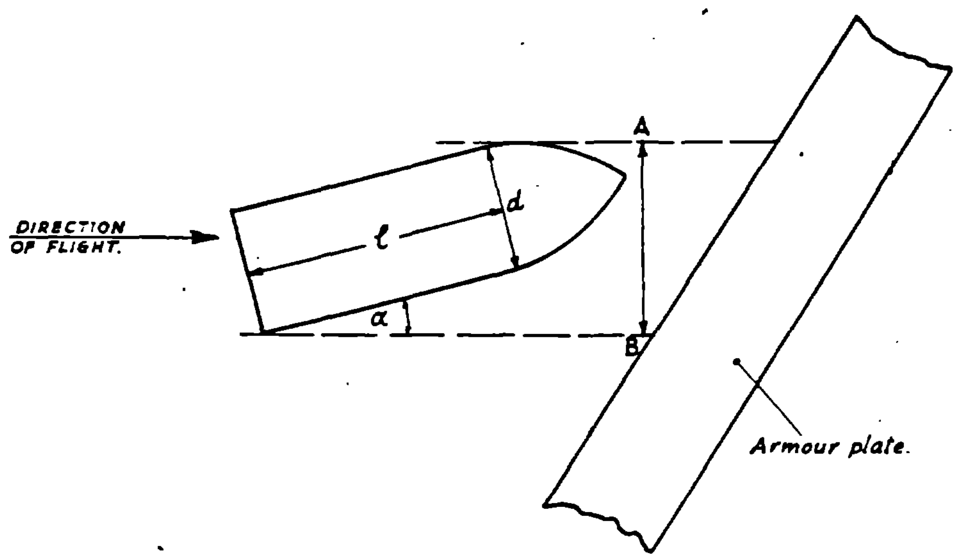
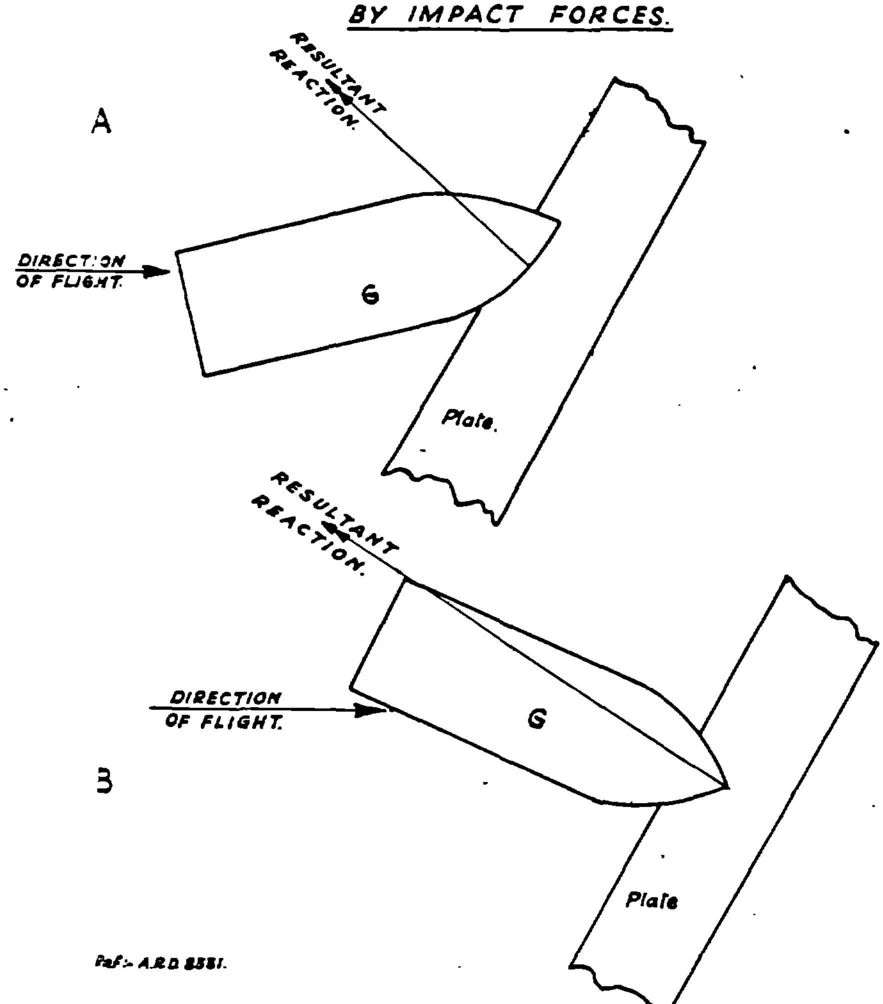


FIG. 42.

INFLUENCE OF YAW ON THE CHANGE IN PRESENTATION CAUSED BY IMPACT FORCES.



Ref:- A.R.D. 8581.

the amplitude of the yaw on impact. The angular velocities possessed by the projectile immediately before impact due to its yawing motion in air are so small compared with those arising on impact that they can be neglected.

3.1. *Yaw development and yaw prevention in relation to aircraft targets.*

In the case of armoured bombers it is possible to a large extent to define the conditions of attack. In the late war, tactical considerations of the attack of fighter aircraft on enemy bombers made it reasonable to assume that the bullets would come from a direction astern of the bomber within a cone of fairly small angle, and that the axis of the cone would be nearly perpendicular to the armoured bulkhead protecting the pilot's cabin. From the preceding section and Fig. 42, it is clear that in normal attack yaw must always be disadvantageous to the attacking projectile. Although enemy bombers carried no auxiliary plates for the deliberate introduction of yaw the bullets could arrive at the armour only after penetrating the aircraft skin and various components of structure and apparatus. These impacts apply couples to the bullet and, since there may be a comparatively long flight (up to 17 feet) within the aircraft, between the preliminary impact and that against the armour, the yaw at the armour may have any value up to its maximum. Analysis of the effects of yaw may be found in Refs. A.11, 106—109, 125, 215 and of its causes in Ref. 142. The first group of papers give empirical results for the frequency distribution of yaw against replica aircraft targets, with some results for equipped aircraft, and the results for correlation between yaw at impact and performance against the armour. On the first point, the frequency distribution naturally depends on the calibre and design of the attacking bullet. In general, it was found that performance followed that which would be expected from the projected-area considerations illustrated in Fig. 41 or from the somewhat similar assumption that the effect of a given yaw α is equivalent to that of an increase α in the obliquity of attack.

The specific causes of yaw development in armour-piercing aircraft bullets are discussed in Ref. 142. Whatever components the bullet may hit in its path within the aircraft, it is obvious that it must penetrate the aircraft skin and that because of the direction of attack this penetration must occur at very high obliquity. Impacts against dural of the thickness used on enemy bombers were photographed at very high frequencies by multiple spark apparatus. Previous work had shown that when 0·303 inch ball ammunition was used against such targets, the bullet nose was rotated towards the normal to such an extent that the bullet soon set itself perpendicular to the plane of the target. The photographs had shown that in these conditions the bullet continued to plough through the dural, but that in doing so it was itself cut into two parts. Only the base continued into the aircraft, the separated head flying outside.

The factors governing yaw development of other types of bullet were elucidated from photographic sequences such as those shown in Figs. 43 and 44. The latter figure shows a projectile which is able to traverse the replica target without significant yaw development.

3.2. *Yaw development and its effect on critical velocity in Naval targets.*

The attack of an armoured deck in a warship provides an example where, unlike that of the aircraft, the existence of yaw is almost certain to assist penetration. Just as in the aircraft problem, tactical considerations limit the variability in impact conditions, and it appears on analysis that the conditions of Fig. 42B must obtain. The problem is examined in Refs. 435, 436, where multiple spark photography has in this case been applied to scale models of the naval conditions. The main armour of a battleship is likely to be of the cemented type and is not protected by outer plating. The interior is, however, also protected by an "armoured deck" and if a shell is to reach this deck it must come from above through the upper decks. The distance at which an engagement is likely to be fought defines both the angle of descent and the striking velocity within fairly close limits. Taking the deck as horizontal, the angle between the deck normal and the line of flight is thus known at the first deck and the problem reduces to that of finding, in these conditions, the yaw caused by penetration of the preliminary decks, the deviation and velocity loss, and the influence of these factors on the impact at the armoured deck.

The influence of the preliminary decks on the performance of the projectile against the armoured deck depends on the following three factors:—

(i). In its transit through a thin plate the projectile receives an angular velocity tending to turn its axis towards the plate normal.

(ii). Sufficient space exists between the preliminary decks to allow the angular velocity to develop a significant angular displacement, but the gyroscopic effect from the spin is not sufficiently large in this space to cause much rotation of the axis from the original plane of motion.

(iii). The attack on the armoured deck occurs at high obliquity, so that the strong tendency to ricochet which exists in unyawed attack is reduced by a yaw which brings the projectile axis nearer to the plate normal.

The first two points are illustrated in Fig. 45 (see also Fig. 38, Chapter 3). In the last frame of this sequence the shell has covered less than half the distance between the first and last decks and the inclination of its axis to the original line of flight is already large. This particular target is thicker and harder than the preliminary decks; but the latter produce a similar effect with smaller amplitude. Each preliminary deck gives a rotation in the same sense and the shell, therefore, arrives at the target with a yaw which may be sufficient to make a large difference in the system of forces to which it is then subjected. Fig. 46 shows a sequence in which an initially capped shell has traversed three preliminary decks and arrived at the armoured deck with a yaw large enough to cause it to topple (" Topple " is defined below).

From Fig 42B it can be seen that cases can arise in oblique attack in which small yaws will cause a reaction favouring a ricochet type of motion and large yaws will cause a transverse rotation in the opposite sense. The term " topple " is used to describe this motion in which the axis of the projectile moves towards the normal. It is to be expected that both ricochet and topple represent wastage of energy, and that the most favourable presentation for penetration exists when yaw is such that in the initial stages very little transverse rotation is caused. For some combinations of target thickness and velocity it is thus likely that a range of yaws will exist, within which penetration will be achieved, but outside which failure will occur, by ricochet for the smaller yaws and by topple for the greater. These results were observed against the armoured deck on the model scale.

For a given thickness of armour the range of yaw within which penetration occurs is plainly dependent on striking velocity. If the velocity is too low failure will occur whatever the yaw may be, and if it is sufficiently high the plate will be defeated at all values of the yaw. The results established in the model investigation gave quantitative information for the Naval case.

4. Spaced Armour and Cap-Stripping.

When consideration is given to the use of complex targets in defence, it is found much more advantageous to exploit systems which break or deform the projectile than to depend on induced yaw. This situation arises not only because yaw can sometimes assist the attacking projectile, as in the attack through decks, but because the full yaw amplitude necessarily takes time to develop after the impulse originating it. Hence, to enable a large yaw to exist at the final armour there must either be a large distance between the components of the system, which is impossible in land vehicles, or the preliminary target must give a very large impulse. A substantial part of the total weight would then be absorbed in the initial target and the system would in most circumstances become inefficient for the reasons discussed in Section 2. For an uncapped projectile it is therefore necessary to find whether a comparatively light preliminary plate can be made to cause breakage. For a capped projectile it may suffice to remove the cap, since its efficiency will then be reduced if the main armour is sufficiently hard, or it may be necessary to use both a cap-stripping plate and a breakage plate.

4.1. *Breakage of armour piercing projectiles by thin plates.*

The circumstances in which small and moderate calibre A.P. projectiles can be broken by thin plates are discussed in Refs. 294, 328, 329, 378, 446 and 447. In considering means by which large yaws might be induced in bullets attacking aircraft the rather surprising result was found that comparatively thin targets, through which the projectiles could easily penetrate, would, in certain conditions, invariably break the bullets. These targets are specified in the above reports, where the factors involved are analysed.

The determination of a target which is sufficient to cause breakage represents only part of the task of defining an assembly to defeat the attacking projectile. The total energy of the fragments after penetration of a thin plate is not greatly reduced compared with the initial energy of the shot. It is entirely a matter of experiment to find

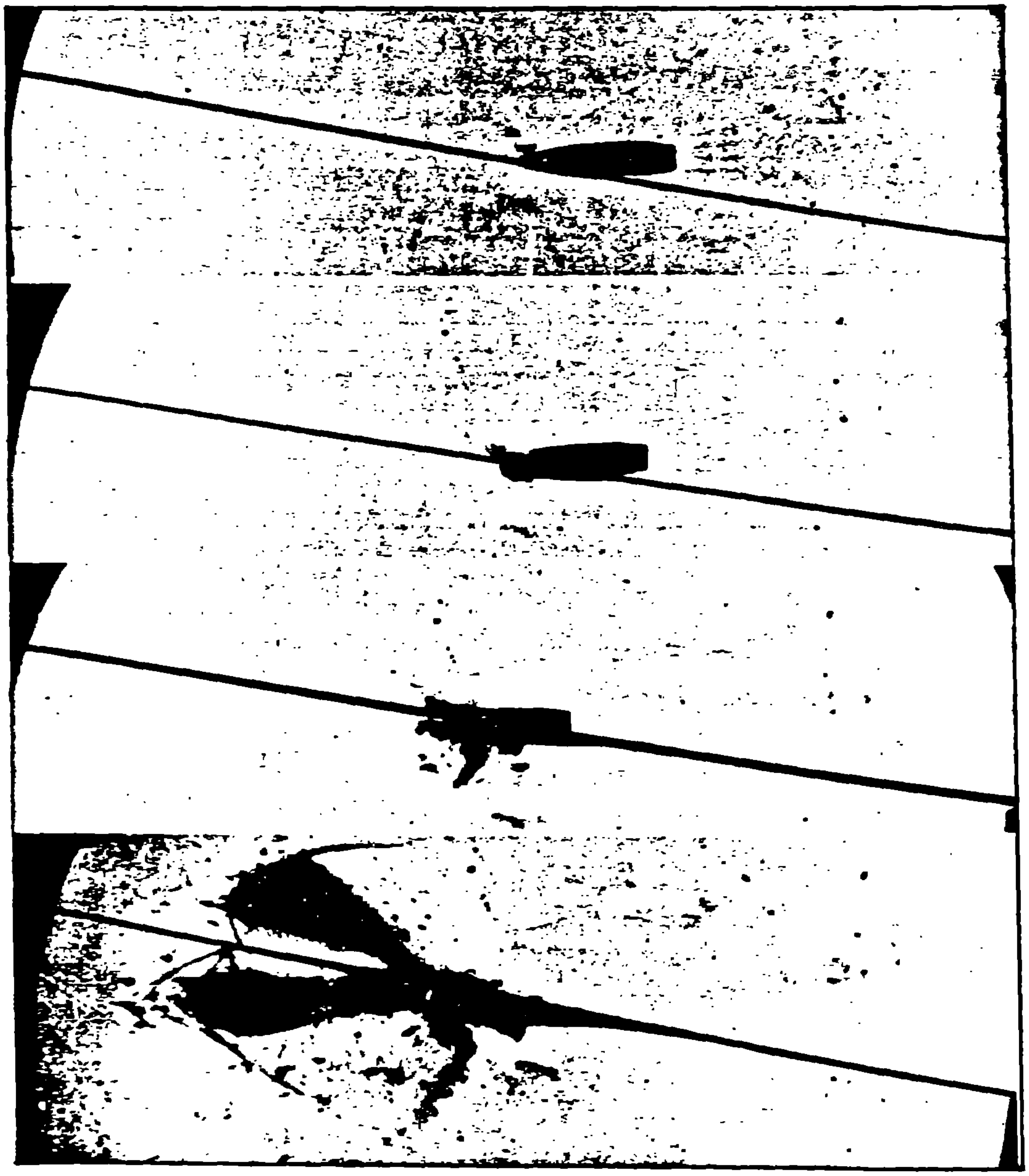


Fig. 43.
Multiple spark photographs showing yaw development as a result of penetration of aircraft skin.

56-A

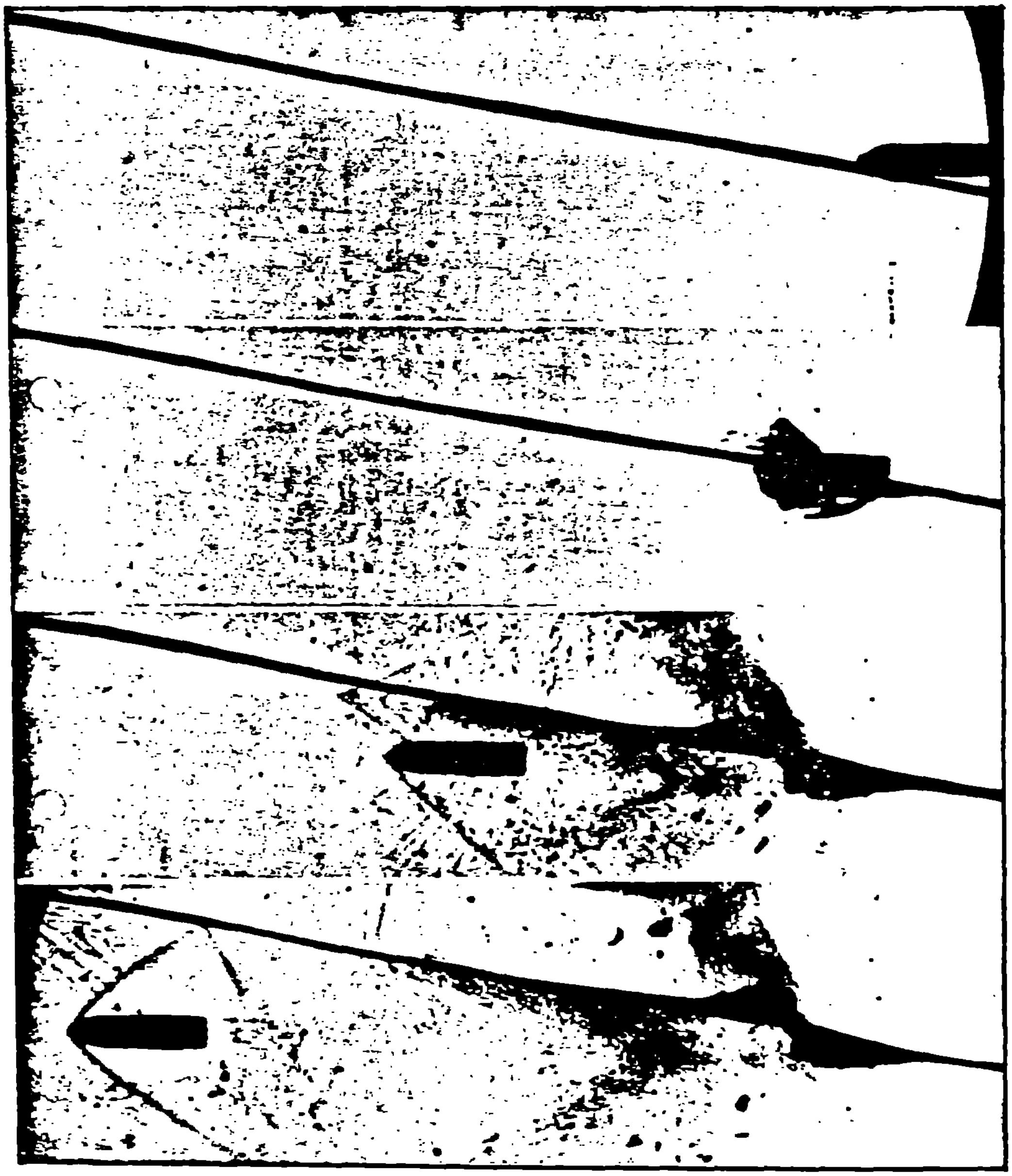


FIG. 44.
Multiple spark photographs of the penetration of aircraft skin by a bullet to a design which prevents yaw development.

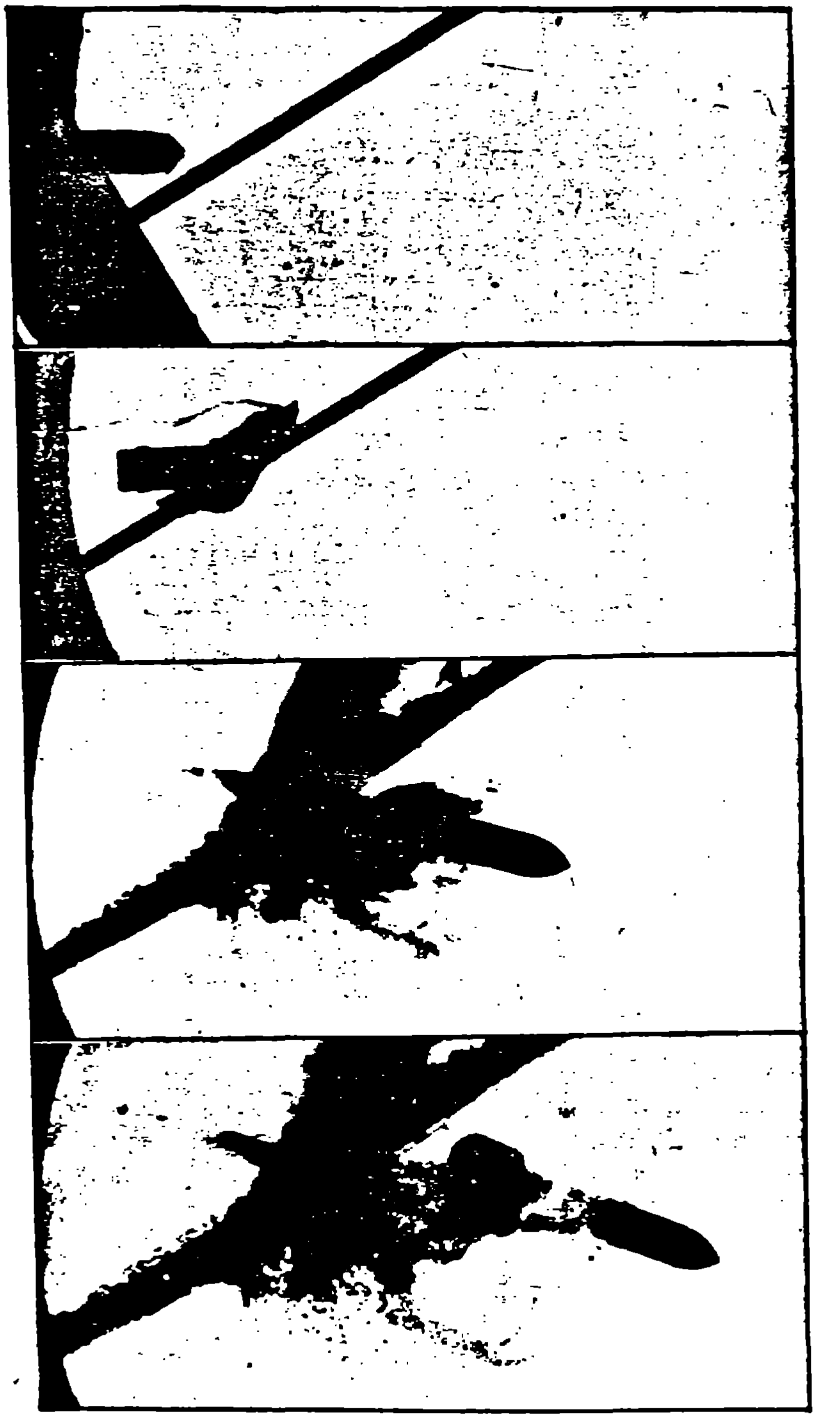


Fig. 45.
Multiple spark photographs showing the resultant transverse rotation of a projectile after passage through an oblique plate.

56-2

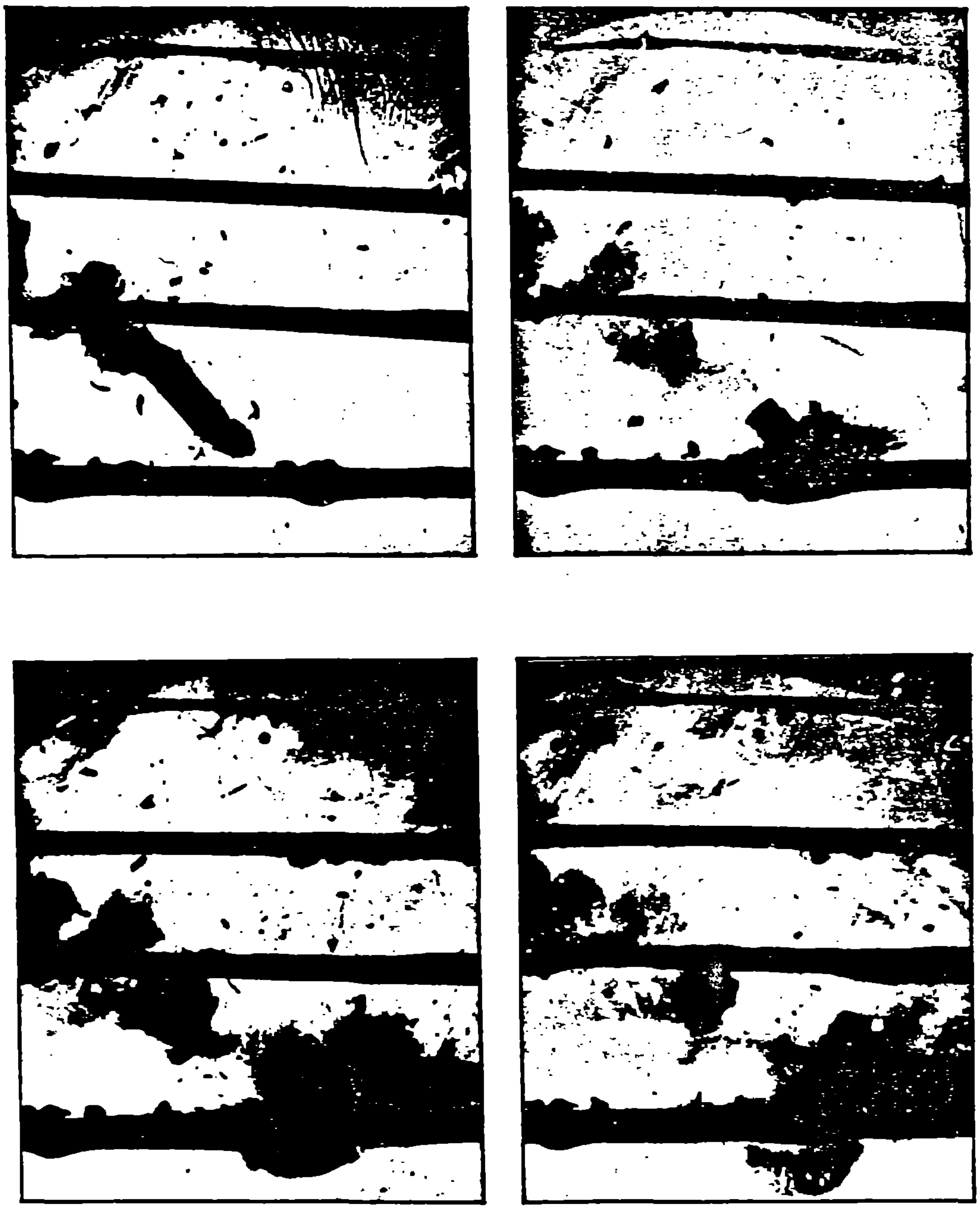


FIG. 46.
Multiple spark photographs on model scale of a shell toppling against an armoured deck after traversing three preliminary decks.

56-d

what is the most efficient and convenient method of stopping the fragments. Empirical investigation provided the necessary information and it was found possible to define a system of spaced plate defence giving significant saving in weight.

The breakage effects first demonstrated on small arms bullets were later shown to be applicable to larger calibre projectiles (Refs. 328, 329 and 447). It might be anticipated that the base-tempering adopted on larger scale projectiles would necessitate a relatively thicker front plate in order to cause breakage. Not only is this not found to be the case, but it appears that smaller values of t/d than those required for small arms can be used in the front plate to give break-up in the larger shot.

For projectiles other than conventional A.P. bullets and monobloc shot the shatter plates may fail to produce breakage. From firings with 20 mm. Mauser A.P. ammunition (Ref. 374) it appears that a projectile with reduced nose hardness may survive an impact which would break harder bullets. It has not been established whether, from the attacking aspect, there would be any overall advantage in using a slightly softer head against a spaced target. Such projectiles would, of course, have a lower performance against single plates. Except in cases where they are known to be the only type used for the attack, as discussed in Ref. 374, the possibility of their use can be neglected. For projectiles of 2-pr. and upwards, however, the possible existence of armour piercing caps must be considered. When such caps are present, thin targets do not break the projectiles. Consideration is therefore required of means by which the cap can be broken or removed before impact occurs against the breakage plate.

4.2. *Removal or breakage of armour piercing caps.*

Since the caps of projectiles may vary in their design, manner of attachment and heat-treatment, and since also the piercing cap may be preceded by a ballistic cap, it is not possible to give any general law governing the behaviour of caps against thin targets. For a given type of projectile it is nevertheless possible to investigate the dependence of cap behaviour on target thickness and obliquity and projectile velocity. Trials can thus be made to find whether, for cap-breakage, there is a critical velocity analogous to that for penetration of a shot through a plate. Experiments designed to find such critical velocities are described in Ref. 327. The experimental method is almost necessarily photographic, since in the absence of photographs it is extremely difficult to determine whether or not the cap has been removed and, if it has, the state of separation or disintegration. The results obtained by photographic methods in Ref. 327 show that the factors affecting "critical velocity for cap-breakage" are similar to those for critical velocities for plate penetration, to the extent that removal or breakage is facilitated by increases in (i) striking velocity, (ii) thickness of plate, (iii) obliquity of attack and (iv) hardness of the target plate.

Examples of photographs of the effects of various targets on caps are shown in Figs. 47, 48 and 49. Fig. 47 (a) shows how perforation may be effected with damage only to the ballistic cap and Fig. 47 (b) shows how the piercing cap may be removed without being broken. Complete cap-breakage is shown in Fig. 47 (c). Effects similar to those of Figs. 47 (b) and (c), but on another scale, are shown in Figs. 48 and 49. Fig. 48 shows a case of cap displacement without breakage. Both in this case and that of Fig. 47 (b) there is a strong probability that the shot would behave as if capped on a rear target and in these cases "de-capping" is not deemed to have occurred. Fig. 49 shows two views taken simultaneously for a case in which the cap is satisfactorily broken. The dome of material in front of the projectile does not derive from the cap, but from the mild steel plate and would give no protection to the projectile nose in an impact against hard armour. The photographic investigations have enabled quantitative conclusions to be drawn on the "critical velocity" relationships applicable to cap-stripping.

5. Application of Spaced Armour to Land Vehicles.

The application of spaced armour to land vehicles is complicated by the severe limitation in available space and by the necessity to protect against a large range of angles of attack. As regards the latter point, it is not possible to restrict consideration to the protection given under normal attack on the grounds that oblique attack favours defence. The protection of a vehicle is not assessed alone on the basis of any complete immunity it may give against a specified projectile up to a specified velocity of attack. The assessment includes the "partial immunity" conferred against other conditions of attack.

It is clear that the variation in performance of spaced plates as angle of attack is changed will differ from that of a single plate. The single plate, or the rear plate in a combination, is either vertical or inclined about a horizontal axis. Similarly, the inclination of the breakage plate must be about a horizontal axis, to ensure that it has an oblique presentation from all directions in a horizontal plane. These are the only directions considered, since angles of descent will be small for ground firing in those conditions under which the attacking projectile has much prospect of success, and attack from aircraft is left out of consideration. The first plate may be expected to deviate the projectile from its original course and so to cause an alteration in the angle of attack of the second plate. The extent of this alteration will depend on the particular conditions of attack. The factors involved are illustrated in Fig. 50.

Let O be the point of impact on the first plate and let $ABCC^1$ be points on a sphere of centre O such that :—

OB is the normal to the second plate (OB is assumed to be horizontal).

OA is the normal to the first plate, $AOB=\alpha$

CO is the original direction of motion making θ with OB (in a horizontal plane).

$C'O$ is the direction of motion after deviation, making $\angle\theta'$ with OB.

Assuming the deviation to be in the plane defined by the original direction of motion and the normal at impact on the first plate, the angle of deviation is $\phi-\phi'$

where :—$\phi=\angle AOC$, $\phi'=\angle AOC'$

From the two spherical triangles ABC, ABC' :—

$$\cos\theta=\cos\alpha\cos\phi+\sin\alpha\sin\phi\cos A$$

$$\cos\theta'=\cos\alpha\cos\phi'+\sin\alpha\sin\phi'\cos A$$

and hence :—

$$\sin\phi\cos\theta'-\sin\phi'\cos\theta=\cos\alpha\sin(\phi-\phi') \quad \ldots \quad \ldots \quad \ldots \quad (26)$$

where, since B is a right angle :—

$$\cos\phi=\cos\alpha\cos\theta \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad \ldots \quad (27)$$

θ' may be expressed directly in terms θ, α and the deflection $(\phi-\phi')$ by combining (26) and (27) in the relation :—

$$\cos\theta'=\cos\theta\cos(\phi-\phi')+\cos\alpha\sin^2\theta\sin(\phi-\phi').\ \times(1-\cos^2\alpha\cos^2\theta)-\tfrac{1}{2} \quad (28)$$

Using (26) and (27), or (28), the angle of attack θ' on the second plate can be found from α, θ and the deviation $\phi-\phi'$. Two simple cases to consider are (i) $\theta=O$, (ii) $\alpha=O$.

(i). $\theta=O$.

In this case in the absence of the first plate the attack would have been normal on the second plate. Substitution in the equation gives $\phi=\alpha$ and $\theta'=\phi-\phi'$, *i.e.*, the angle of attack is increased to the full extent of the deflection, as is obvious from the fact that the whole motion is in the plane ACB.

(ii). $\alpha=O$.

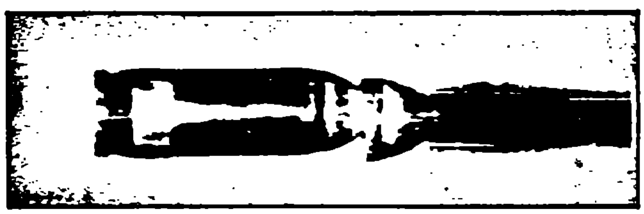
In this case both plates are vertical, $\phi=\theta$ from (27), and from (26) or (28) $\theta'=\theta-(\phi-\phi')$, *i.e.*, the angle of attack is diminished by the full amount of the deflection. This again is obvious since the motion is now all in plane OBC. Any addition or subtraction to the angle of attack intermediate between these two cases is possible, and there is thus in general no simple way of expressing the deflection governed by (26) and (27). It is, however, not surprising that experiment indicates that single plates gain more in immunity than spaced plates as obliquity of attack is increased. A spaced plate assembly is likely to be chosen with reference to its performance under attack along a line normal to the second plate. So far as deflection has any effect the gain is greatest in this condition. As obliquity increases, the gain from this cause diminishes, and at some value alters to a loss. It also appears probable that obliquity has a less marked effect on the penetration of fragments than it has on integral projectiles. From both these causes, therefore, sensitivity of spaced plates to angle of attack is less than that of single plate.

Experimental work on the practicability of using spaced armour on a heavy armoured car is described in Refs. 328, 329. An investigation of the use of thin spaced plates for protection against 0·303 inch A.P. shot is described in Ref. 322. In this application very

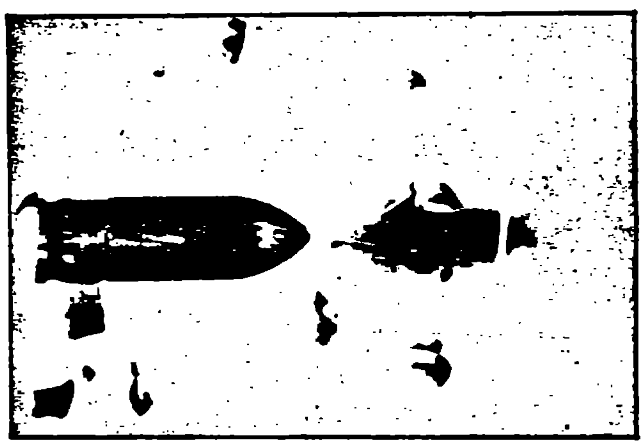
FIG. 47.

Arditron flash photographs of the effects of thin plates on 2-pr. capped projectile.

(a). Ballistic cap deformed.



(b). Piercing cap detached but unbroken.



(c). Piercing cap disintegrated.

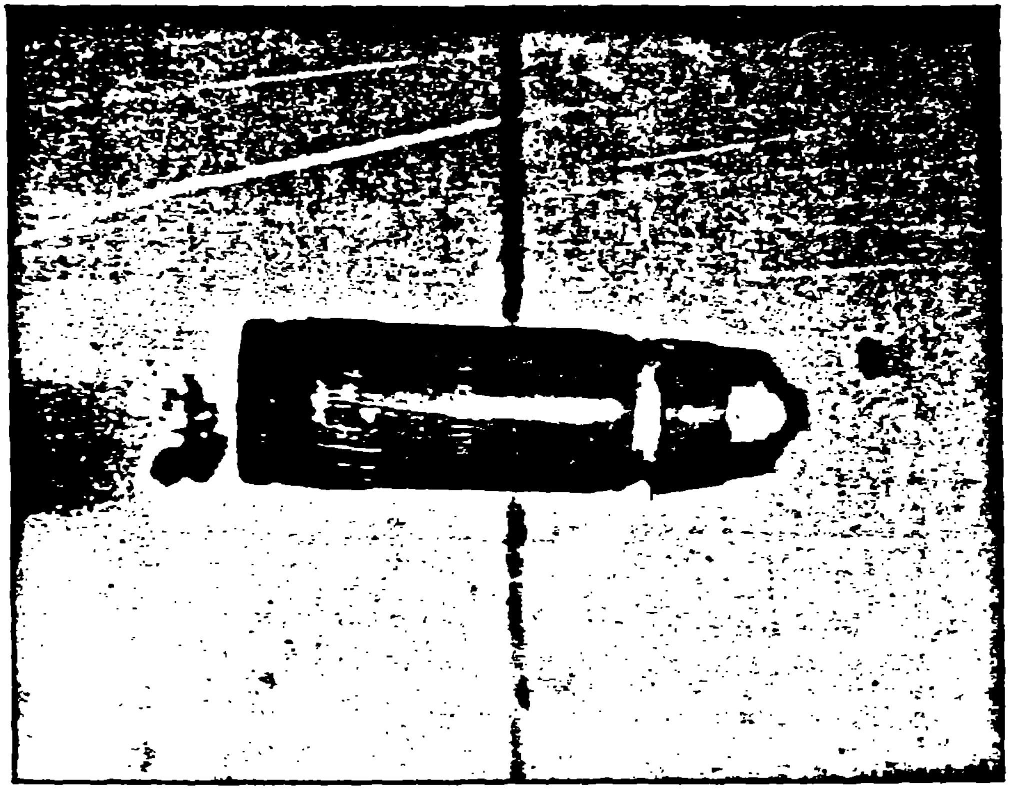


FIG. 48.
Arditron flash photograph of 6-pr. Displacement of cap.

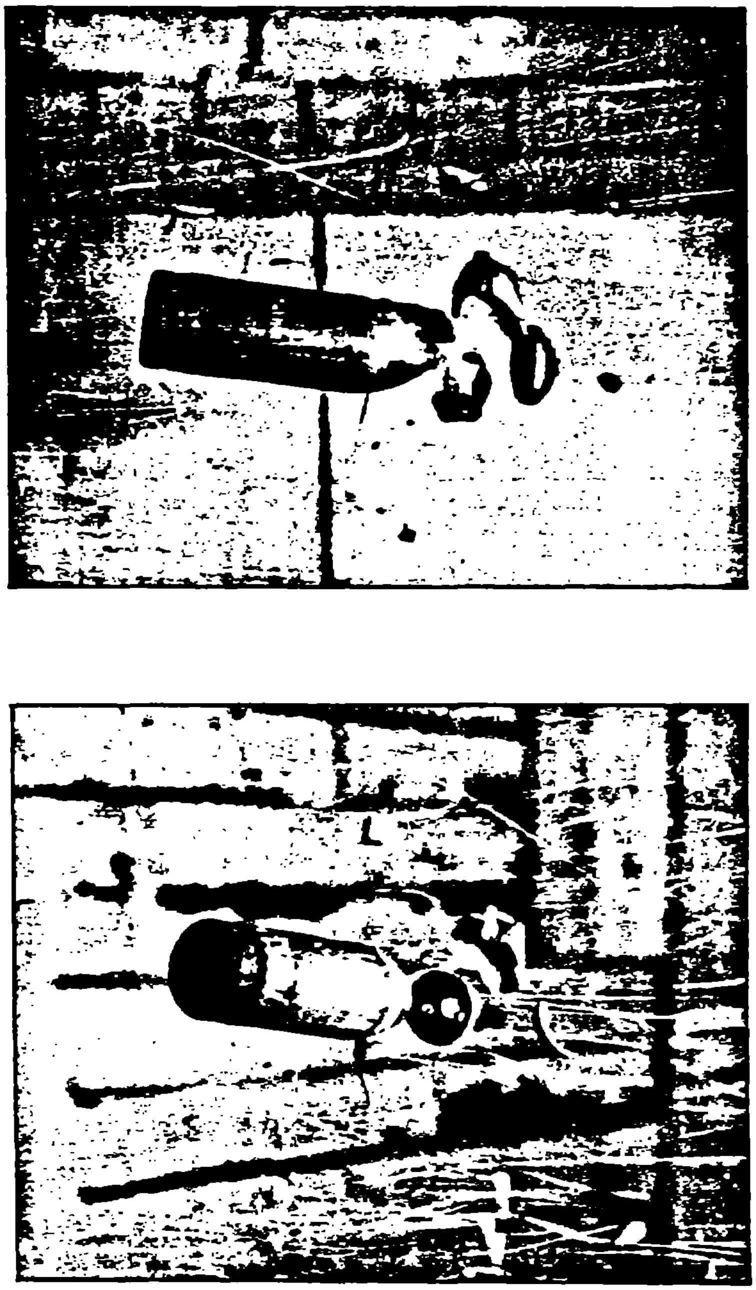
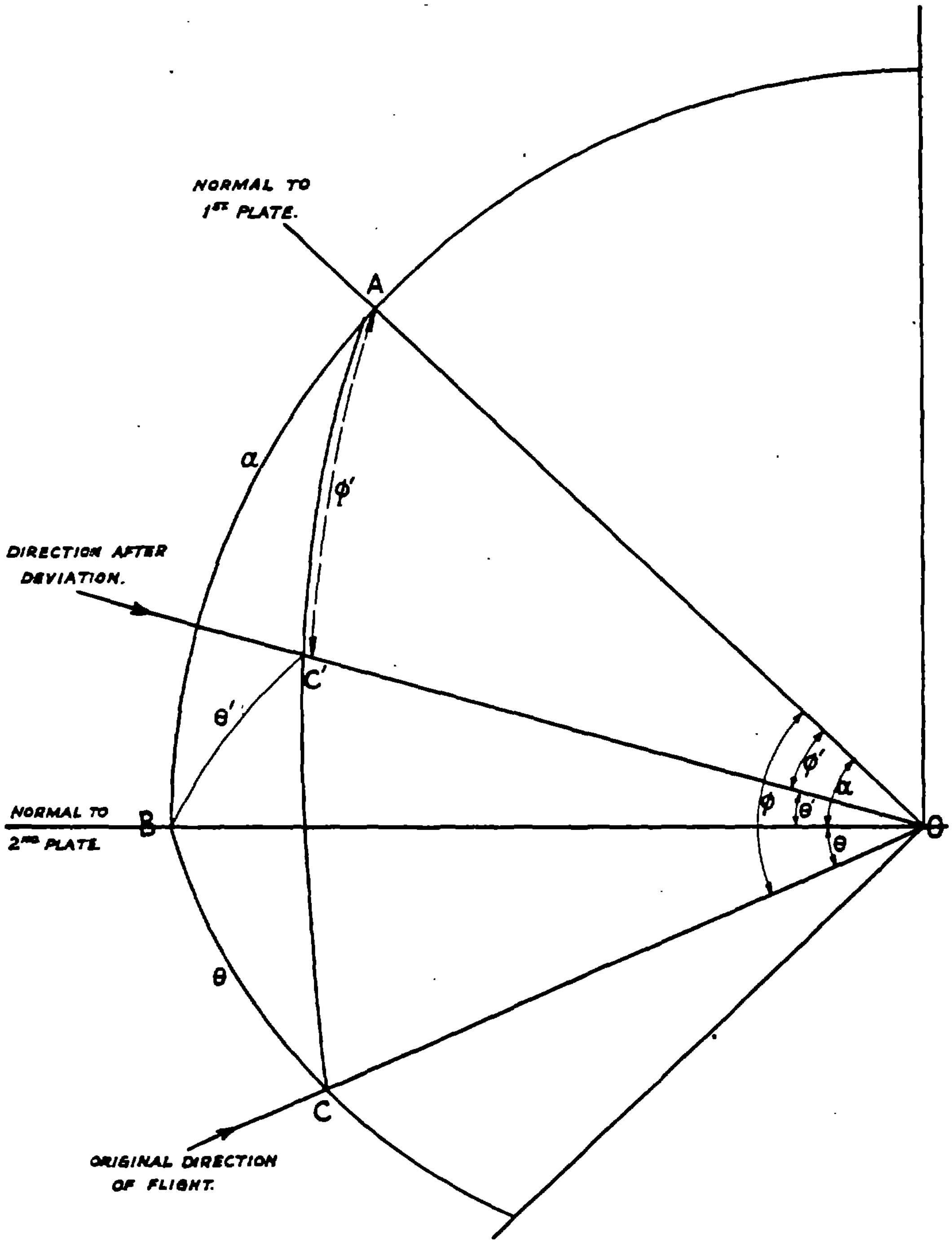


FIG. 49.
Arditron flash photographs of 6-pr.
Two simultaneous views from different aspects showing
breakage of piercing cap.

FIG. 50.

ARMOUR PLATE PENETRATION.

EFFECTS OF DEVIATION CAUSED BY FIRST PLATE ON THE ANGLE OF ATTACK OF THE SECOND PLATE IN OBLIQUE IMPACTS ON SPACED ARMOUR.



Ref:- A.R.D. 8331.

lightly armoured vehicles were considered and the gain from spacing was attributed to yaw. The investigations relevant to heavy armoured cars examine the performance of spaced targets under attack by 2-pr. shot as direction and velocity are varied.

A description is given of observations on the effectiveness of various three-plate dispositions when thicknesses, quality, and inter-plate distances are varied. Specifications are given of some combinatons which represent substantial economy in weight. Nevertheless, the **general conclusion is that full exploitation of spaced-plate principles requires more space than is usually available in land fighting vehicles.**

CHAPTER 5.

CORED PROJECTILES.

By R. Beeching.

1. INTRODUCTION.

As will be clear from previous chapters, a certain minimum energy is necessary to produce a hole of given diameter in any plate, and for plate of any one quality, this minimum energy is a function of plate thickness and hole diameter. Therefore, for success at even " point blank " range, the gun must be capable of giving the shot this minimum amount of kinetic energy, while some surplus to allow for retardation is necessary for defeat of the target at longer ranges. Thus, all the while a gun fires solid, full calibre projectiles of a given energy there is a fixed limit to the amount of armour which it can penetrate, and when shot quality is such that this limit is reached, no further improvement is possible with this type of projectile and a given gun.

During the war, however, there was a continual need to increase penetration performance in relation to gun energy, either to enable existing guns to defeat some new target, or to make it possible to develop a new gun of reasonable size yet capable of defeating comparatively thick armour. Therefore, it was necessary to overcome the limitation imposed by the use of solid, full calibre projectiles. One way of doing this was to use a projectile which employed the available shot energy more economically, by making a smaller hole in the plate. This result was achieved by employing projectiles having a heavy piercing core of reduced calibre, with light surrounding components to build the projectile up to full gun calibre. With such projectiles a large part of the mass, and hence a large part of the kinetic energy, was concentrated in the core, and this was employed to perforate a core diameter hole in the plate (see Table 8, page 63).

For reasons which will become apparent in the next section, successful development of projectiles of this type was dependent upon employment of sintered tungsten carbide as a core material.

2. THE PRINCIPLES OF CORED SHOT DESIGN.

In general, the purpose of using cored projectiles is to obtain greater penetrative power than is possible with the same weapon firing solid steel shot. It is interesting, therefore, to consider what conditions must be satisfied to ensure that this result is achieved.

As the Milne formula shows, the energy necessary to produce a hole in a given plate varies as $d^{1.67}$, where d is the shot calibre. Therefore, if it is assumed that this same relationship holds for the piercing core of a cored projectile, it is evident that a projectile of this type should be capable of penetrating a greater thickness of armour than the corresponding solid shot, provided it has a kinetic energy greater than $\frac{\frac{1}{2}M_1 V_1^2}{r^{1.67}}$

where M_1 is the mass of the solid shot;

V_1 is the velocity of the solid shot; and

r is the ratio of the solid shot diameter to that of the core.

Consider now the problem of giving the core adequate kinetic energy. The cored shot will normally be considerably lighter than the solid shot, even when the core is of high density material. Since, therefore, the velocity obtainable from a given gun is determined approximately by the relationship $\frac{1}{2}\left(M+\frac{C}{2}\right)V^2=\text{constant}$

where M = shot mass;

C = charge weight; and

V = shot velocity;

the lighter shot will have a higher velocity, but will not have quite such a high muzzle energy as the heavier shot, because a greater proportion of the total available energy will

be used in accelerating the propellant gasses. However, for the present purpose it will be assumed that the shot energy available from a given gun is independent of shot weight, although this assumption is favourable to the cored shot.

Suppose the cored shot has a core of mass M_2 and the mass of the other components is m. Then, if we assume shot energy is constant for a given gun, the ratio of the kinetic energy of the core to that of the solid shot, will be

$$\frac{M_2}{M_2+m}$$

If the cored shot is to be superior to the solid shot

$$\frac{M_2}{M_2+m} < \frac{1}{r^{1.57}}.$$

Experience shows that this condition cannot be satisfied with an adequate margin, if the core is of steel, unless the steel core is made very long, because the weight of components necessary to build a projectile of full gun calibre around the core have a mass which is too great in relation to the mass of the core. Further, the core cannot be made very long, both because it would break up during angle attack and because it would tend to shatter. This is the main reason why a high density core is necessary.

If the weight of the outer components of a cored shot could be made very small, then, apart from the reduction of gun efficiency as shot weight falls and velocity increases, the smaller the core diameter were made, the greater the penetrative performance should become. In practice, however, two factors militate against this. Firstly, the weight of the outer components is by no means negligible, and their weight tends to increase slowly with decrease in core diameter. Since core weight is proportional to the cube of core calibre, it decreases rapidly as the core is reduced. As a result, core energy falls with increasing rapidity with decrease in core size. Secondly, if the core and shot are made very light, and the velocity very high, the retardation due to air resistance becomes large, and shot energy falls rapidly with increase in range. Therefore, there is an optimum range of core calibre in relation to shot calibre, and the best calibre of a heavy tungsten carbide core is found to be around half of the gun calibre.

3. Tungsten Carbide as a Core Material.

Tungsten carbide, sintered either with nickel or cobalt as a binding medium, was adopted as a core material by ourselves and others during the war. Its main virtues are its high density, nearly twice that of steel, and its high hardness and compressive strength.

The high density of the material permits the design of shot having cores of only about half the full shot diameter, yet having about half the total weight of the whole projectile. Further, because the material is so dense, shot with a small calibre core are not so light that they have to be fired at extremely high velocities, as would be the case with shot having a steel core of similar size.

Sintered tungsten carbide of the types used for shot cores has a hardness of around 1000 to 1200 V.D.H. Tests of the compressive strength of such material are difficult. Nevertheless, carbide cores of the hardness mentioned, appear to possess a much higher compressive strength than that of hardened shot steel (V.D.H. 850). In any case, it is found that such cores do not shatter when fired against plates many calibres thick, at striking velocities of 4500 f.s. and over.

The main adverse characteristic of the sintered cores used up to the present time is their brittleness. This can be controlled to some extent by altering the amount and nature of the binder, and by the purity of the powders used, but all types of core used up to the present have been almost completely lacking in ductility.

This brittleness does not matter very much when single plates are attacked at normal incidence. The shot tends to break up before emerging from the plate, but this does not matter if the fragments are not too fine. The break-up may, in fact, spread the lethal effect. At large angles of attack, however, cores do tend to pulverize before emerging from the plate, if they are too brittle.

When they are fired against spaced plate targets, the brittleness of tungsten carbide cores is of greater importance, since the core almost invariably breaks up on penetrating the front plate, and the fragments tend to disperse before striking the second plate. Because of this, it has been found necessary to protect the core with a close fitting steel sheath, in shot intended for the attack of such targets. This has the effect of reducing core break-up and of preventing dispersion of the fragments before the core strikes the second plate.

4. Types of Cored Projectiles.

Three main types of cored projectiles were developed by combatant nations during the war. All of these depended upon the principle of using a heavy, small calibre, core to punch a small hole in the target, with light surrounding components to build up the projectile to bore calibre. Further, British designs of all three types also had a protective steel sheath round the core. The three types were, however, distinguished by the nature of the light surrounding parts.

4.1. *Composite rigid projectiles.*

In the simplest type of cored projectile, commonly known as the composite rigid type, the core is surrounded by light components forming a full calibre projectile, generally having an external form similar to that of a conventional shot or shell. Projectiles of this type leave the gun and travel to the target without change of form.

They have the advantages of relative simplicity and of ready interchangeability with other forms of ammunition. On the other hand, since they are very light in relation to their total cross sectional area, they suffer severe retardation and loss of energy at long ranges.

4.2. *Squeeze bore projectiles.*

To overcome the disadvantage of poor external ballistics associated with composite rigid projectiles, cored projectiles were developed which could be swaged down to a smaller calibre, either in a taper bore barrel or in a barrel fitted with a squeezing muzzle extension. With projectiles of this type. the outer components were so designed that they would squeeze down readily and give a projectile a good ballistic form and of little more than core diameter.

Such projectiles give very much improved long range performance. as compared with the composite rigid type, but suffer from the disadvantage of not being interchangeable with other forms of ammunition.

4.3. *Sabot projectiles.*

To overcome the disadvantages of both the composite rigid and squeeze bore types of projectiles, a third type of cored shot was developed. This was so arranged that the outer components were discarded as the shot left the gun, leaving a sub-projectile, formed by the core and sheath, to continue its travel to the target. This sub-projectile was of high density and could be given an external form of low drag coefficient.

Such projectiles could readily be interchanged with other ammunition, and gave good long range performance.

4.4. *Comparison of performance of A.P.C.B.C. and cored projectiles.*

The relative performance figures at normal and 30 degrees attack for A.P.C.B.C. shot and the corresponding cored projectiles are shown in Table 8. Comparisons are shown for 17-pr., 6-pr. and 2-pr. projectiles. In the latter case the cored projectiles concerned are of the "Littlejohn" muzzle squeeze type, and in the former two cases of the discarding "Sabot" type.

Table 8.

Comparison of thicknesses of plate perforated by full calibre and sub-calibre projectiles.

Gun	Range	Perforation of homo. plate (mm.)			
		Normal		30°	
		A.P.C.B.C.	A.P.D.S. (Sabot projectile)	A.P.C.B.C.	A.P.D.S. (Sabot projectile)
17-pr.	yds. 0	188	272	151	225
	500	175	250	141	208
	1000	162	232	131	191
	2000	138	192	111	161
6-pr.	0	125	184	100	147
	500	115	165	92	132
	1000	106	146	85	116
	2000	89	113	71	90
			L.J. Mark II squeeze bore		L.J. Mark II squeeze bore
2-pr.	0	81	120	66	100
	500	71	105	58	86
	1000	61	90	50	72
	2000	46	60	38	46

5. Special Characteristics of Plate Penetration by Cored Projectiles.

The foregoing sections of this chapter outline some of the ideas underlying the development of tungsten carbide cored projectiles. In this respect, the treatment accorded to cored projectiles is rather different from the treatment of solid shot in other parts of this volume. It was considered, however, that some explanation of the reasons for using such projectiles was necessary, as they are of recent development and may not be familiar to many readers.

More in keeping with the preceding chapters, is consideration of any peculiarities in the mechanism of plate penetration by cored shot, which is the subject of this section.

So far as armour penetration is concerned, only the core and sheath of existing forms of cored projectiles are of importance. The outer components are either discarded, swaged down to become part of the sheath, or are so light as to make no appreciable contribution to perforation of the plate.

The major differences between the mechanism of plate penetration by tungsten carbide cores and by solid steel shot arise from the very great difference in the ratio of thickness of armour perforated to the diameter of the penetrating body. Thus, while steel shot are seldom capable of penetrating more than two calibres of plate, tungsten carbide cores may penetrate plate eight or more core calibres thick. Because of the high velocities at which carbide cores are fired, and their high density, they normally have striking energies four or five times greater than that which would usually be associated with a solid steel shot of the same calibre as the core. This high energy, together with freedom from shatter, account for the great calibre thickness of plate penetrated.

Because the plate thickness penetrated by a carbide core is so great, the mechanism of penetration is much more akin to that assumed in the "expanding hole" theoretical treatment of the penetration problem, described on pp. 20 and 41, than is the case with steel shot. There is little tendency for the core to form a plug, until perforation is nearly complete, and during most stages of penetration the plate may be regarded as approximating to an infinite mass.

5.1. *Cavitation.*

With steel shot there is evidence that the inertia of the plate material causes the initial stages of penetration to be more difficult under dynamic conditions than during static penetration. With such shot, however, only a small part of the total penetration is associated with sideways displacement of plate material, the major part of the displacement being in the forward direction in association with the tendency to plug formation. Thus, the energy necessary for perforation of a plate is not very dependent upon head form, and may even be reduced by the employment of a blunt head.

In the case of carbide cores, however, the major part of the penetration process is associate with radial displacement of plate material by the shot head. In these circumstances, if the head form is unsuitable and the shot velocity is high, the radial velocity imparted to the plate material may be so great as to cause permanent enlargement of the shot hole, and thus offset some of the advantages of the small cross sectional area of the core.

This effect has been observed with cores of unsuitable, discontinuous, head form. With sheathed cores of good head form, or cores of poor form but relatively low striking velocity, the shot hole is of uniform core diameter all the way through the plate, except for a scooping around the entrance produced by the sheath. A typical section of such a hole is shown in Fig. 51a. When the head form is poor and the velocity high, however, a cavitation effect is produced akin to that produced by a fast-moving body in a fluid, and the shot hole is tapered, with a diameter well above core diameter near the entrance face. Moreover, because the core produces this oversize hole, the sheath does not scoop the face of the plate, but is forced into the space between the core and the walls of the hole. Such a hole is shown in Fig. 51b. The theoretical treatment summarized in Chapter 3 (p. 44) gives conclusions which are in close agreement with observation.

6. Penetration Formulæ for Cored Projectiles.

The penetration formulæ originally derived for steel projectiles are found to fit equally well for carbide cores, for all practical purposes. The formula most commonly used in practice in this country for predicting the performance of this type of projectile, as for steel shot, is the Milne formula. As for steel shot, this gives a good fit with observed results for angles of attack up to 30 degrees, and for the ranges of plate thickness and velocity which are of practical interest. The only difference is that the constant (C) in the formula tends to be rather smaller for carbide cores than for steel shot, particularly against plates of high t/d ratio. This means that the carbide core needs rather less energy than would be expected from experience with steel shot fired under conditions of lower t/d ratio.

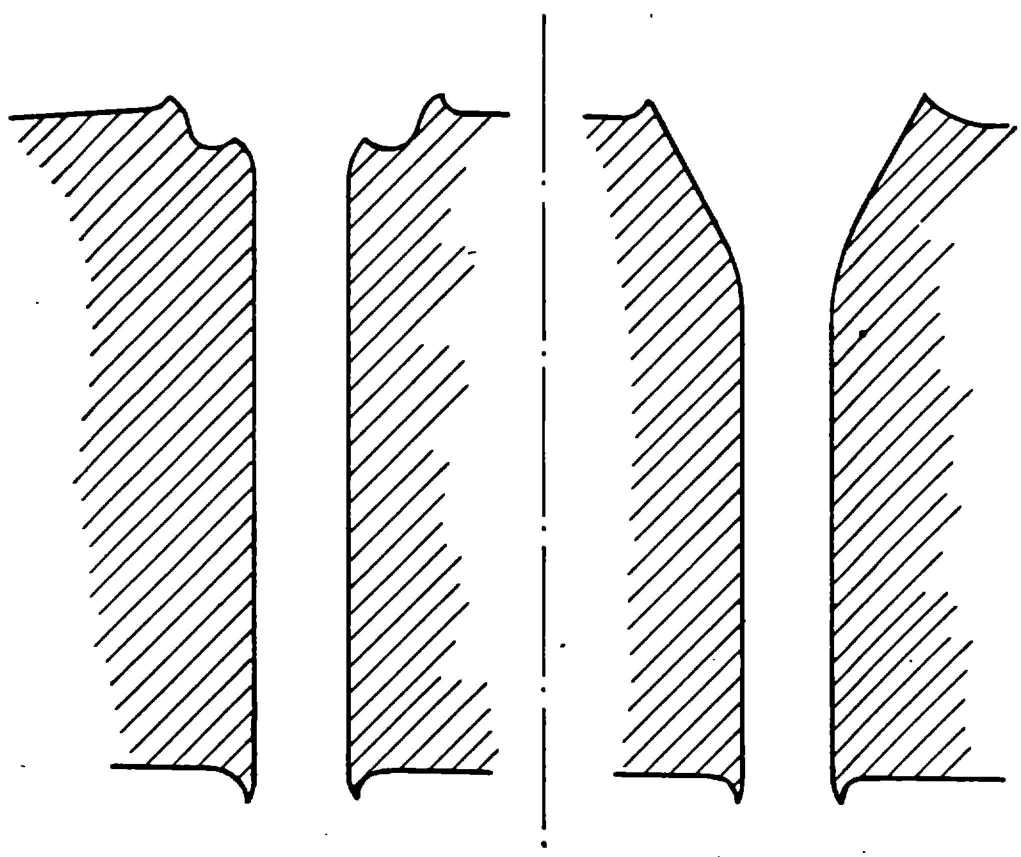
It has been suggested that this is due to a contribution from the steel sheath, which is ignored. This seems improbable, however, particularly as the difference in the constant C is most marked with thick plates. A more probable explanation appears to be that, while the $\left(\frac{t}{d}\right)^{1.43}$ term in the Milne formula implies that the mean resistance to penetration rises with increase in plate thickness, this is not likely to be so marked with cored shot. In-so-far as the middle part of a plate of high t/d may be regarded as approximating to part of an infinite mass, the resistance to penetration over a large part of the penetration must be insensitive to plate thickness. Thus it appears that the index for cored shot should be reduced from 1·43 to more nearly unity, and it is probable that with the thicker plates the value of C has to be reduced to offset the use of the higher value of the index. Over the relatively small ranges of the variables which occur in practice, the observed results fit equally well for a variety of combinations of values of C and the index.

FIG. 51.

ARMOUR PLATE PENETRATION.

SHOWING THE FORM OF HOLE PRODUCED BY CORED SHOT

(a) WHEN NO CAVITATION OCCURS, (b) WITH CAVITATION.



(a) (b)

Ref: A.R.D. 8531.

41-A

APPENDIX I.

LIST OF PAPERS RECEIVED AND SUMMARIZED BY A.P.P. CO-ORDINATING SUB-COMMITTEE.

No.	Title	Authors	Issuing authority	Reference	Date
1	Stress-Strain Relationship in Impact	G. I. Taylor	C.D.R.C.	R.C. 36	September, 1939
2	Impact Experiments with Steel Plates using Bullets and Steel Balls as Projectiles	R. M. Davies	M.O.S. D.S.R.	F72/163	December, 1940
3	The Determination of Static and Dynamic Yield Stresses by a Ball Method	R. M. Davies	M.O.S. D.S.R.	F72/163	March, 1941
4	Examination and Tests of Plates and Projectiles used in Preliminary Splinter Gun Experiments	—	D.S.I.R. M.H.S. N.P.L.	Rept./2499	March, 1940
5	Metallurgical Examination of Fragments from Bombs exploded at Larkhill and Stewartby	—	M.H.S.	Rept./2500	April, 1940
6	Metallurgical Examination of a number of Steel Target Plates and Bomb Fragments	—	M.H.S.	Rept./2505	April, 1940
7	Metallurgical Examination of Targets and Bomb Fragments	—	M.H.S.	Rept./2532	October, 1940
8	The Relationship Between Stress and Strain in the Tensile Impact Test	A. F. C. Brown N. D. G. Vincent	Proc. Inst. Mech. Eng.	Vol. 145, 1941, p.126	June, 1941
9	Penetration of Bullets into Various Types of Material	—	C.D.R.C., B.R.S.	R.C.9	May, 1939
10	The Resistance to Penetration of Concrete Blocks embedded in Clay	—	C.D.R.C., R.R.L.	R.C.89	April, 1940
11	The Velocity of Small Bomb Fragments	—	M.H.S.	A.R.P./135/NSB	December, 1940
12	A Laboratory Method of Projecting Steel Balls of 3/32-in. diameter at High Velocities	—	C.D.R.C.	R.C. 171	December, 1940
13	Fragmentation of German 1 kg. Explosive Incendiary Bomb	—	C.D.R.C.	R.C. 172	January, 1941
14	The Resistance of Plastics, Metals and other Materials to Penetration by 3/32-in. Steel Balls Projected at High Velocity	—	C.D.R.C.	R.C. 182	January, 1941
15	The Compressive Strength of Mild Steel Cylindrical Projectiles at varying Striking Velocities	—	C.D.R.C.	R.C. 186	January, 1941
16	The Resistance of Concrete to Penetration of High Velocity Projectiles Resembling Bomb Splinters	—	C.D.R.C.	R.C. 196	1941
17	Bomb Fragmentation and Degree of Protection afforded by Various Materials	—	C.D.R.C.	R.C. 197	1941
18	Fragmentation of a British 20 pdr. Anti-Personnel Bomb	—	C.D.R.C.	R.C. 198	1941
19	Small Bomb Fragments	E. D. van Rest	C.D.R.C.	R.C. 165	January, 1941

No.	Title	Authors	Issuing authority	Reference	Date
20	The Penetration of Armour Plate in a Static test	G. O. Baines	A.R.D. Woolwich	Met. Rept. 207/41	April, 1941
21	Attack of 0·303-inch Bullets on Petalling Plate	C. A. Adams	A.R.D. Woolwich	—	—
22	Progress Report on the Penetration Behaviour of Light Armour	Dr. H. O'Neill	M.O.S. Met. Com.	A.C. 646 Met. 43	February, 1941
23	Second Progress Report on Penetration Behaviour of Light Armour	—	M.O.S. Met. Com.	A.C. 961 Met. 56	June, 1941
24	High-Speed Tension Tests at Elevated Temperatures—Part 1.	—	—	American Society for Testing Materials. Vol. 40	1940
25	High-Speed Tension Tests at Elevated Temperatures. Parts II and III.	A. Nadai and M. J. Manjoine	—	Journal of Applied Mechanics. Vol. 8	—
26	Experiments on the Penetration of Projectiles	P. Regnauld	(Translated by A.R.P. Dept., Research and Experiments Records)	—	1938
27	Preliminary Report on Austempered Armour Plate	—	Investigation No. 1021. Ore Dressing and Metallurgical Laboratory. Dept. of Mines and Resources, (Ottawa)	—	May, 1941
A 1	Effect of Carbide condition on transition Velocity and Ballistic Properties	—	American information supplied by Imperial and Foreign Liaison. (D.S.R., M.O.S.) W.A.	—	—
A 2	Correlation of Microstructure and Ballistic Properties of Armour Plate	—	" "	—	—
A 3	Structural Models. Part 1—Theory	—	" "	—	—
A 4	Structural Models. Part II Model Investigations of Armour Structures	—	" "	—	—
A 5	Terminal Ballistics	H. P. Robertson	American information supplied by Imperial and Foreign Liaison. (D.S.R., M.O.S.) N.R.C.	S.R. 7/271	January, 1941
A 6	Procedure used at the Naval Proving Ground for the Identification and Adjustment of Armour Penetration and Armour Absorption Functions	—	American information supplied by Imperial and Foreign Liaison	S.R. 7/313	—
A 7	Specification AXS 488 for Rolled Armour Plate	—	" "	S.R. 7/266	August, 1940
A 8	Second Partial Report on Light Armour Investigation	—	U.S. Navy Dept.	S.R. 7/457	March, 1938

No.	Title	Authors	Issuing authority	Reference	Date
A 9	Third Partial Report on Light Armour Investigation	—	U.S. Navy Dept.	S.R. 7/468	April, 1938
A 10	Fourth Partial Report on Light Armour Investigation	—	U.S. Navy Dept.	S.R. 7/497	April, 1938
A 11	Fifth Partial Report on Light Armour Investigation	—	U.S. Navy Dept.	S.R. 7/498	June, 1939
A 12	The Propagation of Shock Waves in Air (I and II)	—	Journal Acoustical Society America, Vol. II p.233	S.R. 7/440	October, 1939
A12A	Sixth Partial Report on Light Armour	—	U.S. Navy Dept.	N.R.L. Rept. 0—1591	February, 1941
A 13	Seventh Partial Report on Light Armour	—	U.S. Navy Dept.	S.R. 7/499 N.R.L. Rept. 1600	March, 1940
A 14	Eighth Partial Report on Light Armour	—	U.S. Navy Dept.	S.R. 7/663 N.R.L. Rept. 0—1745	May, 1941
A 15	Ninth Partial Report on Light Armour	—	U.S. Navy Dept.	S.R. 7/664 N.R.L. Rept. 0—1778	September, 1941
A 16	Ballistic Test of a Boron Carbide Target in Steel	H. D. Smyth and W. Bleakney	N.D.R.C.	S.R. 7/563 Memo. No. A—IM	August, 1941
A 17	On the Probability of Penetration of Armour Plate	L. A. Delsasso, H. P. Robertson and H. D. Smyth	—	S.R. 7/640 Report No. A II	July, 1941
A 18	The Mechanics of Armour Perforation. I Residual Velocity	H. P. Robertson	—	S.R. 7/641 Report No. A. 16	July, 1941
28	Metallurgical Examination of Two Discs of Armour Plate received from the Ordnance Board	—	M.O.S., N.P.L.	WPP/JE/89	July, 1941
29	Examination of 70 mm. Homogeneous Armour Plate attacked by A.P. 2-pr. Mark 111 Shot	—	—	WPP/JM/110	September, 1941
30	Chemical Analysis and Metallurgical Examination of a Piece of Steel Plate and on Small Fragments of the same	—	M.H.S.	THS/JE/2591	October, 1941
31	Chemical Analysis and Metallurgical Examination of Fragments from a Tank and Mine	—	M.H.S.	THS/JE/113	October, 1941
32	The Laws Governing the Penetration of Spherical Steel Projectiles into Cellulose Acetate Sheet—(I) Normal Incidence	—	R.R.L., M.H.S.	A.R.P./249/N.S.B.J.I.	August, 1941
33	The Penetration of Spherical Steel Projectiles into Cellulose Acetate Sheets. (II) Oblique Incidence.	—	—	A.R.P./263/N.S.B.J.I.	September, 1941

No.	Title	Authors	Issuing authority	Reference	Date
34	The Relationship Between Striking Velocity and the Damage caused to Materials by a 3/32 inch Steel Ball	—	M.H.S., R.E.D.	R.C. 232	July, 1941
35	Critical Perforation Velocity of a 3/32 inch Steel Ball fired at Cloth, Manganese Steel and "Tufnol Carp"	B.D. Burnes and S. Zuckerman	C.D.R.C.	R.C. 239	July, 1941
36	Thyraron Control of a Spark Discharge applied to (i) A High Speed Chronograph (ii) Multiple Spark Photography	C.A. Adams	A.R.D. Woolwich	R.D.C. 8340/40 Rpt. A	April, 1941
37	Comparative Examination of Various Silico-Manganese Steel Armour Plate	—	—	Met. Report 380/41	August, 1941
38	Investigation of the Cause of Flaking (Work by Mond Nickel Research)	—	Tech. Co-ord. Committee on Tank Armour	—	August, 1941
39	Manufacture of Cemented Armour Plate in Poland. Foreign Papers	—	Institute of Tech. Res. Gen. Staff, Polish Army	S.R. 7/144	—
40	Some Complexities of Impact Strength	A. V. de Forest	Tech. Pub. No. 1341 Amer. Inst. Min. Met. Engrs.	—	February, 1941
41	Calculation of Yield Stress in a Cylindrical Projectile	G. I. Taylor	C.D.R.C.	R.C. 271	November, 1941
42	Penetration and Residual Velocities	C. Sykes	N.P.L.	A.P.P. Co-ordinating Sub-Committee A.P.P. No. 6	September, 1941
43	Note on Road Research Laboratory Report No. A.R.P./249/N.S.B.J.I.	D. G. Sopwith	—	A.P.P. Co-ordinating Sub-Committee A.P.P. No. 5	October, 1941
44	The Resistance of Cellulose Acetate Sheet of Varying Mechanical Properties to Penetration by Small Projectiles	—	R.R.L., M.H.S.	A.R.P./208/N.S.B.	May, 1941
45	The Effect of Temperature on the Resistance of Cellulose Acetate Plastics and "Perspex" to Penetration by 3/32 inch Steel Balls	—	M.H.S.	R.C. 274	October, 1941
46	Fourth Interim Report on Concrete for Defence Work. The Use of Model Targets and Projectiles in Penetration Tests	—	M.O.S. F/72/Reports/212	M.O.S./26/A.C.W. H.W.W.P. S.R. 5c	October, 1941
47	First Report on the Compressive Yield Strength of Cylindrical Steel Projectiles Fired at Various Striking Velocities—Mild Steel, Medium Carbon Steel, Nickel-Chromium Steel and "Vibrac" Steel	—	M.O.S.	S/72/351 M.O.S./33/ A.C.W.	November, 1941
48	Second Report on the Compressive Yield Strength of Cylindrical Projectiles at Various Striking Velocities—Comparison Between specimens made from the "C" Steel Armour Plates Nos. 3135 and 3140	—	M.O.S.	S/72/351 M.O.S./34/ A.C.W.	November, 1941

No.	Title	Authors	Issuing authority	Reference	Date
49	Analysis of Some Perforation Tests	E. A. Libessart	C.D.R.C. M.H.S.	R.C. 275	November, 1941
50	The Performance of Shot against Plate	E. A. Milne, N. Hinchcliffe	E.B.D., O.B.	E.B.D. Rept. No. 20	November, 1941
51	Addendum to Report on Armour Penetration of 0·303-inch Bullets	—	A.R.D. Woolwich	—	July, 1941
52	Mechanical Properties of Messrs. Firth & Brown's 12-inch "C" Plates Nos. 3135 and 3140	—	A.R.D. Woolwich	Met. Report 584/41	November, 1941
53	Attack of 8 mm. Petalling Plate No. 1899 and 5 mm. Petalling Plate No. 1899A. Part III	G. O. Baines	A.R.D. Woolwich	Physics Section, Ballistics Branch Rept.	October, 1941
54	Attack on Homogeneous Hard Armour by 2-pr.	—	D.T.D.	Exptl. Report A.T. 2	October, 1941
55	Betrachtungen uber die Dynamische Durchdringung. (Foreign papers)	C. Panseri	—	Aluminium Vol. 23, 1941, p. 296	June, 1941
56	Determination of the Resistance to Deformation in Dynamic Upsetting and of the Co-efficient of External Friction for several Structural Steels.	K. Ginzburg, N. Ul'man	—	Iron and Steel Inst., Translation No. 42	September, 1941
A 19	Construction of the N.D.R.C. Experimental Firing Range at Princeton University	H. D. Smyth	N.D.R.C.	Progress Rept. No. A-6 S.R. 7/392	June, 1941
57	Notes on H.A. Bethe's " Theory of Armour Penetration " I. Static Penetration II. Enlargement of a Hole in a Flat Sheet at High Speed	G. I. Taylor	C.D.R.C.	R.C. 279 R.C. 280	November, 1941
58	Report on Distortion of Metal in Penetration by Static Punches and Bullets	—	M.O.S., D.S.R.	F. 72/299	October, 1941
59	First Report on an Investigation of the Mechanical Properties of Selected Armour Plates	—	N.P.L.	Eng. Dept./72K/ P.J.H. ; H.J.T.	December, 1941
60	The Relation between the Penetration Resistance and the Residual Velocity of a Spherical Projectile perforating Plastic Sheets	—	M.O.S., R.R.L.	S/72/351/M.O.S./43/ A.H.D.M., N.S.B.	December, 1941
61	Third Interim Report on the Determination of the Compressive Yield Strength of Steel Projectiles—Effect of Specimen Dimensions on the Results obtained with Mild Steel	—	M.O.S.	F/72/351/M.O.S./45/ A.C.W.	December, 1941
62	On the Analysis of Plating Trials	E. A. Milne, N. Hinchcliffe	E.B.D., O.B.	E.B.D. Rept. No. 22	December, 1941
63	Rubber Bonded Steel Plates for Armour Plate	—	A.R.D. Woolwich	Met. Rept. 642/40	November, 1941
64	Penetration of Steel under Static and Dynamic Conditions	G. O. Baines	A.R.D. Woolwich	R.D./Ball. Rept. 3/41	November, 1941
65	Surface Markings on Projectiles caused by Impact on Armour Plate	W. R. Dean, H. W. Parsons	A.R.D., Woolwich	R.D./Ball. Rept. 4/41	December, 1941

No.	Title	Authors	Issuing authority	Reference	Date
66	An Estimation of the Resistance Offered by a Plastic Medium to the Normal Penetration of a Punch under Static Conditions	E. N. Fox	A.R.D. Woolwich	R.D./Ball. Rept. 14/41	December, 1941
67	Multiple Spark Photographs of the Attack of Armour Plate	C. A. Adams	A.R.D. Woolwich	R.D./Ball. Rept. 19/42	January, 1942
68	Experiments on the Loss of Rotational and Translational Velocity of a Projectile on Passing through a plate	G. O. C. Probert	A.R.D. Woolwich	R.D. 4051/41	January, 1942
69	Notes on the Penetration of Armour Piercing Projectiles	D. Stockdale	D.T.D.	Exptl. Rept. A.T.11	December, 1941
70	The Shatter Effect	D. Stockdale	D.T.D.	Exptl. Rept. A.T.13	January, 1942
71	Ballistic Properties of Armour from PzKw.IV	D. Stockdale	D.T.D.	Exptl. Rept. A.T.17	January, 1942
72	Observations on Homogeneous Armour Plates, (as manufactured in Poland)	Biernacki and Wrazej	Inst. Tech. Res. Gen. Staff, Polish Army	S.R. 7/441	December, 1941
73	Rapid Tension Tests using the Two-Load Method	A. V. de Forest, C. McGregor and A. R. Anderson	—	Amer. Inst. Min. Met. Engrs. Tech. Pub. No. 1393	December, 1941
A 20	Attempt of a Theory of Armour Penetration	H. A. Bethe	—	S.R. 7/399	May, 1941
A 21	Effect of Alloying Elements upon the Physical and Magnetic Properties of Hadfield's Steel for Armour Plate	J. Chipman	N.D.R.C./Serial No. 138	S.R. 7/1214	December, 1941
74	A Report on the X-Ray Examination of Steel Plates after attack by Projectiles and High Explosives	—	D.S.I.R., N.P.L.	C.W./J.M./137	December, 1941
75	Report on the Metallurgical Examination of a Low-Carbon Steel Plate attack by a 2-pr. Shot	—	M.O.S.	W.D.R./J.M./134	January, 1942
76	Report on the Metallurgical Examination of Two 55 mm. Homogeneous Machinable Armour Plates Nos. 2342 and 2343	—	M.O.S.	W.D.R./J.E./153	February, 1942
77	Comments on Two Recent Reports on Formulae for Armour Plate Penetration	D. G. Sopwith	—	A.P.P. Co-ord. Sub. Com. Paper No. 14	February, 1942
78	Summary of Armour Penetration Formulae	D. G. Sopwith	—	A.P.P. Co-ord. Sub. Com. Paper No. 15	February, 1942
79	Data Relating to Shatter	—	—	A.P.P. Co-ord. Sub. Com. Paper No. 23	March, 1942
80	Shatter	—	—	A.P.P. Co-ord. Sub. Com. Paper No. 24	March, 1942
81	"Compressive Yield Strength" of Cylindrical Projectiles out from Various Armour-Piercing Shells and Armour Plares	—	R.R.L.	S/72/351 M.O.S./62/ A.C.W.	February, 1942

No.	Title	Authors	Issuing authority	Reference	Date
82	The Resistance of Mild Steel to Penetration by Steel Balls of Various Sizes	—	M.O.S.	S/72/351 M.O.S./64/ N.S.B. J.I.	February, 1942
83	The " Dynamic " Compressive Strength of Steel from the base of 2-pr. A.P. Shot	—	M.O.S.	S/72/351 M.O.S./74/ A.C.W.	March, 1942
84	Seventh Interim Report on Concrete for Defence Work. Concrete to Resist Multiple 6-pr. Attack. Detailed Results of Model Tests	—	M.O.S.	M.O.S./84/A.C.W. H.W.W.P. F 72 Repts. 212 (S.R. 5o)	April, 1942
85	The " Dynamic " Compressive Yield Strength of Duralumin	—	M.O.S.	M.O.S./97/A.C.W.	April, 1942
86	On the Conduct of Plating Trials, with an Appendix on the Performance of 2-pr. A.P. Shot	E. Milne and N. Hinchcliffe	E.B.D., O.B.	E.B.D. Rept. No. 24	March, 1942
87	Experiments with Carbon-Manganese Steel to Specification D.T.D. 188 to determine its suitability as armour Plating	—	A.R.D. Woolwich	Met. Rept. 551/41	November, 1941
88	Phenomenological Theory of Armour Penetration	J. W. Harding	A.R.D. Woolwich	R.D. Ball. Rept. 21/42	February, 1942
89	Attack of 0·303-inch A.P. on Homogeneous Hard (I.T. 70) and Homogeneous Machineable (I.T. 100) Armour	A. J. McAlpine Downie	D.T.D.	Exptl. Rept. A.T. 14	January, 1942
90	Attack on Homogeneous Hard and Machineable Quality Armour by 15 mm. Besa W. Mark 1 Z	D. Stockdale	D.T.D.	Exptl. Rept. A.T. 19	March, 1942
91	The Effect of Using Capped Shot	D. Stockdale	D.T.D.	Exptl. Rept. A.T. 25	April, 1942
92	Ballistic Properties of Armour from Front of Pz Kw. III.	H. Harris Jones D. Stockdale	D.T.D.	Exptl. Rept. A.T. 26	April, 1942
93	Attack by 2-pr. A.P. on thin Homogeneous Hard and Machineable Quality Armour at Oblique Angles	H. Harris Jones	D.T.D.	Exptl. Rept. A.T. 28	April, 1942
94	Note on the Effect of Low Temperature on the Bullet-Resisting Properties of Homogeneous Hard Armour Plate	F. W. Hill, S. W. Triggs and S. H. Oelman	R.A.E.	Note No. Arm. 33	December, 1941
95	The Heat Treatment of 2-pr. A.P. Shot	—	Mond Nickel, Res. Lab.	R 88	—
96	Armour Plate Improvement as Related to Statistical Analysis of Manufacturing data	—	Dept. of Mines and Resources, Ottawa. Ore Dressing and Metallurgical Labs.	Invest. No. 1144	January, 1942
A 22	A New Type of Accelerometer for High Accelerations	A. V. de Forest and J. R. Benjamin	N.D.R.C.	Rept. on Contract No. N.D.C.rc-199 S.R. 7/1590	January, 1942

No.	Title	Authors	Issuing authority	Reference	Date
A 23	On the Propagation of Plastic Deformation in Solids	T. von Karman	N.D.R.C.	N.D.R. Committee Rept. No. A-29. S.R. 7/1626	January, 1942
97	The Compressive Strength of Two A.P. Shot Materials	—	D.S.I.R., N.P.L.	A.P.P. Co-ord. Sub. Com. Paper No. 25	May, 1942
98	First Progress Report on the Investigation of Scale Effect in Armour Penetration	—	—	A.P.P. Co-ord. Sub. Com. Paper No. 27	May, 1942
99	Note on the Effect of Internal Stresses on the Resistance of Armour Plate to Perforation	—	—	A.P.P. Co-ord. Sub. Com. Paper No. 28	May, 1942
100	Report on the Metallurgical Examination of 2-pr. Shot Fired at Homogeneous Armour Plate	—	—	A.P.P. Co-ord. Sub. Com. Paper No. 29	May, 1942
101	Penetration of Steel under Static and Dynamic Conditions—Parts VI, VII and VIII	G. O. Baines	A.R.D., Woolwich	R.D./Ball./Rept. 22/42	March, 1942
102	Forced Vibrations of an Elastic Plate caused by Normal Impact	W. R. Dean	A.R.D. Woolwich	R.D./Ball./Rept. 24/42	March, 1942
103	An Examination of Beardmore's I.T. 70 Plates which were prone to Spontaneous Cracking	—	A.R.D. Woolwich	Met. Rept. 208/42	April, 1942
104	Examination of Front Hull Inner Plates Assembly from a German Pz. Kw III Tank	—	A.R.D. Woolwich	Met. Rept. 232/42	April, 1942
105	The Ballistic Properties of Armour from Pz. Kw III (A.E.C.)	D. Stockdale	D.T.D.	Exptl. Rept. A.T. 40	June, 1942
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A 25	The Plastic Properties of Metals at High Rates of Strain	F. Seitz, A. W. Lawson and P. Miller	N.D.R.C.	Progress Rept. No. A-41 S.R. 7/1938	April, 1942

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A 27	The New Vertical Firing Chamber and Concrete Laboratory at Princeton University	J. E. Burchard	N.D.R.C.	S.R. 7/2235 Progress Rept. No. A-54	May, 1942
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114	Some Comments on the Road Research Laboratories Report M.O.S./64/N.S.B. J.I. 1942	—	D.S.I.R., N.P.L.	A.P.P. Co-ord. Sub. Com. Paper No. 30	May, 1942
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120	On the Occurrence of "Shatter"	E. A. Milne and H. Hincholiffe	E.B.D., O.B.	E.B.D. Rept. No. 30	October, 1942
121	On the Elastic Stresses Produced by Indenting Thick Plates, with an Application to a Phenomenological Theory of Armour Penetration	J. W. Harding and I. N. Sneddon	A.R.D. Woolwich	R.D./Ball./Rept. 21/42	June, 1942
122	Further Experiments on the Armour Penetration of 0·303-inch Bullets	G. O. Baines	A.R.D. Woolwich	R.D./Ball./Rept. 49/42	July, 1942
123	Penetration of Steel under Static and Dynamic Conditions. Part IX	G. O. Baines	A.R.D. Woolwich	R.D./Ball./Rept. 70/42	October, 1942
124	Some Comments on Gun-Performance Prediction	—	D.T.D.	Exptl. Rept. A.T. 57	September, 1942
125	Penetration of Armour Plate by Yawed 15 mm. Besa A.P. Bullets [R.D. Arm 3(*d*)]	—	M.A.P.	Firing Trial Summary No. 21	August, 1942
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A 31	Addendum to von Karman's Theory of the Propagation of Plastic Deformation in Solids	J. H. Holloman and C. Zener	N.D.R.C. Rept. No. A-37M	S.R. 7/2431	June, 1942
A 32	A Note on von Karman's Theory of the Propagation of Plastic Deformation in Solids	—	N.D.R.C. Memo. No. A-41M	S.R. 7/2432	June, 1942
A 33	Theory of a Two Dimensional Ballistic Pendulum	V. Rojansky	N.D.R.C. Rept. No. A-66	S.R. 7/2567	July, 1942
A 34	Ballistic Tests of Small Armour Plates for the Frankford Arsenal	G. T. Reynolds, R. L. Kramer and W. Bleakney	N.D.R.C. Rept. No. A-67	S.R. 7/2568	July, 1942
A 35	Non-Ballistic Test for Armour Quality	R. F. Mehl, M. Gensamer and C. Barrett	N.D.R.C. Serial No. M-11 Progress Report	S.R. 7/2663	July, 1942
A 36	The Permanent Strain in a Uniform Bar due to Longitudinal Impact	M. P. White and Le V. Griffis	N.D.R.C. Rept. No. 71 Progress Rept.	S.R. 7/2648	July, 1942
A 37	Comments on White and Griffis' Theory of the Permanent Strain in a Uniform Bar due to Longitudinal Impact	H. F. Bohnenblust	N.D.R.C. Memo. No. A-47M	S.R. 7/2797	August, 1942
A 38	The Effect of a Solid Lubricant on Bullet Penetration	R. L. Kramer	N.D.R.C. Memo. No. A-49M	S.R. 7/2975	August, 1942
A 39	Tests of Plastic Armour Received from the National Research Council of Canada	R. J. Emrich and R. A. Beth	N.D.R.C. Memo. No. A-50M	S.R. 7/2975	August, 1942
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133	Second Report on an Investigation of the Mechanical Properties of Selected Armour Plates	—	M.O.S.	Eng. Dept. 72K/P.J.H. H.J.T.	October, 1942
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135	The Dynamic Compressive Yield Strength of Steel Projectiles. Summary of Results of Tests on Various Armour-Piercing Shells and Armour Plates	—	M.O.S.	M.O.S./172/A.C.W.	November, 1942
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137	The Resistance of Thick Plates of Hard and Soft Duralumin to Penetration by Steel Balls of Various Sizes	—	M.O.S.	M.O.S./176/H.L.D.P. S/72/351	December, 1942
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139	Note on de Marre's Formula	W. R. Dean	A.R.D. Woolwich	R.D./Ball./69/42	September, 1942
140	Investigation by Multiple Spark Photography of Angle Attack of 0·303 inch W.Mark I Bullets against Homogeneous Hard Armour Plate	C. A. Adams, H. R. Calvert and H. F. Mills	A.R.D. Woolwich	A.R.D./Ball./75/42	October, 1942
141	0·303 Inch A.P. Trials with D.D./L/14,078	G. O. Baines	A.R.D. Woolwich	R.D./Ball./90/42	December, 1942
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145	Effect of Surface Machining upon the Ballistic Properties of Rolled I.T. 80 Armour	H. O'Neill	L.M.S. Res. Dept.	—	July, 1942
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A 40	A Brief Summary of Recent Data on Penetration in Concrete at Various Scales	P. A. Beth	Interim Report to the Chief of Engineers, U.S. Army, by the Committee on Passive Protection against Bombing 1941—42 No. 18	S.R. 7/1478	—
A 41	Propagation of Plastic Waves. A Comparison of N.D.R.C. A-29 and R.C. 329	H. F. Bohnenblust	N.D.R.C. Memo. No. A-53M	S.R. 7/3140	September, 1942
A 42	Plastic Deformation of Steel under High Pressure	P. W. Bridgman	N.D.R.C. Rept. No. A-95 Progress Report	S.R. 7/3141	September, 1942
A 43	The Propagation of Plastic Waves in Torsion Specimens of finite Length: Theory and Methods of Integration	Th. von Karman H. F. Bohnenblust and D. H. Hyers	N.D.R.C. Progress Rept. No. A-103	S.R. 7/3257	October, 1942
A 44	The Effect of Stopped Impact and Reflection on the Propagation of Plastic Strain in Tension	R. E. Duwez, D. S. Wood, D. S. Clark and J. V. Charyk	N.D.R.C. Progress Rept. No. A-108	S.R. 7/3327	November, 1942
A 45	The Ballistic Properties of Mild Steel, including Preliminary Tests of Armour Steel and Dural	Ballistics Research Group, Princeton University	N.D.R.C. Progress Rept. No. A-111	S.R. 7/3525	November, 1942
A 46	The Correlation of Metallographic Structure and Hardness Limit with Ballistic Properties of Armour Plate	C. H. Lorig, P. C. Rosenthal and A. R. Elsa	N.D.R.C. Progress Rept.	S.R. 7/3547	November, 1942
A 47	The Correlation of Metallographic Structure and Hardness Limit with Ballistic Properties of Armour Plate: Literature Survey	C. H. Lorig	N.D.R.C. Progress Rept.	S.R. 7/3545	November, 1942
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155	Note on von Karman's Formula for the Velocity of Propagation of Plastic Plane Waves	E. A. Milne	A.B.D., O.B.	A.P.P. Co-ord. Sub. Com. Paper No. 55	April, 1943
156	Penetration of Steel Under Static and Dynamic Conditions	G. O. Baines	A.R.D. Woolwich	A.R.D., Ball. Rept. 14/43	February, 1943
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167	Flash Photographs of Bullet Penetration in British Plastic Armour	C. W. Curtis, R. G. Stone and J. Pittenger	Memo. No. A-60M O.S.R.D. 1068	S.R. 7/3630	November, 1942

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160	On the Propagation of the Plastic Deformation produced by an Expanding Cylinder	J. S. Koehler and F. Seitz	Armour and Ordnance Report No. A-139 O.S.R.D. 1214	S.R. 7/3816	January, 1943
170	Flame Hardening of Homogeneous Armour Plate (O.D.-88)	P. E. Kyle	Progress Report Serial No. M-32. O.S.R.D. 1189	S.R. 7/3788	January, 1943
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172	The Evaluation of Welding Procedure and Technique in terms of Ballistic Tests. Part I.—Weldability of Commercial Armour Plate	G. S. Mikhalapov	Progress Report Serial No. M-45. O.S.R.D. 1165	S.R. 7/3781	January, 1943
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174	The Influence of Impact Velocity on the Tensile Properties of Plain Carbon Steels and of a Cast-Steel Armour Plate	P. E. Duwez, D. S. Wood and D. S. Clark	Progress Report No. A-154. O.S.R.D. 1274	S.R. 7/4016	March, 1943
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178	Second Progress Report on Plastic Deformation of Steel under High Pressure	P. W. Bridgman	Armor and Ordnance Report No. A-162 O.S.R.D. 1347	S.R. 7/4199	March, 1943
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183	The Penetration and Perforation of Targets by Bombs, Shell and Irregular Fragments	A. G. Walters and L. Rosenhead	M.O.S., P.D.E.	P.D.E. Rept. 1943/50	October, 1943
184	Report on S.T.A.M. Trial No. 129	—	S.T.A.M.	—	October, 1943
185	Statistical Analysis of Armor Plate Data-Report No. 1	A. Brown and P. J. Stanley	Advisory Service on Quality Control	Tech. Rept. Series E. Q.C./E. 3	February, 1943
186	Relation between Chemical Analysis and Ballistic Limit for Armour Plate	A. Brown and P. J. Stanley	A.S.Q.C.	Tech. Rept. Series E.4NO. Q.C./E/3 Part III	—
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214	The Effectiveness of Light Armour as a Protection against Flak	H. M. Morse	Tech. Div. Service Branch Ball. Sect. Office of Chief of Ordnance U.S.A.	S.R. 7/4745	March, 1943

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217	Progress Explosion Test for Welded Armour Plate	W. C. Snelling	N.D.R.C.	S.R. 7/4206	April, 1943
218	Non-Ballistic Test for Armour Plate	M. Gensmer, C. S. Barrett and R. F. Mehl	O.S.R.D. 1365—Serial No. M-59. Special Report	S.R. 7/4207	April, 1943
219	Preliminary Report on Deflection and Perforation of Steel Plates at Impact Velocities up to 150 ft./sec.	P. E. Duwez, D. S. Wood and D. S. Clark	Armour and Ordnance Dept. No. A-175 O.S.R.D. 1402	S.R. 7/4395	April, 1943
220	Flame Hardening of Homogeneous Armour Plate: Effect of Flame Hardening on the Ballistic Properties of Pre-Heat Treated Homogeneous Armour Plate	P. E. Kyle	Progress Report Serial No. M-69. O.S.R.D. 1409	S.R. 7/4324	May, 1943
221	Improvement of Low Alloy Armour Steels	C. H. Lorig, P. C. Rosenthal, M. C. Udy, A. R. Elsea, G. P. Krumleaf and G. K. Manning	Serial No. M-77 Progress Report O.S.R.D. 1418	S.R. 7/4392	May, 1943
222	The Effect of Cold-Working on the Ballistic Properties of Steel Plate	G. W. Bridgman	Armour and Ordnance Report No. A-177 O.S.R.D. 1429	S.R. 7/4330	May, 1943
223	Weldability of Commercial Armour Plate: The Influence of Thermal Stress Relief on the Hardness of Five Types of 1½ inch Rolled Armour	R. H. Aborn and R. E. Brian	Progress Report Serial No. M-73 O.S.R.D. 1468	S.R. 7/4772	May, 1943
224	Dynamic Tests of the Tensile Properties of S.A.E. 1020 Steels, Armco Iron and 17S.T. Aluminium Alloy	P. E. Duwez, D.S. Wood and D. S. Clark	Armour and Ordnance Report No. A-182 O.S.R.D. 1490	S.R. 7/4401	May, 1943
225	The Influence of Impact Velocity on the Tensile Properties of Class B Armour Plate. Heat-Treated Alloy Steels and Stainless Steels	P. E. Duwez, D. S. Wood and D. S. Clark	Armour and Ordnance Report No. A-195 O.S.R.D. 1641	S.R. 7/43/284	July, 1943
226	Correlation of Metallographic Structure and Hardness Limit in Armour Plate: Par I.—Effects of Austenite Transformation Products on Ballistic Properties	C. H. Lorig, A. R. Elsea, P. C. Rosenthal and G. K. Manning	Final Report Series No. M-118. O.S.R.D. 1696	S.R. 7/43/337	August, 1943
227	The Behaviour of Longitudinal Stress Waves near Discontinuities in Bars of Plastic Material	Le V. Griffis	Armour and Ordnance Report No. A-212 O.S.R.D. 1799	S.R. 7/43/739	September, 1943

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228	Discussion of Energy Measurements in Tension Impact Tests at the California Institute of Technology	P. E. Duwez, D. S. Wood and D. S. Clark	Armour and Ordnance Report No. A-217 O.S.R.D. 1629	S.R. 7/43/740	September, 1943
229	Plastic Deformation of Steel under High Pressure	D. W. Bridgman	Armour and Ordnance Report No. A-218 O.S.R.D. 1868	S.R. 7/43/1025	September, 1943
230	Correlation of Metallographic Structure and Hardness Limit in Armour Plate: Part II—Correlation of Microstructure and Ballistic Properties. Part III—Analyses of Problems presented by Individual Producers	C. H. Lorig, A. R. Elsea, G. K. Manning and P. C. Rosenthal	Final Report Serial No. M-154. O.S.R.D. 1949	S.R. 7/43/1226	October, 1943
231	Examination of Enemy Material: Metallurgical Study of Two Samples of Japanese Welded Light Homogeneous Armour	H. W. Gillett, A. S. Henderson, L. H. Grenell, J. Dunleavy and J. R. Cady	Progress Report Serial No. M-158. O.S.R.D. 1966	S.R. 7/43/1106	October, 1943
232	On the Static and Dynamic Plastic Bending of Plates	D. H. Hyers	Armour and Ordnance Report No. A-228 O.S.R.D. 2018	S.R. 7/44/82	November, 1943
233	Non-Ballistic Test for Armour Plate	M. Gensamer, C. S. Barrett, A. B. Westerman, J. Vajda and R. F. Mehl	Final Report Series No. M-87. O.S.R.D. 2041	S.R. 44/15	November, 1943
234	The Reactions of Thin Beams and Slabs to Impact Loads. Part I.—General Theory Part II.—Beams	H. P. Robertson and R. J. Sluts	Interim Report Chief Eng. U.S. Army by the Committee on Passive Protection against Bombing 1941—42 Nos. 13 and 14	S.R. 7/3762 and 3763	June, 1942
235	Comparison of 0·30 Calibre A.P. Cores under Comparator Microscope	—	British Admiralty Delegation: Plastic Armour Division: Ref.: 10/3/A.H.L./1/43	S.R. 7/4129	June, 1943
236	Comparison of British Steel Protective Plating with American S.T.S. Plating	—	British Admiralty Delegation: Plastic Armour Division: Ref.: 6/2/A.H.L./12/42	S.R. 7/4131	December, 1942
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238	Note on the Collision of a Plastic Cylinder with a rigid Obstacle	F. G. Friedlander	—	A.P.P. Co-ord. Sub. Com. Paper No. 58	March, 1944

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240	Firings with Lubricated Shot	G. O. Baines	A.R.D. Woolwich	T.B. Report 9/43	December, 1943
241	The Effect of the Surface Finish of the Ogive on the Performance of A.P. Shot	G. O. Baines	A.R.D. Woolwich	T.B. Report 11/43	December, 1943
242	The Analysis of the Plastic Deformation in a Cylinder of Shot Steel Striking a Rigid Target	E. H. Lee and S. J. Tupper	A.R.D. Woolwich	S.T.A. Report No. 4/44	January, 1943
243	Penetration of Armour by High Velocity Projectiles and Munroe Jets	R. Hill, N. F. Mott and D. C. Pack	A.R.D. Woolwich	S.T.A. Report 13/44	March, 1943
244	The Perforation of Armour by A.P. Projectiles Part I.—Steel Projectiles	D. Stockdale and V. K. W. Duff	School of Tank Technology	—	January, 1944
245	The Effect of Low Temperature on the Bullet Resisting Properties of Armour Plate	—	M.A.P. Orfordness Research Station	O.R.D. Ref.: F.T. 308	September, 1943
246	A New Method of Determining the Projectile Penetration Resistance of Armour Plate	—	Dept. of Mines and Resources, Ottawa. Ore Dressing and Met. Lab. Investigation No. 1537	S.R. 7/44/644	December, 1943
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248	Types of Failure Occurring in the Shock Test of ½ inch Homogeneous Armour with Calibre 0·50 A.P. Projectiles	—	Ordnance Dept. U.S.A. Watertown Arsenal	S.R. 7/43/808 Rolled Armour Repr. No. 44	March, 1942
249	First Report on Tension Tests under Pressure for the Watertown Arsenal	P. W. Bridgman	W.A. Rept. No. 111/7	S.R. 7/43/1291	March, 1943
250	Second Report on Torsion Experiments for the Watertown Arsenal	P. W. Bridgman	W.A. Rept. No. 111/7-1	S.R. 7/43/1292	March, 1943
251	The Effect of Pre-straining in Tension on the Behaviour of Steel under Tension, Torsion and Compression	P. W. Bridgman	W.A. Rept. No. 111/7-2	S.R. 7/43/1293	July, 1943
252	Development of a Fracture Test to Indicate the Degree of Hardening of Armour Steels on Quenching	A. Hurlich	W.A. Rept. No. 710/532	S.R. 7/43/848	August, 1943
253	Armour Plate—Rolled, Ballistic and Metallurgical 1½ inch S.A.E. 1035 Rolled Homogeneous Armour Plate	E. L. Reed	W.A.	S.R. 7/43/1170 Exp. Rept. No. W.A.L. 710/546	October, 1943
254	Principles of Projectile Design for Penetration. First Partial Rept.	C. Zener and R. E. Peterson	W.A.	S.R. 7/44/123 762/321	October, 1943
255	Tensile Stress—Strain Curves	J. E. Hollomom	W.A.	Rept. No. 630/7-1	November, 1943

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256	To Determine the Effect of Hardness on the Ballistic Properties of 1 inch Cast Plates	—	A.P.G. Rept. No. A.D.-191	S.R. 7/44/478	December, 1942
257	To Determine the Effect of the Ballistic Properties of 1 inch Cast Homogeneous Armour	—	A.P.G. Rept. No. A.D.-380	S.R. 7/44/144	May, 1943
258	To Determine the Effect of Plate Size and the Method of Support on the Resistance to Penetration of ¼ inch and ½ inch Rolled Homogeneous Armour with Cal. 0·30 A.P.M. 2	—	A.P.G. Rept. No. A.D.-527	S.R. 7/44/119	June, 1943
259	Improvement of Present Method of Measuring Plate Thicknesses for Ballistic Tests	—	A.P.G. Rept. No. A.D.-533	S.R. 7/44/116	—
260	Ballistic Tests of Rolled Homogeneous Armour (made from N.E.9430 Steel) under Specification A.X.S. 495—2 and A.X.S. 711	—	A.P.G. Rept. No. A.D.-577	S.R. 7/44/308	September, 1943
261	Shock Test Ballistic Limits on Four ¼ inch, Twenty ½ inch and Four [illegible] inch Face-hardened Plates	—	A.P.G. Rept. No. A.D.-636	S.R. 7/44/309	August, 1943
262	Tenth Partial Report on Light Armour	—	U.S. Navy Dept. N.R.L. Rept. 0—1892	S.R. 7/43/1055	June, 1942
263	The Mechanics of Armour Perforation. III. Resisting Force During the Penetration Cycle	H. P. Robertson	Armour and Ordnance Report No. A-211 O.S.R.D. 1798	S.R. 7/43/1197	October, 1943
264	The Mechanics of Armour Perforation. I. Residual Velocity	H. P. Robertson	Armour and Ordnance Rept. No. A-227 O.S.R.D. 2043	S.R. 7/44/764	November, 1943
265	The Effects of Flame Hardening on the Ballistic Properties of Pre-Heat Treated Homogeneous Armour Plate	P. E. Kyle	Progress Rept. Serial No. M-167. O.S.R.D. 2059	S.R. 7/44/353	November, 1943
266	Distortion of an Armour Plate under Simple Compressive Stress to High Strains	P. W. Bridgman	Armour and Ordnance Rept. No. A-235 O.S.R.D. 3019	S.R. 7/44/1268	December, 1943
267	Investigation of Boron in Armour Plate. Influence of Boron and Chromium on some Properties of Experimental Steels containing 0·3 per cent. Carbon and 0·8, 1·25 or 1·6 per cent. Manganese	T. G. Digges and F. M. Reinhardt	Progress Report Serial No. M-174. O.S.R.D. 3020	S.R. 7/44/368	December, 1943
268	The Influence of Specimen Dimensions and Shape on the Results of Tensile Impact Tests	D. S. Wood, P. E. Duwez and D. S. Clark	Armour and Ordnance Report No. 237 O.S.R.D. 3028	S.R. 7/44/1263	December, 1943
269	Final Report of the Use of Special Non-Alloy Steels for Armour Piercing Capped Shot: (O.D.-107)	J. S. Jackson, D. P. Baswell, C. Fisher and R. B. Schenck	Progress Report Serial No. M-195. O.S.R.D. 3110	S.R. 7/44/698	January, 1944

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271	Preliminary Experiments on the Propagation of Plastic Strain in Tension	P. E. Duwez	Armour and Ordnance Report No. A-244 O.S.R.D. 3207. (Revision of Report No. A-33)	S.R. 7/44/875	January, 1944
272	The Stress Waves Produced in a Plate by a plane Pressure Pulse	J. S. Koehler and F. Seitz	Armour and Ordnance Report No. A-245 O.S.R.D. 3230	S.R. 7/44/1048	February, 1944
273	The Influence of Impact Velocity on the Tensile Properties of Four Magnesium Alloys and 24S Aluminium Alloy	D. S. Clark, P. E. Duwez and D. S. Wood	Armour and Ordnance Report No. A-249 O.S.R.D. 3256	S.R. 7/44/937	February, 1944
274	High Speed Compression Tests of Copper Crusher Gauges and Spheres	C. C. Simpson, E. L. Firman and J. S. Koehler	Armour & Ordnance Rept. No. A-257 O.S.R.D. 3330	S.R. 7/44/1295	March, 1944
275	The Set Added to Compression Cylinders After Impact	E. L. Firman, J. S. Koehler and F. Seitz	Armour & Ordnance Rept. No. A-258 O.S.R.D. 3331	S.R. 7/44/1371	March, 1944
276	Progress Report on Investigation of Boron Armour Plate (OO—87): Influence of Nitrogen on Some Properties of Experimental Steels With and Without Boron	T. G. Digges and F. M. Reinhardt	Progress Rept. Serial No. M-231. O.S.R.D. 3378	S.R.7/44/1240	March, 1944
277	Effects of Flame Hardening on the Ballistic Properties of Pre-heat Treated Homogeneous Armour Plate	E. L. Bartholomew, Jnr., M. S. Burton, F. R. Evans and R. S. Williams	Progress Rept. Serial No. M-233. O.R.S.D. 3416	S.R.7/44/1318	February, 1944
278	The Influence of Impact Velocity on the Tensile Properties of Three Types of Ship Plate: M.S., H.T.S., S.T.S.	D. S. Clark, P. E. Duwez and D. S. Wood	Armour & Ordnance Rept. No. A-261 O.R.S.D. 3420	S.R.7/44/1515	March, 1944
279	First Report on Improvement of Low Alloy Armour Steel (OD—87): Part V. The Effect of Draw Practice on the Mechanical Properties of Six Armour Steels	C. H. Lorig, G. P. Krumlauf, M. K. Barnett, P. C. Rosenthal and G. K. Manning	Progress Rept. Serial No. M-245. O.S.R.D. 3423	S.R.7/44/1225	March, 1944
280	Progress Report of Armour Plate and Low Alloy Steels (OO—85): The Spot Welding of Attachments to Homogeneous Armour	W. F. Hess, A. Muller and W. D. Doty	Progress Rept. Serial No. M-215. O.S.R.D. 3433	S.R.7/44/1227	March, 1944
281	Errors in Residual Velocity Determinations Using Magnetised Shot	P. J. Higgs	D.S.I.R., N.P.L.	Paper No. 62 A.P.P. Co-ord. Sub-Com.	January, 1944

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282	Further Note on the Effect of Internal Stress on the Resistance of Armour Plate to Perforation	A. F. Brown	D.S.I.R., N.P.L.	Paper No. 63. Co-ord. Sub-Com. A.P.P.	February, 1944
283	Measurement of the Deceleration of a Shot Penetrating Armour Plate	—	R.R.L., M.O.S.	M.O.S./300/T.L.W.J.O.S.	December, 1943
284	Seventeenth Interim Report on Concrete for Defence Work—The Effect of Sectional Density on the Penetration Into Concrete of Armour-piercing Shot and the Derivation of a General Penetration Formula	—	M.O.S.	M.O.S./311/A.C.W.	February, 1944
285	Further Measurements of the Deceleration of 2-pr. Shot Attacking 41 mm. Armour Plate	—	M.O.S.	M.O.S./325/W.J.O.S.	March, 1944
286	The Elementary Theory of Shatter Diagrams in the Attack of Armour	E. A. Milne	O.B.	Paper No. 68. A.P.P. Co-ord. Sub-Com.	May, 1944
287	Notes on Proof of Piercing Shell	R. Beeching	O.B.	O.B. *Proc. No.* 27,858	
288	Notes on Composition of A.P. Shot Steel	—	M.O.S.	S.T.A.M. Memo. No. 16	
289	Fracture of Ductile Metals	N. Mott	A.R.D.	S.T.A. Rept. No. 4/43	November, 1943
290	A Survey of R.A.E. Reports Dealing with Stress Waves in Balloon Barrage Cables Due to Transverse Impact	E. H. Lee	A.R.D.	S.T.A. Memo. No. 4/44	
291	Comments on N.R.L. Report No. O—2275, " The Longitudinal Vibrations of a Projectile during Armour Penetration "	E. H. Lee	A.R.D.	S.T.A. Memo. No. 9/44	June, 1944
292	Static and Dynamic Experiment with Capped Shot	G. O. Baines	A.R.D.	T.B. Report No. 7/44	March, 1944
293	Penetration of Steel Under Static and Dynamic Conditions—Further Experiments on the 2-pr. Scale	G. O. Baines	A.R.D.	T.B. Report No. 8/44	March, 1944
294	Armour Disposition Giving High Protection with Small Weight	C. A. Adams, R. H. Calvert, H. F. Hills and J. Vennart	A.R.D.	T.B. Report No. 9/44	March, 1944
295	The Mechanism of Shatter—Part II—The Time Factor in Shatter Behaviour	C. A. Adams	A.R.D.	T.B. Report No. 14/44	April, 1944
296	Note on Shatter of Shot at Normal Impact: Effect of Hardness of Plate	H. R. Calvert and J. Vennart	A.R.D.	T.B. Report No. 15/44	June, 1944
297	Comparison of Static and Dynamic Partial Penetration of Armour Plate by 2-pr. Shot	G. O. Baines	A.R.D.	T.B. Report No. 20/44	July, 1944
298	The Longitudinal Motion of a Shot During Armour Penetration With Special Reference to N.R.L. Report O—2275	C. A. Adams	A.R.D.	T.B. Report No. 23/44	August, 1944
299	Note on R.R.L. Paper No. M.O.S./325/W.J.O.S.	G. O. Baines	A.R.D.	T.B. Report No. 26/44	August, 1944

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301	6-pr. A.P.C.—Varying Hardness Gradients	—	A.R.D.	T.B. Report A.P. No. 5/44	February, 1944
302	To Investigate Steel Composition, Heat-treatment and Hardness Layout for 17-pr. A.P.C. shell	—	A.R.D.	T.B. Report A.P. No.15/44	April, 1944
303	First Report on Grooved Steel Armour Plate. Tests of Grooved and Fluted Plates	D. J. Martin	U.S.A. Ordnance Dept. W.A. Expt. Rept. No. W.A.L. 710/32	S.R. 7/43/1176	February, 1935
304	Photographic Study of Impact of Ball and Armour Piercing Ammunition on Armour Plate	E. L. Reed	W.A. Report No. 710/78	S.R. 7/43/974	December, 1936
305	Metallographic Study of the Deformation of Homogeneous Armour Plate under Impact Ball and Armour Piercing Projectiles	N. A. Mathews and E. L. Reed	W.A. Report No. 710/384	S.R. 7/43/973	September, 1941
306	Development of 9465 Steel for use in Armour Piercing Projectiles, 75 mm. A.P.C. M-61	—	A.P.G. Rept. No. A.D.-P69	—	May, 1943
307	Third Partial Report on Ballistic Tests of Armour at Sub zero Temperatures	—	A.P.G. Report No. A.D.-571	S.R. 7/44/202	September, 1943
308	The Longitudinal Vibrations of a Projectile during Armour Penetration	—	U.S. Navy Dept. N.R.L. Report No. 0—2275	S.R. 7/44/1506	June, 1943
309	The Effect of Inclusions upon the Ballistic Performance of 1¼—2¼ inch Armour Plate	—	N.R.L. Report No. M-2159	S.R. 7/43/757	September, 1943
310	Report on Effect of Plate Temperature upon the Performance of Homogeneous Armour	—	N.R.L. Report No. 0—2262	S.R. 7/44/1702	March, 1944
311	Report on Velocity Loss of a ¼ inch Model Projectile when it penetrates 1/32 inch Cold-Rolled Sheet Steel	—	N.R.L. Report No. 0—2263	S.R. 7/44/1700	March, 1944
312	Bibliography on Impact or Shock Loading of Metals and Alloys including Strain Rates and Instrumentation for Measurement of Dynamic Loading	—	Bureau of Ships, U.S. Navy Res. and Standards Section	S.R. 7/44/2944	November, 1943
313	Ballistic Testing of Armour—Revised Edition	—	Dahlgren Report No. 21—43 Rev.A.	S.R. 7/44/2202	June, 1944
314	Improvement of Low Alloy Steels (O.D.-87): Part VI.—The Effect of Melting Practice on the Properties of Armour Steel	C. H. Lorig, P. C. Rosenthal, J. G. Kura, A. R. Elsea, N. Keysor and G. K. Manning	Final Report Serial No. M-263. O.S.R.D. 3535 N.D.R.C.	S.R. 7/44/1817	April, 1944

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315	Final Report of the Use of special Non-Alloy Steels for Armour Piercing Capped Shot (O.D.-107): Part II.—Results of Experimental Work directed towards Production of a Projectile possessing superior Ballistic Properties	J. S. Jackson, D. P. Bushwell, C. E. Fisher and R. B. Schenck	Final Report Serial No. M-256. O.S.R.D. 3583	S.R. 7/44/1808	April, 1944
316	Behaviour of Metals under Dynamic Conditions (N.O.-11) (N.S.-109): Influence of Impact Velocity on the Tensile Properties of N.E. 8715, N.E. 9415, S.A.E. 1045 and S.A.E. 1090	D. S. Clark, P. E. Duwez and D. S. Wood	Progress Report Serial No. M-257. O.S.R.D. 3695	S.R. 7//44/2027	May, 1944
317	Behaviour of Metals under Dynamic Conditions (N.O.-11) (N.S.-109): Influence of Velocity on the Tensile Properties of some Metals and Alloys	P. E. Duwez and D. S. Clark	Progress Report Serial No. M-288. O.S.R.D. 3837	S.R. 7/44/2268	June, 1944
318	The Propagation of the Plastic Zone along a Tension Bar of a Metal having a well-defined Plastic Limit	J. Miklowitz	Armour and Ordnance Report No. A-280 O.S.R.D. 3864	S.R. 7/44/2669	July, 1944
319	Behaviour of Metals under Dynamic Conditions (N.S.-109): The Propagation of Plastic Strain in Compression	P. E. Duwez, D. S. Clark and H. E. Martens	Progress Report Serial No. M-302. O.S.R.D. 3886	S.R. 7/44/2481	July, 1944
320	Behaviour of Metals under Dynamic Conditions (N.S.-109): A Preliminary Investigation of the Mechanism of Penetration from the Standpoint of Strain Propagation	P. E. Duwez and D. S. Clark	Progress Report Serial No. M-317. O.S.R.D. 3957	S.R. 7/44/2656	July, 1944
321	Effect of Flame Hardening on the Ballistic Properties of Pre-heat Treated Homogeneous Armour Plate	E. L. Bartholomew, Jr., M. S. Burton, F. R. Evans, P. E. Kyle and R. S. Williams	Final Report Serial No. M-329. O.S.R.D. 4110	S.R. 7/44/2935	September, 1944
322	The Use of Thin Spaced Plates of Armour Plate and Mild Steel to Stop 0·303 inch A.P. Shot	—	D.S.I.R., R.R.L., M.O.S.	M.O.S./357/K.L.C.F. D.B.W.	June, 1944
323	Report on Armour Plate 1943	A. M. Walker	M.O.S.	Tech. Report Series "E" No. Q.C./E/9	July, 1944
324	The Performance against Homo-Hard (I.T.70) Plate of Heavy Alloy Cores	J. T. Harris	A.R.D.	T.B. Report No. 17/44	June, 1944
325	The Application of Static Penetration Data to the Calculation of Compressive Stresses in Shot or Shell during Penetration	G. O. Baines	A.R.D.	T.B. Report No. 21/44	June, 1944
326	The Technique of Multiple Spark Photography Applied to Problems in Terminal Ballistics. Part I.—The Penetration of Armour by A.P. Shot with different Hardness Distributions	H. R. Calvert and J. Vennart	A.R.D.	T.B. Report No. 28/44	December, 1944
327	The Effectiveness of Thin Steel Plates in Stripping the Piercing Caps from A.P.C. and A.P.C.B.C. Projectiles	J. T. Harris, G. O. Baines and J. E. Brickell	A.R.D.	T.B. Report No. 29/44	October, 1944
328	First Report on Three Plate Arrangement to defeat 2-pr. A.P.C.B.C. Shot by the Inclusion of Decapping and Shatter Plates	J. D. Thorn	A.R.D.	T.B. Report No. 30/44	September, 1944

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329	On the Shatter of 2-pr. A.P. Shot on Thin, Hard, Oblique Plates	J. D. Thorn	A.R.D.	T.B. Rept. No. 31/44	September, 1944
330	A Comparison of Two 60 lb. N.C. Plates by Static Methods	G. O. Baines	A.R.D.	T.B. Rept. No. 32/44	October, 1944
331	Note on the Comparative Performance of Canadian and British Mark 1 L.J. Cores in a Static Test	G. O. Baines	A.R.D.	T.B. Rept. No. 34/44	October, 1944
332	German 7·5 cm. Pak 40 A.P.C.B.C. Shell	—	A R.D.	Met. Report 129/44	June, 1944
333	To Determine the Performance of British 6-pr. A.P.C.B.C. Shot at Wide Angles of Attack and to Ascertain Whether Any Improvement Could Be Effected by Alteration of the Standard Heat Treatment	—	A.R.D.	T.B. Report A.P. No.35/44	—
334	To compare the Performance of 6-pr. A.P.C. Shot With Penetrative Caps of 0·9% C Steel With That of 6-pr. A.P.C. Shot Fitted With Alloy Steel (S.T.A.21) Caps Against Homogeneous I.T.80 Plates	—	A.R.D.	T.B. Report A.P. No.36/44 Part II	July, 1944
335	To Determine Whether the Hardness Distribution in 20 mm. H.S. A.P. Mark 4 Shot Which Will Give the Lowest Critical Velocity Against Plate to Specification I.T.80 (280—330 Brinell) Will Also Give Optimum Performance Against Plate of I.T.70 Quality (444—477 Brinell)	—	A.R.D.	T.B. Report A.P. No.38/44	August, 1944
336	Break-up Shot on Thin, Oblique Plates	—	D.T.D.	Report No. M 6376, A/14, No. 1	June, 1944
337	Attack of I.T.80 Plates at High Angles of Impact With Heavy A.P. Shot	—	D.T.D.	Report No. M 7000, A/16, No. 1	May, 1944
338	Mechanism of Armour Penetration. Third Partial Report	C. Zener	Ordnance Dept. U.S.A. W.A. Expt. Rept. No. W.A.L. 710/492-1	S.R. 7/44/1677	March, 1944
339	Mechanism of Armour Penetration. Fourth Partial Report	C. Zener	Ordnance Dept. U.S.A. W.A. Expt. Rept. No. W.A.L. 710/492—2	S.R. 7/44/3319	August, 1944
340	To Determine the Security of Cap Attachment and Armour Penetration Characteristics of 37 mm. A.P.C. Shot M.51 With Crimped-on Caps	—	A.P.G. Repts. Nos. A.D.-P.71 and P.76	S.R. 7/44/717	June, 1943
341	Development of Ballistic Limit Determination With One Shot	—	A.P.G. Report No. A.D.-153	S.R. 7/44/746	—
342	Report on the Resistance to Penetration of Duralumin for Use in Connection With Current Design and Development of Gun Shields	I. W. Tiechman	A.P.G. Report No. AD-502	S.R. 7/44/2228	November, 1943
343	Effect of Hardness on the Ballistic Properties of $\frac{1}{16}$ inch—$\frac{1}{4}$ inch Homogeneous Armour	—	A.P.G. Repts. Nos. AD-503, 513, 510, 514, 629	S.R. 7/44/2990, 3259, 3440, 3525 and 3526	May—June, 1944

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344	To Determine Ballistic Performance of $\frac{3}{8}$ inch Hard Homogeneous and $\frac{1}{2}$ inch Face Hardened Armour at Varying Obliquities With Cal. 0·30, Cal. 0·50 and 20 mm. Projectiles	F. S. Wolff	A.P.G. Rept. No. AD 599	S.R. 7/44/2130	January, 1944
345	The Effect of Hardness and Obliquity Upon the Penetration Resistance of $1\frac{1}{4}$-inch Cast Armour to 57 mm. A.P.C. M.86 and 37 mm. A.P. M.74 Projectiles	—	A.P.G. Rept. No. AD–630	S.R. 7/44/3261	May, 1944
	Report of the Ballistic Test of 4-inch Cast Homogeneous Armour Plates Submitted by General Steel Castings Corporation	—	A.P.G. Rept. No. AD–658	S.R. 7/44/3262	April, 1944
	The Effect of Hardness and Obliquity on the Ballistic Properties of 1-inch Cast Armour Submitted by Lebanon Steel Foundry	—	A.P.G. Rept. No. AD–663	S.R. 7/44/3265	June, 1944
	The Effect of Hardness Upon the Resistance to Penetration of 3-inch Cast Armour Against Armour Piercing Projectiles	—	A.P.G. Rept. No. AD–678	S.R. 7/44/3736	June, 1944
346	Penetration of Steel Spheres into Wood as a Function of Striking Velocity	—	A.P.G. Res. Lab. No. 462	S.R. 7/44/2300	May, 1944
347	The Measurement of Forces Which Resist Penetration of S.T.S. Armour, Mild Steel and 24ST Aluminium	—	Work by U.S. Navy Dept. NRL. Rept. No. 0–2276	S.R. 7/44/245/4	May, 1944
348	Penetration of Face Hardened Bullet Proof Armour by Solid Cal. 0·27 Bullet	—	NRL. Rept. No. 0–2290	S.R. 7/44/2564	May, 1944
349	First Partial Report on Projectile Shock on Aircraft Armour Supports	—	NRL. Rept. No. 0–2331	S.R. 7/44/2763	July, 1944
350	Penetration Mechanisms. I—The Penetration of Homogeneous Armour by Uncapped Projectiles at 0° Obliquity	—	Dahlgren Rept. No. 1—43	S.R. 7/44/1303	April, 1943
351	The Effect of Nose Shape on the Ballistic Performance of 15 lb. 3-inch A.P. Solid Shot Against Homogeneous Armour Plate	—	Dahlgren Rept. No. 2—43	S.R. 7/44/3716	February, 1943
352	Penetration of Homogeneous Armour by 3-inch Flat-nosed Projectiles	—	Dahlgren Rept. No. 7—43	S.R. 7/44/3717	April, 1943
353	Penetration Mechanisms. II—Supplementary Report on the Penetration of Homogeneous Plates by Uncapped Projectiles at 0° Obliquity	—	Dahlgren Rept. No. 3—44	S.R. 7/44/1206	February, 1944
354	Penetration of Homogeneous Plate by 3-inch Flat-nosed Projectiles	—	Dahlgren Rept. No. 12—44	S.R. 7/44/2201	April, 1944
355	The Development of a Process for Manufacturing and Welding Face Hardened Armour Plate	R. B. Schenck, J. S. Jackson, D. P. Burwell and C. E. Fisher	N.D.R.C. Final Rept. Serial No. M–290. O.S.R.D. 3912	S.R. 7/44/2521	July, 1944
356	A Compilation of Informal Reports Submitted in Advance of Formal Reports (15 Sep. to 15 Oct. 1944)	—	Ordnance & Terminal Ballistics Rept. No. OTB–3. O.S.R.D.4258	S.R. 7/44/3805	October, 1944

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358	Effect of Locked-up Stresses on Ballistic Performance of Welded Armour. Part II (OD—106)	J. T. Norton, D. Rosenthal and S. B. Maloof	Final Rept. Serial No. M–421. O.S.R.D. 4396	S.R. 7/45/40	November, 1944
359	Behaviour of Metals Under Dynamic Conditions (NS—109): Properties of Yielding	P. E. Duwez, H. E. Martens and D. S. Clark	Progress Rept. Serial No. M–409. O.S.R.D. 4453	S.R. 7/45/749	December, 1944
360	Third Progress Report on the Investigation of Scale Effect in Armour Penetration. Firing Trials at Normal Attack with Geometrically-similar Shot Against Homogeneous Armour of Varied Hardness	A. F. C. Brown and V. M. Hickson	D.S.I.R., N.P.L.	Paper No. 79 A.P.P. Co-ord. Sub. Com.	September, 1944
361	Fourth Progress Report on the Investigation of Scale Effect in Armour Penetration. The Optimum Hardness of Homogeneous Armour for Resistance to Perforation at Normal Attack by Projectiles of Different Sizes	D. G. Sopwith	N.P.L.	Paper No. 80 A.P.P. Co-ord. Sub. Com.	September, 1944
362	Fourth Report on an Investigation of the Mechanical Properties of Selected Armour Plate	P. J. Higgs	N.P.L.	Paper No. 81 A.P.P. Co-ord. Sub. Com.	September, 1944
363	Comments on Naval Research Laboratory Report No. O–2263 "Velocity Loss of a ¼ inch Model Projectile When it Penetrates 1/32 inch Cold-rolled Sheet Steel."	D. G. Sopwith	N.P.L.	Paper No. 83 A.P.P. Co-ord. Sub. Com.	October, 1944
364	The Ballistic Properties of a Mild Steel at Normal Attack	D. G. Sopwith	N.P.L.	Paper No. 84 A.P.P. Co-ord. Sub. Com.	October, 1944
365	Report on the Vibration of A.P. Shot after Impact	V. M. Hickman	N.P.L.	A.P.P. Co-ord. Sub. Com. Paper No. 94	—
366	Report on Firing Trials with 2-pr. A.P.C.B.C. Shot against I.T.80D Quality Plate at Angles of Attack from Normal to 60 degrees	A. F. C. Brown and V. M. Hickman	—	A.P.P. Co-ord. Sub. Com. Paper No. 95	—
367	The Dynamic Compressive Yield Strength of a Nickel-Chrome Steel	—	R.R.L., M.O.S.	M.O.S./390/H.L.D.P.	August, 1944
368	The Elementary Theory of Shatter Diagrams in the Attack of Armour—Parts II and III	E. A. Milne	O.B.	A.P.P. Co-ord. Sub. Com. Paper No. 78	—
369	A Note on Stress and Momentum Curves when a Shot or Shell Perforates a Plate	N. Hinchcliffe	O.B.	A.P.P. Co-ord. Sub. Com. Paper No. 97	February, 1945
370	Penetration into Wood	R. Hill and D. C. Pack	A.R.D.	S.T.A. Report No. 19/44	June, 1944

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371	The Distribution of Stress in a Decelerating Elastic Sphere	W. R. Dean, I. N. Sneddon and H. W. Parson	A.R.D.	S.T.A. Report No. 33/44	June, 1944
372	Cavitation Phenomenon in Ductile Materials and the Dynamic Term in the Resistance to Penetration	R. Hill	A.R.D.	S.T.A. Report No. 34/44	August, 1944
373	The Problem of Discing of Armour Plate	W. R. Dean and I. N. Sneddon	A.R.D.	S.T.A. Report No. 36/44	August, 1944
374	Protection in Aircraft against 20 mm. A.P. Bullets	C. A. Adams, H. R. Calvert, H. F. Hills and J. Vennart	A.R.D.	T.B. Report No. 1/45	May, 1945
375	The Calculation of Shell Cavities from the Basis of Static Experiments	G. O. Baines	A.R.D.	T.B. Report No. 2/45	January, 1945
376	An Investigation of Flaking in Tank Armour by Static Punching Methods	G. O. Baines	A.R.D.	T.B. Report No. 3/45	March, 1945
377	The Scale Effect in Static Penetration	G. O. Baines	A.R.D.	T.B. Report No. 5/45	July, 1945
378	Note on the Performance of Spaced Targets against Low Velocity Attack	C. A. Adams and H. F. Hills	A.R.D.	T.B. Report No. 8/45	July, 1945
379	Determination of the Function of Various Parts of the Penetrative Cap of an Armour Piercing Capped Shot in Defeating the Hardened Face of a Cemented Plate and in Preventing Shatter against Homogeneous Plate of Machineable Quality	—	A.R.D.	T.B. Report No. 7/45	—
380	The Optimum Calibre Length (l/d) Ratio of Tungsten Carbide Cores	—	A.R.D.	T.B. Report No. A.P. 12/45	April, 1945
381	Steels for Armour Piercing Bullet Cores. A Review of their Heat Treatment, Magnetic Hardness, Vickers Diamond Hardness and Correlation of these Physical Properties with S.A.A. Penetration of Standard Armour Plate	W. N. Hindley	A.R.D.	Met. Report No. 56/45	May, 1945
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383	Aircraft Armour. An Empirical Approach to the Efficient Design of Armour for Aircraft	J. F. Sullivan	Ordnance Dept. U.S.A. W.A.L. No. 710/506	S.R. 7/44/2954	January, 1944
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385	Principle of Armour Protection. Third Partial Report	—	W.A. Report No. W.A.L. 710/607-2	S.R. 7/45/101	June, 1944
386	Third Partial Report on the Ballistic Results Obtained on Rolled Homogeneous Armour Submitted for Acceptance under Specification A.X.S. 498-1	—	A.P.G. Report No. A.D./575	S.R. 7/45/2927	—

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388	Shatter of Brine Quenched and Air Quenched Calibre 60 F.X.S.-318 Steel Cores	H. W. Euker and T. A. Read	Frankford Arsenal Report No. R-616	S.R. 7/45/3618	April, 1945
389	Penetration of Homogeneous Plate of One Tensile Strength (110,000 p.s.i.) by 3 inches M-79 Projectiles—Partial Report	—	U.S. Navy Dept. Dahlgren Report No. 8-44	S.R. 7/44/1968	April, 1944
390	Penetration of Homogeneous Plate by 3 inches 13·0 lb. Flat Nosed Projectiles fitted with Windshields. Second Partial Report	—	Dahlgren Report No. 19/44	S.R. 7/44/3466	July, 1944
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394	A Compilation of Informal Reports Submitted in Advance of Formal Reports (15th November to 15th December 1944) "Shatter" of Tungsten Carbide Projectiles	C. V. Curtis	Ordnance and T.B. Report No. O.T.B.-5 O.S.R.D. 4477	S.R. 7/45/144	December, 1944
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396	A Compilation of Informal Reports Submitted in Advance of Formal Reports (15th February to 15th March): Subcalibre Steel Projectiles	C. W. Curtis and R. J. Emrich	Ordnance and T.B. Report No. O.T.B.-8 O.S.R.D. 4829	S.R. 7/45/1477	March, 1945
397	A Compilation of Informal Reports Submitted in Advance of Formal Reports (15th March to 15th April 1945): Oblique Impacts with Tungsten Carbide Projectiles	C. W. Curtis	Ordnance and T.B. Report No. O.T.B.-9 O.S.R.D. 4948	S.R. 7/45/1875	April 1945
398	A Compilation of Informal Reports Submitted in Advance of Formal Reports (15th April to 15th May 1945): Effect of Armour Piercing Cap on Perforation Limits	C. W. Curtis and R. J. Emrich	Ordnance and T.B. Report No. O.T.B.-10 O.S.R.D. 5094	S.R. 7/45/2199	May, 1945
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410	The Plane Problem in the Mathematical Theory of Plasticity for the Case of External Forces Applied to a Closed Contour	S. Christianovitch	A.R.D. Woolwich	S.T.A. Translation No. 1/45	August, 1945
411	The Influence of Hardness of Tilted Plates	—	F.V.D.D.	Materials Branch Report No. M6083A/16 No. 1	September, 1946
412	Device for Measuring the Acceleration of a Shot when it Strikes an Armour Plate	—	H.E.C.	B.I.O.S./Gp.2/H.E.C. No. 595	
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416	Development of Projectile Steels. Second Partial Report	D. van Winkle and P. Zener	W.A. Report No. W.A.L. 321/4-1	S.R. 7/46/1290	May, 1944
417	Principles of Projectile Design for Penetration. Third Partial Report	D. M. van Winkle	W.A. Report No. W.A.L. 762/231-3	S.R. 7/45/4274	June, 1944
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420	Historial Review of the Correlation of Ballistic and Metallurgical Characteristics of Domestic Armour at Watertown Arsenal	M. Bolotsky	W.A. Report No. W.A.L. 710/795	—	—
421	On the Determination of Statistical Ballistic Limit Velocities by the Method of Maximum Likelihood	L. H. Thomas	A.P.G. Report No. 573	S.R. 7/45/4431	September, 1945
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423	Theory of the Plastic Deformation of Thin Plates with Applications	J. M. Richardson	N.D.R.C. Report No. A.344 O.S.R.D. No. 5660	S.R. 7/45/4266	October, 1945
424	The Improvement of Low-Alloy Armour Steel. Part XVI.—A Study of the Effect of Boron on Steels Suitable for Use in Armour from 3 to 6 inches in Thickness	Udy, Rosenthal, Manning and Lorig	N.D.R.C. O.S.R.D., No. 6204	S.R. 7/45/4253	November, 1945
425	The Improvement of Low-Alloy Armour Steel. Part XVIII.—Continuation of Dilatometric Studies of Armour with Respect to Quench-Cracking	Udy, Manning, Rosenthal and Lorig	N.D.R.C. O.S.R.D. No. 6206	S.R. 7/45/4324	November, 1945
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427	Perforation Limits for Non-Shattering Projectiles Against Thick Homogeneous Armour at Normal Incidence	C. W. Curtis and R. L. Kramer	N.D.R.C., Report No. A-393 O.S.R.D. 6464	S.R. 7/46/955	March, 1946
428	Terminal Ballistics of Tungsten Carbide Projectiles: Mechanical Strength of Core Material	C. W. Curtis and R. L. Kramer	N.D.R.C. Report No. A-394 O.S.R.D. 6465	S.R. 7/46/1067	March, 1946
429	Terminal Ballistics of Tungsten Carbide Projectiles: Length Test	C. W. Curtis, R. J. Emrich and R. L. Kramer	N.D.R.C. Report No. A-395 O.S.R.D. 6466	S.R. 7/46/954	March, 1946
430	Terminal Ballistics of Tungsten Carbide Projectiles: Effect of Carrier, Part II	R. J. Emrich	N.R.D.C. Report No. A-396 O.S.R.D. 6467	S.R. 7/46/1045	March, 1946

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432	Terminal Ballistics of Tungsten Carbide Projectiles: Body Failures	C. W. Curtis	N.D.R.C. Report No. A-476 O.S.R.D. 6640	S.R. 7/46/1047	March, 1946
433	Terminal Ballistics of Tungsten Carbide Projectiles: Artillery Type Caps	R. L. Kramer	N.D.R.C. Report No. A-477 O.S.R.D. 6641	S.R. 7/46/104	March, 1946
434	The Terminal Ballistics of Armour-Piercing Projectiles	C. W. Curtis	N.D.R.C. Report No. A-478 O.S.R.D. 6642	S.R. 7/46/1064	March, 1946
435	High Obliquity Attack of Deck Targets by 20 mm. model Shell	H. R. Calvert and J. Vennart	A.R.D.	A.R.D. T.B. Report 7/45	July, 1945
436	High Obliquity Attack of Deck Targets. Part II	H. R. Calvert and J. Vennart	A.R.D.	A.R.D. T.B. Report 2/46	February, 1946
437	Fifth Progress Report on the Investigation of Scale Effect in Armour Penetration	P. J. Higgs and V. M. Hickson	N.P.L.	N.P.L. Eng. Div. 312/47	December, 1947
438	Penetration of Steel under Static and Dynamic Conditions. Part XIII.—Static Penetration with Lubricated Punches	G. O. Baines	A.R.D.	T.B. Report 4/43	—
439	Static Punching Experiments on Mild Steel up to 3·5 t/d	G. O. Baines	A.R.D.	T.B. Report 6/45	—
440	Determination of the Co-efficients of Friction of Steel on Steel at High Velocities	G. Grotsch and E. Plake	(Advisory Council on Scientific Research and Technical Development A.C. 5012)	Z.G.S.S. Vol. 35 Nos. 1 & 2	January, 1940 and February, 1940
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444	The Theory of Armour Penetration by Tungsten Carbide Shot and the Effect of Head Shape on Peformance at High Velocities	R. Hill	A.R.D.	T.B. Report 29/45	—
445	The Theory of Wedge Indentation at Oblique Incidence and its Application to the Calculation of Forces on a Yawed Shot Impacting on Armour at any Angle	R. Hill and R. H. Lee	A.R.D.	T.B. Report 1/46	—
446	The Break-up of Shot on Thin Oblique Plates	—	D.T.D.	Armour Branch Report No. 6367A/14, No. 1	—
447	Spaced Armour Assemblies Depending for Effectiveness on the Break-up of Shot	J. W. Thompson	M.O.S.	Permament Records of Research and Development No. 5-034	—

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