

Chapter 2

Composite Long Rod Insulators

Symbols and Abbreviations

3D	Three-dimensional
CIGRE	Conseil International des Grands Réseaux Électriques (International Council on Large Electric Systems)
DLL	Damage limit load
FE mesh	Finite element mesh
FEM	Finite element method
FE model	Finite element model
FRP	Fibre reinforced plastic
IEC	International Electrotechnical Commission
IEEE	Institute of Electrical and Electronics Engineers
IREQ	Institut de Recherche d'Hydro-Québec (Québec-Hydro's Research Institute)
LVDT	Linear variable differential transformer
RML	Routine mechanical load
SML	Specified mechanical load
UHV	Ultra-high voltage
δ	Half length of the linear region for the shear stress
ΔR	Radius reduction of the FRP rod during crimping
ϑ	Coordinate in circumferential direction of the FRP rod
k	Danger factor
μ	Coefficient of friction between metal and FRP
ν_{LT}	Poisson number "axial-transverse" to the FRP rod
ν_{TT}	Poisson number "transverse-transverse" to the FRP rod
$\sigma_{\vartheta\vartheta}$	Circumferential stress in the FRP rod
σ_M	Standard deviation
$\sigma_{shear}^{\max}$	Shear strength of the FRP rod
σ_{rr}	Radial stress in the FRP rod
σ_z	Tensile stress in the cross-section of the end fitting
σ_{zul}	Tensile strength of the end fitting

σ_{zz}	Axial stress in the FRP rod
τ	Critical shear stress in the end fitting
τ_f	Shear stress in the FRP rod
τ_{max}	Shear strength in the FRP rod
$\tau_{rz}(z, R)$	Shear stress at the surface ($r = R$) of the FRP rod
τ_{zul}	Max. permissible shear stress of the end fitting
b	Wall thickness of the end fitting
d	Diameter of the FRP rod
dz	Length of the differential beam element
D_o	Outer diameter of the end fitting
D_i	Inner diameter of the end fitting
D_k	Ball size (ball diameter)
DS	Stiffness of the fibre
e	Location of critical shear of the end fitting
E	Modulus of elasticity (Young's modulus) of the fibre
E_L	Modulus of elasticity (Young's modulus) in axial direction of the FRP rod
E_T	Modulus of elasticity (Young's modulus) in transverse direction of the FRP rod
F	Cross-section of fibre
F_{max}	Failing load in tension of the insulator
F_N	Pressure load between metal and FRP
F_z	Tensile load in the FRP rod
G_{TT}	Shear modulus "transverse-transverse" of the FRP rod
G_{LT}	Shear modulus "axial-transverse" of the FRP rod
L, L_{cr}, L_p	Crimp length of the end fitting
M_{96}	Average 96 h failing load
M_{av}	Average failing load of the assembled core
p	Contact pressure on the FRP rod
r	Radial coordinate for the FRP rod
R	Radius of the FRP rod before crimping
S	Contact (interface) surface between FRP rod and end fitting
x, y, z	Geometry parameters of the end fitting
x_{cr}	Critical crimp length
x_{max}	Limit crimp length
z	Axial coordinate of the FRP rod

2.1 Applications of Composite Long Rod Insulators

The term "long rod" is actually used for a specific design of porcelain insulator which was first introduced in Germany in the 1920s as an alternative to cap-and-pin insulators (and also for critical ambient conditions, for example high pollution). The construction of a composite long rod is shown in Fig. 2.1. The fibre reinforced core, the metal fittings and the silicone housing can be seen.

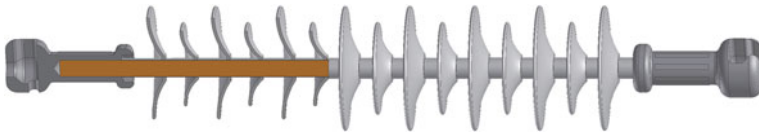


Fig. 2.1 Construction of a composite long rod insulator

Composite long rod insulators benefit from the manufacturing possibilities of the FRP (fibre reinforced plastic) rod, and in particular from the fact that FRP rods can be produced practically endlessly in one piece in lengths of up to 20 m which is not possible with conventional insulator materials such as porcelain and glass. Composite long rod insulators are currently produced in lengths of 10–100 cm for low-voltage and medium-voltage network systems, and in lengths up to 10 m and more for today's UHV network systems (Figs. 2.2 and 2.3).

Composite long rod insulators are primarily used in suspension strings in straight-line supports and as tension strings in anchor towers and dead-end towers (Figs. 2.4 and 2.5). They are also used in the jumpers or portals of outdoor substations. In some cases, composite long rods are used in the guys of wooden poles, and more rarely in the guys of steel towers.

Since, in contrast to porcelain and glass insulators, composite long rod insulators can be formed in one piece up to the highest of voltages, they only weigh a fraction of conventional insulators (at 400 kV they weigh approximately 10 % of a comparable porcelain long rod).

Their use is therefore also favoured in special tower designs such as guyed cross-rope suspension (chainette) towers (Canada, South Africa, Argentina—see Fig. 2.5), or in floating dead-ends to reduce tower height (Fig. 2.6).

With the increasing use of compact lines, even for voltages of 400 kV and above, these insulators can be found as support elements in insulated cross-arms (Fig. 2.7); see also Chap. 4.

Millions of composite long rods have been used for many years at medium-voltage level in a wide range of tower types (wooden, steel, concrete) and for a large number of applications (see Fig. 2.8—straight-line supports, angle towers, dead-end towers, pole mounted transformer stations).

Composite insulators, and in particular their advantages under extreme pollution, have been “discovered” in the meantime by railway operators and are thus also being used increasingly in railway catenary systems.

2.2 Behaviour of Composite Long Rod Insulators Under Mechanical Load

As is evident from the above-mentioned applications of composite long rods, they are subject primarily to tensile load. However, these insulators may also sometimes be subject to torsional load during the line construction and erection process.

Fig. 2.2 Production of a 6 m long composite long rod

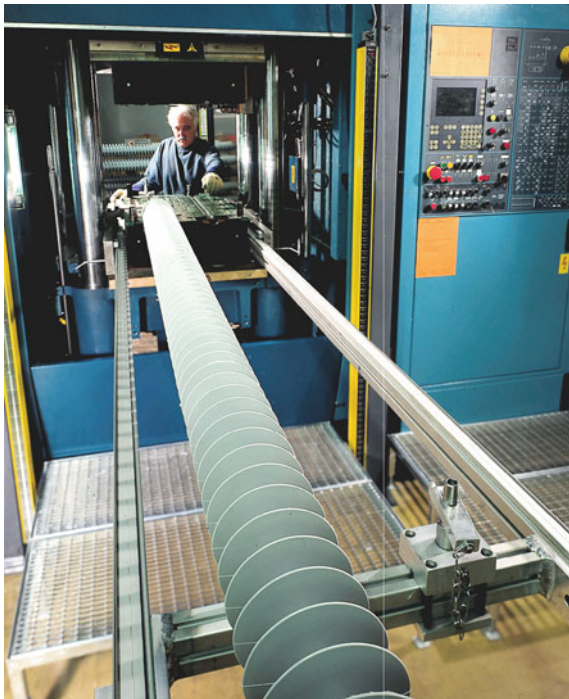
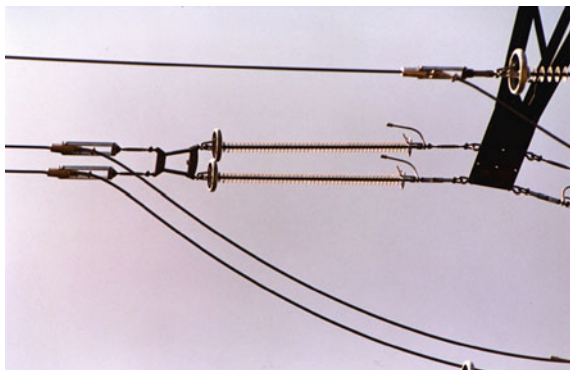


Fig. 2.3 Suspension string



For this reason, this type of load will also be discussed briefly, especially since FRP rods react rather sensitively to torsion. Long-term behaviour, damage mechanisms, and the associated long-term tests will also be discussed. The

Fig. 2.4 Tension string**Fig. 2.5** Cross-rope suspension (chainette)**Fig. 2.6** Floating dead-end

conventional analytical methods used today will then be presented, from the simple formulas for “day-to-day use” to computer-based simulations. The mechanical behaviour of composite long rods is often determined by the quality and application processes of the respective fittings, which will be described in detail. Reference will lastly be made to the necessary mechanical tests, as stipulated in the international standards.

The material for the core rods of the composite insulators concerned in this instance is a fibre reinforced plastic (FRP). Such rods are normally produced by a pultrusion method (see [Chap. 7](#)), that is to say the glass fibres of these rods are oriented uniaxially and in the direction of the rod axis. The rod therefore has different

Fig. 2.7 420 kV insulated cross-arm



Fig. 2.8 20 kV insulators on a wooden tower



Table 2.1 Elasticity constants of the FRP rod

Modulus of elasticity		Shear modulus		Poisson number	
E_L (GPa)	E_T (GPa)	G_{TT} (GPa)	G_{LT} (GPa)	ν_{TT} (–)	ν_{LT} (–)
44	10.3	4.3	5.1	0.5	0.32

material properties in the rod direction and transverse thereto; the rod is said to be “orthotropic” (*orthos* = Greek for perpendicular, *tropos* = Greek for property).

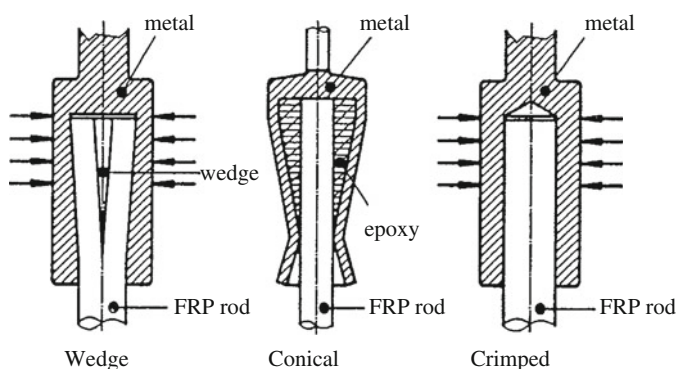
The elasticity constants and the strengths of the rods used in this instance were established by complex tests [1] and are summarised in Tables 2.1 and 2.2.

2.2.1 Long-Term Behaviour of Composite Long Rod Insulators

Despite the many advantages of composite insulators compared to porcelain and glass, it took many years of persuasion, mainly by the manufacturers of composite insulators, before they were widely accepted. In the early years of this technology—i.e. during the 1970s—it was accepted both by the manufacturers and by users

Table 2.2 Strengths of the FRP rod

Strength value	(MPa)
Longitudinal (tension)	1,038
Longitudinal (compression)	−794
Transverse (tension)	32
Transverse (compression)	−141
Longitudinal-transverse (shear)	54

**Fig. 2.9** Different types of fittings of composite long rods

(primarily network operators) that, ultimately, suitable qualification tests were necessary in order to demonstrate both the resistance of the insulation material under adverse ambient conditions (corresponding accelerated tests were developed for this purpose) and the long-term behaviour of the composite insulators under mechanical load. The pioneering work in this field was carried out by Claude de Turreil and his CIGRE working group, and will be presented hereinafter.

De Turreil and his colleagues at IREQ—Institut de Recherche d'Hydro-Québec (Hydro-Québec's Research Institute)—tested insulators provided by three different manufacturers A, B and C under rather complex long-term test conditions (some individual tests lasted up to three years) [2], these insulators differing substantially in terms of the technology of the end fittings: Insulator A uses a conical (potted) fitting, insulator B uses a crimped fitting, and insulator C uses a wedge fitting (Fig. 2.9).

The aim of the first series of tests was to determine the failing load of the insulators as a function of ambient temperature within a range of -25 – 100 °C. The results are shown in Fig. 2.10.

As can be seen, the ultimate tensile stress in all three insulator types increases with decreasing temperature, which is beneficial in particular with their use in cold environments, since the insulators then also have to withstand greater tensile loads as a result of an increase in line tension. It can also be seen that the force-temperature behaviour differs substantially between the three insulator types. Insulators having crimped fittings react less sensitively to changes in temperature, even at low temperatures, at which a negative influence on failing load would be expected as a result of the different coefficients of thermal expansion of the FRP rod and steel.

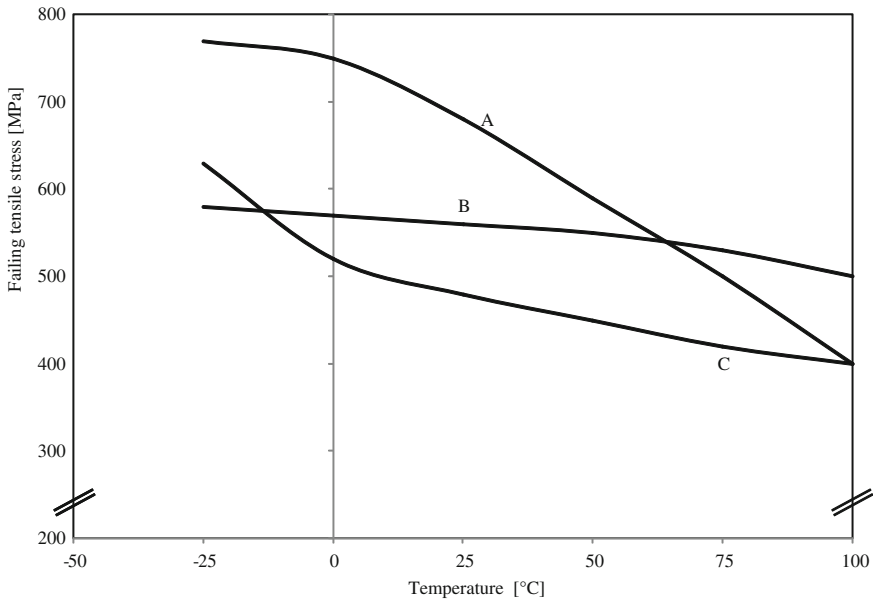


Fig. 2.10 Failing tensile stress of three composite insulators having different fittings as a function of temperature [2]

As more recent CIGRE tests have shown (see Sect. 9.6.3), line temperatures of 200 °C and more which occur in modern high-temperature conductors only have a small effect on the tensile strength of composite insulators.

The aim of the second series of tests was to determine the so-called load-time curve of the insulators. The time until failure under constant tensile load was established in each case. The results of this test are shown in a graph in Fig. 2.11. The insulator used in this instance had crimped fittings, these being the most common fittings used currently. This type of fitting also demonstrated the best long-term behaviour, wherein the relatively broad scattering of the measurement results for a specific load is noticeable and may extend up to three orders of magnitude over the time scale.

It is also interesting that the rate of decline of the load-time curve increases slightly with temperature, as is shown in Fig. 2.12 for insulator B (having crimped fittings), which also behaves better in this regard than insulators A (conical) and C (wedge).

Operational use was simulated in an outdoor test station, where the insulators were subjected to a constant load with simultaneous application of an alternating voltage and climatic influences, the alternating voltage (60 Hz) being approximately 10 % higher than the nominal voltage of the insulators, and the ambient conditions ranging from winter temperatures down to −30 °C, to summer temperatures up to +30 °C, through sunshine, rain, and snow and ice. Although the load-time curve retained its (negative) slope under these conditions (Fig. 2.13), it shifted in parallel “downwards” until at the level of the 50 °C curve (see also Fig. 2.12).

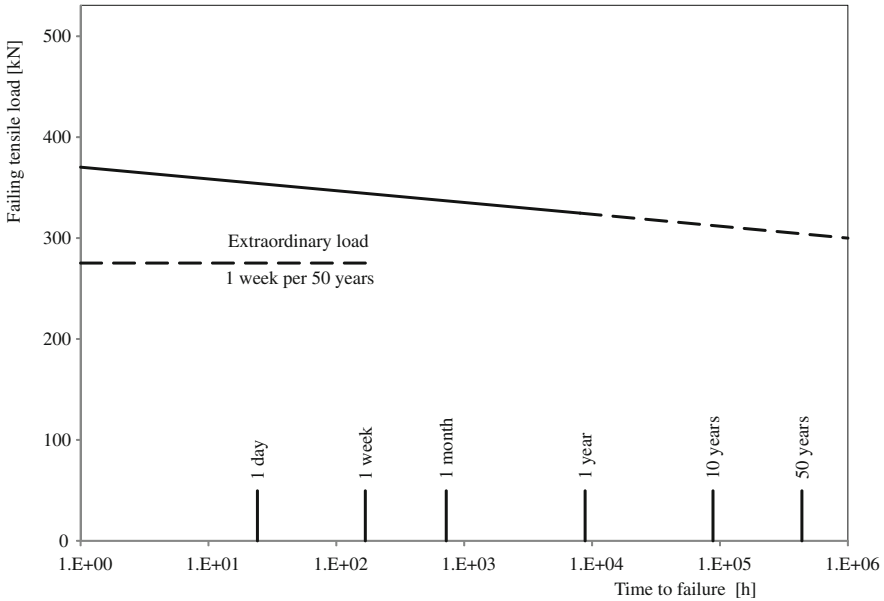


Fig. 2.11 Load-time diagram of an insulator having a crimped fitting at 23 °C [2]

De Tourreil later carried out similar tests [3], but this time with smaller rod diameters (up to 20 mm), the results of which are summarised in Table 2.3. By contrast, the measured load-time curves of all these test specimens are shown in Fig. 2.14, normalised to the respective failing load (100 % value). In these tests, too, the best results were achieved by the insulators having crimped fittings, and in particular the insulator having a swaged fitting.

These ground-breaking tests clearly demonstrated that a composite insulator subjected to constant tensile load (which exceeds a specific threshold value—the damage limit) will fail after a certain period of time, the duration of which depends on the magnitude of the applied load. The failure occurs, however, at a load level which is somewhat lower than the static failing load of the insulator.

This finding is of fundamental importance to the dimensioning of such insulators, since not only the load, but also the duration thereof, should now be considered. This has understandably led to a certain amount of uncertainty among experts in this field, in particular among utility engineers, and further testing was introduced with the aim of better understanding the physical relationships involved. A new test was also introduced into IEC standard 61109/92 (new at that time) on the standardisation of tests for composite insulators, the objective of this test being to ensure that composite insulators would not fail suddenly, even after long periods of load. The first test proposal was unfortunately unrealistic, and has only been replaced in recent years by a more “sensible” test. This development must be borne in mind below [4].

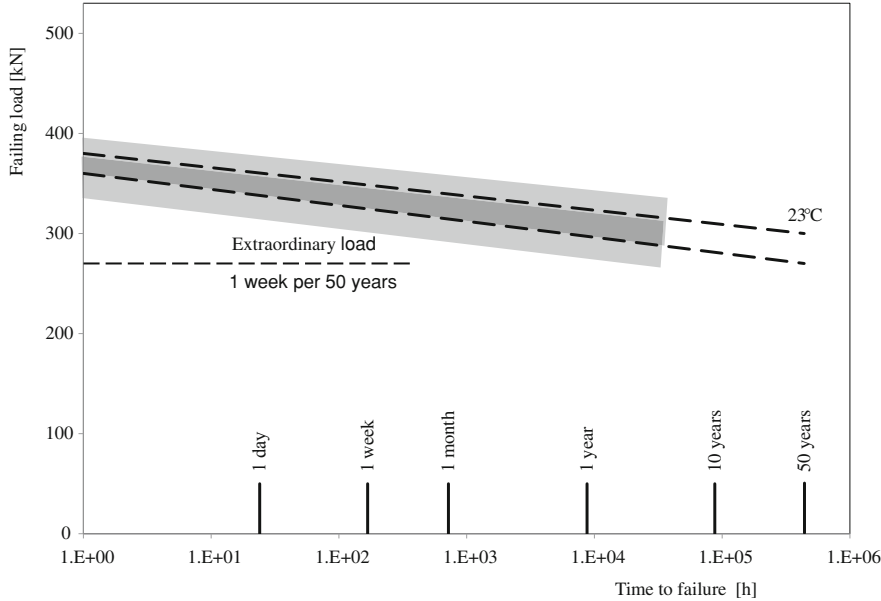


Fig. 2.12 Comparison of the load-time curve for an insulator having a crimped fitting at 23 and 50 °C [2]

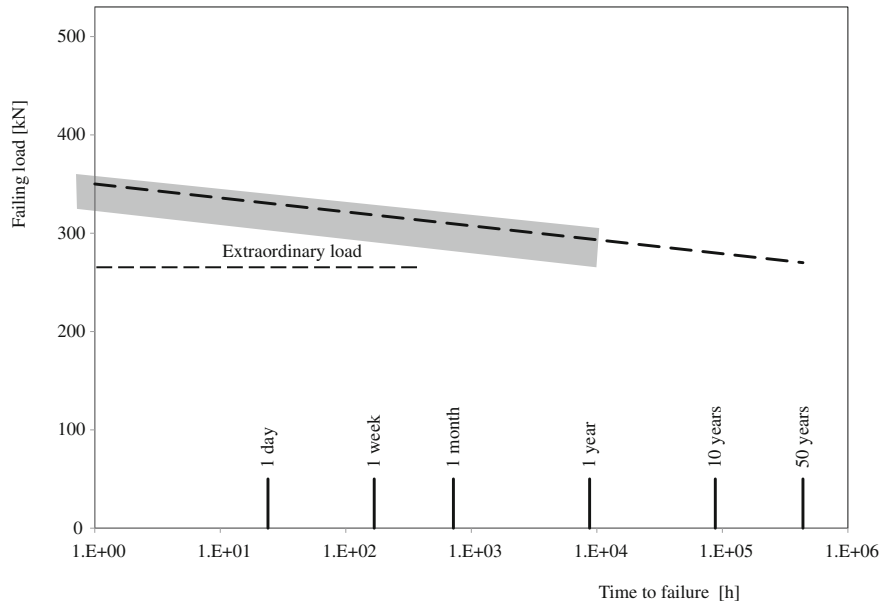
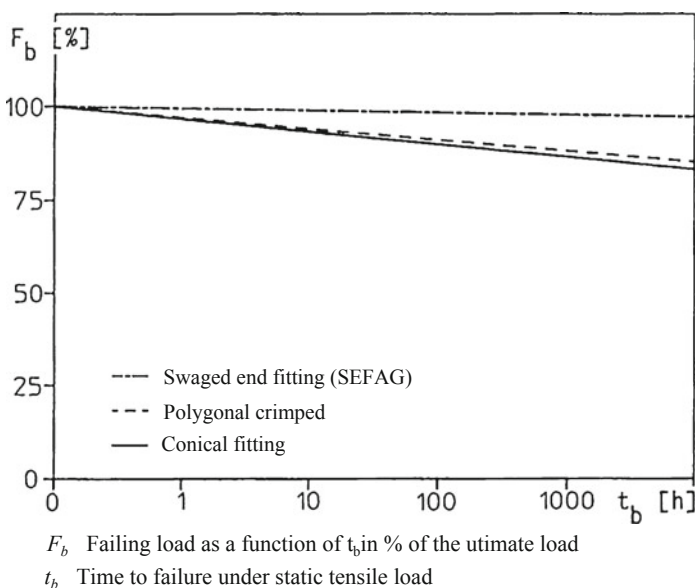


Fig. 2.13 Load-time curve of an insulator having a crimped fitting under mechanical, electrical and climatic load [2]

Table 2.3 Comparison of the failing loads and of the failing stresses of composite long rod insulators having different fittings

Fitting type	Average failing load [kN]	FRP failing stress [MPa]
Conical fitting	176 ± 6	823
Polygonal crimped fitting	219 ± 1	796
Coaxial fitting	338 ± 4	1,076

**Fig. 2.14** Average load-time curves of composite long rods having different fittings, normalised to the respective failing load (see also Table 2.3)

2.2.1.1 Load-Time Curve of Composite Insulators According to IEC 61109/92

In principle, three tests are provided in the “old” IEC 61109/1992 to ascertain the mechanical strength of composite insulators: Annex A details the long-term behaviour of composite insulators within the scope of a *design test*, and describes the associated test procedures. It is also noted that the tensile strength of composite insulators decreases over time; this decrease can be assumed to be linear with the duration of load application.

This is shown in a graph by the straight line *a* in Fig. 2.15. The value F_{Br} represents the average *failing load* of three test specimens established in the tensile test (1 min test). The standard also requires the slope of the straight line *a* to be 8 % at most per decade. This is to be checked by a 96 h test of a further three test specimens at 60 % of the previously determined average failing load F_{Br} , as indicated in Fig. 2.15.

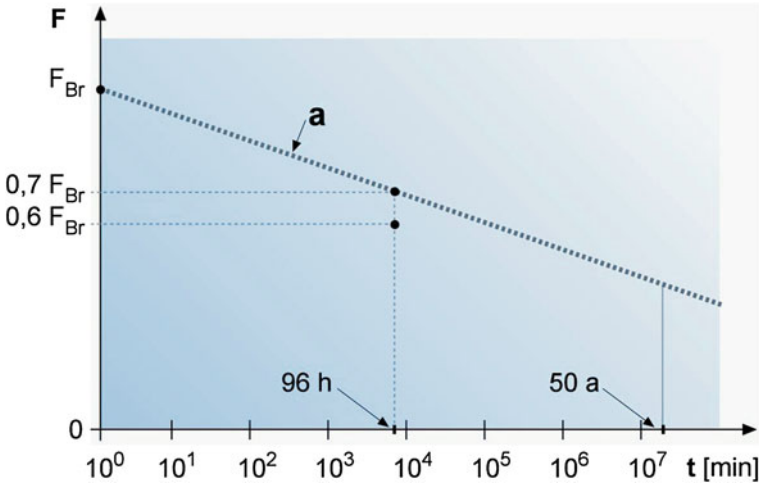


Fig. 2.15 Determining of the load-time curve “a” of a composite insulator according to the “old” IEC 61109/92

The “specified mechanical load” (*SML*) is also introduced in the standard. It is defined as the load which, after a 96 h test at 70 % of the specified mechanical load of the insulator (specified by the manufacturer), is retained in a subsequent 1 min tensile test, and is to be determined in the *type test* (Fig. 2.16). Lastly, a *routine test* at *routine mechanical load* (*RML*) is provided, which is 50 % of the specified mechanical load and has to be demonstrated in all insulators during the tensile test prior to delivery.

As important as it was to demonstrate this specific feature of the mechanical long-term behaviour of composite insulators when this standard was created, its depiction irritated users and often led to redundant over dimensioning. There are two main reasons for this:

Firstly, the linear fall of the withstand load curve in the standard suggests that this failing load of a composite insulator decreases continuously over time. In theory it is often ignored that this curve only indicates the service life until failure of a composite insulator when the insulator is loaded *continuously* by a specific tensile load. Secondly, the undefined linear fall of the curve implies that the tensile strength of the composite insulator would fall practically to zero after a finite, even if rather long, operational period, which leads to the absurd conclusion that a composite insulator which has been stored away and “forgotten” will break into two parts at some point in the future.

The fact that this cannot be true is confirmed not only by sound engineering expertise, but has also been proven by the experimental findings and physical considerations, as detailed hereinafter.

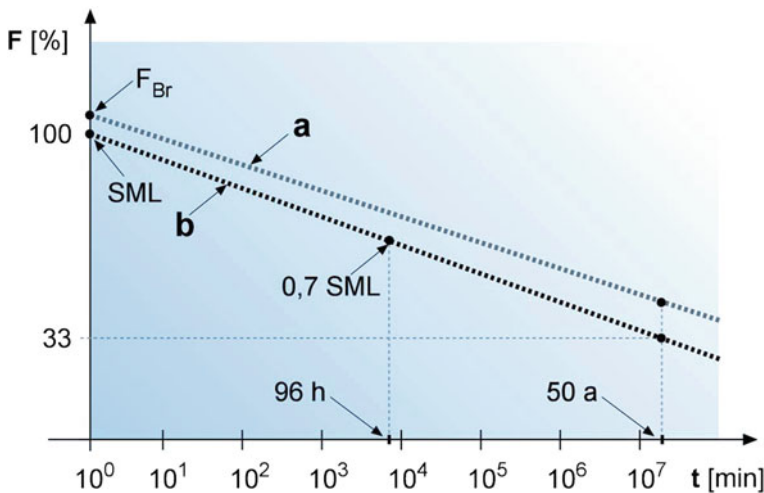


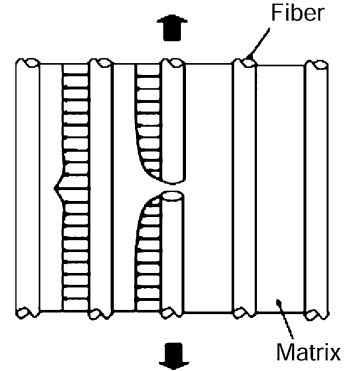
Fig. 2.16 Determining the specified mechanical load (SML) of a composite long rod insulator

2.2.1.2 Mechanical Model for the Long-Term Behaviour of Composite Insulators

The mechanical behaviour, and in particular the time-dependent failure mechanism, of an FRP rod under continued tensile load can be explained qualitatively as follows: Since the resin matrix of the rod can stretch considerably, but in contrast the glass fibres are hardly able to undergo plastic deformation (brittle material), the load is taken over practically completely by the glass fibres within a very short time after application of the external tensile load. It must be taken into account that the properties of the glass fibres vary widely in a random manner. The approximately 800,000 individual glass fibres in the cross-section of an FRP rod of 16 mm diameter not only have different tensile strengths. The fibre cross-section F , and to a certain extent the modulus of elasticity E of the fibres also vary from fibre to fibre, the overall tensile load acting on the FRP rod thus being distributed over the individual fibres in accordance with the tensile stiffnesses DS of said fibres ($DS = E \cdot F$)—that is to say the greater the stiffness of a fibre, the more load it must bear, and since, as mentioned, the tensile strength of the fibre is subject to a certain level of random scattering, some weaker glass fibres will break.

The time-dependent failure mechanism of the FRP rod, which is particularly interesting in this instance, is based on the load transfer from the broken fibres to adjacent fibres (Fig. 2.17). This occurs in such a way that when, for whatever reason, a glass fibre breaks, the resin matrix enveloping the glass fibre transfers the axial load to the cylindrical surface of the broken fibre via the distribution of shear stress, this break therefore going “unnoticed” at said cylindrical surface from a certain distance from the area of the break. Secondly, the matrix transfers the

Fig. 2.17 Load transfer processes in the fibres of an FRP rod under axial load



original load from the broken fibre to the healthy adjacent fibres, which are thus loaded increasingly, which in turn results in these adjacent fibres being stressed to a greater extent at the point of break of the broken fibre. It has been attempted to illustrate this physical process schematically in Fig. 2.17.

However, since the adhesion between glass fibres and resin matrix is subject to relaxation, this leads to a decrease in the “load transfer capability” of the fibre matrix composite over time, especially as the interface between fibre and resin matrix is loaded. The load which could still be borne by the broken fibres is increasingly transferred completely to the healthy fibres, which will also fail by the same mechanism after a certain period of time until all fibres, and therefore the FRP rod, have broken. It makes sense that the higher the load, the more fibres will break over a rather short period of time and the quicker the described relaxation processes will occur. However, if the overall initial load lies below a certain threshold value, for example the limit value for the first fibre breakages (*damage limit load* or *DLL*), then hardly any fibres will break on the basis of this model, even if the load is applied to the insulator for an infinite period of time. Conservative estimations indicate that this damage limit load can be set at least at 50 % of the specified mechanical load. These considerations are only true for failure of the rod, without consideration of the fitting.

This model was confirmed by a number of independent tests which showed that the permanent load curve of suitably designed composite insulators is not linear [5], contrary to the illustration in Fig. 2.16. It can be seen that the measured curves fall relatively steeply at the start, but asymptotically approach a lower threshold value of the damage limit load after a relatively long period of time. This means that a composite insulator retains a significant proportion of its original failing load F_{Br} for an “infinite” period of time.

For example, the results of a number of tests are plotted together in Fig. 2.18, these tests having been carried out by IREQ in the 1980s. In this case, F is the failing load normalised to the 1 min value, and t is the time until failure in minutes (plotted logarithmically); the dashed straight line indicates the DLL (damage limit load) at 60 %.

These assumptions have also been confirmed by practical experience. De Tourreil [6] reports that he measured the 1 min average failing load (M_{av}) on

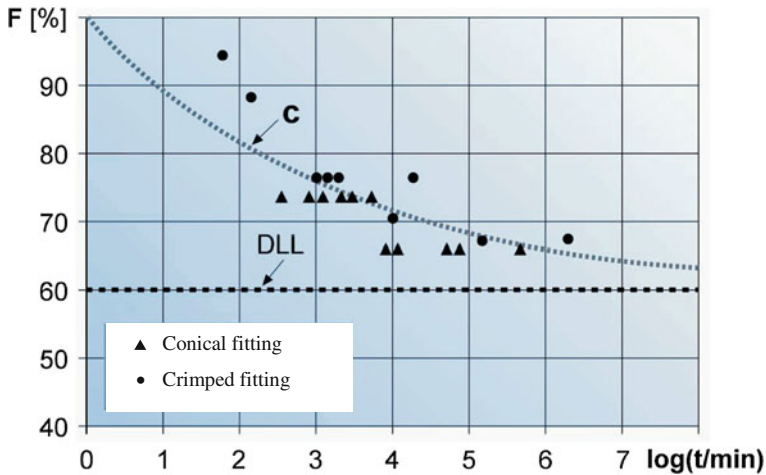


Fig. 2.18 IREQ test results of static long-term behaviour of insulators from various manufacturers

composite insulators which had been in operation for 12 years. No significant difference from the failing load of the same insulator family when new could be established. Later results [7] of mechanical tests on 132 and 400 kV insulators after more than 20 years in operation also confirmed these findings.

The improved understanding of the physical processes during tensile loading of composite insulators as well as the extensive tests and positive operational experience were taken into account during the standardisation and resulted in the design and type tests in IEC 61109, Ed 02 being adapted accordingly. The new edition of IEC 61109 (2008) requires (Fig. 2.19):

- a 96 h test as a design test to determine the “position” of the load-time curve of the insulator (value pair D1 and D2).
- a limit load test as a type test to establish the limit load once the insulator has been loaded for 96 h by a constant load of 0.7 SML (value pair T1 and T2).

The design test establishes M_{av} ($av = \text{average failing load of the assembled core}$) and thus the starting point of the actual load-time curve of the insulator, and also the minimal limit load below which, according to the statements above, no mechanical damage to the insulator should occur, and does so as a result of a long-time test in which a load of $0.60 M_{av}$ has to be maintained over a period of 96 h, the choice of this test parameter being clarified below.

Taking into account the practical capabilities of testing laboratories, the test period of 96 h was selected because it lies in the middle of the logarithmic time scale of 1 min to 50 years. On the other hand, the load was defined by $60 \% M_{av}$, because, in the case of three test specimens which passed this 96 h test at $0.60 M_{av}$, there is a 90 % probability that the average failing load at 96 h will be at least $0.70 M_{av}$. This results from the assumed Gaussian distribution for three test specimens:

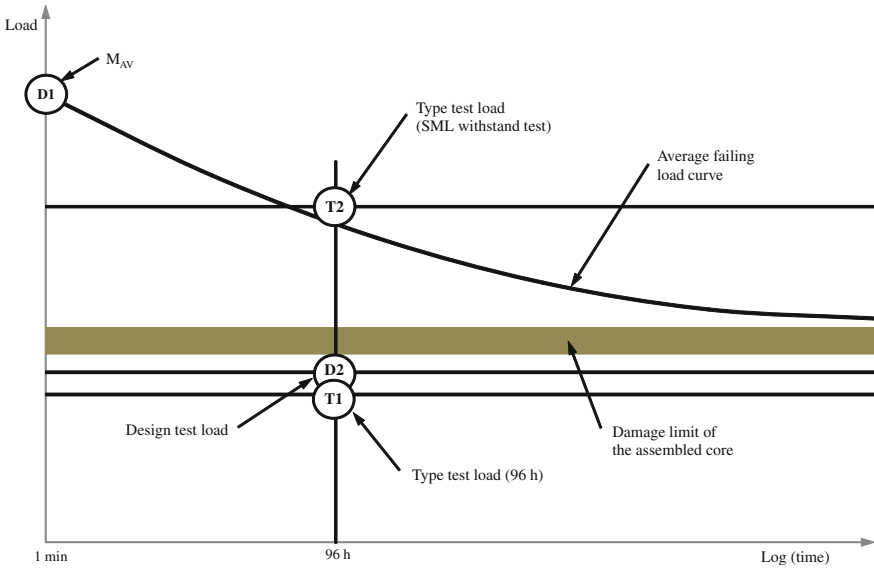


Fig. 2.19 Test loads according to the “new” IEC 61109, Ed O2 (2008)

$$M_{96} - 1.820\sigma_M$$

where M_{96} is the average 96 h failing load and σ_M is the standard deviation.

If the rather conservative value for the standard deviation of $\sigma_M = 0.08$ is used, it follows that

$$0.7 \cdot M_{av}(1 - 1.820 \cdot 0.08) = 0.60 \cdot M_{av}$$

thus justifying the assumption of $0.60 M_{av}$.

However, the standard cited above [IEC 61109, Ed. 2, 2008] allows the use of the “real” standard deviation, as established from tensile tests, in order to establish the 96 h withstand load if more than ten such tests have been carried out.

It should be noted in this regard that, when tested, modern composite insulators provided by well-known manufacturers should have standard deviations for their failing load of no more than 5 %.

2.3 Behaviour of Composite Long Rod Insulators Under Dynamic Load

Outdoor composite insulators are subject to the same loads, in particular the same dynamic loads, as the other components of an overhead transmission line. Loads caused by wind-induced vibrations, such as Aeolian vibrations, sub-span oscillations and galloping are the main dynamic loads. These types of vibration are

described in detail in [8], for example. The behaviour of these insulators under dynamic load will now be described below in greater detail.

In early experiments carried out by IREQ the insulator load was simulated for galloping [3]. According to estimations at that time, a conductor which “gallops” could load the insulator with a pulsed tension of ± 15 kN at a frequency of 0.5 Hz. These parameters were also selected for testing the insulators with pulsed tension loads. It is important to note that the static tensile load (average tension) was selected in such a way that the time until failure of an insulator was within the time window available for the tests (100 h at most). Unrealistically high static tensile loads (compared to the tensile loads experienced by an insulator during operation) were thus created, and these tests are thus better suited to providing a benchmark between different insulators and different fitting technologies.

Figure 2.20 shows the four tested insulators. Insulators A and D1 use a conical fitting, which was conventional at the time (see also Fig. 2.9), whereas insulators B and D2 use a crimped fitting, which is more common nowadays.

The FRP rods failed in all cases, the breakages starting at the transition to the metal fitting. The breakages typically started on the surface of the rod in insulators A and D1, and propagated to the centre of the rod at an angle of 45° to the rod axis with subsequent delamination along the glass fibres. Insulators B and D2 failed either as a result of the rod slipping out of the metal fitting (pull-out) or as a result of extensive delamination.

Rod breakages at an angle of 45° to the rod axis (as in A and D1) were observed less frequently. These different breakage patterns have since been attributed to and explained by the type of load applied to the rod by the metal fitting (see Sect. 2.4.7.4 below, Fig. 2.47).

The test results are illustrated in Fig. 2.21. It can be seen that the design and method of crimping are very important for the behaviour of such insulators under pulsed tensile load. Whereas insulators A and D1, both with a conical fitting, exhibit a similar decline in their failing load after 100 h (compared to their static failing load) of approximately 18–20 %, insulators B and D2, both with crimped fittings but using different crimping techniques, demonstrate considerable differences in this regard. Also insulator B shows a prominent fall of 43 %, insulator D2 “gets by” with “only” 25 %. It is worth mentioning that the same insulator D2 also achieved the best results in the above-described load-time tests (see also Fig. 2.14 and Table 2.3).

The behaviour of composite long rods under pulsed load, as may occur as a result of Aeolian vibrations, has also been tested [9]. The test rig can be seen in Fig. 2.22 during the testing of a 220 kV insulator.

The pulsed tension test started at a maximum pulsed load approximately equal to the SML, which had been determined previously on insulators of similar design. The pulsed load was reduced until failure no longer occurred after 2 million load cycles (assumed to be realistic for service conditions). Figure 2.23 summarises the test results of insulators provided by four different manufacturers (referred to here anonymously by A, B, C and D), the number of load cycles until failure being plotted on the X-axis and, for improved comparison, the ratio of $F_{\max}/SML$ (in %) being plotted on the Y-axis.

Fig. 2.20 Insulators for the dynamic tests [3]

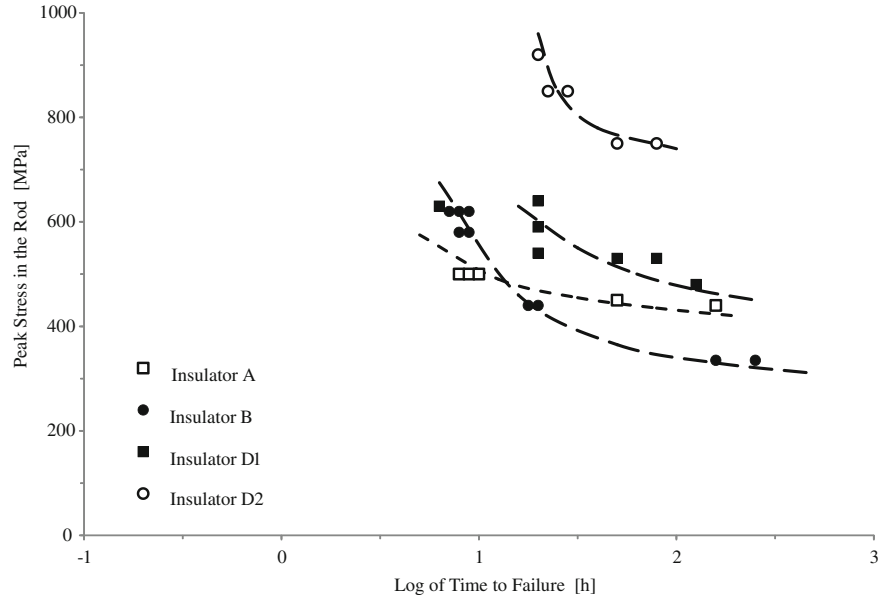
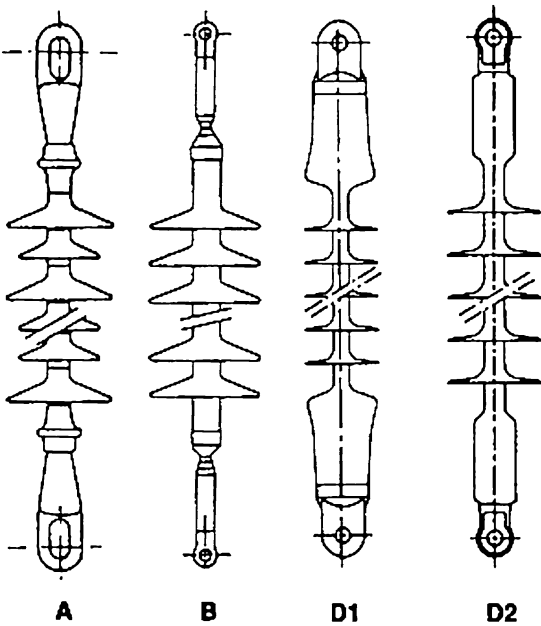


Fig. 2.21 Ultimate pulsed tension stress over time to failure during the dynamic tests [3]

Fig. 2.22 Test station during testing of a 220 kV composite insulator [9]

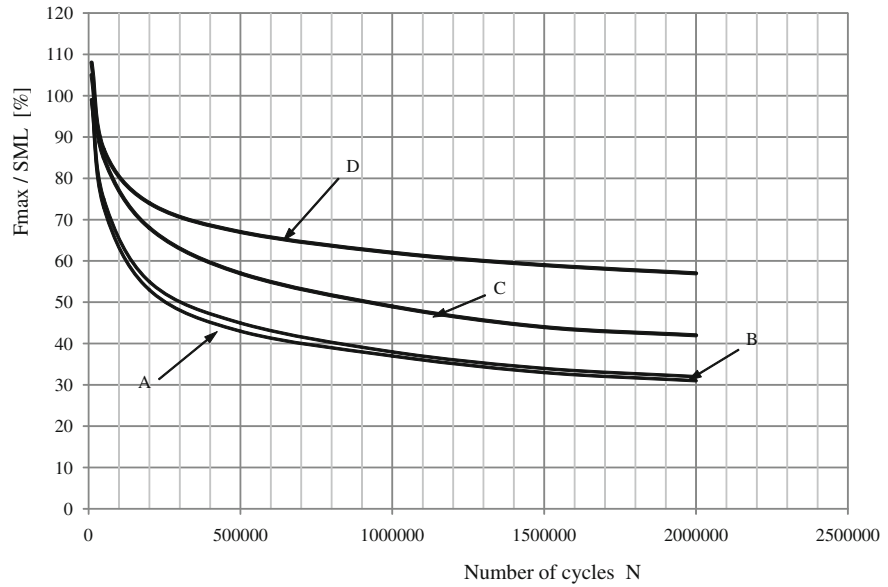
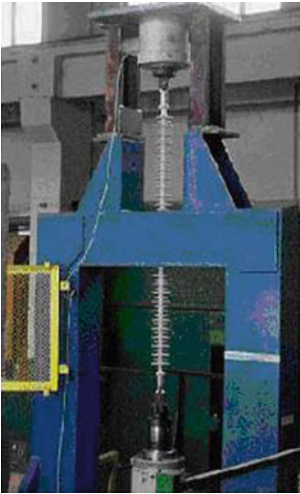


Fig. 2.23 S-N curve of composite long rods [9]

This illustration is known as the S–N curve.

Cracks were observed over the entire surface of the rod, and not just in the areas bordering the metal fitting (Fig. 2.24). The damage to the 25 mm FRP rod of a 400 kV insulator in the vicinity of the upper fitting can be seen (SML 210 kN, test frequency 7 Hz, test load approximately $60 \% \times SML$, number cycles until failure 1.5 million).

It should be noted however that, in accordance with estimations based on [10], the pulsed loads applied during these tests are probably higher than the pulsed loads which would be experienced by the corresponding conductor before fatigue failure. This means that, under such extreme loads, the conductor would fail first. Because of that, conductor vibrations causing similar loads are usually dampened sufficiently in practice [11].

Tests carried out on 120 kN composite long rods having ball-and-socket fittings lead to similar results [12]. In these tests, insulators of similar type but with reinforced end fittings were additionally tested to establish the fatigue properties of the FRP rod itself. The fatigue values of the FRP rod are greater than those of the ball fittings, which failed earlier as a result of the notch effect. In these tests, too, a flattening of the S–N curve of the insulator was observed from approximately 2 million cycles (Fig. 2.25).

This means that the fatigue behaviour of the metal fittings may be crucial under pulsed load, as could occur in the event of wind-induced vibrations.

2.4 Design and Assembly of End Fittings for Composite Long Rods

2.4.1 *Development and State of the Art Technology of Metal Fittings*

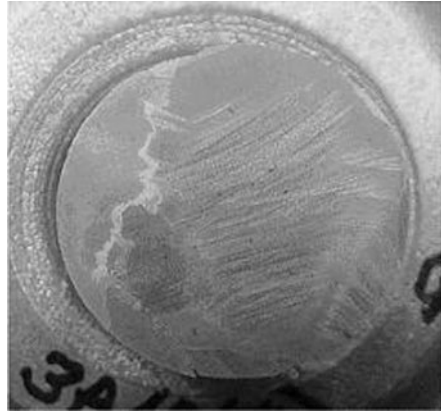
Composite insulators are loaded by rather high tensile loads of up to 500 kN, and more in special cases. These forces have to be transferred both to the tower and to the conductor. This is achieved with the aid of end fittings, which have to be attached to the FRP rod of the insulator in a suitable manner.

Since this type of insulator has to withstand high mechanical loads, its fittings are made of steel (generally hot-dip galvanised) or ductile cast iron. Aluminium is occasionally used if there is a need to reduce weight (railway applications), albeit for rather smaller loads (up to approximately 100 kN). Bronze is used in some special cases where the ambient conditions are extremely corrosive. Typical fitting types for composite long rod insulators are as in Fig. 2.26 from left to right: socket, tongue, clevis, ball, eye and Y-clevis.

The international standards IEC 61466: “Standard Strength Classes and End Fittings” define the dimension and other parameters for these fittings.

The insulator manufacturers have developed different assembly methods over the years for assembly of the end fittings on the FRP rod. The fittings were originally potted to the FRP rod, then in following generations of composite insulators the fittings were wedged in place, and in recent years the fittings have almost exclusively been crimped (see also Fig. 2.9). This type of fitting will therefore be described in greater detail hereinafter.

Fig. 2.24 Damage to the FRP rod [9]



2.4.2 Basic Considerations Regarding the Design of Crimped Fittings

As already mentioned, practically all composite long rod insulators now use crimped fittings. Whether cast or forged, they are characterised by low manufacturing costs, in particular if they can be manufactured in bulk. They are applied to the FRP rod using commercially available crimping equipment. Fittings and crimping methods have been constantly optimised over the years by the insulator manufacturers. The main parameters of the fittings will be defined and explained before discussing the mathematical considerations when designing the fittings.

In Fig. 2.27 the FRP rod and the end fitting are illustrated schematically together with all the dimensions relevant for the crimp [13]. Since practically all dimensions and material properties influence the quality of the crimp to a greater or lesser extent, a large number of tests have to be carried out in order to establish the relationships between said dimensions and properties and the quality of the crimp.

The main information obtained from these tests is summarised as follows:

1. Influence of the crimp length L : It has been found that the failing load of a composite insulator increases approximately proportionally with the crimp length L of the metal fitting, that is to say the longer the fitting, the greater the tensile load which can be withstood by the insulator. In any case, the crimp length should not exceed a certain length, because the electrical values of the insulator might then not be met and, as is known from experience, extremely long crimps do not necessarily result in an proportional increase of the insulator failing load.
2. Influence of the geometry parameters x , y and z : These parameters have a considerable influence on the crimp. For example, a well designed length x reduces the concentration of stress at the opening in the fitting, which in turn has a favourable effect on the failing load M_{av} of the insulator, as can be seen in

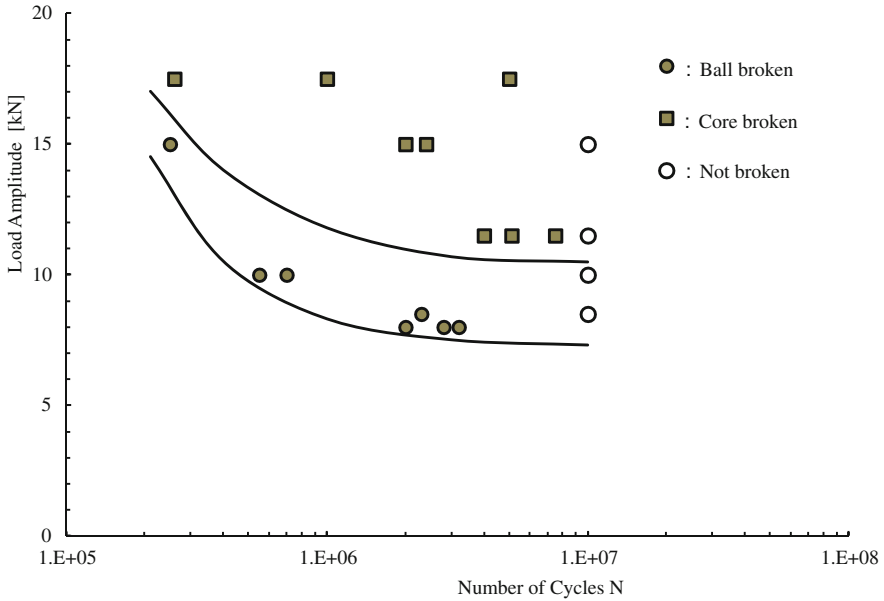


Fig. 2.25 Fatigue properties of 120 kN composite long rods [12]



Fig. 2.26 Typical end fittings for composite long rod insulators

Fig. 2.28. It can be also seen that the failing load M_{av} is considerably reduced below a critical length x_{cr} . On the other hand there is a certain limit x_{max} , above which there is no significant increase in M_{av} .

3. Influence of the wall thickness of the end fitting b : This is strongly related to the material properties of the end fitting. In the case of a high plastic limit of the fitting, the duration of the crimping process, i.e. the application of the pressure load, increases with the wall thickness b of the end fitting.
4. Influence of the roughness of the inner surface of the fitting: Many tests have demonstrated that a smooth inner surface reduces the failing load of the insulator, since in this case the coefficient of friction between the metal fitting and the FRP rod, which is decisive for the damage limit load of the insulator, is also reduced considerably. If, by contrast, the inner surface of the fitting is very rough, the asperities of this surface will damage the surface of the FRP rod,

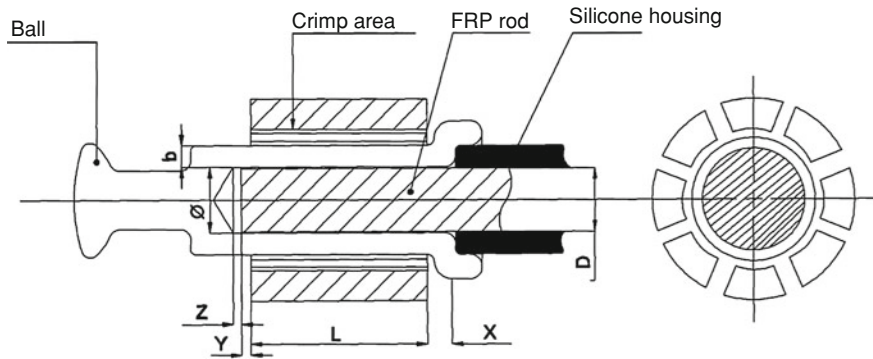


Fig. 2.27 Critical geometrical parameters of the end fitting [13]

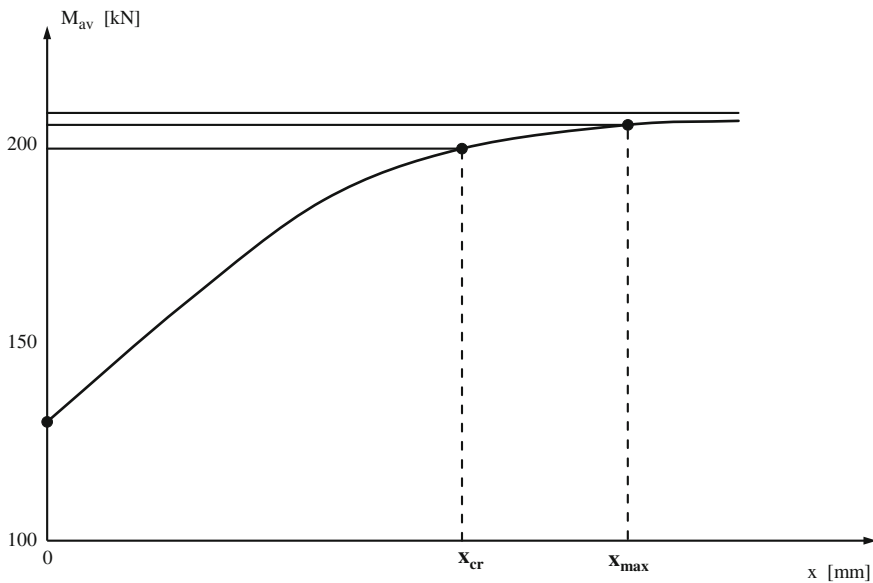


Fig. 2.28 Dependence of M_{av} over x (schematic view) [13]

which will in turn have a negative influence on the damage limit load of the insulator.

5. Influence of the tolerance between the metal fitting and the FRP rod: Sufficient crimping (and thus sufficient damage limit load of the insulator) can be generated by the suitable selection of this tolerance, it being necessary to adjust the crimping parameters accordingly (for example peak value and time dependence of the applied contact pressure).
6. Influence of the material properties of the fitting: As already mentioned above (point 3), the stress–strain curve of the fitting material, and in particular the plastic limit of the fitting material, play a key role during the crimping process.

7. Influence of the material properties of the FRP rod: The modulus of elasticity transverse to the rod axis also plays an important role for production of an effective crimp (Table 2.1). The ultimate strength of the FRP rod should also be taken into account during the crimping process, since a low ultimate strain transverse to the rod axis limits the possible deformation of the metal fitting during the crimping process (Table 2.1).

2.4.3 Assembly of Crimped Fittings

As mentioned at the outset, the crimping technique is now the preferred method for fixing the end fittings to the FRP rod. With this technique, the FRP rod is inserted into the end fitting, which is then crimped onto the rod by applying a radial external pressure, normally using a set of eight crimping jaws.

The crimping jaws are driven either by individual hydraulic cylinders, or by a central cylinder and a sliding system of deflecting cams. These crimping jaws have socket fittings for different crimping tools, which can be interchanged quickly and easily so that fittings of different sizes can be crimped using the same press. The press assembly is shown in Fig. 2.29.

This method leads to a relatively homogeneous distribution of stress and deformation in the fitting and in the FRP rod, since the radial pressure applied to the fitting by the hydraulic tool can also be assumed to be homogeneous over the circumference of the fitting. Although this method calls for relatively narrow tolerances for the dimensions of the metal fitting and of the FRP rod as well as for the roughness of the inner surface of the fitting, it is possible to compensate for small deviations since contact pressure and its time path during the crimping process are monitored as abort criteria. For long fittings, which require high contact pressures, two short crimping areas are provided instead of one long crimping area, since the maximum permissible forces per crimping jaw are fixed by the dimensions of the crimping tools.

2.4.4 Calculations

The calculation of the mechanical stresses in the FRP metal-composite joint of a composite insulator is certainly not simple, since the different basic material behaviours of metal and FRP have to be taken into account and the interface between metal and FRP has to be modelled as realistically as possible.

As is so often the case in practice, the calculation methods develop over time with increasing experience. For the purposes of solving the problem addressed in this instance, a distinction is made between simple and complex analytical calculation methods and numerical simulation methods. The analytical methods are intuitive and can be implemented relatively easily. They are based on practical experience and are regularly checked against the test results. The numerical

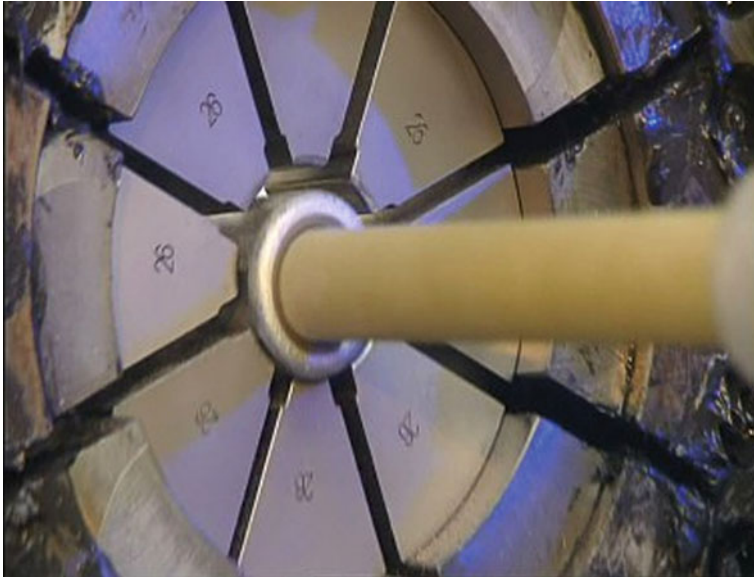


Fig. 2.29 Details of the hydraulic press for crimping of the fitting

methods use the finite element method (FEM) exclusively. They were developed to obtain a better understanding of the stress distribution and failure mechanisms of the FRP rod in particular. They are also used if special applications, such as those involving extremely high tensile loads, are to be designed.

2.4.5 Simple Analytical Method

In the case of the composite long rods, the simple analytical method is primarily used:

1. To establish the FRP rod diameter necessary to achieve the required specified mechanical load (SML) and to pass the relevant tests.
2. To ensure that the mechanical fittings withstand the limit load.

The first objective is achieved by setting a maximum sustainable shear stress of the FRP rod of approximately $\sigma_{shear}^{\max} = 40 \text{ N/mm}^2$. This shear stress is assumed to be constant over the crimping area, that is to say over the interface between the FRP rod and the metal fitting. If d is the rod diameter and L_{cr} is the crimp length, the specified mechanical load (SML) is given as follows:

$$SML < \pi \cdot d \cdot L_{cr} \cdot \sigma_{shear}^{\max}$$

The second objective ensures that the metal fitting will withstand the necessary specified mechanical load (SML). There are two critical zones in a crimped end fitting which have to be considered:

- (a) The cross-sectional area of the fitting (hollow cylinder), Fig 2.30.
- (b) The transition zone from the crimped part to the connection part of the fitting.

With regard to (a):

The tensile stress in the cross-sectional area of the fitting is:

$$\sigma_z = \frac{SML}{\pi(D_a^2 - D_i^2)/4} < \sigma_{zul}$$

where D_a is the outer diameter of the fitting, D_i is the inner diameter of the fitting and σ_{zul} is the tensile strength of the fitting.

With regard to (b):

The dimensions of the transition zones can be seen in the drawing of the end fitting (Fig. 2.30). The hatched areas are subject to shear load and should also withstand the specified mechanical load (SML), with τ_{zul} being the max. Permissible shear stress of the fitting, that is to say:

$$\tau = \frac{SML}{\pi \cdot D_i \cdot \ell} < \tau_{zul}$$

It is not necessary to check the connection element of the fitting (ball, clevis, tongue, etc.) if the standard dimensions specified in IEC 61466 have been used for these parts.

2.4.6 Complex Analytical Method

Such calculation methods are useful since they can be incorporated into the normal software tools used, including table calculation programs, and since, due to their analytical formalism they make it possible to better identify the relationships between the individual geometrical and material values and their effects on the state of stress of the insulator. The methods presented below were carried out within the scope of a research project [1], in which the authors took part in their capacity as representatives of the industry partner.

In the case of crimped fittings, there are three different phases which have to be examined:

1. Crimping: In this phase a pressure is applied to the interface between the FRP rod and the metal fitting by a predefined pressure at the surface of the metal fitting (Fig. 2.31).

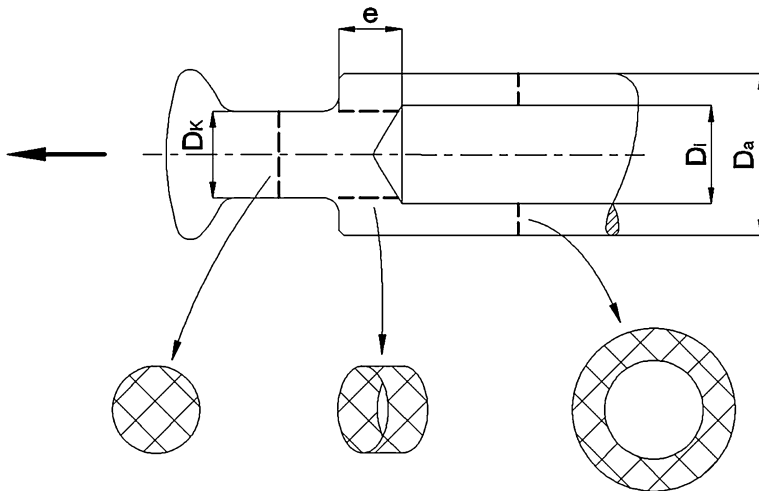


Fig. 2.30 Critical cross-sections when dimensioning an end fitting

2. Relaxation: In this phase the external pressure is removed and the metal fitting is partly relieved of its elastic deformation; the remaining plastic deformation in the metal fitting still exerts a pressure on the FRP rod, which ultimately ensures that the joint between the FRP rod and the metal fitting withstands the external tensile loads (Fig. 2.32).
3. Tensile load: This is the phase in which an external load is applied to the insulator and is increased until the rod slides out from the insulator (pull-out).

2.4.6.1 Crimping

As mentioned above, a radial force is applied to the metal-composite joint during the crimping phase, and therefore the pressure is distributed as uniformly as possible over the outer circumference of this joint.

The external radial pressure p acts on the FRP rod similarly to a hydrostatic pressure. It causes a reduction ΔR from the radius of the FRP rod. This results in a radial σ_{rr} and circumferential stress $\sigma_{\theta\theta}$ of equal magnitude in the FRP rod, and also an extension of the rod in the axial direction owing to the Poisson effect, which in turn leads to a shear stress τ at the metal/FRP interface (Fig. 2.33).

Hooke's law in cylindrical coordinates is as follows:

$$\frac{\Delta R(z)}{R} E_T = \sigma_{rr}(z) - \nu_{TT} \sigma_{\theta\theta}(z)$$

which, taking into account that $\sigma_{rr}(z) = \sigma_{\theta\theta}(z)$ can be rewritten as follows:

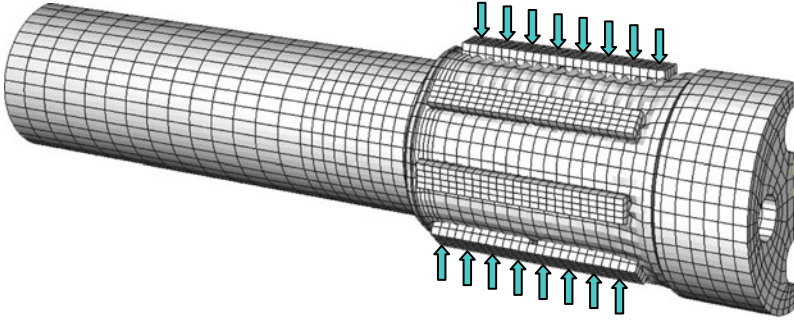


Fig. 2.31 Crimping (schematic view)

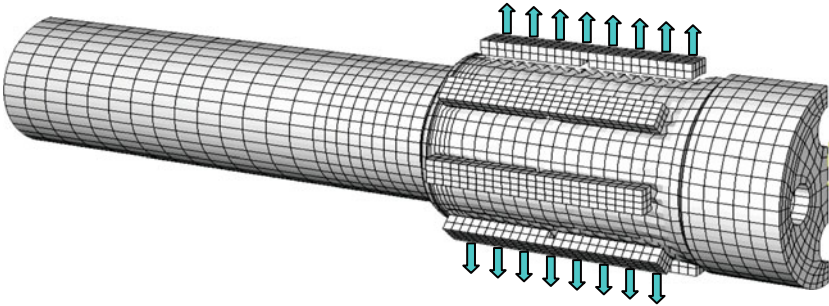


Fig. 2.32 Relaxation (schematic view)

$$\sigma_{rr}(z) = \sigma_{\theta\theta}(z) = \frac{\Delta R(z)}{R} \frac{E_T}{1 - \nu_{TT}}$$

where E_T is the transverse modulus of elasticity and ν_{TT} is the “transverse–transverse” Poisson number of the FRP rod (see Table 2.1).

As a result of the Poisson effect an axial-plastic deformation, which transfers an elastic-longitudinal deformation to the FRP rod via the frictional forces at the metal/FRP interface, also occurs in the metal fitting during the crimping process in addition to the radial deformation.

Owing to the symmetry of the arrangement, this deformation starts at the “centre” of the metal fitting and creates shear stresses τ_f which behave linearly at an interval $(-\delta, +\delta)$ until they reach the threshold value $\tau_{rz,\max}$ set by the FRP material (Fig. 2.34). To simplify matters, $\delta = 0$ is assumed in the analytical calculation, the progression of shear stress over the length of the fitting appearing to be stepped.

This distribution of shear stress over the surface of the rod causes an axial stress in the rod. This can be calculated as follows from the equilibrium of forces in a differential rod element of length dz (Fig. 2.35):

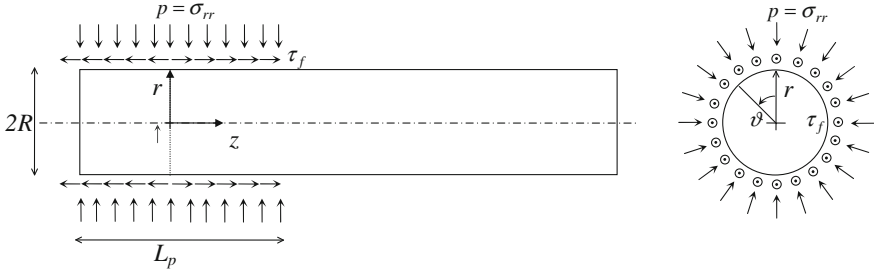


Fig. 2.33 Geometry and loading of the FRP rod during the crimping process [1]

$$\sum F : F_z(z) + \left(\frac{dF_z}{dz} \right) dz - \tau_f(z) \cdot 2\pi R \cdot dz - F_z(z) = 0$$

from which it follows that:

$$\tau_f(z) = \frac{dF_z}{dz} \cdot \frac{1}{2\pi R}$$

The tensile load in the rod $F_z(z)$, under consideration of the boundary conditions $F_z(0) = 0$ and $F_z(L_p) = 0$ and the progression of shear stress $\tau_f(z)$, according to Fig. 2.34 to give:

$$\begin{aligned} F_z(z) &= F_z(z) - F_z(0) = \int_{F_z(0)}^{F_z(z)} dF_z = 2\pi R \int_0^z \tau_f(z) \cdot dz = 2\pi R \cdot \int_0^z (-\tau_{\max}) \cdot dz \\ &= -2\pi R \cdot \tau_{\max} \cdot z \end{aligned}$$

for the range $0 \leq z \leq L_p/2$, and:

$$\begin{aligned} F_z(z) &= F_z(L_p/2) - F_z(z) = \int_{F_z(z)}^{F_z(L_p/2)} dF_z = 2\pi R \int_z^{L_p/2} \tau_f(z) \cdot dz \\ &= 2\pi R \cdot \int_z^{L_p/2} \tau_{\max} \cdot dz = 2\pi R \cdot \tau_{\max} \cdot \left(\frac{L_p}{2} - z \right) \end{aligned}$$

for the range $L_p/2 \leq z \leq L_p$.

On the other hand, the tensile load $F_z(z)$ can also be established from the integral of the axial stress σ_{zz} over this cross-section of the rod. A parabolic Ansatz is adopted for the distribution of σ_{zz} over this cross-section (Fig. 2.36), the maximum stress occurring at the point $r = R$, that is to say at the outer fibres of the rod:

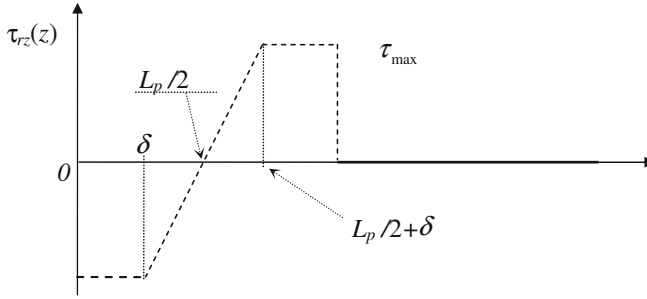


Fig. 2.34 Distribution of shear stress over the FRP surface in the area of the fitting during the crimping process [1]

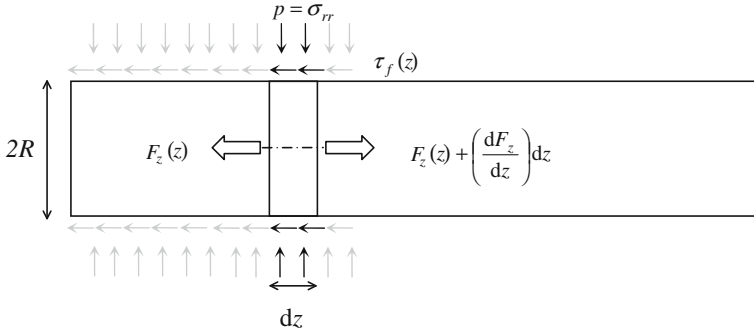


Fig. 2.35 Equilibrium at the differential beam element during crimping [1]

$$\sigma_{zz}(r, z) = \frac{\sigma_{zz}(R, z)}{R^2} r^2$$

It is noted, incidentally, that this “intuitive” approach is based on knowledge of the numerical simulation and, as will also be shown, delivers rather “sensible” results.

From the integration of $\sigma_{zz}(r, z)$ over the cross-section, it follows that:

$$F_z(z) = \int_0^{2\pi} \int_0^R r \cdot \sigma_{zz}(r, z) \cdot dr \cdot d\theta = \frac{\pi}{2} \cdot R^2 \cdot \sigma_{zz}(R, z)$$

If this result is equated with the previously obtained expression for $F_z(z)$,

$$F_z(z) = -2\pi R \cdot \tau_{\max} \cdot z \quad \text{or} \quad F_z(z) = 2\pi R \cdot \tau_{\max} \cdot \left(\frac{L_p}{2} - z \right)$$

$\sigma_{zz}(R, z)$ is ultimately given as follows:

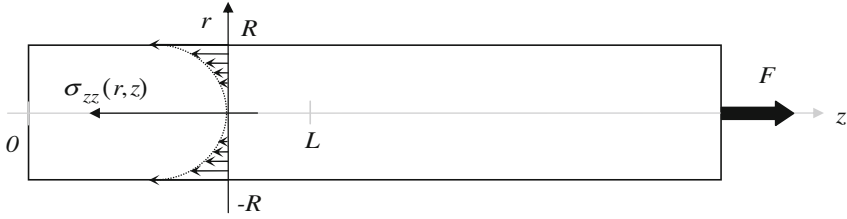


Fig. 2.36 Approach for the distribution of axial stress over the cross-section of the rod during crimping and under tensile load [1]

$$\sigma_{zz}(R, z) = -\frac{4 \cdot z}{R} \tau_{\max} \text{ or } \sigma_{zz}(R, z) = \frac{4 \cdot \left(\frac{L_p}{2} - z\right)}{R} \tau_{\max}$$

The portions of axial stress which originate from the “longitudinal-transverse” Poisson effect therefore have to be included, that is to say those portions which originate from the previously established radial and circumferential stresses in the rod:

$$\sigma_{zz}(z) = -\frac{4 \cdot z}{R} \tau_{\max} + \nu_{LT} [\sigma_{rr}(z) + \sigma_{\vartheta\vartheta}(z)]$$

and:

$$\sigma_{zz}(z) = \frac{4 \cdot \left(\frac{L_p}{2} - z\right)}{R} \tau_{\max} + \nu_{LT} [\sigma_{rr}(z) + \sigma_{\vartheta\vartheta}(z)]$$

And, as already illustrated above:

$$\sigma_{rr}(z) = \sigma_{\vartheta\vartheta}(z)$$

from which it follows that:

$$\begin{aligned} \sigma_{zz}(z) &= -\frac{4 \cdot z}{R} \tau_{\max} + 2 \nu_{LT} \sigma_{rr}(z) \text{ for } 0 \leq z \leq L_p/2 \\ \sigma_{zz}(z) &= \frac{4 \cdot \left(\frac{L_p}{2} - z\right)}{R} \tau_{\max} + 2 \nu_{LT} \sigma_{rr}(z) \text{ for } L_p/2 \leq z \leq L_p \end{aligned}$$

where ν_{LT} is the “longitudinal-transverse” Poisson number of the FRP rod (see Table 2.1).

Figure 2.37 shows the distributions of, $\sigma_{rr}(z, R)$, $\sigma_{\vartheta\vartheta}(z, R)$, $\sigma_{zz}(z, R)$ and $\tau_{rz}(z, R)$, that is to say the various stresses at the outer fibres of the FRP rod, over the crimp length of the fitting L_p for a typical 18.57 mm rod. The results of the numerical simulation (see Sect. 2.4.7) for the same variables are also plotted in the same figure [1]. The good correlation between the analytical and numerical simulations can be seen.

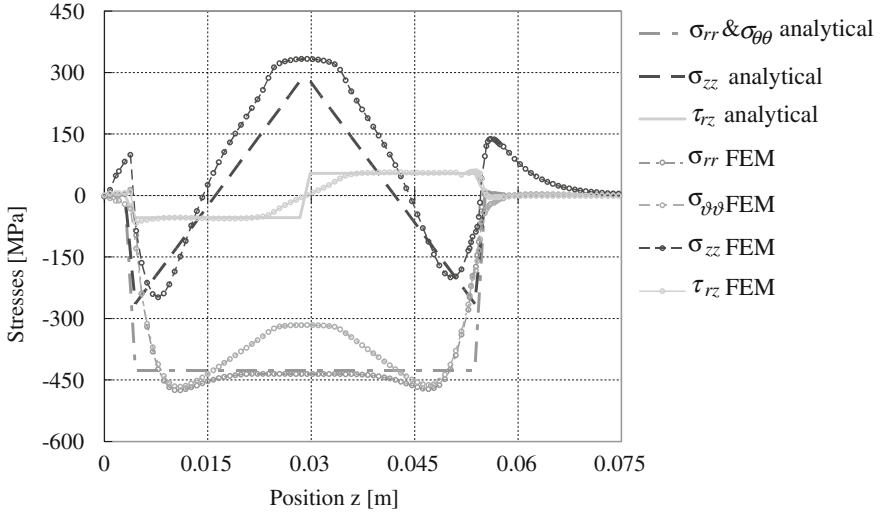


Fig. 2.37 Stress comparison (analytical/FEM) of σ_{zz} , σ_{rr} , $\sigma_{\theta\theta}$ and τ_{rz} at the outer fibres during crimping [1]

2.4.6.2 Relaxation

Once the maximum crimping pressure has been reached, it is set back to zero, as described above. The metal fitting is partly relieved of elastic deformation. The remaining radial plastic deformation continues though to apply a pressure, like-wise radially, to the circumference of the FRP rod, but this pressure is not as high as the pressure produced during the crimping process. The stresses in the FRP rod are calculated accurately using the same formulas as above, wherein all stresses are approximately 30 % lower than during crimping.

2.4.6.3 Tensile Load/Pull-Out

In this phase the insulator is subjected to tensile load until failure. Failure ideally occurs in such a way that the FRP rod slips out from the metal fitting (pull-out). Before discussing the calculation of stress, we will first consider the maximum tensile load which can be sustained.

This tensile strength depends on the magnitude of the residual contact pressure after relaxation and on the coefficient of friction between metal and FRP (this was established by tests to be $\mu \cong 0.35$). It is:

$$F_R = \mu \cdot F_N = \mu \cdot \sigma_{rr} \cdot S$$

where S is the contact (interface) surface between FRP rod and metal fitting (R : rod diameter, L_p : crimp length) as follows:

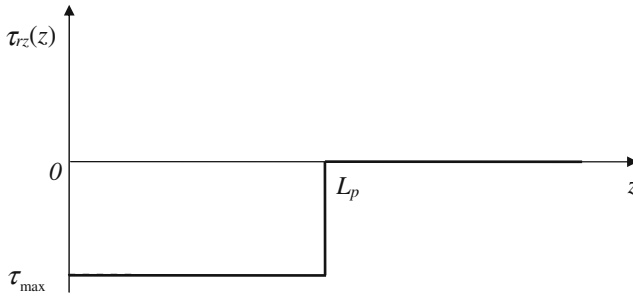


Fig. 2.38 Distribution of the shear stress over the FRP surface in the region of the fitting at the maximum tensile load possible (pull-out) [1]

$$S = 2\pi \cdot R \cdot L_p$$

However, this load is limited by the shear strength $\tau_{\max}$ of the FRP rod:

$$F_{\max} = \tau_{\max} \cdot S$$

A maximum tensile load $F_{\max}$ of 158 kN results where $R = 9.285$ mm, $L_p = 50$ mm and $\tau_{\max} = 51 \text{ N/mm}^2$. This compares well with the measured value of 148 kN (see Sect. 2.4.7.4).

The stress is calculated accurately by the same pattern as for the crimping and relaxation phases. However, the progression of shear stress $\tau_{rz}(z)$, as occurs under tensile load, has to be taken into account when establishing the axial stress $\sigma_{zz}(z)$ in the rod (Fig. 2.38).

The stress distributions for loading by the maximum tensile load are plotted in Fig. 2.39 together with the results from the numerical simulation. In this case, too, a relatively good correlation between the analytical and numerical simulations can be seen.

To summarise:

- (a) For a composite long rod insulator which is subjected purely to tensile load, it is possible to establish, for all three phases of crimping, relaxation and tensile load, both the tensile strength of the FRP rod and the stress distribution in the FRP rod (this being the critical mechanical component of the insulator) using a simple analytical model.
- (b) The most unfavourable stress distribution, and therefore the greatest potential for damage to the FRP rod occurs during crimping, which is why particular attention is to be paid to this process.

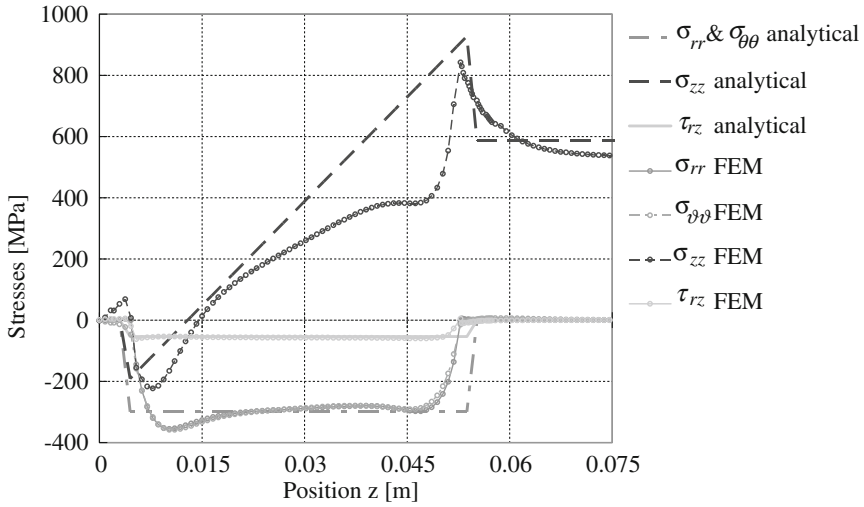


Fig. 2.39 Stress comparison (analytical/FEM) of σ_{zz} , σ_{rr} , $\sigma_{\theta\theta}$ and τ_{rz} at the outer fibres under maximum tensile load [1]

2.4.7 Numerical Simulation Methods

Numerical simulation methods using the finite element method (FEM) are the current state of the art, when non-isotropic materials, plastic deformation, contact problems and non-linearities have to be considered, as is the case here. The basic principles and approach for applying the FEM in the case of composite long rod insulators under tensile load, as described in [14], will be presented hereinafter.

2.4.7.1 Finite Element Modelling

A complete 3D beam FE model of the entire metal-composite joint was created for numerical simulation using commercial software. Non-linear simulations were carried out using this model for the three phases already described above, namely: (a) crimping of the metal fitting on the FRP rod, (b) relaxation of the crimping pressure, and (c) tensile load of the joint.

For a rod of 18.6 mm, which is typical for composite long rods, the FE model consisted of a dense mesh of hexahedral, square 20-node beam elements. Owing to the rotational symmetry of the arrangement, only one sixteenth of the structure was modelled. The resultant 3D FE mesh consisted of 180 elements for the crimping jaws, 2,800 elements for the metal fitting and 1,407 elements for the FRP rod (Fig. 2.40).

Coulomb's friction was applied both for the contact between the crimping jaws and the metal fitting, and for the contact between the metal fitting and the FRP rod,

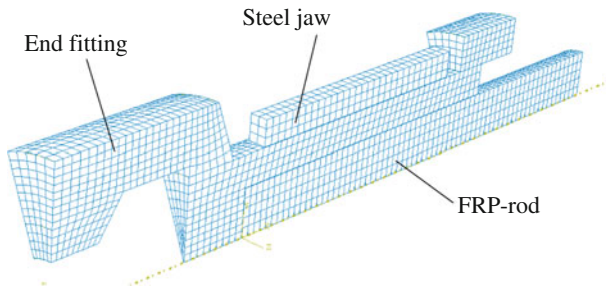


Fig. 2.40 FE model of an insulator segment in the region of the fitting

with a coefficient of friction in both cases of 0.25 being established by suitable tests; this value correlates well with the values from the literature for instances of contact with similar mounting. In addition, a maximum permissible shear stress of the FRP rod of 50 MPa, as resulted from the corresponding tests on the FRP material, was applied at the interface between metal and FRP.

2.4.7.2 Results of the Simulation

The non-linear numerical simulation of the crimping process was carried out using the above-described 3D model for the entire insulator with use of the elastoplastic material law for the metal fitting, as established by way of experiment; linear-elastic behaviour was taken as a basis for the FRP rod.

Figure 2.41 shows the stress distribution in the FRP rod at the interface between FRP and metal. It can clearly be seen that the radial stress σ_{rr} and the circumferential stress $\sigma_{\vartheta\vartheta}$ are not evenly distributed; both experience a double hump at approximately 470 MPa. In the case of average values over the entire interface, this double hump occurs between 350 and 400 MPa. There is a sound theoretical explanation for this double hump: It is caused by the load situation for a cylinder under a band of pressure [15]. The longitudinal stress is distributed more or less parabolically, near the centre of the compression area, with a maximum stress of 320 MPa. When compared to the above axial stresses, the shear stresses are negligible with the exception of the radial-axial shear stress τ_{rz} , which, due to the Poisson effect, has a quasi-sinusoidal distribution with a peak value of ± 50 MPa.

In the second phase (known as relaxation), the external pressure is relieved. The stress distributions are similar to those in the crimping phase, but generally with smaller peak values.

In the final phase of numerical simulation, a tensile load is applied to the FRP rod (while simultaneously keeping the end of the metal fitting rigidly fixed) and is increased until the FRP rod, once it has overcome the frictional forces, begins to slide out of the metal fitting. The associated stress distribution at the interface is shown in Fig. 2.42. While distribution of the radial stress σ_{rr} and of the circumferential stress $\sigma_{\vartheta\vartheta}$ is similar to that in the compression phase (Fig. 2.41), the

Fig. 2.41 Stresses σ_{zz} , σ_{rr} , $\sigma_{\vartheta\vartheta}$ and τ_{rz} on the outer fibres during crimping, numerical simulation

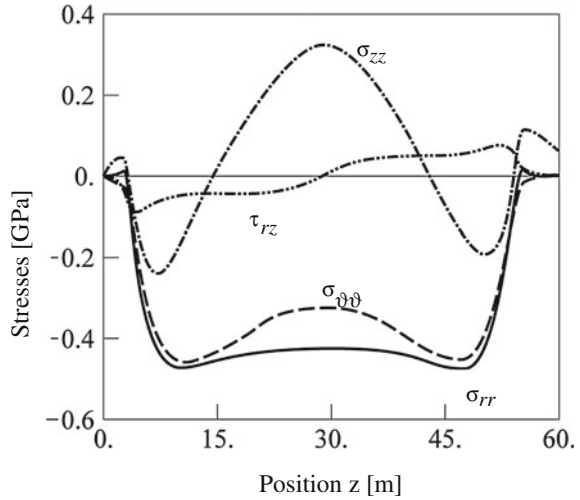
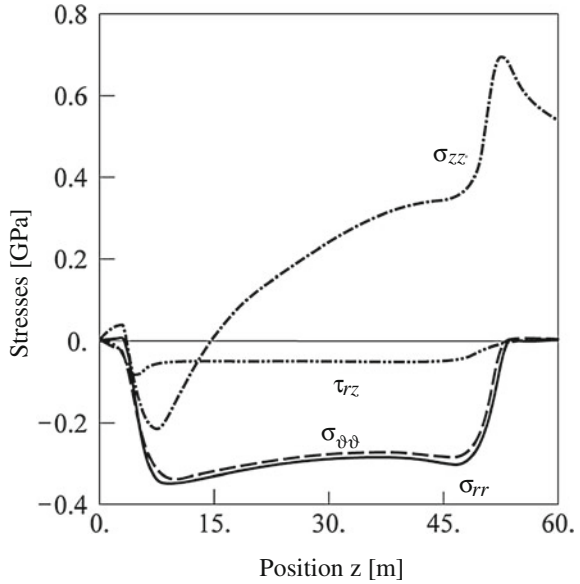


Fig. 2.42 Stresses σ_{zz} , σ_{rr} , $\sigma_{\vartheta\vartheta}$ and τ_{rz} on the outer fibre under maximum tensile load, numerical simulation



maximum values are now approximately 25 % lower. The axial stress σ_{zz} increases up to a value of 690 MPa before falling to 450 MPa, which corresponds to the axial stress in the “free” part of the FRP rod. Lastly, it should be noted that the shear stress τ_{rz} is distributed reasonably evenly, except at the fitting/rod interface where end effects manifest themselves and values of up to 50 MPa are attained, which correspond to the rod’s shear failure limit.

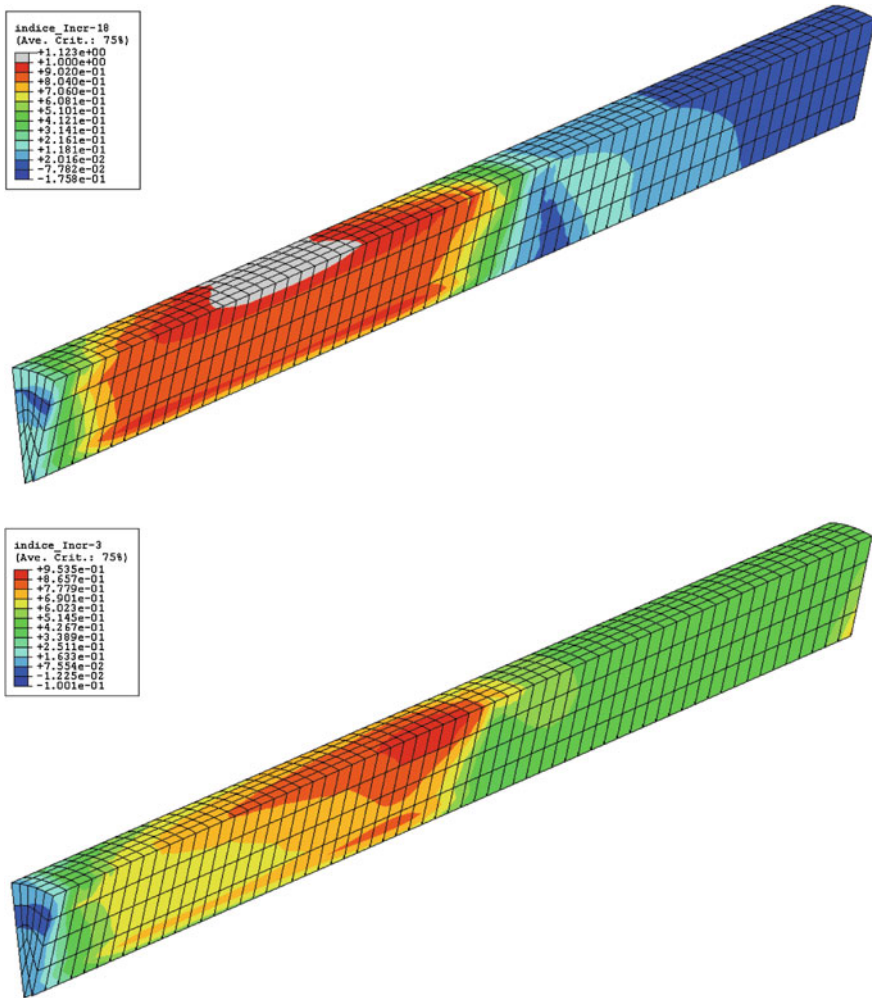


Fig. 2.43 Distribution of the danger factor κ during crimping (*top*) and under maximum tensile load (*bottom*)

2.4.7.3 Numerical Simulation Results in the Event of Failure

It is evident from the above that the critical state of inner stress, which may cause the FRP rod to split in two, occurs during crimping. To confirm this quantitatively, the failure hypothesis developed in [1] is applied to the FRP rod. The material limit values determined in suitable tests are used here for tension (1,038 MPa) and compression (−794 MPa) in the fibre direction of the rod and for tension (32 MPa) and compression (−140 MPa) transverse to fibre direction. Such tests have estimated the limit stress under biaxial load to be −500 MPa. This value is slightly

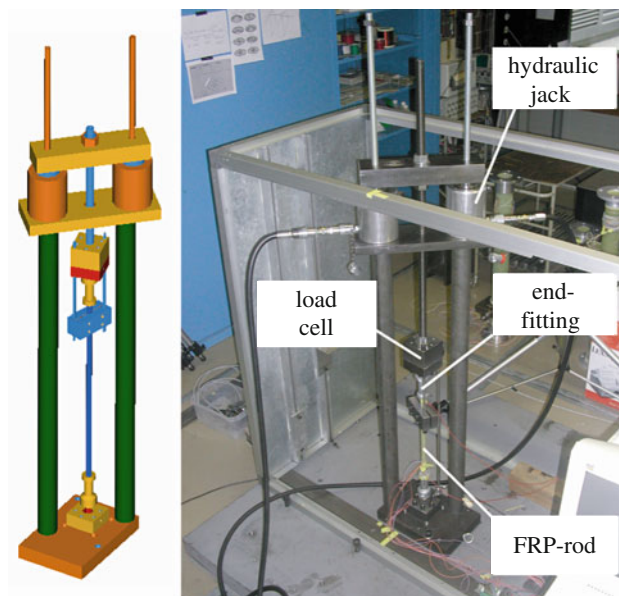


Fig. 2.44 Testing arrangement for the tensile tests

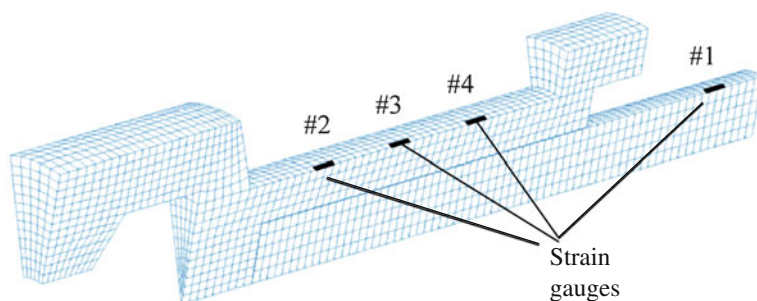


Fig. 2.45 Arrangement of the DMS on the fitting and FRP-rod

higher (absolutely) than the numerically determined value of -470 MPa for radial stress, which should be relatively close to the limit stress at which the first cracks occur in the FRP rod.

The state of stress in the FRP rod is determined using a danger factor κ , the inverse $1/\kappa$ of which represents the maximum possible value by which the load on the insulator (i.e. the crimping pressure during the crimping process or the tensile load during pull-out) can be multiplied until the FRP rod fails. This danger factor is shown in Fig. 2.43. It is important to note that a danger factor of 1.12 occurs close to the metal fitting/FRP rod interface during crimping (see Fig. 2.4, top). However, this transgression (which occurs if the danger factor is greater than one)

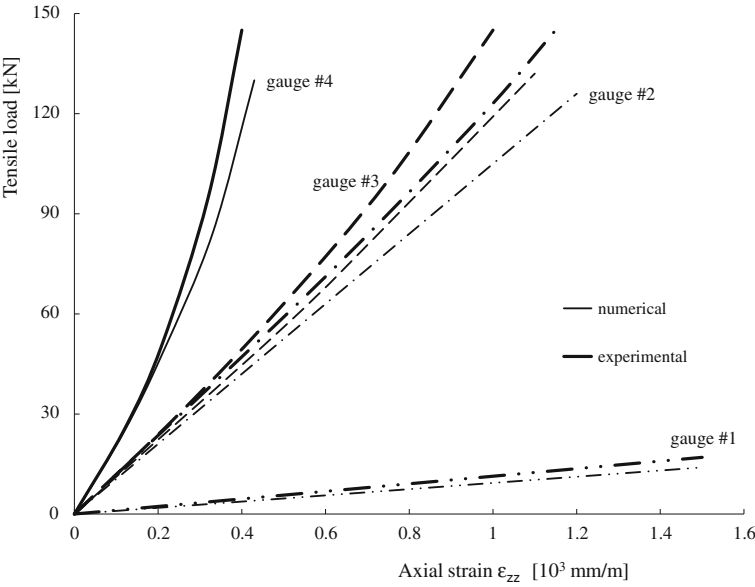


Fig. 2.46 Comparison of measurement/numerical simulation

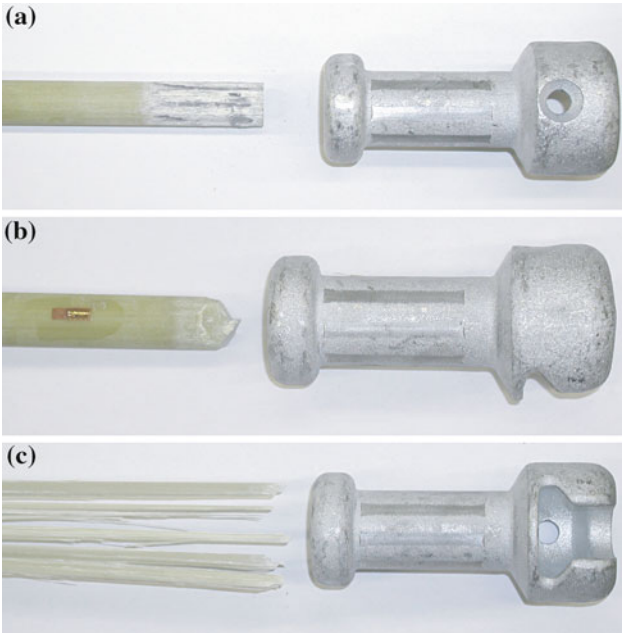


Fig. 2.47 a “Pull-out” (preferred), b Separation failure, c Delamination

is highly localised, thus making it improbable that a latent crack will propagate. The progression of the danger factor during tensile loading and, in particular, when the rod begins to slide out of the metal fitting (which occurs when the maximum bearable tensile load is applied) is shown in Fig. 2.43 (bottom); the values shown here are lower than those during crimping. In this case, the danger factor in the outer glass fibres of the FRP rod reaches relatively high values of 0.95, but therefore still always remains below the threshold value of 1 for the onset of material failure, which also explains the fact that the FRP rod slides out “cleanly” from the fitting during the tensile test without experiencing any delamination (see also Fig. 2.47a).

2.4.7.4 Test Results

The above-described calculation models were validated over a series of tests. Two variables which could be measured relatively accurately, namely some of the measurements of accessible stresses (via the strains) and the maximum pull-out force, which was established during the tensile test, were used for this purpose.

The test rig (Fig. 2.44) was formed of two servo hydraulic cylinders capable to deliver a total of 200 kN, a linear variable differential transformer (LVDT), a load cell and a number of strain gauges (DMS), which were applied to different points of the FRP rod and to the surface of the metal fitting.

The arrangement of the DMS can be seen in Fig. 2.45: DMS#1 was applied to the FRP rod, 88 mm from the end of the rod, which corresponds to 23 mm from the edge of the crimp, that is to say 23 mm from the last point of contact between the metal fitting and the FRP rod. DMS #2, #3 and #4 were placed on the surface of the metal fitting, at 12, 28 and 44 mm from the edge of the crimp respectively. The corresponding test results are plotted in Fig. 2.46 together with the load-strain curves from the FE calculation, from which a very good correlation can be seen, even though the maximum possible tensile load (that is to say the tensile load during pull-out) is underestimated by 11 % in the FE calculation, that is a value of 132 kN was calculated compared to a value of 148 kN in the tensile test.

This difference can be explained if it is considered that, on the one hand, the shear strength of the FRP rod has probably been estimated too low (the calculation assumes a uniform state of shear stress, but in reality only a thin surface layer of the FRP rod is stripped). Furthermore, the experimental load-strain curves with regard to the metal fitting (Fig. 2.46) exhibit non-linear behaviour which becomes more pronounced, the closer the measurement points are to the edge of the crimp, which clearly points towards a progressive transition of the outer tensile load from the FRP rod to the metal fitting.

The most significant aspect of the crimping process was clearly documented by increasing the contact pressure in a further numerical simulation by approximately 25 %. The danger factor reached values of approximately 1.27, which inevitably results in irreversible material damage to the rod; the tensile strength thus falls sharply to 56 kN. The associated damage pattern is shown in Fig. 2.47b; there is a

Table 2.4 Value ranges of the parameters for the sensitivity analysis

Parameter	Coefficient of friction (%)	Tolerances (%)	Crimp length (%)	Wall thickness (%)	Reduction in diameter (%)
Lowest value	-20	-60	-15	-15	-25
Highest value	+60	+60	+15	+15	+25

clean separation failure of the FRP rod. If, by contrast, the crimp pressure is increased to a slightly lesser extent, for example by approximately 15 %, delamination of the FRP material is observed (Fig. 2.47c). If correctly crimped, the rod should always pull out “cleanly” from the metal fitting (Fig. 2.47a).

2.4.7.5 Sensitivity Analysis

A sensitivity analysis of the primary variables for a crimped fitting makes it possible to validate the values selected originally, and also makes it possible to optimise the load bearing capacity of the fitting/insulator assembly. In [14], the effect of the following dimensions was examined: Coefficient of friction between FRP rod and fitting, tolerance between FRP rod and fitting, length of the crimp zone, wall thickness of the fitting, and reduction in diameter of the FRP rod during the crimping process. The variation range of these parameters are listed in Table 2.4, each one being divided into five intervals and the corresponding calculations were made using the 3D FE Model presented above.

The results are summarised in Fig. 2.48 with the von Mises stress as a reference for the load of the FRP rod. It can be seen that the parameters to which this assembly reacts most sensitively are the tolerance between rod and fitting, the crimp length, and the reduction in diameter of the FRP rod during the crimping process. For example, if the tolerance is increased by 60 %, the von Mises stress reduces by 16 or 12 % (crimping and max. tensile load respectively), but at the same time the maximum pull-out force reduces by 11 % from 132 to 118 kN. On the other hand, if the crimp zone is extended by 15 %, the pull-out force increases by 20 % from 132 to 158 kN, albeit at the cost of a von Mises stress which is approximately 13 % higher in the event of rod pull-out. The rod is damaged internally to such an extent that there is forced rupture upon pull-out (Fig. 2.47b). As expected, the sharp reduction in rod diameter during crimping by 25 % results in a massive increase in the von Mises stress by 28 %, but only in a marginal increase in the maximum tensile load by 5 % from 132 to 139 kN. A reduction in crimping with regard to rod diameter by 25 % (compared to the initial state of 100 %) results in a dramatic decrease in the failing load of the insulator by a whole 28 %, that is to say from 132 to 95 kN. By contrast, neither the coefficient of friction between FRP rod and metal fitting, nor the wall thickness of the fitting

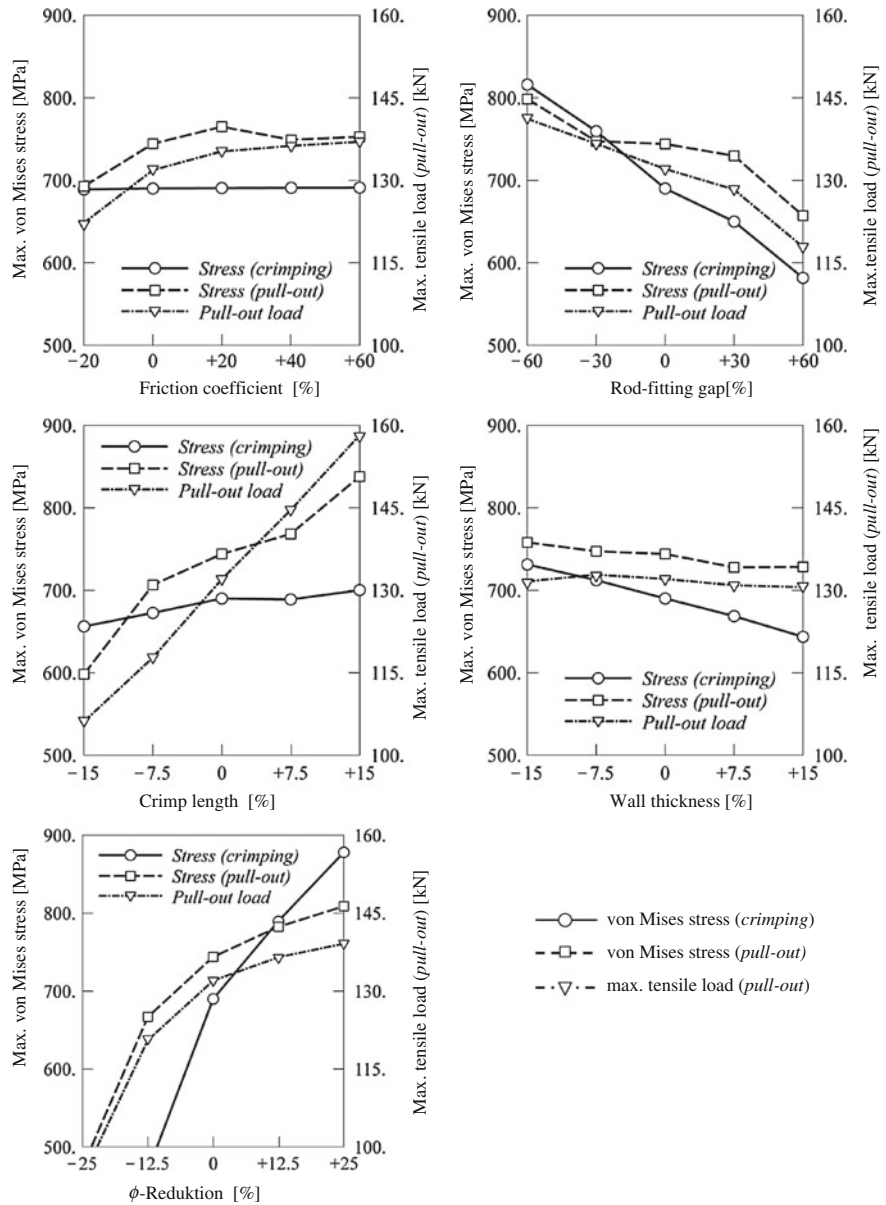


Fig. 2.48 Results of the sensitivity analysis

have a substantial effect on the stress regime of the rod or on the failing load of the insulator, although a moderate decrease in the von Mises stress (during crimping) is to be observed with an increase in the wall thickness of the fitting.

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