



Industrial Lubrication and Tribology

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Markus Klein Bernd-Robert Höhn Klaus Michaelis Rainer Annast

Article information:

To cite this document:

Markus Klein Bernd-Robert Höhn Klaus Michaelis Rainer Annast, (2011), "Theoretical and experimental investigations about flank breakage in bevel gears", Industrial Lubrication and Tribology, Vol. 63 Iss 1 pp. 5 - 10

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Theoretical and experimental investigations about flank breakage in bevel gears

Markus Klein, Bernd-Robert Höhn and Klaus Michaelis

Gear Research Centre, FZG, TU München, Munich, Germany, and

Rainer Annast

BMW AG, Munich, Germany

Abstract

Purpose – A failure mode called “flank breakage” is increasingly observed in cylindrical and bevel gears. Up to now, there was no calculation method available to determine the load-carrying capacity related to flank breakage in bevel gears. Therefore, a research project was initiated to investigate the described failure mode in bevel gears and to develop a calculation method to predict the risk of flank breakage of such gears. The purpose of this paper is to describe this project.

Design/methodology/approach – The presented research project contained: determination of the decisive influence parameters in experimental investigations with bevel gears; development of a model to explain flank breakage in bevel gears; and development of a calculation method and design rules to avoid flank breakage.

Findings – In systematic tests, the influenced parameters of flank breakage were investigated. Besides the load torque, especially the case depth and the core hardness turned out as decisive parameters. A higher sulfur concentration in the material does not seem to be critical. The analysis of damage patterns of test and practical gears showed that the initiating crack always started below the surface in the region of the transition from case to core. For unidirectional loading, the crack propagates to the active flank on the one side and to the tooth root on the other side. On the basis of these findings, a local and a simplified calculation method were developed to estimate the risk of flank breakage.

Originality/value – With the described calculation method, it is now possible to evaluate running gears according to their risk of flank breakage and design new gears with a sufficient safety factor to avoid this failure.

Keywords Gearing, Breaking load, Load capacity

Paper type Research paper

Nomenclature

a	= hypoid offset (Mm)
b_0	= half of Hertzian contact width (Mm)
c	= strength factor
d_{e2}	= outer pitch diameter wheel (Mm)
H_{KCH}	= depth of transition from case to core (Mm)
HV	= Vickers hardness (HV)
k	= slope of S-N curve
K_A	= application factor
m_{mn}	= mean normal module (Mm)
n	= load cycles pinion
p_H	= Hertzian pressure (N/mm ²)
n	= pinion speed (1/min)
t	= pinion torque (Nm)
y	= co-ordinate into tooth depth (Mm)
z	= number of teeth
α_n	= pressure angle
σ_H	= decisive flank pressure (N/mm ²)
τ_A	= local strength (N/mm ²)
τ_{eff}	= effective shear stress (N/mm ²)

$\tau_{eff,a}$	= shear stress amplitude (N/mm ²)
$\tau_{eff,ES}$	= residual shear stress (N/mm ²)

Indices

1, 2	= pinion, wheel
D	= continuously transmittable

Introduction

Flank breakage often appears without any other surface failures (scuffing, pitting, and micropitting), but sometimes also together with such failures on different teeth of a gear (Figure 1). Although normally several teeth are affected by flank breakage, it also can be seen that only one tooth fails.

The run time till the failure appears strongly varies and is usually very high ($N > 10^8$) both on high- and low-load levels. Flank breakage is more often observed in gears with large modules and wheel diameters than in smaller gears.

Up to now, there was no calculation method available to determine the load-carrying capacity related to flank breakage. It was assumed that flank breakage is influenced by pressure angles higher than $\alpha_n = 20^\circ$ (ZF-Norm 201, 1990), which causes bending stresses in the flank higher than in the tooth root. Further on, non-adequate material properties and heat

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Industrial Lubrication and Tribology
63/1 (2011) 5–10
© Emerald Group Publishing Limited [ISSN 0036-8792]
[DOI 10.1108/00368791111101786]

Paper presented during the 3rd International Conference on Integrity, Reliability and Failure – IRF'2009, within the Symposium on Integrity, Reliability and Failure of GEARS and TRANSMISSIONS, held at Faculdade de Engenharia da Universidade do Porto, Portugal, July 20-24, 2009.

Figure 1 Flank breakage at a test pinion



treatment were expected to increase the risk of flank breakage, especially too low core strength (Dudley and Winter, 1961; Coleman, 1967) and toughness (Gärtner, 1998) or too low (Dudley and Winter, 1961) or too high (Coleman) case depths. In many American publications, flank breakage is also called “subsurface fatigue” or “subcase fatigue”. In these papers, the flank pressure is also regarded as the decisive parameter (Sandberg, 1981; ANSI/AGMA 2003-B97, 1997; Sharma *et al.*, 1977; Elkholy, 1983; Pederson and Rice, 1961).

The intentions of the presented research project were:

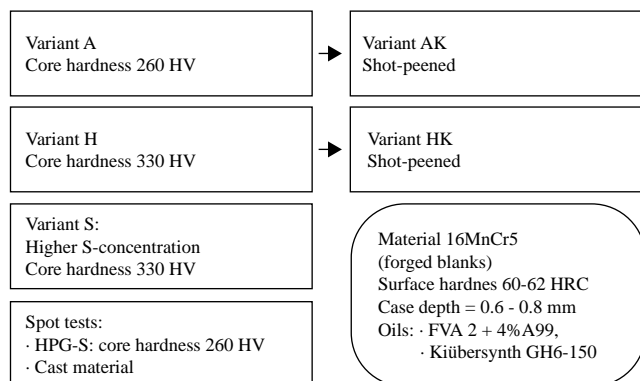
- determination of the decisive influence parameters in experimental investigations;
- development of a model to explain flank breakage in bevel gears; and
- development of a calculation method and design rules to avoid flank breakage.

Experimental investigations

Test gears

In Figure 2, the test gears for the experimental investigations are shown. The main geometry data are: number of teeth $z_1/z_2 = 11/39$; mean normal module $m_{mn} = 3.02$ mm; outer pitch diameter $d_{e2} = 170$ mm; and hypoid offset $a = 15$ mm.

Figure 2 Test gears



The variants A-S are lapped and then phosphated. Variants A and AK have a relative low core hardness of 260 HV. Variants H, HK, and S were manufactured by usual heat treatment. The pinions of variant S were made of 16MnCr5 with a higher sulfur concentration of 0.05 percent. Variants AK, HK, and S were shot-peened to avoid tooth root breakage. For some spot tests, the influence of the finishing process was investigated with hard cut (HPG-S) and phosphated gears. Additionally, the area reduction ratio of the material was investigated by using cast material instead of forged blanks. All the test results were compared with the test results of Thomas (1998).

Test conditions

The tests were carried out on FZG back-to-back test rigs for bevel gears. The gears were run on the drive side flank (concave pinion flank) with a pinion speed of $n_1 = 4,200$ /min. For the flank breakage tests, the oil Klübersynth GH6-150 (ISO-VG 150, polyglycol) was used, for the pitting investigation the ISO-VG 32 mineral oil FVA2 + 4%A99 (Schilling, 1985). The oil was sprayed into the contact in sufficient quantities with a temperature of 90°C. For the variants H, HK, and S, a lower temperature of 70°C was chosen to avoid tempering of the pinions.

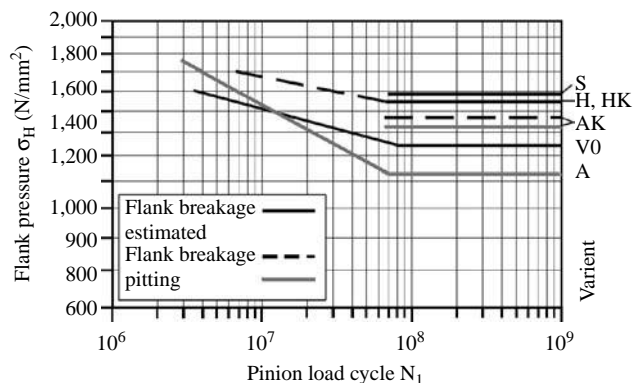
Test results

The test results for variants A-S and the variant V0 of Thomas (1998) are shown in Figure 3 for 50 percent failure probability.

The test gears H, HK, and S failed by flank breakage. With few exceptions always the pinions were damaged. The runtime varied up to a factor 8. The variants H (without shot-peening) and HK (with shot-peening) showed a similar load-carrying capacity, and therefore were evaluated together. Because of a high variation of the test results and different failures at higher loads (pitting), the time strength curve was only estimated. For variant S with a higher sulfur concentration, only loads in the high cycle fatigue were run. The results are very similar to the variants with normal sulfur concentration. Thus, the higher amount of sulfur has no influence on the load-carrying capacity.

In the tests with the variants A and AK with lower core hardness, only one flank breakage occurred. The tests with FVA2 + 4%A showed pitting, whereas in the tests with Klübersynth no surface failures appeared. With higher loads,

Figure 3 S-N-curves for variants A-S and V0 of Thomas (1998) for 50 percent failure probability



tooth-root breakage occurred and only variant AK reached the calculated endurance limit. For variant A, a decreased surface hardness was recognized that caused a lower pitting resistance.

Additionally, some tests with short-overload cycles (pinion torque $T_1 = 700 \text{ Nm}$, $N_1 = 100 \dots 200$ revolutions) were run. After the overload cycles, the gears again were run on endurance limit load level for $N_1 = 5 (\dots 10^6)$ to $10 (\dots 10^6)$ revolutions. For variant AK, three flank breakages were recognized, for variant HK, only one, whereas variant S failed by tooth root breakage. Variant A showed no failure although the overload cycle was repeated four times.

In the spot tests for the investigation of the influence of the finishing process and the area reduction ratio of the material pitting, tooth-root breakage occurred. The loads in these tests were lower than the endurance limit of variants H, HK, and S referring to flank breakage. Thus, an influence of the finishing process and the degree of deformation could not be stated.

Damage patterns

Test gears

For the evaluation of the test results, only those test gears were taken into consideration that failed without any other surface failures as, e.g. pitting. The area of fraction lies in the active flank. Thus, the failure mode can be distinguished from tooth-root breakages, where the first cracks start in the tooth fillet near the 30° tangent. All tests were run on the drive side flank. All test gears showed the same path of crack propagation as the tests of Thomas (1998). The cracks start below the surface in the active flank, 3–4 mm below the tip of the teeth. They mostly lie parallel, sometimes slightly inclined to the lengthwise direction of the teeth. The cracks range over 50 to 90 percent of the face width and end at the tip of the teeth to the toe and the heel side of the flanks (Figure 4).

The area of fraction has an angle of 40 to 90° to the active flank and runs to the tooth root of the non-working flank (Figure 5). The cracks usually are parallel in the area of fraction. Thus, the starting crack cannot be located exactly.

To determine the path of crack propagation the fraction pieces were cut in a normal section in the area, where the starting crack was assumed. In this section, secondary cracks were investigated; these secondary cracks run typically in the same direction as the primary crack propagation. Figure 6 shows an enlarged part of Figure 5 that shows the secondary cracks on a fraction piece of variant HK. These secondary cracks indicate that the main crack propagated from below the surface to the active flank.

The analysis of the fraction patterns of the test gears shows that the cracks start 0.5–1.2 mm below the surface within the case depth. Only variant AK shows a crack initiation in a

Figure 4 Fraction of variant HK (ME 37536) – crack lies on half of the tooth height of the active flank (heel left side, $T_1 = 550 \text{ Nm}$)

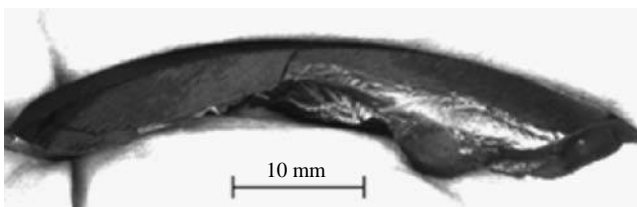


Figure 5 Fraction of variant HK (ME 37536) – normal section, active flank on the right side

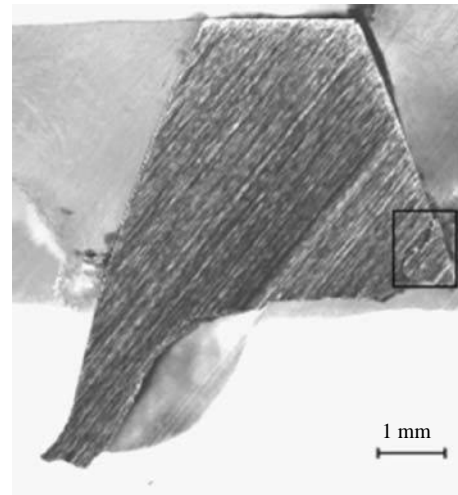
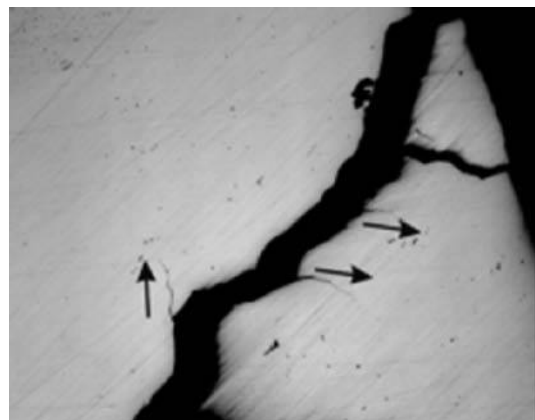


Figure 6 Fraction of variant HK (ME 37536) – crack propagation to the surface of the active flank



shattered area near the surface. Further on, it could be shown that all the breakages were endurance failures and there were no inclusions that initiated the cracks.

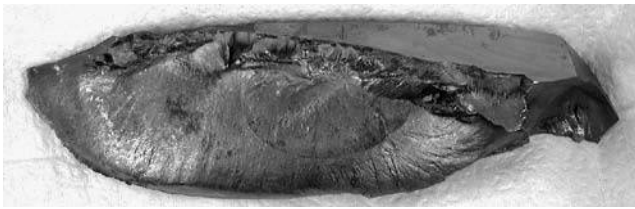
Gears from practical applications

In addition to the test gears, failures from gears in practical applications were investigated. For these gears, the fraction pattern, the area of the starting crack, and the crack propagation were analysed. Metallographic analyses were also carried out. The gears had modules from $m_{mn} = 14.3\text{--}19.5 \text{ mm}$ and were made of 18CrNiMo7-6 and case hardened.

Figure 7 shows exemplarily the fraction piece of a marine gear ($m_{mn} = 14.3 \text{ mm}$) with flank breakage on five teeth of the wheel.

The fraction pattern shows that the crack started approximately 5 mm below the surface of the active flank in the centre of the half-ellipse. The inspection of this area in a scanning electron microscope showed that the material was strongly deformed in this area. Therefore, it was not possible to identify exactly the crack initiation. Further on, no material impurities were found. Several parallel cracks can be seen in direction to the

Figure 7 Flank breakage at a wheel of a marine gear unit



Note: Topview on the fraction pattern, active flank upside, starting crack in the centre of the half-ellipse

flank surface. They also started below the surface. The assumed starting crack lies 18 mm below the tip of the tooth, and thus exactly on half-height of the active flank.

To analyse the secondary cracks the fraction piece was cut in a normal section. Figure 8 shows that up to a depth of 3.6 mm, below the surface, the crack propagated in direction to the surface, in a depth of more than 5 mm in direction to the core (Figure 9). Thus, the starting crack initiated in 3.6–5 mm depth below the surface.

The shown damage pattern of the marine gear is very similar to the test gears. The starting crack lies below the case depth in the region between case and core. All flank breakages were endurance failures. Material impurities could only be shown for one gear. For all the other gears, the initiating crack could not be located exactly.

The area of fraction is inclined to the active flank in an angle of 40 to 110° and reaches the surface on half of the tooth height of the active flank. On the other side, the fraction reaches the surface in the tooth root area of the coast side flank.

On the active flank, several wedge shaped fractions form after the initiating crack in the tooth. Some of the gear units showed only one tooth broken, but often several teeth were broken. Thus, flank breakage can be seen as a systematic failure. Half of the investigated gears of practical application

Figure 8 Marine gear: secondary cracks in direction to the active flank

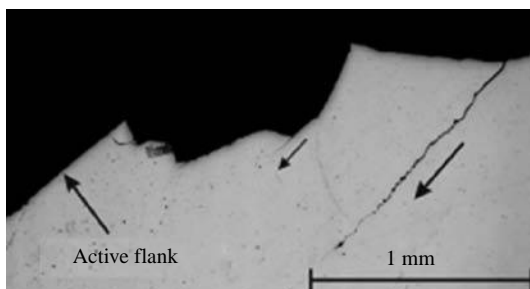


Figure 9 Marine gear: secondary crack in direction to the core (5 mm subsurface)



showed scuffing that also could be seen on the fraction pieces. This means that this failure occurred before the flank breakage. Scuffing always indicates high stresses on the flank and often arises after short impacts that are much higher than the nominal load.

Theoretical considerations

Stress calculation on and below the surface

Both, bevel gears with and without hypoid offset have point contact due to flank form modifications. Because of very long ellipses with a large axial ratio of 12.3 (test gears) to 30...75 (practical gears) the calculation of the subsurface stresses results in similar values for point and line contact. Therefore, the results presented in the following are based on the calculation of a line contact. The sliding velocity in the contact between the two flanks together with the normal load results in shear and thermal stresses. Those stresses occurring on the flank surface influence the subsurface stresses till a depth of $y = 0.5 \cdot b_0$ (b_0 = half of Hertzian contact width; Oster, 1982). Thus, they are irrelevant for flank breakage. The elasto-hydrodynamic pressure distribution in the loaded contact is also not influencing the subsurface stresses in a greater depth. For the calculation of the stresses below the surface, it is sufficient to consider the pressure distribution in the contact. To describe the multi-axial stress distribution in the contact and the rotation of the principal coordinate system, the shear stress intensity hypothesis is used. In opposite to other approaches, this criterion describes the time-dependent stresses (Höhn and Oster, 1995).

Calculation of the dynamic load-carrying capacity under consideration of the residual stresses

In the dynamic calculation of the load-carrying capacity static loads, as, e.g. the residual stresses, are considered as mean stresses. First of all, the effective shear stress τ_{eff} is calculated from the stresses induced by the load and the residual stresses. Then, the shear stress $\tau_{eff,ES}$ resulting only from the residual stresses is subtracted. This results in the decisive shear stress amplitude $\tau_{eff,a}$:

$$\tau_{eff,a} = \tau_{eff} - \tau_{eff,ES} \quad (1)$$

The local Vickers hardness is used as the local strength τ_A :

$$\tau_A = c \cdot HV \quad (2)$$

This approach was originally used for pitting calculations and is only valid for residual compressive stresses, as they normally appear in the case depth. Based on tests with spur gears, Tobie (2001) found that the factor c must be $c = 0.4$. Because of the mechanical balance in the tooth, residual tensile stresses occur in the core. They are taken into consideration in the calculation of the strength by the mean stress sensitivity, but reduce the strength only marginally. The core is expected to be uncritical because of the decreasing stresses over the depth. This is confirmed by the damage patterns that show the initiating crack in the transition of case to core.

The pressure distribution on the surface as well as the curvature of the flanks is taken from the FVA calculation program BECAL (Baumann *et al.*, 1998). The Hertzian contact width b_0 is calculated from the pressure p_H and the relative radius of curvature ρ_{rel} . The local safety factor is defined as the ratio of the local strength τ_A and the local stress $\tau_{eff,a}$. For the test

gear variants H and HK, the gradient of the local safety factor was calculated considering the continuously transmittable load torque and the measured residual stresses (Figure 10). The local safety factor in the core is estimated.

The minimum safety factor is reached in the region between case and core, and thus is well correlating with the damage patterns of variant H and HK as discussed before. For variant S, the initiating crack could not be located exactly because of the row-like structure of the material in this region. The investigated practical gears show a very flat gradient of the local safety factor inside the tooth without a distinctive minimum.

These investigations and the damage patterns have shown that the region between case and core is decisive for flank breakage. Because of the change of residual compressive to tensile stresses the residual stresses, are very low in this region (MackAldener, 2001). Thus, they can be neglected.

Simplified calculation method of the load-carrying capacity for flank breakage

For a simplified calculation, the load-dependent stress and the strength based on the Vickers hardness are regarded at the transition of case to core. Because the ratio of shear stress intensity τ_{eff} and maximum shear stress τ_H is constant in this region, the stresses are calculated with the shear stress hypothesis. The gradient of the maximum shear stress, described by the component $\tau_{H1,2}$, is derived by the Hertzian pressure p_H on the surface and half of the contact width b_0 :

$$\tau_{H1,2} = p_H \left(\frac{y}{b} - \frac{(y/b_0)^2}{\sqrt{1 + (y/b_0)^2}} \right) \quad (3)$$

The described calculation method was applied to the test gears as well as to the gears from practical applications (module $m_{mn} = 10.6 \dots 19.5$) (Figure 11). The calculation was made in the mid of the face width on half of the tooth height.

To derive the critical value of the ratio $\tau_{H1,2}/H_{KH}$ the test gears were calculated with the continuously transmittable load torque derived from the test results and the measured hardness patterns and case depths. The test gears that failed by flank breakage show a ratio $\tau_{H1,2}/H_{KH}$ of $0.6 \text{ N/mm}^2/\text{HV}$, only variant AK with an unusual heat treatment lies far above. The practical gears were calculated with the load torques of their application together with usual application factors of $KA = 2$ for

Figure 10 Gradient of the local stress, strength and safety factor for the test gear variants H and HK considering the enduringly transmittable load torque

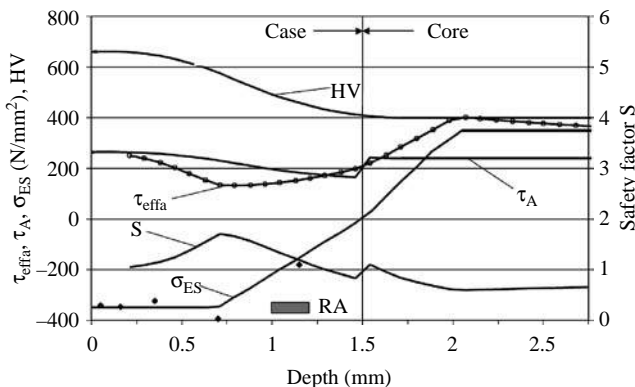
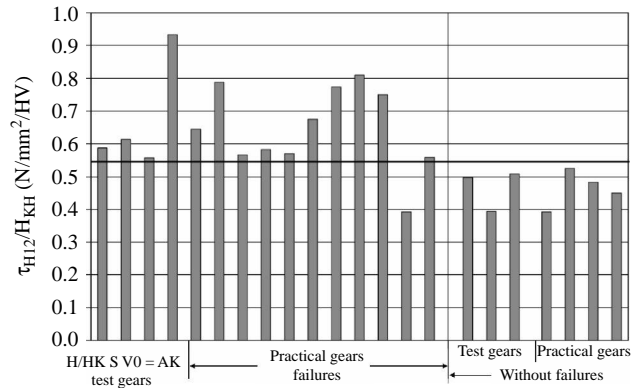


Figure 11 Ratio $\tau_{H1,2}/H_{KH}$ at the transition from case to core for the test and practical gears



the power plant gears and $KA = 1.3$ for the marine gears, respectively. For the practical gears with failures the measured hardness patterns and case depths were introduced into the calculation. For the practical gears without failure a case depth of $0.15 \cdot m_{mn}$, a core hardness of 400 HV , and a depth of the transition from case to core of $y = 1.85 \cdot \text{case depth}$ was assumed. The practical gears with failure show a ratio $\tau_{H1,2}/H_{KH}$ greater than $0.55 \text{ N/mm}^2/\text{HV}$ with one exception. All gears without failure are below this ratio. The comparison of the calculation results showed that the simplified calculation method shows a good correlation of test and practical gears. For new gears, it is advised to design the geometry and heat treatment not to exceed a ratio $\tau_{H1,2}/H_{KH} = 0.55$.

Conclusion

In systematic tests, the influence parameters of flank breakage were investigated. Besides the load torque, especially the case depth and the core hardness turned out as decisive parameters. A higher sulfur concentration in the material does not seem to be critical. The analysis of damage patterns of test and practical gears showed that the initiating crack always started below the surface in the region of the transition from case to core. For unidirectional loading, the crack propagates to the active flank on the one side and to the tooth root on the other side.

The stress distribution resulting from the load and the residual stresses were analysed theoretically and compared with the local strength that was derived from the gradient of the local hardness. On this basis, a simplified calculation method was developed to estimate the risk of flank breakage. In this method, only the region of the transition from case to core is regarded. A critical limit was derived from the analysis of the test and practical gears with this simplified calculation method. If this value is exceeded, the risk of flank breakage is high.

With this calculation method, it is now possible to evaluate running gears according to their risk of flank breakage and design new gears with a sufficient safety factor to avoid this failure.

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Corresponding author

Markus Klein can be contacted at: klein@fzg.mw.tum.de

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