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Petroleum, petrochemical and natural gas industries — Calculation of heater-tube thickness in petroleum refineries

Industries du pétrole, de la pétrochimie et du gaz naturel — Calcul de l'épaisseur des tubes de fours de raffineries de pétrole

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Foreword

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The main task of technical committees is to prepare International Standards. Draft International Standards adopted by the technical committees are circulated to the member bodies for voting. Publication as an International Standard requires approval by at least 75 % of the member bodies casting a vote.

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ISO 13704 was prepared by Technical Committee ISO/TC 67, *Materials, equipment and offshore structures for petroleum, petrochemical and natural gas industries*, Subcommittee SC 6, *Processing equipment and systems*.

This second edition cancels and replaces the first edition (ISO 13704:2001), which has been technically revised.

Petroleum, petrochemical and natural gas industries — Calculation of heater-tube thickness in petroleum refineries

1 Scope

This International Standard specifies the requirements and gives recommendations for the procedures and design criteria used for calculating the required wall thickness of new tubes and associated component fittings for petroleum-refinery heaters. These procedures are appropriate for designing tubes for service in both corrosive and non-corrosive applications. These procedures have been developed specifically for the design of refinery and related process-fired heater tubes (direct-fired, heat-absorbing tubes within enclosures). These procedures are not intended to be used for the design of external piping.

This International Standard does not give recommendations for tube retirement thickness; Annex A describes a technique for estimating the life remaining for a heater tube.

2 Terms and definitions

For the purposes of this document, the following terms and definitions apply.

2.1
actual inside diameter
D_i
inside diameter of a new tube

NOTE The actual inside diameter is used to calculate the tube skin temperature in Annex B and the thermal stress in Annex C.

2.2
component fitting
fitting connected to the fired heater tubes

EXAMPLES Return bends, elbows, reducers.

NOTE 1 There is a distinction between standard component fittings and specially designed component fittings; see 4.9.

NOTE 2 Typical material specifications for standard component fittings are ASTM A 234, ASTM A 403 and ASTM B 366.

2.3
corrosion allowance
δ_{CA}
additional material thickness added to allow for material loss during the design life of the component

2.4
design life
t_{DL}
operating time used as a basis for tube design

NOTE The design life is not necessarily the same as the retirement or replacement life.

2.5
design metal temperature
T_d
tube-metal or skin temperature used for design

NOTE This is determined by calculating the maximum tube metal temperature (T_{max} in Annex B) or the equivalent tube metal temperature (T_{eq} in 2.8) and adding an appropriate temperature allowance (see 2.16). A procedure for calculating the maximum tube metal temperature from the heat-flux density is included in Annex B. When the equivalent tube metal temperature is used, the maximum operating temperature can be greater than the design metal temperature. When the equivalent tube metal temperature is used to determine the design metal temperature, this design metal temperature is only applicable to the rupture design. It is necessary to develop a separate design metal temperature applicable to the elastic design. The design metal temperature applicable to the elastic design is the maximum calculated tube metal temperature among all operating cases plus the appropriate temperature allowance.

2.6
elastic allowable stress
σ_{el}
allowable stress for the elastic range

See 5.2.

2.7
elastic design pressure
p_{el}
maximum pressure that the heater coil can sustain for short periods of time

NOTE This pressure is usually related to relief-valve settings, pump shut-in pressures, etc.

2.8
equivalent tube metal temperature
T_{eq}
calculated constant metal temperature that in a specified period of time produces the same creep damage as does a changing metal temperature

NOTE In 4.8 the equivalent tube metal temperature concept is described in more detail. It provides a procedure to calculate the equivalent tube metal temperature based on a linear change of tube metal temperature from start-of-run to end-of-run.

2.9
inside diameter
D_i^*
inside diameter of a tube with the corrosion allowance removed; used in the design calculations

NOTE The inside diameter of an as-cast tube is the inside diameter of the tube with the porosity and corrosion allowances removed.

2.10
minimum thickness
δ_{min}
minimum required thickness of a new tube, taking into account all appropriate allowances

NOTE See Equation (5).

2.11
outside diameter
D_o
outside diameter of a new tube

2.12
rupture allowable stress
σ_r
allowable stress for the creep-rupture range

See 4.4.

2.13
rupture design pressure
p_r
maximum operating pressure that the coil section can sustain during normal operation

2.14
rupture exponent
n
parameter used for design in the creep-rupture range

NOTE See figures in Annexes E and F.

2.15
stress thickness
δ_σ
thickness, excluding all thickness allowances, calculated from an equation that uses an allowable stress

2.16
temperature allowance
T_A
part of the design metal temperature that is included for process- or flue-gas mal-distribution, operating unknowns, and design inaccuracies

NOTE The temperature allowance is added to the calculated maximum tube metal temperature or to the equivalent tube metal temperature to obtain the design metal temperature (see 2.5).

3 General design information

3.1 Information required

The design parameters (design pressures, design fluid temperature, corrosion allowance and tube material) shall be defined. In addition, the following information shall be furnished:

a) design life of the heater tube;

b) whether the equivalent-temperature concept is to be applied and, if so, the operating conditions at the start and at the end of the run;

c) temperature allowance (see ISO 13705), if any;

d) corrosion fraction (if different from that shown in Figure 1);

e) whether elastic-range thermal-stress limits are to be applied.

If any of items a) to e) are not furnished, use the following applicable parameters:

— design life equal to 100 000 h;

— design metal temperature based on the maximum metal temperature (the equivalent-temperature concept shall not apply);

— temperature allowance equal to 15 °C (25 °F);

— corrosion fraction given in Figure 1;

— elastic-range thermal-stress limits.

3.2 Limitations for design procedures

3.2.1 The allowable stresses are based on a consideration of yield strength and rupture strength only; plastic or creep strain has not been considered. Using these allowable stresses can result in small permanent strains in some applications; however, these small strains do not affect the safety or operability of heater tubes.

3.2.2 No considerations are included for adverse environmental effects, such as graphitization, carburization or hydrogen attack. Limitations imposed by hydrogen attack can be developed from the Nelson curves in API 941 [1].

3.2.3 These design procedures have been developed for seamless tubes. They are not applicable to tubes that have a longitudinal weld. ISO 13705 allows only seamless tubes.

3.2.4 These design procedures have been developed for thin tubes (tubes with a thickness-to-outside-diameter ratio, δ_{min}/D_o, of less than 0,15). Additional considerations can apply to the design of thicker tubes.

3.2.5 No considerations are included for the effects of cyclic pressure or cyclic thermal loading.

3.2.6 Limits for thermal stresses are provided in Annex C. Limits for stresses developed by mass, supports, end connections and so forth are not discussed in this International Standard.

3.2.7 Most of the Larson-Miller parameter referenced curves in 5.6 are not Larson-Miller curves in the traditional sense but are derived from the 100 000 h rupture strength as explained in Clause H.3. Consequently, the curves might not provide a reliable estimate of the rupture strength for a design life that is less than 20 000 h or more than 200 000 h.

3.2.8 The procedures in this International Standard have been developed for systems in which the heater tubes are subject to an internal pressure that exceeds the external pressure. There are some cases in which a heater tube can be subject to a greater external pressure than the internal pressure. This can occur, for example, in vacuum heaters or on other types of heaters during shutdown or trip conditions, especially when a unit is cooling or draining, forming a vacuum inside the heater tubes. Conditions where external pressures exceed the internal pressures can govern heater-tube wall thickness. Determination of this (i.e. vacuum design) is not covered in this International Standard. In the absence of any local or national codes that can apply, it is recommended that a pressure vessel code, such as ASME VIII (Division 1, UG-28) or EN 13445, be used, as such codes also address external pressure designs.

4 Design

4.1 General

There is a fundamental difference between the behaviour of carbon steel in a hot-oil heater tube operating at 300 °C (575 °F) and that of chromium-molybdenum steel in a catalytic-reformer heater tube operating at 600 °C (1 110 °F). The steel operating at the higher temperature creeps, or deforms permanently, even at stress levels well below the yield strength. If the tube metal temperature is high enough for the effects of creep to be significant, the tube eventually fails due to creep rupture, although no corrosion or oxidation mechanism is active. For the steel operating at the lower temperature, the effects of creep are non-existent or negligible. Experience indicates that, in this case, the tube lasts indefinitely, unless a corrosion or an oxidation mechanism is active.

Since there is a fundamental difference between the behaviour of the materials at these two temperatures, there are two different design considerations for heater tubes: elastic design and creep-rupture design. Elastic design is design in the elastic range, at lower temperatures, in which allowable stresses are based on the yield strength (see 4.3). Creep-rupture design (which is referred to below as rupture design) is the design for the creep-rupture range, at higher temperatures, in which allowable stresses are based on the rupture strength (see 4.4).

The temperature that separates the elastic and creep-rupture ranges of a heater tube is not a single value; it is a range of temperatures that depends on the alloy. For carbon steel, the lower end of this temperature range is about 425 °C (800 °F); for type 347 stainless steel, the lower end of this temperature range is about 590 °C (1 100 °F). The considerations that govern the design range also include the elastic design pressure, the rupture design pressure, the design life and the corrosion allowance.

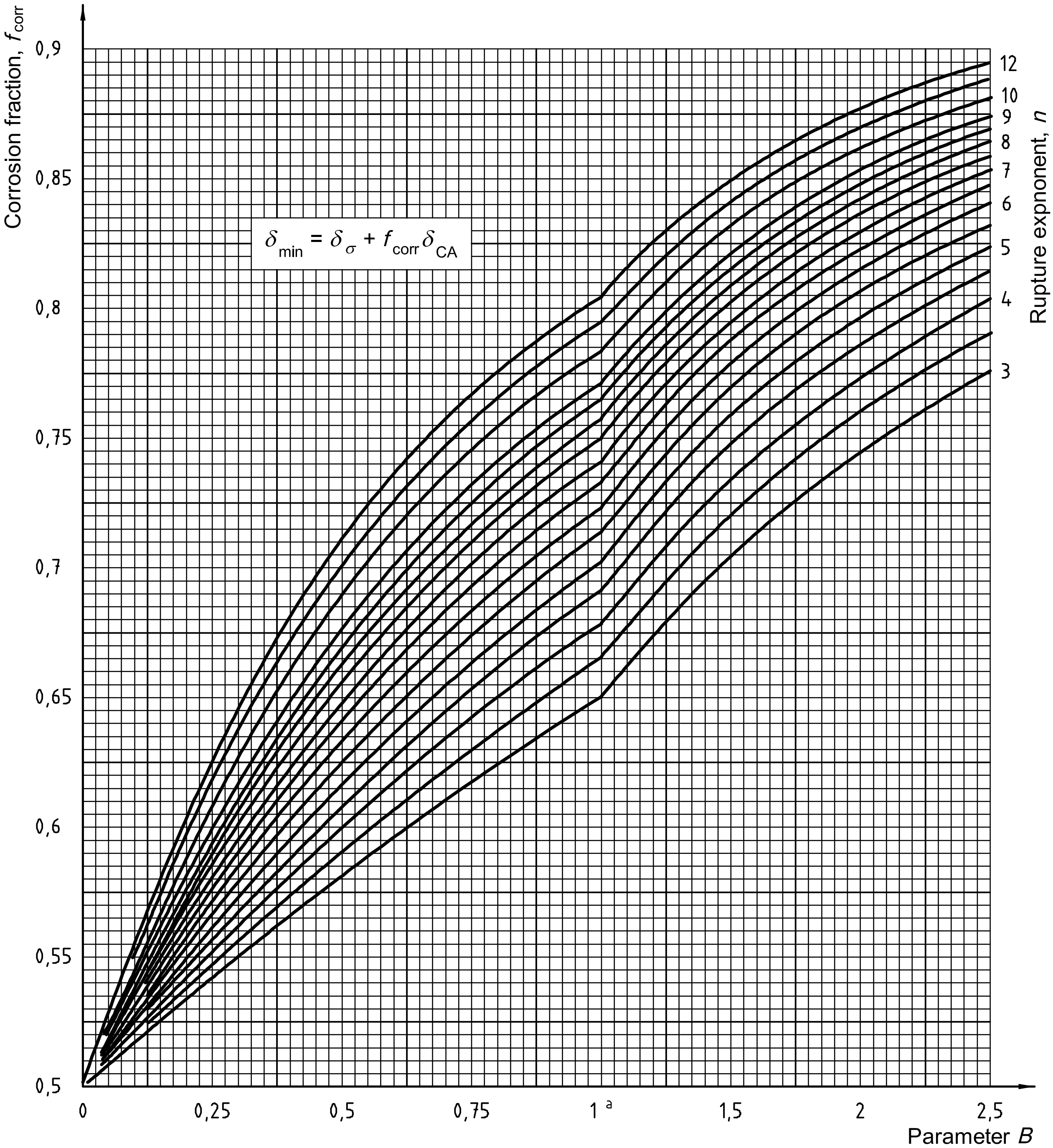
The rupture design pressure is never more than the elastic design pressure. The characteristic that differentiates these two pressures is the relative length of time over which they are sustained. The rupture design pressure is a long-term loading condition that remains relatively uniform over a period of years. The elastic design pressure is usually a short-term loading condition that typically lasts only hours or days. The rupture design pressure is used in the rupture design equation, since creep damage accumulates as a result of the action of the operating, or long-term, stress. The elastic design pressure is used in the elastic design equation to prevent excessive stresses in the tube during periods of operation at the maximum pressure.

The tube shall be designed to withstand the rupture design pressure for long periods of operation. If the normal operating pressure increases during an operating run, the highest pressure shall be taken as the rupture design pressure.

In the temperature range near or above the point where the elastic and rupture allowable stress curves cross, both elastic and rupture design equations are to be used. The larger value of δ_{min} should govern the design (see 4.5). A sample calculation that uses these methods is included in Clause 6. Calculation sheets (see Annex D) are available for summarizing the calculations of minimum thickness and equivalent tube metal temperature.

The allowable minimum thickness of a new tube is given in Table 1.

All of the design equations described in Clause 4 are summarized in Table 2.



Key

$\delta_{\sigma} = \dfrac{p_r D_o}{2\sigma_r + p_r}$

δ_{CA} is the corrosion allowance

D_o is the outside diameter

σ_r is the rupture allowable stress

p_r is the rupture design pressure

$B = \delta_{CA}/\delta_{\sigma}$

a Note change of scale at X = 1.

Figure 1 — Corrosion fraction

4.2 Equation for stress

In both the elastic range and the creep-rupture range, the design equation is based on the mean-diameter equation for stress in a tube. In the elastic range, the elastic design pressure, p_{el}, and the elastic allowable stress, σ_{el}, are used. In the creep-rupture range, the rupture design pressure, p_r, and the rupture allowable stress ,σ_r, are used.

The mean-diameter equation gives a good estimate of the pressure that produces yielding through the entire tube wall in thin tubes (see 3.2.4 for a definition of thin tubes). The mean-diameter equation also provides a good correlation between the creep rupture of a pressurized tube and a uniaxial test specimen. It is, therefore, a good equation to use in both the elastic range and the creep-rupture range[16], [17], [18], [19]. The mean-diameter equation for stress is as given in Equation (1):

$$\sigma = \frac{p}{2}\left(\frac{D_o}{\delta} - 1\right) = \frac{p}{2}\left(\frac{D_i}{\delta} + 1\right) \tag{1}$$

where

σ is the stress, expressed in megapascals [pounds per square inch[1)]];

p is the pressure, expressed in megapascals (pounds per square inch);

D_o is the outside diameter, expressed in millimetres (inches);

D_i is the inside diameter, expressed in millimetres (inches), including the corrosion allowance;

δ is the thickness, expressed in millimetres (inches).

The equations for the stress thickness, δ_σ, in 4.3 and 4.4 are derived from Equation (1).

4.3 Elastic design (lower temperatures)

The elastic design is based on preventing failure by bursting when the pressure is at its maximum (that is, when a pressure excursion has reached p_{el} near the end of the design life after the corrosion allowance has been used up. With the elastic design, δ_σ and δ_{min} (see 4.6) are calculated as given in Equations (2) and (3):

$$\delta_\sigma = \frac{p_{el} D_o}{2\sigma_{el} + p_{el}} \text{ or } \delta_\sigma = \frac{p_{el} D_i^*}{2\sigma_{el} - p_{el}} \tag{2}$$

$$\delta_{min} = \delta_\sigma + \delta_{CA} \tag{3}$$

where

D_i^* is the inside diameter, expressed in millimetres (inches), with corrosion allowance removed;

σ_{el} is the elastic allowable stress, expressed in megapascals (pounds per square inch), at the design metal temperature.

1) The unit “pounds per square inch (psi)” is referred to as “pound-force per square inch (lbf/in^2)” in ISO/IEC 80000.

4.4 Rupture design (higher temperatures)

The rupture design is based on preventing failure by creep rupture during the design life. With the rupture design, δ_σ and δ_{min} (see 4.6) are calculated from Equations (4) and (5):

$$\delta_\sigma = \frac{p_r D_o}{2\sigma_r + p_r} \text{ or } \delta_\sigma = \frac{p_r D_i^*}{2\sigma_r - p_r} \tag{4}$$

$$\delta_{min} = \delta_\sigma + f_{corr}\delta_{CA} \tag{5}$$

where

σ_r is the rupture allowable stress, expressed in megapascals (pounds per square inch), at the design metal temperature and the design life;

f_{corr} is the corrosion fraction, given as a function of B and n in Figure 1;

B $= \delta_{CA}/\delta_\sigma$

n is the rupture exponent at the design metal temperature (shown in the figures given in Annexes E and F).

The derivation of the corrosion fraction is described in Annex G. It is recognized in this derivation that stress is reduced by the corrosion allowance; correspondingly, the rupture life is increased.

Equations (4) and (5) are suitable for heater tubes; however, if special circumstances require that the user choose a more conservative design, a corrosion fraction of unity (f_{corr} = 1) may be specified.

4.5 Intermediate temperature range

At temperatures near or above the point where the curves of σ_{el} and σ_r intersect in the figures given in Annexes E and F, either elastic or rupture considerations govern the design. In this temperature range, it is necessary to apply both the elastic and the rupture designs. The larger value of δ_{min} shall govern the design.

4.6 Minimum allowable thickness

The minimum thickness, δ_{min}, of a new tube (including the corrosion allowance) shall not be less than that shown in Table 1. For ferritic steels, the values shown are the minimum allowable thicknesses of schedule 40 average wall pipe. For austenitic steels, the values are the minimum allowable thicknesses of schedule 10S average wall pipe. (Table 5 shows which alloys are ferritic and which are austenitic.) The minimum allowable thicknesses are 0,875 times the average thicknesses. These minima are based on industry practice. The minimum allowable thickness is not the retirement or replacement thickness of a used tube.

4.7 Minimum and average thicknesses

The minimum thickness, δ_{min}, is calculated as described in 4.3 and 4.4. Tubes that are purchased to this minimum thickness have a greater average thickness. A thickness tolerance is specified in each ASTM specification. For most of the ASTM specifications shown in the figures given in Annexes E and F, the tolerance on the minimum thickness is $\left(\begin{smallmatrix}0\\+28\end{smallmatrix}\right)$ % for hot-finished tubes and $\left(\begin{smallmatrix}0\\+22\end{smallmatrix}\right)$ % for cold-drawn tubes. This is equivalent to tolerances on the average thickness of ± 12,3 % and ± 9,9 %, respectively. The remaining ASTM specifications require that the minimum thickness be greater than 0,875 times the average thickness, which is equivalent to a tolerance on the average thickness of + 12,5 %.

With a $\left({}^{0}_{+28} \right)$ % tolerance, a tube that is purchased to a 12,7 mm (0,500 in) minimum-thickness specification has the following average thickness:

$$(12,7)(1 + 0,28/2) = 14,5 \text{ mm } (0,570 \text{ in})$$

To obtain a minimum thickness of 12,7 mm (0,500 in) in a tube purchased to a ± 12,5 % tolerance on the average thickness, the average thickness shall be specified as follows:

$$(12,7)/(0,875) = 14,5 \text{ mm } (0,571 \text{ in})$$

All thickness specifications shall indicate whether the specified value is a minimum or an average thickness. The tolerance used to relate the minimum and average wall thicknesses shall be the tolerance given in the ASTM specification to which the tubes are purchased.

Table 1 — Minimum allowable thickness of new tubes

Tube outside diameter		Minimum thickness			
		Ferritic steel tubes		Austenitic steel tubes	
mm	(in)	mm	(in)	mm	(in)
60,3	(2,375)	3,4	(0,135)	2,4	(0,095)
73,0	(2,875)	4,5	(0,178)	2,7	(0,105)
88,9	(3,50)	4,8	(0,189)	2,7	(0,105)
101,6	(4,00)	5,0	(0,198)	2,7	(0,105)
114,3	(4,50)	5,3	(0,207)	2,7	(0,105)
141,3	(5,563)	5,7	(0,226)	3,0	(0,117)
168,3	(6,625)	6,2	(0,245)	3,0	(0,117)
219,1	(8,625)	7,2	(0,282)	3,3	(0,130)
273,1	(10,75)	8,1	(0,319)	3,7	(0,144)

4.8 Equivalent tube metal temperature

In the creep-rupture range, the accumulation of damage is a function of the actual operating tube metal temperatures (TMTs). For applications in which there are significant differences between start-of-run and end-of-run TMTs, a design based on the maximum temperature can be excessive, since the actual operating TMT is usually less than the maximum.

For a linear change in metal temperature from start of run, T_{sor}, to end of run, T_{eor}, an equivalent tube metal temperature, T_{eq}, can be calculated as shown in Equation (6). A tube operating at the equivalent tube metal temperature sustains the same creep damage as one that operates from the start-of-run to end-of-run temperatures.

$$T_{eq} = T_{sor} + f_T \left(T_{eor} - T_{sor} \right) \tag{6}$$

where

T_{eq} is the equivalent tube metal temperature, expressed in degrees Celsius (Fahrenheit);

T_{sor} is the tube metal temperature, expressed in degrees Celsius (Fahrenheit), at start of run;

T_{eor} is the tube metal temperature, expressed in degrees Celsius (Fahrenheit), at end of run;

f_T is the temperature fraction given in Figure 2.

The derivation of the temperature fraction is described in Annex G. The temperature fraction is a function of two parameters, V and N, as given in Equations (7) and (8):

$$V = n_0 \left(\frac{\Delta T^*}{T^*_{sor}} \right) \ln \left(\frac{A}{\sigma_0} \right) \quad (7)$$

$$N = n_0 \left(\frac{\Delta \delta}{\delta_0} \right) \quad (8)$$

where

n_0 is the rupture exponent at T_{sor};

ΔT^* is the temperature change, equal to $T_{eor} - T_{sor}$, expressed in kelvin [degrees Rankine[2)]], during the operating period;

T^*_{sor} $= T_{sor} + 273$ K ($T_{sor} + 460$ °R);

ln is the natural logarithm;

$\Delta\delta$ is the change in thickness, equal to $\phi_{corr} t_{op}$, expressed in millimetres (inches), during the operating period;

ϕ_{corr} is the corrosion rate, expressed in millimetres per year (in inches per year);

t_{op} is the duration of operating period, expressed in years;

δ_0 is the initial thickness, expressed in millimetres (inches), at the start of the run;

σ_0 is the initial stress, expressed in megapascals (pounds per square inch), at the start of the run, using Equation (1);

A is the material constant, expressed in megapascals (pounds per square inch).

The constant A is given in Table 3. The significance of the material constant is explained in Clause G.5.

2) Rankine is a deprecated unit.

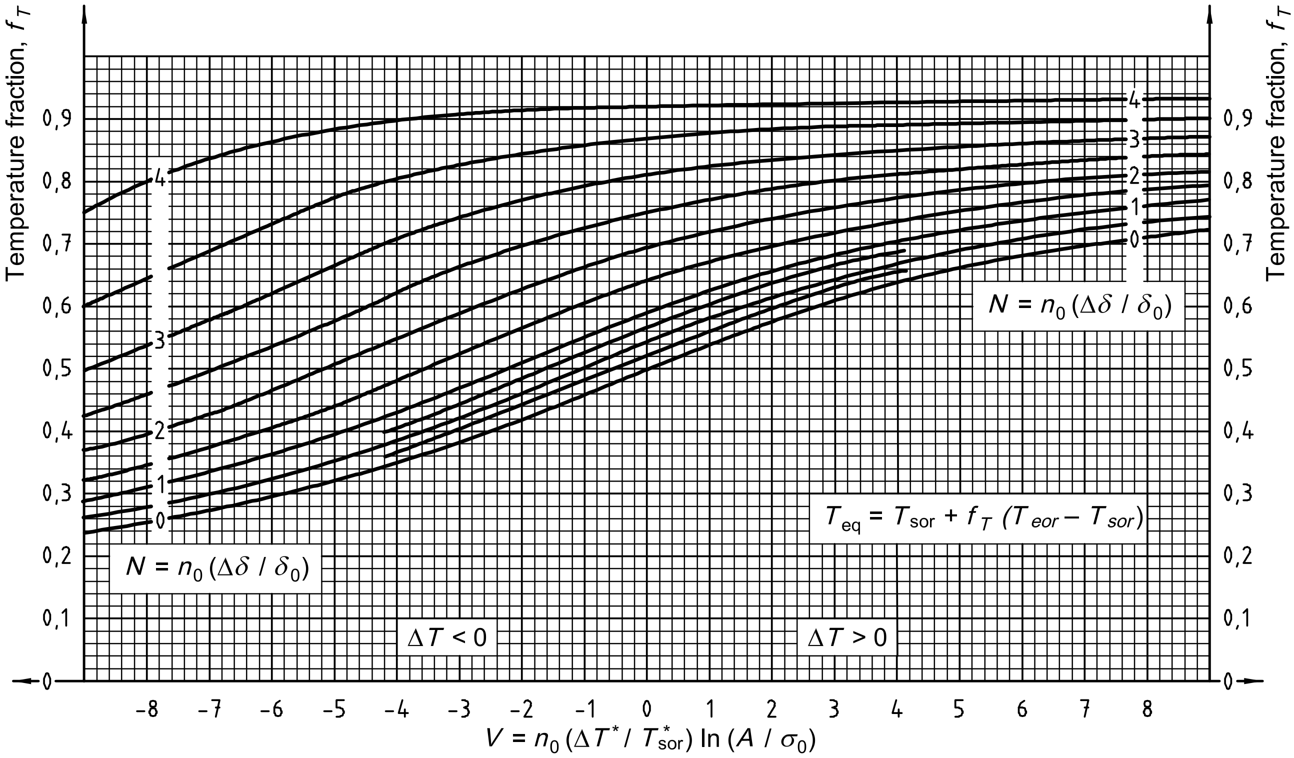


Figure 2 — Temperature fraction

Table 2 — Summary of working equations

Elastic design (lower temperatures)**:**

$$\delta_\sigma = \frac{p_{el} D_o}{2\sigma_{el} + p_{el}} \text{ or } \delta_\sigma = \frac{p_{el} D_i^*}{2\sigma_{el} - p_{el}} \quad (2)$$

$$\delta_{min} = \delta_\sigma + \delta_{CA} \quad (3)$$

Rupture design (higher temperatures)**:**

$$\delta_\sigma = \frac{p_r D_o}{2\sigma_r + p_r} \text{ or } \delta_\sigma = \frac{p_r D_i^*}{2\sigma_r - p_r} \quad (4)$$

$$\delta_{min} = \delta_\sigma + f_{corr}\delta_{CA} \quad (5)$$

where

- δ_σ is the stress thickness, expressed in millimetres (inches);
- p_{el} is the elastic design gauge pressure, expressed in megapascals (pounds per square inch);
- p_r is the rupture design gauge pressure, expressed in megapascals (pounds per square inch);
- D_o is the outside diameter, expressed in millimetres (inches);
- D_i^* is the inside diameter, expressed in millimetres (inches), with the corrosion allowance removed;
- σ_{el} is the elastic allowable stress, expressed in megapascals (pounds per square inch), at the design metal temperature;
- σ_r is the rupture allowable stress, expressed in megapascals (pounds per square inch), at the design metal temperature and design life;
- δ_{min} is the minimum thickness, expressed in millimetres (inches), including corrosion allowance;
- δ_{CA} is the corrosion allowance, expressed in millimetres (inches);
- f_{corr} is the corrosion fraction, given in Figure 1 as a function of B and n, where $B = \delta_{CA}/\delta_\sigma$;
- n is the rupture exponent at the design metal temperature.

Equivalent tube metal temperature:

$$T_{eq} = T_{sor} + f_T (T_{eor} - T_{sor}) \quad (6)$$

where

- ΔT^* $(= T_{eor} - T_{sor})$ is the temperature change, expressed in kelvin (degrees Rankine), during the operating period;
- T_{sor} is the tube metal temperature, expressed in degrees Celsius (Fahrenheit), at the start of the run;
- T_{eor} is the tube metal temperature, expressed in degrees Celsius (Fahrenheit), at the end of the run;
- T_{sor}^* $= T_{sor} + 273$ K ($T_{sor} + 460$ °R);
- A is the material constant, expressed in megapascals (pounds per square inch) from Table 3;
- σ_0 is the initial stress, expressed in megapascals (pounds per square inch), at the start of the run using Equation (1);
- $\Delta\delta$ $(= \phi_{corr} t_{op})$ is the change in thickness, expressed in millimetres (inches), during the operating period;
- δ_0 is the initial thickness, expressed in millimetres (inches), at the start of the run;
- ϕ_{corr} is the corrosion rate, expressed in millimetres per year (inches per year);
- t_{op} is the duration, expressed in years, of the operating period.

Table 3 — Material constant for temperature fraction

Material	Type or grade	Constant A MPa	(psi)
Low-carbon steel	—	$7{,}46 \times 10^5$	$(1{,}08 \times 10^8)$
Medium-carbon steel	B	$2{,}88 \times 10^5$	$(4{,}17 \times 10^7)$
C-½Mo steel	T1 or P1	$2{,}01 \times 10^7$	$(2{,}91 \times 10^9)$
1-¼Cr-½Mo steel	T11 or P11	$5{,}17 \times 10^7$	$(7{,}49 \times 10^9)$
2-¼Cr-1Mo steel	T22 or P22	$8{,}64 \times 10^5$	$(1{,}25 \times 10^8)$
3Cr-1Mo steel	T21 or P21	$2{,}12 \times 10^6$	$(3{,}07 \times 10^8)$
5Cr-½Mo steel	T5 or P5	$5{,}49 \times 10^5$	$(7{,}97 \times 10^7)$
5Cr-½Mo-Si steel	T5b or P5b	$2{,}88 \times 10^5$	$(4{,}18 \times 10^7)$
7Cr-½Mo steel	T7 or P7	$1{,}64 \times 10^5$	$(2{,}37 \times 10^7)$
9Cr-1Mo steel	T9 or P9	$7{,}54 \times 10^6$	$(1{,}09 \times 10^9)$
9Cr-1Mo V steel	T91 or P91	$2{,}23 \times 10^6$	$(3{,}24 \times 10^8)$
18Cr-8Ni steel	304 or 304H	$1{,}55 \times 10^6$	$(2{,}25 \times 10^8)$
16Cr-12Ni-2Mo steel	316 or 316H	$1{,}24 \times 10^6$	$(1{,}79 \times 10^8)$
16Cr-12Ni-2Mo steel	316L	$1{,}37 \times 10^6$	$(1{,}99 \times 10^8)$
18Cr-10Ni-Ti steel	321	$1{,}32 \times 10^6$	$(1{,}92 \times 10^8)$
18Cr-10Ni-Ti steel	321H	$2{,}76 \times 10^5$	$(4{,}00 \times 10^7)$
18Cr-10Ni-Nb[a] steel	347 or 347H	$1{,}23 \times 10^6$	$(1{,}79 \times 10^8)$
Ni-Fe-Cr	Alloy 800H / 800HT	$1{,}03 \times 10^5$	$(1{,}50 \times 10^7)$
25Cr-20Ni	HK40	$2{,}50 \times 10^5$	$(3{,}63 \times 10^7)$

[a] Formerly called columbium, Cb.

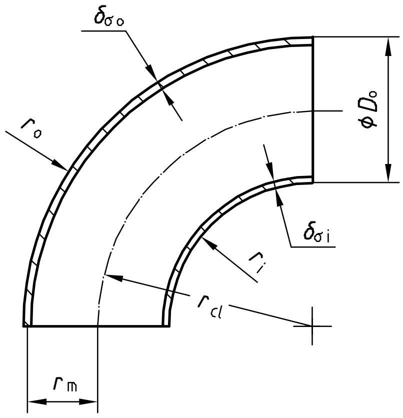
The temperature fraction and the equivalent temperature shall be calculated for the first operating cycle. In applications that involve very high corrosion rates, the temperature fraction for the last cycle is greater than that for the first. In such cases, the calculation of the temperature fraction and the equivalent temperature should be based on the last cycle.

If the temperature change from start-of-run to end-of-run is other than linear, a judgment shall be made regarding the use of the value of f_T given in Figure 2.

Note that the calculated thickness of a tube is a function of the equivalent temperature, which, in turn, is a function of the thickness (through the initial stress). A few iterations can be necessary to arrive at the design. (See the sample calculation in 6.4.)

4.9 Component fittings

Component fittings manufactured in accordance with ASME B16.9 are considered suitable for use at the pressure-temperature ratings specified therein. Other component fittings shall be specially designed in accordance with this subclause.



r_o = outer radius

r_i = inner radius

For other symbols, see text below Equation (9).

Figure 3 — Return bend and elbow geometry

The stress variations in a return bend or elbow (see Figure 3) are far more complex than in a straight tube. The hoop stresses at the inner radius of a return bend are higher than in a straight tube of the same thickness. It might be necessary for the minimum thickness at the inner radius to be greater than the minimum thickness of the attached tube.

Because fabrication processes for forged return bends generally result in greater thickness at the inner radius, the higher stresses at the inner radius can be sustained without failure in most situations.

The hoop stress σ_i, expressed in megapascals (pounds per square inch), along the inner radius of the bend is given by Equation (9):

$$\sigma_i = \frac{2r_{cl} - r_m}{2(r_{cl} - r_m)} \sigma \tag{9}$$

where

r_{cl} is the centre line radius of the bend, expressed in millimetres (inches);

r_m is the mean radius of the tube, expressed in millimetres (inches);

σ is the stress, expressed in megapascals (pounds per square inch), given by Equation (1).

The hoop stress σ_o, expressed in megapascals (pounds per square inch), along the outer radius is given by Equation (10):

$$\sigma_o = \frac{2r_{cl} + r_m}{2(r_{cl} + r_m)} \sigma \tag{10}$$

Using the approximation that r_m is almost equal to $D_o/2$, Equation (9) can be solved for the stress thickness at the inner radius. For design, the stress thickness is given by Equation (11).

$$\delta_{\sigma i} = \frac{D_o p}{2N_i \sigma + p} \quad (11)$$

where

$\delta_{\sigma i}$ is the stress thickness, expressed in millimetres (inches), at the inner radius;

$$N_i = \frac{4\frac{r_{cl}}{D_o} - 2}{4\frac{r_{cl}}{D_o} - 1} \quad (12)$$

σ is the allowable stress, expressed in megapascals (pounds per square inch) at the design metal temperature.

NOTE 1 p represents both elastic design pressure and rupture design pressure.

The return bend thickness evaluations shall be made using both elastic design pressure and rupture design pressure, and the governing thicknesses shall be the larger values at the inner and outer radii.

Using the approximation given above, Equation (10) can be solved for the stress thickness at the outer radius. For elastic design, the stress thickness is as given in Equation (13):

$$\delta_{\sigma o} = \frac{D_o p}{2N_o \sigma + p} \quad (13)$$

where

$\delta_{\sigma o}$ is the stress thickness, expressed in millimetres (inches), at the outer radius;

$$N_o = \frac{4\frac{r_{cl}}{D_o} + 2}{4\frac{r_{cl}}{D_o} + 1} \quad (14)$$

σ is the allowable stress, expressed in megapascals (pounds per square inch), at the design metal temperature.

NOTE 2 p represents both elastic design pressure and rupture design pressure.

The return bend thickness evaluations shall be made using both elastic design pressure and rupture design pressure, and the governing thicknesses shall be the larger values at the inner and outer radii.

The minimum thickness, $\delta_{\sigma i}$, at the inside radius and the minimum thickness, $\delta_{\sigma o}$, at the outside radius shall be calculated using Equations (11) and (13). The corrosion allowance, δ_{CA}, shall be added to the minimum calculated thickness.

The minimum thickness along the neutral axis of the bend shall be the same as for a straight tube.

5 Allowable stresses

5.1 General

The allowable stresses for various heater-tube alloys are plotted against design metal temperature in Figures E.1 to E.19 (SI units) and Figures F.1 to F.19 [US Customary (USC) units]. The values shown in these figures are recommended only for the design of heater tubes. These figures show two different allowable stresses, the elastic allowable stress and the rupture allowable stress. The bases for these allowable stresses are given in 5.2 and 5.3 (see also 3.2.3).

5.2 Elastic allowable stress

The elastic allowable stress, σ_{el}, is two-thirds of the yield strength at temperature for ferritic steels and 90 % of the yield strength at temperature for austenitic steels. The data sources for the yield strength are given in Annex H.

If a different design basis is desired for special circumstances, the user shall specify the basis, and the alternative elastic allowable stress shall be developed from the yield strength.

5.3 Rupture allowable stress

The rupture allowable stress, σ_r, is 100 % of the minimum rupture strength for a specified design life. Annex H defines the minimum rupture strength and provides the data sources. The 20 000-h, 40 000-h, 60 000-h and 100 000-h rupture allowable stresses were developed from the Larson-Miller parameter curves for the minimum rupture strength shown on the right-hand side of Figures E.1 to E.19 (Figures F.1 to F.19). For a design life other than those shown, the corresponding rupture allowable stress shall be developed from the Larson-Miller parameter curves for the minimum rupture strength (see 5.6).

If a different design basis is desired, the user shall specify the basis, and the alternative rupture allowable stress shall be developed from the Larson-Miller parameter curves for the minimum or average rupture strength. If the resulting rupture allowable stress is greater than the minimum rupture strength for the design life, the effects of creep on the tube design equation should be considered.

5.4 Rupture exponent

Figures E.1 to E.19 (Figures F.1 to F.19) show the rupture exponent, n, as a function of the design metal temperature. The rupture exponent is used for design in the creep-rupture range (see 4.4). The meaning of the rupture exponent is discussed in Clause H.4.

5.5 Yield and tensile strengths

Figures E.1 to E.19 (Figures F.1 to F.19) also show the yield and tensile strengths. These curves are included only for reference. Their sources are given in Annex H.

5.6 Larson-Miller parameter curves

On the right-hand side of Figures E.1 to E.19 (Figures F.1 to F.19) are plots of the minimum and the average 100 000-h rupture strengths against the Larson-Miller parameter. The Larson-Miller parameter is calculated from the design metal temperature, T_d, and the design life, t_{DL}, as given in Equations (15) and (16):

When T_d is expressed in degrees Celsius:

$$(T_d + 273)(C_{LM} + \lg t_{DL}) \times 10^{-3} \qquad (15)$$

When T_d is expressed in degrees Fahrenheit:

$$(T_d + 460)(C_{LM} + \lg t_{DL}) \times 10^{-3} \qquad (16)$$

The Larson-Miller constant, C_{LM}, is stated in the curves. (See Clause H.3 for a detailed description of these curves).

The curves for minimum and average rupture strength can be used to calculate remaining tube life, as shown in Annex A.

The plot of the minimum rupture strength against the Larson-Miller parameter is included so that the rupture allowable stress can be determined for any design life. The curves shall not be used to determine rupture allowable stresses for temperatures higher than the limiting design metal temperatures shown in Table 4 and Figures E.1 to E.19 (Figures F.1 to F.19). Furthermore, the curves can give inaccurate rupture allowable stresses for tube life less than 20 000 h or greater than 200 000 h (see Clause H.3).

5.7 Limiting design metal temperature

The limiting design metal temperature for each heater-tube alloy is given in Table 4. The limiting design metal temperature is the upper limit of the reliability of the rupture strength data. Higher temperatures, i.e. up to 30 °C (50 °F) below the lower critical temperature, are permitted for short-term operating conditions, such as those that exist during steam-air decoking or regeneration. Operation at higher temperatures can result in changes in the alloy's microstructure. Lower critical temperatures for ferritic steels are shown in Table 4. Austenitic steels do not have lower critical temperatures. Other considerations can require lower operating-temperature limits, such as oxidation, graphitization, carburization, and hydrogen attack. These factors shall be considered when furnace tubes are designed.

Table 4 — Limiting design metal temperature for heater-tube alloys

Materials	Type or grade	Limiting design metal temperature		Lower critical temperature	
		°C	(°F)	°C	(°F)
Carbon steel	B	540	(1 000)	720	(1 325)
C-½Mo steel	T1 or P1	595	(1 100)	720	(1 325)
1¼Cr-½Mo steel	T11 or P11	595	(1 100)	775	(1 430)
2¼Cr-1Mo steel	T22 or P22	650	(1 200)	805	(1 480)
3Cr-1Mo steel	T21 or P21	650	(1 200)	815	(1 500)
5Cr-½Mo steel	T5 or P5	650	(1 200)	820	(1 510)
5Cr-½Mo-Si steel	T5b or P5b	705	(1 300)	845	(1 550)
7Cr-½Mo steel	T7 or P7	705	(1 300)	825	(1 515)
9Cr-1Mo steel	T9 or P9	705	(1 300)	825	(1 515)
9Cr-1Mo-V steel	T91 or P91	650 [a]	(1 200 [a])	830	(1 525)
18Cr-8Ni steel	304 or 304H	815	(1 500)	—	—
16Cr-12Ni-2Mo steel	316 or 316H	815	(1 500)	—	—
16Cr-12Ni-2Mo steel	316L	815	(1 500)	—	—
18Cr-10Ni-Ti steel	321 or 321H	815	(1 500)	—	—
18Cr-10Ni-Nb steel	347 or 347H	815	(1 500)	—	—
Ni-Fe-Cr	Alloy 800H/800HT	985 [a]	(1 800 [a])	—	—
25Cr-20Ni	HK40	1 010 [a]	(1 850 [a])	—	—

[a] This is the upper limit on the reliability of the rupture strength data (see Annex H); however, these materials are commonly used for heater tubes at higher temperatures in applications where the internal pressure is so low that rupture strength does not govern the design.

5.8 Allowable stress curves

Figures E.1 to E.19 provide the elastic allowable stress and the rupture allowable stress in SI units for most common heater-tube alloys. Figures F.1 to F.19 show the same data in USC units.

The sources for these curves are provided in Annex H. The figure number for each alloy is shown in Table 5.

Table 5 — Index to allowable stress curves

Steel type	Figure number	Alloy
Ferritic	E.1 (F.1)	Low-carbon steel (A 161, A 192)
	E.2 (F.2)	Medium-carbon steel (A 53B, A 106B, A 210A-1)
	E.3 (F.3)	C-½Mo
	E.4[a] (F.4)	1¼Cr-½Mo
	E.5[a] (F.5)	2¼Cr-1Mo
	E.6[a] (F.6)	3Cr-1Mo
	E.7[a] (F.7)	5Cr-½Mo
	E.8 (F.8)	5Cr-½Mo-Si
	E.9[a] (F.9)	7Cr-½Mo
	E.10[a] (F.10)	9Cr-1Mo
	E.11 (F.11)	9Cr-1Mo-V
Austenitic	E.12 (F.12)	18Cr-8Ni (304 and 304H)
	E.13 (F.13)	16Cr-12Ni-2Mo (316 and 316H)
	E.14 (F.14)	16Cr-12Ni-2Mo (316L)
	E.15 (F.15)	18Cr-10Ni-Ti (321)
	E.16 (F.16)	18Cr-10Ni-Ti (321H)
	E.17 (F.17)	18Cr-10Ni-Nb (347 and 347H)
	E.18 (F.18)	Ni-Fe-Cr (Alloy 800H/800HT)
	E.19 (F.19)	25Cr-20Ni (HK40)

[a] Broken lines on these figures indicate the elastic allowable stresses for the A 200 grades. These figures do not show the yield strengths of the A 200 grades. The yield strengths of the A 200 grades are 83 % of the yield strengths shown. The tensile strengths, rupture allowable stresses, rupture strengths, and rupture exponents for the A 200 grades are the same as for the A 213 and A 335 grades.

6 Sample calculations

6.1 Elastic design

The following example illustrates the use of design equations for the elastic range. Suppose the following information is given (the USC unit conversions in parentheses are approximate):

Material = 18Cr-10Ni-Nb, type 347 stainless steel

D_o = 168,3 mm (6,625 in)

p_{el} = 6,2 MPa gauge (900 psig)

T_d = 425 °C (800 °F)

δ_{CA} = 3,2 mm (0,125 in)

From Figure E.17 (SI units) or Figure F.17 (USC units):

σ_{el} = 124 MPa (18 100 psi)

σ_y = 138 MPa (20 110 psi)

Using Equations (2) and (3):

$$\delta_\sigma = \frac{(6{,}2)(168{,}3)}{2(125)+6{,}2} = 4{,}0 \text{ mm}$$

$$\delta_{min} = 4{,}0 + 3{,}2 = 7{,}2 \text{ mm}$$

In USC units:

$$\delta_\sigma = \frac{(900)(6{,}625)}{2(18\,250)+900} = 0{,}159 \text{ in}$$

$$\delta_{min} = 0{,}159 + 0{,}125 = 0{,}284 \text{ in}$$

This design calculation is summarized in the calculation sheet in Figure 4.

CALCULATION SHEET SI units (USC units)		
Heater ____ Plant ____ Refinery ____		
Coil ____ Material Type 347 ASTM Spec. A 213		
Calculation of minimum thickness	**Elastic design**	**Rupture design**
Outside diameter, mm (in)	D_o = 168,3 (6,625)	D_o =
Design pressure, gauge, MPa (psi)	p_{el} = 6,2 (900)	p_r =
Maximum or equivalent metal temperature, °C (°F)	T_{max} =	T_{max} =
Temperature allowance, °C (°F)	T_A =	T_A =
Design metal temperature, °C (°F)	T_d = 425 (800)	T_d =
Design life, h	—	t_{DL} =
Allowable stress at T_d, Figures E.1 to E.19 (Figures F.1 to F.19), MPa (psi)	σ_{el} = 125 (18 250)	σ_r =
Stress thickness, Equation (2) or (4), mm (in)	δ_σ = 4,04 (0,159)	δ_σ =
Corrosion allowance, mm (in)	δ_{CA} = 3,2 (0,125)	δ_{CA} =
Corrosion fraction, Figure 1, n = B =	—	f_{corr} =
Minimum thickness, Equations (3) or (5), mm (in)	δ_{min} = 7,2 (0,284)	δ_{min} =

Figure 4 — Sample calculation for elastic design

6.2 Thermal-stress check (for elastic range only)

The thermal stress, σ_T, in the tube designed in accordance with 6.1 shall be checked using the following values for the variables in the equations given in Annex C:

$\alpha = 1{,}81 \times 10^{-5}$ K^{-1} ($10{,}05 \times 10^{-6}$ R^{-1}) (thermal expansion coefficient taken from Table C-3 of Reference [20]);

$E = 1{,}66 \times 10^5$ MPa ($24{,}1 \times 10^6$ psi) (modulus of elasticity taken from Table C-6 of Reference [20]);

$\nu = 0{,}3$ (Poisson's ratio value commonly used for steels);

$q_o = 63{,}1$ kW/m^2 [20 000 Btu/(h·ft^2)] (assumed heat-flux density);

$\lambda_s = 20{,}6$ W/(m·K) [11,9 Btu/(h·ft °F)] (thermal conductivity).

Using SI units in Equation (C.2):

$$X = \left[\frac{(1{,}81)\ (1{,}66)}{4\ (1 - 0{,}3)}\right]\left[\frac{(63{,}1)\ (168{,}3)}{(20{,}6)}\right]$$

$X = 553{,}2$ MPa

Using USC units in Equation (C.2):

$$X = \left[\frac{(10{,}05)\ (24{,}1)}{4\ (1\ -\ 0{,}3)}\right]\left[\frac{(20\ 000)\ (6{,}625)}{(11{,}9)\ (12)}\right]$$

$X = 8{,}026 \times 10^4$ psi

The thickness calculated in 6.1 is the minimum. The average thickness shall be used in the thermal-stress calculation. The average thickness (see 4.7) is calculated as follows:

In SI units:

$(7{,}2)\ (1 + 0{,}14) = 8{,}2$ mm

In USC units:

$(0{,}284)\ (1 + 0{,}14) = 0{,}324$ in

The actual inside diameter is calculated as follows:

In SI units:

$D_i = 168{,}3 - 2(8{,}2) = 151{,}9$ mm

$y = 168{,}3/151{,}9 = 1{,}108$

where y is the ratio of outside diameter to actual inside diameter, D_o/D_i.

In USC units:

$D_i = 6{,}625 - 2(0{,}324) = 5{,}977$ in

$y = 6{,}625/5{,}977 = 1{,}108$

The term in brackets in Equation (C.1) is calculated as follows:

$$\frac{2(1{,}108)^2}{(1{,}108)^2 - 1}\ \ln(1{,}108) - 1\ =\ 0{,}106$$

Using Equation (C.1), the maximum thermal stress, $\sigma_{T\max}$, is calculated as follows:

$\sigma_{T\max} = (553{,}2)\ (0{,}106)$

$\sigma_{T\max} = 58{,}6$ MPa

In USC units:

$\sigma_{T\max} = (8{,}026 \times 10^4)\ (0{,}106)$

$\sigma_{T\max} = 8\ 508$ psi

The limits for this stress for austenitic steels are given by Equations (C.4) and (C.6), in which the yield strength is 140 MPa (20 200 psi).

$\sigma_{T,\text{lim1}} = [2{,}7 - 0{,}9(1{,}108)]\ (140)$

$\sigma_{T,\text{lim1}} = 238$ MPa

$\sigma_{T,\text{lim2}} = (1{,}8)\ (140)$

$\sigma_{T,\text{lim2}} = 252$ MPa

In USC units:

$\sigma_{T,\text{lim1}} = [2{,}7 - 0{,}9(1{,}108)]\ (20\ 200)$

$\sigma_{T,\text{lim1}} = 34\ 400$ psi

$\sigma_{T,\text{lim2}} = (1{,}8)\ (20\ 200)$

$\sigma_{T,\text{lim2}} = 36\ 360$ psi

Since the maximum thermal stress is less than these limits, the design is acceptable.

If a thicker tube is specified arbitrarily (as Schedule 80S can be in this example), the actual average tube thickness shall be used in calculating the thermal stress and its limits as follows:

The inside diameter of a 6-in Schedule 80S tube is as follows:

$D_\text{i} = 146{,}3$ mm

therefore

$y = 168{,}3/146{,}3 = 1{,}150$

In USC units:

$D_\text{i} = 5{,}761$ in

$y = 6{,}625/5{,}761 = 1{,}150$

The term in brackets in Equation (C.1) is calculated as follows:

$$\frac{2\,(1{,}150)^2}{(1{,}150)^2 - 1}\ \ln\,(1{,}150) - 1 = 0{,}146$$

Using Equation (C.1), the maximum thermal stress is calculated as follows:

$\sigma_{T\max} = (553{,}2)\ (0{,}146)$

$\sigma_{T\max} = 80{,}9$ MPa

In USC units:

$$\sigma_{T\max} = (8{,}026 \times 10^4)\ (0{,}146)$$

$$\sigma_{T\max} = 11\ 718 \text{ psi}$$

The average thickness of this tube is 11,0 mm (0,432 in), so the minimum thickness is calculated as follows:

$$\delta_{\min} = \frac{11{,}0}{1+0{,}14} = 9{,}6 \text{ mm}$$

In USC units:

$$\delta_{\min} = \frac{0{,}432}{1+0{,}14} = 0{,}379 \text{ in}$$

Using Equation (C.9), the stress is calculated as follows:

$$\sigma_{\text{pm}} = \frac{6{,}2}{2}\left(\frac{168{,}3}{9{,}6} - 1\right) = 51{,}2 \text{ MPa}$$

In USC units:

$$\sigma_{\text{pm}} = \frac{900}{2}\left(\frac{6{,}625}{0{,}379} - 1\right) = 7\ 416 \text{ psi}$$

The thermal-stress limit based on the primary plus secondary stress intensity is calculated using Equation (C.14). Using the values above, this limit is calculated as follows:

$$\sigma_{T,\text{lim1}} = (2{,}7 \times 140) - (1{,}15 \times 51{,}2)$$

$$\sigma_{T,\text{lim1}} = 319{,}1 \text{ MPa}$$

In USC units:

$$\sigma_{T,\text{lim1}} = (2{,}7 \times 20\ 200) - (1{,}15 \times 7\ 416)$$

$$\sigma_{T,\text{lim2}} = 46\ 010 \text{ psi}$$

The thermal-stress ratchet limit is calculated using Equation (C.19). In this case, the limit is as follows:

$$\sigma_{T,\text{lim2}} = 4[(1{,}35 \times 140) - 51{,}2]$$

$$\sigma_{T,\text{lim2}} = 551{,}2 \text{ MPa}$$

In USC units:

$$\sigma_{T,\text{lim2}} = 4[(1{,}35 \times 20\ 200) - 7\ 416]$$

$$\sigma_{T,\text{lim2}} = 79\ 416 \text{ psi}$$

The thermal stress in the thicker tube is well below these limits.

6.3 Rupture design with constant temperature

A modification of the example in 6.1 illustrates how the design equations are used for the creep-rupture range. Suppose the tube described in 6.1 is designed for the following conditions:

$T_d = 705$ °C (1 300 °F)

$t_{DL} = 100\ 000$ h

$p_r = 5{,}8$ MPa gauge (840 psig)

From Figure E.17 (SI units) or Figure F.17 (USC units).

$\sigma_r = 37{,}3$ MPa (5 450 psi)

Using Equation (4):

In SI units:

$$\delta_\sigma = \frac{(5{,}8)(168{,}3)}{2(37.3)+5{,}8} = 12{,}1\,\text{mm}$$

In USC units:

$$\delta_\sigma = \frac{(840)(6{,}625)}{2(5\ 450)+840} = 0{,}474\ \text{in}$$

From this:

In SI units:

$$B = \frac{3{,}2}{12{,}1} = 0{,}264$$

In USC units:

$$B = \frac{0{,}125}{0{,}474} = 0{,}264$$

From Figure E.17 (SI units) or Figure F.17 (USC units)

$n = 4{,}4$

With these values for B and n, use Figure 1 to obtain the following corrosion fraction:

$f_{corr} = 0{,}558$

Hence, using Equation (5):

In SI units:

$\delta_{min} = 12{,}1 + (0{,}558 \times 3{,}2)$

$\delta_{min} = 13{,}9$ mm

In USC units:

$$\delta_{min} = 0{,}474 + (0{,}558 \times 0{,}125)$$

$$\delta_{min} = 0{,}544 \text{ in}$$

To confirm that this is an appropriate design, the elastic design is checked using the elastic design pressure instead of the rupture design pressure. Using Equations (2) and (3) with the conditions given above:

In SI units:

$$\sigma_{el} = 113 \text{ MPa}$$

$$\delta_{\sigma} = \frac{(6{,}2)(168{,}3)}{2(113) + 6{,}2} = 4{,}5 \text{ mm}$$

$$\delta_{min} = 4{,}5 + 3{,}2 = 7{,}7 \text{ mm}$$

In USC units:

$$\sigma_{el} = 16\,400 \text{ psi}$$

$$\delta_{\sigma} = \frac{(900)\,(6{,}625)}{2(16\,400)\, +\, 900} = 0{,}177 \text{ in}$$

$$\delta_{min} = 0{,}177 + 0{,}125 = 0{,}302 \text{ in}$$

Since δ_{min} based on rupture design is greater, it governs the design. This design calculation is summarized on the calculation sheet in Figure 5.

CALCULATION SHEET SI units (USC units)		
Heater ______ Plant ______ Refinery ______ Coil Material Type 347 ASTM Spec. A 213		
Calculation of minimum thickness	**Elastic design**	**Rupture design**
Outside diameter, mm (in)	D_o = 168,3 (6,625)	D_o = 168,3 (6,625)
Design pressure, gauge, MPa (psi)	p_{el} = 6,2 (900)	p_r = 5,8 (840)
Maximum or equivalent metal temperature, °C (°F)	T_{max} =	T_{max} =
Temperature allowance, °C (°F)	T_A =	T_A =
Design metal temperature, °C (°F)	T_d = 705 (1 300)	T_d = 705 (1 300)
Design life, h	—	t_{DL} = 100 000
Allowable stress at T_d, Figures E.1 to E.19 (Figures F.1 to F.19), MPa (psi)	σ_{el} = 113 (16 400)	σ_r = 37,3 (5 450)
Stress thickness, Equation (2) or (4), mm (in)	δ_{σ} = 4,5 (0,177)	δ_{σ} = 12,0 (0,474)
Corrosion allowance, mm (in)	δ_{CA} = 3,18 (0,125)	δ_{CA} = 3,18 (0,125)
Corrosion fraction, Figure 1, $n = 4{,}4$; $B = 0{,}264$	—	f_{corr} = 0,558
Minimum thickness, Equation (3) or (5), mm (in)	δ_{min} = 7,67 (0,302)	δ_{min} = 13,82 (0,544)

Figure 5 — Sample calculation for rupture design (constant temperature)

6.4 Rupture design with linearly changing temperature

Suppose the tube described in 6.3 operates in a service for which the estimated tube metal temperature varies from 635 °C (1 175 °F) at the start of run to 690 °C (1 275 °F) at the end of run. Assume that the run lasts a year, during which the thickness changes by about 0,33 mm (0,013 in).

Assume that the initial minimum thickness is 8,0 mm (0,315 in); therefore, using Equation (1), the initial stress is as follows:

In SI units:

$$\sigma_0 = \frac{5,8}{2}\left(\frac{168,3}{8,0} - 1\right) = 58,1 \text{ MPa}$$

In USC units:

$$\sigma_0 = \frac{840}{2}\left(\frac{6,625}{0,315} - 1\right) = 8\ 413 \text{ psi}$$

At the start-of-run temperature, $n_0 = 4,8$. From Table 3, A is $1,23 \times 10^6$ MPa ($1,79 \times 10^8$ psi). The parameters for the temperature fraction are, therefore, as follows:

In SI units:

$$V = 4,8\left(\frac{55}{908}\right) \ln\left(\frac{1,23 \times 10^6}{58,1}\right) = 2,9$$

$$N = 4,8\left(\frac{0,33}{8,0}\right) = 0,2$$

In USC units:

$$V = 4,8\left(\frac{100}{1635}\right) \ln\left(\frac{1,79 \times 10^8}{8\ 413}\right) = 2,9$$

$$N = 4,8\left(\frac{0,013}{0,315}\right) = 0,2$$

From Figure 2, $f_T = 0,62$, and the equivalent temperature is calculated using Equation (6) as follows:

In SI units:

$$T_{eq} = 635 + (0,62 \times 55) = 669 \text{ °C}$$

In USC units:

$$T_{eq} = 1\ 175 + (0,62 \times 100) = 1\ 237 \text{ °F}$$

A temperature allowance of 15 °C (25 °F) is added to yield a design temperature of 684 °C (1 262 °F), which is rounded up to 685 °C (1 265 °F). Using this temperature to carry out the design procedure illustrated in 6.3 yields the following:

In SI units:

$$\delta_\sigma = 9{,}9 \text{ mm}$$

$$\delta_{min} = 9{,}9 + (0{,}572 \times 3{,}2)$$

$$\delta_{min} = 11{,}7 \text{ mm}$$

In USC units:

$$\delta_\sigma = 0{,}388 \text{ in}$$

$$\delta_{min} = 0{,}388 + (0{,}572 \times 0{,}125)$$

$$\delta_{min} = 0{,}460 \text{ in}$$

This thickness is different from the 8,0 mm (0,315 in) thickness that was initially assumed. Using this thickness, the initial stress is calculated as follows:

In SI units:

$$\sigma_0 = \frac{5{,}7}{2}\left(\frac{168{,}3}{11{,}7} - 1\right) = 38{,}8 \text{ MPa}$$

In USC units:

$$\sigma_0 = \frac{840}{2}\left(\frac{6{,}625}{0{,}460} - 1\right) = 5\,629 \text{ psi}$$

With this stress, the temperature-fraction parameters V and N become the following:

In SI units:

$$V = 4{,}8\left(\frac{55}{908}\right) \ln\left(\frac{1{,}23 \times 10^6}{38{,}8}\right) = 3{,}0$$

$$N = 4{,}8\left(\frac{0{,}33}{11{,}7}\right) = 0{,}1$$

In USC units:

$$V = 4{,}8\left(\frac{100}{1635}\right) \ln\left(\frac{1{,}79 \times 10^8}{5\,629}\right) = 3{,}0$$

$$N = 4{,}8\left(\frac{0{,}013}{0{,}460}\right) = 0{,}1$$

Using these values in Figure 2, $f_T = 0{,}62$, the value that was determined in the first calculation. Since the temperature fraction did not change, further iteration is not necessary. This design calculation is summarized in the calculation sheet in Figure 6.

CALCULATION SHEET SI units (USC units)		
Heater ________ Plant ________ Refinery ________ Coil Material Type 347 ASTM Spec. A 213		
Calculation of minimum thickness	**Elastic design**	**Rupture design**
Outside diameter, mm (in)	D_o =	D_o = 168,3 (6,625)
Design pressure, gauge, MPa (psi)	p_{el} =	p_r = 5,8 (840)
Maximum or equivalent metal temperature, °C (°F)	T_{eq} =	T_{eq} = 669 (1 237)
Temperature allowance, °C (°F)	T_A =	T_A = 15 (25)
Design metal temperature, °C (°F)	T_d =	T_d = 685 (1 265)
Design life, h	—	t_{DL} = 100 000
Allowable stress at T_d, Figures E.1 to E.19, (Figures F.1 to F.19) MPa (psi)	σ_{el} =	σ_r = 46,6 (6 750)
Stress thickness, Equation (2) or (4), mm (in)	δ_σ =	δ_σ = 9,85 (0,388)
Corrosion allowance, mm (in)	δ_{CA} =	δ_{CA} = 3,18 (0,125)
Corrosion fraction, Figure 1, $n = 4{,}5$; $B = 0{,}322$	—	f_{corr} = 0,572
Minimum thickness, Equation (3) or (5), mm (in)	δ_{min} =	δ_{min} = 11,68 (0,460)
Calculation of equivalent tube metal temperature		
Duration of operating period, years	t_{op} =	1,0
Metal temperature, start of run, °C (°F)	T_{sor} =	635 (1 175)
Metal temperature, end of run, °C (°F)	T_{eor} =	690 (1 275)
Temperature change during operating period, K (°R)	ΔT^* =	55 (100)
Metal absolute temperature, start of run, K (°R)	T^*_{sor} =	908 (1 635)
Thickness change during operating period, mm (in)	$\Delta\delta$ =	0,33 (0,013)
Assumed initial thickness, mm (in)	δ_0 =	8,00 (0,315)
Corresponding initial stress, Equation (1), MPa (psi)	σ_0 =	58,1 (8 413)
Material constant, Table 3, MPa (psi)	A =	$1{,}23 \times 10^6$ ($1{,}79 \times 10^8$)
Rupture exponent at T_{sor}, Figures E.1 to E.19 (Figures F.1 to F.19)	n_0 =	4,8
Temperature fraction, Figure 2, $V = 2{,}9$; $N = 0{,}2$	f_T =	0,62
Equivalent metal temperature, Equation (6), °C (°F)	T_{eq} =	669 (1 237)

Figure 6 — Sample calculation for rupture design (changing temperature)

Annex A
(informative)

Estimation of remaining tube life

A.1 General

Figures E.1 to E.19 (Figures F.1 to F.19) and the considerations made in Annex G have applications other than for the design of new tubes. They can also be used to help answer re-rating and retirement questions about existing tubes that have operated in the creep-rupture range. This annex describes how tube damage and remaining life can be estimated. Because of the uncertainties involved in these calculations, decisions about tube retirement should not be based solely on the results of these calculations. Other factors such as tube thickness or diameter-strain measurements should be primary considerations in decisions about tube retirement.

There are three primary areas of uncertainty in these calculations. First, it is necessary to estimate the accumulated tube damage (the life fraction used up) based on the operating history, i.e. the influence from the operating pressure, the tube-metal temperature, and the corrosion rate, of the tube. The uncertainties in these factors, particularly the temperature, can have a significant effect on the estimate. Second, knowledge of the actual rupture strength of a given tube is not precise. The example calculation in Clause A.2 demonstrates the effects of this uncertainty. Finally, it is necessary to consider the tube-damage rule as described in Clause G.2. However, as mentioned in Clause G.2, the limitations of this hypothesis are not well understood. In spite of all these uncertainties, the estimation that is made using the procedure described in this annex can provide information that assists in making decisions about tube re-rating and retirement.

The essence of this calculation procedure can be outlined as follows. The operating history is divided into periods of time during which the pressure, metal temperature, and corrosion rate are assumed constant. For each of these periods, the life fraction used up is calculated. The sum of these calculated life fractions is the total accumulated tube damage. The fraction remaining is calculated by subtracting this sum from unity. Finally, the remaining life fraction is transformed into an estimate of the expected life at specified operating conditions.

A more detailed life-assessment evaluation for heater tubes operating in the creep-rupture range can be found in API RP 579:2000, Appendix F.

A.2 Estimation of accumulated tube damage

Since the concepts required to estimate damage are developed elsewhere in this International Standard, they are not repeated here. The calculation procedure can best be explained by working through an example. For this example, the following conditions are assumed:

Material:	18Cr-10Ni-Nb (type 347) stainless steel;
Outside diameter:	168,3 mm (6,625 in);
Initial minimum thickness:	6,8 mm (0,268 in).

It is also assumed that the operating history of the tube can be approximated as shown in Table A.1. (The SI conversions are approximate.)

It is not necessary that the operating periods be of uniform length. In an actual heater, neither the operating pressure nor the metal temperature is uniform. Nonetheless, for this calculation, they are assumed to be uniform during each period. The values chosen for each period should represent typical values. The choice of the length of the operating period depends on the extent of the variation of the pressure and temperature.

It is necessary to approximate the operating history for the tube thickness. This history can usually be developed from thickness measurements made before the initial start-up and during routine heater-tube inspections. For all of these estimates, it is assumed that the outside diameter remains constant.

Table A.1 — Approximation of the operating history

Operation period	**Duration**	**Operating gauge pressure**		**Tube metal temperature**		**Minimum thickness**			
						Beginning		**End**	
	a[a]	MPa	(psi)	°C	(°F)	mm	(in)	mm	(in)
1	1,3	3,96	(575)	649	(1 200)	6,81	(0,268)	6,40	(0,252)
2	0,6	4,27	(620)	665	(1 230)	6,40	(0,252)	6,20	(0,244)
3	2,1	4,07	(590)	660	(1 220)	6,20	(0,244)	5,51	(0,217)
4	2,0	4,34	(630)	665	(1 230)	5,51	(0,217)	4,83	(0,190)

[a] "a" is the international unit symbol for "year".

This information can be used to calculate the life fractions shown in Table A.2.

For tubes undergoing corrosion, an equation similar to Equation (G.17) can be developed for the life fraction; however, this is not necessary since sufficient accuracy can be achieved for this calculation by using the average stress for each period (that is, the average of the stress at the beginning and at the end of the operating period).

The minimum and average Larson-Miller values in Table A.2 are determined from the average stress using the Larson-Miller parameter curves for minimum and average rupture strength in Figures E.1 to E.19 (SI units) or Figures F.1 to F.19 (USC units). For this example, Figure F.17 was used.

With these Larson-Miller values and the metal temperature for each period, the expression for the Larson-Miller parameter was solved for the rupture time. This expression is at the top of Figures E.1 to E.19. Since this expression gives the rupture time in hours, the value needs converting to years. The resulting times based on the minimum rupture strength and the average rupture strength are shown in Table A.2.

The following example illustrates how to calculate the minimum-strength rupture time, t_{DL}, for the first operating period from the equations for $\delta_{\sigma,\mathrm{AVE}}$, the average stress thickness, and σ_{r}, the rupture allowable stress. The equations to be solved are as follows:

In SI units:

$$\delta_{\sigma,\mathrm{AVE}} = \frac{6{,}81 + 6{,}40}{2} = 6{,}605 \text{ mm}$$

In USC units:

$$\delta_{\sigma,\mathrm{AVE}} = \frac{0{,}268 + 0{,}252}{2} = 0{,}260 \text{ in}$$

In SI units:

$$\sigma_{\mathrm{r}} = \frac{1}{2}\left(\frac{p_{\mathrm{r}} D_{\mathrm{o}}}{\delta_{\sigma,\mathrm{AVE}}} - p_{\mathrm{r}}\right) = \frac{1}{2}\left(\frac{3{,}96 \times 168{,}3}{6{,}605} - 3{,}96\right) = 48{,}47 \text{ MPa}$$

In USC units:

$$\sigma_r = \frac{1}{2}\left(\frac{p_r D_o}{\delta_{\sigma,AVE}} - p_r\right) = \frac{1}{2}\left(\frac{575 \times 6,625}{0,260} - 575\right) = 7\,038 \text{ psi}$$

At 48,47 MPa, using the minimum rupture strength, the Larson-Miller Parameter, C_{LM}, equals 19,02 in SI units.

At 7 038 psi, using the minimum rupture strength, the Larson-Miller Parameter, C_{LM}, equals 34,35 in USC units.

In USC units:

$$C_{LM} = (T_d + 460)\ (15 + \lg t_{DL}) \times 10^{-3}$$

Therefore:

$$34,35 = (1\,200 + 460)\ (15 + \lg t_{DL}) \times 10^{-3}$$

$$\lg t_{DL} = 5,69$$

$$t_{DL} = 490\,000 \text{ hours}$$

$$t_{DL} = 55,9 \text{ years}$$

The life fractions are simply the duration of the operating period divided by the rupture time that corresponds to that period. Using the minimum-strength rupture time calculated above, the fraction for the first line in Table A.2 is 1,3/55,9, which equals 0,02. The accumulated damage is the sum of the fractions.

The effect of the uncertainty about the rupture strength is evident in Table A.2. If the actual rupture strength of this tube is in the lower part of the scatter band (near the minimum rupture strength), then 64 % of the tube life has been used. If the actual strength is in the middle of the scatter band (near the average rupture strength), then only 23 % of the tube life has been used. If the actual rupture strength is higher, even less of the tube life has been used.

The effect of the uncertainty about the operating temperature can also be evaluated. Suppose the actual metal temperature of this tube were 5 °C (9 °F) higher than that shown in Table A.1. To estimate the effect of this difference, the life-fraction calculations in Table A.2 have been made with the slightly higher temperature. The corresponding accumulated damage fractions are 0,81 and 0,28, respectively. These should be compared with the values 0,64 and 0,23 that were calculated first.

Table A.2 — Life fractions for each period

Operating period	**Average stress**		**Larson-Miller values**				**Rupture time based on minimum strength**		**Rupture time based on average strength**	
			minimum		average					
	MPa	psi	°C	(°F)	°C	(°F)	years	life fraction	years	life fraction
1	48,47	(7 038)	19,02	(34,35)	19,48	(35,09)	55,9	0,02	154,8	0,01
2	54,91	(7 973)	18,83	(33,90)	19,25	(34,66)	13,1	0,05	35,8	0,02
3	56,66	(8 213)	18,77	(33,80)	19,19	(34,55)	15,0	0,14	42,1	0,05
4	68,78	(9 985)	18,41	(33,15)	18,83	(33,90)	4,7	0,43	13,1	0,15
					Accumulated damage =			0,64		0,23

A.3 Estimation of remaining tube life

As in Clause A.2, this calculation procedure is best explained using an example. The example used is summarized in Tables A.3 and A.4. The life fraction remaining for this tube is as follows:

Minimum rupture strength: equals 1 minus 0,64, or 0,36;

Average rupture strength: equals 1 minus 0,23, or 0,77.

These fractions should be converted to the expected life under the specified operating conditions.

The following related questions can be asked at this point.

a) What is the estimated life at a given operating pressure, metal temperature, and corrosion rate?

b) For a specified operating pressure and corrosion rate, what temperature limit should be imposed for the tube to last a minimum period of time?

c) How much should the operating pressure or metal temperature be reduced to extend the expected life by a given percentage?

Not all of these questions are answered in this annex, but the method used to develop the answers should be clear from the following example.

For this example, the expected operating conditions are as follows:

Operating gauge pressure: 4,27 MPa (620 psi);

Metal temperature: 660 °C (1 220 °F);

Corrosion rate: 0,33 mm/a (0,013 in/a).

From these values, a table of future-life fractions can be developed as shown in Table A.3 for the minimum rupture strength and in Table A.4 for the average rupture strength. As before, the average stress is the average of the stresses at the beginning and end of each operating period.

Since the tube in the example is undergoing corrosion, the life estimation should be calculated in steps. For this example, a 1-year step was used. As can be seen from the two tables, the estimated life of this tube is between 1,5 a and 4,5 a. If the rupture strength were in the upper part of the scatter band (above the average rupture strength), the estimated life would be even longer.

For tubes that are not undergoing corrosion, estimating the life is easier. The rupture life is calculated, as above, from the anticipated stress and temperature. The estimated remaining life is simply the fraction remaining multiplied by the rupture life. In these cases, tables such as Tables A.3 and A.4 are not required.

The example given above describes a way to answer Question a), posed at the beginning of this subclause: What is the estimated life for a specified set of operating conditions? Question b), concerning the temperature limit that should be imposed for a specified pressure, corrosion rate, and minimum life, can be answered as follows. The pressure and corrosion rate can be used to calculate an average stress from which a Larson-Miller value can be found using the curves in Figures E.1 to E.19. With this value and a rupture life calculated by dividing the required life by the remaining life fraction, the Larson-Miller-parameter equation can be solved for the maximum temperature. The other questions can be answered in similar ways.

Table A.3 — Future life fractions, minimum rupture strength

Time	Minimum thickness		Average stress		Minimum Larson-Miller value		Rupture time	Fraction	Remaining Fraction
a	mm	(in)	MPa	(psi)	°C	(°F)	a		
0	4,83	(0,190)	—	—	—	—	—	—	0,36
1	4,50	(0,177)	74,99	(10 896)	18,25	(32,85)	4,1	0,24	0,12
1,5	4,34	(0,171)	79,19	(11 497)	18,14	(32,66)	3,1	0,16	−0,04

Table A.4 — Future life fractions, average rupture strength

Time	Minimum thickness		Average stress		Minimum Larson-Miller value		Rupture time	Fraction	Remaining fraction
a	mm	(in)	MPa	(psi)	°C	(°F)	a		
0	4,83	(0,190)	—	—	—	—	—	—	0,77
1	4,50	(0,177)	74,99	(10 896)	18,66	(33,60)	11,4	0,09	0,68
2	4,17	(0,164)	80,87	(11 753)	18,53	(33,35)	8,2	0,12	0,56
3	3,84	(0,151)	87,74	(12 752)	18,37	(33,07)	5,5	0,18	0,38
4	3,51	(0,138)	95,84	(13 932)	18,22	(32,80)	3,8	0,26	0,12
4,5	3,35	(0,132)	102,76	(14 940)	18,07	(32,53)	2,6	0,19	−0,07

Annex B
(informative)

Calculation of maximum radiant section tube skin temperature

B.1 General

This annex provides a procedure for calculating the maximum radiant section tube metal (skin) temperature. Correlations for estimating the fluid-film heat-transfer coefficient are given in Clause B.2. A method for estimating the maximum local heat-flux density is given in Clause B.3. The equations used to calculate the maximum tube skin temperature and the temperature distribution through the tube wall are described in Clause B.4. The sample calculation in Clause B.5 demonstrates the use of these equations.

The maximum tube metal temperature (TMT) might or might not be located towards the process outlet of a fired heater. Factors including inside film coefficient, radiant heat flux, heater/tube geometry and fluid flow regime all influence the maximum TMT calculation. In some cases, such as with vacuum heaters, a tube-by-tube analysis from the fluid outlet to before the initial boiling point (IBP) should be performed.

If the heater is required to operate in turndown or operating conditions other than design mode, the purchaser shall identify this on the data sheet. A review of these operations is required.

B.2 Heat-transfer coefficient

A value necessary for calculating the maximum tube metal temperature is the fluid heat-transfer coefficient at the inside wall of the tube. Although the following correlations are extensively used and accepted in heater design, they have inherent inaccuracies associated with all simplified correlations that are used to describe complex relationships.

For single-phase fluids, the heat-transfer coefficient is calculated by one of the two equations below, where Re is the Reynolds number and Pr is the Prandtl number. No correlation is included for the heat-transfer coefficient in laminar flow, since this flow regime is rare in process heaters. There is inadequate information for reliably determining the inside coefficient in laminar flow for oil in tube sizes that are normally used in process heaters.

The heat-transfer coefficient, K_l, expressed in W/(m²·K) [Btu/(h·ft²·°F)], for the liquid flow with $Re > 10\,000$ is calculated using Equation (B.1) from Reference [35]:

$$K_\mathrm{l} = 0{,}023 \left(\frac{\lambda_{\mathrm{f},T\mathrm{b}}}{D_\mathrm{i}} \right) Re^{0{,}8} Pr^{0{,}33} \left(\frac{\mu_{\mathrm{f},T\mathrm{b}}}{\mu_{\mathrm{f},T\mathrm{w}}} \right)^{0{,}14} \tag{B.1}$$

where

$$Re = \frac{D_\mathrm{i} q_{m\mathrm{A}}}{\mu_{\mathrm{f},T\mathrm{b}}} \tag{B.2}$$

$$Pr = \frac{c_p \, \mu_{\mathrm{f},T\mathrm{b}}}{\lambda_{\mathrm{f},T\mathrm{b}}} \tag{B.3}$$

$q_{m\mathrm{A}}$ is the areic mass flow rate, in kg/(m²·s) [lb/(ft²·h)], of the fluid;

c_p is the specific heat capacity, in J/(kg·K) [Btu/(lb·°R)], of the fluid at bulk temperature.

$\lambda_{f,Tb}$ is the thermal conductivity, expressed in W/(m·K) [Btu/(h·ft²·°F)], of the fluid at bulk temperature;

D_i is the inside diameter, expressed in metres (feet), of the tube;

$\mu_{f,Tb}$ is the absolute viscosity, in Pa·s [lb/(ft·h)], of the fluid at bulk temperature;

$\mu_{f,Tw}$ is the absolute viscosity, in Pa·s [lb/(ft·h)], of the fluid at wall temperature.

The heat-transfer coefficient, K_v, expressed in W/(m²·K) [Btu/(h·ft²·°F)], for the vapour flow with $Re > 15\,000$ is calculated using Equation (B.4) from Reference [36]:

$$K_v = 0{,}021 \left(\frac{\lambda_{f,Tb}}{D_i} \right) Re^{0{,}8} Pr^{0{,}4} \left(\frac{T_b}{T_w} \right)^{0{,}5} \tag{B.4}$$

where

T_b is the absolute bulk temperature, expressed in kelvin (degrees Rankine), of the vapour;

T_w is the absolute wall temperature, expressed in kelvin (degrees Rankine), of the vapour.

All of the material properties except $\mu_{f,Tw}$ are evaluated at the bulk fluid temperature. To convert absolute viscosity in millipascal-seconds to pounds per foot per hour, multiply $\mu_{f,Tw}$ by 2,42.

For two-phase flows, the heat-transfer coefficient can be approximated using Equation (B.5):

$$K_{2p} = K_l w_l + K_v w_v \tag{B.5}$$

where

K_{2p} is the heat-transfer coefficient, expressed in W/(m²·K) [Btu/(h·ft²·°F)], for two phases;

w_l is the mass fraction of the liquid;

w_v is the mass fraction of the vapour.

The liquid and vapour heat-transfer coefficients, K_l and K_v, should be calculated using the mixed-phase areic mass flow rate but using the liquid and the vapour material properties, respectively.

NOTE In two-phase flow applications where dispersed-flow or mist-flow regimes occur due to entrainment of tiny liquid droplets in the vapour (e.g. towards the outlet of vacuum heaters), the heat-transfer coefficient can be calculated using the correlation for the vapour phase using Equation (B.4), based on the total flow rate, rather than being approximated by Equation (B.5).

B.3 Maximum local heat-flux density

The average heat-flux density in the radiant section of a heater (or in a zone of the radiant section) is equal to the duty in the section or zone divided by the total outside surface area of the coil in the section or zone. The maximum local heat-flux density at any point in the coil can be estimated from the average heat-flux density. The maximum local heat-flux density is used with the equations in Clause B.4 to calculate the maximum tube metal temperature.

Local heat-flux densities vary considerably throughout a heater because of non-uniformities around and along each tube. Circumferential variations result from variations in the radiant heat-flux density produced by shadings of other tubes or from the placement of the tubes next to a wall. Conduction around the tubes and convection flows of flue gases tend to reduce the circumferential variations in the heat-flux density. The longitudinal variations result from the proximity to burners and variations in the radiant firebox and the bulk fluid temperatures. In addition to variations in the radiant section, the tubes in the shock section of a heater can have a high convective heat-flux density.

The maximum radiant heat-flux density, $q_{\mathrm{R,max}}$, expressed in W/m^2 [Btu/(h·ft^2)], for the outside surface at any point in a coil can be estimated from Equation (B.6):

$$q_{\mathrm{R,max}} = F_{\mathrm{cir}} F_{\mathrm{L}} F_T q_{\mathrm{R,ave}} + q_{\mathrm{conv}} \tag{B.6}$$

where

F_{cir} is the factor accounting for circumferential heat-flux-density variations;

F_{L} is the factor accounting for longitudinal heat-flux-density variations;

F_T is the factor accounting for the effect of tube metal temperature on the radiant heat-flux density;

$q_{\mathrm{R,ave}}$ is the average radiant heat-flux density, in W/m^2 [Btu/(h·ft^2)], for the outside surface;

q_{conv} is the average convective heat-flux density, in W/m^2 [Btu/(h·ft^2)], for the outside surface.

The circumferential variation factor, F_{cir}, is given as a function of tube spacing and coil geometry in Figure B.1. The factor given by this figure is the ratio of the maximum local heat-flux density at the fully exposed face of a tube to the average heat-flux density around the tube. This figure was developed from considerations of radiant heat transfer only. As mentioned above, influences such as conduction around the tube and flue-gas convection act to reduce this factor. Since these influences are not included in this calculation, the calculated value is somewhat higher than the actual maximum heat-flux density.

The longitudinal variation factor, F_{L}, is not easy to quantify. Values between 1,0 and 1,5 are most often used. In a firebox that has a very uniform distribution of heat-flux density, a value of 1,0 can be appropriate. Values greater than 1,5 can be appropriate in a firebox that has an extremely uneven distribution of heat-flux density (for example, a long or a tall, narrow firebox with burners in one end only).

The tube metal temperature factor, F_T, is less than 1,0 near the coil outlet or in areas of maximum tube metal temperature. It is greater than 1,0 in areas of lower tube metal temperatures. For most applications, the factor can be approximated as given in Equation (B.7):

$$F_T = \left(\frac{T_{\mathrm{g,ave}}^{*\,4} - T_{\mathrm{tm}}^{*\,4}}{T_{\mathrm{g,ave}}^{*\,4} - T_{\mathrm{tm,ave}}^{*\,4}} \right) \tag{B.7}$$

where

$T_{\mathrm{g,ave}}^{*}$ is the average flue-gas temperature, expressed in kelvin (degrees Rankine), in the radiant section;

T_{tm}^{*} is the tube metal temperature, expressed in kelvin (degrees Rankine), at the point under consideration;

$T_{\mathrm{tm,ave}}^{*}$ is the average tube metal temperature, expressed in kelvin (degrees Rankine), in the radiant section.

The convective heat-flux density in most parts of a radiant section is usually small compared with the radiant heat-flux density. In the shock section, however, the convective heat-flux density can be significant; it should therefore be added to the radiant heat-flux density when the maximum heat-flux density in the shock section is estimated.

B.4 Maximum tube metal temperature

In addition to the heat-transfer coefficient and the maximum heat-flux density, the temperature profile of the fluid in the coil is necessary for calculating the maximum tube metal temperature in the radiant section of the heater. This profile, which is often calculated by the heater supplier, defines the variation of the bulk fluid temperature through the heater coil. For operation at or near design, the design profile can be used. For operation significantly different from design, a bulk temperature profile shall be developed.

Once the bulk fluid temperature is known at any point in the coil, the maximum tube metal temperature, T_{max}, expressed in degrees Celsius (Fahrenheit), can be calculated from Equations (B.8) to (B.12):

$$T_{max} = T_{bf} + \Delta T_{ff} + \Delta T_{f} + \Delta T_{tw} \tag{B.8}$$

where

T_{bf} is the bulk fluid temperature, expressed in degrees Celsius (Fahrenheit);

ΔT_{f} is the temperature difference across any internal fouling, expressed in degrees Celsius (Fahrenheit);

ΔT_{ff} is the temperature difference across the fluid film, expressed in degrees Celsius (Fahrenheit);

ΔT_{tw} is the temperature difference across the tube wall, expressed in degrees Celsius (Fahrenheit).

$$\Delta T_{ff} = \frac{q_{R,max}}{K_{ff}} \left(\frac{D_o}{D_i} \right) \tag{B.9}$$

where

K_{ff} is the fluid-film heat-transfer coefficient, expressed in W/(m^2·K) [Btu/(h·ft^2·°F)];

$q_{R,max}$ is the maximum radiant heat-flux density, expressed in W/(m^2·K) [Btu/(h·ft^2·°F)], for the outside surface;

D_o is the outside diameter, expressed in metres (feet), of the tube;

D_i is the inside diameter, expressed in metres (feet), of the tube.

$$\Delta T_{f} = q_{R,max} R_{f} \left(\frac{D_o}{D_i - \delta_f} \right) \tag{B.10}$$

where

δ_f is the coke and/or scale thickness, expressed in metres (feet);

R_f is the fouling factor inside the tube due to the presence of any internal fouling, coke or scale, expressed in m^2·K/W (h·ft^2 °F/Btu).

$$\Delta T_{\text{tw}} = q_{\text{R,max}} \left(\frac{D_{\text{o}} \ln\left(\frac{D_{\text{o}}}{D_{\text{i}}}\right)}{2\lambda_{\text{tm}}} \right) \tag{B.11}$$

where

λ_{tm} is the thermal conductivity, expressed in W/(m·K) [Btu/(h·ft·°F)], of the tube metal.

The effect of internal fouling on the tube metal temperature can be calculated if a fouling factor rather than coke thickness has been provided on the fired heater data sheets (see ISO 13705). The fouling factor, R_{f}, may also be expressed as a function of coke or scale thickness and thermal conductivity, as given in Equation (B.12), if only coke or scale thickness is provided:

$$R_{\text{f}} = \frac{\delta_{\text{f}}}{\lambda_{\text{f}}} \tag{B.12}$$

where

δ_{f} is the coke and/or scale thickness, expressed in metres (feet).

λ_{f} is the thermal conductivity of coke or scale, expressed in W/(m^2·K) [Btu/h·ft·°F];

If a thickness for a layer of coke or scale is specified, the effective inside diameter of the tube is adjusted as noted in Equation (B.10). The effects of internal fouling, coke or scale on tube metal temperature can be calculated using Equations (B.8) and (B.10).

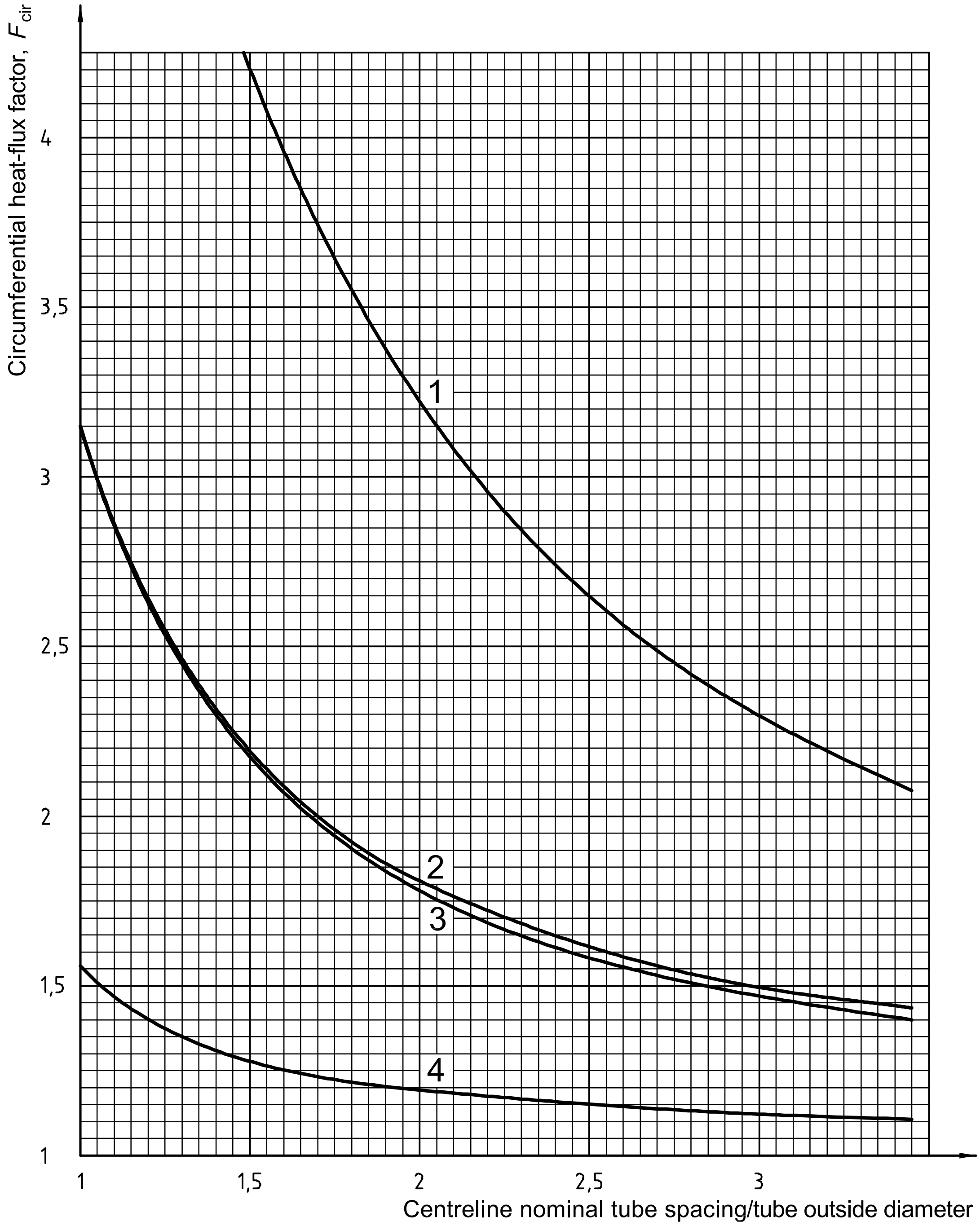
Equation (B.13) should be used to calculate the maximum fluid-film temperature coincident with maximum radiant heat flux, T_{fm}, expressed in degrees Celsius (Fahrenheit).

$$T_{\text{fm}} = T_{\text{bf}} + \Delta T_{\text{ff}} \tag{B.13}$$

The maximum film temperature shall be calculated on a clean-tube basis except when evaluating the maximum tube metal temperature.

In the absence of thermal conductivity data provided by the purchaser, the following range of values may be used for petroleum coke: 4,91 W/m·K to 5.89 W/m·K (2.8 Btu/h·ft·°F to 3.4 Btu/h·ft·°F).

The thermal conductivity of the tube material, λ_{tm}, used in Equation (B.11) should be evaluated at the average tube wall temperature.



Key

1 curve 1 for a double row against a wall, triangular spacing

2 curve 2 for a double row with equal radiation from both sides and two diameters between rows, equilateral spacing

3 curve 3 for a single row against a wall

4 curve 4 for a single row with equal radiation from both sides

These curves are valid when used with a tube-centre-to-refractory-wall spacing of 1,5 times the nominal tube diameter. Any appreciable variation from this spacing should be given special consideration.

NOTE 1 These curves do not take into consideration the convection heat transfer to the tubes, circumferential heat transfer by conduction through the tube wall, or variations in heat-flux density in different zones of the radiant section.

NOTE 2 These curves are based on the work of H. C. Hottel. Applicable papers are identified in Reference [35], page 69.

Figure B.1 — Ratio of maximum local to average heat flux

B.5 Sample calculation

The following sample calculation demonstrates how to use the equations given in Clauses B.2 to B.4.

NOTE Differences in results between calculations in SI and USC units for dimensionless numbers are due to the significant figures used in the dimension conversions.

In the heater under consideration, the medium-carbon-steel tubes are in a single row against the wall. Other aspects of the heater configuration are as follows:

Tube spacing is 203,2 mm (= 0,667 ft = 8,0 in).

D_o = 114,3 mm (= 0,375 ft = 4,5 in);

$\delta_{t,ave}$ = 6,4 mm (= 0,020 8 ft = 0,25 in);

D_i = 101,6 mm (= 0,333 ft = 4,0 in);

δ_f = 0 mm (0 in);

λ_{tm} = 42,2 W/(m·K) [24,4 Btu/(h·ft·°F)] at an assumed tube metal temperature of 380 °C (720 °F).

The flow in the tubes is two-phase with 10 % mass vapour. Other operating conditions are as follows:

Flow rate (total liquid plus vapour) is 6,3 kg/s (50 000 lb/h).

T_b = 271 °C (520 °F);

$q_{R,ave}$ = 31 546 W/m^2 [10 000 Btu/(h·ft^2)].

The properties of the liquid at the bulk temperature are as follows:

$\mu_{f,Tb} = 2,0 \times 10^{-3}$ Pa·s [4,84 lb/(h·ft)];

$\lambda_{f,Tb}$ = 0,116 3 W/(m·K) [0,067 2 Btu/(h·ft·°F)];

$c_{p,f}$ = 2 847 J/(kg·K) [0,68 Btu/(lb·°F)].

The properties of the vapour at the bulk temperature are as follows:

$\mu_{v,Tb} = 7,0 \times 10^{-6}$ Pa·s [0,017 lb/(ft·h)];

$\lambda_{v,Tb}$ = 0,034 6 W/(m·K) [0,020 Btu/(h·ft·°F)];

$c_{p,v}$ = 2 394 J/(kg·K) [0,572 Btu/(lb·°F)].

From the inside diameter, the flow area is equal to $8,107 \times 10^{-3}$ m^2 (0,087 3 ft^2). Using the total flow rate:

$q_{mA} = 6,3/(8,107 \times 10^{-3})$,

q_{mA} = 777,1 kg/(m^2·s).

In USC units:

q_{mA} = (50 000/0,087 3),

$q_{mA} = 5,73 \times 10^5$ lb/(h·ft^2).

The Reynolds number [Equation (B.2)] is calculated as follows:

For liquid:

In SI units:

$$Re = \frac{(0{,}1016)\quad(777{,}1)}{0{,}002} = 3{,}95 \times 10^{4}$$

In USC units:

$$Re = \frac{(0{,}333)\quad(5{,}73 \times 10^{5})}{4{,}84} = 3{,}94 \times 10^{4}$$

For vapour

In SI units:

$$Re = \frac{(0{,}1016)\quad(777{,}1)}{7{,}0 \times 10^{-6}} = 1{,}13 \times 10^{7}$$

In USC units:

$$Re = \frac{(0{,}333)\quad(5{,}73 \times 10^{5})}{0{,}017} = 1{,}12 \times 10^{7}$$

The Prandtl number [Equation (B.3)] is calculated as follows:

For liquid:

In SI units:

$$Pr = \frac{(2\,847)\quad(0{,}002)}{0{,}116\,3} = 49{,}0$$

In USC units:

$$Pr = \frac{(0{,}68)\quad(4{,}84)}{0{,}067\,2} = 49{,}0$$

For vapour:

In SI units:

$$Pr = \frac{(2\,395)\quad(7{,}0 \times 10^{-6})}{0{,}034\,6} = 0{,}485$$

In USC units:

$$Pr = \frac{(0{,}572)\quad(0{,}017)}{0{,}020} = 0{,}486$$

Assume that for the liquid:

$$\left(\frac{\mu_{\mathrm{f},T\mathrm{b}}}{\mu_{\mathrm{f},T\mathrm{w}}}\right)^{0{,}14} = 1{,}1$$

Assume that for the vapour:

$$\left(\frac{T_{\mathrm{b}}}{T_{\mathrm{w}}}\right)^{0,5} = 0,91$$

These assumptions will be checked later. Using Equation (B.1):

$$K_{\mathrm{l}} = 0,023\left(\frac{\lambda_{\mathrm{f},T\mathrm{b}}}{D_{\mathrm{i}}}\right)(3,94\times10^{4})^{0,8}(49,0)^{0,33}(1,1)$$

$$= 433,8\left(\frac{\lambda_{\mathrm{f},T\mathrm{b}}}{D_{\mathrm{i}}}\right)$$

Using Equation (B.4):

$$K_{\mathrm{v}} = 0,021\left(\frac{\lambda_{\mathrm{f},T\mathrm{b}}}{D_{\mathrm{i}}}\right)(1,12\times10^{7})^{0,8}(0,486)^{0,4}(0,91)$$

$$= 6\ 242\left(\frac{\lambda_{\mathrm{f},T\mathrm{b}}}{D_{\mathrm{i}}}\right)$$

Hence:

In SI units:

$$K_{\mathrm{l}} = 433,8\left(\frac{0,116\ 3}{0,101\ 6}\right) = 497\ \mathrm{W/m^2\cdot K}$$

$$K_{\mathrm{v}} = 6\ 242\left(\frac{0,034\ 6}{0,101\ 6}\right) = 2\ 126\ \mathrm{W/m^2\cdot K}$$

In USC units:

$$K_{\mathrm{l}} = 433,8\left(\frac{0,067\ 2}{0,333}\right) = 87,5\ \mathrm{Btu/h\cdot ft^2\cdot {}^\circ F}$$

$$K_{\mathrm{v}} = 6\ 242\left(\frac{0,020}{0,333}\right) = 375\ \mathrm{Btu/h\cdot ft^2\cdot {}^\circ F}$$

The two-phase heat-transfer coefficient can then be calculated using Equation (B.5):

In SI units:

$$K_{2\mathrm{p}} = (0,90)K_{\mathrm{l}} + (0,10)K_{\mathrm{v}}$$

$$= (0,90)(497) + (0,10)(2\ 126)$$

$$= 659,9\ \mathrm{W/(m^2\cdot K)}$$

In USC units:

$$K_{2\mathrm{p}} = (0,90)(87,5) + (0,10)(375)$$

$$= 116,3\ \mathrm{Btu/(h\cdot ft^2\ {}^\circ F)}$$

The ratio of tube spacing to tube diameter is as follows:

In SI units:

$$\frac{203,2}{114,3} = 1,78$$

In USC units:

$$\frac{8,0}{4,5} = 1,78$$

From Figure B.1, F_{cir} = 1,91. Assume that for this heater, F_L = 1,1, F_T = 1,0, and q_{conv} = 0 (that is, there is no convective heat-flux density at this point). Using Equation (B.6):

In SI units:

$$q_{R,max} = (1,91)(1,1)(1,0)(31\ 546)$$

$$= 66\ 278\ \text{W/m}^2$$

In USC units:

$$q_{R,max} = (1,91)(1,1)(1,0)(10\ 000)$$

$$= 21\ 010\ \text{Btu/(h·ft}^2)$$

The temperature difference through each part of the system can now be calculated from Equation (B.9) for the fluid film:

In SI units:

$$\Delta T_{ff} = \left(\frac{66\ 278}{659,9}\right)\left(\frac{114,3}{101,6}\right) = 113\ \text{K}$$

In USC units:

$$\Delta T_{ff} = \left(\frac{21\ 010}{116,3}\right)\left(\frac{0,375}{0,333}\right) = 203\ °\text{R}$$

From Equation (B.11) for the tube wall:

In SI units:

$$\Delta T_{tw} = 66\ 278\left[\frac{114,3\ \ln(114,3/101,6)}{2(42,2)}\right] \times 10^{-3} = 11\ \text{K}$$

In USC units:

$$\Delta T_{tw} = 21\ 028\left[\frac{0,375\ \ln(0,375/0,333)}{2(24,4)}\right] = 19\ °\text{R}$$

Using Equation (B.8), the maximum tube metal temperature is as follows:

In SI units:

$$T_{max} = 271 + 113 + 11 = 395\ °\text{C}$$

In USC units:

$$T_{max} = 520 + 203 + 19 = 742\ °F$$

Checking the assumed viscosity ratio, at the oil-film temperature calculated above, 271 + 113 = 384 °C (520 + 203 = 723 °F), the viscosity is 1,1 mPa·s (2,66 lb/ft). So, for the liquid:

In SI units:

$$\left(\frac{\mu_{f,Tb}}{\mu_{f,Tw}}\right)^{0,14} = \left(\frac{0,002\ 0}{0,0011}\right)^{0,14} = (1,82)^{0,14} = 1,09$$

In USC units:

$$\left(\frac{\mu_{f,T\,b}}{\mu_{f,T\,w}}\right)^{0,14} = \left(\frac{4,84}{2,66}\right)^{0,14} = (1,82)^{0,14} = 1,09$$

For the vapour:

In SI units:

$$\left(\frac{T_b}{T_w}\right)^{0,5} = \left(\frac{270 + 273}{384 + 273}\right)^{0,5} = (0,83)^{0,5} = 0,91$$

In USC units:

$$\left(\frac{T_b}{T_w}\right)^{0,5} = \left(\frac{520 + 460}{723 + 460}\right)^{0,5} = (0,83)^{0,5} = 0,91$$

Both values are close to the values assumed for the calculation of K_l and K_v, so no additional work is needed.

The mean tube wall temperature is as follows:

In SI units:

$$270 + 113 + \frac{11}{2} = 388\ °C$$

In USC units:

$$520 + 203 + \frac{19}{2} = 732\ °F$$

This is close to the temperature assumed for the tube conductivity, so no additional work is required.

Annex C
(normative)

Thermal-stress limitations (elastic range)

C.1 General

In heater tubes, the thermal stress of greatest concern is the one developed by the radial distribution of temperature through the thickness. This stress can become particularly significant in thick stainless steel tubes exposed to high heat flux densities.

There are two limits for thermal stress; both are described in Reference [21], paragraphs 4-134 and 5-130. These limits apply only in the elastic range; in the rupture range, an appropriate limit for thermal stress has not been established.

C.2 Equation for thermal stress

The following equation gives the maximum thermal stress, $\sigma_{T\max}$, in a tube:

$$\sigma_{T\max} = X\left[\left(\frac{2y^2}{y^2-1}\right)\ln y - 1\right] \qquad \text{(C.1)}$$

where

$$X = \left[\frac{\alpha E}{2\,(1-\nu)}\right]\left[\frac{\Delta T}{\ln y}\right] = \left[\frac{\alpha E}{4\,(1-\nu)}\right]\left[\frac{q_o D_o}{\lambda_s}\right] \qquad \text{(C.2)}$$

α is the coefficient of thermal expansion;

E is the modulus of elasticity;

ν is Poisson's ratio;

ΔT is the temperature difference across the tube wall;

y is D_o/D_i, ratio of outside diameter to actual inside diameter;

q_o is the heat-flux density on the outside surface of the tube;

λ_s is the thermal conductivity of the steel.

The material properties α, E, ν, and λ_s shall be evaluated at the mean temperature of the tube wall. The average wall thickness shall also be used in this equation (see 4.7).

C.3 Limits on thermal stress

The limitation, $\sigma_{T,\text{lim1}}$, on primary plus secondary stress intensity of Reference [21], paragraph 4-134, can be approximated for thermal stress as given in Equations (C.3) and (C.4) (see Clause C.4 for the derivation).

For ferritic steels:

$$\sigma_{T,\text{lim1}} = (2{,}0 - 0{,}67y)\ \sigma_\text{y} \qquad \text{(C.3)}$$

For austenitic steels:

$$\sigma_{T,\text{lim1}} = (2{,}7 - 0{,}90y)\ \sigma_\text{y} \qquad \text{(C.4)}$$

where σ_y is the yield strength.

The thermal-stress ratchet limit, $\sigma_{T,\text{lim2}}$, of Reference [21], paragraph 5-130, can be approximated for thermal stress as given in Equations (C.5) and (C.6) (see Clause C.5 for derivation).

For ferritic steels:

$$\sigma_{T,\text{lim2}} = 1{,}33\sigma_\text{y} \qquad \text{(C.5)}$$

For austenitic steels:

$$\sigma_{T,\text{lim2}} = 1{,}8\sigma_\text{y} \qquad \text{(C.6)}$$

Both the primary plus secondary stress limit ($\sigma_{T,\text{lim1}}$) and the thermal-stress ratchet limit ($\sigma_{T,\text{lim2}}$) shall be met if the tube is designed for the elastic range.

C.4 Derivation of limits on primary plus secondary stress intensity

The limit on primary plus secondary stress intensity can be expressed symbolically as given by the inequality in Equation (C.7):

$$\sigma_\text{pl} + \sigma_\text{pb} + \sigma_\text{cir,max} < 3\ \sigma_\text{m} \qquad \text{(C.7)}$$

where

$\sigma_\text{cir,max}$ is the maximum circumferential thermal stress which, for this application, is the maximum thermal stress given by equation (C.1);

σ_pl is the local primary membrane stress;

σ_pb is the primary bending stress.

From Reference [21], for tubes with an internal pressure:

$$\sigma_\text{pl} + \sigma_\text{pb} = p_\text{el}\left(\frac{2y^2}{y^2 - 1}\right) \qquad \text{(C.8)}$$

where

p_el is the elastic design pressure;

y is the ratio of outside to actual inside diameter, equal to D_o/D_i.

If the primary membrane stress intensity, σ_{pm}, is given by Equation (C.9),

$$\sigma_{pm} = \frac{p_{el}}{2}\left(\frac{D_o}{\delta} - 1\right) = \frac{p_{el}}{2}\left(\frac{y+1}{y-1}\right) \tag{C.9}$$

it can, then, be easily shown that Equation (C.10), gives a first approximation and provides an upper bound:

$$\sigma_{pl} + \sigma_{pb} \cong y\sigma_{pm} \tag{C.10}$$

In Reference [21], σ_m is the allowable membrane stress intensity. For ferritic steels above about 340 °C (650 °F), σ_m is equal to two-thirds of the yield strength, σ_y, as given in Equation (C.11):

$$3\ \sigma_m = 2\ \sigma_y \tag{C.11}$$

For austenitic steels above about 260 °C (500 °F), σ_m is 90 % of σ_y, as given in Equation (C.12):

$$3\ \sigma_m = 2{,}7\ \sigma_y \tag{C.12}$$

Heater tubes usually operate above these temperatures.

Combining all of this, the primary plus secondary stress intensity limit on thermal stress can be expressed as given in Equations (C.13) and (C.14):

For ferritic steels

$$\sigma_{T,lim1} = 2\sigma_y - y\sigma_{pm} \tag{C.13}$$

For austenitic steels

$$\sigma_{T,lim1} = 2{,}7\sigma_y - y\sigma_{pm} \tag{C.14}$$

where $\sigma_{T,lim1}$ is the maximum value permitted for the thermal stress, σ_T.

For ferritic-steel and austenitic-steel heater tubes designed according to this International Standard, the inequalities in Equations (C.15) and (C.16), respectively, hold:

$$\sigma_{pm} < 0{,}67\sigma_y \tag{C.15}$$

$$\sigma_{pm} < 0{,}90\sigma_y \tag{C.16}$$

The thermal-stress limit, $\sigma_{T,lim1}$, can therefore be approximated as given in Equations (C.17) and (C.18):

For ferritic steels:

$$\sigma_{T,lim1} = (2{,}0 - 0{,}67y)\sigma_y \tag{C.17}$$

For austenitic steels:

$$\sigma_{T,lim1} = (2{,}7 - 0{,}90y)\sigma_y \tag{C.18}$$

The limits expressed by these equations are simple and appropriate. If the thermal stress is less than this limit, the design is appropriate. If the thermal stress exceeds the limit given by these equations, then, the more exact form of Equation (C.13) or (C.14) shall be used with the primary membrane stress intensity given by Equation (C.9). Also, if the tube thickness is arbitrarily increased over the thickness calculated in 4.3, then the primary membrane stress intensity shall be calculated using the actual average thickness, and Equation (C.13) or Equation (C.14) shall be used to calculate the thermal-stress limit.

C.5 Derivation of limits on thermal-stress ratchet

The limit, $\sigma_{T,\text{lim2}}$, set to avoid thermal-stress ratchet can be expressed as given in Equation (C.19) [21]:

$$\sigma_{T,\text{lim2}} = 4(\sigma - \sigma_{\text{pm}}) \tag{C.19}$$

For ferritic steels:

$$\sigma = \sigma_{\text{y}} \tag{C.20}$$

For austenitic steels above about 260 °C (500 °F):

$$\sigma = 1{,}5\ (0{,}9\ \sigma_{\text{y}}) = 1{,}35\ \sigma_{\text{y}} \tag{C.21}$$

As before, σ_{pm} is derived from Equation (C.9). Using the inequalities in Equation (C.15) or Equation (C.16), this limit can be approximated as given in Equations (C.22) and (C.23):

For ferritic steels:

$$\sigma_{T,\text{lim2}} = 1{,}33\ \sigma_{\text{y}} \tag{C.22}$$

For austenitic steels:

$$\sigma_{T,\text{lim2}} = 1{,}8\ \sigma_{\text{y}} \tag{C.23}$$

As with the limits developed in C.4, these limits are approximate. If the thermal stress exceeds this limit or if the tube thickness is arbitrarily increased, the exact limit expressed by Equation (C.19) shall be used with the primary membrane stress intensity given by Equation (C.9).

Annex D
(informative)

Calculation sheets

This annex contains calculation sheets that are useful in aiding and documenting the calculation of minimum thickness and equivalent tube metal temperature. Individual sheets are provided for calculations in SI units or in USC units. These calculation sheets may be reproduced.

ISO 13704 CALCULATION SHEET SI units		
Heater ____________ Plant ____________ Refinery ____________ Coil Material ASTM Spec.		
Calculation of minimum thickness	**Elastic design**	**Rupture design**
Outside diameter, mm	D_o =	D_o =
Design pressure, MPa (gauge)	p_{el} =	p_r =
Maximum or equivalent metal temperature, °C	T_{max} =	T_{max} =
Temperature allowance, °C	T_A =	T_A =
Design metal temperature, °C	T_d =	T_d =
Design life, h	—	t_{DL} =
Allowable stress at T_d, Figures E.1 to E.19, MPa	σ_{el} =	σ_r =
Stress thickness, Equation (2) or (4), mm	δ_σ =	δ_σ =
Corrosion allowance, mm	δ_{CA} =	δ_{CA} =
Corrosion fraction, Figure 1, $n =$; $B =$	—	f_{corr} =
Minimum thickness, Equation (3) or (5), mm	δ_{min} =	δ_{min} =
Calculation of equivalent tube metal temperature		
Duration of operating period, years	t_{op} =	
Metal temperature, start of run, °C	T_{sor} =	
Metal temperature, end of run, °C	T_{eor} =	
Temperature change during operating period, K	ΔT =	
Metal absolute temperature, start of run, K	T^*_{sor} =	
Thickness change during operating period, mm	$\Delta\delta$ =	
Assumed initial thickness, mm	δ_0 =	
Corresponding initial stress, Equation (1), MPa	σ_0 =	
Material constant, Table 3, MPa	A =	
Rupture exponent at T_{sor1}, Figures E.1 to E.19	n_0 =	
Temperature fraction, Figure 2, $V =$; $N =$	f_T =	
Equivalent tube metal temperature, Equation (6), °C	T_{eq} =	

ISO 13704 CALCULATION SHEET (USC units)		
Heater ______ Plant ______ Refinery ______		
Coil Material ASTM Spec.		
Calculation of minimum thickness	**Elastic design**	**Rupture design**
Outside diameter, in	$D_o =$	$D_o =$
Design pressure, psi (gauge)	$p_{el} =$	$p_r =$
Maximum or equivalent metal temperature, °F	$T_{max} =$	$T_{max} =$
Temperature allowance, °F	$T_A =$	$T_A =$
Design metal temperature, °F	$T_d =$	$T_d =$
Design life, h	—	$t_{DL} =$
Allowable stress at T_d, Figures F.1 to F.19, psi	$\sigma_{el} =$	$\sigma_r =$
Stress thickness, Equation (2) or (4), in	$\delta_\sigma =$	$\delta_\sigma =$
Corrosion allowance, in	$\delta_{CA} =$	$\delta_{CA} =$
Corrosion fraction, Figure 1, $n =$; $B =$	—	$f_{corr} =$
Minimum thickness, Equation (3) or (5), in	$\delta_{min} =$	$\delta_{min} =$
Calculation of equivalent tube metal temperature		
Duration of operating period, years	$t_{op} =$	
Metal temperature, start of run, °F	$T_{sor} =$	
Metal temperature, end of run, °F	$T_{eor} =$	
Temperature change during operating period, °R	$\Delta T =$	
Metal absolute temperature, start of run, °R	$T^*_{sor} =$	
Thickness change during operating period, in	$\Delta\delta =$	
Assumed initial thickness, in	$\delta_0 =$	
Corresponding initial stress, Equation (1), psi	$\sigma_0 =$	
Material constant, Table 3, psi	$A =$	
Rupture exponent at T_{sor1}, Figures F.1 to F.19	$n_0 =$	
Temperature fraction, Figure 2, $V =$; $N =$	$f_T =$	
Equivalent tube metal temperature, Equation (6), °F	$T_{eq} =$	

Annex E
(normative)

Stress curves (SI units)

Stress curves, given in SI units, are presented in Figures E.1 to E.19.

List of Figures (SI units)

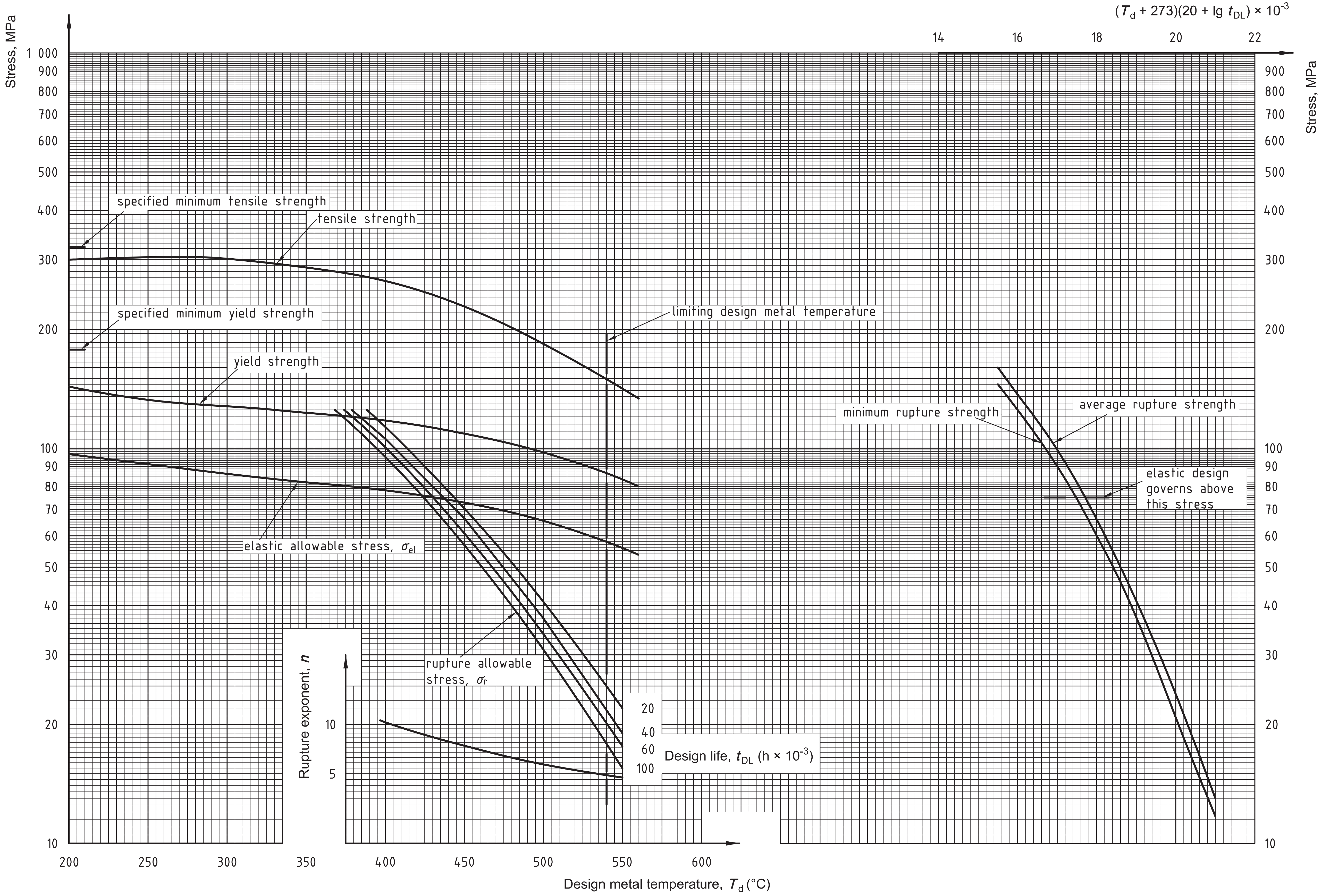


Figure E.1 — Stress curves (SI units) for ASTM A 161 and ASTM A 192 low-carbon steels

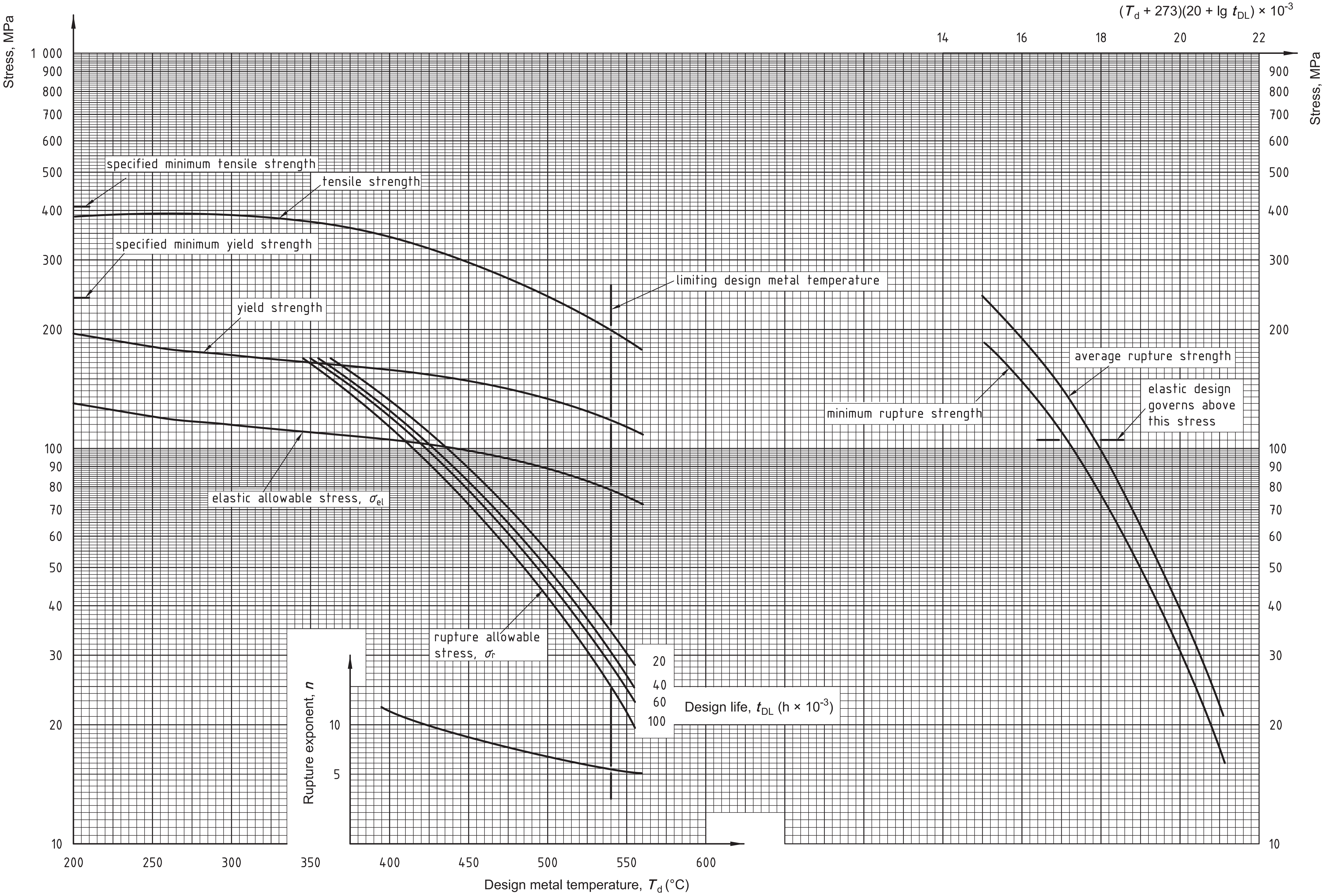


Figure E.2 — Stress curves (SI units) for ASTM A 53 Grade B (seamless), ASTM A 106 Grade B and ASTM 210 Grade A-1 medium-carbon steels

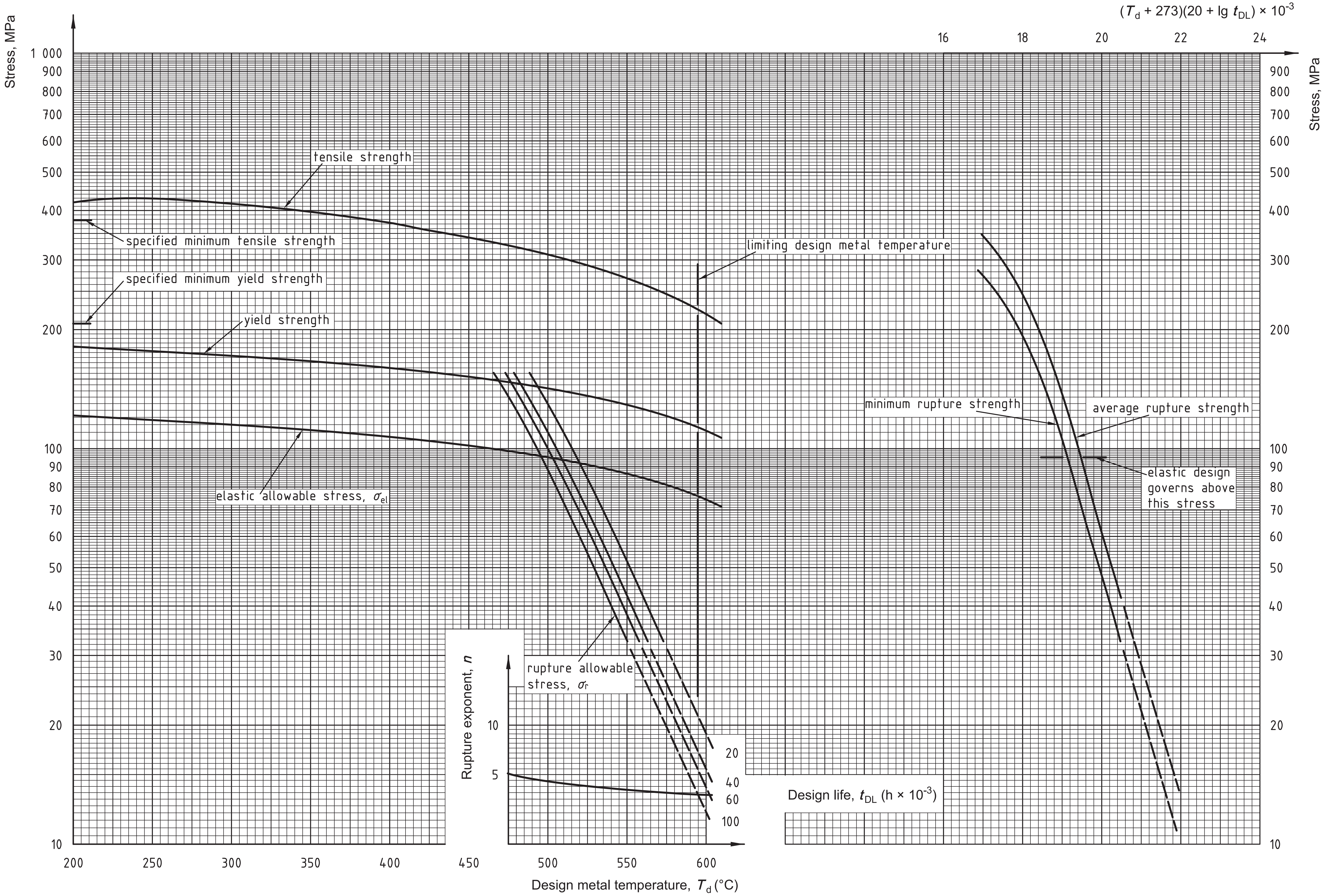
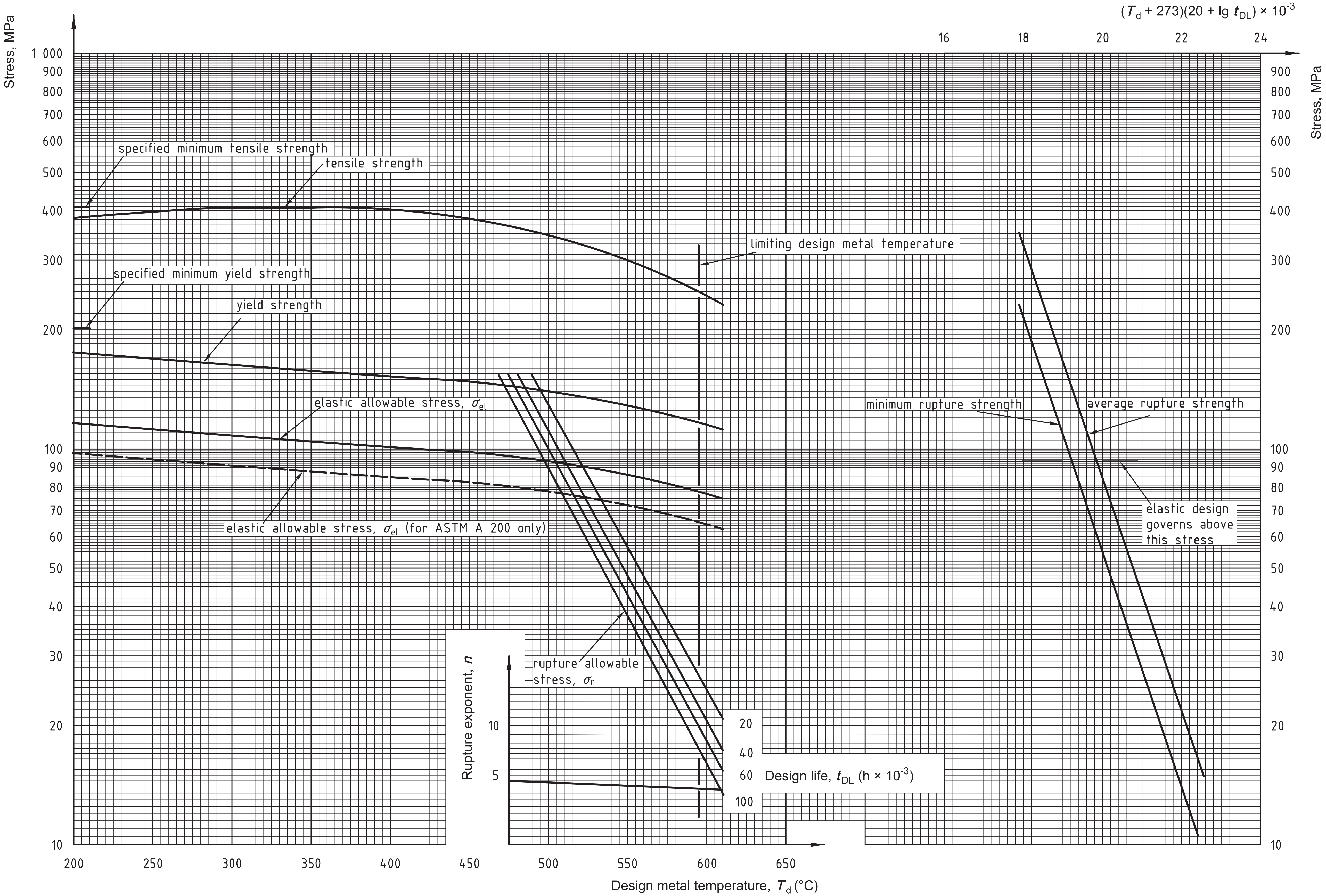
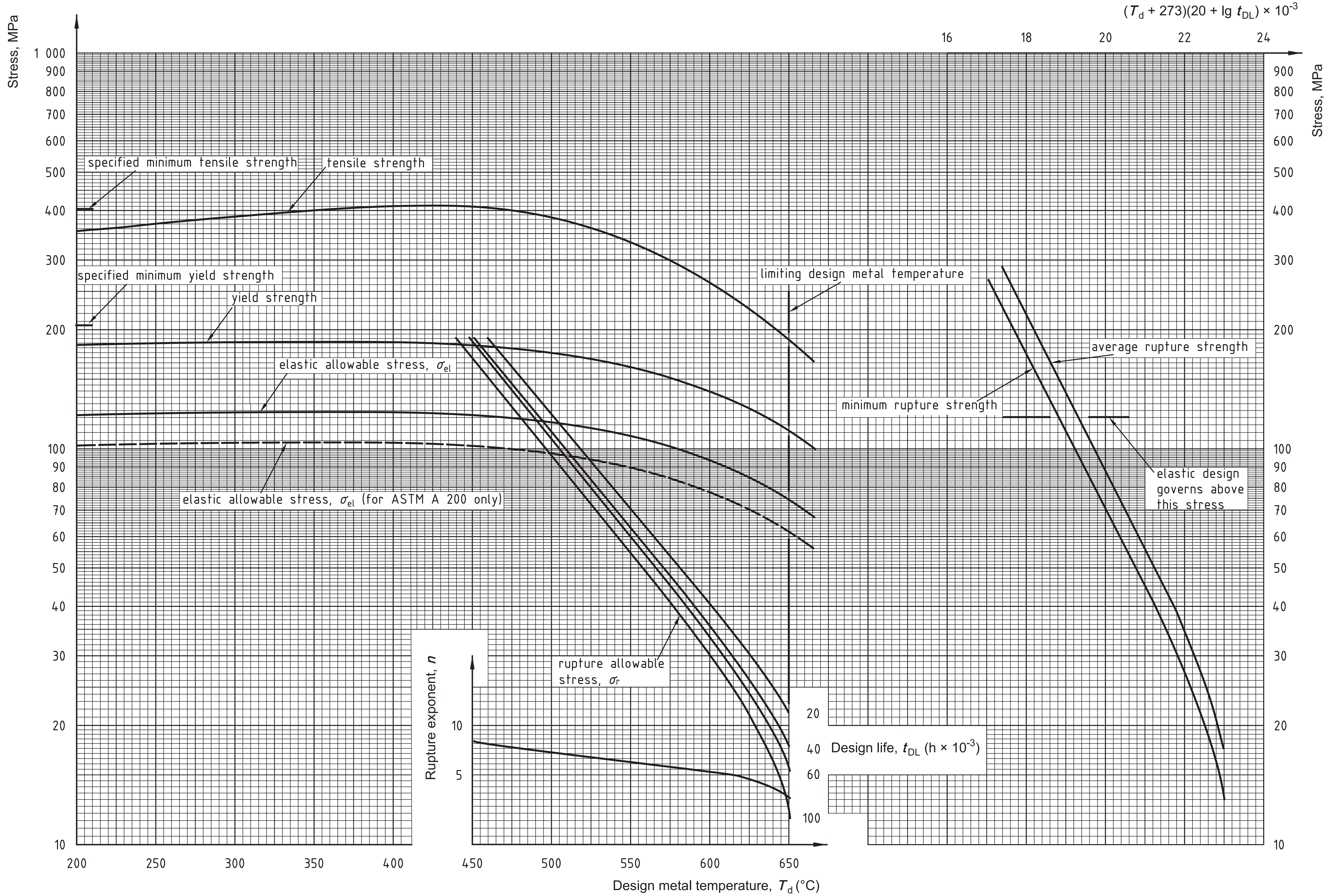


Figure E.3 — Stress curves (SI units) for ASTM A 161 T1, ASTM A 209 T1 and ASTM A 335 P1 C-½Mo steels



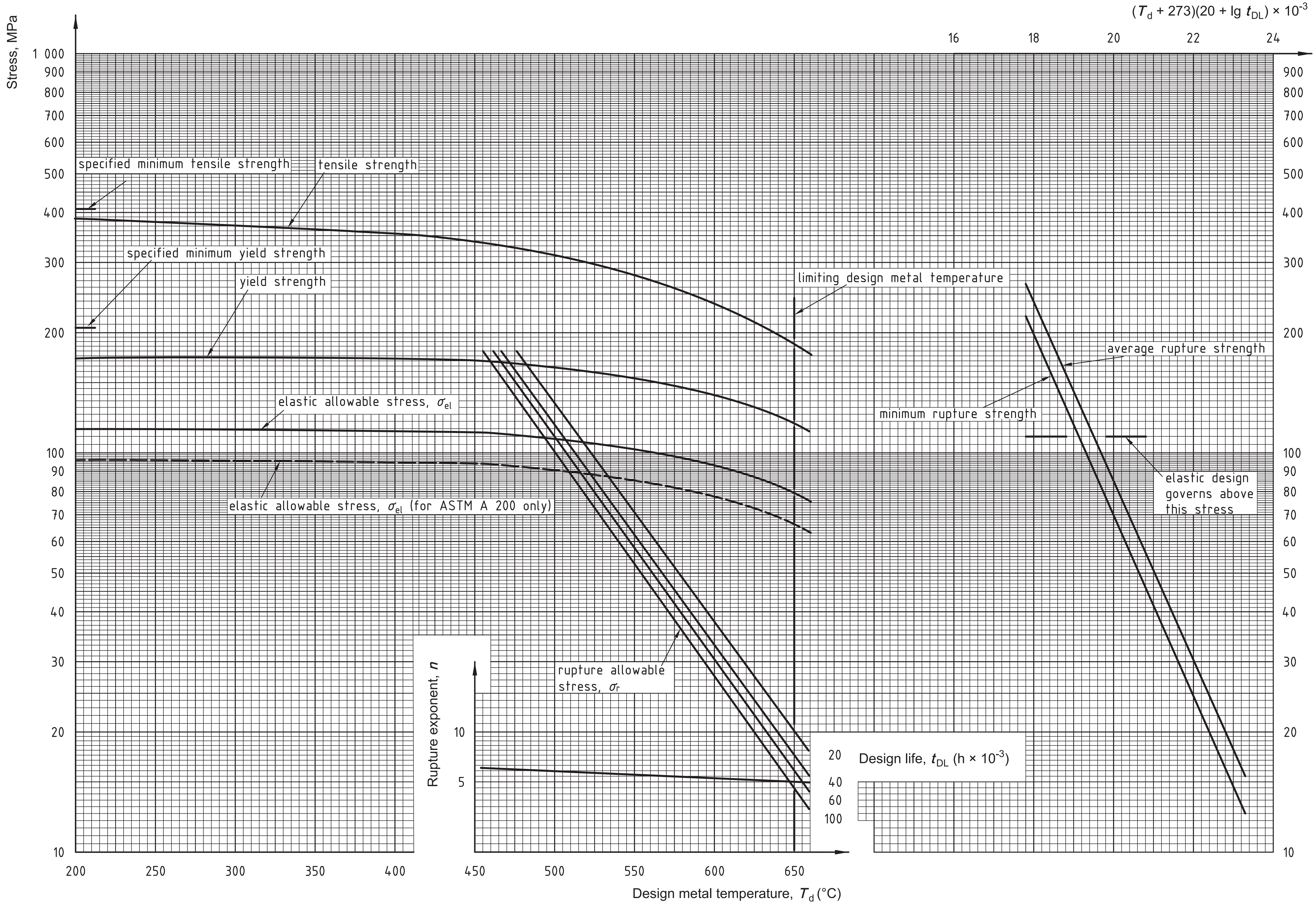
NOTE Broken lines indicate the elastic allowable stresses for the A 200 grade. This figure does not show the yield strength of the A 200 grade. The yield strength of the A 200 grade is 83 % of the yield strength shown. The tensile strength, rupture allowable stress, rupture strength, and rupture exponent for the A 200 grade are the same as for the A 213 and A 335 grades.

Figure E.4 — Stress curves (SI units) for ASTM A 200 T11, ASTM A 213 T11 and ASTM A 335 P11 1¼Cr-½Mo steels



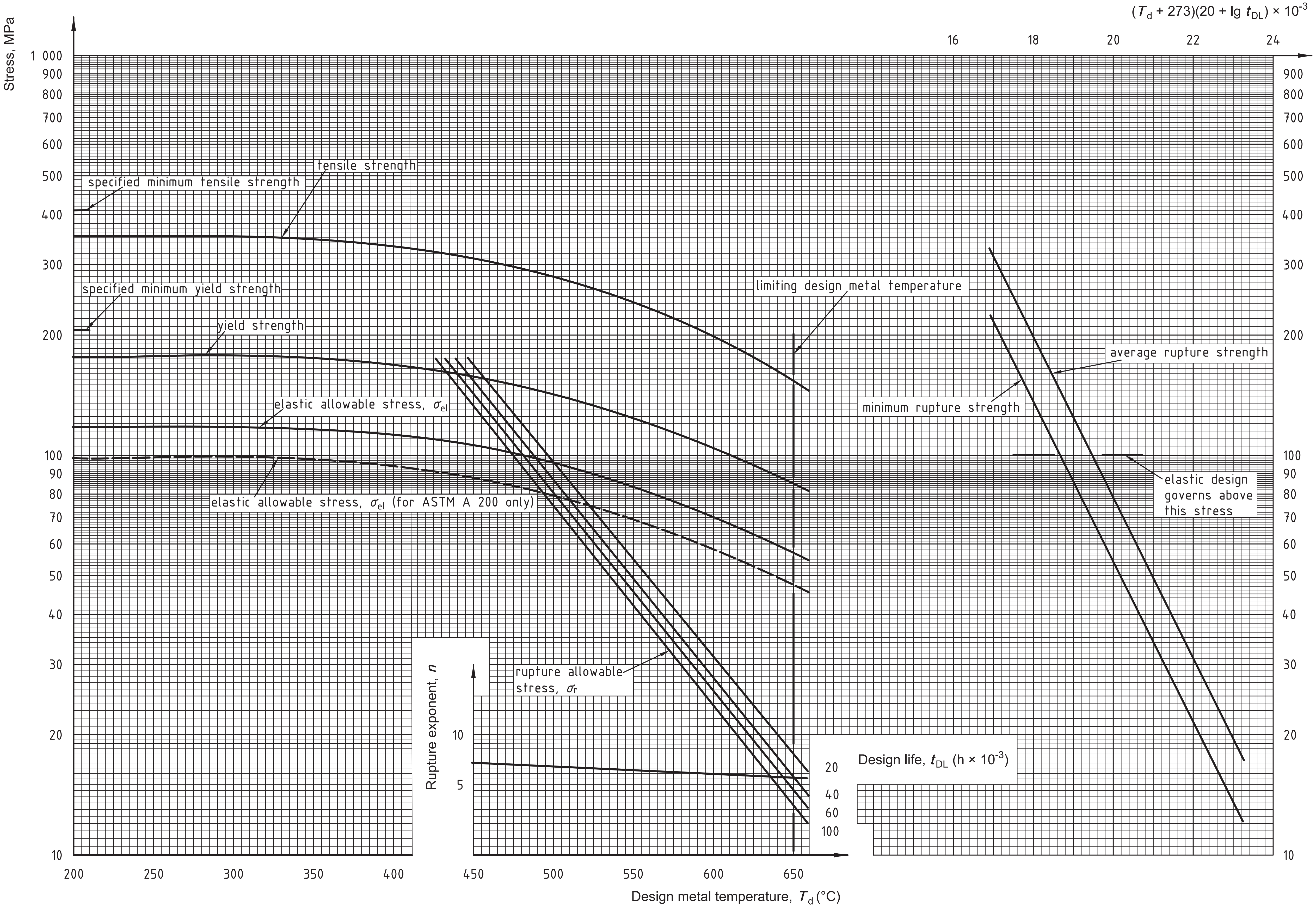
NOTE Broken lines indicate the elastic allowable stresses for the A 200 grade. This figure does not show the yield strength of the A 200 grade. The yield strength of the A 200 grade is 83 % of the yield strength shown. The tensile strength, rupture allowable stress, rupture strength, and rupture exponent for the A 200 grade are the same as for the A 213 and A 335 grades.

Figure E.5 — Stress curves (SI units) for ASTM A 200 T22, ASTM A 213 T22 and ASTM A 335 P22 2¼Cr-1Mo steels



NOTE Broken lines indicate the elastic allowable stresses for the A 200 grade. This figure does not show the yield strength of the A 200 grade. The yield strength of the A 200 grade is 83 % of the yield strength shown. The tensile strength, rupture allowable stress, rupture strength, and rupture exponent for the A 200 grade are the same as for the A 213 and A 335 grades.

Figure E.6 — Stress curves (SI units) for ASTM A 200 T21, ASTM A 213 T21 and ASTM A 335 P21 3Cr-1Mo steels



NOTE Broken lines indicate the elastic allowable stresses for the A 200 grade. This figure does not show the yield strength of the A 200 grade. The yield strength of the A 200 grade is 83 % of the yield strength shown. The tensile strength, rupture allowable stress, rupture strength, and rupture exponent for the A 200 grade are the same as for the A 213 and A 335 grades.

Figure E.7 — Stress curves (SI units) for ASTM A 200 T5, ASTM A 213 T5 and ASTM A 335 P5 5Cr-½Mo steels

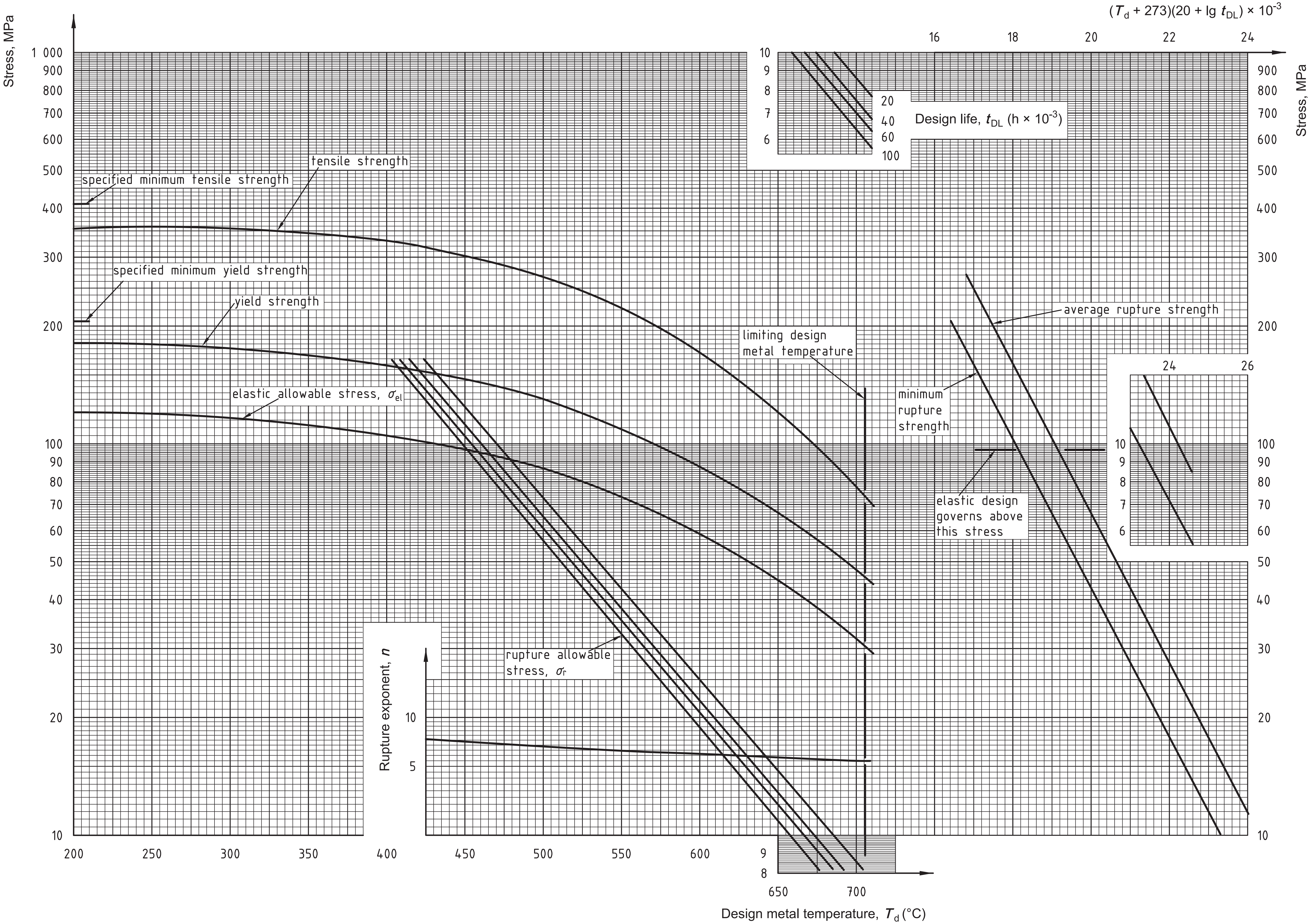
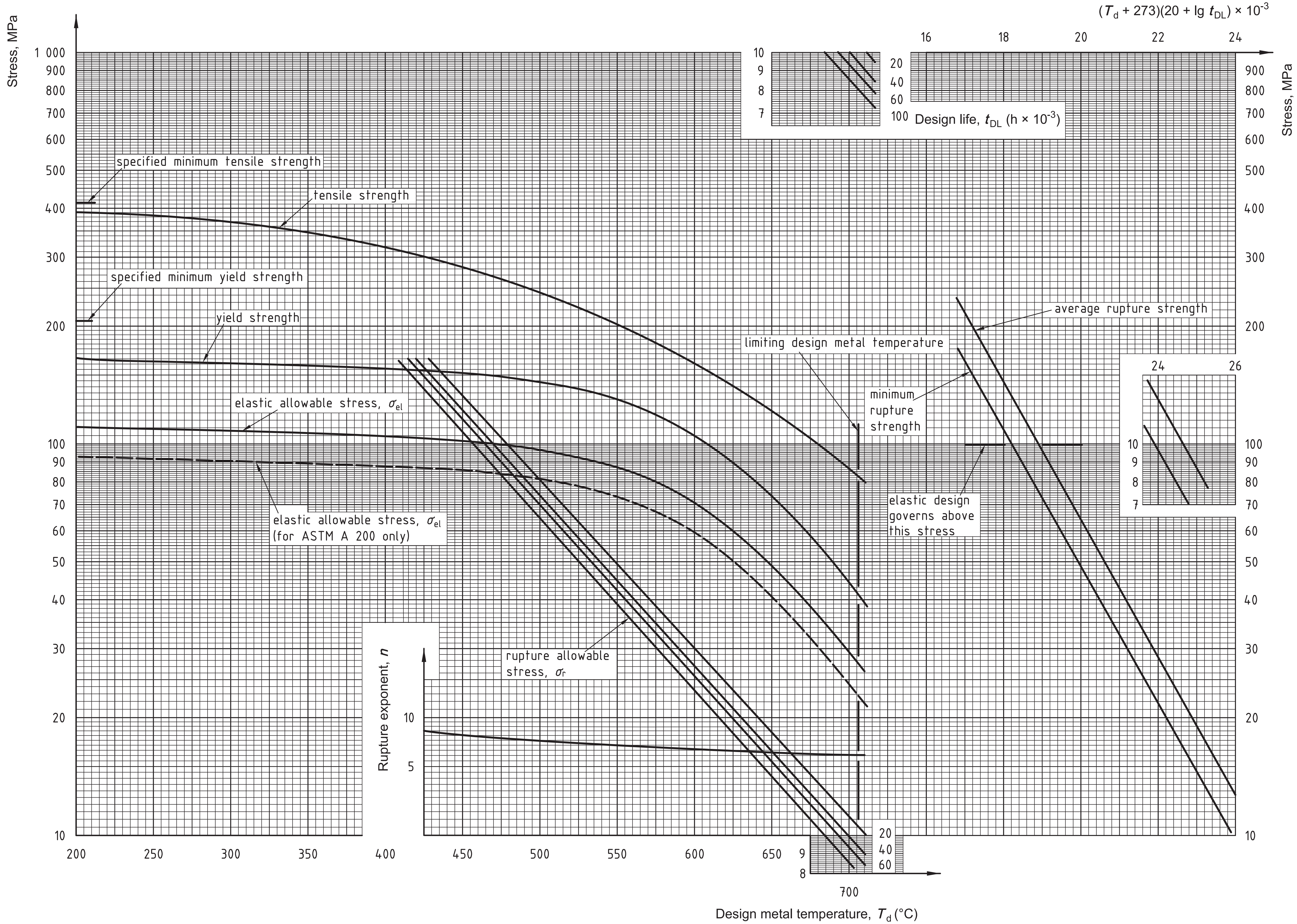
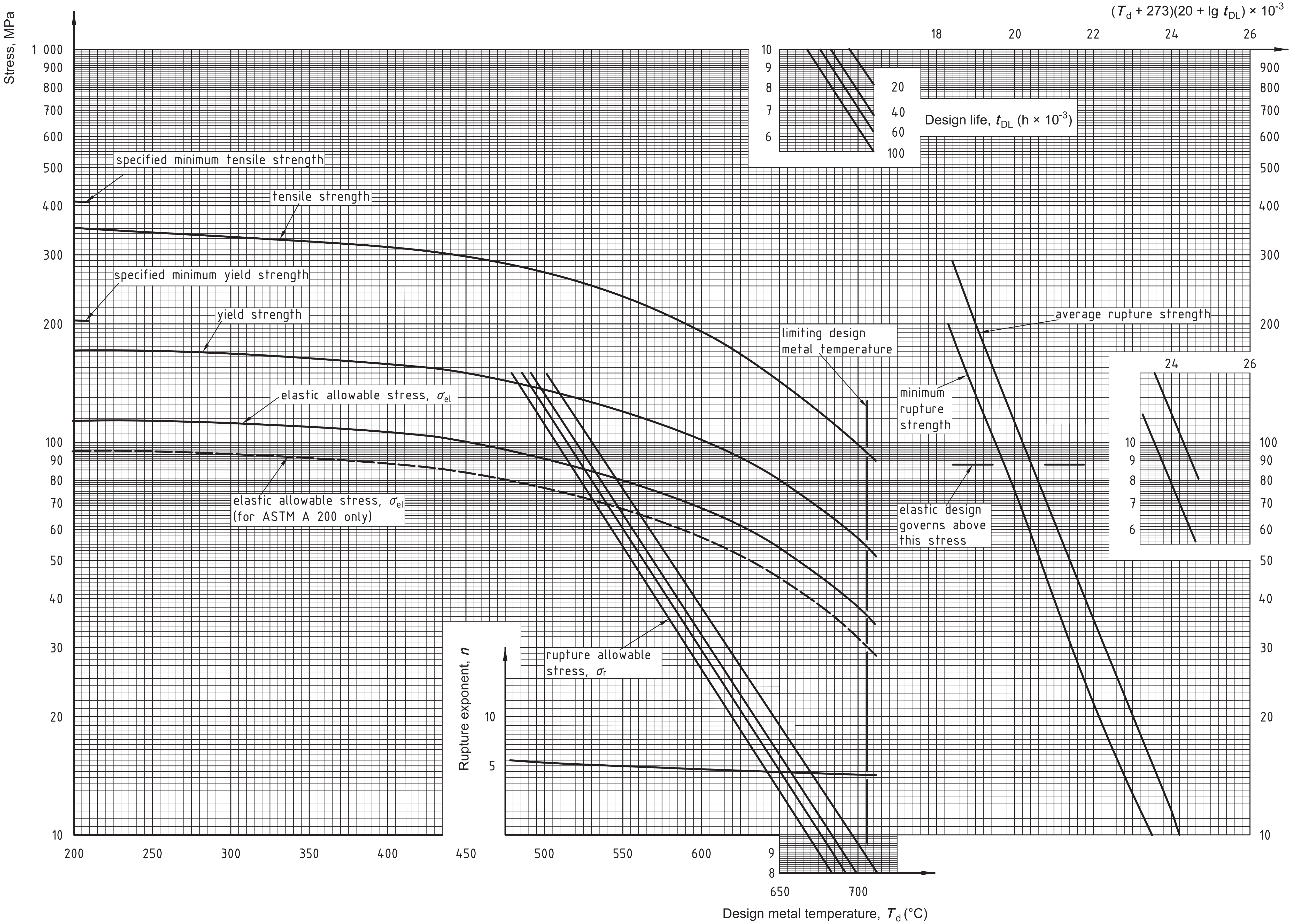


Figure E.8 — Stress curves (SI units) for ASTM A 213 T5b and ASTM A 335 P5b 5Cr-½Mo-Si steels



NOTE Broken lines indicate the elastic allowable stresses for the A 200 grade. This figure does not show the yield strength of the A 200 grade. The yield strength of the A 200 grade is 83 % of the yield strength shown. The tensile strength, rupture allowable stress, rupture strength, and rupture exponent for the A 200 grade are the same as for the A 213 and A 335 grades.

Figure E.9 — Stress curves (SI units) for ASTM A 200 T7, ASTM A 213 T7 and ASTM A 335 P7 7Cr-½Mo steels



NOTE Broken lines indicate the elastic allowable stresses for the A 200 grade. This figure does not show the yield strength of the A 200 grade. The yield strength of the A 200 grade is 83 % of the yield strength shown. The tensile strength, rupture allowable stress, rupture strength, and rupture exponent for the A 200 grade are the same as for the A 213 and A 335 grades.

Figure E.10 — Stress curves (SI units) for ASTM A 200 T9, ASTM A 213 T9 and ASTM A 335 P9 9Cr-1Mo steels

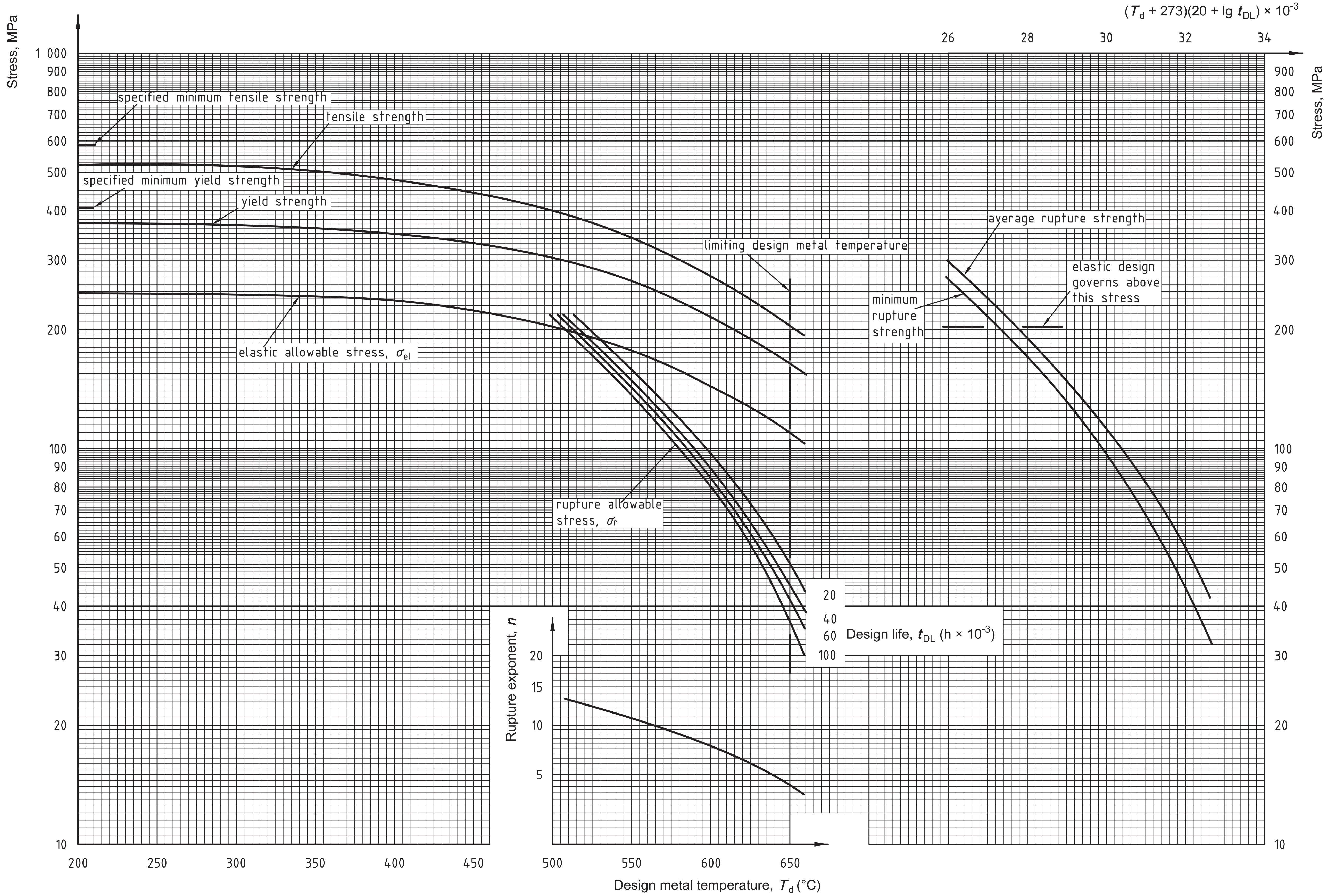
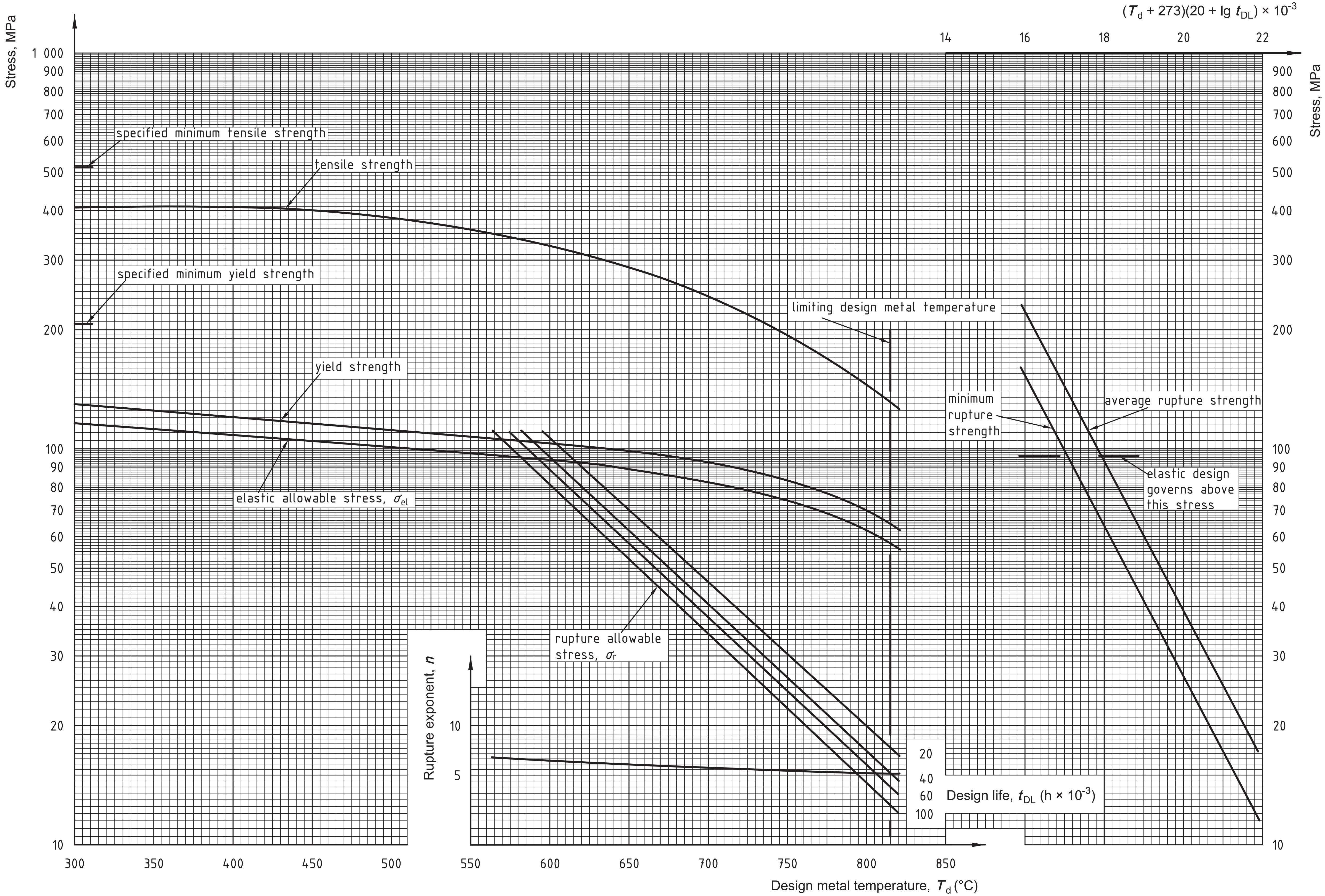
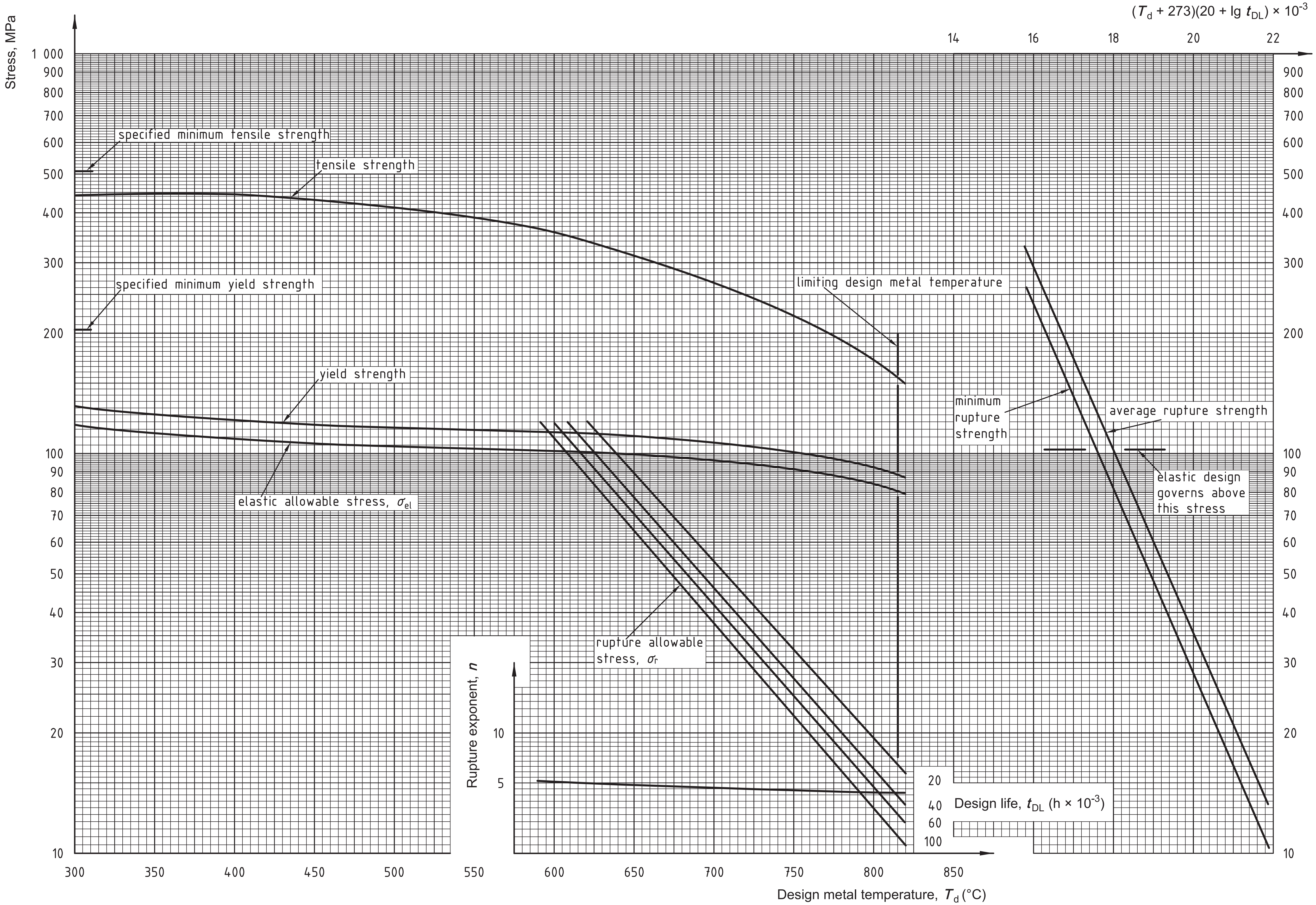


Figure E.11 — Stress curves (SI units) for ASTM A 200 T91, ASTM A 213 T91 and ASTM A 335 P91 9Cr-1Mo-V steels



NOTE Above 538 °C, the stress values for type 304 apply only if carbon content is 0,04 % or higher.

Figure E.12 — Stress curves (SI units) for ASTM A 213, ASTM A 271, ASTM A 312 and ASTM A 376 types 304 and 304H (18Cr-8Ni) stainless steels



NOTE Above 538 °C, the stress values for type 316 apply only if carbon content is 0,04 % or higher.

Figure E.13 — Stress curves (SI units) for ASTM A 213, ASTM A 271, ASTM A 312 and ASTM A 376 types 316 and 316H (16Cr-12Ni-2Mo) stainless steels

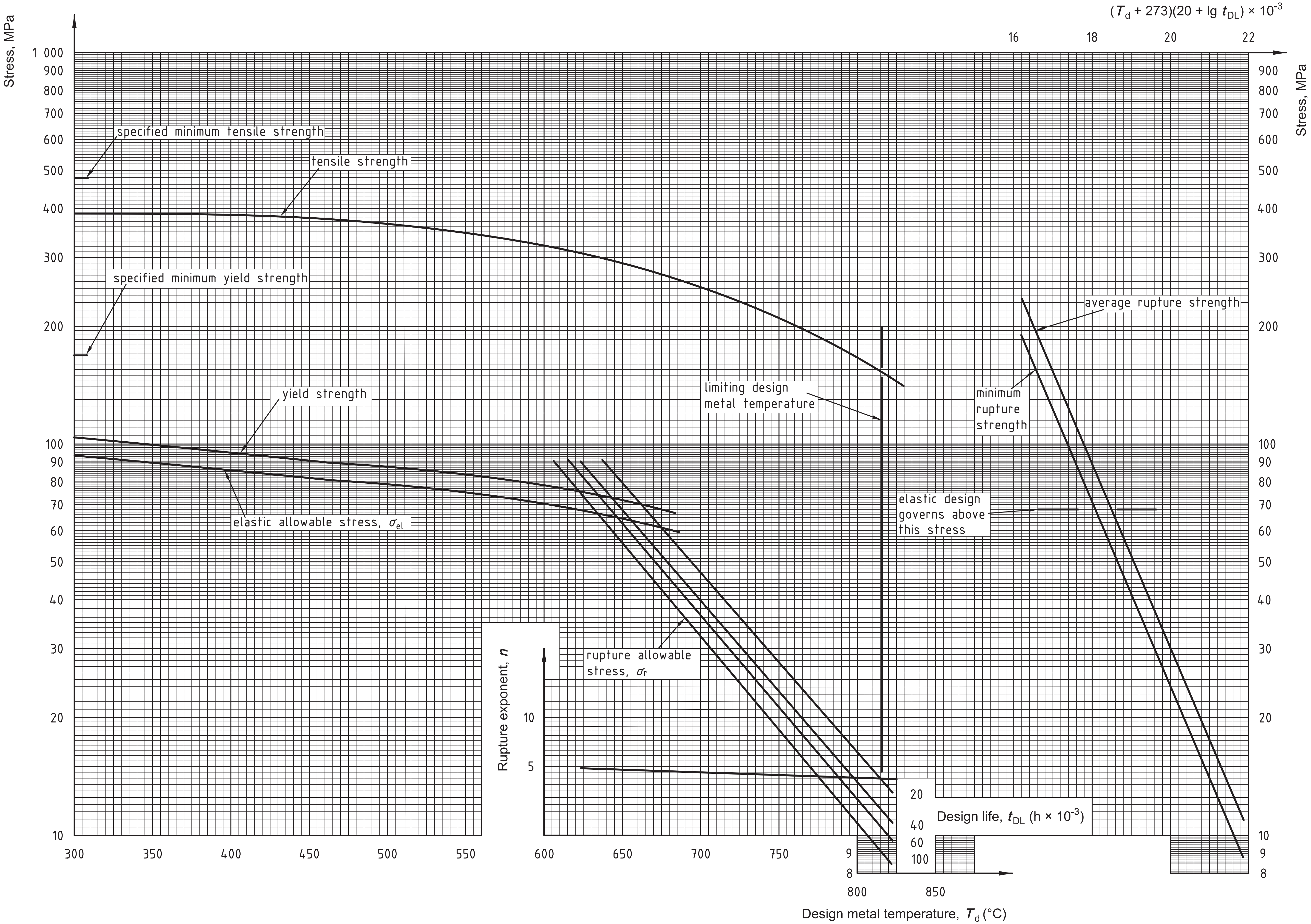
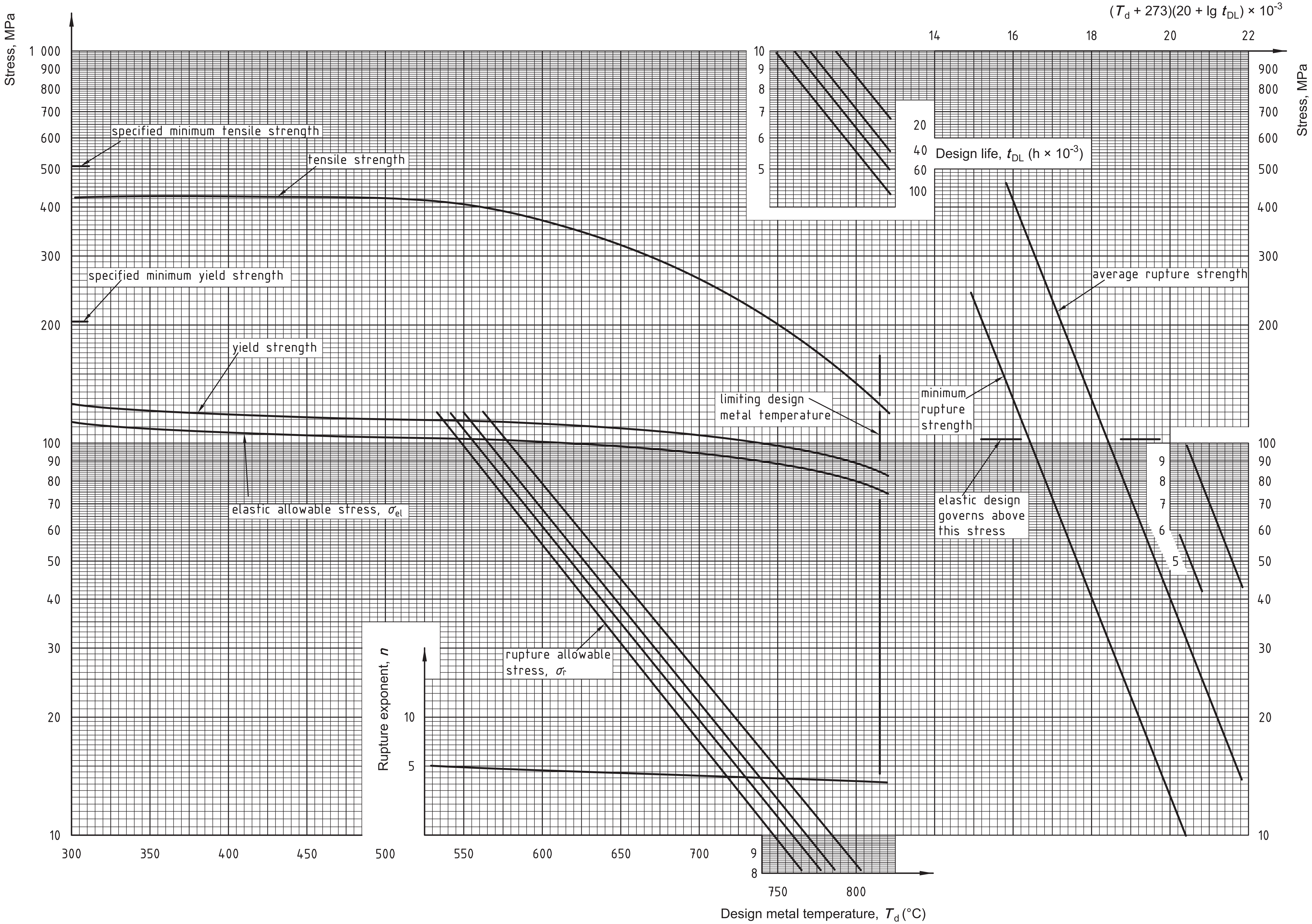
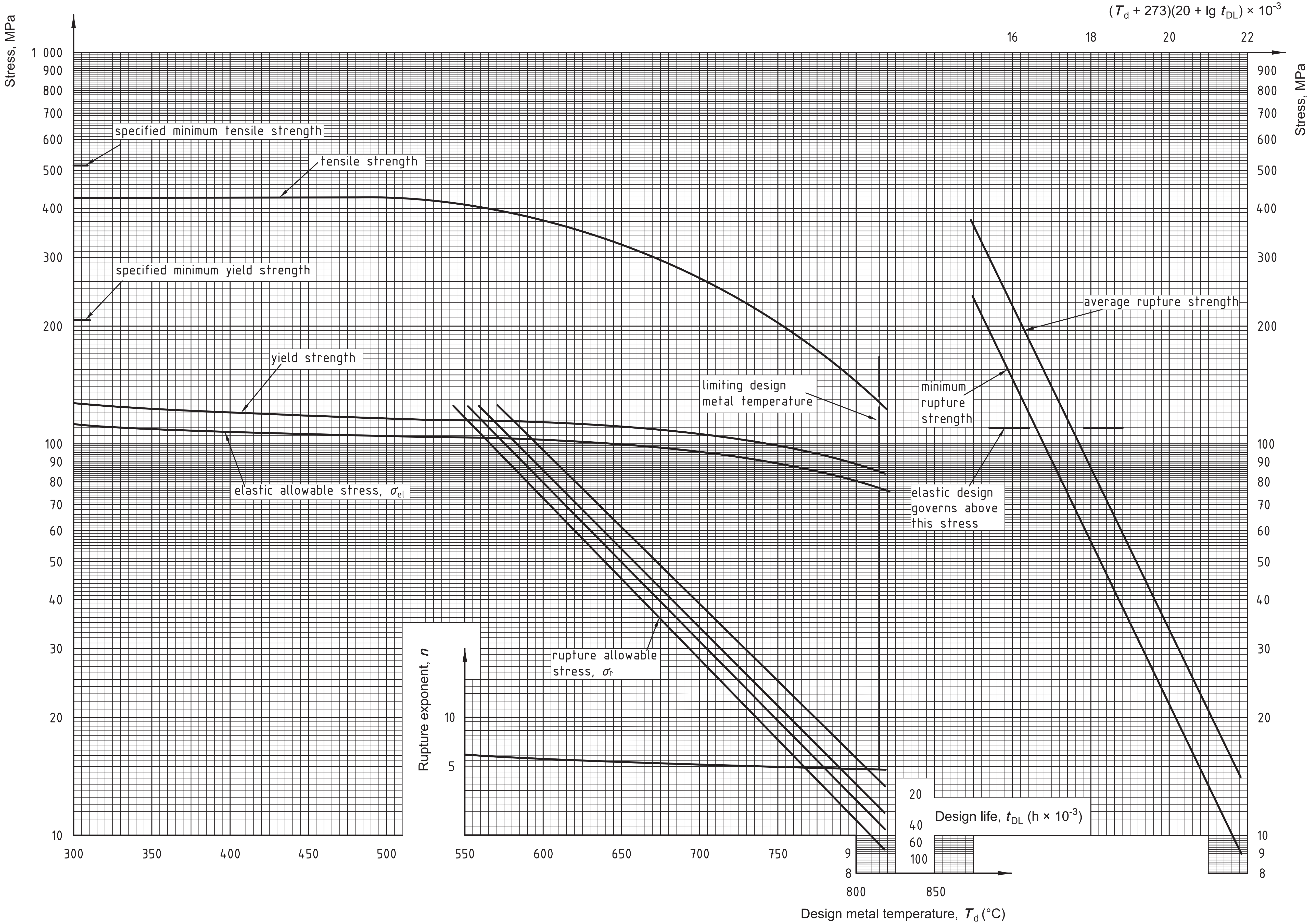


Figure E.14 — Stress curves (SI units) for ASTM A 213 and ASTM A 312 type 316L (16Cr-12Ni-2Mo) stainless steels



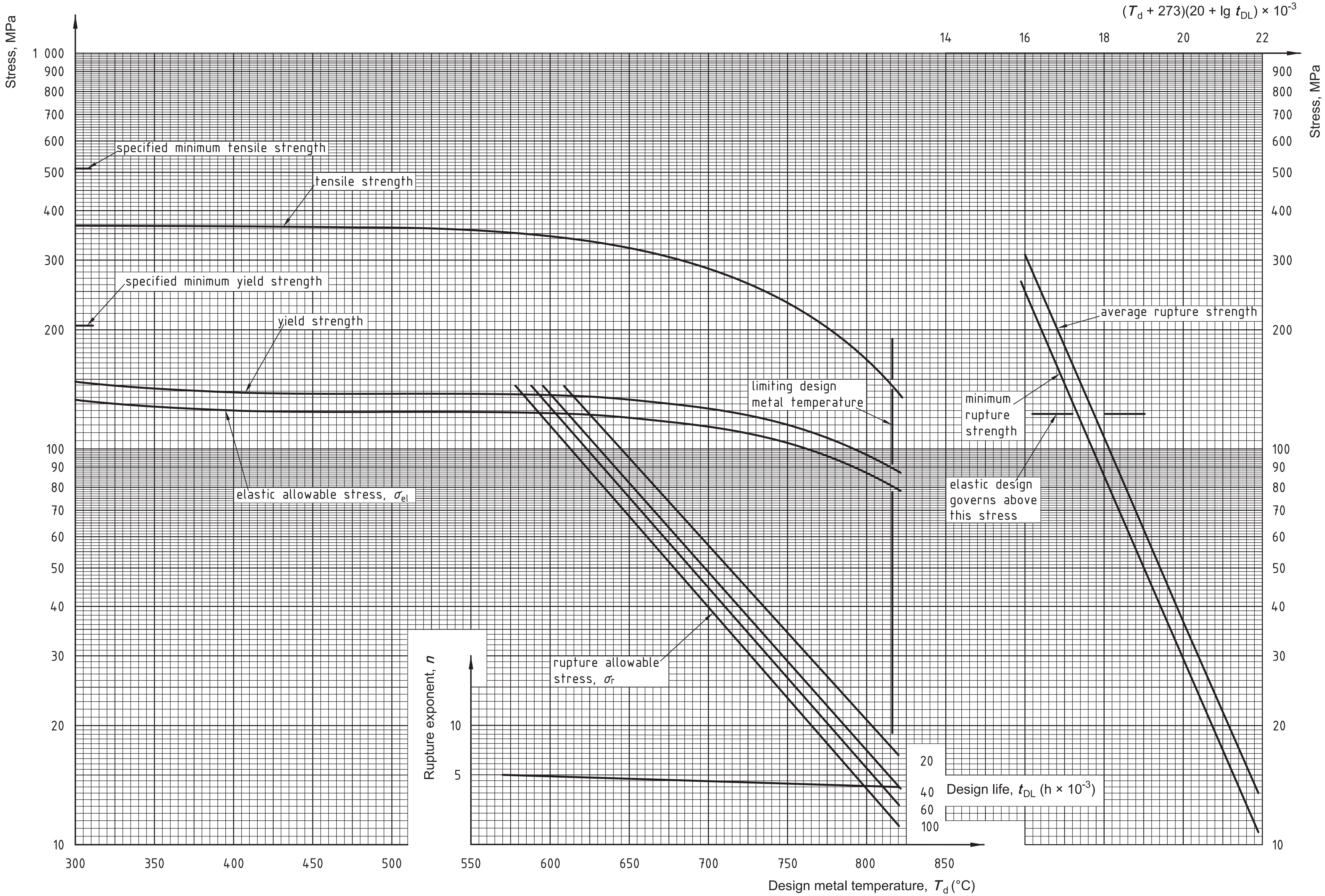
NOTE Above 538 °C, the stress values for type 321 apply only if carbon content is 0,04 % or higher.

Figure E.15 — Stress curves (SI units) for ASTM A 213, ASTM A 271, ASTM A 312 and ASTM A 376 type 321 (18Cr-10Ni-Ti) stainless steels



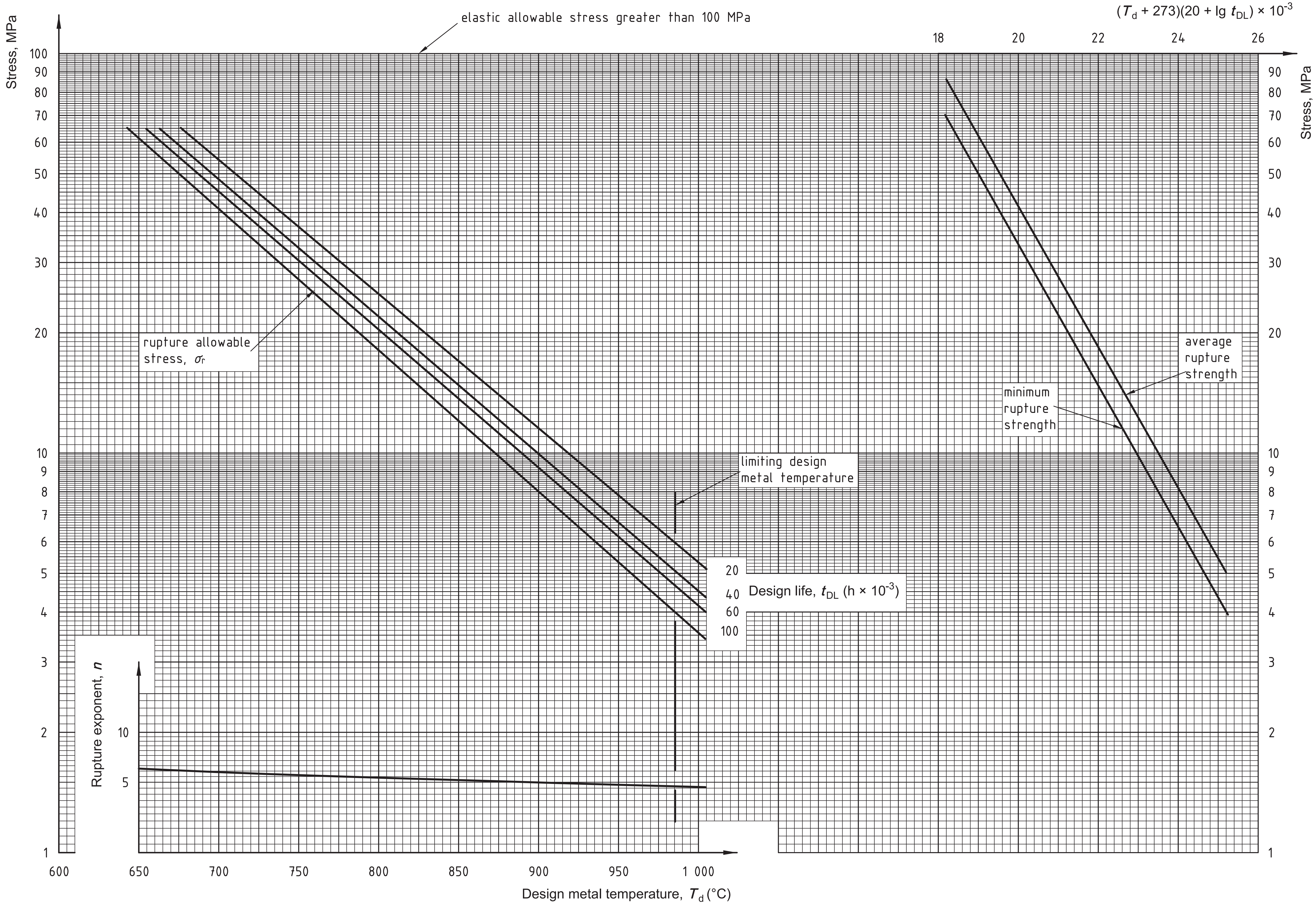
NOTE Above 538 °C, the stress values for type 347 apply only if carbon content is 0,04 % or higher.

Figure E.16 — Stress curves (SI units) for ASTM A 213, ASTM A 271, ASTM A 312 and ASTM A 376 type 321H (18Cr-10Ni-Ti) stainless steels



NOTE Above 538 °C, the stress values for type 347 apply only if carbon content is 0,04 % or higher.

Figure E.17 — Stress curves (SI units) for ASTM A 213, ASTM A 271, ASTM A 312 and ASTM A 376 types 347 and 347H (18Cr-10Ni-Nb) stainless steels



NOTE The average grain size corresponds to ASTM No. 5 or coarser.

Figure E.18 — Stress curves (SI units) for ASTM B 407 UNS N08810 and UNS N08811 alloys 800H and 800HT (Ni-Fe-Cr) stainless steels

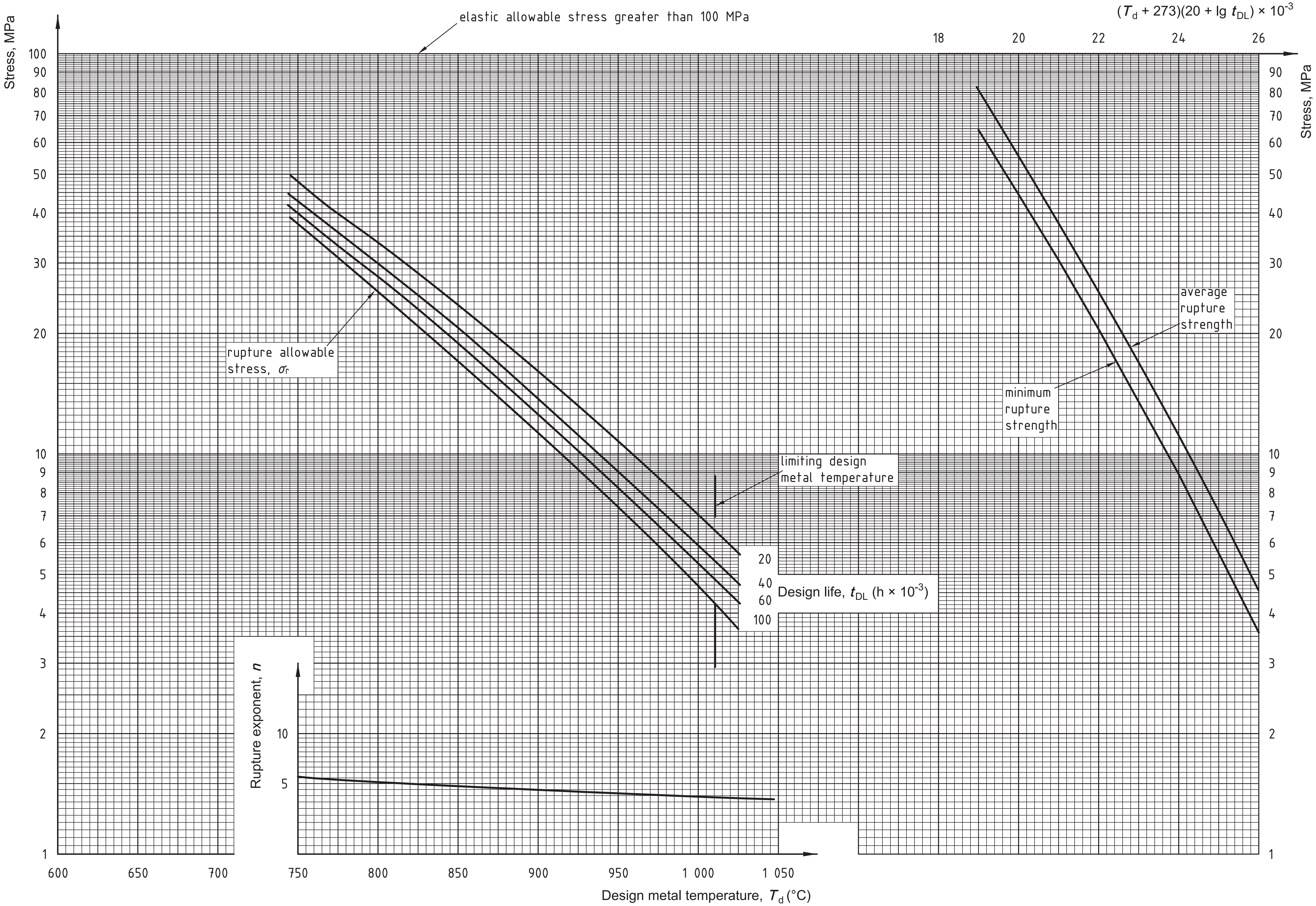


Figure E.19 — Stress curves (SI units) for ASTM A 608 Grade HK40 (25Cr-20Ni) stainless steels

Annex F
(normative)

Stress curves (USC units)

Stress curves, given in USC units, are presented in Figures F.1 to F.19.

List of Figures (USC units)

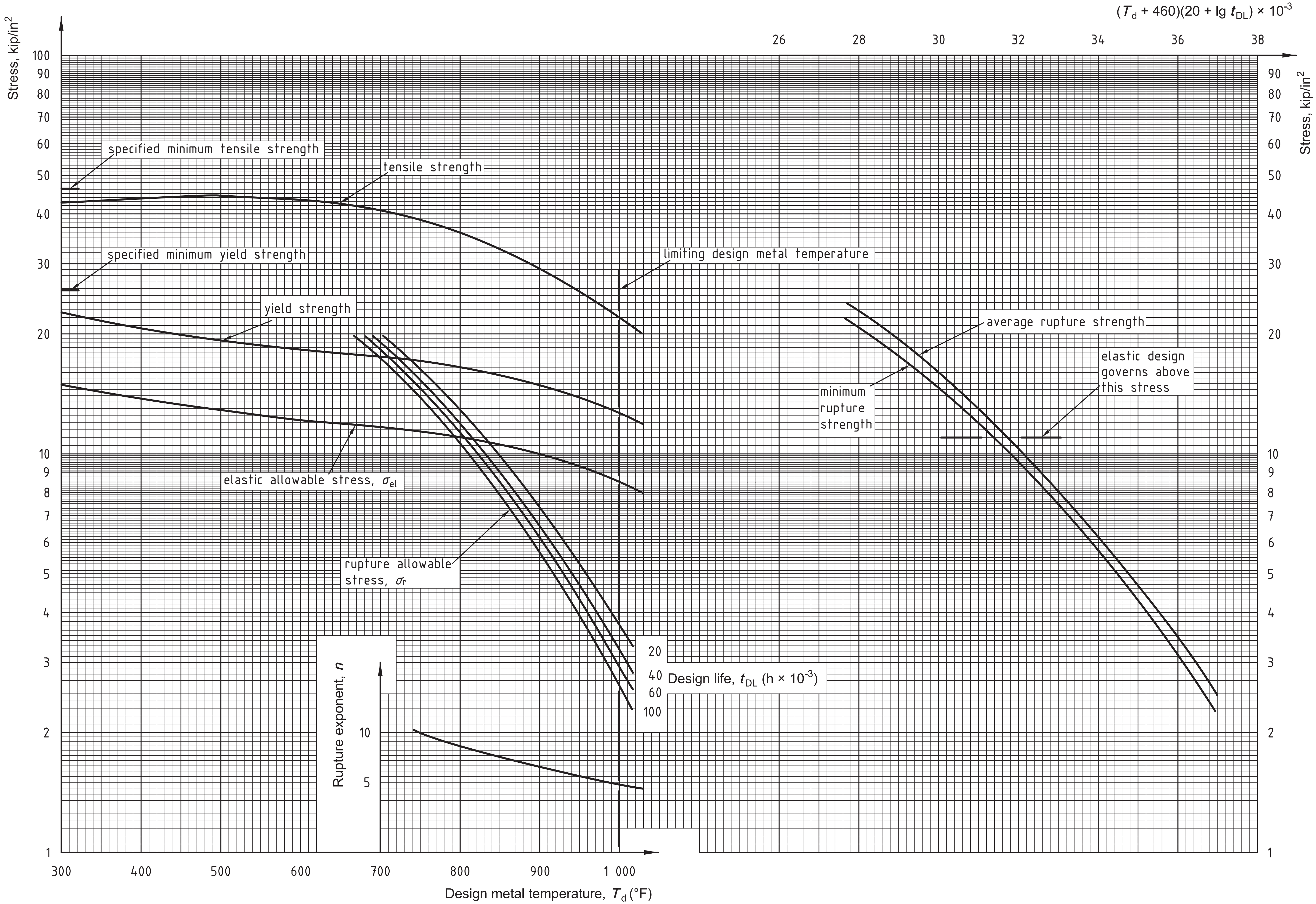


Figure F.1 — Stress curves (USC units) for ASTM A 161 and ASTM A 192 low-carbon steels

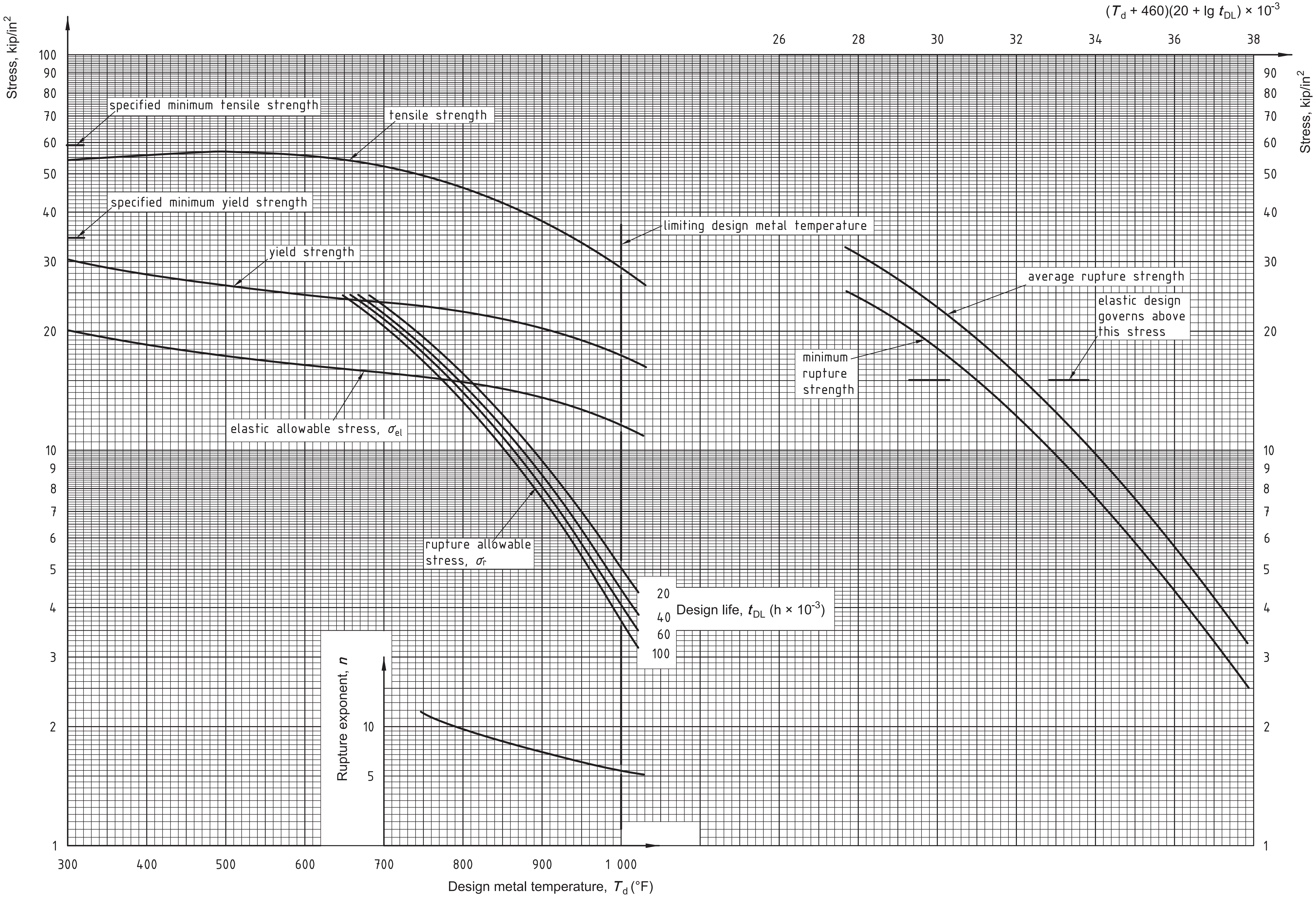


Figure F.2 — Stress curves (USC units) for ASTM A 53 Grade B (seamless), ASTM A 106 Grade B and ASTM 210 Grade A-1 medium-carbon steels

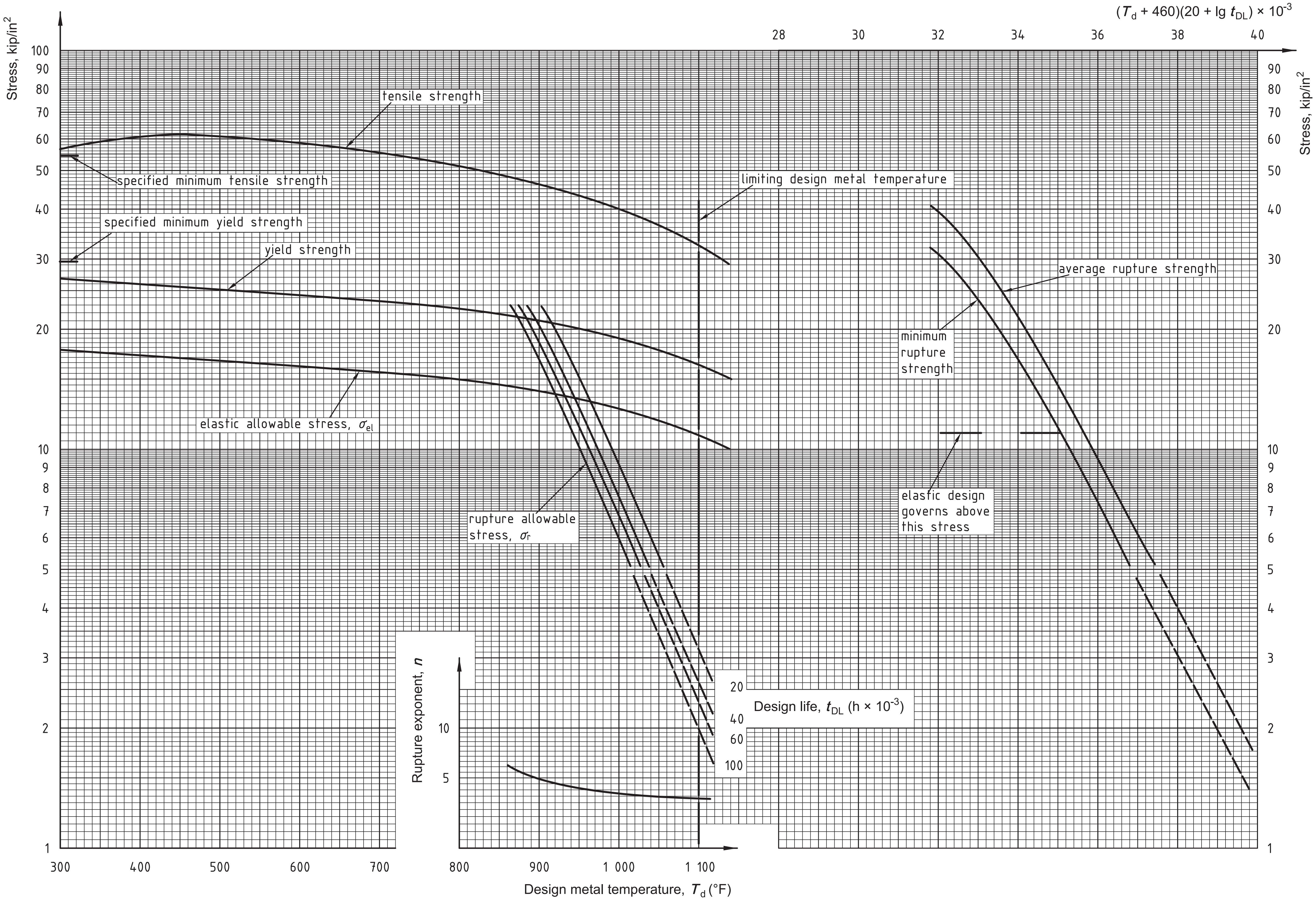
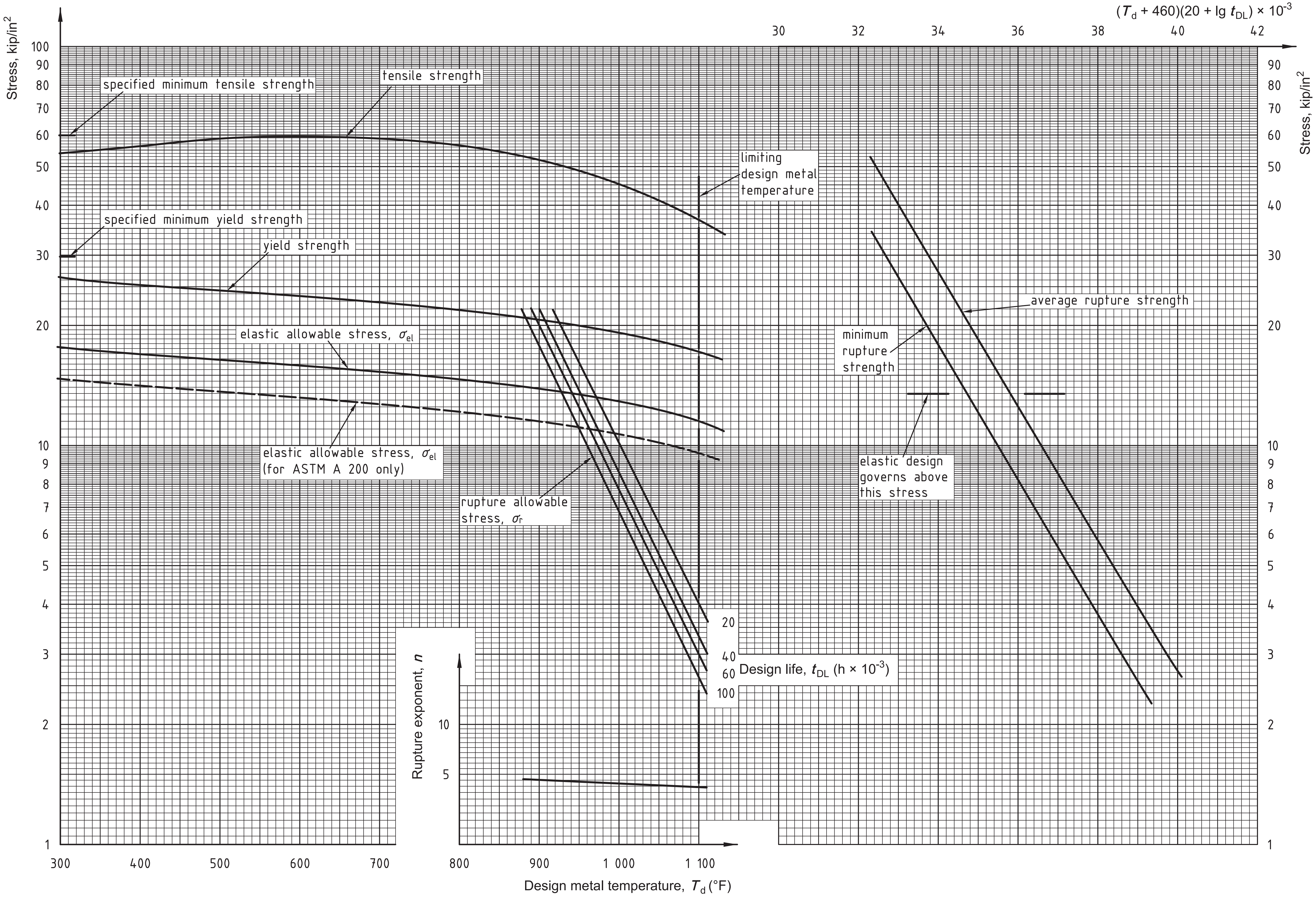
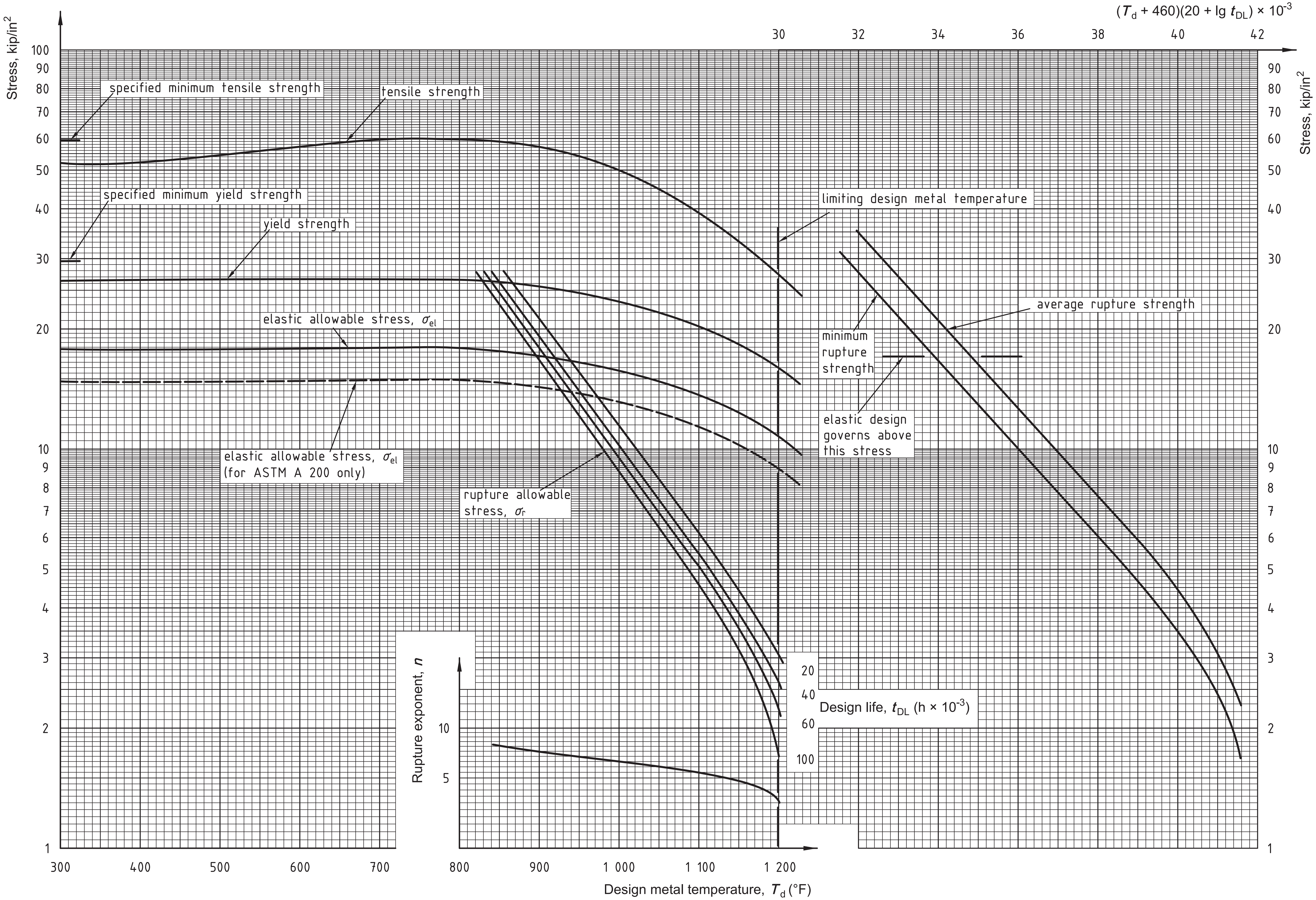


Figure F.3 — Stress curves (USC units) for ASTM A 161 T1, ASTM A 209 T1 and ASTM A 335 P1 C-½Mo steels



NOTE Broken lines indicate the elastic allowable stresses for the A 200 grade. This figure does not show the yield strength of the A 200 grade. The yield strength of the A 200 grade is 83 % of the yield strength shown. The tensile strength, rupture allowable stress, rupture strength, and rupture exponent for the A 200 grade are the same as for the A 213 and A 335 grades.

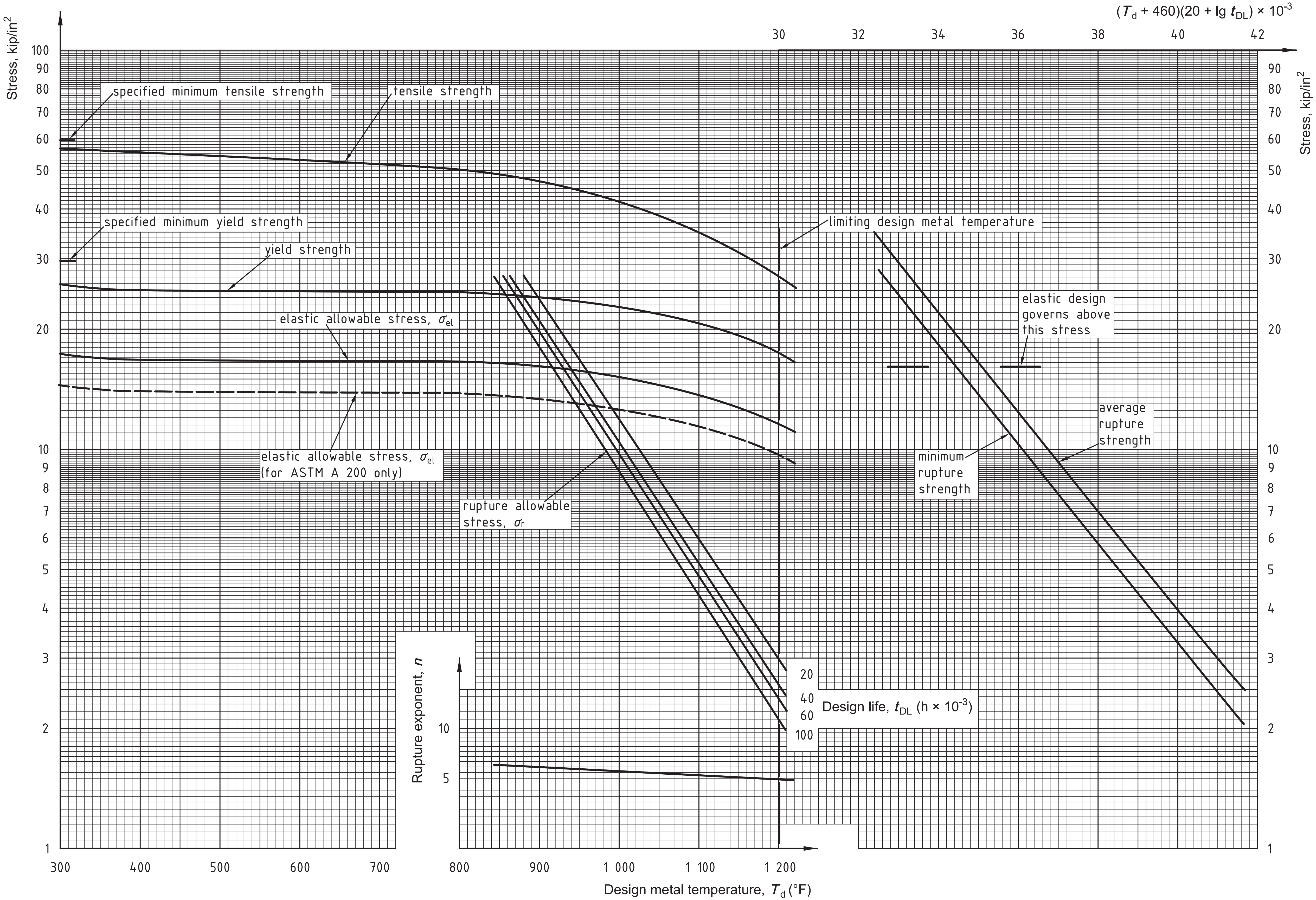
Figure F.4 — Stress curves (USC units) for ASTM A 200 T11, ASTM A 213 T11 and ASTM A 335 P11 1¼Cr-½Mo steels



NOTE 1 The unit "kip/in²" (kilopounds per square inch) is referred to as kilo "pound-force per square inch" in ISO/IEC 80000.

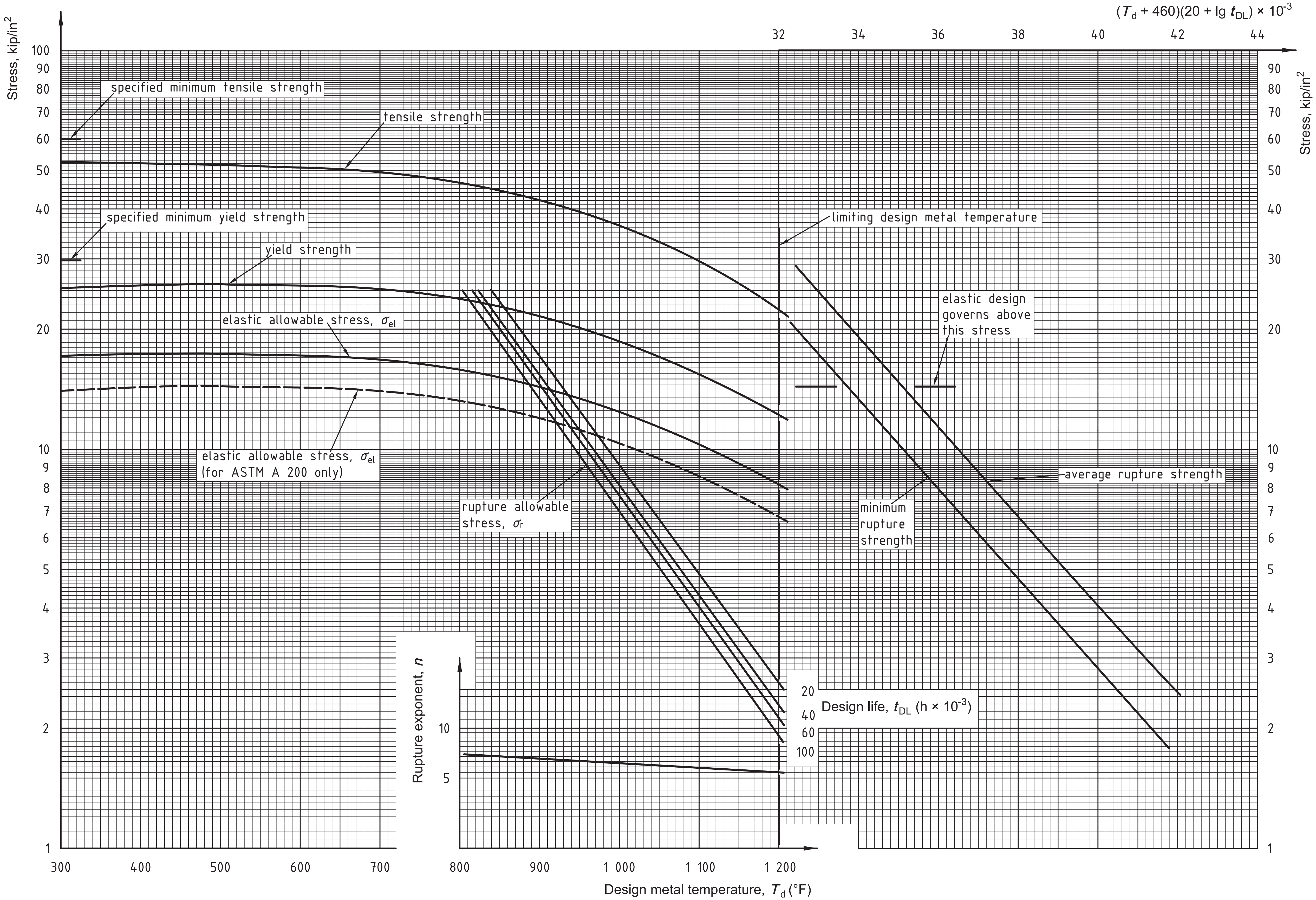
NOTE 2 Broken lines indicate the elastic allowable stresses for the A 200 grade. This figure does not show the yield strength of the A 200 grade. The yield strength of the A 200 grade is 83 % of the yield strength shown. The tensile strength, rupture allowable stress, rupture strength, and rupture exponent for the A 200 grade are the same as for the A 213 and A 335 grades.

Figure F.5 — Stress curves (USC units) for ASTM A 200 T22, ASTM A 213 T22 and ASTM A 335 P22 2¼Cr-1Mo steels



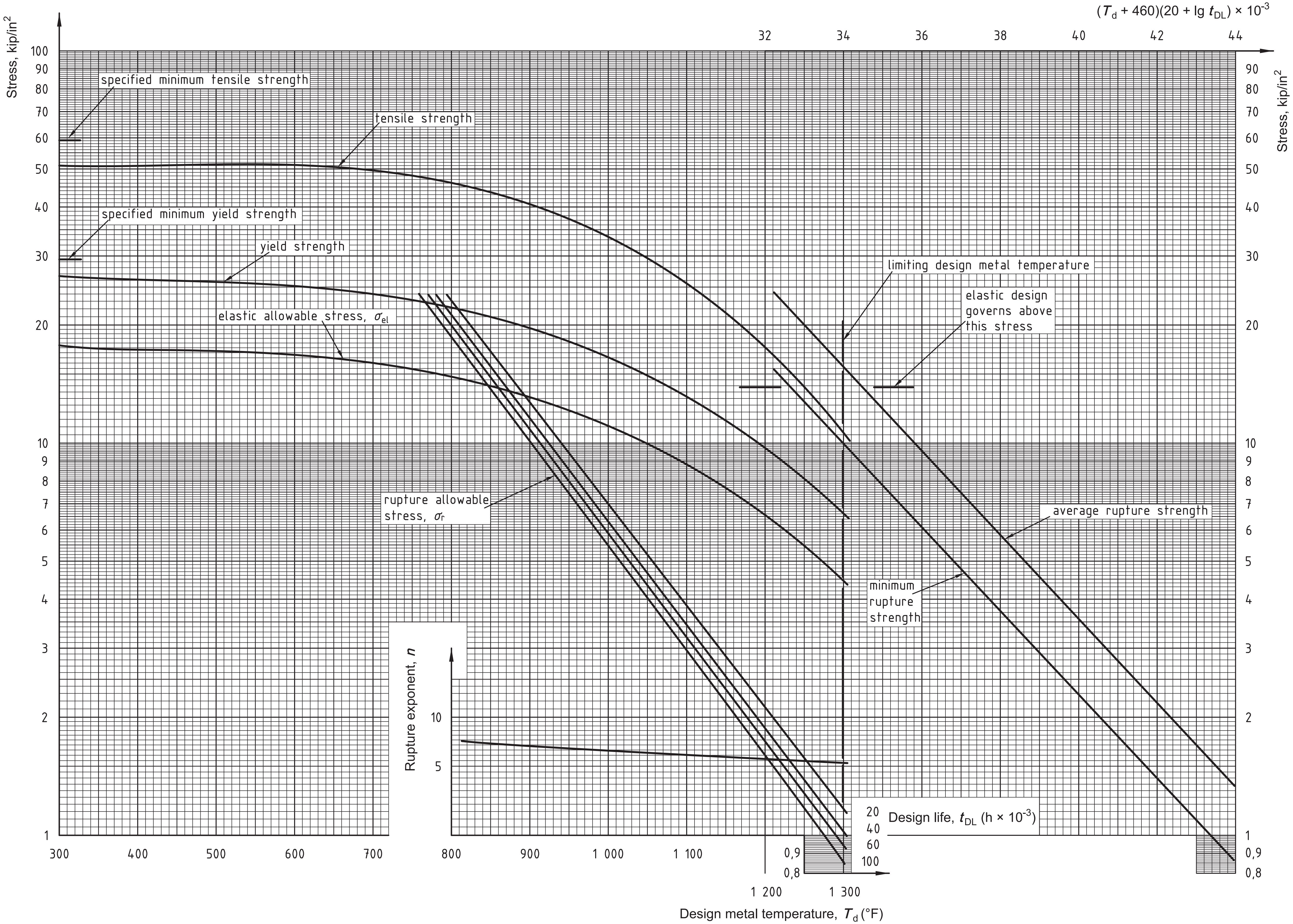
NOTE Broken lines indicate the elastic allowable stresses for the A 200 grade. This figure does not show the yield strength of the A 200 grade. The yield strength of the A 200 grade is 83 % of the yield strength shown. The tensile strength, rupture allowable stress, rupture strength, and rupture exponent for the A 200 grade are the same as for the A 213 and A 335 grades.

Figure F.6 — Stress curves (USC units) for ASTM A 200 T21, ASTM A 213 T21 and ASTM A 335 P21 3Cr-1Mo steels



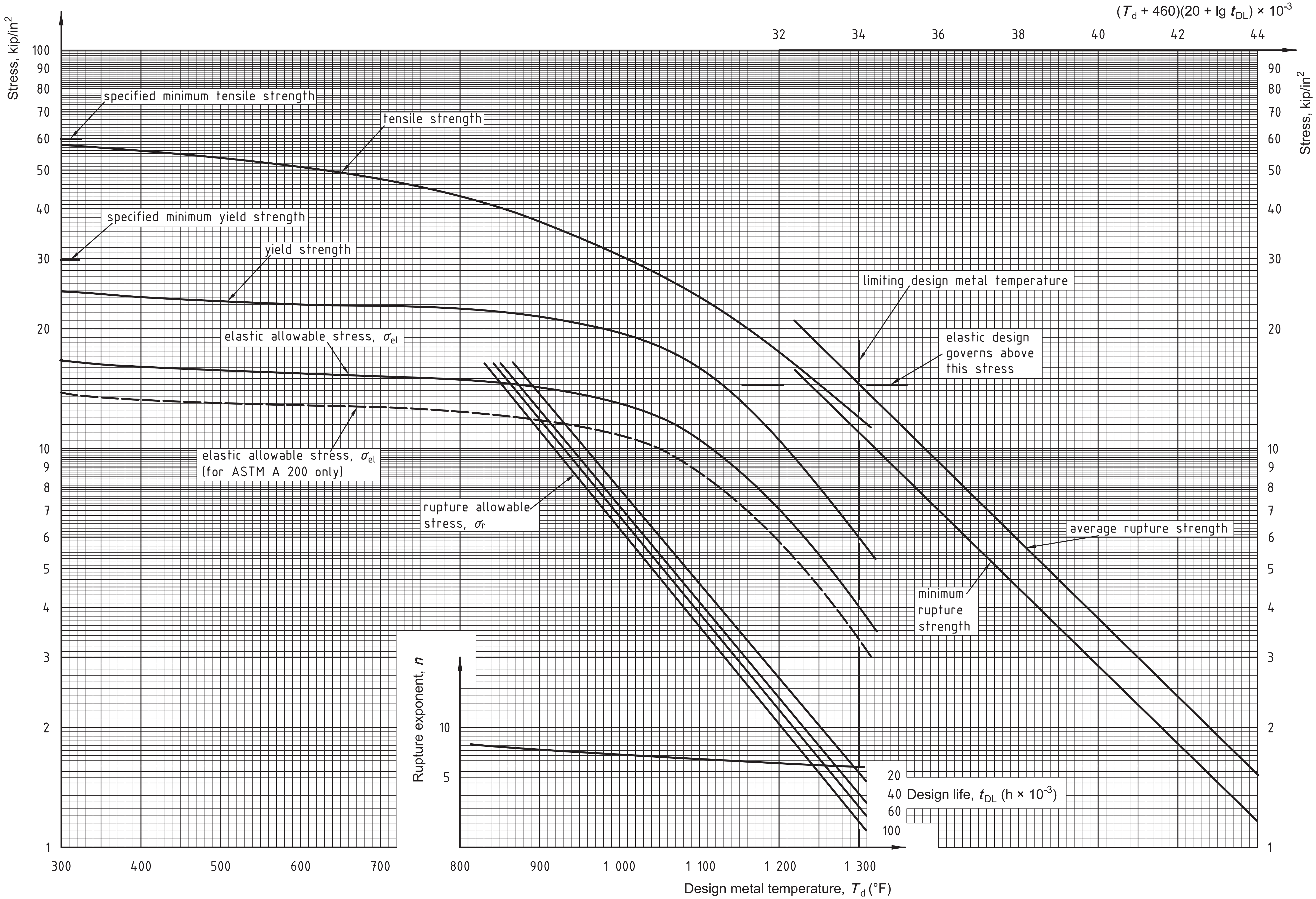
NOTE Broken lines indicate the elastic allowable stresses for the A 200 grade. This figure does not show the yield strength of the A 200 grade. The yield strength of the A 200 grade is 83 % of the yield strength shown. The tensile strength, rupture allowable stress, rupture strength, and rupture exponent for the A 200 grade are the same as for the A 213 and A 335 grades.

Figure F.7 — Stress curves (USC units) for ASTM A 200 T5, ASTM A 213 T5 and ASTM A 335 P5 5Cr-½Mo steels



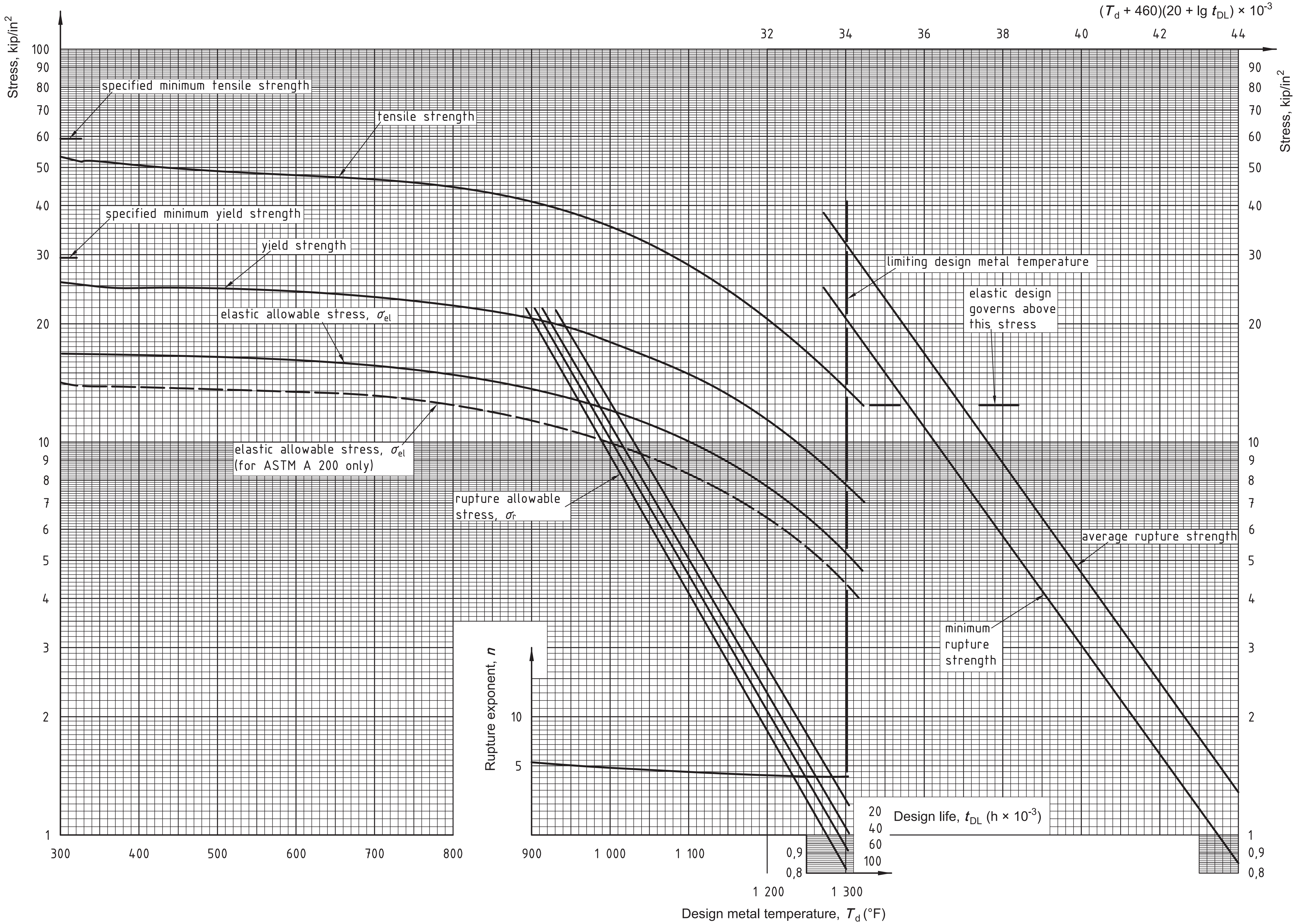
NOTE The unit "kip/in²" (kilopounds per square inch) is referred to as kilo "pound-force per square inch" in ISO/IEC 80000.

Figure F.8 — Stress curves (USC units) for ASTM A 213 T5b and ASTM A 335 P5b 5Cr-½Mo-Si steels



NOTE Broken lines indicate the elastic allowable stresses for the A 200 grade. This figure does not show the yield strength of the A 200 grade. The yield strength of the A 200 grade is 83 % of the yield strength shown. The tensile strength, rupture allowable stress, rupture strength, and rupture exponent for the A 200 grade are the same as for the A 213 and A 335 grades.

Figure F.9 — Stress curves (USC units) for ASTM A 200 T7, ASTM A 213 T7 and ASTM A 335 P7 7Cr-½Mo steels



NOTE Broken lines indicate the elastic allowable stresses for the A 200 grade. This figure does not show the yield strength of the A 200 grade. The yield strength of the A 200 grade is 83 % of the yield strength shown. The tensile strength, rupture allowable stress, rupture strength, and rupture exponent for the A 200 grade are the same as for the A 213 and A 335 grades.

Figure F.10 — Stress curves (USC units) for ASTM A 200 T9, ASTM A 213 T9 and ASTM A 335 P9 9Cr-1Mo steels

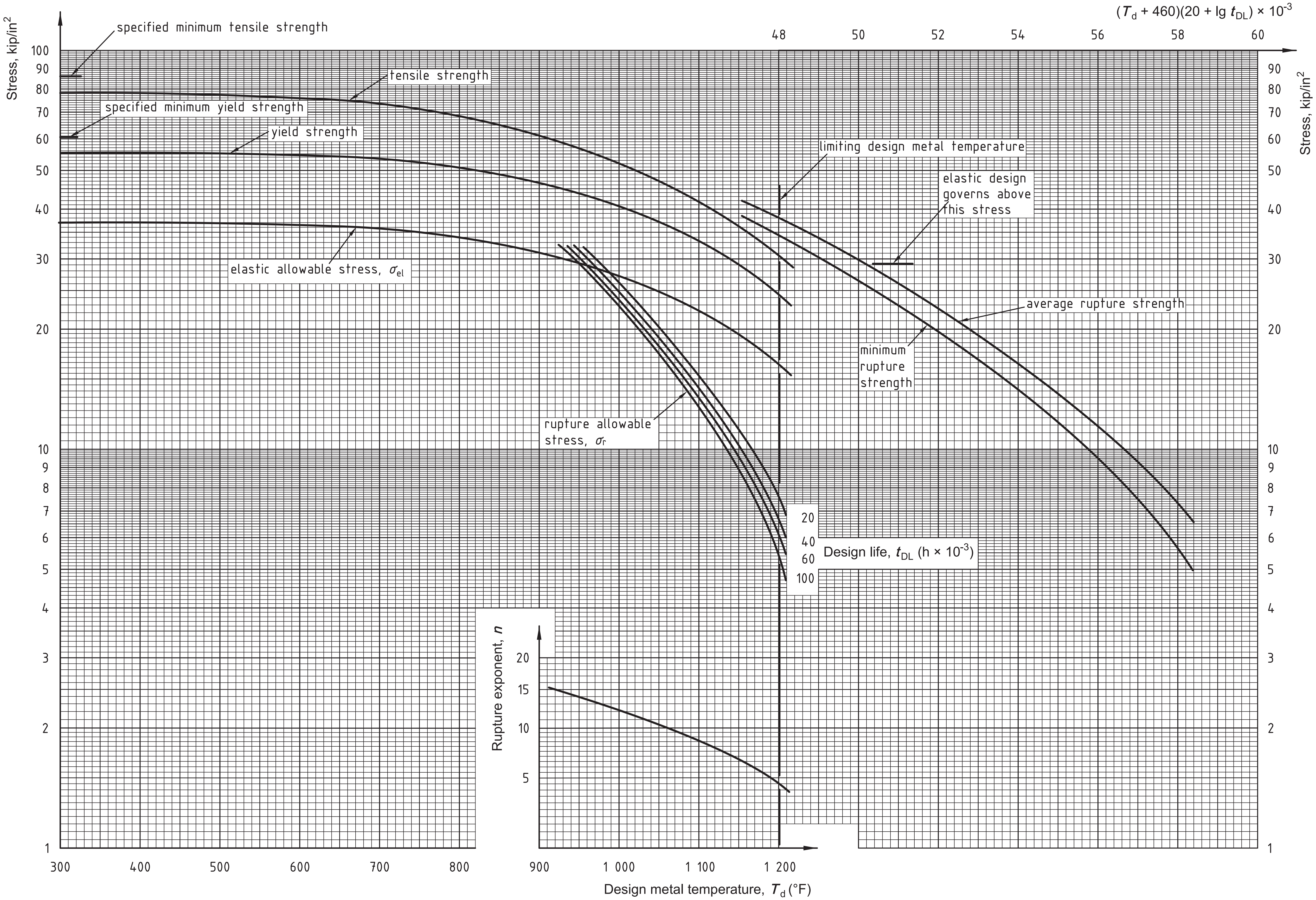
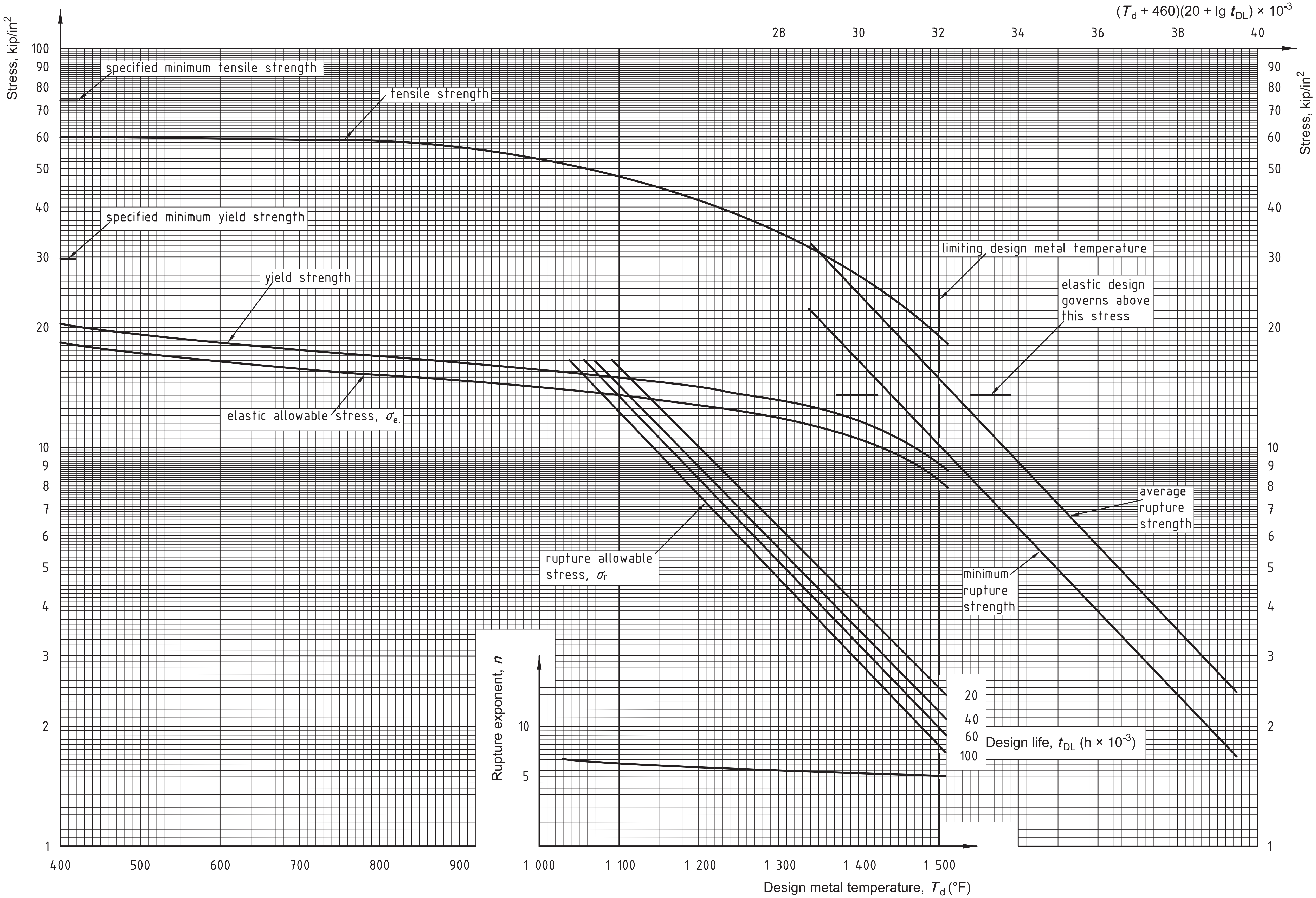


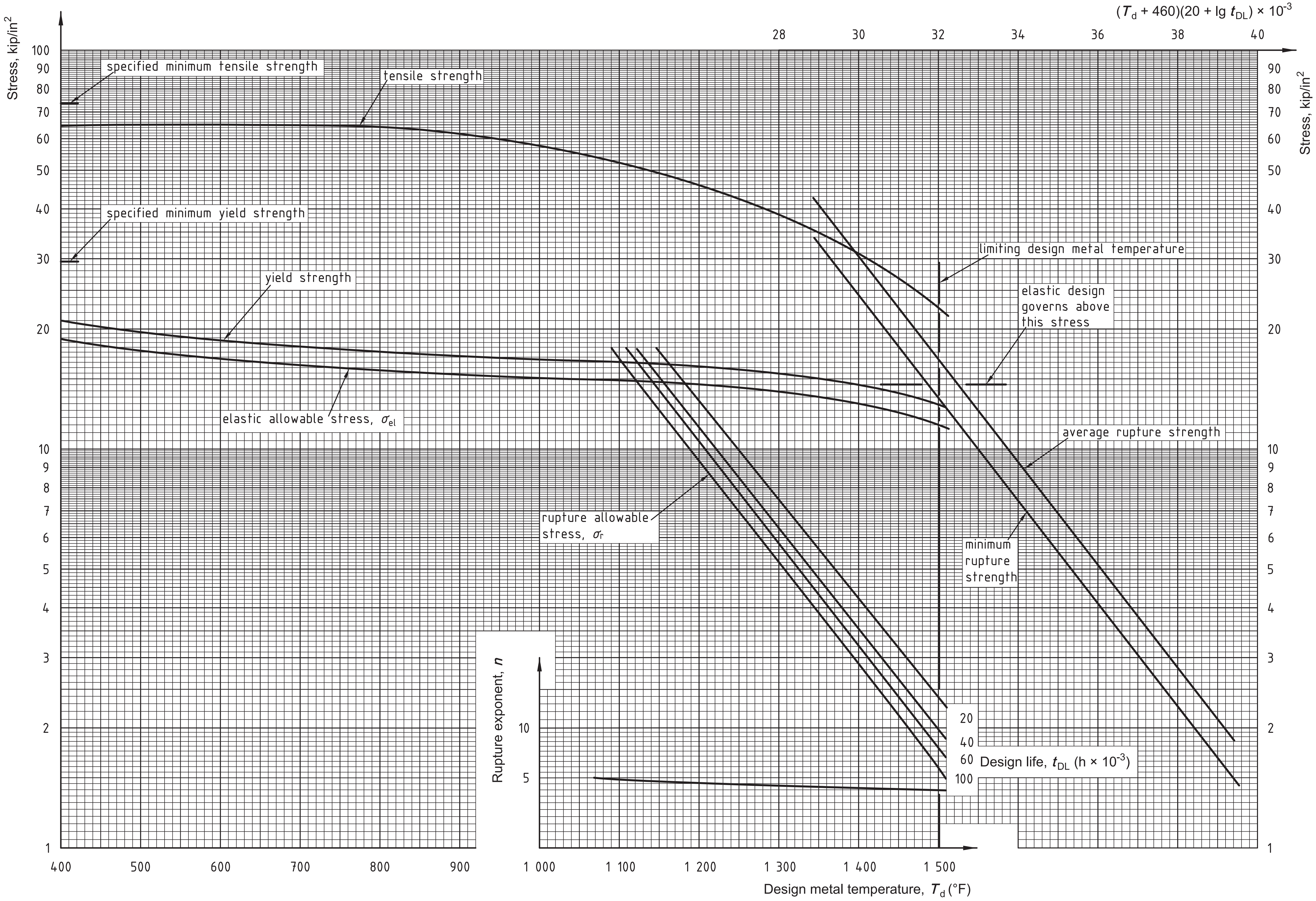
Figure F.11 — Stress curves (USC units) for ASTM A 200 T91, ASTM A 213 T91 and ASTM A 335 P91 9Cr-1Mo-V steels



NOTE 1 The unit "kip/in^2" (kilopounds per square inch) is referred to as kilo "pound-force per square inch" in ISO/IEC 80000.

NOTE 2 Above 1 000 °F, the stress values for type 304 apply only if carbon content is 0,04 % or higher.

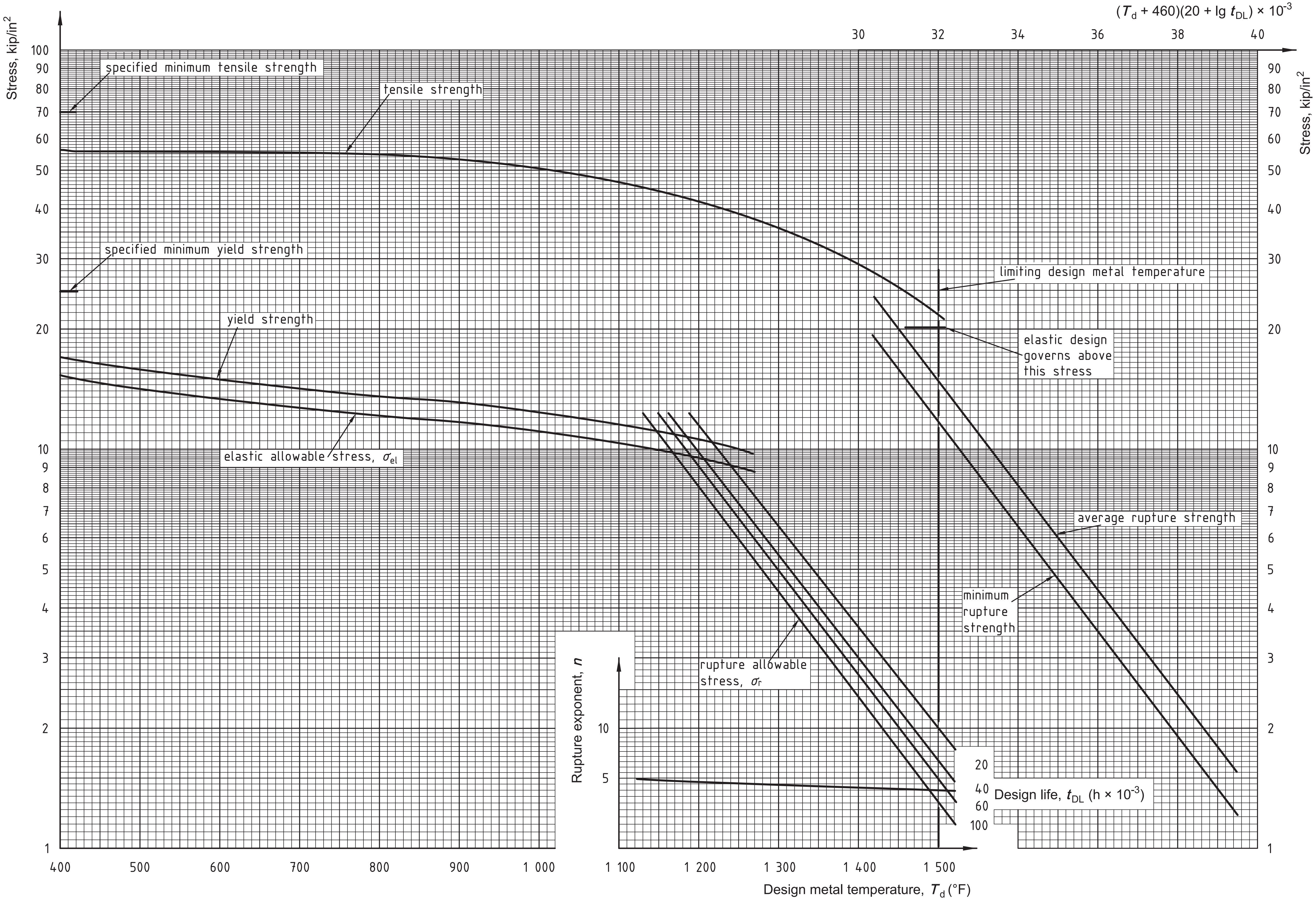
Figure F.12 — Stress curves (USC units) for ASTM A 213, ASTM A 271, ASTM A 312 and ASTM A 376 types 304 and 304H (18Cr-8Ni) stainless steels



NOTE 1 The unit "kip/in²" (kilopounds per square inch) is referred to as kilo "pound-force per square inch" in ISO/IEC 80000.

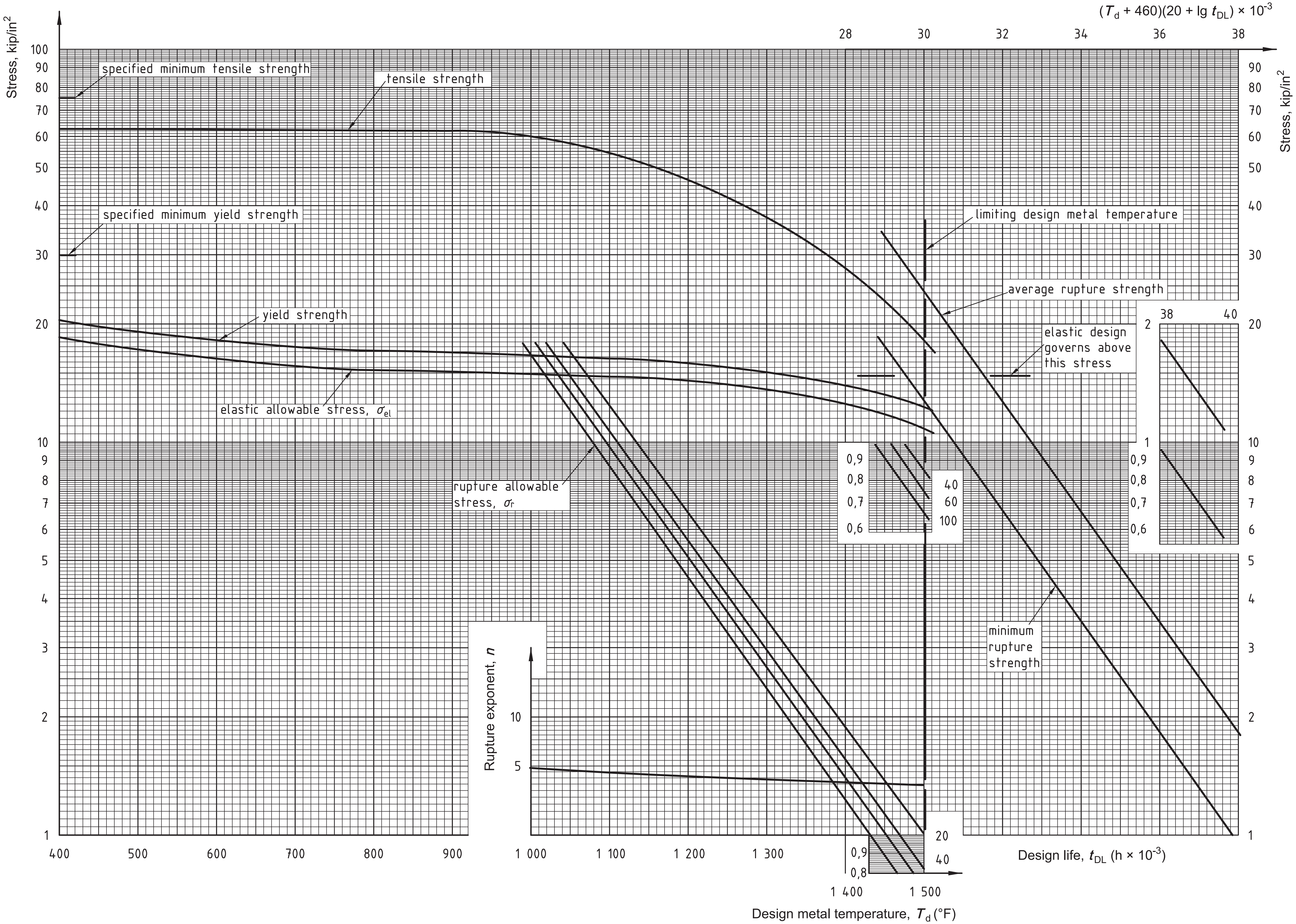
NOTE 2 Above 1 000 °F, the stress values for type 316 apply only if carbon content is 0,04 % or higher.

Figure F.13 — Stress curves (USC units) for ASTM A 213, ASTM A 271, ASTM A 312 and ASTM A 376 types 316 and 316H (16Cr-12Ni-2Mo) stainless steels



NOTE The unit "kip/in²" (kilopounds per square inch) is referred to as kilo "pound-force per square inch" in ISO/IEC 80000.

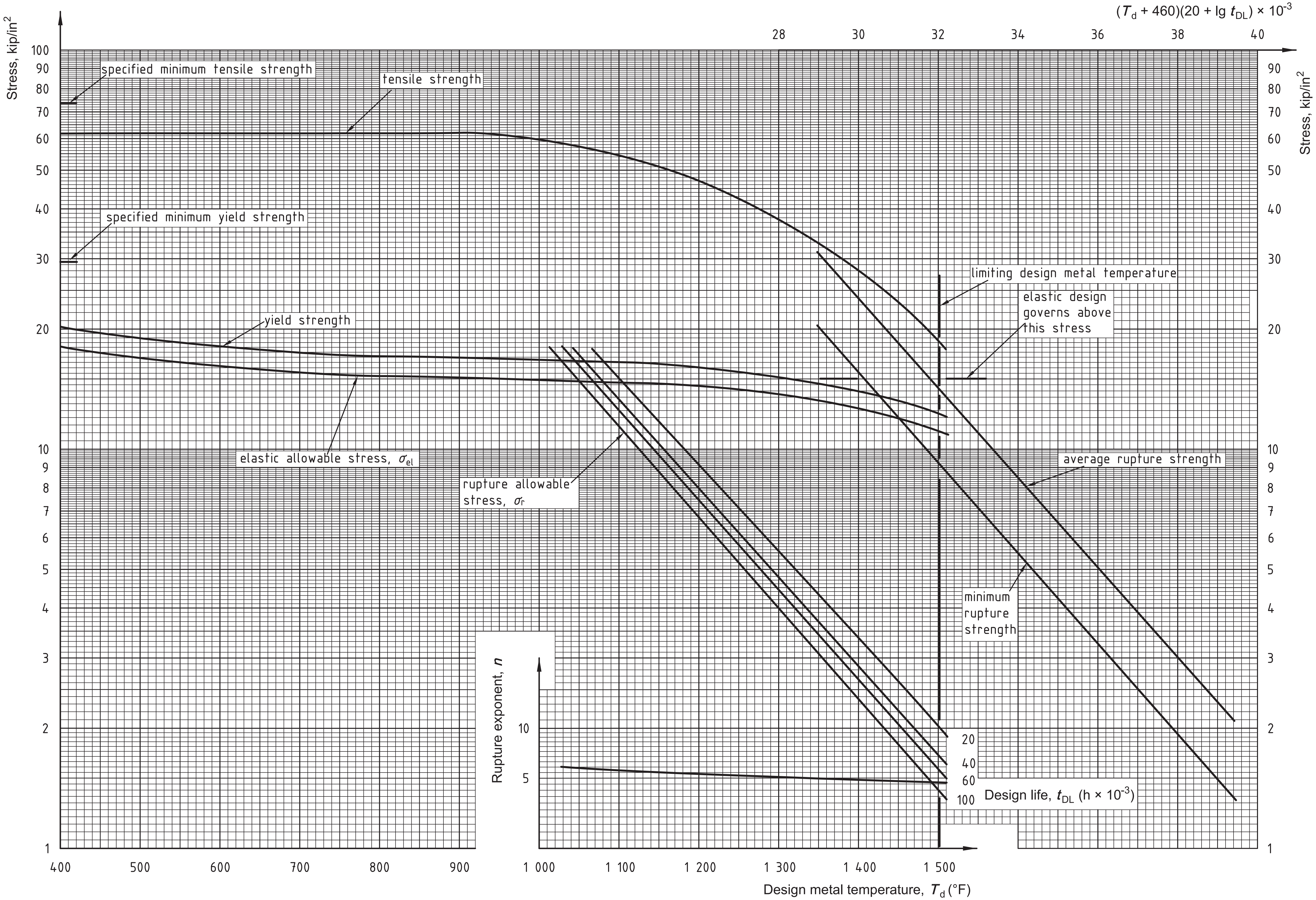
Figure F.14 — Stress curves (USC units) for ASTM A 213 and ASTM A 312 type 316L (16Cr-12Ni-2Mo) stainless steels



NOTE 1 The unit "kip/in²" (kilopounds per square inch) is referred to as kilo "pound-force per square inch" in ISO/IEC 80000.

NOTE 2 Above 1 000 °F, the stress values for type 321 apply only if carbon content is 0,04 % or higher.

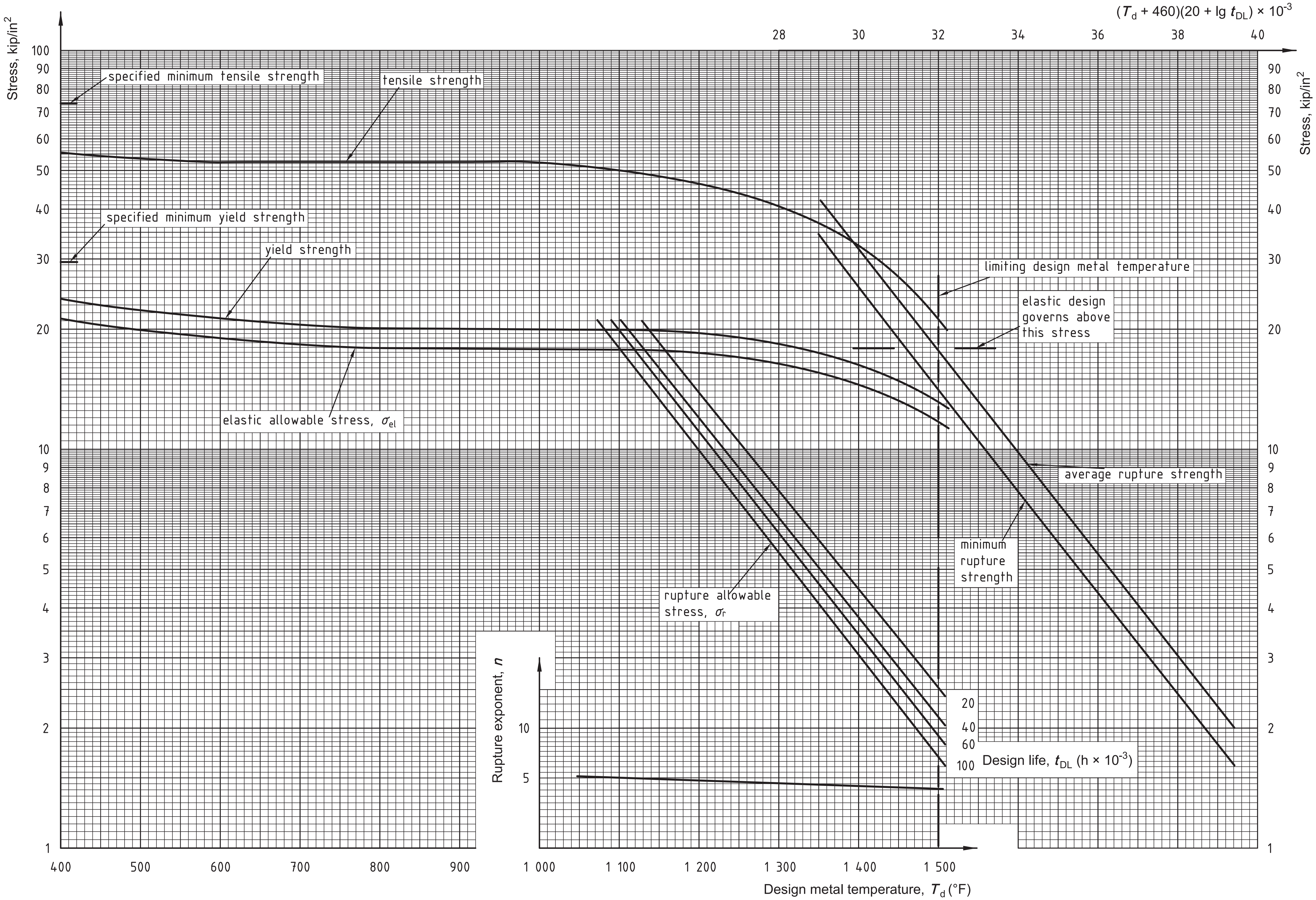
Figure F.15 — Stress curves (USC units) for ASTM A 213, ASTM A 271, ASTM A 312 and ASTM A 376 type 321 (18Cr-10Ni-Ti) stainless steels



NOTE 1 The unit "kip/in²" (kilopounds per square inch) is referred to as kilo "pound-force per square inch" in ISO/IEC 80000.

NOTE 2 Above 1 000 °F, the stress values for type 347 apply only if carbon content is 0,04 % or higher.

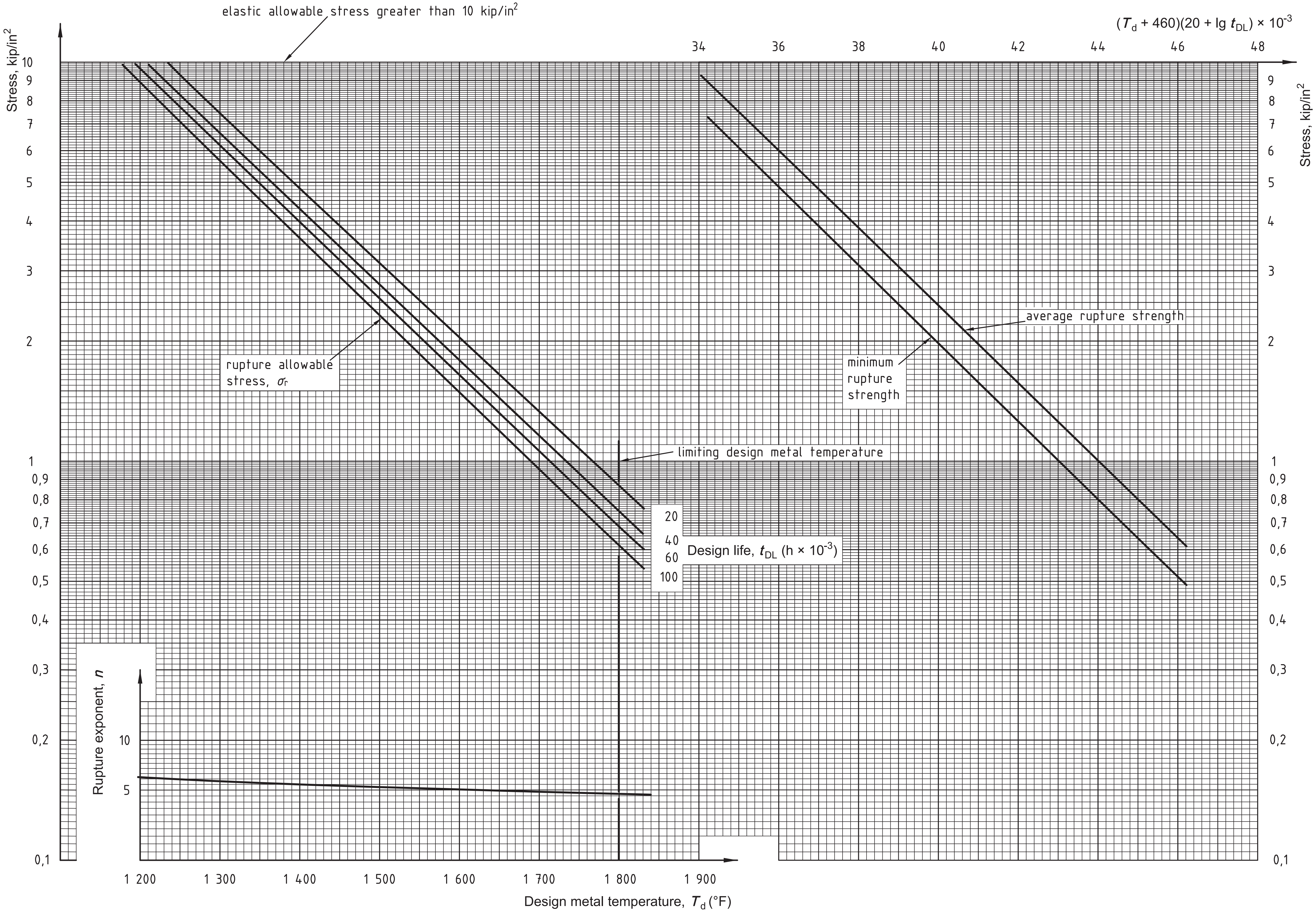
Figure F.16 — Stress curves (USC units) for ASTM A 213, ASTM A 271, ASTM A 312 and ASTM A 376 type 321H (18Cr-10Ni-Ti) stainless steels



NOTE 1 The unit "kip/in²" (kilopounds per square inch) is referred to as kilo "pound-force per square inch" in ISO/IEC 80000.

NOTE 2 Above 1 000 °F, the stress values for type 347 apply only if carbon content is 0,04 % or higher.

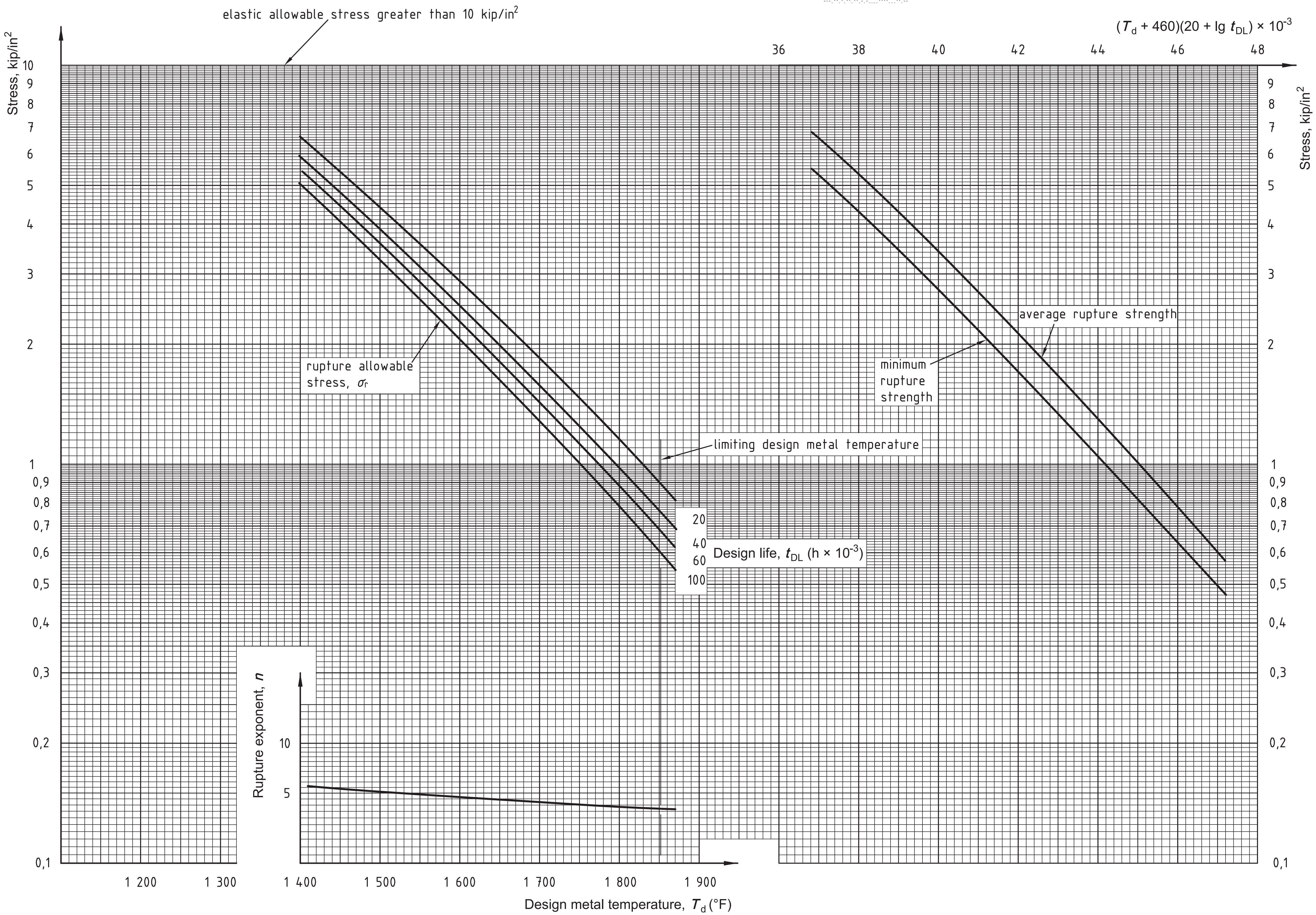
Figure F.17 — Stress curves (USC units) for ASTM A 213, ASTM A 271, ASTM A 312 and ASTM A 376 types 347 and 347H (18Cr-10Ni-Nb) stainless steels



NOTE 1 The unit "kip/in^2" (kilopounds per square inch) is referred to as kilo "pound-force per square inch" in ISO/IEC 80000.

NOTE 2 The average grain size corresponds to ASTM No. 5 or coarser.

Figure F.18 — Stress curves (USC units) for ASTM B 407 UNS N08810 and UNS N08811 alloys 800H and 800HT (Ni-Fe-Cr) stainless steels



NOTE The unit "kip/in²" (kilopounds per square inch) is referred to as kilo "pound-force per square inch" in ISO/IEC 80000.

Figure F.19 — Stress curves (USC units) for ASTM A 608 Grade HK40 (25Cr-20Ni) stainless steels

Annex G
(normative)

Derivation of corrosion fraction and temperature fraction

G.1 General

The 1958 version of API RP 530[28] contained a method for designing tubes in the creep-rupture range. The method took into consideration the effects of stress reductions produced by the corrosion allowance. In developing this design method, the following ideas were used.

At temperatures in the creep-rupture range, the life of a tube is limited. The rate of using up the life depends on temperature and stress. Under the assumption of constant temperature, the rate of using up the life increases as the stress increases. In other words, the tube lasts longer if the stress is lower.

If the tube undergoes corrosion or oxidation, the tube thickness will decrease in time. Therefore, under the assumption of constant pressure, the stress in the tube increases in time. As a result, the rate of using up the rupture life also increases in time.

An integral of this effect over the life of the tube was solved graphically in the 1988 version of API RP 530[29] and developed using the linear-damage rule (see Clause G.2). The result is a non-linear equation that provides the initial tube thickness for various combinations of design temperature and design life.

The concept of corrosion fraction used in 4.4 and derived in this annex is developed from the same ideas, and is a simplified method of achieving the same results.

Suppose a tube has an initial thickness, δ_σ, calculated using Equation (4). This is the minimum thickness required to achieve the design life without corrosion. If the tube does not undergo corrosion, the stress in the tube will always equal the minimum rupture strength for the design life, σ_r. This tube will probably fail after the end of the design life.

If this tube were designed for use in a corrosive environment and had a corrosion allowance of δ_{CA}, the minimum thickness, δ_{min}, can be set as given in Equation (G.1):

$$\delta_{min} = \delta_\sigma + \delta_{CA} \tag{G.1}$$

The stress is initially less than σ_r. After operating for its design life, the corrosion allowance is used up, and the stress is only then equal to σ_r. Since the stress has always been lower than σ_r, the tube still has some time to operate before it fails.

Suppose, instead, that the initial thickness were set as given in Equation (G.2):

$$\delta_{min} = \delta_\sigma + f_{corr}\delta_{CA} \tag{G.2}$$

In this equation, f_{corr} is a fraction less than unity. The stress is initially less than σ_r, and the rate of using up the rupture life is low. At the end of the design life, the tube thickness is as given in Equation (G.3):

$$\delta_{min} - \delta_{CA} = \delta_\sigma - (1 - f_{corr})\delta_{CA} \tag{G.3}$$

This thickness is less than δ_σ; therefore, at the end of the design life, the stress is greater than σ_r, and the rate of using up the rupture life is high. If the value of f_{corr} is selected properly, the integrated effect of this changing rate of using up the rupture life yields a rupture life equal to the design life. The corrosion fraction, f_{corr}, given in Figure 1 is such a value.

The curves in Figure 1 were developed by solving the non-linear equation that results from applying the linear-damage rule. Figure 1 can be applied to any design life, provided only that the corrosion allowance, δ_{CA}, and rupture allowable stress, σ_r, are based on the same design life.

G.2 Linear-damage rule

Consider a tube that is operated at a constant stress, σ, and a constant temperature, T, for a period of time, Δt. Corresponding to this stress and temperature is the rupture life, t_r, as given in Equation (G.4):

$$t_r = t_r(\sigma, T) \tag{G.4}$$

The fraction, $\Delta t/t$, is then the fraction of the rupture life used up during this operating period. After j operating periods, each with a corresponding fraction as given in Equation (G.5),

$$\left(\frac{\Delta t}{t_r}\right)_{i=1,2,3,\ldots j} \tag{G.5}$$

the total fraction, F (also known as the life fraction), of the rupture life used up would be the sum of the fractions used in each period, as given in Equation (G.6):

$$F(j) = \sum_{i=1}^{j}\left(\frac{\Delta t}{t_r}\right)_i \tag{G.6}$$

In developing this equation, no restrictions were placed on the stress and temperature from period to period. It was assumed only that during any one period the stress and temperature were constant. The life fraction, therefore, provides a way of estimating the rupture life used up after periods of varying stress and temperature.

The linear-damage rule asserts that creep rupture occurs when the life fraction totals unity, that is, when $F(j) = 1$.

The limitations of this rule are not well understood. Nevertheless, the engineering utility of this rule is widely accepted, and this rule is frequently used in both creep-rupture and fatigue analysis (see References [31], [32], [33] and [34]).

G.3 Derivation of equation for corrosion fraction

With continually varying stress and temperature, the life fraction can be expressed as an integral as given in Equation (G.7):

$$F(t_{op}) = \int_0^{t_{op}} \frac{dt}{t_r} \tag{G.7}$$

where

t_{op} is the operating life;

t_r is $t_r(\sigma, T)$, i.e. the rupture life at stress, σ, and temperature, T;

t is the time.

In general, both the stress, σ, and the temperature, T, are functions of time.

The rupture life, t_r, can be related to the stress as given in Equation (G.8), at least over limited regions of stress or time (see H.4):

$$t_r = m\sigma^{-n} \tag{G.8}$$

where

m is a material parameter which is a function of temperature;

n is the rupture exponent, which is a function of temperature and is related to the slope of the stress-rupture curve.

For a specified design life, t_{DL}, and corresponding rupture strength, σ_r, Equations (G.9) to (G.11) hold:

$$t_{DL} = m\sigma_r^{-n} \tag{G.9}$$

So:

$$m = t_{DL}\sigma_r^{n} \tag{G.10}$$

Hence:

$$t_r = t_{DL} \left(\frac{\sigma_r}{\sigma}\right)^n \tag{G.11}$$

Substituting Equation (G.11) into Equation (G.7), the life fraction can be expressed as given in Equation (G.12):

$$F(t_{op}) = \int_0^{t_{op}} \left[\frac{\sigma(t)}{\sigma_r}\right]^n \frac{dy}{t_{DL}} \tag{G.12}$$

where $\sigma(t)$ is the stress expressed as a function of time.

This integral can be calculated once the temperature and stress history are known, but in general this calculation is difficult to perform. For the purposes of this development for tube design, the temperature is assumed to be constant. (This assumption is not made in Clause G.5.) The remaining variable is, therefore, the stress as a function of time, $\sigma(t)$, which is given by the mean-diameter equation for stress as in Equation (G.13):

$$\sigma(t) = \frac{p_r}{2}\left(\frac{D_o}{\delta(t)} - 1\right) \tag{G.13}$$

where

p_r is the rupture design pressure;

D_o is the outside diameter;

$\delta(t)$ is the thickness expressed as a function of time.

In general, the rupture design pressure (operating pressure) is also a function of time; however, like temperature, it is assumed to be constant for the purposes of tube design. The thickness is determined from Equation (G.14):

$$\delta(t) = \delta_0 - \phi_{corr}\, t \tag{G.14}$$

where

δ_0 is the initial thickness;

ϕ_{corr} is the corrosion rate.

Calculating $F(t_{op})$ is then simply a matter of substituting Equations (G.13) and (G.14) into Equation (G.12) and integrating. This integration cannot be done in closed form; a simplifying assumption is needed.

Let δ_σ be the thickness calculated from σ_r as given in Equation (G.15):

$$\delta_\sigma = \frac{p_r D_o}{2\sigma_r + p_r} \tag{G.15}$$

To a first approximation, Equation (G.16) holds:

$$\sigma(t) \cong \frac{\delta_\sigma}{\delta(t)} \tag{G.16}$$

Substituting Equations (G.13), (G.14), and (G.16) into Equation (G.12) and integrating results in Equation (G.17):

$$F(t_{op}) = \frac{\delta_\sigma^n}{(n-1)\phi_{corr} t_{DL}} \left[\left(\frac{1}{\delta_0 - \phi_{corr} t_{op}} \right)^{n-1} - \left(\frac{1}{\delta_0} \right)^{n-1} \right] \tag{G.17}$$

At $t = t_{DL}$, $F(t_{DL})$ should equal unity; that is, the accumulated damage fraction should equal unity at the end of the design life. Using $F(t) = 1$ and $t = t_{DL}$ in Equation (G.17) results in Equation (G.18):

$$1 = \frac{\delta_\sigma^n}{(n-1)\varphi_{corr} t_{DL}} \left[\left(\frac{1}{\delta_0 - \varphi_{corr} t_{DL}} \right)^{n-1} - \left(\frac{1}{\delta_0} \right)^{n-1} \right] \tag{G.18}$$

Now let $\delta_0 = \delta_\sigma + f_{corr}\delta_{CA}$ and $B = \delta_{CA}/\delta_\sigma$, where $\delta_{CA} = \phi_{corr}\, t_{DL}$; that is, the corrosion allowance is defined as being equal to the corrosion rate times the design life. With these changes, Equation (G.18) reduces to an equation as a function of the corrosion fraction, f_{corr}, as given in Equation (G.19):

$$1 = \frac{1}{(n-1)B} \left[\left(\frac{1}{1 + f_{corr}B - B} \right)^{n-1} - \left(\frac{1}{1 + f_{corr}B} \right)^{n-1} \right] \tag{G.19}$$

For given values of B and n, Equation (G.19) can be solved for the corrosion fraction, f_{corr}. The solutions are shown in Figure 1.

G.4 Limitations of the corrosion fraction

In addition to the limitations of the linear-damage rule mentioned in Clause G.2, the corrosion fraction has other limitations. For the derivation, the temperature, pressure, and corrosion rate were assumed to be constant throughout the operating life. In an operating heater, these factors are usually not constant; nevertheless, the assumptions of constant pressure, temperature and corrosion rate are made for any tube design. The assumptions are, therefore, justified in this case, since the corrosion fraction is part of the rupture design procedure. (The assumption of constant temperature is not made in Clause G.5.)

The derivation of the corrosion fraction also relies on the relationship between rupture life and stress expressed in Equation (G.11). For those materials that show a straight-line Larson-Miller parameter curve in

Figures E.1 to E.19, this representation is exact. For those materials that show a curvilinear Larson-Miller parameter curve, using Equation (G.11) is equivalent to making a straight-line approximation of the curve. To minimize the resulting error, the values of the rupture exponent shown in Figures E.1 to E.19 were developed from the minimum 60 000-h and 100 000-h rupture strengths (see Clause H.4). In effect, this applies the straight-line approximation to a shorter segment of the curved line and minimizes the error over the usual range of application.

Finally, the mathematical approximation of Equation (G.16) was used. A more accurate approximation is available; however, when it is used, the resulting graphical solution for the corrosion fraction is more difficult to use. Furthermore, the resulting corrosion fraction differs from that given in Figure 1 by less than 0,5 %. This small error and the simplicity of using Figure 1 justify the approximation of Equation (G.16).

G.5 Derivation of equation for temperature fraction

Since tube design in the creep-rupture range is very sensitive to temperature, special consideration should be given to cases in which a large difference exists between start-of-run and end-of-run temperatures. In the derivation of the corrosion fraction in Clause G.3, the temperature was assumed to remain constant. The corrosion fraction can be applied to cases in which the temperature varies if an equivalent temperature can be calculated. The equivalent temperature should be such that a tube operating at this constant equivalent temperature sustains the same creep damage as a tube operating at the changing temperature. Equation (G.6) can be used to calculate an equivalent temperature for a case in which the temperature changes linearly from start of run to end of run.

Equation (G.11) was developed to relate the rupture life, t_r, to the applied stress, σ. A comparable equation is needed to relate the rupture life to both stress and temperature. This equation can be derived by means of the Larson-Miller parameter plot. When this plot is a straight line (or when the curve can be approximated by a straight line), the stress, σ, can be related to the Larson-Miller parameter, Γ, as given in Equation (G.20):

$$\sigma = a \times 10^{-b\Gamma} \quad \text{(G.20)}$$

where

a, b are curve-fit constants;

$\Gamma = T^* (C_{LM} + \lg t_r) \times 10^{-3}$;

T^* is the absolute temperature, expressed in kelvin;

C_{LM} is the Larson-Miller constant;

t_r is the rupture time, expressed in hours.

Solving Equation (G.20) for t_r yields Equation (G.21):

$$t_r = \frac{1}{10^{C_{LM}}} \left(\frac{a}{\sigma} \right)^{1\,000/(bT^*)} \quad \text{(G.21)}$$

Using Equation (G.21), the life fraction, $F(t_{op})$ given by Equation (G.7) becomes Equation (G.22):

$$F(t_{op}) = \int_0^{t_{op}} 10^{C_{LM}} \left(\frac{\sigma}{a} \right)^{1\,000/(bT^*)} dt \quad \text{(G.22)}$$

where

σ is stress as a function of time;

T^* is the absolute temperature as a function of time.

The thickness, $\delta(t)$, which is also a function of time, can be expressed as given in Equation (G.23):

$$\delta(t) = \delta_0 - \left(\frac{\Delta\delta}{t_{op}}\right)t \quad = \delta_0\left[1 - \left(\frac{\Delta\delta}{\delta_0}\right)\left(\frac{t}{t_{op}}\right)\right] \tag{G.23}$$

where

δ_0 is the initial thickness;

$\Delta\delta$ is the thickness change in time t_{op};

t_{op} is the duration of the operating period.

For this derivation, let

$$B = \frac{\Delta\delta}{\delta_0} \tag{G.24}$$

$$\rho = \frac{t}{t_{op}} \tag{G.25}$$

Therefore

$$\delta(t) = \delta_0(1 - B\rho) \tag{G.26}$$

Using Equations (G.13) and (G.26) and the approximation given by Equation (G.16), the stress can be expressed as given in Equation (G.27):

$$\sigma(t) \cong \sigma_0\left[\frac{\delta_0}{\delta(t)}\right] = \frac{\sigma_0}{1 - B\rho} \tag{G.27}$$

where

$$\sigma_0 = \frac{p_r}{2}\left(\frac{D_o}{\delta_0} - 1\right) \tag{G.28}$$

If a linear change in temperature occurs during the time t_{op}, then the temperature, T^*, can be expressed as a function of time, t, as given in Equation (G.29):

$$T^*(t) = T_0^* + \left(\frac{\Delta T}{t_{op}}\right)t = T_0^*\left[1 + \left(\frac{\Delta T}{T_0}\right)\left(\frac{t}{t_{op}}\right)\right] \tag{G.29}$$

where

T_0^* is the initial absolute temperature, expressed in kelvin;

ΔT is the temperature change in operating time period, t_{op}, expressed in kelvin.

Let

$$\gamma = \frac{\Delta T}{T_0^*} \tag{G.30}$$

Using Equations (G.25) and (G.30), the equation for temperature becomes as given in Equation (G.31):

$$T(t) = T_0^*(1 + \gamma\rho) \tag{G.31}$$

Using Equations (G.27) and (G.31), Equation (G.22) can be written as given in Equation (G.32):

$$F(t_{\text{op}}) = \int_0^1 10^{C_{\text{LM}}} \left[\left(\frac{\sigma_0}{a} \right) \left(\frac{1}{1 - B\rho} \right) \right]^{n_0/(1+\gamma\rho)} t_{\text{op}} \text{d}\rho \tag{G.32}$$

where

$$n_0 = \frac{1\ 000}{bT_0^*}$$

n_0 is the rupture exponent at the initial temperature, T_0^*.

The aim of this analysis is to find a constant equivalent temperature, T_{eq}^*, between T_0^* and ($T_0^* + \Delta T$) such that the life fraction at the end of the period t_{op} with the linearly changing temperature is equal to the life fraction with the equivalent temperature. This equivalent temperature can be expressed as given in Equation (G.33):

$$T_{\text{eq}}^* = T_0^*\ (1 + \gamma\varpi), \quad 0 < \varpi < 1 \tag{G.33}$$

From Equation (G.32), the resulting life fraction is as given in Equation (G.34):

$$F(t_{\text{op}}) = \int_0^1 10^{C_{\text{LM}}} \left[\left(\frac{\sigma_0}{a} \right) \left(\frac{1}{1 - B\rho} \right) \right]^{n_0/(1+\gamma\varpi)} t_{\text{op}} \text{d}\rho \tag{G.34}$$

Equating Equations (G.32) and (G.34) and dividing out common terms yields an integral equation for the parameter ϖ:

$$\int_0^1 \left[\left(\frac{\sigma_0}{a} \right) \left(\frac{1}{1 - B\rho} \right) \right]^{n_0/(1+\gamma\rho)} \text{d}\rho = \int_0^1 \left[\left(\frac{\sigma_0}{a} \right) \left(\frac{1}{1 - B\rho} \right) \right]^{n_0/(1+\gamma\varpi)} \text{d}\rho \tag{G.35}$$

For given values of σ_0, a, n_0, b, and γ, Equation (G.35) can be solved numerically for ϖ. Using ϖ and Equations (G.30) and (G.33), the equivalent temperature is calculated as given in Equation (G.36):

$$T_{\text{eq}}^* = T_0^* \left(1 + \frac{\Delta T}{T_0^*} \varpi \right) = T_0^* + \varpi \Delta T \tag{G.36}$$

The parameter ϖ is the temperature fraction, f_T, in 4.8.

The solutions to Equation (G.35) can be approximated by a graph if the given values are combined into two parameters as given in Equations (G.37) and (G.38):

$$V = n_0 \gamma \ln\left(\frac{a}{\sigma_0}\right) = n_0 \left(\frac{\Delta T}{T_0^*}\right) \ln\left(\frac{a}{\sigma_0}\right) \quad \text{(G.37)}$$

$$N = n_0 B = n_0 \left(\frac{\Delta\sigma}{\sigma_0}\right) \quad \text{(G.38)}$$

Using these two parameters, the solutions to Equation (G.35) are shown in Figure 2.

The constant A in Table 3 is one of the least-squares curve-fit constants, a and b, in the equation $\sigma = a \times 10^{-b\Gamma}$, where Γ is the Larson-Miller parameter and σ is the minimum rupture strength. For materials that have a linear Larson-Miller parameter curve, A can be calculated directly from any two points on the curve. For all other materials, a least-squares approximation of the minimum rupture strength is calculated in the stress region below the intersection of the rupture and elastic allowable stresses, since this is the region of most applications. For the purpose of calculating the temperature fraction, this accuracy is sufficient.

Annex H
(informative)

Data sources

H.1 General

Whenever possible, the yield-, tensile- and rupture-strength data displayed in Figures E.1 to E.19 and Figures F.1 to F.19 were taken from the ASTM Data Series Publications [22],[23],[24],[25],[26],[27] as explained in Table H.1. These publications contain discussions and detailed descriptions of the data that are not repeated in this annex. The material that follows is limited to a discussion of deviations from published data and of data that have been used but are not generally available.

H.2 Minimum rupture strength

The ASTM Data Series Publications contain evaluations of various rupture-strength extrapolation techniques. From these evaluations, the most reliable extrapolation was selected. The average and minimum 100 000-h rupture strengths calculated by this method are used in this International Standard. The minimum rupture strength used is the lower 95 % confidence limit; 95 % of all samples should have rupture strengths greater than this value. This minimum rupture strength is obtained by using least-squares techniques to calculate a curve for the average rupture strength and subtracting 1,65 times the standard deviation of the data from this average. The Data Series reference with its specific figure number for each alloy are listed in Table H.1.

Table H.1 — Sources of data for yield, tensile and rupture strengths

Alloy	ASTM publication	Yield strength[a]	Tensile strength[a]	Rupture strength	Method used	Comments
Carbon steels	DS 11S1[24]	Figure 7c	Figure 7d	(See H.6.1)	LM [b]	Fine-grained, tempered values used.
C-½Mo steel	DS 47[25]	Figure 7a	Figure 7b	(See H.6.2)	LM	—
1¼Cr-½Mo steel	DS 50[26]	Figure 6c	Figure 6d	(See H.6.3)	IL[c]	Non-plate values used.
2¼Cr-1Mo steel	DS 6S2[23]	Figure 7a	Figure 7b	(See H.6.4)	MC [d]	—
3Cr-1Mo steel	DS 58[27]	Figure 7a	Figure 7b	Figure 17c [a]	IL	—
5Cr-½Mo steel	DS 58[27]	Figure 8a	Figure 8b	Figure 26c [a]	IL	—
5Cr-½Mo-Si steel	DS 58[27]	Figure 9a	Figure 9b	Figure 33c [a]	IL	—
7Cr-½Mo steel	DS 58[27]	Figure 11a	Figure 11b	Figure 47c [a]	IL	—
9Cr-1Mo steel	DS 58[27]	Figure 12a	Figure 12b	Figure 54c [a]	IL	—
9Cr-1Mo-V steel	MPC [e]	—	—	—	LM	—
18Cr-8Ni steel	DS 5S2[22]	Figure 14b	Figure 15b	Tables 7, 10 [a]	IL	Adjusted values used. Figures 14a and 15a used above 540 °C (1 000 °F).[a]
16Cr-12Ni-2Mo steel	DS 5S2[22]	Figure 14e	Figure 15e	Tables 7, 10 [a]	IL	Adjusted values used.
16Cr-12Ni-2Mo (316L) steel	DS 5S2[22]	Figure 14f	Figure 15f	Table 7 [a]	IL	Minimum is 80 % of average.
18Cr-10Ni-Ti steel	DS 5S2[22]	Figure 14g	Figure 15g	Tables 7, 10 [a]	IL	Adjusted values used.
18Cr-10Ni-Nb steel	DS 5S2[22]	Figure 14h	Figure 15h	Tables 7, 10 [a]	IL	Adjusted values used.
Ni-Fe-Cr (Alloy 800H/800HT)	—	—	—	(See H.6.5)	LM	—
25Cr-20Ni (HK40)	—	—	—	(See H.6.6)	LM	—

[a] Reference to the ASTM Data Series publication given in column 2.

[b] LM indicates Larson-Miller.

[c] IL indicates individual lots (see ASTM DS publication for definition).

[d] MC indicates Manson Compromise.

[e] Data from Materials Properties Council, Inc.

H.3 Larson-Miller parameter curves

The Larson-Miller parameter combines design metal temperature, T_d, and design life, t_{DL}, expressed in hours, as follows.

When T_d is expressed in degrees Celsius, the parameter has the form of Equation (H.1):

$$(T_d + 273)\,(C_{LM} + \lg t_{DL}) \times 10^{-3} \quad \text{(H.1)}$$

When T_d is expressed in degrees Fahrenheit, the parameter has the form of Equation (H.2):

$$(T_d + 460)\,(C_{LM} + \lg t_{DL}) \times 10^{-3} \quad \text{(H.2)}$$

The generally accepted empirical values of $C_{LM} = 20$ and $C_{LM} = 15$ are used for ferritic steels and austenitic steels, respectively. The value of $C_{LM} = 30$ is used for T91 or P91, 9Cr-1Mo-V steel. To calculate the rupture allowable stress for any given design metal temperature and design life, the appropriate value of C_{LM} should be used to calculate the parameter, and one of the Larson-Miller parameter curves should then be used to find the corresponding rupture strength.

To the right in Figures E.1 to E.19 (Figures F.1 to F.19) are Larson-Miller parameter curves that permit tubes to be designed for lives other than 100 000 h. These curves were developed from the average and minimum 100 000-h rupture strengths. They can be used to estimate the rupture allowable stress (minimum rupture strength) for design lives from 20 000 h to 200 000 h. The resulting 20 000-h, 40 000-h and 60 000-h rupture allowable stresses are shown with the 100 000-h rupture allowable stress to the left in Figures E.1 to E.19 (Figures F.1 to F.19).

This is not the normal use of the Larson-Miller parameter. The Larson-Miller curve is traditionally developed from rupture-strength test data as one way to extrapolate long-term rupture strengths from short-term data. The resulting extrapolation is suitable for some alloys but not for all. Most of the ASTM Data Series Publications listed in Table H.1 examine the suitability of this Larson-Miller extrapolation.

The Larson-Miller parameter curves used in this International Standard were developed from the extrapolated values of the 100 000-h rupture strength. The values used are those listed in the various ASTM Data Series Publications. They have been estimated in the manner believed to be most reliable. For low- and medium-carbon steels, alloy 800H/800HT, and HK40, the 100 000-h rupture strength has been estimated using a Larson-Miller extrapolation (other means have been used for the other alloys). Table H.1 lists the extrapolation method used for each alloy. Consequently, the Larson-Miller parameter curves in this International Standard are not the same as those shown in the various ASTM Data Series Publications. For those cases in which the 100 000-h rupture strength was determined by other means, the Larson-Miller parameter curves in this International Standard might not give reliable estimates of the rupture strength for times less than 20 000 h or more than 200 000 h.

H.4 Rupture exponent

Constant-temperature creep-rupture data can be conveniently plotted on a log-log graph, log (stress) versus log (rupture time). These stress-rupture curves can often be represented by a straight line or can be approximated by a straight line in limited regions. The straight line for the rupture time, t_r, can be expressed as given in Equation (H.3):

$$t_r = m\sigma^{-n} \quad \text{(H.3)}$$

where

- m is a material parameter which is a function of temperature;
- n is the rupture exponent, which is a function of temperature and is related to the slope of the stress-rupture curve;
- σ is the stress.

The value of the rupture exponent, n, can be calculated from two points on the curve. If the rupture time for a stress σ_1 is t_{r1} and the rupture time for a stress σ_2 is t_{r2}, then n can be calculated from Equation (H.4):

$$n = \frac{\lg\left(t_{r1}/t_{r2}\right)}{\lg\left(\sigma_2/\sigma_1\right)} \tag{H.4}$$

If the stress-rupture curve is a straight line, any two points on that line give the same value of n. If the stress-rupture curve is not a straight line, the value of n depends on which two points are chosen, since the slope of the straight-line approximation depends on which part of the curve is approximated.

The rupture exponents plotted in Figures E.1 to E.19 (Figures F.1 to F.19) were determined from the 60 000-h and 100 000-h minimum rupture strengths as estimated by the Larson-Miller parameter curves. These particular times were chosen to give a straight-line approximation over the range of the usual operating stress levels.

H.5 Modification of, and additions to, published data

Whenever possible, the data used to generate Figures E.1 to E.19 (Figures F.1 to F.19) were taken from the ASTM Data Series Publications[22],[23],[24],[25],[26],[27]. Specific figure and table references for the yield, tensile, and rupture strengths are given in Table H.1. In some cases, the rupture-strength extrapolations were modified for this practice, or the data were used to develop new extrapolations. These modifications and additions are described in H.6.2 to H.6.8. Alloy 800H/800HT and HK40 are not covered by recent ASTM publications. The data used to develop the figures for these alloys are described in H.6.5 and H.6.6, respectively.

H.6 Steels

H.6.1 Carbon steels

For the determination of rupture strength in Data Series 11S1 no distinction is made between low-carbon steel (A 192) and medium-carbon steel (A 106 and A 210). Data from all three alloys were used to calculate the Larson-Miller curve in Data Series 11S1. For this International Standard, a distinction was made by separating the data and calculating two Larson-Miller curves: for low-carbon steel in Figures E.1 and F.1 and for medium-carbon steel in Figures E.2 and F.2. The procedure for establishing the average and minimum rupture strengths was otherwise identical to that used in Data Series 11S1. Larson-Miller curves that represent the average strength were generated by the least-squares method; curves that represent minimum strength were generated by subtracting from the average-strength curves 1,65 times the standard deviation of the data.

H.6.2 C-$^1/_2$Mo steel

The Larson-Miller curves in Data Series 47, Figure 18a, have an inflection point close to a parameter value of 37. The upturn to the right is considered questionable. For this International Standard, the parameter curves shown in Figure F.3 were arbitrarily extended by straight lines above a parameter value of 37. These extensions are shown as dashed lines in Figure E.3 (Figure F.3).

H.6.3 1$^1/_4$Cr-$^1/_2$Mo steel

The regression of the individual lot extrapolations in Data Series 50, Figure 27c, used a polynomial of third degree or higher. The resulting average and minimum rupture-strength curves show an upturn to the right. This upturn also results when the data points shown on Figure 27c are fitted with a quadratic curve. Since this upturn is considered questionable, the data points shown in Figure 27c were used to calculate a first-degree curve for this International Standard. The resulting curves for average and minimum rupture strengths are shown in Figure E.4 (Figure F.4).

H.6.4 2¼Cr-1Mo steel

The most reasonable extrapolation in Data Series 6S2 is provided by the strength-temperature regression curve shown in Figure 22 and again in Figure 26. As with 1¼Cr-½Mo steel in Data Series 50, the regression used a polynomial of third degree or higher. The resulting curve is considered questionable. For this International Standard the Manson Compromise curve in Data Series 6S2, Figure 26, was used below 595 °C (1 100 °F) and was extended downward to intersect the strength-temperature regression curve at 650 °C (1 200 °F). The resulting curves for average and minimum 100 000-h rupture strength shown in Figure E.5 (Figure F.5) of this International Standard are generally equal to or below the strength-temperature regression curves of Data Series 6S2.

H.6.5 Ni-Fe-Cr (Alloy 800H/800HT)

The Larson-Miller curves for alloy 800H/800HT in Figure E.18 (Figure F.18) were developed from 91 rupture-test data points from one source. These tests used samples from six heats of alloy 800H/800HT (with appropriate chemistry and grain size) that were made in bar, plate and tube product forms. All tests were run at temperatures of 980 °C (1 800 °F) or lower, except for one that was run at 1 040 °C (1 900 °F). The linear curves for the average and minimum rupture strengths were calculated using least-squares techniques. Using a quadratic curve did not appreciably improve the fit of these data.

H.6.6 25Cr-20Ni (HK40)

The Larson-Miller curves for HK40 in Figure E.19 (Figure F.19) were developed from 87 rupture-test data points. These tests came from four sources and involved seven heats of HK40. The carbon content of these heats ranged from 0,35 to 0,45. No data from tests that were run at temperatures of 1 040 °C (1 900 °F) or higher were used in this evaluation, since significant metallurgical changes that affect the rupture strength occur above this temperature. The quadratic curves for the average and minimum rupture strengths were calculated using least squares techniques.

H.6.7 25Cr-35Ni-HP-modified

Stress curves for HP-modified cast tubes are not included. This material is proprietary to individual foundries. As such, it is not feasible to develop generic stress data that would apply to all manufacturers of this material.

H.6.8 9Cr-1Mo-V steel

The maximum limit for this material has been restricted to 650 °C (1 200 °F) due to the lack of stress data above this temperature; see Figure E.11 (Figure F.11).

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