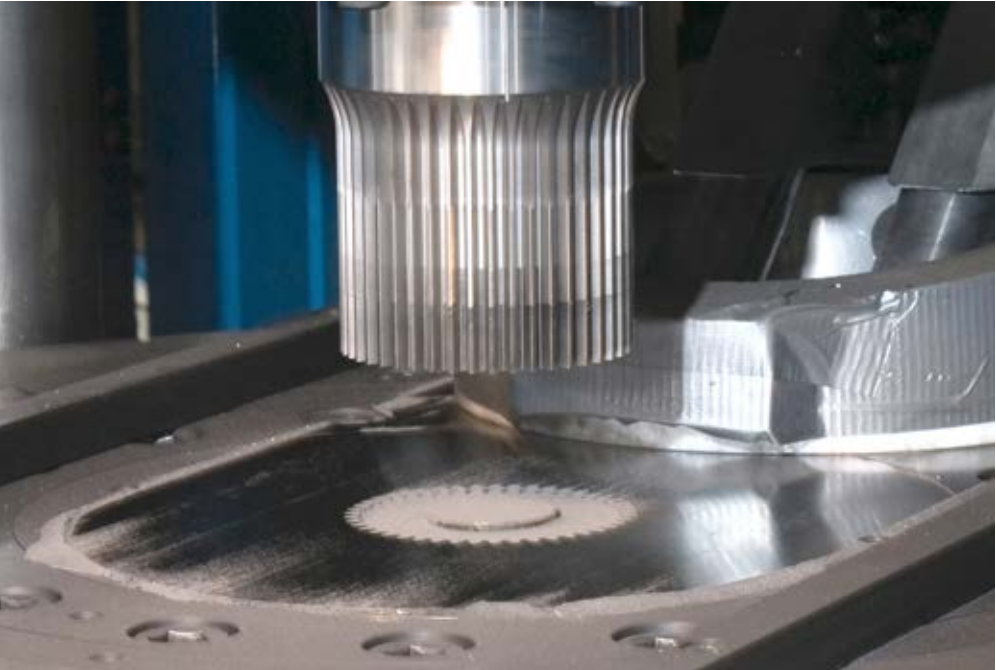




2

Production of Sintered Components

Höganäs Handbook for Sintered Components

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The data presented in the handbook has been obtained from test specimens, sintered under well-controlled conditions, in the Höganäs AB laboratory.

Note that data established for any particular production equipment or conditions may differ from those presented in this handbook.

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In order to fully comprehend the possibilities and limitations of powder compaction, it is necessary not only to study the empirical phenomena of this process, but also to reveal the basic mechanisms behind them.

Compacting of Metal Powders

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Introduction

The forming of a sintered component begins with the densification of the metal powder in a rigid die having a cavity of more or less complicated contour. In this operation, high pressures (usually 650 N/mm^2) are exerted upon the powder in the die cavity, simultaneously from top and bottom, via two or more vertically moving compacting punches.

Under the influence of such high compaction pressures, the powder particles are being squeezed together so closely that their surface irregularities interlock and a certain amount of cold welding takes place between their surfaces.

After ejection from the die, if the compaction operation was successful, the compact owns sufficient strength (so-called green strength) to withstand further handling without damage. In order to facilitate the compaction operation and reduce tool wear to a minimum, a lubricant is admixed to the powder before compaction.

In order to fully comprehend the possibilities and limitations of powder compaction, it is required not only to study the empirical phenomena of this process, but also to reveal the basic mechanisms behind them.

4.1 Density - Porosity - Compacting Pressure

At first, some definitions are required:

- **Specific Weight:** $\rho = m/V_t$ (measured in g/cm^3); m = mass of the material; V_t = true volume of the material.
- **Density:** $\delta = m/V_b$ (measured in g/cm^3); m = mass of the powder resp. compact; V_b = bulk volume (enveloping volume).
- **Theoretical Density:** δ_{th} = density of a (practically not attainable) pore-free powder compact (measured in g/cm^3).
- **Porosity:** $\phi = 1 - \delta/\delta_{th}$ (number without dimension).
- **Compaction Pressure (die compaction):** P = compaction force/face area of compact (measured in MPa or N/mm^2).
- **Compaction Pressure (isostatic compaction):** P = pressure of the hydraulic medium (measured in MPa or N/mm^2).

4.1.1 Empirical Density-Pressure Curves

Powder Compacting in a Cylindrical Die.

The strength properties of sintered components increase with increasing density but their economy drops with increasing energy input and increasing load on the compaction tool. Thus, it is most desirable, for both economic and technical reasons, to achieve the highest possible compact density at the lowest possible pressure.

Density-pressure curves give information about the frame within which a suitable compromise may be found. These curves are generally obtained from standard laboratory tests where a number of compacts are made at different pressures in a carbide die having a cylindrical bore of 25 mm diameter. The densities of the compacts are plotted against compacting pressures. The diagram in *Fig. 4.1* shows density-pressure curves for two commercial iron powders (NC100.24 and ASC100.29).

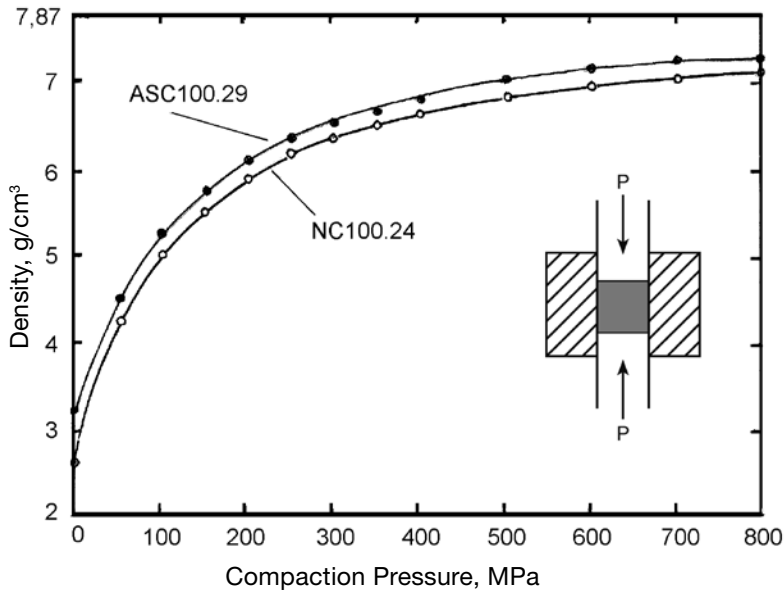


Figure 4.1. Density-pressure curves for two commercial iron powders compacted in a carbide die having an inner diameter of 25 mm. Lubricant additions: 0.75% Zn-stearate.

A striking feature of these curves is the fact that their slope decreases considerably with increasing compaction pressures and that the density of massive pure iron (7.86 g/cm^3) obviously cannot be reached at feasible pressures. We notice, further, that the two iron powders despite their chemical identity yield different density-pressure curves. This different compaction behaviour arises from differences of their particle structure. See Chapter 3.

Isostatic Powder Compacting.

A powder under isostatic pressure shows a similar densification behaviour as in die-compaction. This is illustrated by the following example: Samples of electrolytic iron powder, hermetically enclosed in thin rubber jackets and embedded in a hydraulic medium, were subjected to varying isostatic pressures. Since there is no die-wall friction in isostatic compaction, the powder was not admixed with any lubricants. The so obtained densification curves are shown in Fig. 4.2.

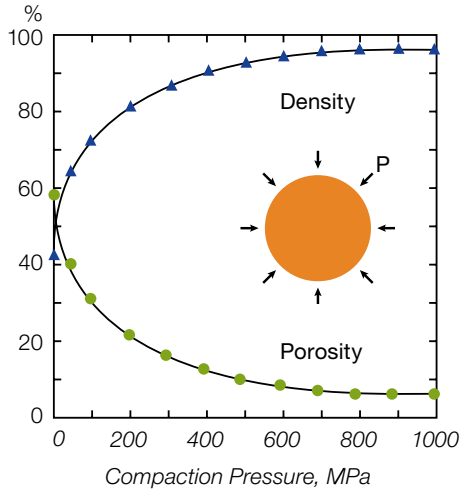


Figure 4.2. Relative density and porosity as functions of isostatic compaction pressure. Electrolytic iron powder hermetically enclosed in thin rubber jackets subjected to hydraulic pressure.

Adaptation of contact areas between adjacent powder particles, caused by plastic deformation, can be seen from the microstructure of a copper powder compact shown in *Fig. 4.3*. From this microstructure, it can also be seen that bigger powder particles form bridges around much smaller particles which thus, have escaped deformation.

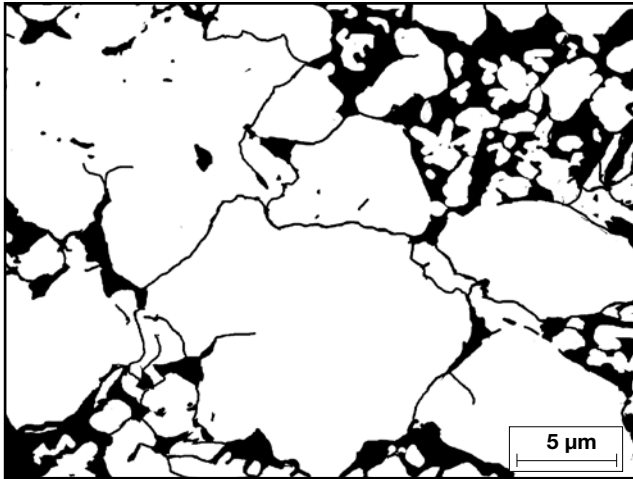


Figure 4.3. Adaptation of surface contours due to plastic deformation of adjacent powder particles. Electrolytic copper powder compacted at 200 N/mm^2 .

4.1.2 Principle Limits to Densification

Since early in the 1930's, powder metallurgists have endeavored to find a suitable mathematical description of the process for powder densification. The number of formulas to which this effect have been suggested over the last three decades is legion. However, none of these formulas, most of them extracted from simple curve-fitting exercises, has proven to be sufficiently universal and substantiated by general physical principles to be acceptable as sound theory of powder densification.

In work shop practice, such formulas are dispensable because it is far more reliable to establish relevant densification curves experimentally than to calculate them from complicated and questionable formulas.

On the other hand, it is quite useful to understand, in principle at least, in which way the process of powder densification is influenced and limited by general laws of physics and mechanics.

Deformation Strengthening of Powder Particles.

Disregarding, for the moment, the problem of wall friction in die-compaction and considering isostatic compaction of powder only, we recognize that the problem of powder densification arises from an underlying physical problem which can be defined as follows:

- With increasing densification, the powder particles are plastically deformed and increasingly deformation strengthened, i.e. their yield point is steadily being raised.
- Simultaneously, the contact areas between particles are increasing and consequently, the effective shearing-stresses inside the particles are decreasing. Thus, at constant external pressure, decreasing shearing-stresses meet a rising yield point and all further particle deformation ceases, i.e. the densification process stops.

The deformation strengthening of the powder particles can be made evident by means of X-ray structural analysis. In *Fig. 4.4*, three photo-records of X-ray back-reflections are shown, obtained (A) from a commercial sponge-iron powder, (B) from a compact of this powder pressed at 290 N/mm², and (C) from the same compact after soft-annealing for 2 minutes at 930°C.

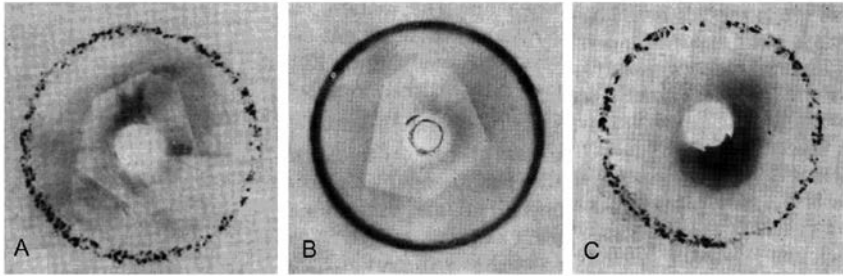


Figure 4.4. Deformation strengthening of powder particles in the compacting of sponge iron powder (Höganäs grade NC100.24). Photographic records of X-ray back-reflections (Cr-K α radiation, V-filter). (A) powder before compacting, (B) compact made at 3 t/cm², (C) the same compact after soft-annealing for 2 minutes at 930°C.

The distinct X-ray reflections (sharp black spots) on photo-records (A) and (C) give evidence of undisturbed crystal lattices in powder particles free from deformation-strengthening. The diffuse ring-shaped X-ray reflection on photo-record (B) gives evidence of severely disturbed crystal lattices in deformation-strengthened powder particles.

Decrease of Maximum Shearing Stress.

In a state of densification where the powder particles are squeezed together to such an extent that the initially interconnected pores between them have degenerated to small isolated pores, the stress distribution around each of them can be fairly well approximated by the stress distribution in a hollow sphere under hydrostatic outside pressure P . Let the hollow sphere be of metal having a yield-point σ_0 . Let R be the outer radius of the sphere and r its inner radius.

According to theory of elasticity, plastic deformation will occur when the maximum shearing stress τ_m at the outer surface of the hollow sphere exceeds the shearing yield-stress $\tau_0 = \sigma_0/2$, i.e. when $\tau_m(R) \geq \sigma_0/2$. See sketch in Fig. 4.5. From the principle of *Mohr's circle* we derive the general relationship $\tau_m = (\sigma_r - \sigma_t)/2$. Thus the condition of plastic flow for the hollow sphere is:

$$\sigma_r(R) - \sigma_t(R) \geq \sigma_0 \quad (4.1)$$

The radial stress $\sigma_r(R)$ and the tangential stress $\sigma_t(R)$ close to the outer surface of the hollow sphere are given by the following relations:

$$\sigma_r(R) = -P \quad (4.2)$$

and

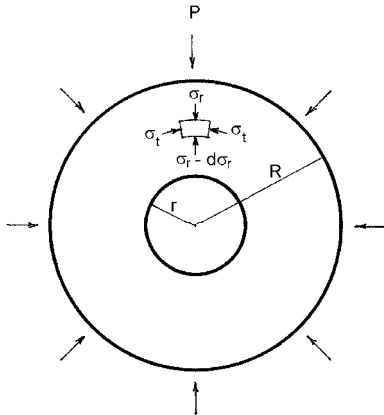
$$\sigma_t(R) = -P \frac{2R^3 + r^3}{2(R^3 - r^3)} \quad (4.3)$$

Introducing (4.2) and (4.3) into (4.1) yields:

$$P \frac{3r^3}{2(R^3 - r^3)} \geq \sigma_0 \quad (4.4)$$

or:

$$P \geq \frac{2}{3} \sigma_0 \frac{R^3 - r^3}{r^3} \quad (4.5)$$



Condition of Flow:

$$P \geq \frac{2}{3} \sigma_0 \frac{R^3 - r^3}{r^3}$$

Figure 4.5. Condition of plastic flow in a hollow sphere of metal under hydraulic outside pressure P.

R = outer diameter, r = inner diameter,
 σ_0 = yield point of the metal,
 σ_r = radial stress,
 σ_t = tangential stress.

According to equation (4.5), the hydrostatic pressure P , required to provoke plastic deformation of the hollow sphere, is higher the smaller the volume of the hole ($\sim r^3$) is relative to the metal volume of the sphere ($\sim R^3 - r^3$). In other words: an infinitely high pressure would be required to reduce the hole inside the metal sphere to nothing.

Transferring this result analogously to the small isolated pores inside a highly densified powder compact, it appears plausible that these small pores cannot be eliminated by means of feasible pressures - not even in the absence of deformation strengthening. At constant external pressure, the maximum shearing stress anywhere in the compact is smaller, the smaller the residual pores are.

Theoretical Density of Powder Mixes.

Sintered components are usually manufactured from mixes of unalloyed or low-alloyed iron powder with additives like graphite, other metal powders and lubricants. Compact densities attainable with such powder mixes are, of course, influenced by the specific weights and the relative amounts of the additives and of impurities if any. The (only theoretically achievable) pore-free density δ_M of a powder mix can be calculated as follows:

ρ_{Fe} be the specific weight of the iron powder (base powder),
 w_{Fe} be the weight percentage of the iron powder,
 $\rho_1, \rho_2, \rho_3, \dots$ be the specific weights of additives and impurities,
 $w_1, w_2, w_3, \dots$ be the weight percentages of additives and impurities.

Then, the theoretically achievable pore-free density of the powder mix is:

$$\delta_M = 100 / (w_{Fe}/\rho_{Fe} + w_1/\rho_1 + w_2/\rho_2 + w_3/\rho_3 + \dots) \quad (4.6)$$

In Table 4.1, the specific weights are given of some additives and impurities as occurring in iron powder mixes. Using the data from this table and equation (4.6), the theoretical densities of various powder mixes on the basis of ASC100.29 have been calculated and plotted as functions of the relative amounts of the respective additives in the diagram shown in *Fig. 4.6*.

From the diagram it can be seen that added lubricants (indispensable for the reduction of die-wall friction) have the most lowering effect on the theoretical density of powder mixes. In the compaction process, part of the added lubricant is being squeezed towards the die-wall where it fulfills its intended function.

The remaining part of the lubricant gets entrapped inside closed pores where it develops a hydraulic pressure opposing the densification process.

**Table 4.1. Specific Weights of some Metals,
Additives and Impurities as occurring in Iron Powder Mixes**

Metal, Additive, Impurity	Specific Weight (g/cm ³)	Metal, Additive	Specific Weight (g/cm ³)
Fe (purest iron)	7.87	NC100.24	7.79
FeO	5.30	SC100.26	7.80
SiO ₂	2.30	ASC100.29	7.85
Graphite	2.29	MnS	4.1
Cu	8.95	Ni (pure nickel)	8.90
Zn-stearate	1.14	Amide wax	1.0

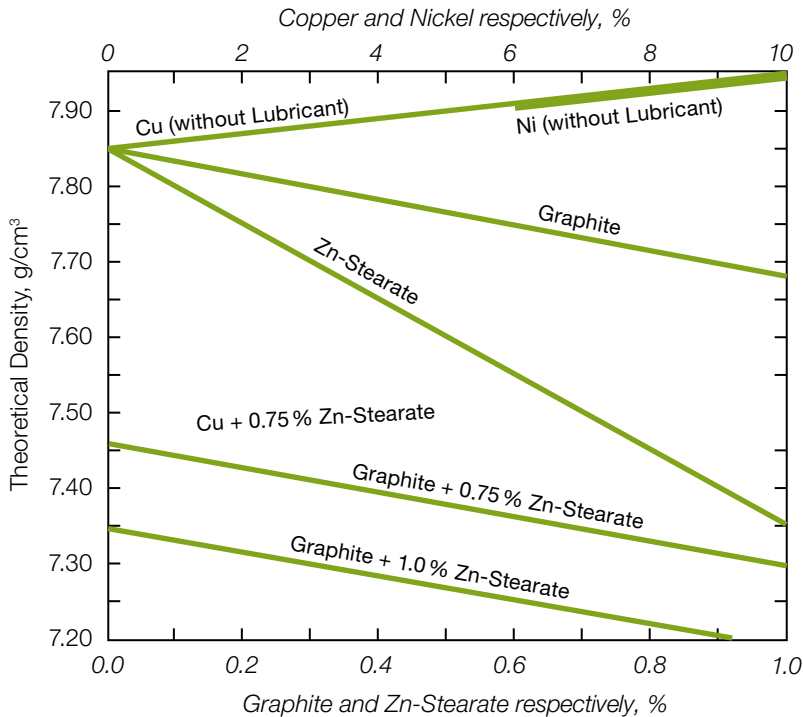


Figure 4.6. Influence of added alloying elements and lubricants on the theoretical (pore-free) density of iron powder mixes based on ASC100.29.

Density-pressure curves, established in the laboratory according to standard compacting procedures, are useful guidelines for the approximate dimensioning of compaction tools. But they do not allow accurate predictions of pressures and densities to be expected when compacting complicated structural parts in dies with deep and narrow filling spaces (viz. gears and long thin-walled bushings).

In such instances, only carefully conducted compaction tests in the actual die can give reliable information.

4.2 Radial Pressure - Axial Pressure

When the piston of a hydraulic cylinder exerts pressure upon the liquid inside the cylinder, the pressure applied in axial direction is transformed 1:1 to radial pressure upon the cylinder wall. When a powder is being compacted in a rigid cylindrical die, the axial pressure, exerted upon the powder by the compacting punch, is only partly transformed to radial pressure upon the die wall.

This radial pressure can be quite substantial, but it cannot reach the level of the axial pressure because a powder is no liquid and has no hydraulic properties.

4.2.1 Hysteresis of the Radial Pressure

The way in which the empirical relationship between radial and axial pressure is governed by general laws of physics and mechanics can be understood, in principle at least, from a simple model, suggested in 1960 by W.M. Long* and presented in detail below. First, we consider a free-standing cylindrical plug of metal having a modulus of elasticity E and a Poisson factor ν . A compressive axial stress σ_a , applied to the end-faces of the plug, provokes, by laws of elasticity, a radial stress σ_r , and the radius of the plug is expanded by the factor

$$\epsilon_r = (\sigma_r - \nu\sigma_r - \nu\sigma_a)/E \quad (4.7)$$

We now imagine the same plug being put into a tightly fitting cylindrical die. The die is assumed to have a modulus of elasticity much larger than that of the metal plug. Further, it is assumed that the die is extremely well lubricated, so that any friction between the plug and the die-wall is negligible. Exerting, via two counteracting punches, axial pressure upon the plug, its radial expansion ϵ_r is negligibly small because the die expands extremely little due to its large modulus of elasticity. Thus, $\epsilon_r = 0$ is a sufficiently close approximation of reality, and from (4.7), it follows:

$$\sigma_r - \nu\sigma_r - \nu\sigma_a = 0 \quad (4.8)$$

* W.M. Long, Powder Metallurgy, No. 6, 1960.

Hence, the relationship between radial and axial stress in the plug is:

$$\sigma_r = \sigma_a \nu / (1 - \nu), \text{ elastic loading} \quad (4.9)$$

The maximum shearing-stress in the plug (derived from Mohr's circle) is always :

$$\tau_{\max} = (\sigma_a - \sigma_r) / 2 \quad (4.10)$$

With increasing axial stress in the plug, $\tau_{\max}$ increases too, until it exceeds the shearing yield-stress $\tau_0 = \sigma_0 / 2$, i.e. until $\tau_{\max} \geq \sigma_0 / 2$. Then, from (4.10), the following condition of flow emerges:

$$(\sigma_a - \sigma_r) \geq \sigma_0, (\sigma_0 = \text{yield point of the metal plug}). \quad (4.11)$$

Now, plastic flow occurs in the plug, and the relationship between radial and axial stress in the plug is:

$$\sigma_r = \sigma_a - \sigma_0, \text{ plastic loading} \quad (4.12)$$

At axial pressure release, $\tau_{\max}$ immediately falls below the level of the shearing yield-stress ($\tau_{\max} < \sigma_0 / 2$), and the stresses in the metal plug are being released according to:

$$\sigma_r = \sigma_a \nu / (1 - \nu) + k, \text{ elastic releasing (k = constant)} \quad (4.13)$$

In the course of continued release, the axial stress in the plug decreases and eventually becomes even smaller than the radial stress. From this point on, the following condition of flow rules:

$$(\sigma_r - \sigma_a) \geq \sigma_0 \quad (4.14)$$

and the relationship between radial and axial stress is:

$$\sigma_r = \sigma_a + \sigma_0, \text{ plastic releasing} \quad (4.15)$$

From the above description, it is evident that the entire loading-releasing cycle, which the metal plug undergoes in the compaction die, forms a hysteresis as illustrated in the diagram in *Fig. 4.7 a*.

A particularly interesting detail of this hysteresis is the fact that, after complete release of the axial stress, the plug remains under a compressive radial stress σ_r which is equal to the metals yield point σ_0 . In this respect, Long's model provides a plausible explanation of the spring back effect (see § 4.4) occurring when powder compacts are ejected from the compacting die.

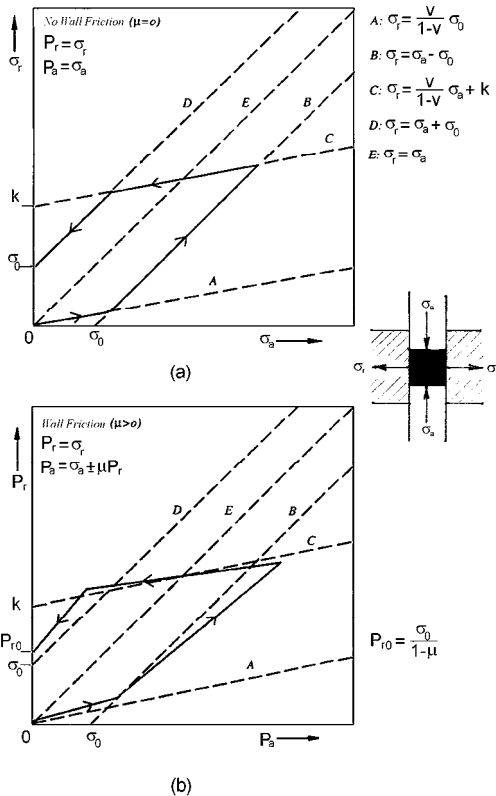


Figure 4.7. Relationship between radial and axial pressure occurring in a cylindrical metal plug inside a rigid die during a cycle of loading and releasing the axial pressure.

(a) Theoretical model disregarding die-wall friction.

(b) Theoretical model including the aspect of die-wall friction.

Although Long's model oversimplifies reality in several respects (absence of wall friction and deformation strengthening), it provides, along general lines, a fairly satisfactory description of the actual relationship between radial and axial pressure occurring when metal powder is being compacted in a rigid die.

Experimental proof of the hysteresis curve predicted by Long's model has been given for several materials by Long himself as well as by other authors. A modified model, suggested by G. Bockstiegel, includes the aspect of die-wall

friction as briefly described below. The frictional forces, occurring at the die wall during powder compaction, act in a direction opposite to the movement of the compaction punch. Thus, while the punch moves in inward direction, the compressive axial stress in the powder σ_a is smaller than the external punch pressure P_a , and while the punch moves in outward direction, σ_a is larger than P_a . It can be assumed that the frictional force at the die wall is approximately proportional to the radial pressure P_r acting upon the die wall. Hence, the following statement is made:

$$\sigma_a = P_a \pm \mu P_r \quad (4.16)$$

The negative sign refers to the phase of pressure increase, the positive sign to the phase of pressure release. μ is the frictional coefficient residing at the die wall. The radial pressure upon the die wall P_r is identical with the radial stress in the powder, i.e. $P_r = \sigma_r$.

Introducing (4.16) into Long's equations (4.9), (4.12), (4.13) and (4.15), these are transformed into corresponding equations pertaining to the modified model:

$$P_r = P_a \nu / (1 - \nu - \mu \nu), \text{ elastic loading} \quad (4.9')$$

$$P_r = (P_a - \sigma_0) / (1 + \mu), \text{ plastic loading} \quad (4.12')$$

$$P_r = P_a \nu / (1 - \nu + \mu \nu) + k', \text{ elastic releasing, } (k' = \text{constant}) \quad (4.13')$$

$$P_r = (P_a + \sigma_0) / (1 - \mu), \text{ plastic releasing} \quad (4.15')$$

For $\mu = 0$ (no wall friction), the modified equations (') are identical with Long's original equations (). Although the modified model is based on a statement which rather simplifies the real conditions of stress and friction at the die wall, it makes evident that the inclusion of wall friction does not change Long's model in its general outlines. The hysteresis curve of the loading-releasing cycle is merely being somewhat distorted. See diagram in *Fig. 4.7 b*.

During the densification of metal powders, the powder mass does not suddenly switch from elastic to plastic behaviour as suggested by Long's model, but the transition occurs gradually in the individual powder particles. Apart from this difference, deformation strengthening occurs in the powder particles during densification.

Corresponding to these circumstances, the slope of experimental hysteresis curves changes gradually with increasing pressure instead of suddenly. See example shown in *Fig. 4.8*.

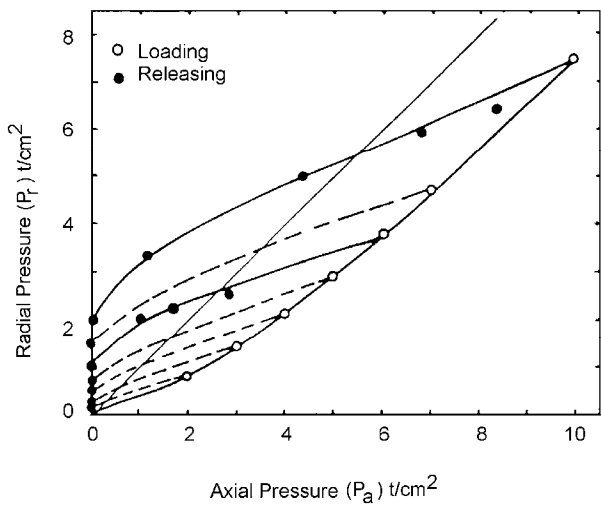


Figure 4.8. Radial and axial pressures measured on compacts of sponge iron powder during a loading releasing cycle in a cylindrical die.

4.2.2 Influence of the Yield Point.

From Long's model, it is evident that the radial pressure, which a metal plug or a mass of metal powder under axial pressure exerts upon the wall of a compacting die, is smaller the higher the yield point of the metal is. Vice versa, from the same model, it can be concluded that a metal powder with extremely low yield point and negligible tendency to deformation strengthening, like lead powder for instance, should exhibit a nearly hydraulic behaviour when compacted in a rigid die.

Experimental proof is in the diagram shown in *Fig. 4.9*. The entire loading-releasing cycle for lead powder does not show any hysteresis, and its very slight deviation from the ideal hydraulic straight line is due to frictional forces at the die wall.

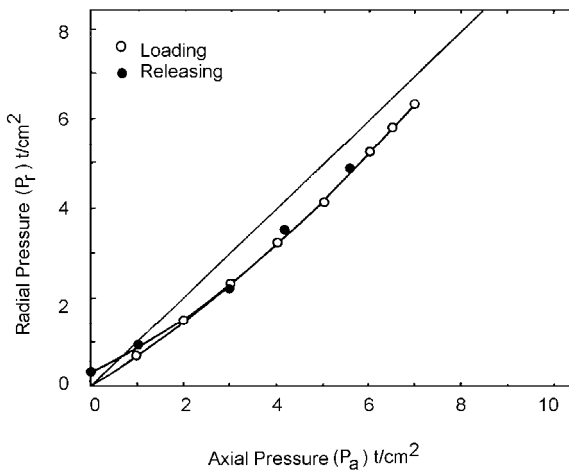


Figure. 4.9. Radial and axial pressures measured on compacts of lead powder during a loading releasing cycle in a cylindrical die.

These findings suggest that higher and more homogeneous densities in metal powder compacts could be achieved, if the compacting procedure would be executed at elevated temperatures where the yield point of the metal is lower than at Room Temperature experiments with various iron powder mixes, carried out at the Höganäs laboratory, and production runs, made by Höganäs, have proven that already an increase of the powder temperature to 150 - 200°C is sufficient to achieve substantially higher densities and improved properties.* **

* U. Engström and B. Johansson, Höganäs Iron Powder Information PM 94-9.

** 4 J. Tengzelius, Höganäs Iron Powder Information PM 95-2

The principal influence of a temperature depended yield point on the relationship between axial and radial pressure emerges from the theoretical hysteresis curves shown in *Fig. 4.10*. From these curves, it can be seen that the maximum radial pressure increases but the residual radial pressure, after complete release of the axial pressure, decreases when the yield point is lowered at elevated temperatures.

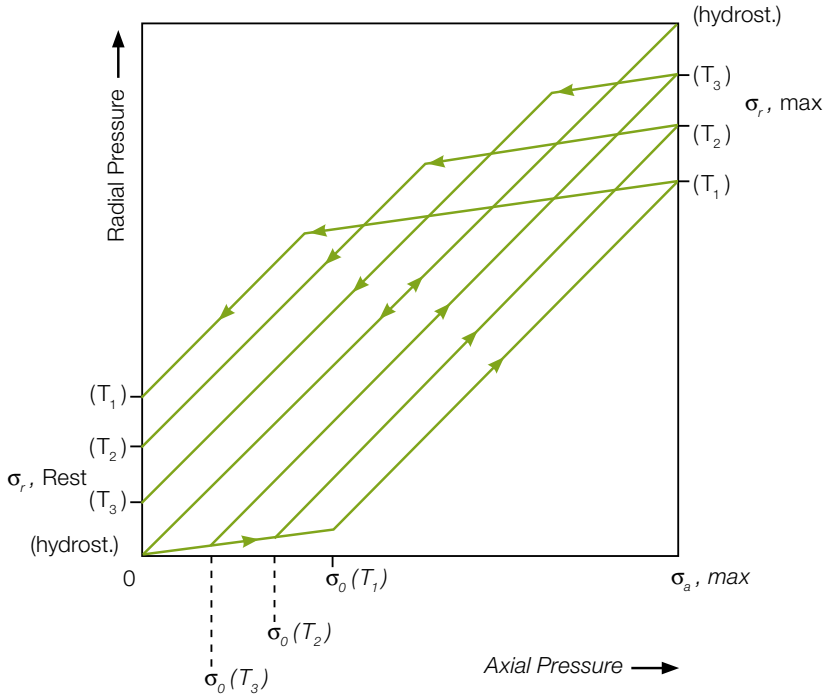


Figure 4.10. Influence of the yield point σ_0 on the relationship between radial and axial pressure for a metal plug inside a cylindrical die during a loading-releasing cycle.

Example: the yield point $\sigma_0(T)$ decreases with increasing temperature T ($T_3 > T_2 > T_1$).

4.3 Axial Density Distribution

Frictional forces at the wall of the compaction die restrain the densification of the powder because they act against the external pressure P exerted by the compaction punch. With increasing distance from the face of the compaction punch, the axial stress σ_a , available for the local densification of the powder, decreases. This becomes especially adversely apparent in the manufacturing of long thin-walled bushings which at their waist line show substantially lower densities than at their two ends. In order to find an explanation to this phenomenon, we take a closer look at the balance of forces in the powder mass during densification.

We consider densification of powder in a deep cylindrical compaction die with inner diameter $2r$. The upper punch is assumed to have entered the die and already compacted the powder to a certain degree so that the axial stress in the powder directly underneath the punch face is $\sigma_a(0)$. The variable vertical distance from the punch face be x . We imagine the powder column in the die as being composed of thin discs stacked upon one another like coins. We select one disc at distance x from the punch face. Its height be dx , its cross-sectional area is $F = \pi r^2$, and its small lateral area is $f = 2\pi r dx$. See sketch in *Fig. 4.11*.

The axial stress, acting upon the top face of this disc, is $\sigma_a(x)$. Due to friction between the lateral face of the disc and the die wall, the axial stress $\sigma_a(x+dx)$, acting upon the bottom face of the disc, is somewhat smaller than $\sigma_a(x)$. We assume that the frictional force is approximately proportional to the axial stress $\sigma_a(x)$ and to the lateral face f of the disc. After these preliminaries, we calculate the equilibrium between all forces acting upon the selected disc.

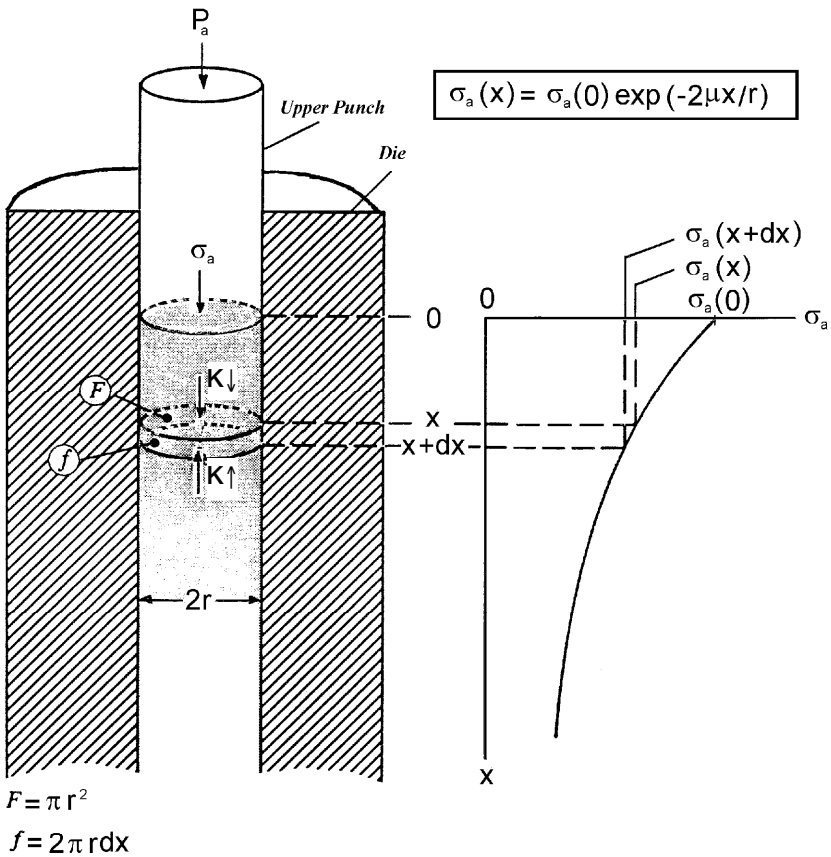


Figure 4.11. Axial stress σ_a in a powder mass as a function of distance x from the face of the upper compaction punch.

The force acting upon the top face of the disc is:

$$K_{\downarrow} = \pi r^2 \sigma_a(x) \quad (4.17)$$

The force acting upon the bottom face of the disc is:

$$K_{\uparrow} = \pi r^2 \sigma_a(x+dx) \quad (4.18)$$

The frictional force acting upon the lateral face of the disc is:

$$K\mu = \mu 2\pi r dx \sigma_a(x), (\mu = \text{coefficient of friction}) \quad (4.19)$$

Equilibrium of forces resides when

$$K_{\downarrow} - K_{\uparrow} = K\mu \quad (4.20)$$

From (4.17) to (4.20), it follows:

$$d\sigma_a = \sigma_a(x+dx) - \sigma_a(x) = -2\mu \sigma_a(x) dx/r \quad (4.21)$$

Integration of this differential equation yields:

$$\sigma_a(x) = \sigma_a(0) \exp(-2\mu x/r) \quad (4.22)$$

From this equation, it is seen that the axial compressive stress in the powder mass $\sigma_a(x)$ decreases exponentially with increasing distance x from the face of moving upper punch, and the more so, the larger the frictional coefficient μ and the smaller the inner diameter $2r$ of the die. The sketch in *Fig. 4.11* illustrates the situation. An exactly equivalent situation arises, of course, in relation to a moving lower punch. Thus when a powder is being compacted between symmetrically moving punches (which is usually the case), the axial stresses at both ends of the compact are larger than anywhere mid between.

Consequently, powder compacts usually have a zone of lower density approximately mid between their end faces. This zone of lower density is often referred to as *neutral zone* (ref. to chapter 5). Thus, compacts having thin sections, long in compacting direction, are very fragile before they are sintered.

4.4 Ejecting Force and Spring Back

One direct consequence of the residual radial stress σ_{r0} as discussed in § 4.2.1, is the fact that a substantial force is required to eject a powder compact from the compaction die. Consider a compact of height h sitting in a cylindrical die having an inner diameter $2r$.

Its cross-sectional area is $F = \pi r^2$, and its lateral area is $f = 2\pi r h$. The frictional coefficient at the die wall be μ . Then, the required ejection force is:

$$K_{\uparrow\uparrow} = \mu 2\pi r h \sigma_{r0} \quad (4.23)$$

and the pressure exerted by the ejecting lower punch upon the bottom of the compact is:

$$P_{\uparrow\uparrow} = K_{\uparrow\uparrow} / \pi r^2 = \sigma_{r0} 4\mu h / 2r \quad (4.24)$$

According to equation (4.24), the pressure $P_{\uparrow\uparrow}$ acting upon the bottom face of the compact during ejection is higher, the longer the compact is relative to its diameter ($h/2r$). The ejecting pressure is also directly proportional to the frictional coefficient μ .

At the onset of the ejecting process, the frictional coefficient μ and, consequently, the ejecting pressure $P_{\uparrow\uparrow}$ adopt a peak value (adhesive friction) substantially above the "normal" level (sliding friction). See schematic diagram in *Fig. 4.12*. This peak pressure can, in certain cases e.g. with long thin-walled bushings, exceed the maximum pressure that occurred in the compaction process.

This has two consequences:

- (a) A certain re-densification effect occurs at the lower end of the compact.
- (b) A long and slender bottom punch, just strong enough to withstand the compaction load, may yield or break under the ejecting load.

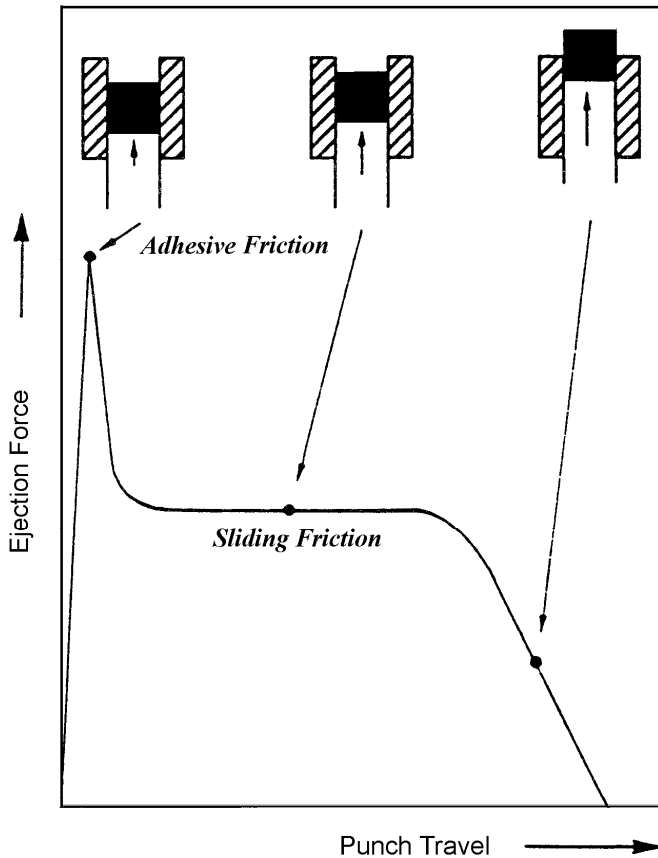


Figure. 4.12. Ejecting force as a function of the movement of the ejecting bottom punch; schematic.

If the wall of the compaction die is worn or insufficiently lubricated, it may come to cold-welding effects between the compact and the die wall, recognizable from an excessive increase of the ejecting pressure and a typical stick-slip behaviour (creaking noise). See records from ejecting experiments shown in Fig. 4.13.

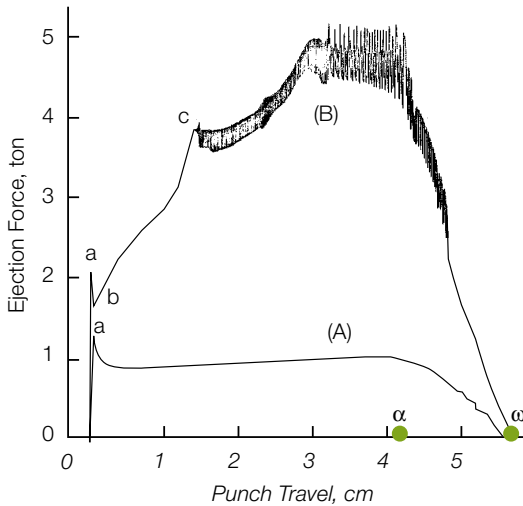


Figure 4.13. Influence of the type of lubricant on variations of the ejecting force during ejection of iron powder compacts from a cylindrical hard-metal die having an inner diameter of 25 mm. Atomized iron powder < 150 μ , compacting pressure: $P_a = 8 \text{ t/cm}^2$, compact density: $d = 7.2 \text{ g/cm}^3$, height of compact: $h = 15 \text{ mm}$, ejecting speed: 3 mm/s.

(A) lubricant: 0.75% Metallub, (B) lubricant: 0.75% Zn-stearate, worn die. (a) adhesive friction peak, (b) beginning of sliding friction, (c) severe cold-welding effects between compact and die wall. (α) compact begins to leave the die, (ω) compact has left the die.

Another consequence of the residual radial pressure becomes apparent at the moment when the compact, on ejection, passes the upper rim of the die. The upper part of the compact, sticking out of the die, expands elastically while the lower part is still under the influence of the residual radial pressure. The horizontal shearing stress arising in this situation may generate horizontal cracks in the compact. In order to diminish the shearing stress and avoid cracks in the compact, it is recommended to slightly taper the exit of the die and to round the edges of the exit.

The elastic expansion of the compact after ejection from the compaction die is called spring back and is measured according to the following formula:

$$S(\%) = 100 (\lambda_c - \lambda_d) / \lambda_d \quad (4.25)$$

where $S(\%)$ = Spring back (%), λ_c = transversal dimension of the (ejected) compact, λ_d = corresponding dimension of the compaction die (after ejection of the compact).

The spring back depends on the following parameters:

- compaction pressure, compacting density
- powder properties
- lubricants and alloying additions
- shape and elastic properties of the compaction die.

The dependence of spring back on compacting density emerges from the diagram in Fig. 4.14. Two important points can be taken from this diagram:

- The powder grade has a strong influence on spring back. (This must be kept in mind when, in the production of precision structural parts, for one or the other reason, the powder grade is changed).
- At high densities, a small scatter in density entails a wider scatter in spring back. (This can turn out to have adverse effects on the final tolerances of the sintered structural parts).

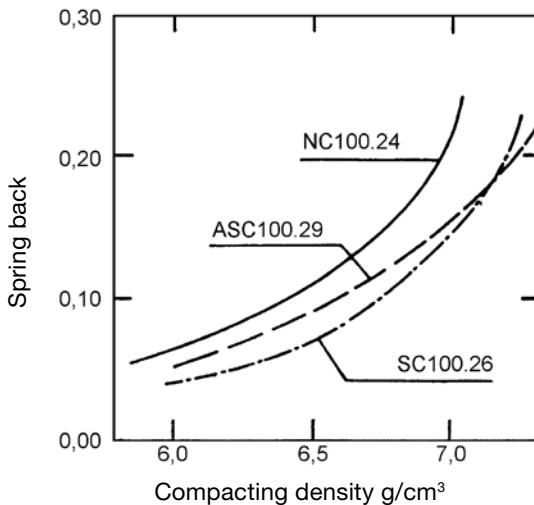


Figure 4.14: Spring back as a function of compact density for three different iron powders. Lubricant addition: 0.8% Zn-stearate.

The decision whether a given structural component can be manufactured by means of P/M-technique depends essentially upon the question whether a suitable compaction tool can be designed and built.

Compaction Tools



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5.1 Introductory Remarks

All compacting tools work by the same general principle:

Metal powder is filled, by gravity, into the cavity of a rigid die. There it is being compacted between two or more axially moving upper and lower punches to form a body of more or less complicated shape and of fairly homogeneous density. The so obtained compact is removed from the die by adequately shifting die and lower punches relative to one another.

The so described procedure appears fairly simple but, as usual, the devil is in the "nuts and bolts", especially when dealing with structural components of complicated shape.

The following twelve points may give a first clue to the problems involved in designing a powder compaction tool:

1. All portions of the die cavity must, in a reliable way, be filled with exact amounts of powder.
2. The density distribution in the compact should be as homogeneous as possible.
3. In all portions of the die cavity, the densification of the powder should take place simultaneously, in order to warrant a sufficiently good binding between adjacent portions. It has to be taken into account that powder flows only very little in lateral directions during densification.
4. The compact must be removable from the compaction tool without getting damaged.
5. All required movements of tool members must be adequately controlled and must be repeatable with sufficient accuracy.
6. The tool should have as few punches as possible.
7. During the entire compaction cycle, punches must never jam, neither with the die, nor with core rods, nor with one another.
8. All tool members must withstand the load exerted upon them during the compaction cycle. They must be as wear-resistant as possible and have the highest possible life expectancy.

9. All functions of the tool must be optimally adapted to the functions available on the compaction press.
10. In order to keep set-up times to a minimum, the design of the tool should be such as to facilitate assembling and installation on the press.
11. In order to keep production stops as short as possible, worn-out tool members should be as easily replaceable as possible.
12. The manufacturing costs for the tool must be reasonable in relation to its expected life-time and to the total number of compacts to be produced in it.

The experienced tool designer knows how difficult it is, in some cases, to do justice to all these points. The more complicated a structural component is, the larger is usually the required number of movements of tool members and of control functions on the press. In the following paragraphs, we will deal with several of the above listed points in more detail.

5.2 The Compaction Cycle

The compacting cycle can be divided into three stages:

1. Filling the die
2. Densifying the powder
3. Removing the compact from the die

Each of these stages is characterized by specific positions or movements of the individual tool members. And in each of these stages, specific technical problems occur, which we will now deal with.

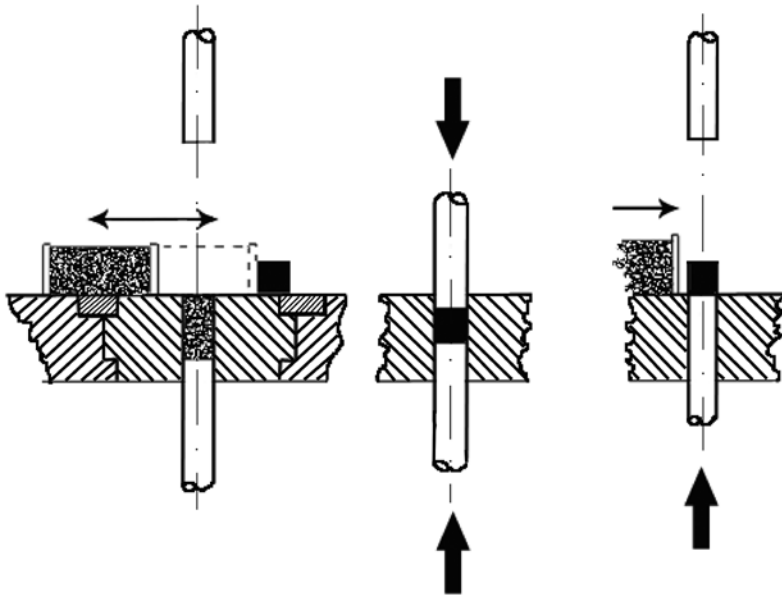


Figure 5.1. Three stages in a compaction cycle: 1) filling the die, 2) densifying the powder, 3) ejecting the compact.

5.2.1 Filling the Die

The powder falls or flows by its own gravity from the filling device into the die cavity. It is almost trivial to mention that cavities having a wide cross-section are more easily filled with powder than such having a narrow cross-section. What is to be considered a narrow cross-section, in this respect, depends on the size of the biggest powder particles.

Most commercial powders include particle sizes up to approx. 0.15 to 0.20 mm. In order to warrant an unimpeded powder flow and a satisfactory die fill, the smallest lateral dimension of a die cavity has to be considerably larger than the largest powder particles. Otherwise, bridging phenomena occur in the powder, of the kind as shown schematically in *Fig. 5.2*, entailing an uneven fill of the die cavity.

The powder may also segregate when flowing through narrow cross-sections. By experience, die cavities can be just about satisfactorily filled, if their smallest lateral dimension is approx. five times larger than the size of the largest powder particles. Thus a conclusion is that structural parts having lateral dimensions smaller than approx. 1 mm are not suitable to be compacted from powder.

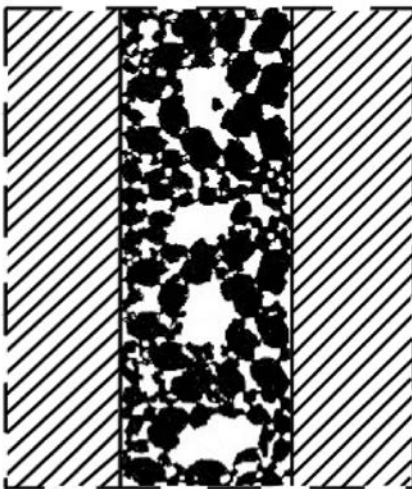


Figure 5.2. Formation of bridges when filling narrow cross-sections.

In cases where the die cavity consists of several portions having different profiles and depths, the filling density of the powder in these portions may vary due to varying flow and filling behaviour of the powder. It may also happen that the

filling density in narrow portions is lower at the bottom than at the top. Such variations in filling density may result in correspondingly varying compact densities. In order to compensate for variations in filling density between different portions of the die cavity, the filling depths of these portions have to be correspondingly pre-adjusted. Larger density variations in the powder compact have negative effects upon its green strength as well as upon its dimensional accuracy and mechanical properties after subsequent sintering and heat-treatment. In order to warrant a satisfactorily homogeneous density in powder compacts, the lateral dimensions of its different portions should measure at least $1/6$ of their respective heights.

5.2.2 Densifying the Powder

In Chapter 4, it has been explained that, due to friction between powder and die wall (core rod), compacts are denser at their two ends near the moving compaction punches, than at their center. The location of lowest density in a compact is usually apparent to the naked eye as a dull zone on the shining lateral surface of the compact.

In most cases, it is best for the properties of the compact if the zone of lowest density, the *neutral zone*, is located approx. half-way between top and bottom of the compact. This is the case when densification takes place between upper and lower punches that move symmetrically relative to the compaction die. Such symmetrical punch movement can, in principle, be achieved in three different ways, as illustrated in *Fig. 5.3*.

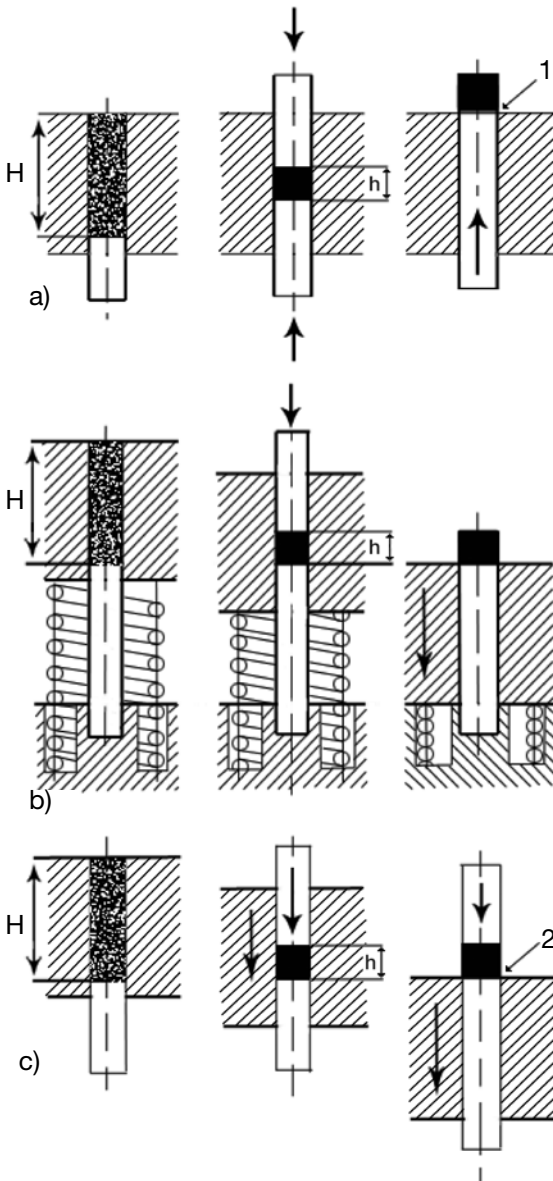


Figure 5.3 Three different concepts to achieve symmetrical double-sided densification:

a) stationary die, and two punches moving symmetrically towards one another,
 b) stationary lower punch and a "floating" die,
 c) stationary lower punch, and the die being withdrawn at half the speed of the upper punch.

- a) The die is stationary and the symmetrical movements of the upper and of the lower punch are generated directly by the press.
- b) The lower punch is stationary and the die is supported by springs or hydraulic cushions to compensate for its gravity. As the upper punch compresses the powder, frictional forces, occurring at the die wall, move the die downwards relative to the stationary lower punch. (Floating-die principle).
- c) The lower punch is stationary. The movements of the die and of the upper punch are actively controlled in such a way that, during densification, the die moves downwards relative to the stationary punch at half the speed of the upper punch.

In case a), the compact is ejected from the die by a corresponding upwards movement of the lower punch. (Ejection principle). In cases b) and c), the compact, resting on the stationary lower punch, gets clear of the die as the latter is being stripped downwards. (Withdrawal principle). Each of the three mentioned procedures, requires the availability of specific functions on the compacting press.

The procedure of the floating die (b) demands only two simple functions from a press: one mechanically or hydraulically generated downward stroke of an upper punch capable of exerting large forces, and one mechanically or hydraulically generated downward stroke of a lower punch capable of exerting somewhat smaller forces.

This procedure is not applicable to compacts having portions of different compaction heights. It also has the disadvantage that the movement of the die, during densification, is generated entirely by frictional forces which are uncontrollable since they are heavily influenced by variations of the lubricant content in the powder, by variations of the die temperature during production and by progressing wear on the die wall. Today, for complicated structural parts, procedures according to a) or c), or combinations of both, are being utilized. They require multiple-function presses, having at least two separately controllable movements capable of exerting large forces and at least one separately controllable additional movement capable of exerting somewhat smaller forces.

As an example of procedure a), four stages of the compacting cycle for a bushing are shown schematically in *Fig. 5.4*. As can be seen, die and core rod do not shift position during densification of the powder. During ejection, the core rod remains in the bushing until the bushing has left the die and has expanded elastically. Then the core rod is withdrawn frictionless. This has a

double advantage:

1. the required ejecting force is considerably smaller and,
2. the pores in the surface of the bore stay open – which they do not if the surface is plastically deformed under high frictional shearing stresses caused by a core rod withdrawn under pressure. (A bushing without open pores in the surface of its bore has no self-lubricating properties).

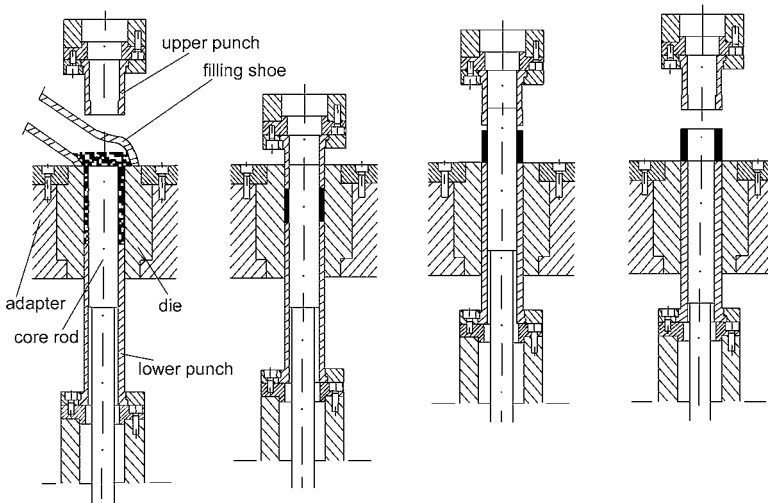


Figure 5.4 Four stages in the compaction cycle for a straight cylindrical bushing.

In the case of thin-walled bushings, the narrow space between die and core rod can be filled more easily if, at the beginning of the filling process, the core rod is withdrawn to a lower position. After the wider die cavity has been filled with powder, the core rod is raised to its normal position, pushing excessive powder back into the filling-shoe. See schematic illustration in *Fig. 5.5*.

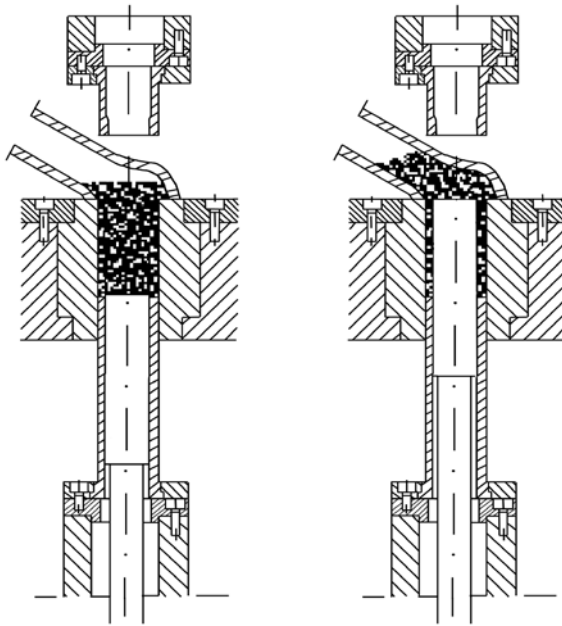


Figure 5.5 Filling of the die cavity with the core rod withdrawn.

As an example of procedure c), three stages of the compaction cycle for a simple two-level part are shown schematically in *Fig. 5.6*. Die and lower punches are mounted on a tool rig, a so-called adapter, which, as a whole, is inserted into the press. Typical for this particular tooling principle is a sidewise retractable slide which, during the compaction phase, supports one of the lower punches.

The right lower punch is, via a connecting rod, lifted to its filling position by means of a spring. During the compaction phase, the lower ram of the press pulls the die platen down at half the speed of the upper punch, while the left lower punch rests on the stationary base platen of the adapter. Under the pressure built-up in the densified powder, the right lower punch moves downwards, against the force of the supporting spring, until it sets upon the slide.

After compaction, the lower ram of the press pulls the die platen further down, and a wedge attached to the die platen forces the slide sidewise. The now unsupported right lower punch follows the die platen down until the compact has come completely clear of the compaction tool.

Nowadays, multi-cross sectional parts are mainly produced on hydraulic CNC multi-level compaction presses. Thus all part levels are individually monitored and a homogenous density distribution is ensured. This gives several advantages

like dimensional stability, improved process capability and mechanical properties of the compacted part. Apart from that, hydraulically driven and controlled tools are more wear-resistant and need less maintenance than tools with sliding support. Further new developments in the market goes into multi-level tools on electric driven compaction presses which might be able to further improve the powder metallurgical production process.

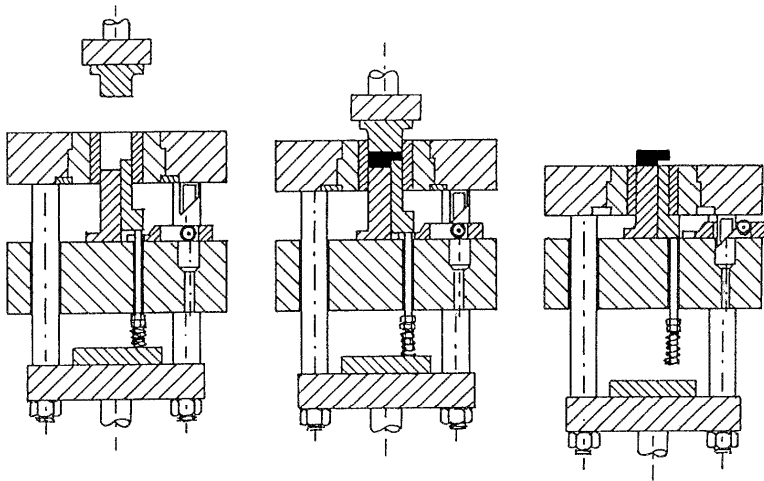


Figure 5.6 Three stages in the compaction cycle for a simple two-level part utilizing a withdrawal-type tool with sliding support.

5.2.3 Removing the Compact from the Die

During the compaction cycle on a mechanical press without any auxiliary devices, the upper punch exerts its maximum pressure at the lower dead-point. Then, it moves upwards again, suddenly taking the axial pressure off the compact and the lower punches which now expand elastically in axial direction.

If there are lower punches of different length (as e.g. when compacting flanged bushings), their different axial expansions can create cracks in the compact yet before it leaves the die. Different elastic expansion of differently high portions of the compact add to this effect. See schematic illustration in *Fig. 5.7*.

Cracks caused by this effect are malicious, especially in flanged bushings, because they are difficult to detect and do not heal during subsequent sintering. In order to avoid this kind of cracks, all portions of the compact must be kept under a well balanced moderate axial pressure during the whole ejecting procedure.

At the end of the compaction phase, die and lower punches are shifted relative to one another in such a way that the compact is being pushed towards the exit of the die. To achieve this effect, it is irrelevant whether the die is stationary and the punches are moving or vice versa. The important point is that, during this procedure, the lower punches are not moving relative to one another in such a way that cracks are created in the compact.

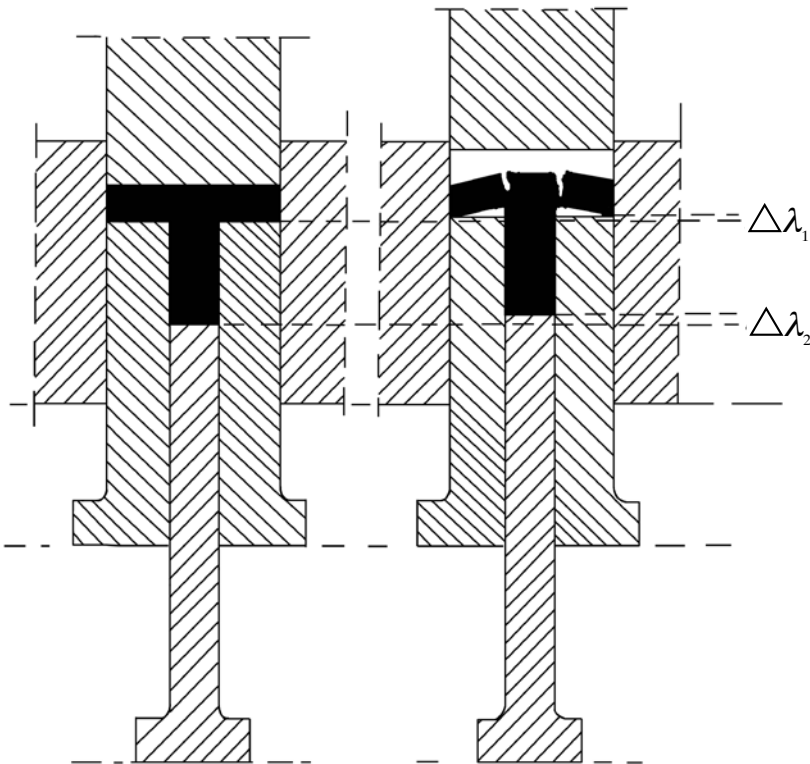


Figure 5.7 Crack formation due to different elastic expansion of two lower punches when the upper punch is being released.

As the compact exits the die, the protruding part, freed from the compressive lateral stress of the die, expands laterally, while the rest of the compact is still constrained in the die. In this transient phase, high shearing stresses occur which may create horizontal cracks in the compact as illustrated schematically in *Fig. 5.8a*.

In order to reduce these shearing stresses, the die is slightly tapered at the exit and its rim is rounded off. See schematic illustration in *Fig. 5.8b*.

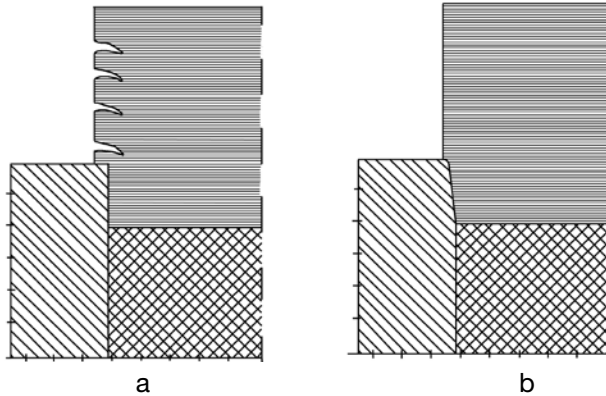


Figure 5.8 Ejection procedure: a) crack formation as the compact passes a sharp upper rim of the die cavity, b) crack formation avoided by tapering the die exit and rounding-off the upper rim of the die cavity.

Particularly susceptible to cracking during ejection are compacts of the type as schematically illustrated in *Fig. 5.9*. The compact shown consists of a sturdy upper portion and a thin skirt-like lower portion. Shock absorber pistons for automobiles fall into this category.

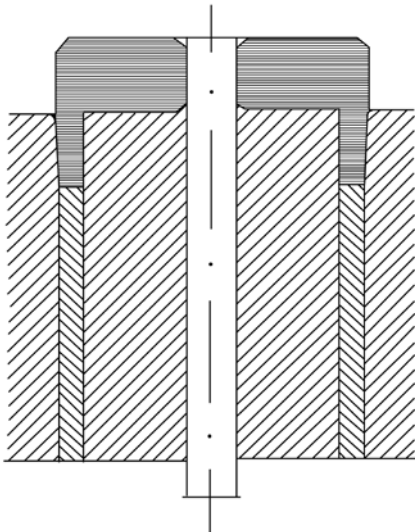


Figure 5.9 Ejection procedure: risk of crack formation between the sturdy upper segment and the thin skirt-like lower segment of a compact (e.g. shock absorber piston).

The lateral contours of certain portions of a complicated compact are partly or entirely defined by lateral faces of core rods and punches. In order to clear all portions of the compact from the tool without creating cracks, the movements of all tool members involved in the ejecting process must be separately controllable. This requires not only a complicated tool design but also a press equipped with adequate auxiliary functions.

After ejection, the compact has to be removed from the press, without getting damaged. In the simplest case, the next stroke of the filling shoe pushes the compact to a chute on which it slides, in single file with its equals, into a suitable container for intermediate storing before sintering.

Fragile compacts and compacts of delicate shape, have to be picked up carefully by means of a small automatic gripping device which transfers them individually to a special tray on which they subsequently can pass through the sintering furnace. Compacts must, of course, have sufficient green-strength to withstand handling without abrasion or breakage. And they should, if ever possible, have one sufficiently plane face to stand on stable on their way through the sintering furnace.

In certain cases, it may be advantageous to turn the compacts automatically as they come out of the die before letting them slide down a chute or before placing them on a tray.

5.2.4 Compaction Cycle on Presses Equipped with Multiple Platen Systems

Complicated sequences of punch movements are required in cases where the shape of the compact cannot be duplicated proportionally by the filling space. A typical example is a component with a blind hole and a flange at the same end, as shown in *Fig. 5.10*. The only way to produce this part, if the type of press allows it, is by powder transfer:

First, the die cavity is filled up with powder as if the blind hole was at the opposite end of the die. Then dropping this column of powder, without densifying it, downwards to the lower end of the part. The different powder columns must then be densified at different rates proportional to their initial heights in order to achieve the same pressure gradient in all powder columns, such as to avoid radial powder transfer and to achieve favourable positions of the neutral zones. In order to avoid cracks during ejection of the compact, a certain axial pressure must be maintained, on all portions of the compact.

Last, when the compact has cleared the die, the inner upper punch is extracted from the compact against the supporting outer upper punch. Many structural

parts, such as employed in the automobile industry, are of multi-level type with shapes even more complex than the example shown in *Fig. 5.10*.

The complicated sequences of punch movements involved in the compacting procedure for these parts can be performed successfully only on special types of presses. During all stages of the compaction cycle, the time- pressure- and stroke-depending movements of die, core rods and various upper and lower punches have to be coordinated in the correct relation to one another.

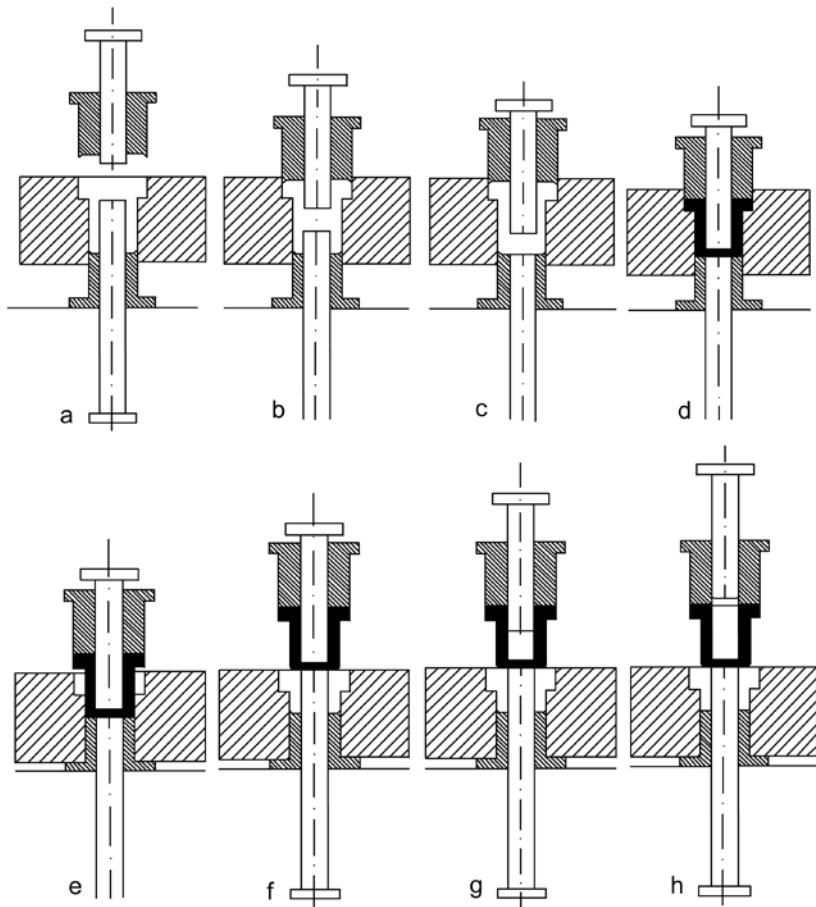


Figure 5.10 Compaction cycle for a component with flange and blind hole at the same end : a) filling, b), c) powder transfer without densification, d) densification, e) f) g) h) ejection.

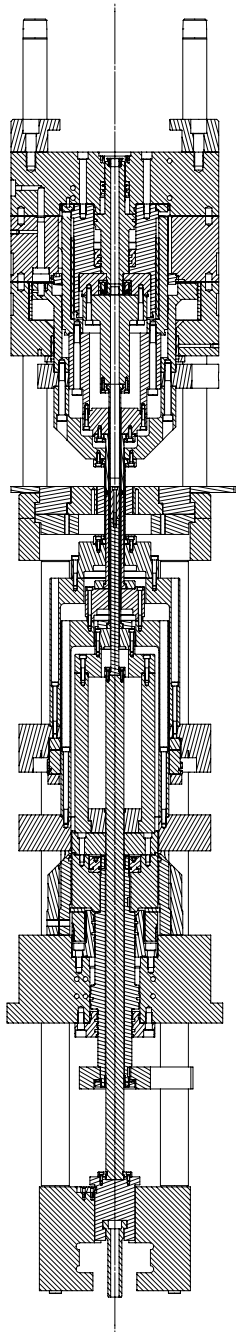


Figure 5.11 Multi-platen adapter, type Dorst HMA160.33 with eight separately controllable tool movements, used for compacting a synchronizing hub.

On modern hydraulic CNC-presses with integrated multi-platen adapter, working according to a combined withdrawal/ejection procedure, up to ten separately controllable movements of die, core rods and punches are available. By means of a precision-measurement system in combination with a highly sensitive servo-hydraulic system, exactly timed sequences of all required movements can be programmed both with respect to pressure and stroke length. In Fig. 5.11, a multi-platen adapter, type DORST HMA160.33, for eight separately controllable movements is shown. This type of adapter is utilized e.g. for compaction of synchronizing hubs with three upper and three lower levels.

In Fig. 5.12 is the compaction of a double-gear with internal splines illustrated. The double-gear has upper and lower faces on three different levels each. Apart from die and core rod, which move simultaneously, the tool has three separately controllable upper punches, one stationary and two separately controllable lower punches.

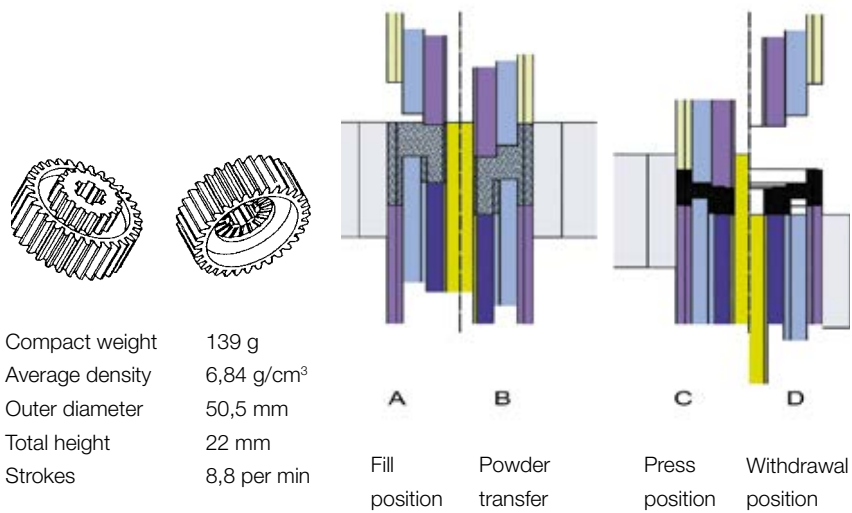


Figure 5.12 Four stages in compacting a double-gear with internal splines on a multi-platen adapter, type DORST MPA/H140. For technical data, see table 5.1.

The achieved homogenous density distribution in this part is indicated on the drawing shown in *Fig. 5.13*.

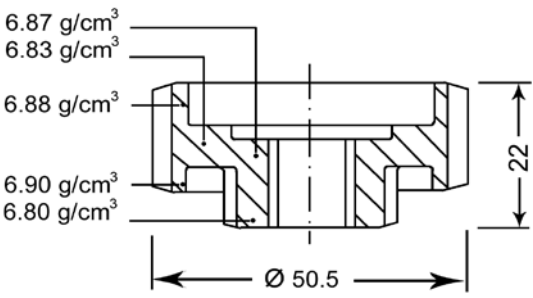


Figure 5.13. Density distribution in the double-gear produced on a multi-platen adapter as shown in Fig. 5.12.

Table 5.1. Technical data

Press	Dorst TPA 140
Adapter	MPA/H140
Compacting Force	95 ton
Compacting Speed	8,8 pieces/min
Powder	Distaloy AE
Compacting Area	12,6 cm²
Weight	139 g
Average Density	6,84 g/cm³

5.3 Designing a Compaction Tool

In the following, we outline the principal procedure of designing a compaction tool. As a representative example, we choose a part having two parallel holes and two portions of different height as shown in Fig. 5.14. Based on the technical drawing of this structural part, a proportionally correct sketch of the tool is being developed from which the required functions of the various tool members can be be understood.

Subsequently, exact dimensions and tolerances for all tool members are being established. Eventually, adequate tool materials as well as machining- and heat-treating procedures are being considered.

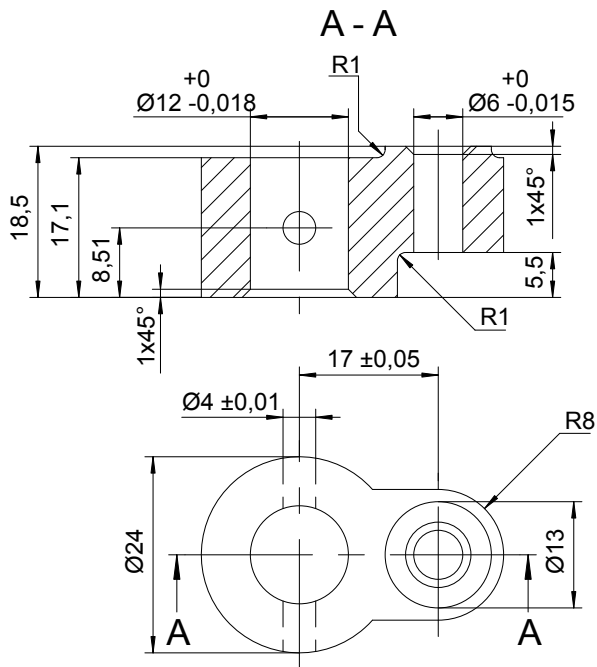


Figure 5.14 Drawing of a crank having two portions of different height and two axial bores, intended to be manufactured by PM-technique.

5.3.1 Functional Sketch of the Tool

The development of the functional sketch proceeds, essentially, in four steps:

Step 1.

First, it has to be decided which way around the part is best to be compacted. Since the part has one relatively flat and one stepped face, the most practical way to compact it is with its flat face up. Then, one undivided upper punch suffices, but two lower punches are required.

Step 2.

After it has been decided with which side up the part is to be compacted, a vertical section through the part is outlined on drawing paper and all vertical boundaries of the section are extended upwards and downwards. These extended lines indicate already the vertical contours of die, punches and core rods. The horizontal boundaries of the section indicate the positions of the punch faces at the end of the compaction stage. See sketch (a) in *Fig. 5.15*.

Step 3.

The required filling depths for the two portions of the part can be calculated by means of the ratio Q between compact density and filling density (apparent density) of the powder according to the following relationship:

$$Q = \text{Compact Density} / \text{Filling Density} = \text{Depth of Fill} / \text{Height of Compact}$$

Commercial iron powders have filling densities between 2.4 and 3.0 g/cm³. If we base our example on an assumed filling density of 2.60 g/cm³, and an assumed compact density of 6.42 g/cm³, then: $Q = 6.42/2.60 = 2.47$.

In order to obtain the required depths of fill, the heights H_1 and H_2 of the two portions of our part have to be multiplied with this factor. The height of the left portion of the part is $H_1 = 17$ mm, and the height of its right portion is $H_2 = 13$ mm. Thus, the respective depths of fill are $F_1 = 17 \text{ mm} \times 2.47 = 42 \text{ mm}$ and $F_2 = 13 \text{ mm} \times 2.47 = 32.1 \text{ mm}$. We decide that the left powder column is to be compacted symmetrically from top and bottom. This means, during densification of the left powder column, the upper punch and the left lower punch are to travel equal distances inside the die. Consequently, at the end of the densification process, the center of the left portion is located half-way between the upper rim of the die and the filling position of the left lower punch.

Thus, we mark the position of the upper rim of the die at distance $F_1/2 = 21$ mm above and the filling position of the left lower punch at distance $F_1/2 = 21$ mm below the center of the left portion. Then, at distance $F_2 = 32.1$ mm below the so found upper rim of the die, we mark the position of the right lower punch. See sketch (b) in *Fig. 5.15*.

Step 4.

Assuming that a minimum guidance in the die of 25 mm is required for the lower punches, the die has to be at least 25 mm higher than the largest filling depth. Thus, we mark the lower rim of the die at distance $A = F_1 + 25$ mm = 67 mm below its upper rim. Eventually, the lengths of the punches are to be considered. Both lower punches have to be long enough to fully eject the compact from the die, i.e. they have to be at least 67 mm long.

The upper punch has, of course, to be long enough to penetrate the die as deep as needed to attain the desired compact height, i.e. its length has to be at least $(F_1 - H_1)/2 = 12.5$ mm. To these lengths, a margin of 5 - 10 mm should be added to allow for the correction of worn punch profiles. After this, the rough design of our compaction tool is complete. See sketch (c) in *Fig. 5.15*.

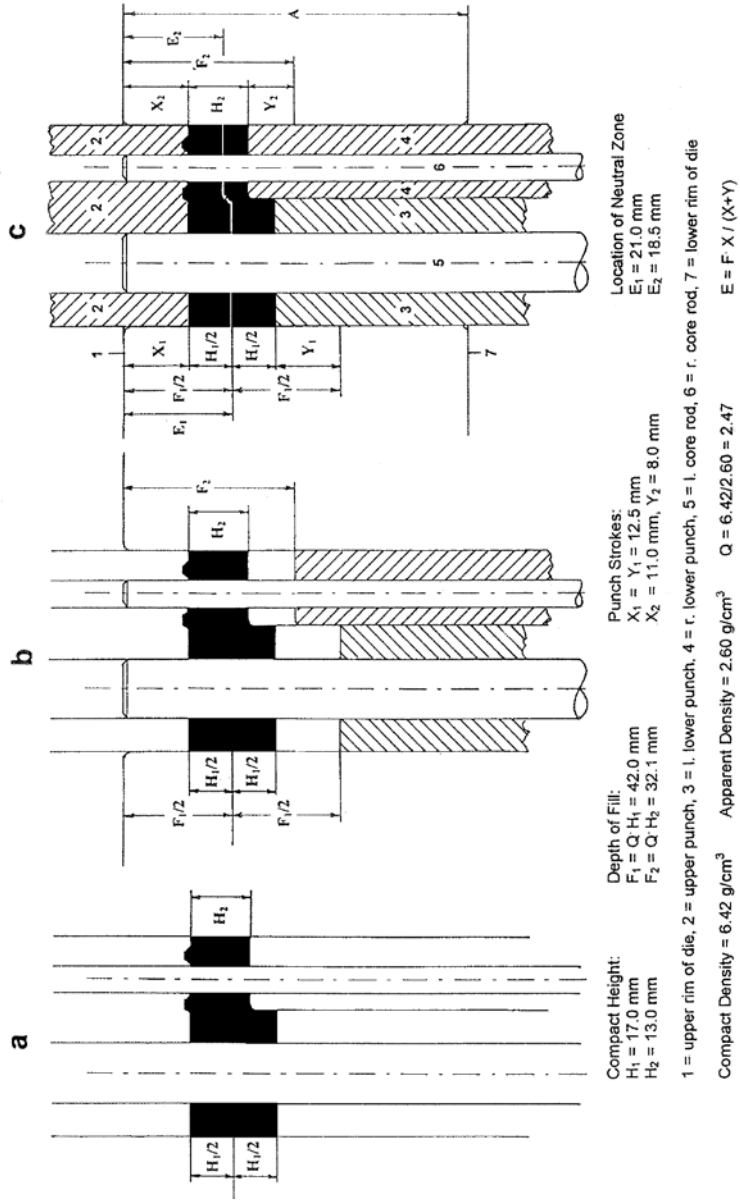


Figure 5.15 Step-by-step sketching of a compacting tool for the component shown in Fig. 5.14: a) drawing the contours of die walls, punches and core rods, b) finding the filling positions of the lower punches and the position of the upper rim of the die, c) finding the location of neutral zones and the position of the lower rim of the die.

The final design of this tool, conceived for the withdrawal method, can be seen from the drawing shown in *Fig. 5.16*.

Of special interest, in this context, is the **location of the neutral zone** (zone of lowest density) in the two sections of our compact. In chapter 4 (Compacting of Metal Powders) it has been explained that, due to frictional forces at the die wall, the compact density decreases with increasing distance from the face of a moving punch.

If only the upper punch is moving relative to the die, the zone of lowest density is located at the face of the stationary lower punch. If upper and lower punch are moving symmetrically relative to the die, the zone of lowest density appears exactly half-way between the faces of the moving punches. If the two punches move unsymmetrically, the zone of lowest density lies nearer to the face of the lesser moving punch.

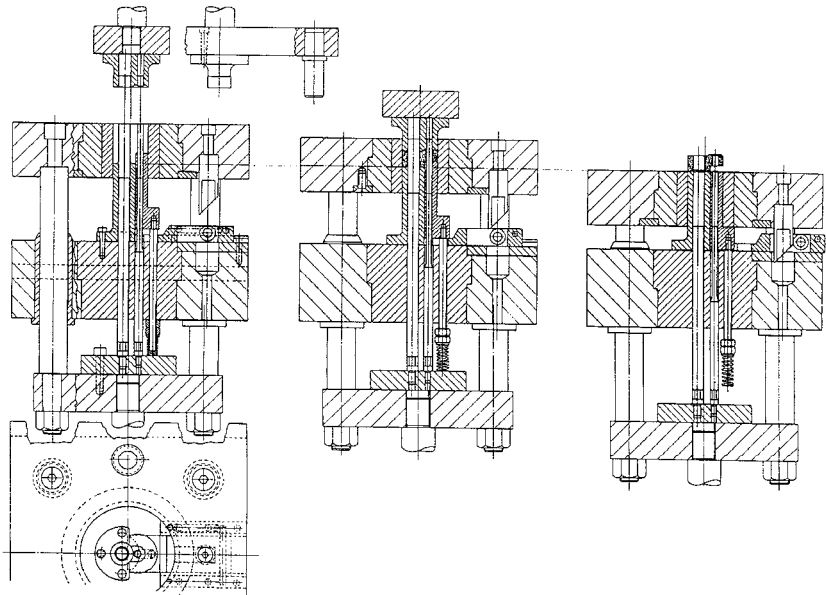


Figure 5.16 Complete design of the tool sketched in Fig. 5.15, adapted to the withdrawal principle with sliding support. These types of toolings nowadays will be designed as hydraulically pre-lifted tool types.

The relationship between punch movements and location of the neutral zone can be described by a simple formula. Be F the depth of fill, be X and Y the distances traveled by the upper and lower punch respectively, and be E the distance of the neutral zone from the upper rim of the die, then the following general relationship applies:

$$E = F \frac{X}{X + Y} \quad (5.1)$$

If upper and lower punch move symmetrically relative to the die, i.e. if $X = Y$, it follows:

$$E = \frac{F}{2} \quad (5.2)$$

During densification of the left portion of the compact, upper and lower punch travel the same distance $X_1 = Y_1 = 12.5$ mm. Thus, according to (5.2), the neutral zone of this portion is located at distance $E_1 = F_1 / 2 = 42 \text{ mm} / 2 = 21$ mm below the upper rim of the die.

The location of the neutral zone in the right portion of the compact can be calculated as follows. Since the upper punch has a 1.5 mm deep groove (to form the little bulge on top of the right portion), it can dip into the die approx. 1.5 mm deep without noticeably densifying the right powder column; (the powder escapes into the groove).

Until reaching its lowest position, the upper punch travels a remaining distance of $X_2 = X_1 - 1.5 \text{ mm} = 11$ mm. Simultaneously, the right lower punch travels a distance of $Y_2 = 8.1$ mm upwards. Thus, according to (5.1), the neutral zone of the right portion of the compact is located at distance $E_2 = 32.1 \times 11 / (11 + 8.1) = 18.5$ mm below the upper rim of the die, i.e. 2.5 mm below the center of the right portion and 2.5 mm higher than the neutral zone of the left portion. If the neutral zones of the two portions would be too far apart, cracks might be created at the joint of the two portions during densification.

Ideally, the movements of the two lower punches should be coordinated in such a way that the two powder columns standing upon them get densified simultaneously and homogeneously. If densification in the two powder columns proceeds at different rates, unsymmetrical lateral pressures act upon the two parallel core rods, possibly causing unacceptable deviations from specified tolerances on central distance and parallelism of the two bores. Prematurely worn or broken core rods may also be a consequence of unsymmetrical lateral pressures.

5.3.2 Dimensions and Tolerances on Tool Members

When pinpointing the final dimensions and tolerances for the various tool members, not only the final dimensions and tolerances of the structural part, as specified on the customers' drawing, must be considered, but also the dimensional changes which the compact undergoes during ejection from the compacting die and during subsequent sintering.

Dimensional changes of the compact's longitudinal dimensions do not constitute any greater problem, because they can relatively easily be compensated for by slight adjustments of punch positions and movements. Much more critical are dimensional changes of the compact's transversal dimensions, because they cannot be adjusted without disassembling the compaction tool and regrind or entirely remake die and punches. Thus, before finally laying down transversal dimensions and tolerances of tool members, it is most important to very carefully establish the dimensional changes of the compact under production-like compacting and sintering conditions.

Dimensional change data from previously produced parts of similar shape and composition may be a good guidance. To rely solely on data established under laboratory conditions is risky. In this context, it must be kept in mind that dimensional changes during sintering are sensitive not only to variations in sintering temperature and time but also to variations in powder composition and compact density. We demonstrate the procedure of calculating the transversal dimensions of a compacting tool for the case of a straight bushing. The drawing of the bushing specifies:

outer diameter = D_a , tolerance = $+\Delta D_a$, inner diameter = D_i , tolerance = $-\Delta D_i$.

From previous production of similar bushings, the following data are known: average spring back after compacting = e %, average dimensional change during sintering = s % (+ for swelling, - for shrinkage). The tool dimensions to be calculated are: inner diameter of the die = d_m , and outer diameter of the core rod = d_k . It is expected that, due to wear during production, the inner diameter of the die (d_m) increases and the outer diameter of the core rod (d_k) decreases.

In order to keep the dimensions of the sintered bushing within specified tolerances, the following limitations have to be observed when dimensioning die and core rod:

$$(D_a + \Delta D_a)/(1 + e + s) > d_m > D_a/(1 + e + s) \quad (5.3)$$

and

$$D_i/(1 + e + s) > d_k > (D_i - \Delta D_i)/(1 + e + s) \quad (5.4)$$

Theoretically, the optimal utilization of die and core rod would be attainable if the initial value of d_m is as small as the right side of (5.3) allows, and the initial value of d_k as large as the left side of (5.4) allows. In order to make sure that the dimensions of the sintered bushings are within specified tolerances even in case dimensional changes e and s should vary, the specified tolerance ranges are narrowed at both ends by 20%. In other words, it is being assumed that the specified limits are $D_a + 0.2\Delta D_a$ and $D_a + 0.8\Delta D_a$ for the outer and $D_i - 0.2\Delta D_i$ and $D_i - 0.8\Delta D_i$ for the inner diameter of the bushing. Thus, for the inner diameter of the die and for the outer diameter of the core rod, the following relationships are stated:

$$d_m = (D_a + 0.2\Delta D_a) / (1 + e + s) \quad (5.5)$$

$$d_k = (D_i - 0.2\Delta D_i) / (1 + e + s) \quad (5.6)$$

Consequently, the allowable wear on the die is:

$$\Delta d_m = 0.6\Delta D_a / (1 + e + s) \quad (5.7)$$

and the allowable wear on the core rod is:

$$\Delta d_k = -0.6\Delta D_i / (1 + e + s) \quad (5.8)$$

Applying equations (5.5) to (5.8) to the structural part shown in Fig. 5.15, we can now calculate the final transverse dimensions of the compaction tool. According to specifications on the drawing, the outer diameter of the higher portion of the part is $D_a = 23.90$ mm with tolerance $\Delta D_a = +0.20$ mm, and its inner diameter is $D_i = 12.00$ mm with tolerance $\Delta D_i = -0.018$ mm. We assume that the average spring back is $e = +0.1\%$ and the average dimensional change during sintering is $s = +0.4\%$. On the basis of these data, we obtain for the initial values of the inner diameter d_m of the die and of the outer diameter of the core rod d_k :

$$d_m = (23.90 + 0.2 \times 0.2) / 1.005 = 23.821 \text{ mm}$$

$$d_k = (12 - 0.2 \times 0.018) / 1.005 = 11.937 \text{ mm}$$

and for the allowable wear:

$$\Delta d_m = (0.6 \times 0.2) / 1.005 = 0.119 \text{ mm}$$

$$\Delta d_k = -(0.6 \times 0.018) / 1.005 = -0.011 \text{ mm}$$

The remaining tool dimensions can be calculated analogously. A small computer program takes quickly and reliably care of these calculations. It is recommended to collect, in a synoptical table, all important dimensional data, pertaining to a structural part to be produced or already in production. See *Table 5.2*.

Table 5.2. Dimensional Data Pertaining to the Component shown in Fig. 5.15

B	Z (mm)	S (mm)	P (mm)	K (mm)	W (mm)	V (mm)
D _a (1)	23,90 ^{+0,20}	≥ 23,940	≥ 23,845	23,821	23,817 ^{+0,009}	+0,119
D _i (1)	12,00 ^{-0,018}	≤ 11,996	≤ 11,949	11,937	11,943 ^{-0,006}	-0,011
D _a (2)	15,90 ^{+0,20}	≥ 15,940	≥ 15,877	15,861	15,856 ^{+0,008}	+0,119
D _i (2)	6,00 ^{-0,015}	≤ 5,997	≤ 5,973	5,967	5,97 ^{-0,005}	-0,009
L	16,95 ^{+0,10}	17,00	16,932	16,916	16,912 ^{+0,008}	0,000
L = central distance of the two bores D _i (1) and D _i (2) B = designation Z = dimension and tolerance specified on customer's drawing P = allowable average dimension after compacting in virgin tool S = allowable average dimension after sintering (at the beginning of tool usage) K = guiding measure for tool design W = virginal tool dimension (manufacturing tolerance IT 5) V = allowable wear spring back = 0.1%; dim. change after sintering = 0.4% (assumed values)						

The dimensions (W) given in Table 5.2 are referring to die and core rod sizes, as the die and core rods actually form the profile of the component, whereas the punches only form the faces. The punches are marked with a clearance dimension, but no tolerance, and a note is added setting the actual clearance in terms of the die or core rods. This is important, because the clearances involved are so small, that to state a separate tolerance for both die and punch, would mean a greater variation in actual clearance than is practical.

As an example, a circular die cavity can be ground and lapped to a tolerance 0.005 mm and a circular punch can be made to a similar tolerance, thus giving a total tolerance for the two parts of 0.010 mm. If we require a clearance between die and punch of 0.010 to 0.015 mm, it is clear that it is better to state a tolerance only for the die which actually forms the profile of the compact and give the punch size as a clearance rather than as a size with a tolerance. This method gives the toolmaker a better opportunity to produce an effective clearance without working to impossible tolerances.

Clearance recommendations vary, depending on compaction pressure, type of powder and other circumstances. Makers of bushings use clearances as small as 0.005 to 0.010 mm in some cases, but generally accepted clearances are given in Table 5.3.

**Table 5.3. Recommended Clearance
between Sliding Tool Members***

Tool Dimension (mm)	Clearance (=IT 5) (μm)
≤ 10	10 – 15
10 – 18	12 – 18
18 – 30	15 – 22
30 – 50	18 – 27
50 – 80	21 – 32
80 – 120	25 – 38

When applying the approximate clearances recommended in Table 5.3, it must be kept in mind that punches expand elastically under the compacting load. This means that the clearance between die and punches decreases and the clearance between core rod and punch increases. The application of such narrow clearances to profiled dies and punches presents a difficult toolmaking problem, but the satisfactory running of the tool over a reasonable period does not permit greater clearances.

A prerequisite for a long tool-life is an extremely good finish on all sliding surfaces (typical: 0.2 μm) and a proper pairing of the surface hardnesses of the sliding partners. Here applies an old rule from mechanical engineering: *Sliding partners should not be made from exactly the same material and must have different surface hardnesses.*

* H.G. Taylor, A Critical Review of the Effects of Press and Tool Design upon the Economics of Sintered Structural Components, Powder Metallurgy, 1965, Vol. 8, No 16 (S. 285 - 318).

5.3.3 Tool Materials

Punches.

As has been mentioned before, powders are usually compacted with pressures between approx. 400 and 800 MPa/mm². All punches of the compaction tool have to withstand these high loads not only once but several 100 000 to 1 000 000 times without breaking or getting plastically deformed. Neither may they under these loads expand elastically to such an extent that they jam in the die. Even an ever so small amount of plastic deformation during one compacting cycle would, after a number of cycles, lead to a sizable shortening and thickening of the punch. It does not take much imagination to realize the consequences: As the punch gets shorter, the height of the compacts increases correspondingly and as the punch gets thicker, it eventually jams in the die and breaks and possibly damages the entire tool.

Thus, punches must possess high compressive yield strength, high toughness and high fatigue strength. In cases where punches form part of the side walls of the compaction tool, they must, in addition to the mentioned properties, have a sufficiently high surface hardness. Surface-hardening of punches, if necessary, has to be carried out with great care, in order to avoid embrittlement and surface cracking. Only the toughest types of tool steels are suitable for punches. Ideally, they should combine the following properties:

- Good machinability when soft-annealed.
- Highest possible toughness and fatigue strength after hardening.
- Highest possible dimensional stability and lowest possible susceptibility to cracking in the hardening procedure.
- Highest possible wear resistance.

Selecting the right tool steel for a particular punch and choosing the appropriate heat-treatment, is mainly a matter of experience. Specification charts and heat-treating suggestions provided by steel makers can be helpful.

Some typical tool steels used for punches are listed below:

- ASP2023 (Erasteel)
- SPM23 (Uddeholm)
- Vanadis 4 extra (Uddeholm)
- S690 (Böhler)
- CPM3V (Crucible)

More information can be found on these websites:

- www.erasteel.com
- www.uddeholm.com
- www.bohlersteel.com
- www.crucible.com

Dies and Core Rods.

Dies and core rods should best be made from cemented carbides. Although being much more expensive than steel, cemented carbides, because of their extremely high hardness and superior wear resistance, are the most economic choice for large production series. For shorter series, however, certain high-speed steels are a less expensive alternative. Due to their high content of hard carbides embedded in a tough steel matrix, high-speed steels are quite wear-resistant, though not on par with cemented carbides. Cemented carbide dies must always be backed up by a shrink-ring of tough steel to prevent it from bursting under the high radial pressure exerted upon its inner wall during the compaction procedure. The shrink-fitting process provokes high compressive tangential stresses in the inner wall of the die, increasing its wear resistance even further. The ratio between outer and inner diameter of the shrink-ring should be at least 2:1, or better, 4:1.

Sharp corners or incisions in the profile of the die cavity should be avoided, since they provoke high tangential tensile stresses which might burst the die. On the other hand, when the shape of the structural part requires sharp corners or incisions in the die, it is not necessarily a disaster if the die should crack, because in most cases, the shrink-ring keeps the cracked die in place.

As can be seen, e.g. from the drawing in *Fig. 5.16*, core rods are usually much longer than the punches in which they are guided. During the compaction and during the ejecting phase, core rods are, via frictional forces, subjected

alternately to high compressive and high tensile stresses, especially if they are thin and have complicated profiles. Core rods should, therefore, be as tough and fatigue resistant as possible. But this requirement is obviously in conflict with the demand for highest possible wear resistance, i.e. highest possible surface hardness. This conflict can be solved, e.g. in one of the following two ways:

- a) The core rod is made in one piece, heat-treated for toughness and induction-hardened at its upper end where it is exposed to wear.
- b) The core rod is made in two pieces, one short upper piece of cemented carbide which is joined, by one or another method, to a long lower piece of tough-hardened steel.

5.4 Further Recommendations

Symmetrical Load Distribution on Punches.

The tool assembly on the press should be carefully centered, to warrant the punches being loaded as symmetrically as possible during compacting. For punches with circular or regular cross-section, their cross-sectional center of gravity can easily be brought in line with the center line of the press and frictional forces act symmetrically upon their lateral faces.

Achieving a symmetrical load distribution, on punches with unsymmetrical cross-sections, is a more complicated affair. Their cross-sectional center of gravity can certainly be brought in line with the center line of the press, but frictional forces do not act symmetrically upon their lateral faces. Since those frictional forces cannot be calculated very accurately in beforehand, the optimal centering of the tool assembly on the press may constitute a serious problem.

In a badly centered tool, punches get out of parallel with die and core rods when subjected to the compaction load. They scrape hard on die and core rods, causing excessive local wear which, if not detected and corrected in time, leads to a complete break-down of the tool.

When loaded unsymmetrical, thin and sleeve-like punches tend to bend elastically to such a degree, that clearances between them and the die wall get out of concentricity. At places of enlarged clearance, powder is being extruded into the gap, forming excessive burrs on the face of the compact. At places of narrowed clearance, punches scrape hard on die walls and core rods. This leads to excessive tool wear and increases the risk of jammed punches and broken core rods. An uneven density distribution adds to this effect.

Influence of Profiles.

For good functionality and long life of the various tool members it is important, not only to choose the right tool material but also to avoid profiles that provoke high stress peaks under load. Finite element analysis can help to avoid unsuitable shapes and profiles. In particular, the following points should be observed:

- Avoid sharp corners and edges on the cross-sectional profiles of die, punches and core rods.
- Avoid sharp-edged protrusions or incisions on punch faces.
- Avoid core rod diameters smaller than $1/3$ - to $1/5$ the length of the core rod's portion in contact with the powder.

In order to avoid kinking under load, keep unguided portions of core rods and connecting rods as short as possible.

The strict observation of these recommendations helps to increase the fatigue strength and wear resistance of tool members and to prevent stress-induced cracks during the heat-treatment of the tool and later when it is operating.

5.4.1 Tooling Costs

The manufacturing costs of compacting tools can vary between some 10 000 and 100 000 US \$, depending on size and number of separately moveable parts. Tools for long series of compacts must, of course, be designed for maximal possible tool-life. This means: cemented carbides for the die and for the shaping segments of the core rods, high quality steel and optimal heat-treatment for the punches, maximum surface finish on all sliding faces and a perfect fit between die, punches and core rods - in other words, high material and workshop costs.

The plain material costs for a compaction tool amount to approx. 15% of the total manufacturing costs (designing cost not included). With very complicated tools, the share of material costs is even smaller. This makes it clear that saving on material costs often turns out to be saving at the wrong end. Costs for waste, tool repair, production losses, and delayed delivery, as consequences of failing tool materials or sloppy tool assembling, can amount to a multiple of the total initial tooling costs.

Designing time can easily accumulate to several weeks if the tool is of a more complicated type. Computer-aided design and machining as well as computer-controlled production procedures, are generally used, but are no substitute for the creativity of the tool designer or for the experience and skill of the toolmaker.

From the standpoint of economy, it is important to carefully watch the performance of any particular tool during its entire life-time and to document pedantically character and cause of any malfunction of the tool as well as the life of each tool member. Only by such systematic routine, a reliable tool know-how can be accumulated, which helps to avoid future mistakes in tool design and toolmaking.

Sintering is the process by which metal powder compacts (or loose metal powders) are transformed into coherent solids at temperatures below their melting point. During sintering, the powder particles are bonded together by diffusion and other atomic transport mechanisms and the resulting somewhat porous body acquires a certain mechanical strength.

Sintering

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6.1 General Aspects

The sintering process is governed by the following parameters:

- temperature and time
- geometrical structure of the powder particles
- composition of the powder mix
- density of the powder compact
- composition of the protective atmosphere in the sintering furnace

The practical significance of these parameters can be described briefly as follows:

Temperature and Time.

The higher the sintering temperature, the shorter is the sintering time required to achieve a desired degree of bonding between the powder particles in a powder compact (specified e.g. in terms of mechanical strength).

This constitutes a dilemma: From the view point of production efficiency, shorter sintering times would be preferable; but the correspondingly higher sintering temperatures are less economical because of higher maintenance costs for the sintering furnace. In iron powder metallurgy, common sintering conditions are: 15 - 60 min at 1120 - 1150°C.

Geometrical Structure of the Powder Particles.

At given sintering conditions, powders consisting of fine particles or particles of high internal porosity (large specific surface), sinter faster than powders consisting of coarse compact particles. Again, we have a dilemma: Fine powders are usually more difficult to compact than coarse powders and compacts made from fine powder shrink more during sintering than compacts made from coarse powder. Particles of commercial iron powders (spongy or compact types) for structural parts are usually $\leq 150 \mu\text{m}$ (ref. Chapter 3).

Composition of the Powder Mix.

The components of powder mixes are selected and proportioned with a view to achieving desired physical properties and controlling dimensional changes during sintering (ref. Chapter 3). When mixes of two or more different metal

powders (e.g. iron, nickel and molybdenum) are sintered, alloying between the components takes place simultaneously with the bonding process.

At common sintering temperatures (1120 - 1150°C), alloying processes are slow (except between iron and carbon) and a complete homogenization of the metallic alloying elements is not achievable. If the powder mix contains a component that forms a liquid phase at sintering temperature (e.g. copper in iron powder mixes), bonding between particles as well as alloying processes are accelerated.

Density of the Powder Compact.

The greater the density of a powder compact, the larger is the total contact area between powder particles and the more efficient are bonding and alloying processes during sintering. Furthermore, these processes are enhanced by the disturbances in the particles' crystal lattice caused by plastic deformation during compaction (ref. Chapter 1, § 1.2.3, § 1.2.4).

Composition of the Protective Atmosphere in the Sintering Furnace.

The protective atmosphere has to fulfill several functions during sintering which in some respects are contradictory. On the one hand, the atmosphere is to protect the sinter goods from oxidation and reduce possibly present residual oxides; on the other hand, it is to prevent decarburization of carbon-containing material and, vice versa, prevent carburization of carbon-free material.

This illustrates the problem of choosing the right atmosphere for each particular type of sinter goods. In iron powder metallurgy, the following sintering atmospheres are common :

- reducing-decarburizing type: hydrogen (H_2), cracked ammonia (75% H_2 , 25% N_2)
- reducing-carburizing type: endogas (32% H_2 , 23% CO, 0-0.2% CO_2 , 0-0.5% CH_4 , bal. N_2)
- neutral type: cryogenic nitrogen (N_2), if desirable with minor additions of H_2 (to take care of residual oxides) or of methane or propane (to restore carbon losses)

Proper choice and careful control of the sintering atmosphere are important but difficult because of circumstances which will be dealt with in detail in paragraph 6.4.

6.2 Basic Mechanisms of Sintering

6.2.1 Solid State Sintering of Homogeneous Material

Judging by the changing shape of the interspace between sintering particles, the sintering process passes through two different stages: 1) an early stage with local bonding (neck formation) between adjacent particles and 2) a late stage with pore-rounding and pore shrinkage. In both stages, the bulk volume of the sintering particles shrinks – in the early stage, the center distance between adjacent particles decreases, in the late stage, the total pore volume shrinks. See schematic illustrations in *Fig. 6.1*.

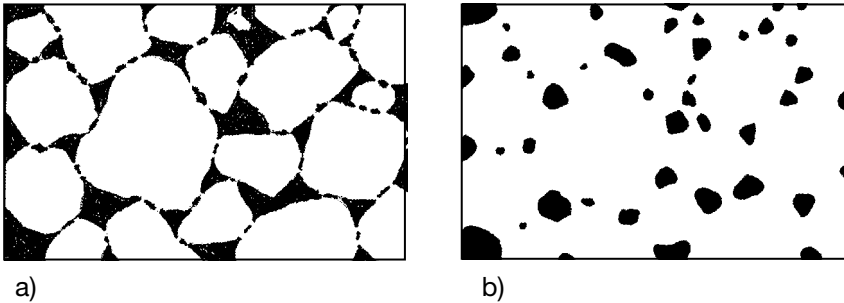


Figure 6.1. Early (a) and late (b) stage of sintering, schematically.

The driving force behind these sintering phenomena is minimization of the *free surface energy* ($\Delta G_{\text{surface}} < 0$) of the particle agglomerate (ref. chapter 1, § 1.4.1.).

Bonding between powder particles requires transport of material from their inside to points and areas where they are in contact with one another. Pore-rounding and pore shrinkage require transport of material from the dense volume to the pore surfaces, as well as from softer to sharper corners of the pore surface.

In the absence of a liquid phase, five different transport mechanisms are possible:

- volume diffusion (migration of vacancies)
- grain-boundary diffusion
- surface diffusion
- viscous or plastic flow (caused by surface tension or internal stresses)
- evaporation/condensation of atoms on surfaces

In order to find out which of these mechanisms is predominant in the sintering process, the growth of necks, formed between spherical particles during sintering, has been studied experimentally. See micrographs in *Fig. 6.2*.

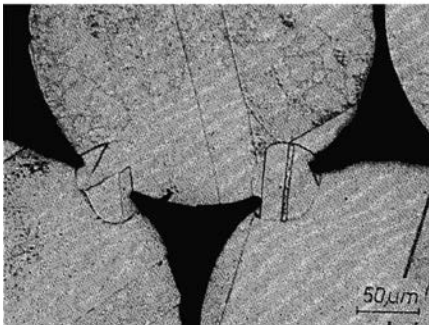
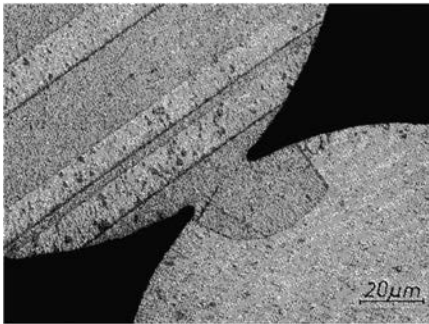


Figure 6.2. Neck formation between sintering copper spheres.

According to a theoretical model developed by C.G. Kuczynski*, the growth of these necks is governed by the following law:

$$\left(\frac{x}{a}\right)^n \sim t \quad (6.1)$$

a = particle diameter, x = neck width, t = sintering time

See schematic representation in *Fig. 6.3*. Kuczynski's model predicts: $n = 2$ for viscous or plastic flow, $n = 3$ for evaporation/condensation, $n = 5$ for volume diffusion, $n = 7$ for surface diffusion.

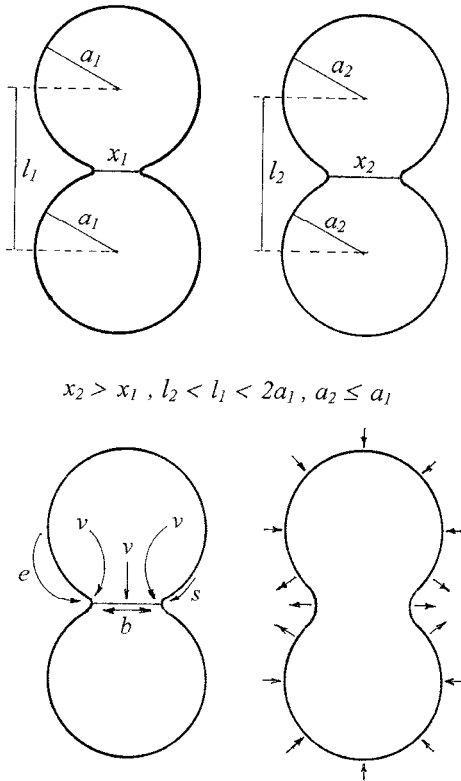


Figure 6.3. Growth of neck width between spherical particles during sintering (according to a theoretical model by C.G. Kuczynski.)

above : time law.

below : various mechanisms of material transport.

v = volume diffusion, b = grain boundary diffusion
 s = surface diffusion, e = evaporation/condensation
 $\leftarrow \downarrow$ = forces from surface tension (viscous flow)

* C.G. Kuczynski, Self-diffusion in Sintering of Metallic Particles, J. Metals 1, No. 2, pp. 169-78, (1949)

The validity of formula (6.1) is confirmed by extensive experimental material* ** * * * *. In the case of spherical metal particles, an exponent $n = 5$, and in the case of spherical glass particles, an exponent $n = 2$ was found to agree best with the experimental results. See diagrams in Fig. 6.4.

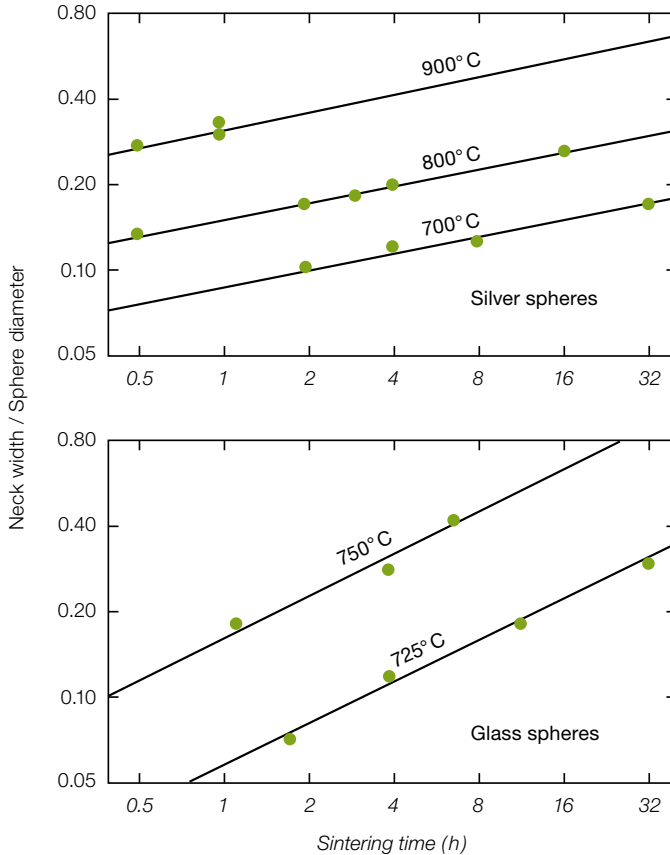


Figure 6.4. Neck growth between spherical particles, examined experimentally as functions of sintering time and temperature ; x = neck width, a = particle diameter; slope of curve (log-log scale) $1/n = 1/5$ for silver particles (top), and $1/n = 1/2$ for Na-K-Si-glass particles (bottom).

* Ya.I. Frenkel, Viscous Flow of Crystalline Bodies under Action of Surface Tension, J. Phys. (U.S.S.R.), 9, p. 385 (1945, in English).

** N. Cabrera, Sintering of Metal Particles, J. Metals, 188 Trans., p.667, (1950).

*** P. Schwed, Surface Diffusion in Sintering of Spheres on Planes, J. Metals, 3, p.245, (1951).

**** G. Bockstiegel, On the Rate of Sintering, J. Metals, 8, pp. 580-85, (1956).

***** C. Herring, Effects of Change of Scale on Sintering Phenomena, J. Appl. Phys.21,(4), pp. 301-303, (1950).

From these results, it can be concluded that, in the early stage of sintering, volume diffusion is the predominant mechanism for metal particles and viscous flow for glass particles. It is very likely but more difficult to confirm experimentally that, in the early stage of sintering, volume diffusion is predominant also in the case of non-spherical metal particles and metal powder compacts. In the late stage of sintering, volume diffusion is, no doubt, responsible for the phenomenon of pore rounding. The sketch in *Fig. 6.5a* shows schematically how vacancies migrate from the sharp corners to the flatter parts of the pore surface.

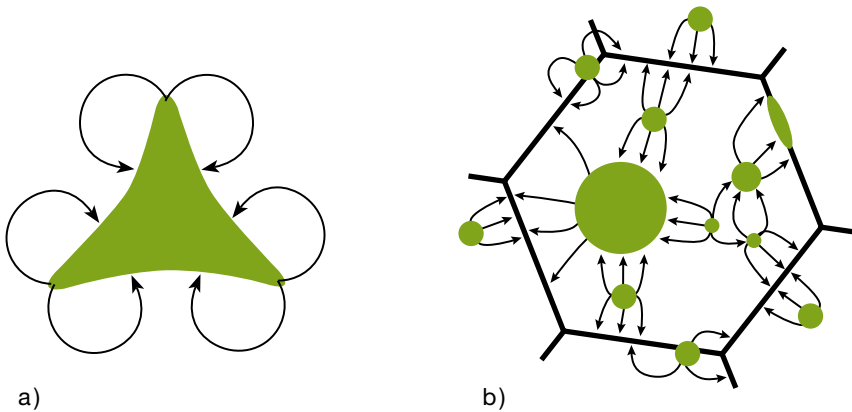


Figure 6.5. Vacancies migrating (a) from sharp corners to flatter parts of the pore surface, and (b) from smaller pores to near-by larger pores and grain boundaries (schematically).

But volume diffusion does not fully account for the observed rates of pore shrinkage and changes in the distribution of pore sizes. In fact, vacancies, emanating from the surface of a pore, do not migrate all the way to the outer surface of the sintering body. They either "condense" at the surface of nearby larger pores, or get trapped at grain boundaries where they are formed into rows or sheets which subsequently collapse owing to plastic flow. See schematic illustrations in *Fig. 6.5b*.

From the micrographs in *Fig. 6.6*, it can be seen how larger pores increase in size on account of smaller ones and how small pores disappear in the neighbourhood of grain boundaries.

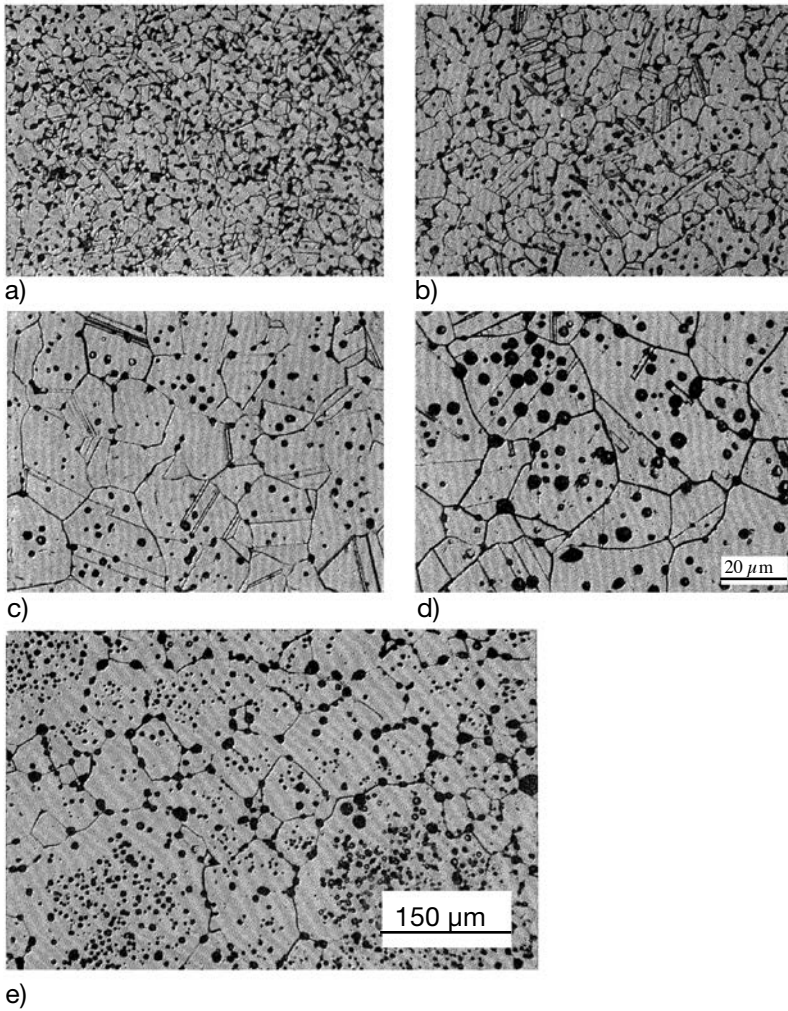


Figure 6.6. a) - e) Change of grain-size and of pore-size and -distribution in the microstructure of sintered copper powder compacts. Sintering temperature: 1000°C, sintering times:
a) 4 min, b) 8 min, c) 30 min, d) 120 min, e) pore-free zones near grain boundaries and larger pores in grain centers of sintered iron.

6.2.2 Solid State Sintering of Heterogeneous Material

When a mixture of particles of two different metals is being sintered, alloying takes place at locations where necks are formed between particles of different metallic identity. These two processes interact with one another: On the one hand, the growth rate of the neck now depends not only on the diffusion rates in the two pure metals but also on the different diffusion rates in the various alloy phases being formed in and on either side of the neck. On the other hand, the neck width controls the rate of alloy formation. The outcome of this interaction varies with the chemical identity of the two metals: it may have an accelerating, a delaying or no effect at all on the growth rate of the neck.

The schematic diagrams in *Fig. 6.7* show the relationship between phase diagram and alloy formation at the neck between two different particles.

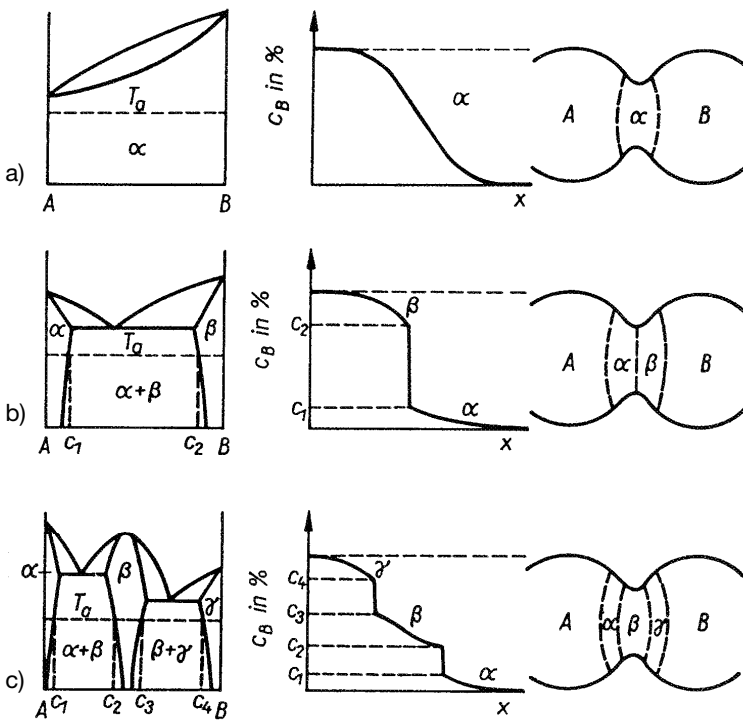


Figure 6.7. Relation between equilibrium diagrams and phase formation during sintering in the contact region between particles of different metallic identity.

In commercial iron powder mixes, the particles of alloying additions are usually much smaller than those of the base powder. While the mean size of the iron particles is approx. $100\ \mu\text{m}$, the particle size of alloying additions is usually below $20\ \mu\text{m}$ or finer.

In a compact made from such a powder mix, the distribution of alloying elements is very uneven at the beginning of the sintering process. During sintering, the alloying atoms diffuse from the surface to the center of the iron powder particles. The rate of homogenization depends on the respective diffusion coefficient which, in turn, depends on temperature. See diagram in *Fig. 6.8*.

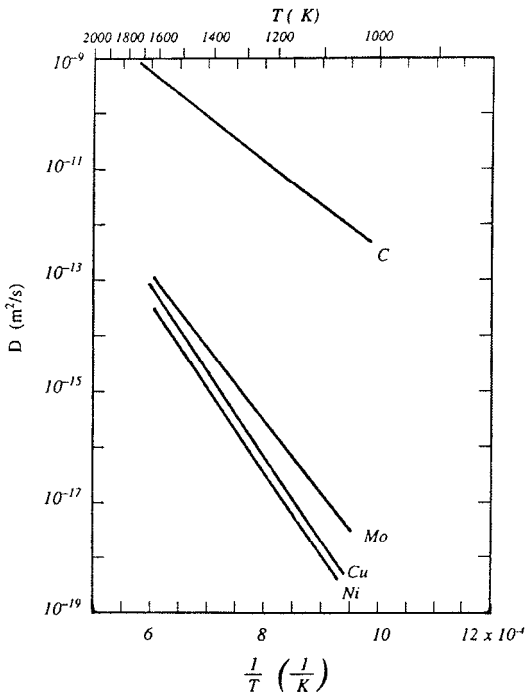


Figure. 6.8. Diffusion coefficients for carbon, molybdenum, copper and nickel as functions of absolute temperature. (log D over $1/T$).

Interstitial elements like carbon (added in the form of graphite) diffuse very rapidly in iron, while substitutional elements like nickel, copper and molybdenum diffuse much more slowly. Assuming that the alloying element consists of small spherical particles randomly dispersed in a dense iron matrix, the time t_p required to achieve a certain degree of homogenization p can be calculated from diffusion

equations as described in chapter 1, § 1.3. The homogenization time t_p is given by the following expression:

$$t_p = \frac{a^2}{4\pi D} \left[\frac{\pi C_o}{6 C_a (1-p)} \right]^{2/3} \quad (6.2)$$

a = diameter of the alloying particles, D = diffusion coefficient, C_o = initial concentration of the alloying element in the dispersed alloying particles (usually 100%), C_a = average concentration of the alloying element in the base metal, $p = C_{\min} / C_{\max}$ = degree of homogenization.

The diagram in Fig. 6.9 shows required homogenization times, calculated from (6.2), for 4% spherical nickel particles dispersed in an iron matrix at different temperatures and for different degrees of homogenization.

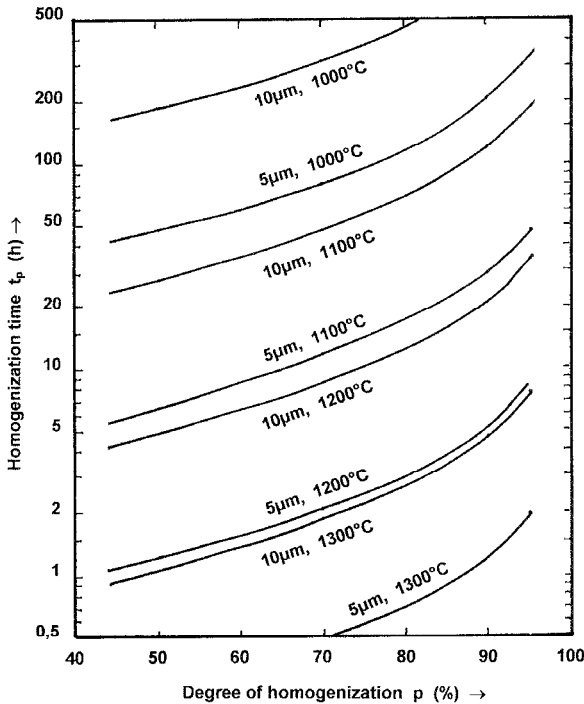


Figure 6.9. Degree of homogenization of nickel in iron as a function of time and temperature for randomly dispersed spherical pure nickel particles. Particle diameters $a = 5\mu\text{m}$ and $a = 10\mu\text{m}$, average concentration $C_a = 4\%$.

The diagram in *Fig. 6.10* shows experimentally determined degrees of homogenization of nickel and carbon in sintered compacts made from iron powder admixed with 4 wt.% nickel powder and 0.6% graphite.

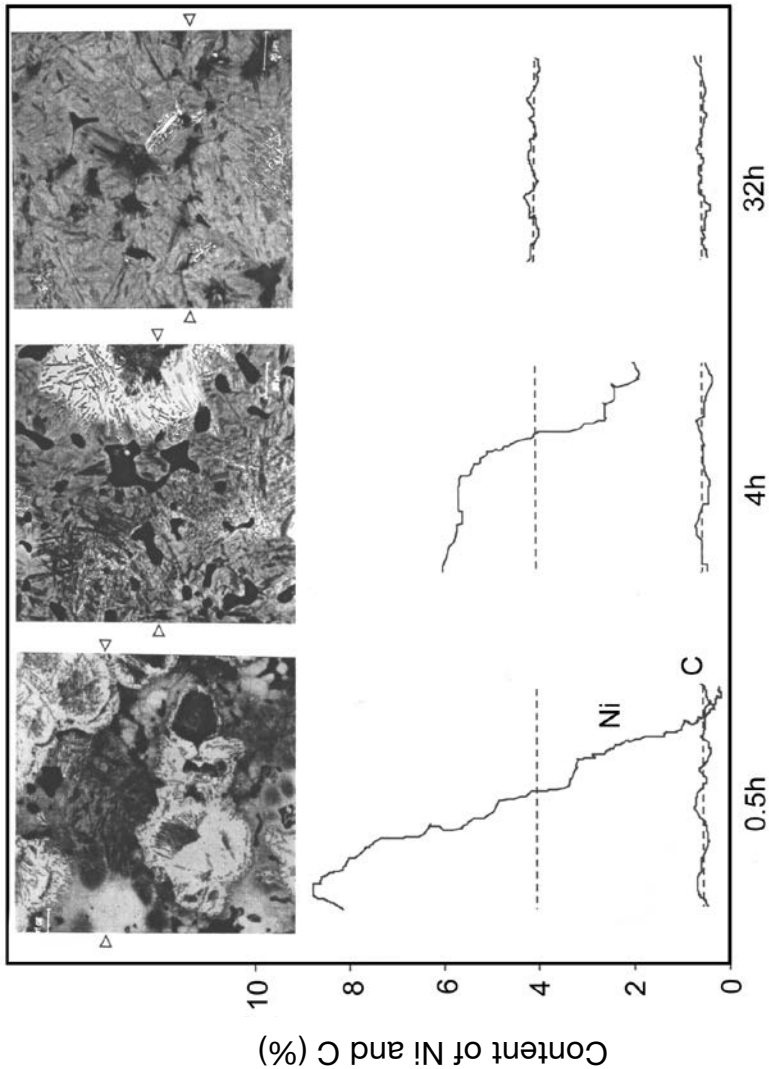


Figure 6.10. Homogenization of nickel and carbon during sintering at 1120°C in a compacted iron-4% nickel-0.6% graphite powder mix.

6.2.3 Sintering in Presence of a Transient Liquid Phase

Consider a compact made from a mixture of particles of two different metals. If one component of the mixture melts at sintering temperature, the arising liquid phase is first being pulled by capillary forces into the narrow gaps between the particles of the solid component, creating the largest possible contact area between liquid and solid phase. Then, alloying takes place and, if the initial proportion of the liquid phase is smaller than its solubility in the solid phase, the liquid phase eventually disappears. The bulk volume of the compact swells because the melting particles leave behind large pores, while the framework of solid particles increases in volume corresponding to the amount of dissolved liquid phase. See schematic illustration in *Fig. 6.11*.

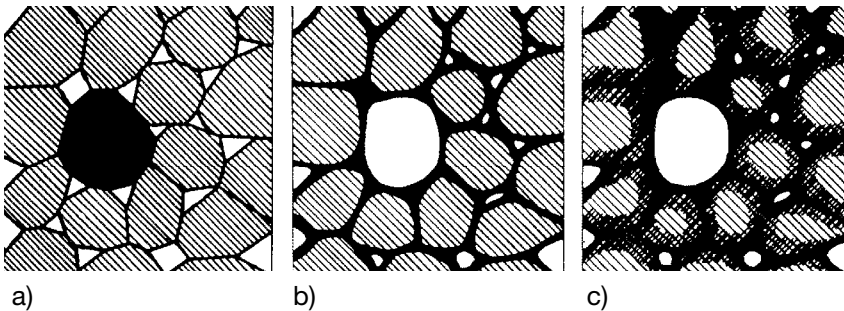


Figure 6.11. Sintering with a transient liquid phase (schematically);

- a) initial heterogeneous powder compact ,
- b) one component of the powder mix melts and infiltrates the narrow gaps between the solid particles leaving large pores behind,
- c) alloying takes place between liquid and solid phase, and the liquid phase gradually disappears again.

The micrographs shown in *Fig. 6.12* demonstrate the swelling of a compact, made from a mixture of 90 wt.% Fe-powder and 10 wt.% Cu-powder, when sintered at a temperature above the melting point of copper (1083°C). It can be seen that the liquid copper not only infiltrates the gaps between the iron powder particles but also penetrates their grain boundaries.

Liquid copper can easily penetrate the grain boundaries of solid iron because the energy stored in the new interfaces between liquid copper and solid iron is smaller than the energy stored in the initial grain boundaries (minimization of the free energy of interfaces).

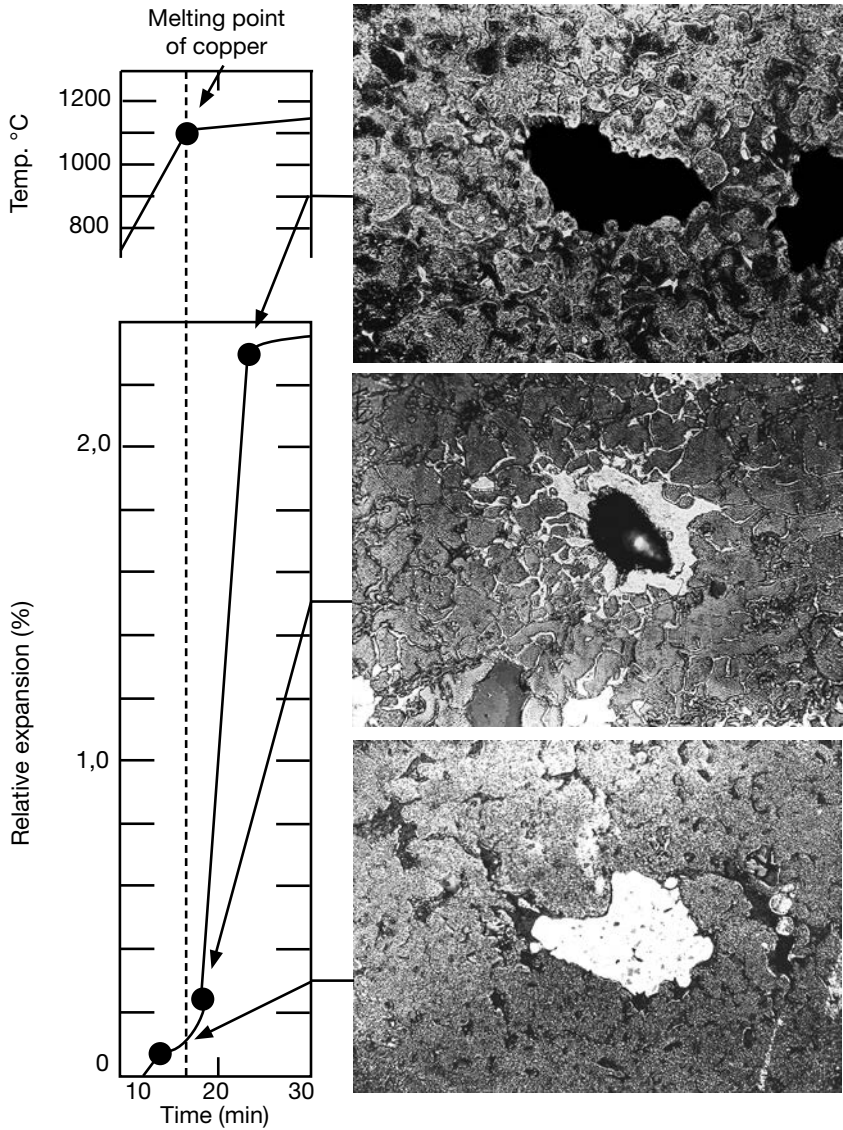


Figure 6.12. Three stages in sintering at 1150°C a compact made from a mixture of 90% iron powder (MH100.24) and 10% copper powder. Curves at the left-hand side of the micrographs show the increase of temperature and of linear expansion of the compact (corrected for shrinkage without copper)

If, in the example above, the pure iron particles are substituted with carburized iron particles having a pearlitic microstructure, the liquid copper penetrates the interfaces between ferrite and cementite lamellae. This leads eventually to a partial disintegration of the pearlitic particles.

Consequently, the initially rigid framework of solid particles collapses locally and the bulk volume of the compact shrinks. The micrograph in *Fig. 6.13* shows beginning disintegration of pearlitic iron particles under the influence of liquid copper.

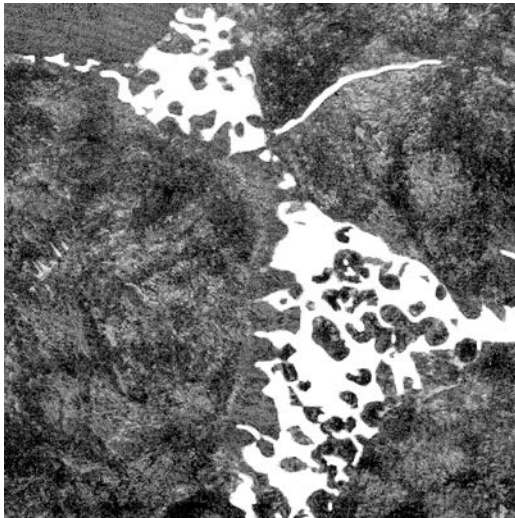


Figure 6.13. Beginning disintegration of pearlitic particles under the influence of liquid copper

These examples explain why additions of copper to iron powder mixes result in less shrinkage or produce growth during sintering of structural parts and why additions of carbon (graphite) to iron-copper powder mixes compensate the growth-producing effect of copper. (See diagrams in *Fig. 6.18* further down).

6.2.4 Activated Sintering

A special kind of sintering with a transient liquid phase is often referred to as *activated sintering*. Here, a base powder is admixed with a small amount of a metal or metal compound which, although having a melting point above sintering temperature, forms a low-melting eutectic together with the base metal. See *Fig. 6.14*.

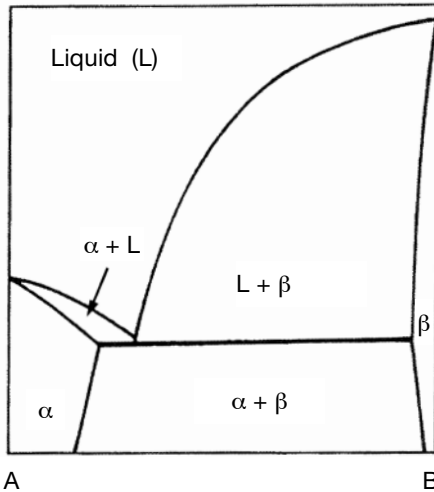


Figure 6.14. Activated sintering by creating a low melting eutectic between base metal and "activator"

The added metal or metal compound is called the *activator*. During sintering, atoms from the activator diffuse into the particles of the base metal until the latter begin to melt superficially. This superficial melting enhances the formation of necks between adjacent particles of the base metal. As the activator continues to diffuse deeper into the particles of the base metal, the liquid phase (eutectic) disappears again. Activated sintering is utilized e.g. in the manufacturing of so called *heavy metals*.

Here, an addition of only a few percent of nickel powder to tungsten powder produces a transient tungsten-rich eutectic at 1495°C which substantially accelerates the sintering process. The sintering of iron powder can be activated through small additions (e.g. 3 wt.%) of finely ground ferro-phosphorous (Fe_3P). As can be seen from the binary phase diagram shown in Fig. 6.15, Fe and Fe_3P form a eutectic at 1050°C.

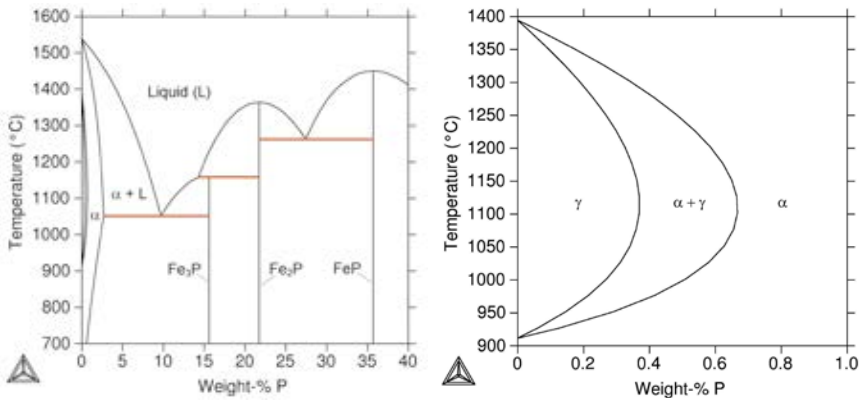


Figure 6.15. Binary phase diagrams for the Fe-P system. a) Fe and Fe₃P form an eutectic at 1050°C. b) Two-phase region ($\alpha + \gamma$) for 0.35-0.65% P at 1120°C.

During sintering at 1120°C, the phosphorous concentration at the surface of the iron powder particles temporarily exceeds 2.6 wt.%, and the particles melt superficially. But as the phosphorous diffuses deeper into the iron particles, its concentration at the surface drops below 2.6 wt.% again, and the liquid phase disappears.

Then, a second benefit of phosphorous becomes effective: Surface regions of the iron particles with phosphorous concentrations between 2.6 and 0.65 wt% have changed from austenite to ferrite. There is also a two phase region with both austenite and ferrite for P concentrations between 0.35 and 0.65 wt% at 1120°C. As will be seen in the next paragraph, the coefficient of self-diffusion (volume diffusion) for iron is approx. 300 times greater in ferrite than in austenite. Consequently, at equal temperature, sintering proceeds faster in ferrite than in austenite.

6.3 Sintering Behaviour of Iron Powder Compacts

In powder metallurgy industry, the efficiency of the sintering process is judged by the quality of the physical properties it lends to the sintered parts in relation to its processing costs. Thus, in the manufacturing of structural parts based on iron powder, a prime interest is to achieve optimal strength and dimensional stability at lowest possible sintering temperatures and shortest possible sintering times.

The following paragraphs provide some general guidelines to a better understanding of the principal relationships between sintering conditions and resulting properties. Detailed information about the sintering behaviour of a large variety of iron powders and iron powder mixes is available from Höganäs in the form of special brochures and technical reports.

6.3.1 Plain Iron Powders

The influence of sintering time and temperature on density, tensile strength and elongation of iron powder compacts (NC100.24) has been examined under laboratory conditions. Tensile test bars were compacted (in a lubricated die) from NC100.24 (without lubricant addition) to a density of 6.3 g/cm^3 .

When examining the influence of sintering time, the test bars were sintered, one by one, under dry hydrogen in a narrow furnace muffle (ID = 25 mm) at different temperatures. The test bars were heated and cooled very rapidly. As can be seen from the diagrams in *Fig. 6.16*, tensile strength and elongation increase rapidly during the first few minutes of sintering but more and more slowly as sintering continues, while the density increases only moderately over the entire range of sintering times.

When examining the influence of sintering temperature, the test bars were sintered, five at a time, for one hour under dry hydrogen in a laboratory furnace. Heating-up time approx. 10 min; cooling time to below 400°C approx. 10 min.

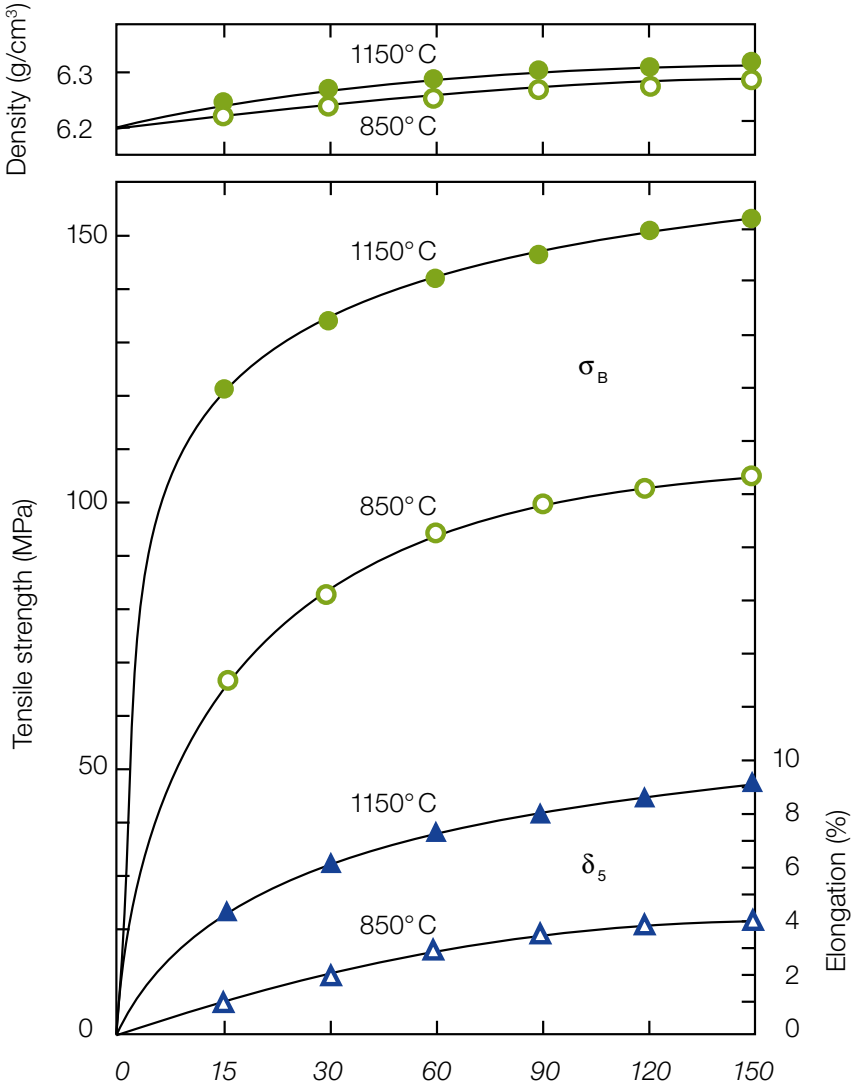


Figure 6.16. Tensile strength, elongation and density of sintered iron (MH100.24) as functions of sintering time at two different temperatures.

From the diagram in Fig. 6.17, two important features are apparent:

- Tensile strength and elongation adopt noticeable values first at sintering temperatures above 650 and 750°C respectively. From there-on, they increase almost exponentially until reaching an intermediate maximum at approx. 900°C. Just above 910°C, where the crystal structure of iron changes from ferrite to austenite, the values of tensile strength and elongation suddenly drop a little and then increase again, but more slowly than below 910°C.
- The temperature dependence of the self-diffusion coefficient of iron, drawn in the same diagram for comparison, drops dramatically as ferrite changes to austenite ($D_\gamma \approx D_\alpha/300$).

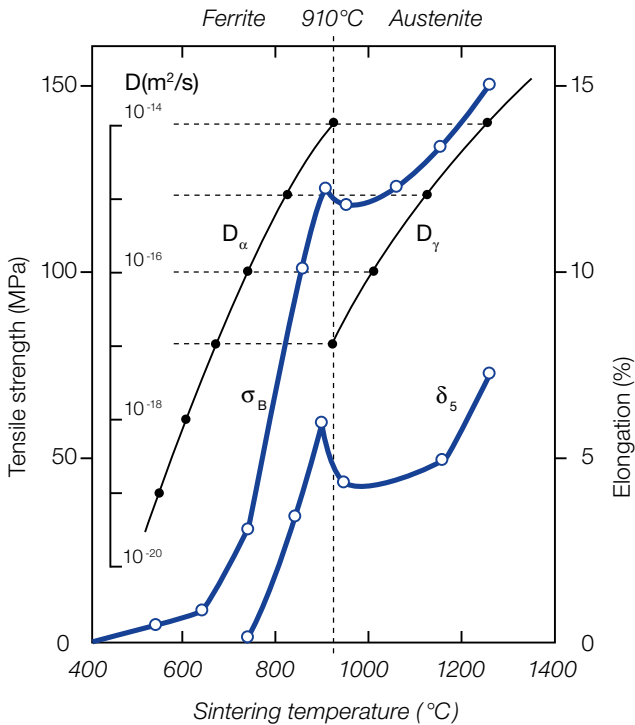


Figure 6.17. Tensile strength and elongation of sintered iron (NC100.24, density: 6.3g/ cm³, sintering: 1h in H₂), and the self-diffusion coefficient of iron as functions of sintering temperature.

The parallelism between these two features is not incidental. On the contrary, it is strong evidence of the predominant role which volume diffusion plays in the sintering process of iron. (Note: the coefficients of grain boundary diffusion and surface diffusion do not change substantially at the transition from ferrite to austenite). The effect of the drastic change of the diffusion coefficient on tensile strength and elongation is muffled by the following circumstance:

All test bars begin to sinter already during the heating-up period, while still in the ferrite state and those which are heated up to higher temperatures have already acquired a certain level of strength before they change from ferrite to austenite.

6.3.2 Iron-Copper and Iron-Copper-Carbon Powder Mixes

In order to utilize the advantage of a transient liquid phase during sintering and to achieve higher strength properties, many commercial iron powder mixes contain copper. Copper additions to iron powder can produce undesirable dimensional growth during sintering.

Graphite additions to iron-copper powder mixes counteract the dimensional growth caused by the copper (see § 6.2.3). The carburization of the iron caused by the graphite additions boosts the mechanical strength of the sintered parts.

The influence of varying additions of copper and graphite on tensile strength and dimensional changes achieved at different sintering temperatures can be seen from the diagrams in *Fig. 6.18*. Compacting and sintering procedures were the same as for the test bars of plain iron powder discussed in the preceding paragraph.

During sintering, approx. 0.2% of the added graphite was lost to the sintering atmosphere in the form of carbon monoxide (CO) and the microstructure of the carbon-containing test bars after sintering was pearlitic.

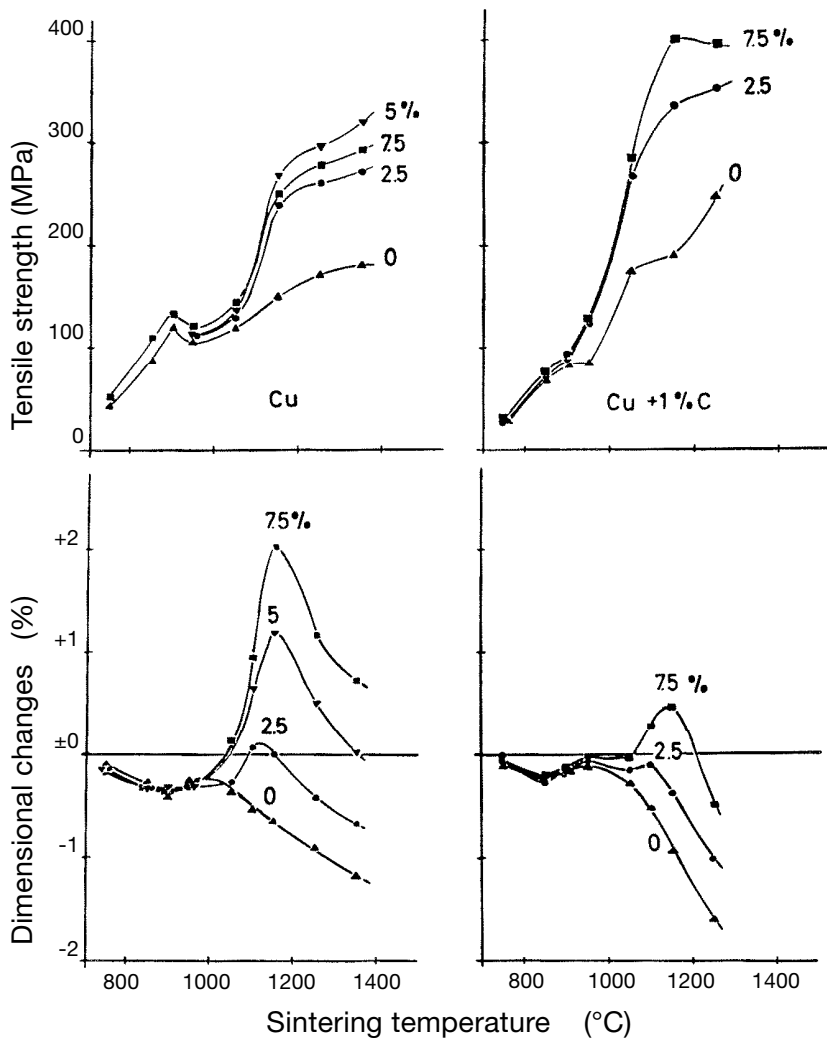


Figure 6.18. Influence of varying additions of copper and graphite and of sintering temperature on tensile strength and dimensional changes of sintered iron (NC100.24, green density: 6.3 g/cm³, sintering: 1h in H₂), at indicated temperatures.

6.4 The Sintering Atmosphere

The main purpose of sintering atmospheres is to protect the powder compacts from oxidation during sintering and to reduce residual surface oxides in order to improve the metallic contact between adjacent powder particles. A further purpose of sintering atmospheres is to protect carbon-containing compacts from decarburization.

6.4.1 General Problematic

As has been mentioned already in paragraph 6.1, mainly three different types of sintering atmospheres are common in iron powder metallurgy: *reducing-decarburizing* (e.g. hydrogen, cracked ammonia), *reducing-carburizing* (e.g. endogas) and *neutral* (e.g. nitrogen).

At a cursory glance, the choice may seem obvious: A reducing atmosphere for carbon-free materials and a non-decarburizing or neutral atmosphere for carbon-containing materials.

However, apart from economical considerations, there are some technical and thermodynamical problems which complicate both the choice and the control of the proper atmosphere:

- **Technical problems** arise in connection with the proper control of flow rates and flow directions of the atmosphere in continuous sintering furnaces. A continuous furnace of modern design, for the sintering of iron powder structural parts consists of up to five zones serving different purposes:

- 1) the so-called *burn-off zone*, where the lubricants (contained in the compacts) are burned off between 250 and 700°C ,
- 2) the *hot zone*, where the iron powder parts are sintered at 1120 - 1150°C,
- 3) the so-called *carbon restoring zone*, where superficially decarburized parts can be recarburized at 800 - 900°C, and
- 4) the so-called *rapid cooling zone*, where the iron powder parts are rapidly cooled to enable martensite transformation, and
- 5) the *cooling zone*, where the sintered parts are cooled down to approx. 250-150°C, before being exposed to air. See schematic drawing in Fig. 6.19. Ideally, each one of these zones would require its own specific combination of flow rate, flow direction and composition of atmosphere. However, ideal conditions are not achievable. To find practicable compromises and provide adequate furnace designs, is the business of the manufacturers of industrial sintering furnaces. Within the frame of this chapter, we cannot enlarge on problems of furnace design; instead, we refer to the competence and specific know-how of furnace makers.

Courtesy of Cremer Thermoprossanlagen GmbH

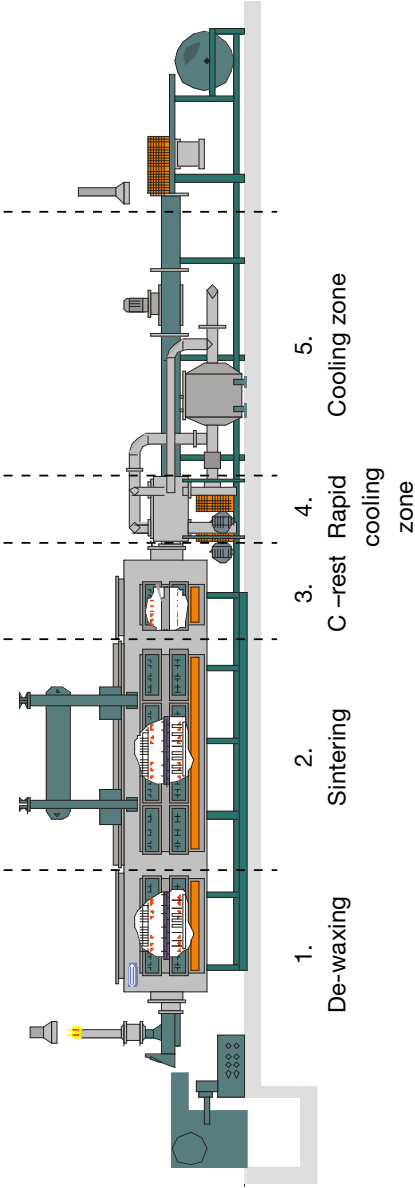


Figure 6.19. Zones of a continuous sintering furnace (schematically)

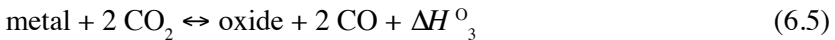
- **Thermodynamical problems** arise from the circumstance that a sintering atmosphere of given composition changes character with temperature. For instance:
the character of endogas changes with rising temperature from carburizing to decarburizing and the character of hydrogen (with traces of water vapor) changes with falling temperature from reducing to oxidizing. Furthermore, the atmosphere changes its composition while reacting with the sintered material. Reduction of residual oxides enriches the atmosphere with water vapor; decarburization of sintered material enriches the atmosphere with carbon monoxide. In the following paragraphs, we will discuss these problems in more detail.

6.4.2 Thermodynamical Aspects During Sintering

Sintering atmospheres usually contain, in varying proportions, several of the following components: N_2 , O_2 , H_2 , H_2O (vapor), C (soot), CO, CO_2 (and in some cases also CH_4 or propane). Depending on the relative proportions of these components, the atmosphere is reducing, oxidizing, carburizing, decarburizing or neutral.

Oxidation and Reduction.

Oxidation of metals or reduction of metal oxides in sintering atmospheres can proceed by either of the following three reactions :



Corresponding reactions take place between H_2 and H_2O and between CO and CO_2 :



$\Delta H^0_1, \Delta H^0_2, \Delta H^0_3, \Delta H^0_4, \Delta H^0_5$ are the amounts of heat released (per mole O_2) in the respective oxidizing reaction. The corresponding changes of free energy are:

$$\Delta G^0_1 = -\Delta H^0_1, \Delta G^0_2 = -\Delta H^0_2, \Delta G^0_3 = -\Delta H^0_3, \Delta G^0_4 = -\Delta H^0_4, \Delta G^0_5 = -\Delta H^0_5$$

The Free Energy of Oxidation.

The change of free energy (per mole O_2) ΔG^0_i during the oxidation of a metal (or other chemical element) in a gaseous medium is given by one of the following three equations, depending on the type of oxidizing agent:

if O_2 is the only oxidizing agent:

$$\Delta G^0_1 = -RT \ln \left(\frac{a_{oxide}}{a_{metal} \cdot P_{O_2}} \right) \quad (6.8)$$

if H_2O is the only oxidizing agent:

$$\Delta G^0_2 = -RT \ln \left(\frac{a_{oxide} \cdot P^2_{H_2}}{a_{metal} \cdot P^2_{H_2O}} \right) \quad (6.9)$$

if CO_2 is the only oxidizing agent:

$$\Delta G^0_3 = -RT \ln \left(\frac{a_{oxide} \cdot P^2_{CO}}{a_{metal} \cdot P^2_{CO_2}} \right) \quad (6.10)$$

R = universal gas constant. T = absolute temperature. a_{metal} , a_{oxide} = activities of the pure metal and of the oxide respectively. The activity of a pure metal or oxide is defined as being = 1 and the activity is lowered when the metal or oxide is present as a solid solute in any alloyed material. For example, the activity of Cr is lower than 1 in a stainless steel as is also the case for Sn in a Bronze material. P_{O_2} , P_{H_2O} , P_{CO_2} ... = partial pressures of the reacting components of the atmosphere.

The Ellingham-Richardson Diagram.

A standard measure for the tendency of a metal (chemical element) to oxidize is the heat released when 1 mole of gaseous O_2 at 1 atm pressure combines with the pure metal (pure element) to form oxide. The corresponding change of the free energy of the reacting system is designated by ΔG° .

The temperature has no dependence of ΔG° which follows directly from (6.8) when $P_{O_2} = 1$:

$$\Delta G^\circ = -RT \ln \left(\frac{a_{oxide}}{a_{metal}} \right) \quad (6.11)$$

A very convenient way of presenting experimentally obtained values of ΔG° for different metals is by means of *Ellingham-Richardson diagrams*. See example in Fig. 6.20.

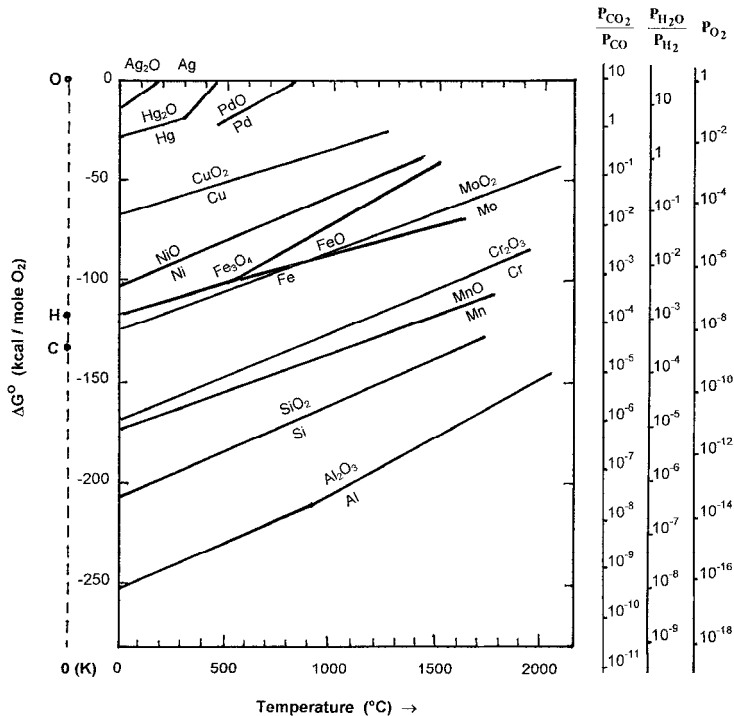


Figure 6.20. Ellingham-Richardson diagram: Change of free energy ΔG° when 1 mole of oxygen (O_2) at 1 atm pressure combines with a pure metal to form oxide.

The advantage of these diagrams is that they give the free energy released by the combination of a *fixed amount* (1 mole) of the oxidizing agent. The relative affinity of the elements to the oxidizing agent is thus shown directly. The further down in the diagram the ΔG° line of the metal is situated, the greater is its affinity to oxygen. For instance: the distance between the ΔG° lines of iron and aluminum is 537.7 kJ/mole O_2 (128.3 kcal/mole O_2), i.e. aluminum is a very strong reducing agent for iron oxide.

This circumstance is utilized e.g. in so-called *thermite welding*. Here, a proper mixture of iron oxide powder and aluminum powder is ignited to the effect that the aluminum reduces the iron oxide, and the enormous amount of released reaction heat melts the metallic iron.

Dissociation Temperature.

At the so-called *standard dissociation temperature*, the oxide is in equilibrium ($\Delta G^\circ = 0$) with the pure metal and gaseous oxygen (O_2) at 1 atm pressure. As can be seen from the Ellingham-Richardson diagram in Fig. 6.20, metal oxides can in principal be reduced to metal simply by heating them in air at this temperature.

Some values are : Au $< 0^\circ\text{C}$, Ag 185°C , Hg 430°C , Pt-group metals $800 - 1200^\circ\text{C}$, Fe $> 4000^\circ\text{C}$. Apart from the noble metals, no other metal oxides can be reduced simply by heating in an industrial furnace without the presence of some reducing agent.

Dissociation Pressure.

At any given temperature, a metal and its oxide are in equilibrium with a particular partial pressure of oxygen P_{O_2} . This pressure is called *equilibrium dissociation pressure*. Above this pressure, the metal oxidizes. Below this pressure, the oxide dissociates into metal and gaseous oxygen. This pressure is calculated as follows:

Combining equations (6.8) and (6.11) yields:

$$\Delta G^\circ_1 = \Delta G^\circ - RT \ln P_{O_2} \quad (6.12)$$

The reacting system is in equilibrium when $\Delta G^\circ_1 = 0$. Hence:

$$P_{O_2} = \exp(\Delta G^\circ / RT) \quad (6.13)$$

In the Ellingham-Richardson diagram, the dissociation pressure for a metal oxide at a given temperature T can easily be found by drawing a straight line from point "O" at the upper left corner of the diagram to the point with abscissa T on the ΔG° line of the metal in question. Extrapolating this straight line to the scale marked P_{O_2} at the right-hand side of the diagram, one can directly read the dissociation pressure. For iron oxide (FeO) at 1120°C , for instance, we find $P_{O_2} \approx 10^{-12}$ atm. See diagram in Fig. 6.21.

This tells us that simple heating of iron oxide in conventional vacuum or inert gas of conventional purity is entirely unsatisfactory. A reducing gas has to be added to the furnace atmosphere.

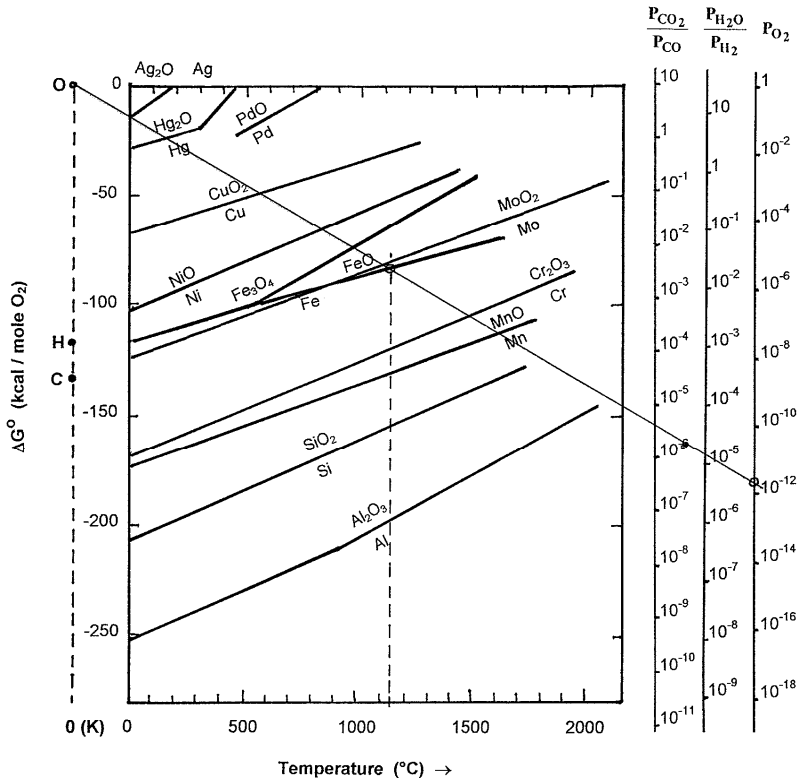


Figure 6.21. Graphical determination of the equilibrium dissociation pressure P_{O_2} for iron oxide (FeO) at 1120°C .

The Influence of Reducing Agents.

The influence of reducing agents like gaseous mixtures of H_2 and H_2O or CO and CO_2 is governed by the pertaining equilibrium point. We derive the dependence of the equilibrium point on temperature and on partial pressure ratio P_{H_2O}/P_{H_2} or P_{CO_2}/P_{CO} :

Combining equations (6.9) and (6.11) yields

$$\Delta G^o_2 = \Delta G^o - 2 RT \ln(P_{H_2O}/P_{H_2}) \quad (6.14)$$

The reacting system is in equilibrium when $\Delta G^o_2 = 0$. Hence:

$$P_{H_2O}/P_{H_2} = \exp(\Delta G^o/2 RT) \quad (6.15)$$

Combining equations (6.10) and (6.11) yields:

$$\Delta G^o_3 = \Delta G^o - 2 RT \ln(P_{CO_2}/P_{CO}) \quad (6.16)$$

The reacting system is in equilibrium when $\Delta G^o_3 = 0$. Hence:

$$P_{CO_2}/P_{CO} = \exp(\Delta G^o/2 RT) \quad (6.17)$$

At any given temperature T , a metal and its oxide are in equilibrium with a partial pressure ratio P_{H_2O}/P_{H_2} as given by (6.15) or with a ratio P_{CO_2}/P_{CO} as given by (6.17). Below this ratio, the oxide is reduced to metal. Above this ratio, the metal is oxidized.

A convenient way of finding the equilibrium temperature is by plotting the right-hand side of (6.14) or (6.16) against temperature in the Ellingham-Richardson diagram as shown in *Fig. 6.22*.

We draw a straight line from point "H" or from point "C" to the applying ratio on the P_{H_2O}/P_{H_2} scale or on the P_{CO_2}/P_{CO} scale of the diagram respectively. Where this straight line crosses the ΔG^o line is the equilibrium point. Below this temperature the metal is oxidized; above it is not.

Three examples may illustrate the method:

1. Fe does not oxidize at temperatures above approx. 550°C when the $P_{H_2O}/P_{H_2} = 25/100$ (dew point +60°C); neither do Cu, Mo and Ni.
2. Fe does not oxidize at any temperature when $P_{CO_2}/P_{CO} = 1/10$ (= 10% CO_2); neither do Cu, Mo and Ni.
3. Cr oxidizes at temperatures below 1300°C even when $P_{CO_2}/P_{CO} = 1/1000$ (= 0.1% CO_2).

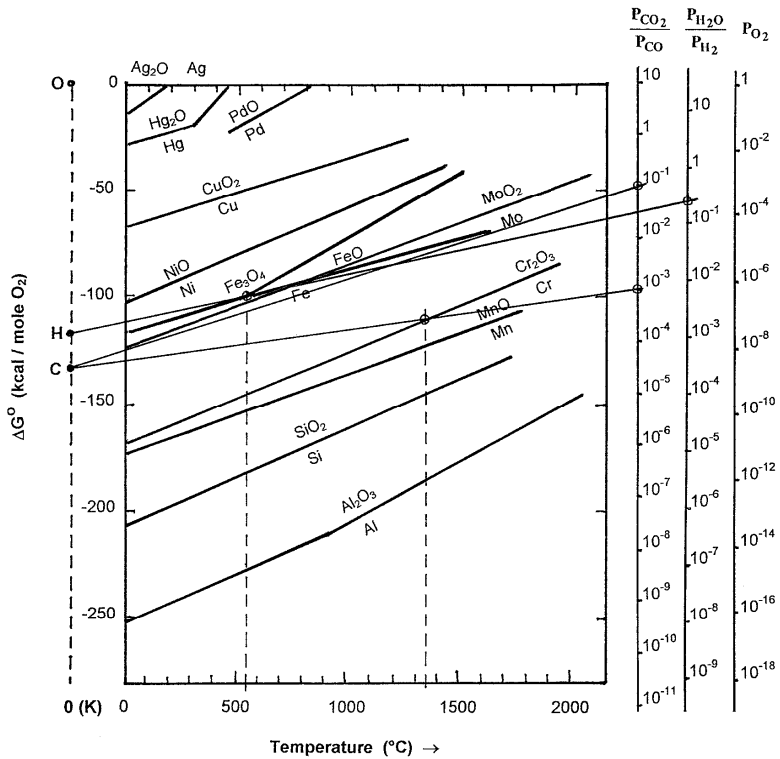


Figure 6.22. Graphical determination of equilibrium temperatures for Fe in an H_2O/H_2 - and in a CO_2/CO - atmosphere, and for Cr in a CO_2/CO - atmosphere.

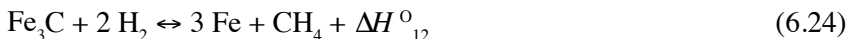
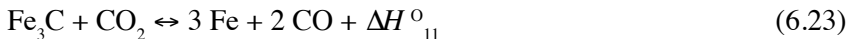
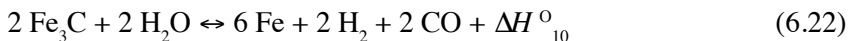
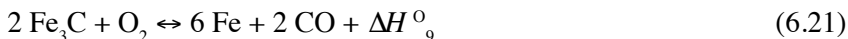
Decarburization and Carburization.

The following reactions are involved in the decarburization or carburization of carbon-containing iron powder compacts:

When carbon is present in the form of graphite:



When carbon is present in the form of cementite:



$\Delta H^0_6, \Delta H^0_7, \dots, \Delta H^0_{12}$ are the amounts of heat released (per mole O_2) in the respective decarburizing reaction.

The dependence of these reactions on temperature and partial pressure ratios of the involved gas components can, in principal, be presented by means of Ellingham-Richardson diagrams in a similar fashion as has been demonstrated.

For practical purposes, however, it is more convenient to study the influence of temperature and partial pressure ratios from a type of diagrams presented in the following paragraph.

6.4.3 Equilibrium Diagrams: Iron - Sintering Atmosphere

Ellingham-Richardson diagrams are useful for the understanding of the thermodynamical basis of chemical reactions between metals and atmospheres. However, in the particular case of iron, special phase diagrams present more conveniently the influence of temperature and gas composition upon the equilibrium between iron, iron oxides, and iron carbide (cementite).

The System: Fe - FeO - Fe₃O₄ - H₂ - H₂O.

In the diagram in Fig. 6.23, the equilibrium lines (phase boundaries) between Fe, FeO and Fe₃O₄ are drawn as function of reaction temperature and percentage of H₂O (water vapor) relative to H₂. The most important feature of this diagram is the slope of the border line that separates Fe from FeO and Fe₃O₄. It indicates that water vapor is more oxidizing at lower than at higher temperatures. This means that a fairly low content of water vapor – which is harmless at maximum temperature in the sintering furnace – might very well be oxidizing in the cooling or in the pre-heating zone. In actual fact, at temperatures below 200°C, a water vapor content of as low as 2% is still oxidizing.

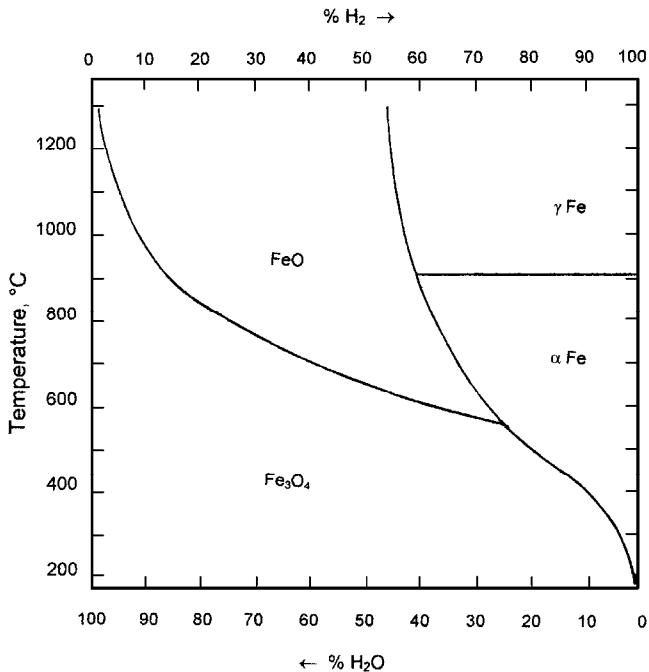
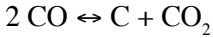


Figure 6.23. Equilibrium diagram : Fe - FeO - Fe₃O₄ - H₂ - H₂O.

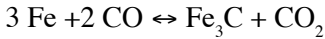
The system: Fe - FeO - Fe₃O₄ - Fe₃C - CO - CO₂.

In the diagram Fig. 6.24, the equilibrium lines (phase boundaries) between Fe, FeO and Fe₃O₄ are drawn as function of reaction temperature and percentage of CO₂ relative to CO.

Also drawn, in the same diagram, are the almost parallel equilibrium lines for the *Boudouard reaction*:



and for the *cementite reaction* :



At lower temperatures, the Boudouard reaction is generally the most prevalent and results in the deposition of soot on the sintering parts. However, at temperatures above 700 - 800°C, the carburizing reaction is dominant. Deposition of soot is suppressed by fast heating and cooling in the sintering furnace. Note that carbon monoxide is more strongly reducing at lower than at higher temperatures while, above 800°C, its carburizing action gets gradually weaker with increasing temperature.

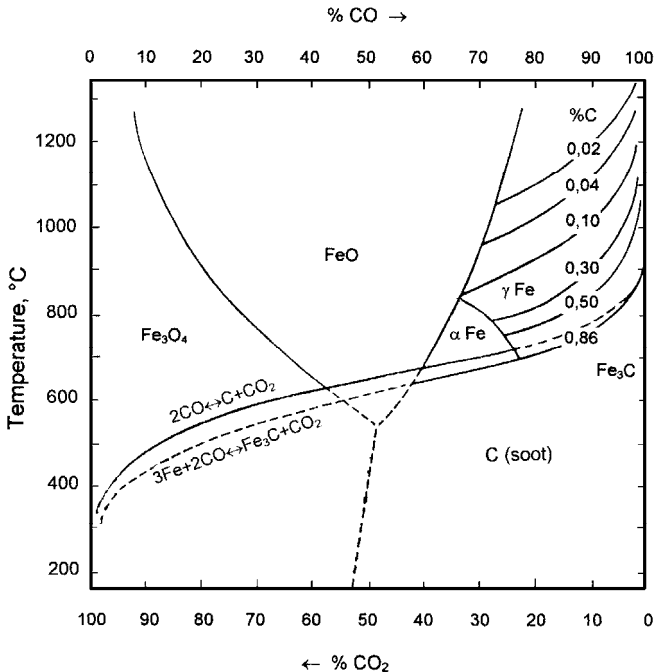
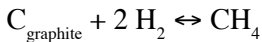


Figure 6.24. Equilibrium diagram : Fe - FeO - Fe₃O₄ - CO - CO₂.

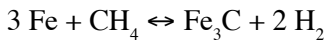
At a sintering temperature of 1120°C, a ratio of 25% CO₂ / 75% CO is strongly decarburizing but still sufficiently reducing. To maintain carburizing conditions at this temperature, the content of CO₂ in the sintering atmosphere has to be decreased to a very low value. However, with decreasing contents of CO₂, the control of the carbon content in the sintering parts gets increasingly difficult. At 1120°C, an increase of the CO₂ content from 0.1 to 0.2% can change the action of the CO/CO₂ - atmosphere from carburizing to decarburizing. This means that, in this atmosphere, a satisfactory control of the carbon content in the sintering parts is practically impossible at 1120°C.

The System: Fe - Fe₃C - C - H₂ - CH₄

When compacts of iron powder with admixed graphite are sintered in an atmosphere containing H₂, the following two reactions take place:



and



The equilibrium lines of these reactions are presented as functions of temperature and CH₄ - content in the phase diagram in Fig. 6.25.

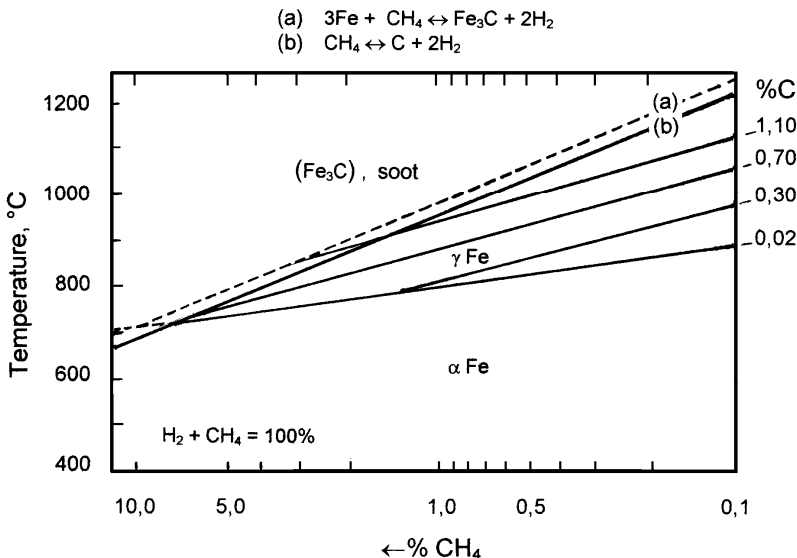


Figure 6.25. Equilibrium diagram : Fe - Fe₃C - C - CH₄.

The effect of CH_4 (methane) is different from that of CO . In contrast to carbon monoxide, methane acts increasingly reducing and carburizing with increasing temperatures. Even very small amounts of methane in the sintering atmosphere cause carburization or, above a certain temperature limit, carbon deposition.

Mixed Systems.

In mixtures of several gases (e.g. such as endogas), very complex temperature-dependent interactions take place between the various gas components. The diagram in *Fig. 6.26* shows how various gas mixtures are oxidizing, reducing, carburizing or decarburizing, depending on partial pressure ratios $P_{\text{H}_2\text{O}}/P_{\text{H}_2}$, $P_{\text{CO}_2}/P_{\text{CO}}$ and $P_{\text{CH}_4}/P_{\text{H}_2}$.

From the diagram emerges clearly that it is practically impossible to control the carbon content in the sintered parts at common sintering temperatures (1120 - 1150°C).

At these temperatures, even extremely small changes of the partial pressure ratios $P_{\text{CO}_2}/P_{\text{CO}}$ and/or $P_{\text{CH}_4}/P_{\text{H}_2}$ are sufficient to switch the gas mixture from being carburizing to being decarburizing. On the other hand, carbon control is unproblematic at temperatures around 800°C. This is a strong argument for equipping continuous sintering furnaces with a re-carburizing zone, operating at approx. 800°C, between sintering and cooling zone.

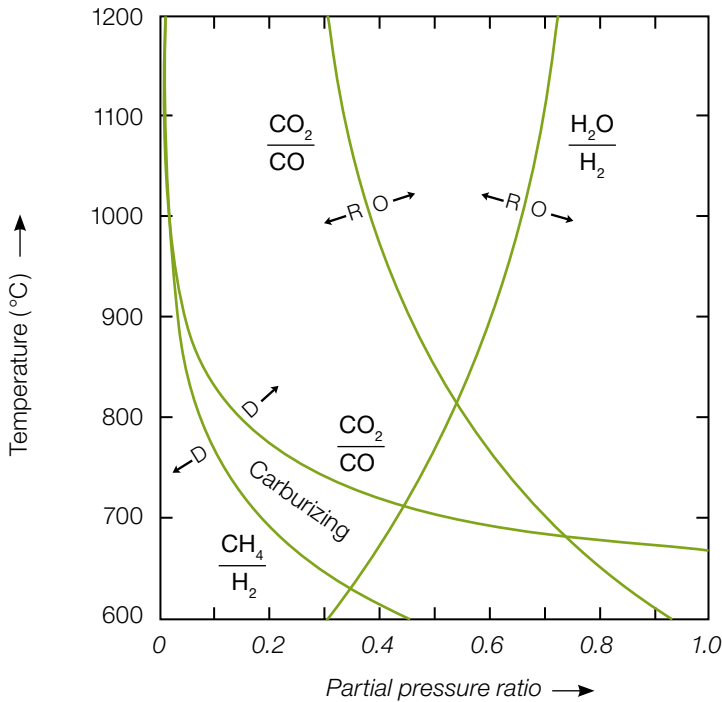


Figure 6.26. Influence of temperature and partial pressure ratios upon the character of gas mixtures. R = reducing, O = oxidizing, C = carburizing, D = decarburizing.

6.4.4 Industrial Sintering Atmospheres

Local workshop conditions, the type of material to be sintered and economic considerations govern the selection of a suitable sintering atmosphere. The correct choice is of great importance not only for the achievement of optimal product quality but also for good economy.

Hydrogen and Cracked Ammonia.

Pure hydrogen, electrolytically or cryogenically produced, is the most unproblematic atmosphere for sintering carbon-free iron powder parts. As a rule, however, it is not economical, except in combination with high priced products such as alnico magnets and stainless steel parts.

An excellent substitute for pure hydrogen is *cracked ammonia* which consists of 75% H_2 and 25% N_2 . The strong reducing action of this gas mixture is favourable in eliminating residual oxides which are present in all commercial iron powders. It is easy to handle and although it is not the most economic atmosphere, it eliminates many production problems and yields a uniform and high quality sintered product.

Because of their strong decarburizing action, neither pure hydrogen nor cracked ammonia can be used in the sintering of carbon-containing iron powder parts.

Hydrogen and cracked ammonia form explosive mixtures with air. Thus, sintering in these gases can only be conducted in furnaces equipped with a gas-tight muffle.

Endogas.

Relatively inexpensive sintering atmospheres are produced in a special generator by incomplete combustion of a mixture of fuel gas and air, using a catalyst. Common fuel gases are e.g. methane (CH_4), propane (C_3H_8), or natural gas. The combustion product contains H_2 , H_2O , CO , CO_2 , N_2 and CH_4 . Its composition varies with the air/fuel ratio and can be reducing, carburizing, decarburizing, inert, or even oxidizing.

The generated gas is called endogas when produced endo-thermically with low air/fuel ratios, and exogas when produced exo-thermically with high air/fuel ratios. See diagram in *Fig. 6.27*.

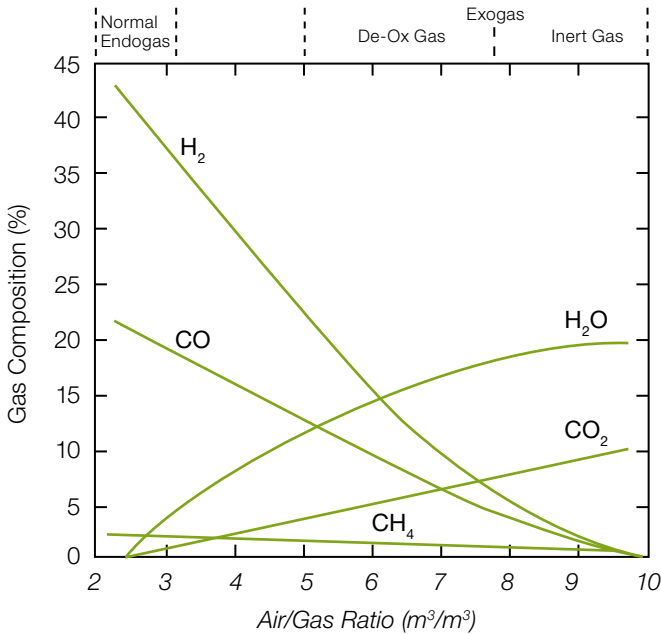


Figure 6.27. Influence of air/gas ratio on analysis of endogas and exogas assuming that the fuel is pure methane (CH_4).

In iron powder metallurgy today, the use of exogas is less common, but endogas is widely used in the sintering of carbon-containing iron parts. When leaving the generator, normal endogas may contain up to 4% water vapor (H_2O) which makes it strongly decarburizing. To make it suitable for the sintering of carbon-containing iron powder parts, it has to be dried (e.g. by means of a refrigerant cooler and a desiccant agent) to at least below 0.2% H_2 (dew point: $-10^\circ C$). The strong influence of the dew point on the carbon potential of endogas is shown in the diagram in Fig. 6.28.

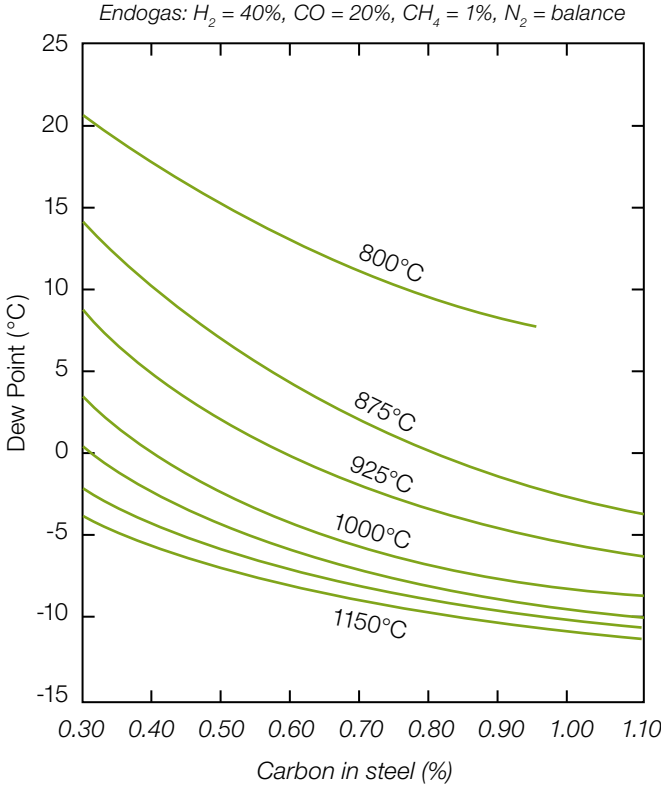


Figure. 6.28. Equilibrium of normal endogas and carbon in steel at different temperatures (dew point over carbon potential).

In endogas, very complex interactions take place between the various gas components. The temperature varies throughout the sintering cycle and the gas composition changes due to reactions with residual iron oxides, mixed-in graphite, or leaking air. This makes it very difficult to calculate, on the basis of any diagram, a suitable gas analysis for a given carbon content in the finished product. The diagrams are, however, important for the understanding of the behaviour of various gas mixtures.

Endogas is poisonous and forms explosive mixtures with air. Endogas is harmful to the heating elements of the furnace when getting into contact with them. It can cause disastrous soot deposition when leaking into the brick-work of the furnace. Thus, sintering in endogas can only be conducted in furnaces equipped with a gas-tight muffle.

Nitrogen.

Compacts made from graphite-containing iron powder mixes can very well be sintered in (cryogenic) nitrogen. The graphite present in the compacts, reacting with residual oxides in the iron powder and with leaking air, produces sufficiently reducing and carburizing conditions in the furnace. If necessary, the reducing action of this atmosphere can be controlled by bleeding-in very small amounts of wet or dry hydrogen into the hot zone of the furnace.

Correspondingly, its carburizing action can be controlled by bleeding-in very small amounts of methane into the re-carburizing zone of the furnace. Nitrogen, although being somewhat more expensive, has several advantages over endogas.

Nitrogen is neither poisonous nor does it form explosive mixtures with air. It does not react with the heating elements or any other parts of the furnace. Thus, sintering in nitrogen can be conducted in furnaces without gas-tight muffle.

Control of Sintering Atmospheres.

The composition of sintering atmospheres should preferably be monitored, not only at room temperature outside, but also at residing temperatures inside the various zones of the furnace. Interesting points where gas samples may be taken are:

- after the gas generator (or storage tank)
- inside the re-carburization zone
- at the point of maximum temperature in the furnace
- at outlet points

From the preceding paragraphs, it is evident that the two most crucial properties of a sintering atmosphere are its dew-point ($P_{\text{H}_2\text{O}}/P_{\text{H}_2}$) and its carbon potential ($P_{\text{CO}_2}/P_{\text{CO}}$ and $P_{\text{CH}_4}/P_{\text{H}_2}$).

Several dew-point meters are on the market; completely automatic or hand-operated, with or without auxiliary equipment for recording and regulating the dew-point of the atmosphere.

Among the different principles of dew-point measurement, the following three may be mentioned:

Method 1.

If a compressed gas is allowed to expand, its temperature drops and at the dew-point of the gas, water vapor (if any) precipitates as a mist.

Method 2.

The instrument is fitted with a mirror which can be cooled down to a known temperature. When the gas is allowed to pass the mirror, a film of water condenses on the mirror at the dew-point.

Method 3.

Many salts have different electrical resistivities at different moisture contents and temperatures. If the temperature is kept constant, a dew-point meter can be based on the electrical resistivity of the salt.

Modern automatic devices for monitoring and recording the amounts of carbon dioxide, carbon monoxide and methane are based on the absorption of infra-red radiation by the gas. The principle is that each of these gases absorb different wave lengths of the infra-red light and the absorption is proportional to the concentration of the gas in the mixture.

The oxygen content in the sintering atmosphere can be measured in situ by means of a ZrO_2 - cell which operates on the principle that the partial pressure of oxygen in the atmosphere is compared with that of a well defined test gas. The gas to be analyzed is in contact with one side of the cell, the test gas with the other side. The difference of the partial pressures creates an electrical potential which is monitored and can be utilized to steer automatic measures for correcting the composition of the atmosphere.

In all cases, gas samples should be collected in the flowing gas stream; they should never be collected in dead corners. To protect the instrument from dust and soot in the gas, it is often recommended to use a filter through which the gas sample is drawn.

The filter may, for instance, be made from glass wool. Gas samples must be large enough and the flow of gas through the tubes maintained for so long a time that all remaining gas from earlier tests is cleaned out.

6.4.5 Cracking of Iron Powder Compacts during Lubricant Burn-off

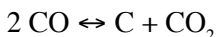
Cracked and blistered sintered iron parts are an ill-famed phenomenon which sporadically pops up and disappear again seemingly without any comprehensible cause. See photographs in *Fig. 6.29*.



Figure 6.29. Sintered iron powder compact cracked and blistered by carbon precipitation inside pores.

It has often been assumed that this harmful phenomenon is caused by a too rapidly decomposing lubricant in the burn-off zone of the sintering furnace. Thorough systematic investigations have since shown that this assumption is wrong.

It is not the decomposing lubricant that cracks the parts; it is the solid carbon which inside the pores of the parts precipitates from the carbon monoxide in the endogas, according to the Boudouard reaction.*



* A. Taskinen, M.H. Tikkanen, G. Bockstiegel, Carbon Deposition in Iron Powder Compacts during De-lubrication Processes, Höganäs PM Iron Powder Information, PM 80-8, (1980).

The rate of this reaction is highest between 500 and 700°C and is catalyzed by metallic iron, nickel and cobalt.

The diagram in *Fig. 6.30* shows the thermodynamical limits for carbon precipitation at different temperatures in different artificial gas mixtures containing varying amounts of CO, CO₂, CH₄, H₂, H₂O, O₂ and N₂. Carbon precipitation occurs only to the left of the temperature curves. It is evident that carbon precipitation occurs in all common endogas compositions (shaded area) below approx. 650°C.

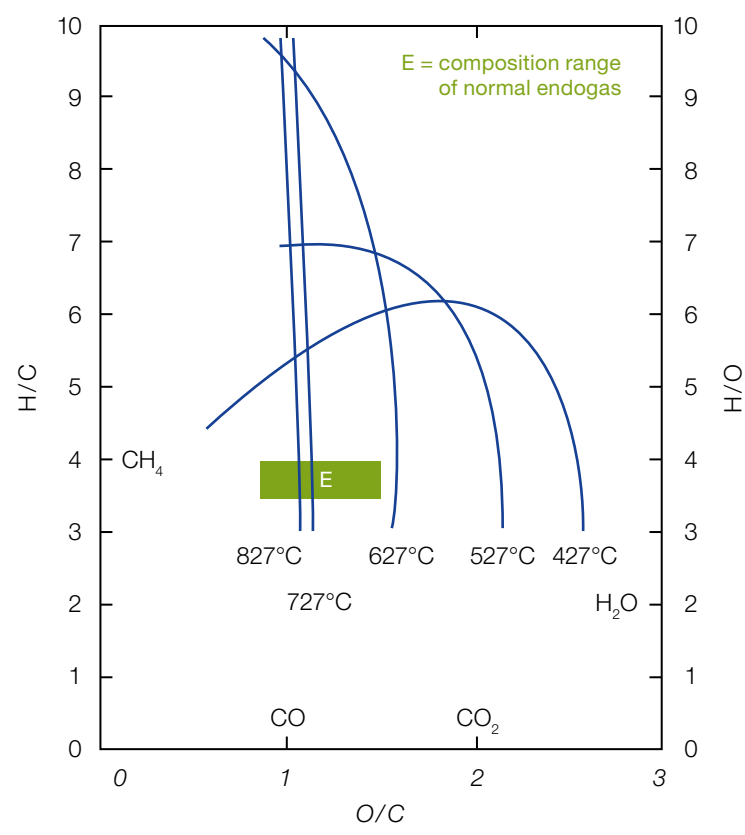


Figure 6.30. Calculated composition limits for carbon precipitation from gas mixtures containing CO, CO₂, CH₄, H₂, H₂O, O₂ and N₂.

The obvious conclusion is that carbon precipitation can be prevented or substantially reduced by heating the iron powder compacts as rapidly as possible to temperature above 650°C. Practical experience with the so-called *Rapid Burn-Off* technique (RBO) confirms this conclusion, i.e. iron powder compacts which are sintered in furnaces equipped with an efficient rapid burn-off zone do not crack or blister.

The diagram in Fig. 6.31 shows the influence of the gas composition at low heating rate (4°C/min) on carbon precipitation in iron powder compacts. By means of a thermobalance, the weight changes of the iron powder compacts were registered as a function of temperature. On the registered curves, we notice a weight loss due to escaping stearates between 250 and 400°C.

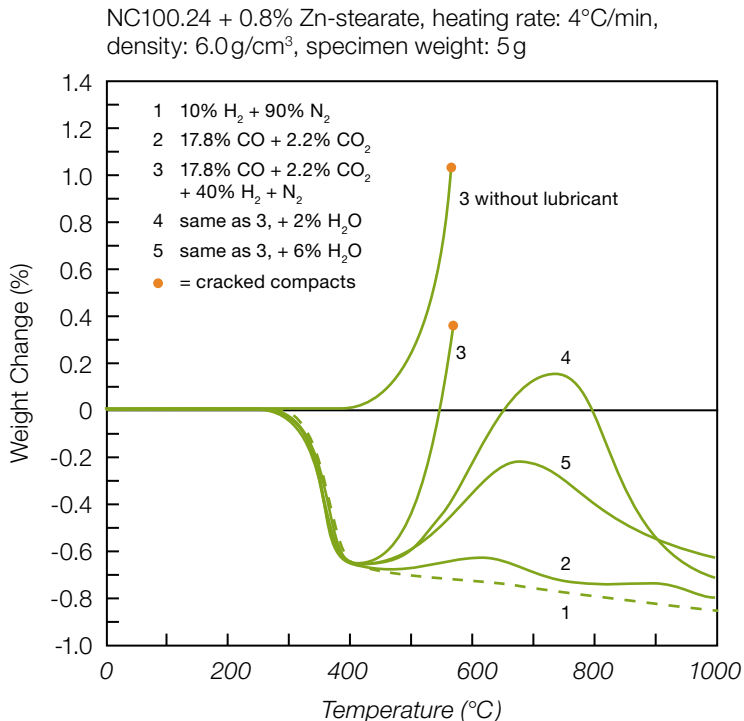


Figure 6.31. Influence of gas composition on carbon precipitation and cracking of sintered iron powder parts.

In dry endogas, the weight loss is followed by a substantial weight increase between 500 and 600°C due to carbon precipitation inside the compacts causing severe cracking and blistering. The weight increase and the blistering phenomenon is reduced by adding water vapor (H_2O) to the endogas. In a gas mixture of 10% H_2 + 90% N_2 , no weight increase and no blistering or cracking occurs. The diagram in *Fig. 6.32* shows the influence of the heating rate in dry endogas on carbon precipitation in iron powder compacts. At different heating rates, weight changes of the iron powder compacts were registered as described above. On the registered curves, we notice again a weight loss due to escaping stearamates (beginning at approx. 250°C) followed by a weight increase due to carbon precipitation inside the compacts.

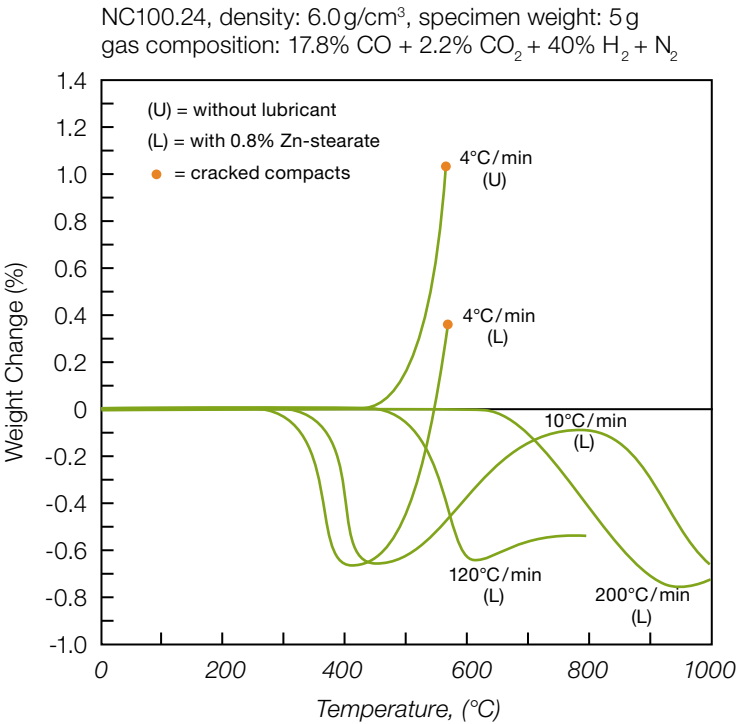


Figure 6.32. Influence of heating rate in dry endogas on carbon precipitation and cracking of sintered iron powder parts

At a heating rate of 4°C/min, this weight increase is very substantial in the temperature range between 500 and 600°C and causes severe blistering and cracking of the compacts. With increasing heating rates, the weight increase is more and more reduced, and the cracking and blistering phenomenon disappears gradually.

Based on these findings, the following practical measures to avoid cracked and blistered sintered iron powder compacts seem adequate:

1. prefer gas mixtures of nitrogen and hydrogen to endogas.
If this is not opportune,
2. use rapid burn-off technique, and/or
3. enrich endogas with water vapor in the burn-off zone.

In order to increase their density, improve their dimensional accuracy and complete their final shape, sintered parts are re-pressed, sized or coined.

Re-Pressing, Coining and Sizing



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7.1 Definitions

Re-pressing, coining and sizing are similar in so far as they all involve plastic deformation of sintered parts. The differences between them could be defined as follows:

- The purpose of re-pressing is to increase the density of pre-sintered parts (by 5 to 20%) before final sintering. The plastic deformation is substantial and the forces required for this operation are comparable to those occurring during pressing.
- Sizing is used to obtain high dimensional accuracy, thus compensating for warpage or other dimensional defects occurring in the sintering operation. Only a slight plastic deformation is necessary and the forces required for the sizing operation are normally quite moderate. An increase in density is not intended and usually $< 5\%$.
- Coining has a double purpose. Not only is dimensional accuracy improved, as in sizing, but by the use of high forces, the density of the parts is increased, as in re-pressing. Due to considerable strain-hardening occurring in the coining operation, tensile strength and hardness of the parts increase correspondingly while elongation decreases. This increase in mechanical properties is in many cases so important that soft, unalloyed sintered parts often gain sufficient strength for use under quite severe conditions.

7.2 Re-Pressing

From the diagram in Fig. 7.1 it can be seen how rapidly pressing pressure rises, relative to density, above 6.0 g/cm^3 . Final densities higher than this are often required to obtain the necessary properties. The following example illustrates the advantage of re-pressing or coining in such cases.

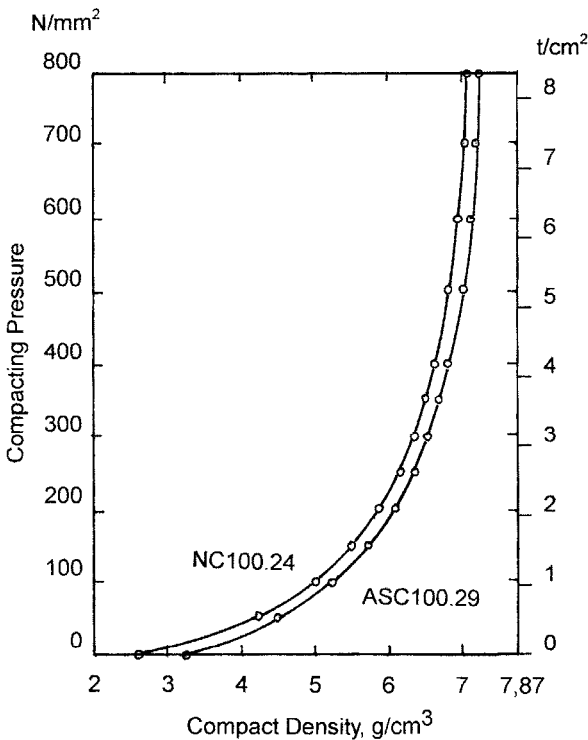


Figure 7.1 pressing pressure as a function of achieved compact density.

Pressing a pure iron powder to a density of 7.25 g/cm^3 requires a pressing pressure of 800 N/mm^2 ($= 8.16 \text{ t/cm}^2$). The same density can be achieved when pressing the powder at 490 N/mm^2 ($= 5 \text{ t/cm}^2$), sintering for 30 min at 850°C and re-pressing (or coining) at 490 N/mm^2 ($= 5 \text{ t/cm}^2$). See Fig. 7.2. The difference between 490 N/mm^2 and 800 N/mm^2 is quite substantial, considering that,

from pressing pressures of approx. 700 N/mm^2 and upwards, the tool operates at loads very near the elastic limit of the tool materials involved. This may cause the tool to wear or break at a rate making the use of such high pressing pressures uneconomical and impractical. Another reason for re-pressing is the possibility of using a short, moderate pre-sintering of alloy powder mixtures, thus preventing any considerable diffusion of the various elements in the powder mix. The purpose of this pre-sintering is partly to soft anneal the green powder compact and partly to cause a sufficient adhesion between the powder particles to allow re-pressing without damaging the compact. A sufficient soft-annealing of the green compacts could be achieved already at a temperature as low as 600°C where any graphite contained in the iron powder mix has no carburizing (i.e. hardening) effect on the compacts.

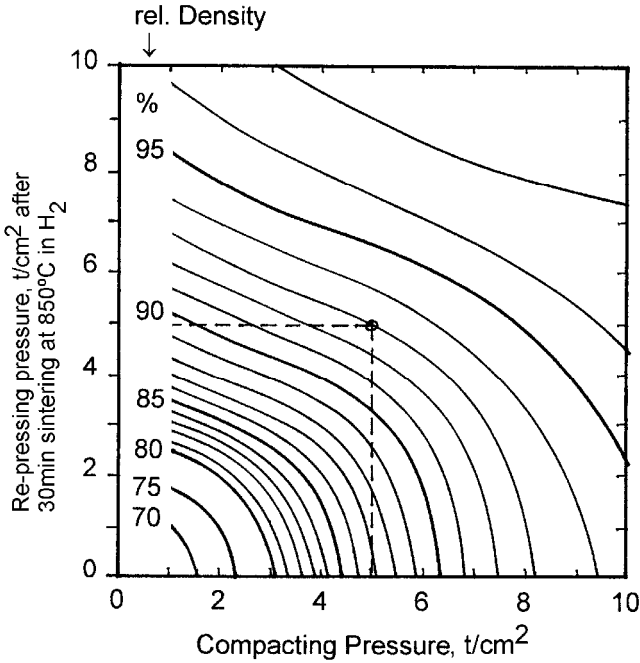


Figure 7.2 Influence of pressing and re-pressing pressure on relative compact density. Iron powder: NC100.24-type. Pre-sintering: 30 min at 850°C in H_2 .

At the following second sintering – provided temperature and time are sufficient – the diffusion of the various alloying elements can take place and proceed to such an extent that a strong, high-duty alloyed steel part is obtained. In some cases where production quantities are small and the shape of the part is simple, re-pressing (coining, sizing) can be done using the same press and tools as for pressing. For large quantities, however, it is normally preferred to perform the re-pressing (coining, sizing) in special tools. For reasons of economy, it is often of advantage to use simple mechanical presses instead of the much more expensive powder compacting presses.

7.3 General Principles of Sizing and Coining

As both sizing and coining involve elastic and plastic deformation of the part, certain guiding principles can be stated:

- The hardness of parts to be sized or coined should not exceed HV180 after sintering.
- Wherever possible, the various surfaces of the part should be sized progressively not simultaneously.
- The external forms should be sized before the holes, to prevent cracking.
- As each surface is sized, it must be held to size until all the progressive stages of sizing are completed.
- Except where only a small portion of the part is to be sized, every surface of the part must finally be in contact with and controlled by the tool.
- When coining shouldered parts, the shoulder should be supported, either on a floating die, or on a floating punch, during final compression.

As sized and coined parts are subjected to elastic and plastic deformation, the tool through which the stress is applied is also subjected to corresponding deformation loads.

The tool must be designed for maximum rigidity because, although the deformation loads may well be within the elastic limit of the tool material, the resulting expansion of the tool under load will affect the final size of the part.

Designs, particular for coining, should be as simple as possible, with the minimum number of moving parts. Dies and punches should be made as short as possible, the controlling factor being the length of the component to be processed.

Sizing and coining involve reduction or increase in the dimensions of the component and this action is performed by forcing the component into a die or over a core rod. It follows that most of the wear takes place on the die edges and on the core rod nose. Wear on the die walls and core rod sides is usually caused by friction during ejection of the component.

The actual work done in sizing and coining is divided between the swaging of the vertical faces, as the component is forced into the die, and the final

compressing of the horizontal faces. The work done in forcing the component into the die and over the core rod depends upon the density and the material of the component, the lubricant, the reduction of area and the shape and surface finish of the die or core rod.

Reduction in area is always kept to a minimum, since densification is achieved during the final compression, but distortion and size variations due to sintering must be accommodated.

The radius R of the die edge or core rod nose at the swaging point has a great effect upon the load required to force the component into the tool and upon the surface finish of the sized component.

Workshop experience tells that excessive sizing loads are avoided if the approach angle α at the swaging point S does not exceed 15° and that sizing results are best if the radius R is approx. 30 times the intended linear reduction Δx of the component ($R \approx 30\Delta x$). See Fig. 7.3a.

$$\begin{aligned}\sin \alpha &= H/R \\ \cos \alpha &= (R - \Delta x)/R \\ R &= \Delta x / (1 - \cos \alpha) \\ H &= \Delta x \sin \alpha / (1 - \cos \alpha) \\ \text{for } \alpha = 15^\circ : R &= 29.3 \Delta x \\ H &= 7.6 \Delta x\end{aligned}$$

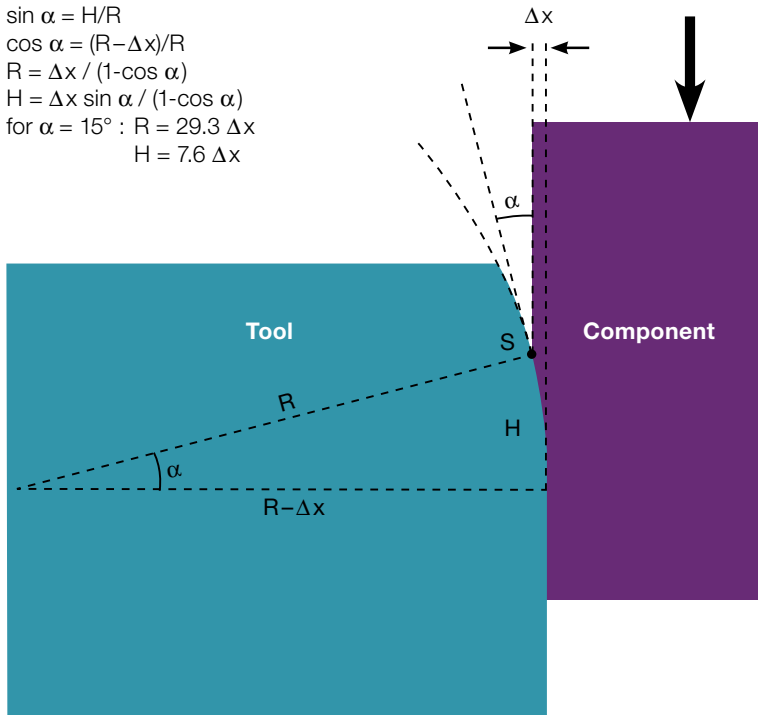


Figure 7.3 a) Computing the swaging radius R on core rod and die of the sizing tool.

For example: if the intended linear reduction of the component is $\Delta x = 75 \mu\text{m}$, the radius of the die edge should be $R \approx 2.25 \text{ mm}$. Thus during sizing, the linear reduction Δx takes place in a peripheral zone of height $H (= 0.57 \text{ mm})$ which gradually moves from the bottom to the top of the component. Where die or core rod are relieved, the shape shown in Fig. 7.3b is convenient, but if the relief dimension is important and less than indicated in the sketch, this can be modified to suit.

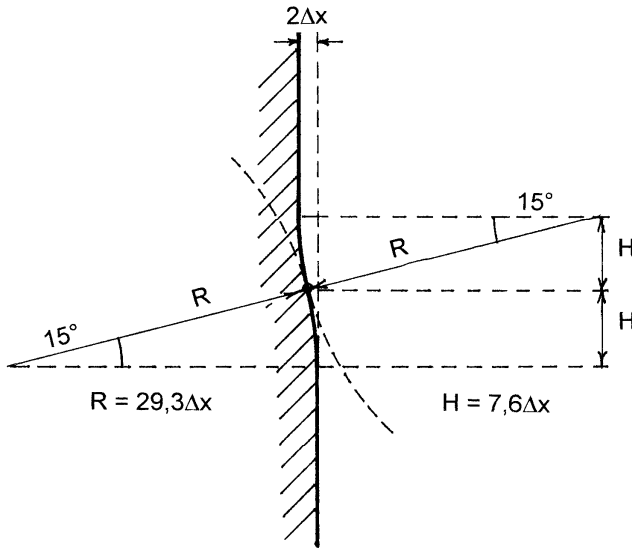


Figure 7.3 b) Suitable relief on die or core rod.

When the part has been forced to its lowest position in the die and receives the maximum compression load, the elastic and plastic deformation makes the part grip the die wall and core rod. When the load is removed, this gripping effect is reduced by the residual elastic characteristics of the material, but the plastic deformation remains.

Any faults in the surface finish of the tool now act as keys, locking the part to the tool. The ejecting punch must overcome this locking action and separate the part from the tool. The sizing or coining load required is dependent upon pressing area and final density of the part. This load must be well within the capacity of the press. As a general rule, the length of the part should not exceed 20% of the stroke of the press.

7.4 Lubrication for Sizing and Coining

An important factor in sizing or coining is the lubrication of the surfaces of the part and/or of the die. Satisfactory lubrication reduces the load required to size or coin a given part, reduces wear on the tools and improves the surface finish of the parts.

Three methods of surface lubrication are commonly used in this process:

- Surface lubrication of the parts by oil spray
- Tumbling the parts in dry lubricant
- Die lubrication

Surface Lubrication by Oil Spray.

This is done either by hand-spraying trays of parts, arranged in a single layer, or by passing the parts continuously through a series of fixed sprays. The vibrating chute which feeds the parts to the die is most satisfactory for the latter operation. The chute should be perforated to allow surplus oil to drain away into the oil reservoir. It is sometimes necessary to heat the oil reservoir to thin the oil sufficiently for easy spraying.

It must be emphasized that spraying the parts with oil must be very sparing. Otherwise, the capillary action of the interconnecting pores in the parts will draw in oil until the pores are filled. When such an oil-filled part is subjected to external pressure, the oil acts as a hydraulic cushion, supporting the metallic structure and resisting the effort of the press and tool. When the load is released, the part will tend to return to its original shape.

Special types of lubricants have been developed for the metal-forming industry, based upon oleic acid and these lubricants have proved efficient as surface lubricants for sizing metal powder components. The addition of a small amount of molybdenum disulfide to a suitable lubricating oil also produces good results, both in surface finish and in reducing the sizing load. Another method is the spraying of components with a heated solution of zinc stearate or stearic acid in oil. This solution is very suitable for the high pressures required in coining.

Tumbling in Dry Lubricant.

The parts are put into a tumbling barrel with dry zinc stearate in powder form. The tumbling action smears the zinc stearate on the surfaces of the parts. When sufficient lubricant is adhering to the parts, the barrel is emptied and the parts separated from surplus lubricant by sieving. This method is satisfactory where external faces are concerned. Holes can only be treated by the addition of special tumbling grits, of shape and size to suit the holes.

Die Lubrication.

Die lubrication has an immediate advantage in that no separate lubrication operation is necessary on the parts. By this method, the die walls and core rod are sprayed with lubricant at regular intervals, the frequency depending upon the needs of the operation. The design of this lubricating equipment is greatly dependent upon the dimensions and design of the tool.

Fig. 7.4a shows schematically the method of lubricating core rod and die. The ring surrounding the core must be large enough to permit the ram to complete its cycle without touching the ring. In each case a small metal tube is formed to a ring and on the inside of the ring are drilled small holes at a suitable angle. When oil is forced through the holes in the tube, it sprays on the core rod and die walls.

Fig. 7.4b shows a core rod attached below the die and drilled with a central hole and small radial holes so that oil is sprayed on the die walls and also inside the lower punch to lubricate the core rod. The radial holes are drilled in the relieved portion of the core rod.

Fig. 7.4c shows a method of fitting the die lubricating ring beneath the locating plate. The ring is protected from damage and does not obstruct loading the component.

Fig. 7.4d suggests a method of spraying the die walls by arranging the small holes to form a spiral. With this method, a core rod could be attached below the die without obstructing the spray.

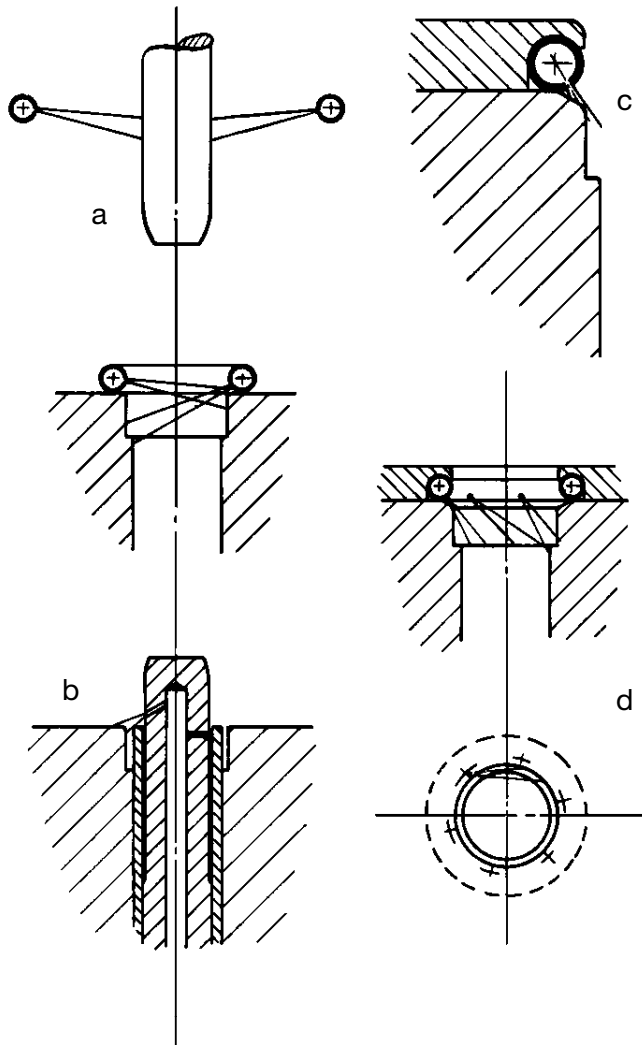


Figure 7.4 a) - d) Various arrangements for spray lubrication of die and core rod.

The pump supplying the lubricant can be worked by any convenient motion of the press and by the addition of a suitable mechanism, the pump can be arranged to work only once in several cycles as required.

The die, which lies flush with the press table, is shown fitted with a location plate d , for positioning the part over the die. In most cases, this plate can be cut away at the front for placing and removing the parts by hand. Where the part to be handled is high relative to its base, the location plate must be thick enough to hold the part upright. The sizing or coining operation proceeds as follows:

- The part rests upon the lower punch at the loading position. The lower punch is lifted by a knockout operated in sequence with the press. The knockout moves three ejection rods e which in turn lift the disc f and the lower punch.
- When the cycle begins, the lower punch and part withdraw as the upper punch descends or the part rests on the lip of the die until the upper punch forces it downwards.
- The lower punch comes to rest upon the bolster g and the part is sized by compression from the upper punch. The upper face of the component should be at least 10 mm below the die face, or below the relieved portion of the die, to allow for die wear.
- As the upper punch rises, the lower punch, after a short delay, ejects the part to the die face to complete the cycle. To accommodate a core rod, the disc f has a central hole and the bolster g has a screwed hole.

7.5.2 Plain Bushings

Problems.

The sizing of bushings presents many problems including:

- *Tolerances.* A bushing is usually assembled as a press-fit into a housing and after assembly must have a satisfactory working clearance on a spindle. As housing, bushing outer diameter, bushing inner diameter and spindle each have their own tolerance range, the final tolerances on the bushing are usually very small.
- *Density.* The bushing must act as an oil reservoir, therefore, the correct density of the bushing must be maintained in its final state.
- *Surface Finish.* The outside diameter of a bushing must have high surface finish to aid the fitting of the bushing into the housing. The finish of the inside diameter must be equally fine to reduce friction. On the other hand, if the bushing is too heavily worked on its inside diameter, the surface pores are closed and the capillary action of the oil reservoir is reduced.

- *Chamfers.* The external chamfers on a bushing are helpful in guiding the bushing into the housing. And the internal chamfers assist assembling of the spindle. Sharp edges on either external or internal diameters must be avoided if the bushing is to operate satisfactorily. Even where chamfers on sized diameters are not requested, a small chamfer on the sintered part assists in sizing. The action of sizing tends to form a slight burr at the end of the sized diameter and this tendency is reduced if the diameter ends in a chamfer.
- *Proportions.* The ratio of length to wall thickness of any bushing is usually high, to economize in material and space. This high ratio adds difficulties in sizing as the greater density variations in a thin-walled bushing increases size variations in sintering. These size variations, which may take the form of a swelling in diameter either at the ends or near the middle of the bushing, must be eliminated in sizing. The result is an attempt to overwork the swelled section or sections and the greater punch pressure required for this tends to overdensify the bushing and shorten its length. In extreme cases, the bushing might even collapse while entering the die. Careful control of density in pressing and of sintering conditions, is necessary for long thin bushings. Lubrication during sizing can greatly affect the results.
- *Eccentricity.* Obviously, the bushing is required with the least possible eccentricity. This problem cannot properly be dealt with at the sizing stage. Unless the bushing is compacted with minimum eccentricity, the fault cannot be corrected in sizing.

All problems outlined above have been overcome as a result of experience and we indicate below some of the ways in which bushings can be satisfactorily sized.

Simple Concepts.

Fig. 7.6a shows the simplest tooling for sizing bushings. As the length of a bushing is sometimes not held to close tolerances, only the diameters are sized in this tool. The action of sizing tends to lengthen the bushing if the wall thickness is reduced, but friction between tool and bushing can often more or less cancel out this tendency and the result is a slight increase in density of the part. In the design shown, top punch and core rod operate as one piece.

The sintered size of the bushing is such that the core rod can pass through the bore without pulling the bushing into the die. The top punch then pushes the bushing into the die, closing it on the core rod. The bushing is traversed down

the full length of the die and on emerging below the die, the bushing expands slightly, due to its elastic properties, and loosens its grip on the core rod.

As the core rod and punch return upwards, the bushing is held by the sharp edge of the die aperture and drops away into a container or chute. This type of sizing action requires only a plain crankshaft press without knockout or any other equipment.

Fig. 7.6b shows the design of tooling in which the part is sized on diameters and end faces. In this case, a separate core rod is rigidly attached below the die and is surrounded by the bottom punch. The part is forced into the die by the top punch, passing over the relieved end of the core rod.

As it travels further down the die, the bushing is forced over the thicker portion of the core rod, until it is finally sized between upper and lower punches. The top punch is then withdrawn and the part ejected to the die-face by the bottom punch. This tooling requires a plain crankshaft press with an adjustable knockout below the die table for the bottom punch motion.

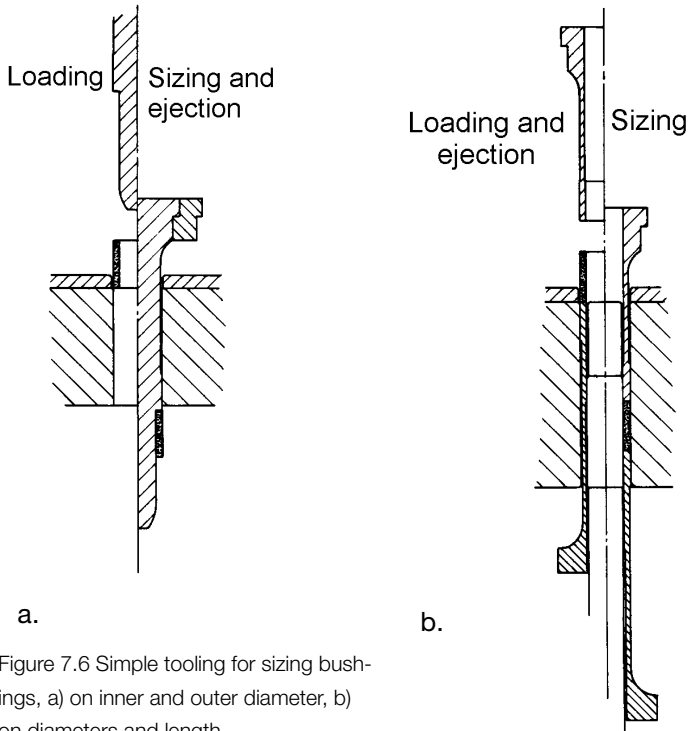


Figure 7.6 Simple tooling for sizing bushings, a) on inner and outer diameter, b) on diameters and length.

Advanced Concept.

A further stage in the development of progressive sizing is shown in *Fig. 7.7*. A double-action crankshaft press with a cam-operated blank holder is required for this cycle.

The core rod in this design is controlled by the crankshaft of the press and moves independently of the top punch. The top punch is attached to the blank holder.

As in the simple design shown in *Fig. 7.6a*, the core rod passes through the bushing before the part is forced into the die by the top punch. As the part reaches the bottom punch, the faces of the bushing are sized. The core rod is then withdrawn, followed by the top punch and the part is ejected to the die-face by the bottom punch.

If the cams operating the blank holder are properly designed, the core rod and top punch will travel at equal speed so that during the downward motion of the bushing the core rod does not move relative to the bushing.

The only wear on the core rod therefore is during its extraction from the bushing. It is preferable in such design that the knockout which operates the bottom punch should be mechanical and not dependent upon the return springs which are normally used in lifting the blank holder on the upward stroke.

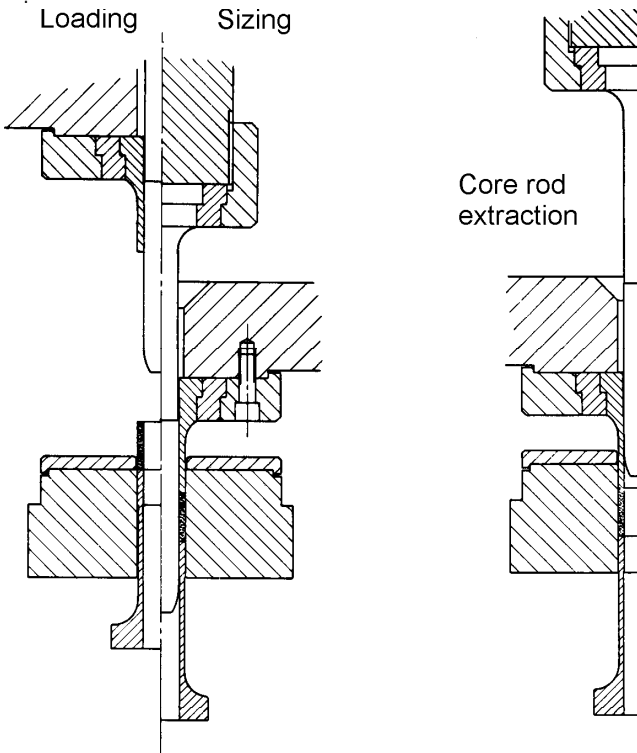


Figure 7.7 Sizing bushings in a double-action press

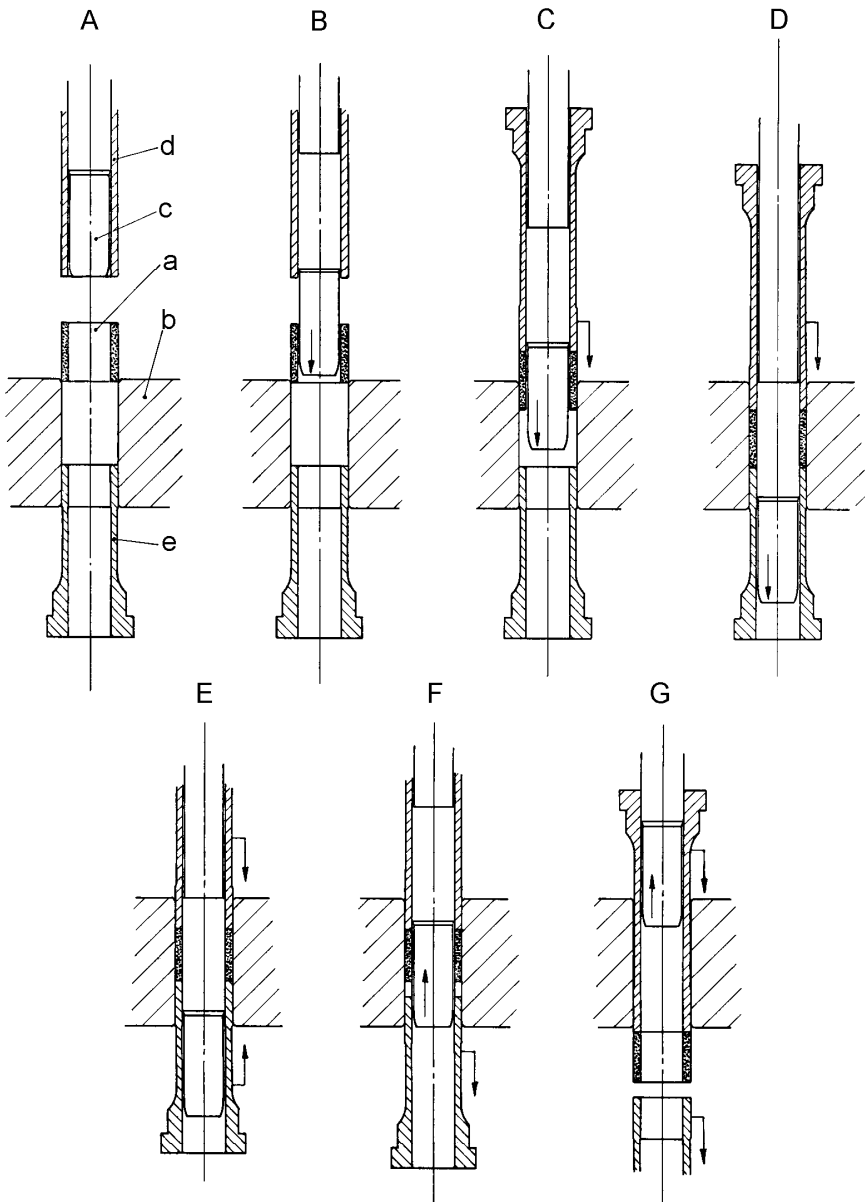


Figure 7.8 Auto-cycle press for the sizing of bushings.

Fig. 7.8 shows the operation cycle of a cam operated press specially designed and built for the sizing of bushings.

The various steps involved in the sizing operation can be commented as follows:

- A)** A special "catcher" brings the bushing *a* in place, just above the slightly tapered entrance of the die *b*.
- B)** The core rod *c* enters into the bore of the bushing. Its lower end has a somewhat smaller diameter (about 0.10 to 0.25 mm) than its upper part. When the core rod enters into the bushing, ovalness caused by warping during sintering is adjusted sufficiently to permit the bushing to enter into the die.
- C)** The bushing is forced into the die by the upper punch *d*. The velocity of the upper punch at this moment is about equal to that of the core rod, so that the bushing surrounds the smaller part of the core rod during its entrance into the die.
- D)** When the die has been completely closed by the upper punch, the core rod continues its movement so that its upper larger part completely traverses the bore of the bushing.
- E)** When the bushing thus has been sized by the core rod, the lower punch *e* and the upper punch move towards each other until the bushing has been squeezed to its exact height.
- F)** The lower punch moves downwards and the core rod upwards.
- G)** The bushing is then ejected to the underside of the die by the upper punch and deflected clear of the lower punch by an air jet.

After steps have been completed, the cycle is repeated with the next bushing.

Mechanical feeding and removal of bushings is essential where large scale high speed production is demanded. The operation cycle shown in *Fig. 7.8* simplifies the automatic feeding of bushings, as the sized bushing is not returned to the die face.

The easiest way of feeding plain bushings is by rolling them down on a chute. To take advantage of both these ideas, sizing of bushings is sometimes done in a horizontal press. The bushings lie on their sides in a sloping chute and the next bushing to be fed actually touches the side of the "upper" punch. Withdrawal of the "upper" punch permits this bushing to move into position for sizing and it is ejected on the other side of the die. Both feeding and clearance of the bushing after ejection are thus assisted by gravity.

Serrated Core Rod.

As nearly all the work of sizing the bore of a component is done by the nose radius, one method of easing the load at this point is the use of a serrated or stepped core rod.

Fig. 7.9 shows a detail of such a core rod which is designed rather like a broach but with the cutting edges replaced by the sizing radius.

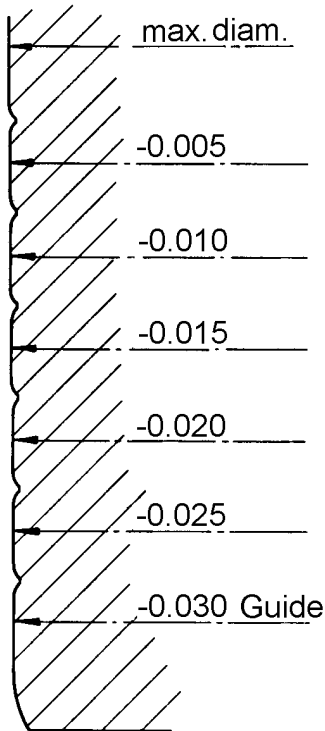


Figure 7.9 Serrated core rod.

The effect of this design is to spread the work over several stages, but of course, a long bushing will either require the serrations set very far apart, or more than one sizing radius will be inside the bushing, with an increase in the sizing load. The controlling factor here is the press stroke available, but even if two or three of the serrations are within the bushing length, the sizing action is easier.

Core Rod with a Bulge.

Fig. 7.10 shows another approach to the sizing of bores. The operating cycle can be commented as follows:

- A)** The bushing lies at the entrance of the die and is supported by a spring-loaded lower punch.
- B)** The relieved end of the core rod passes through the bushing and the upper punch forces the bushing into the die. At this point, the bushing is compressed to its final length. The core rod end is now guided in the lower punch.
- C)** The core rod has a very short bulge which does the actual sizing. This bulge is now forced through the bushing to size the bore.
- D)** The core rod moves upwards, re-sizing the bore while still guided in the lower punch.
- E)** The upper punch withdraws and the bushing is ejected by the lower punch.

The important points in this design are:

- The outside diameter and the length of the bushing are fully sized before the bore.
- The core rod is guided in the lower punch. An unguided core rod tends to wander, particularly when sizing long bushings. The guiding of the core rod end in the lower punch prevents this.
- The sizing is done by a short bulge on the core rod. The usual rule in sizing is that the working part of the core rod should be longer than the bushing to ensure a straight hole and control all the bore surface. By this alternative method, straightness is achieved by guiding the core rod end and the sizing bulge is passed right through the bore. This action requires less load than the normal core rod, but as the sizing bulge passes, the bore will tend to close slightly.
- As the core rod is withdrawn, the sizing action is repeated in an upward direction. This second sizing does less work than the downward sizing and gives a fine finish to the bore of the bushing.

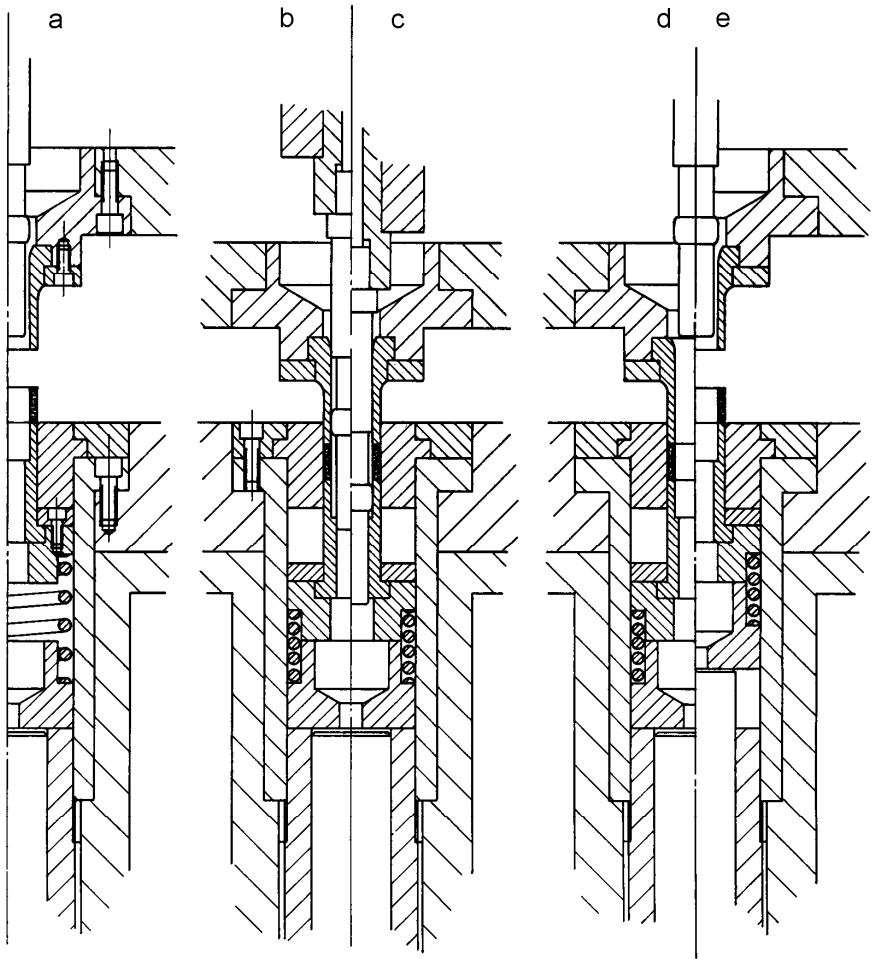


Figure 7.10 "Button" sizing bushings on a double-action press. ("button" refers to a short bulging portion on the core rod).

Sizing by Balls.

In some cases, bore tolerances after assembly are required to such a close limit that a final sizing operation is necessary after assembly of the bushing. This operation is usually done by forcing a hardened steel ball of suitable size through the bushing.

Consistently close tolerances as small as $5\text{ }\mu\text{m}$ to $7\text{ }\mu\text{m}$ are claimed for this method if the limitations of the process as given below are understood and accepted:

- The normal sizing operation on the bushing must be done to the closest practical tolerance.
- The bushing, after assembly, must leave the absolute minimum for correction by ball sizing. The aim should be to have the upper limit of the assembled bushings falling within the required final tolerance and only the variation in bushing diameter after assembly should fall below the required lower limit. *Fig. 7.11* shows this schematically.
- The bushing must not project from the housing and the housing must be rigid enough to give adequate support to the bushing during the operation.

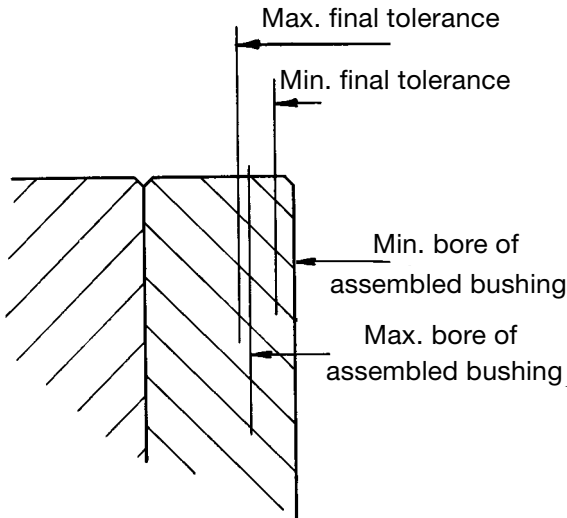


Figure 7.11 Tolerance diagram for ball-sizing bushings after assembly.

The use of a ball for sizing a bushing has certain advantages and limitations. The spherical form offers an infinite number of new faces to the bore and therefore, wears very little and gives consistent results. Standard steel balls can be reduced to any required size by immersion in a suitable acid solution. Replacement of the balls is much less expensive than replacement of a worn core rod.

On the other hand, a ball can only follow the path of least resistance whereas a cylindrical core rod tends to make a straight hole. For this reason, the increase in bore diameter cannot be more than $10\text{ }\mu\text{m}$ to $20\text{ }\mu\text{m}$ and the process is generally limited to short holes.

As shown in *Fig. 7.12*, the equipment for ball sizing can be very simple consisting of a hand press, a location plate for the housing, an undersize core rod with the end ground flat and a supply of balls. The core rod is attached to the press ram, the housing located by hand and a ball placed in the mouth of the bushing. The ram is brought down and forces the ball through the bushing.

The simplicity of the operation often leads to its use in other ways, e.g. in the correcting of short thick components which have been rejected after sizing for undersize bores, due perhaps to a worn core rod. On the other hand, where ball sizing is required as a necessary operation for large quantity production, semi-automatic equipment can be designed to perform the operation at a high rate.

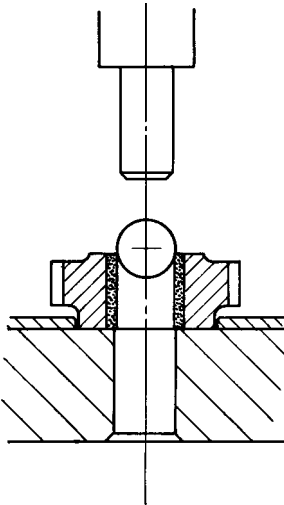


Figure 7.12 Simple ball-sizing for assembled bushings.

Fig. 7.13a shows a design for use with a normal crankshaft press fitted with a knockout. A rotary feed table brings the components into position below the core rod. The balls are arranged to re-circulate, being lifted up in a tube by the knockout after each operation so that the top ball rolls down into a spring clip below the core rod ready for the next operation.

In the alternative design shown in *Fig. 7.13b* the balls are forced upward through the component which is lifted up slightly to rest below a seating above the rotary feed table. The balls re-circulate by gravity. The ram could be operated either mechanically or hydraulically. This procedure is well suited for use on a multiple station machine which presses the bushings in place, the ball sizes the assembly and performs other operations.

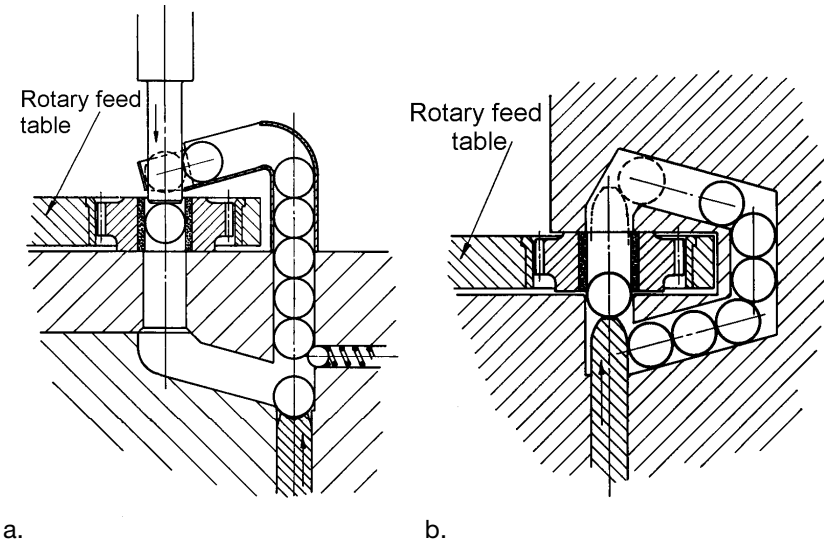


Figure 7.13 Automatic ball-sizing, a) balls being fed and pushed from above, b) balls being fed and pushed from below.

Fitting of Bushings.

Earlier in this chapter, we mentioned that tolerances on bushings were dependent upon the tolerances of the housing into which they were fitted. Bushings are always located on a shouldered mandrel when being assembled into a housing. As the shoulder forces the bushing into the housing, the mandrel helps to control the final size of the bore of the bushing. The size of the mandrel is dependent upon many factors including the bore of the bushing, wall thickness and interference with the housing.

Manufacturers of standard ranges of bushings usually specify correct mandrel sizes for each bushing. As a general guide, the mandrel is made 0.02% to 0.04% larger than the minimum tolerance of the bore.

As the bushing is pressed into the housing, the bushing bore contracts upon the mandrel. After assembly, the mandrel can be withdrawn without difficulty. This method of assembling bushings prevents the tendency to wrinkling which results from the reduction in the outside diameter during assembly.

Spherical Bushings.

The sizing operation on a spherical bushing has some peculiarities which are worth examination.

- A spherical bushing must have a bore with good surface finish and narrow tolerance.
- The spherical diameter must be held within close limits and as the two spherical surfaces must obviously be sized by opposed parts of the tooling, this means in practice a close tolerance on the height of the part.
- The bore of a spherical bushing after sintering tends to vary due to the changing wall thickness.
- The spherical form of the bushing is naturally highly resistant to the sizing action, as a spherical form has the greatest resistance to pressure exerted evenly over its whole surface.
- In addition to sizing the bore and spherical form, the small flats left in pressing must be forced within the spherical form.

A simple tool for sizing spherical bushings is shown in *Fig. 7.14*. The bushing is located over the relieved end of a fixed core rod and rests upon the lower punch. The upper punch descends, pushing the spherical bushing into the die, then over the full diameter of the core rod until finally the spherical form is sized between the upper punch and the spherical portion of the die.

After the upper punch has been raised, the lower punch ejects the component to the die face. One fault in such design is that whereas the spherical form in the die blends smoothly with the cylindrical outer diameter, the spherical form in the upper punch cannot blend smoothly due to the sharp edge on the punch.

It is therefore necessary in such a tool to double-size the bushing, inverting it after the first cycle, in order that both shoulders formed by the edges of the pressing punches should be properly re-formed. For this reason, such a tool design is only useful for small quantities.

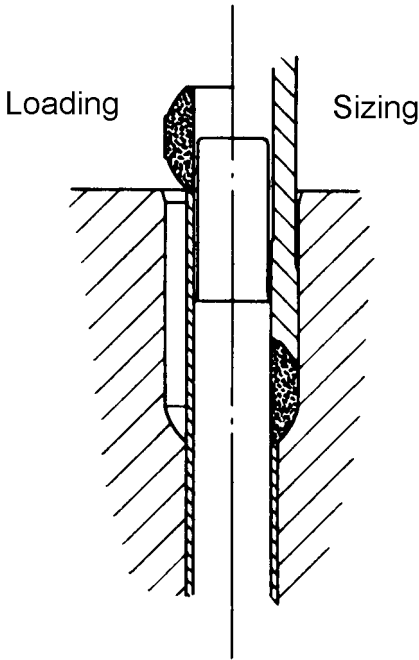


Figure 7.14 Simple "turn-over" sizing for spherical bushings.

Fig. 7.15, shows a tool design in which the sizing of the pressing flats can be accomplished in one cycle. Here, the component is again located on the relieved end of a fixed core rod. The die in this design is spring-supported and has a shallow cavity exactly half the length of the finished part. The upper punch does not enter the die, but has a flat land surrounding the cavity. The upper punch cavity is the mirror image of the die cavity, each containing exactly half the outer form of the part. As the upper punch descends, it forces the component down the core rod into the die and with the faces of upper punch and die slightly separated, the die also moves downward.

The component is carried over the full diameter of the core rod until it reaches its lower stop when final compression by the upper punch sizes the outer form of the part. As the upper punch withdraws, the die returns to its initial position and the lower punch follows to eject the component.

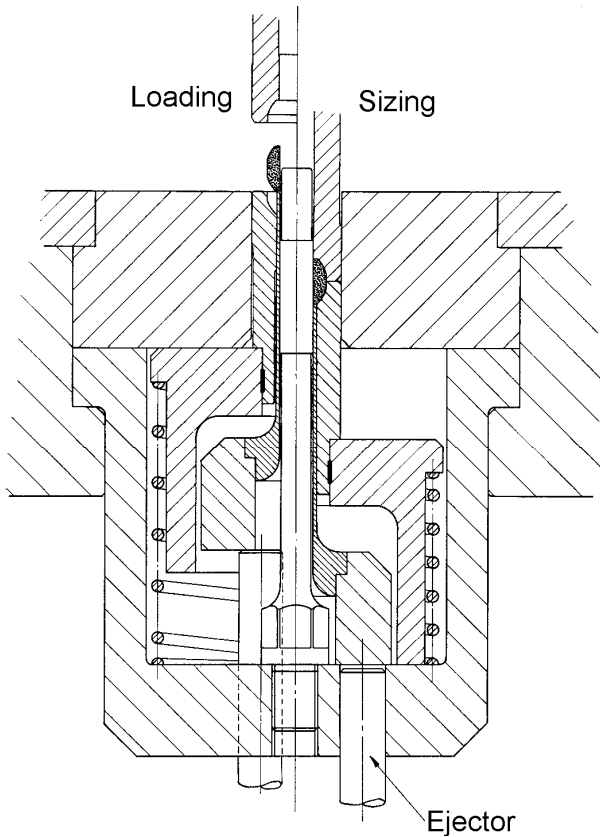


Figure 7.15 Complete sizing for spherical bushings.

There are two possible sources of trouble in this design:

1. The core rod relief must be kept to a minimum to ensure that the component is properly located, as otherwise the edges formed on the bushing by the pressing punches will catch the edge of the upper punch cavity and damage the bushing. A small radius or chamfer on the edge of the upper punch cavity helps to avoid this trouble.
2. As the faces of upper punch and die are in contact at the final sizing stage, these faces must be kept clean. If the part has been produced too long in pressing, there will be a tendency for material to be extruded between punch and die faces just before these faces meet. This will result in oversize parts with sharp burr and will overload both press and tools.

The central cylindrical portion on the outside of the spherical bushing is usually specified only because it is essential when pressing the green compact. The tolerance on the cylindrical portion is therefore not important and in fact, the customer would probably prefer the bushing entirely spherical.

In sizing, as the upper punch cavity gradually closes up on the die cavity, the outer form of the bushing is changed as shown in *Fig. 7.16 a and b*.

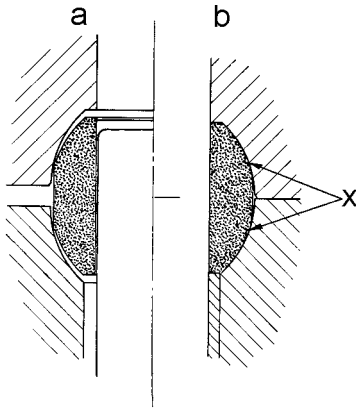


Figure 7.16 Detail of sizing action on spherical bushings.

Fig. 7.16a shows the sintered bushing holding the upper punch and die apart as it is moved downward. Only the small shoulders touch the upper punch and die at this stage.

Fig. 7.16b shows the bushing at the final compression stage. The small shoulders have been forced into the spherical form, but small depressions are always visible where the shoulders have been reformed (at X in the figure).

7.5.3 Profiled Parts with Holes

A typical example of a profiled part with hole is the cam shown in *Fig. 7.17a*. This type of part is particularly suited to the powder metal technique. The cam profile and the keyed hole will almost certainly have tolerances requiring sizing or coining. Coining can also improve the wear resistance of the material.

The tool design for this part is similar to *Fig. 7.5* with the addition of a relieved core screwed into the central hole of the die bolster. The core rod profile must be positioned to suit the loading position of the component.

This is often arranged by the use of a thin adjusting washer beneath the core rod shoulder. The problem with offset loading appears again, as it did in *Fig. 7.5*, and in this case, the core rod presents an additional problem.

It would obviously be preferable to set the core rod on the ram centerline, both to simplify toolmaking and to avoid an offset load on the core rod. In the example shown, the latter factor is probably more important than the offset loading of the ram and the core rod is therefore placed centrally. The combination of a profiled outer form with a profiled hole raises the question of correct alignment in the finished part. The necessity for avoiding eccentricity errors at the pressing stage has been pointed out.

This applies equally to alignment of external and internal profiles. Sizing and coining tools cannot be expected to correct errors in alignment due to faults in pressing and attempts to reset the key in correct alignment with the cam profile will certainly end in a broken core rod.

Alternatively, an upper core rod can be used, as shown in *Fig. 7.7*, if a suitable press is available, but it should be remembered that with an upper core rod, the bore should be sintered oversize. With a thick-walled component it is more difficult to make the oversize bore contract to the core rod.

Fig. 7.17b shows another profiled part having, in this case, two holes. Except that the sizing of the holes will require twin core rods set on a single base, the general design picture is unchanged. The problem here is another aspect of the alignment – in this case, variations in the center distance of the two holes. Unless a careful check is maintained during the pressing and sintering operations, the parts presented for sizing will have excessive variations in hole centers.

The holes are small and the sizing core rods correspondingly weak, so that even if the core rods do not break, being sufficiently flexible, the resulting holes will tend to be out of parallel and bell-mouthed. For these reasons, variations in hole centers, after sintering must be strictly limited.

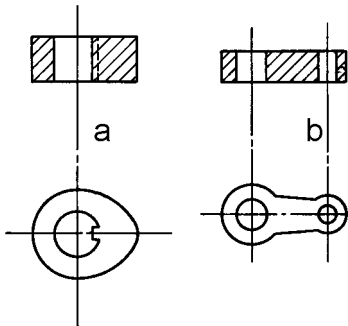


Figure 7.17 Typical profiled components with holes.

In *Fig. 7.10* we gave an example in which the bore of a bushing was double-sized by a short bulge on the core rod. An example of this method applied to an external profile is the tooling developed by engineers of the Ford Motor Co. in the USA for sizing oil pump gears and similar forms.

Manufacture of a solid tungsten carbide die of 75 mm length and containing an accurate gear profile presented such problems that it was decided to experiment with a short die section and double-size the gears by passing them through the short die and then re-passing them upwards before ejection. This method has since been used by other companies and a typical design is shown in *Fig. 7.18*.

The die is made up of three sections, a location plate, (*a*) into which the sintered gear is placed (by hand or by an automatic feeding device), a tungsten carbide ring, (*b*) only 12 mm thick and a lower die, (*c*) made of tool steel. The core rod is attached below the die.

The sintered gear is produced slightly oversize on both bore and outside form and rests on the rounded-off lip of the tungsten carbide ring. The upper punch forces the gear down through the tungsten carbide ring, closing the bore on to the core rod. The lower section of the die is made larger than the tungsten carbide ring by an amount less than its normal expansion and as the gear passes into the lower die, it expands slightly. During the entire sizing operation, there is no compression of the gear faces between upper and lower punches, as the end faces of the gear are ground to close tolerances in a later operation. The dimensions must be carefully considered on such a design, to prevent lead or spiral on the gears, as a result of the short die.

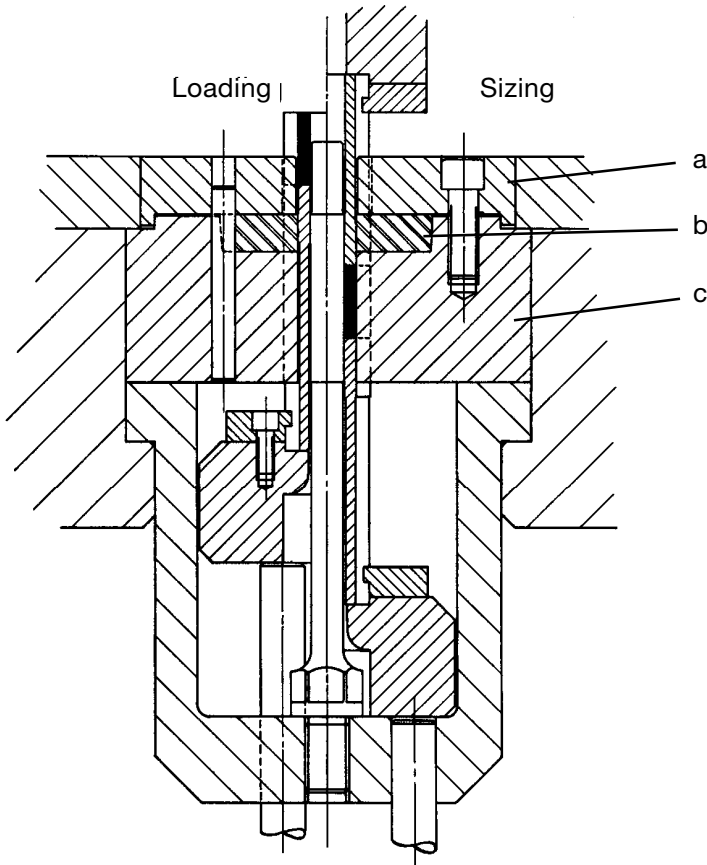


Figure 7.18 "Ring" sizing for profiled components like e.g. oil pump gears;

a = location plate,

b = profiled sizing ring of tungsten carbide,

c = tool steel die.

7.5.4 Parts with External Flanges

The typical part in this family is the flanged bushing, but there are also many other types of parts with flanges, as e.g. flanged connections. In a normal flanged bushing, the narrowest tolerances are required on the inner diameter and on the body outer diameter. It is, however, necessary to control the flange outer diameter and flange faces also, to avoid variations in the final size of the bore at the flanged end.

Fig. 7.19 shows a tool design in which the part is located over relieved end of a core rod secured to the base of the tool. As the press cycle begins, the lower punch drops away and the part rests between the core rod and the smaller diameter of the die.

The upper punch completes the movement of the part on to the die shoulder. The die, which has a limited downward motion, is supported on wedges, rubber pads, or a pneumatic cushion. The die support should be adjustable as it must be strong enough to resist the force of the bushing as it is pushed into the die. If the support pressure is too weak, the die will move downwards before the bushing's outer diameter has been sized and both external and internal sizing will take place simultaneously.

The continuing motion of the upper punch carries the part downwards, over the final diameter of the core rod and sizes the length of the part against the lower punch.

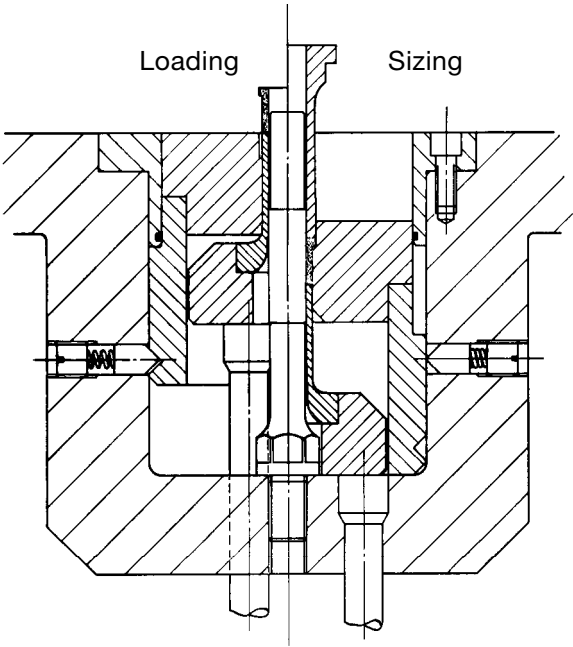


Figure 7.19 Sizing flanged bushings in a single-action press.

Stops beneath the die control the flange thickness also. After the upper punch has been withdrawn, the part is ejected by the lower punch, carrying the die upwards to its original position.

In all cases where sizing is required on a diameter which finishes below a shoulder, a radius is essential at the junction of shoulder and sized diameter, as the die shoulder must be rounded-off to perform its function of swaging the part to size. The proposals made in connection with *Fig. 7.3*, regarding the swaging radius, can be applied here.

Fig. 7.20 shows an alternative design for use with a double-action press. Here, the die does not move and the progressive sizing action is obtained by the separate motions of the upper punch, attached to the blank holder and the core rod, attached to the main ram.

To overcome the difficulty of locating the bushing, a dummy core rod is used which projects above the die face. This dummy core rod is spring-supported and is pushed downwards by the upper core rod as it descends.

The relative motion of upper punch and upper core rod can be arranged as shown in *Fig. 7.7*, where the bushing is contracted on to the core rod, or as in *Fig. 7.8*, where the core rod passes through the bushing after the outer diameter has been sized.

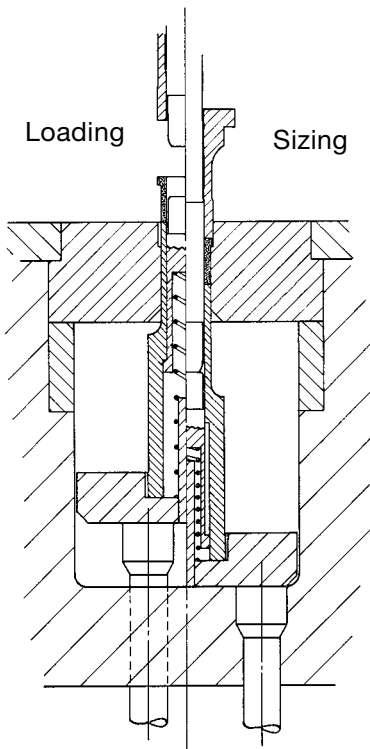


Figure 7.20 Sizing flanged bushings in a double-action press.

Fig. 7.21 shows how the proportions of a part can affect the tool design. Here, the long flange portion can be located by an outer location plate, leaving enough of the part projecting for the operator (or gripping device) to locate and remove it without difficulty. The dummy core rod shown in *Fig. 7.20* is unnecessary.

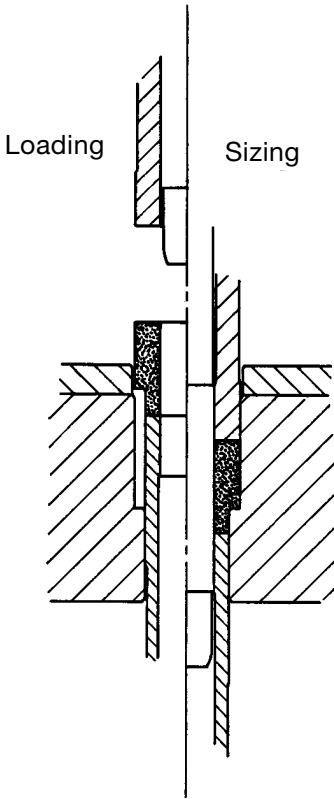


Figure 7.21 Sizing bushings with thick flanges.

The coining of shouldered parts presents another problem to the tool designer. Many coining operations require a reduction in volume by 10% or more. As the face area of the part is reduced very little, almost all the reduction in volume is achieved by reduction in length of the part. A 10% reduction in the flanged bushing shown in *Fig. 7.19* would mean a reduction in the length below the flange of 1.5 mm.

If the tool is designed with a fixed die as in *Fig. 7.20*, the end of the bushing will meet the lower punch while the flange is still 1.5 mm above the die shoulder.

Any material moved by the swaging action of the die shoulder will tend to build up a wave beneath the flange of the bushing. The final downward movement of the bushing flange as it is compressed to correct length and density, tends to force this wave of material outwards and form a separate layer in the corner of the flange.

In practice, where circumstances permit, the sintered part is usually made small enough to go easily inside the die shoulder and thus no swaging action takes place. Even with this precaution, it is advisable, to avoid cracking on the bushing shoulder, to use a floating die design if the length beneath the shoulder is more than 6 or 7 mm.

7.5.5 Parts with Internal Flanges

The typical part in this family is the piston. *Fig. 7.22* shows a simple design for sizing surfaces of a piston.

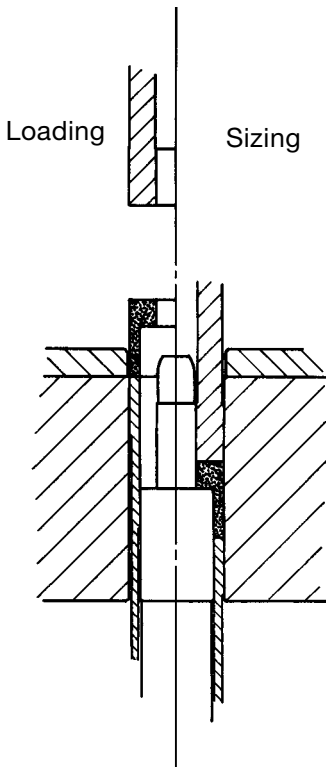


Figure 7.22 Complete sizing of piston.

The part is placed within a location plate and rests upon the lower punch in its loading position. A shouldered core rod is rigidly secured below the die. As the upper punch descends, it first forces the piston skirt into the die and then over the core rod.

If the proportions of the part permit, the length of the core rod tip, between the relieved portion and the core rod shoulder, should be longer than the skirt of the piston.

If this can be arranged, then the small bore of the piston will be sized before the skirt. Otherwise, the two bores are sized simultaneously. The part is ejected to the die face by the lower punch.

Many small pistons, used in automobile shock absorbers and for other purposes, have circular ribs on both faces of the piston head. Where these ribs have to be sized, it is sometimes more convenient to simplify the sizing operation by centerless grinding the outer diameter of the piston in a subsequent operation.

The simple tool shown in *Fig. 7.23* is then quite satisfactory and the job can frequently be done in a hand press. The part is placed head downwards in a shallow die plate and the core rod, attached to the ram, descends to size the small bore and set the form of the ribs. As this action usually causes the part to grip the core rod, a simple stripper plate, attached to the die table, surrounds the core rod and the part is freed as the core rod retracts through the stripper plate.

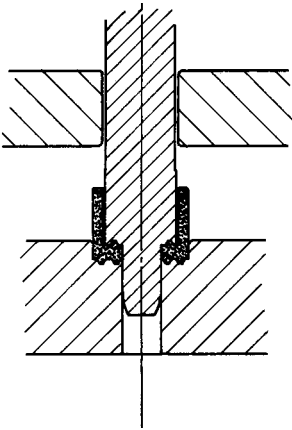


Figure 7.23 Sizing piston faces and bore.

Fig. 7.24 shows a design suitable for a double-action press, where complete sizing is required on a piston. The part is placed within the location plate, resting upon the lower punch. The core rod is attached to the ram, and the upper punch

to the blank holder. Core rod and upper punch descend together, the punch forcing the part down the die to its final position. As the upper punch slows, the core rod speed is maintained and the core rod sizes the small bore and large bore before finally sizing the ribs on the piston head. The core rod is withdrawn before the upper punch and the lower punch then ejects the piston to the die face.

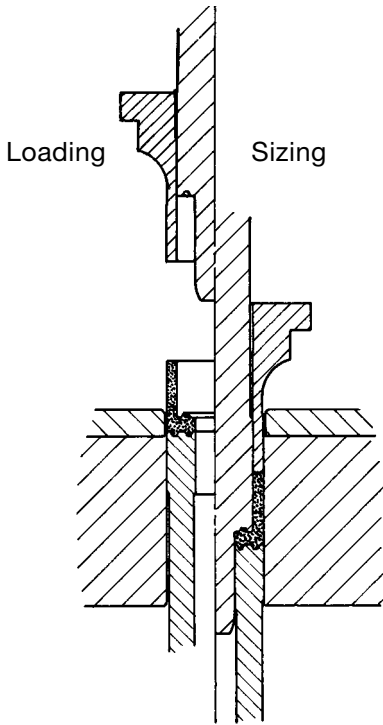
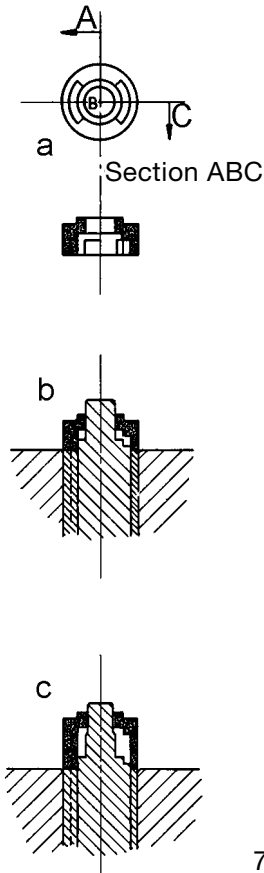


Figure 7.24 Sizing pistons in a double-action press.

There are numerous cases where a part is required with two internal steps and profiled internal forms are not uncommon. *Fig. 7.25a* shows an example of this type of part. The various problems and possibilities, connected with a profiled part like this, offer several alternative sizing tool designs.

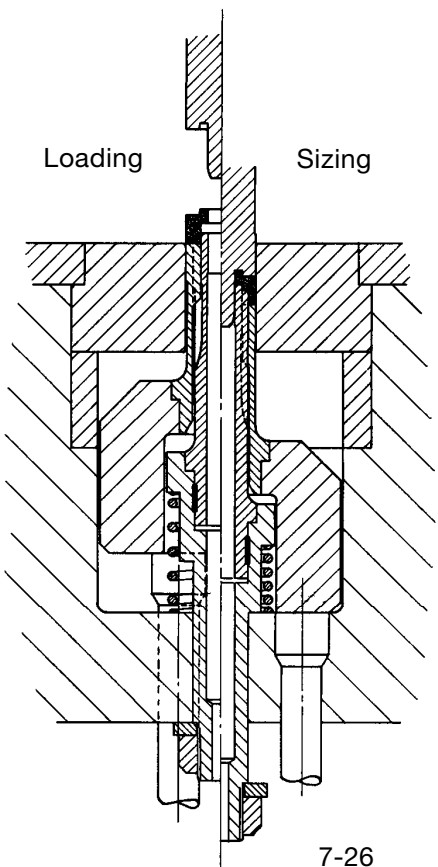
Considering this stage by stage, the first point to be decided is the method of location. An external location will not prevent misalignment of the internal splines. Therefore, the part must be located on the core rod. An upper core rod cannot be used for location, so we start with a core rod within the die.

If we begin with the design shown in *Fig. 7.25b*, we have a lower punch supporting the skirt of the part and a core rod having three diameters within the part. This core rod is raised upon a spring to the ejecting position and is forced down upon a stop by the action of the upper punch. The profiled portion of the core rod must project above the face of the lower punch after ejection to provide location for the part. A practical minimum for this location height is 1.5 mm.



7-25

Figure 7.25 Location of pistons with internal profiles.



7-26

Figure 7.26 Sizing pistons using upper core rod.

Two factors are immediately evident.

First, the sintered part must be large enough to fit freely over the core rod. This is often necessary and can be convenient if the part has been made oversize on the outer diameter and length dimensions, so that sufficient material is moved in sizing to close the part on to the core rod.

Secondly, the part after ejection is not free of the core rod. It is likely that the skirt of this part will, in fact, be free (i.e. not tightly fitting on the core rod), as the work done in sizing will have given the part an internal stress which will cause it to expand slightly upon leaving the die.

This same effect can also tend to free the smaller bore of the part, but as the diameter here is only 50% of the larger bore, the expansion of the part will be correspondingly reduced. We are speaking now of very small dimensional changes. 10 to 20 μm might be anticipated on the skirt in this instance.

If the expansion on the smaller bore is 50% of this, it will be appreciable that very small variations can make the difference between a part which lifts easily and one which resists all attempts to move it.

For example, variations in sintered diameter of the small projecting boss on the upper face of the part could easily upset the anticipated expansion of the smaller bore. Another factor which can affect the removal of the part is that, in some cases, the stress within the part can actually provoke a tendency for the smaller bore to shrink as it comes off the core rod, even though the outside diameter of the part expands. For this reason, the tool might not work well and one possible answer to the problem is shown in *Fig. 7.25c*.

As we are discussing a hypothetical part, portions have been assumed which demonstrate the typical problems. If, however, we have a part with a longer skirt relative to the thickness of the head, the problem of freeing the small bore from the core becomes simpler. The core rod tip can now be relieved as shown and the part is easily located and removed. If the proportions of the part do not permit the above solution, the design shown in *Fig. 7.26* presents another approach.

The major difficulty has been the freeing of the part from the smallest portion of the core rod, so this portion is now attached to the upper punch. The other internal forms are located on the spring-supported punch fitting within the lower punch. The part is still located upon the profile form and the small bore of the part must be large enough to permit the descending core rod to fit easily inside it. The upper punch then forces the part into the die, completes the sizing and upon withdrawal allows the part to be ejected and removed without difficulty.

Although the upper core rod and punch size only a small portion of the total vertical surface of the part, it is still possible that these portions of the part and

the amount of work done in sizing might cause the part to grip the core and be drawn out of the die.

If a double-acting press is available, the core rod and upper punch can be operated as in *Fig. 7.21*. alternatively, the design shown in *Fig. 7.27* can be used. In this design, the smallest bore is sized by a fixed core rod fitting within the spring-supported lower punch.

The fixed lower core rod can be relieved, giving the double advantage that the smallest bore of the part can, if desired, be small after sintering and the sizing action can be arranged progressively if the core rod relief is positioned correctly.

On the other hand, the design shown in *Fig. 7.27* has one disadvantage. In this case, an additional moving part is required.

Any moving part must have sufficient clearance for satisfactory operation and although each clearance may be only 12 to 20 μm , every additional moving part means a possible increase in eccentricity of the part.

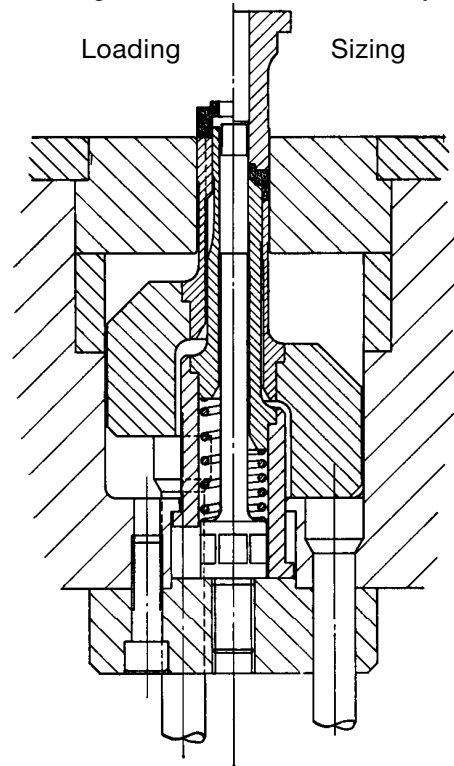


Figure 7.27 Sizing profiled pistons using lower core rod.

From the foregoing examination of the design problems for various types of parts, it should be clear that tool designs are very much dependent upon the type of press available for sizing. In all that has been said it has been assumed that the presses operate upon a cycle normal for crank presses.

As the normal press completes its cycle with the ram at Top Dead Center, it follows that the ejection punch will stop at its highest point, level with the die face. In some cases, however, it can be arranged that the press stops some way beyond Top Dead Center, or the ejection mechanism can be offset in such a manner that the ejection punch comes to die face level, thus freeing the part, and then withdraws slightly before coming to rest. The part will remain on the die face, due to its slight expansion on leaving the die. An example of such a case is shown in *Fig. 7.28*. The part is similar in type to that in *Fig. 7.25a* but here, the body of the part is much more solid and would probably not free itself from the core rod unless completely ejected.

If the motion of the ejection punch can be arranged so that it frees the part entirely from the core rod and then withdraws sufficiently to permit location of the next part on the core rod, the operation becomes considerably simpler.

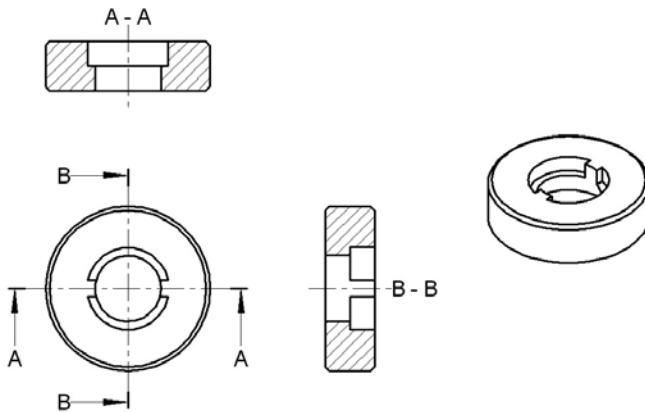


Figure 7.28 Thick-walled component with internal profile.

7.5.6 Other Complex Parts

Types of parts of more complex shape than those treated in the preceding paragraphs have special problems in pressing, particularly with ejection type tooling, but if such complex parts can be satisfactorily pressed, sizing and coining is usually less difficult. In practice, tooling designs for sizing and coining such parts are combinations based upon the designs already examined.

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