
Finite element analysis of sand-casting defect effects on the service life of aluminum alloy flange coupling components

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Abstract. Aluminum alloy flange couplings are widely used in rotating machinery and power-transmission systems, but their production by sand casting, can introduce defects such as gas porosity, shrinkage porosity, and oxide bifilms. Although such defects can act as stress concentrator and promote fatigue crack initiation, their effect on flange coupling life under combined axial-torsional loading has not been quantified. This study addresses that gap through an integrated framework combining solidification simulation, finite element analysis (FEA), and continuum damage mechanics (CDM). Defect-free and defective 3D models of an A356-T6 flange coupling were analyzed in ANSYS under nine combined axial force (10–30 kN) and torque (2–6 kN·m) under fully reversed loading $R = -1$, followed by extended finite element method (XFEM) crack propagation analysis. The results indicate that the presence of defect increases maximum von-Mises stress by 13–22% and reduce minimum fatigue life by 52–61%, with the severe case dropping life from 85,340 to 33,376 cycles (60.89% reduction). A bulk damage estimate based on porosity fraction (20–36% reduction) under-predicted the reduction obtained with explicit voids, showing that void volume fraction alone is not a sufficient quality indicator. Overall, this work provides a numerical framework for assessing the fatigue performance of sand-cast aluminum flange couplings under combined axial-torsional loading.

1. Introduction

Aluminum alloy flange couplings are key components in piping, power-transmission systems, and automotive, due to their light weight, corrosion resistance, versatility, capability to produce large scale part economically, and ease of assembly. These components are almost produced by sand casting, a process that unavoidably introduces gas porosity, shrinkage porosity, and oxide bifilms, all recognized causes of premature fatigue failure in cast components [1, 2]. These defects act as local stress concentrators and fatigue crack initiation sites, particularly at geometrically critical locations such as the hub-flange fillet, and are the critical cause of shortened service life under cyclic operating conditions [3, 4, 5]. In casting practice, it is essentially impossible to produce components entirely free from defects. Consequently, most conventional designs rely on large safety factors, often resulting in over-designed, unnecessarily heavy components. However, there is limited research quantifying how the size, shape, and location of casting defects; specifically within the stress-concentration zone of a flange coupling affect its service life under the combined axial-torsional loading typical of motor-pump systems. Advanced defect-characterization

equipment such as computed tomography (CT) scanning, scanning electron microscopy (SEM), and acoustic (AE) is rarely available in academic settings such as those in Ethiopia, and without detailed FEA it is difficult to predict whether a given defect size will remain stable or significantly accelerate fatigue damage under operational loads.

The economic stakes are significant: sand and high-pressure casting operations for aluminum alloys report component rejection rates on the order of 3–5% due to porosity, shrinkage, and related defects [6], and real-world failure investigations; such as a diesel engine cylinder head failure traced directly to fatigue cracks initiated at casting porosity [7] and illustrate how a defect invisible to visual inspection can govern the failure of an entire component. Recent work has begun applying CDM to flange-type components specifically: Wang et al. [8] incorporated pore size directly into a CDM damage-evolution equation to predict fatigue life of die-cast aluminum, while more recent studies have extended damage-mechanics and FEA approaches to welded flange shafts [9] and rigid flange coupling material optimization [10], though neither incorporates sand-casting defect physics for cast aluminum flanges specifically. This study addresses that gap by (1) identifying the principal sand-casting defects relevant to aluminum flange couplings, (2) developing a constitutive damage model linking defect volume fraction and local stress concentration to fatigue life reduction, (3) implementing defect-free versus defective FEA under combined axial-torsional cyclic loading, and (4) evaluating the numerical results through mesh convergence, analytical comparison, and comparison with published fatigue data.

1.1. Related work on casting defects and stress concentration

Casting defects in aluminum alloys comes from the interaction of fluid flow, solidification, heat transfer, and mechanical stress concentration, that primary condition for porosity, shrinkage, segregation, and oxide inclusions [11]. Numerical simulation of hydrogen pore formation and its interaction with dendritic and eutectic structures has been used to express oxide nucleation and micro-porosity formation in aluminum-silicon alloys [11, 12], and generalized criterion functions have been developed and validated for predicting shrinkage porosity in wheel castings [13]. Entrained oxide bifilms are recognized as extending crack-like defects that act as nucleation sites for hydrogen pores, as confirmed through reduced-pressure testing [14], and integration of non-destructive CT scanning with Gurson porous plasticity modeling has been used to correlate melt quality with tensile properties [15]. Larger interconnected pores are more mechanically affect than small scattered ones, according to a thorough analysis of porosity sources in Al-Si cast alloys [16].

Casting defects, especially porosity and shrinkage pores, has been primary stress concentrators influencing fatigue strength and crack initiation [17]. Stress concentration around single and multiple pore models was shown to increase with pore size and proximity to the free surface, with finite-element-based fatigue predictions showing 5–30% error relative to experimental data [18]. Defect morphology and position further govern the fatigue limit, with a defect behaving as “internal” once its distance from the surface exceeds a threshold value, as confirmed by 3D X-ray micro-tomography and finite element analysis [19]. Fatigue-limit prediction models for extruded aluminum alloys demonstrate that manufacturing porosity acts as a stress concentrator analogous to casting defects [20], and the cylinder head failure investigation discussed above confirms that casting porosity independently governs component failure regardless of loading condition [7].

1.2. Related work on fatigue crack mechanisms and numerical modeling

Particularly in cast aluminum, fractographic transitions from intergranular to transgranular failure under fatigue crack growth have been reported for AA6061 alloy [22], and crack mechanisms in A356-T6 transition with stress intensity factor range from crystallographic to eutectic-silicon-controlled growth as micro-structure varies [21], and for AA6061 alloy under fatigue crack de-

velopment, fractographic transitions from intergranular to transgranular failure with increasing stress have been reported [22]. Fatigue crack nucleation from clusters of non-metallic inclusions in high-strength aluminum alloys has been further modeled by peridynamics simulation frameworks [23].

Cohesive XFEM models calibrated on 7075-T6 aluminum separate plasticity effects from crack propagation under overload conditions [24], and the XFEM and Linear Elastic Fracture Mechanics (LEFM) have emerged as key tools for fatigue crack growth simulation. A recent review of XFEM applications to fatigue crack growth and fracture in welded joints shows that zone-specific material modeling can improve fatigue life predictions by 10–15% over homogeneous models [25]. Also, casting processes have been optimized and cast-component directly predicted through the use of finite element simulation. It has been shown that porosity, secondary dendritic arm spacing (SDAS), and clustered defects detected by computed tomography have significantly influence on sponge-like porosity layers under complex service loads [26, 27]. A previous study used effective-depth techniques and Kitagawa–Takahashi diagrams to evaluate the fatigue limits of defective A357-T6 alloy under different loading ratios [28].

Numerical fatigue-life prediction still relies heavily on damage-tolerance and safe-life design criteria. A356-T6’s multiaxial fatigue behavior under combined loading has been fractographically characterized, offering a validation reference for mixed-mode crack propagation under combined axial-torsional loading, including the effects of residual stress [30]. Mixed-mode fatigue crack growth models incorporating the Paris law and equivalent stress intensity factor formulations have demonstrated close agreement with experimental data for Al 6061-T6 [29]. The fatigue life of high-pressure die-cast aluminum has been successfully predicted by continuum damage mechanics methods that directly incorporate pore size into the damage evolution equation [8], and fatigue life prediction based explicitly on porosity characteristics has been shown for cast aluminum alloys more generally [31]. Defect-correlated Kitagawa-Takahashi diagrams derived from computed tomography provide further validation benchmarks for the present study [32]. The dimensionless Niyama criterion [33] and pore-centric models combining shrinkage and gas porosity [34] support the physical basis for the defect volume fractions adopted here.

Although many studies have examined casting defects and fatigue behavior, previous work has not addressed flange-specific multiaxial fatigue behavior together with realistic sand-casting defect modeling in aluminum components. The current work immediately fills the gap left by recent continuum damage mechanics applications to flange-type components, which have been restricted to welded steel flange shafts [9] or defect-free geometric and material optimization of stiff flange couplings [10].

2. Methodology

2.1. Model geometry and material

The material selected is the aluminum-silicon-magnesium casting alloy A356-T6 (Al-7Si-0.3Mg), sand cast and T6 heat treated (solution treatment at 540°C plus artificial aging), widely used for flange couplings in automotive, industrial, and power-transmission applications. Key mechanical properties used in simulation are summarized in Table 1.

Table 1. Basic mechanical and fatigue properties of A356-T6 (sand cast).

Property	Value
Density	2680 kg/m ³
Young’s modulus	72 GPa
Poisson’s ratio	0.33
Yield strength (0.2% offset)	220 MPa
Ultimate tensile strength	310 MPa
Endurance limit (defect-free, 10 ⁷ cycles)	75 MPa

A standard rigid flange coupling was modeled in SolidWorks for a 40 mm shaft diameter (hub outer diameter 80 mm, flange outer diameter 160 mm, 6 × M12 bolts, hub-flange fillet radius 5 mm).

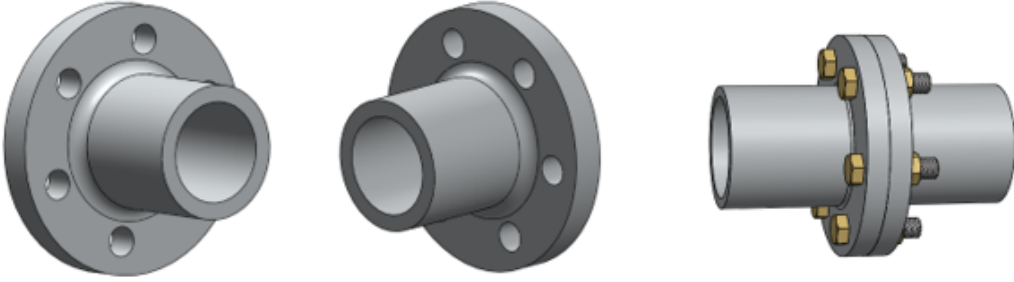


Figure 1. 3D design of the flange coupling: single flange (defect-free and defective) and assembled configuration.

2.2. Defect modeling and constitutive damage approach

Three principal sand-casting defects were considered: gas porosity, shrinkage, and oxide bifilms. Defect locations were justified using a transient heat-conduction solidification simulation form (Finite Difference Method (FEM), axisymmetric cylindrical coordinates), which confirmed that slower cooling near thick sections promotes larger and many defects at these specific locations, consistently with dimensionless Niyama criterion [33] and pore-centric shrinkage gas porosity models [34] used to interpret the result of defect volume fractions.

Table 2. The most common sand-casting defect size, shape and location

Defect type	Size	Shape	Location
Gas porosity	0.5–1 mm	Sphere	Near bolt hole
Shrinkage	1–3 mm	Irregular/sphere	Hub flange junction
Oxide inclusion (Bifilm)	1–5 mm	Crack-like notch	Inner bore

A scalar damage variable D was introduced to represent the effect of casting porosity. Following the density-based damage concept of Lemaitre and Dufailly [35], the damage variable can be related to the density ratio as $D = 1 - (\rho^*/\rho)^{2/3}$. Assuming that the reduction in density is

attributable to the void volume fraction f_v , such that $\rho^*/\rho = 1 - f_v$, Equation (1) is obtained:

$$D = 1 - (1 - f_v)^{2/3} \quad (1)$$

where f_v is the void volume fraction, ρ is the density of the undamaged material, and ρ^* is the effective density of the damaged material. To estimate the corresponding fatigue-life penalty associated with the bulk damage, an empirical life-scaling relation was adopted in this study:

$$N_{f,defective} = N_{f,defect-free} \times (1 - D)^k \quad (2)$$

where k is an empirical damage-sensitivity exponent. The resulting prediction was subsequently compared with the FEA fatigue-life results in Section 3.3.

$$K_t^{eff} = 1 + \alpha\sqrt{a/r} \quad (3)$$

where a is the equivalent defect size, r is the defect root radius, and α is a geometry-dependent factor. This framework is consistent with porosity-based fatigue life prediction approaches reported elsewhere for cast aluminum alloys [31] and following the pore-size-informed CDM approach of Wang et al. [8].

2.3. Finite element model and loading

Both defect-free and defective models were meshed with 3D tetrahedral elements in ANSYS Mechanical, with local refinement (down to 1 mm, confirmed by mesh convergence to within 1% variation) at the fillet, bolt holes, and defect locations. A single flange was modeled; the bolts, the mating flange and the bolt preload were not modeled. The shaft connection was represented by two remote points on the inner bore, with the X axis taken as the shaft axis. Remote point A, at the center of the bore on one side and coupled to the bore surface there, acts as the support: its translations are fixed, the rotation about the shaft axis is fixed so that the applied torque is reacted by the support (leaving it free would allow rigid-body rotation), and the remaining rotations are as listed in table 3. Remote point B, at the center of the opposite side of the bore and coupled to the bore surface there, carries the axial force F (along X) and the torque T (about X), distributed over the coupled surface. The load therefore passes from B through the hub and the hub–flange fillet to the support at A.

Supporting the model on the bore, rather than on the bolt holes, isolates the load path through the hub and fillet where the defects of interest act; a preliminary model with the bolt holes fixed produced unrealistic stress concentrations and was discarded. Consequently the load transfer through the flange plate and bolts is not represented, and stresses at the bolt holes and in the flange plate are not representative. As a consistency check, the maximum von Mises stress of the defect-free model is 1.32–1.38 times the nominal hub stress $\sigma_{nom} = \sqrt{\sigma_{ax}^2 + 3\tau^2}$, with $\sigma_{ax} = F/A$ and $\tau = Tr_o/J$ (hub outer diameter 80 mm, bore 40 mm), in all nine load cases. This nearly constant ratio is the fillet stress concentration of the model and shows that the torque is transmitted through the hub and fillet and reacted at the support.

Table 3. Boundary conditions of the single-flange model (X = shaft axis).

Item	Location	Constraint / load
Remote displacement (support)	Point A, bore center, one side	$U_X = U_Y = U_Z = 0$
Axial force	Point B, bore center, opposite side	F along X : 10, 20, 30 kN
Torque	Point B	T about X : 2, 4, 6 kN·m

The axial forces (10, 20 and 30 kN) and torques (2, 4 and 6 kN·m) were applied as the *amplitudes* of an in-phase (proportional), constant-amplitude, fully reversed cycle ($R = -1$, zero

mean stress), so each cycle alternates between $(+F, +T)$ and $(-F, -T)$ (figure 2). Fatigue life was obtained from the equivalent (von Mises) alternating stress σ_a using the S-N data of table 4, interpolated semi-logarithmically (stress linear in $\log N$). The last point (75 MPa at 10^7 cycles) is the endurance limit S_e , so life is capped at 10^7 cycles and S-N data from [30]. The Goodman option was selected in the ANSYS Fatigue Tool, but it has no effect here because the mean stress is zero, and Basquin's relation was not used. The safety factor is

$$\text{SF} = \frac{S_e}{\sigma_a}, \quad S_e = 75 \text{ MPa}, \quad (4)$$

so SF varies with stress even when life is at the run-out cap, and $\text{SF} < 1$ corresponds to finite life.

Table 4. S-N data used in the fatigue analysis (fully reversed).

Alternating stress (MPa)	Cycles to failure, N
300	10^3
220	10^4
140	10^5
95	10^6
75	10^7

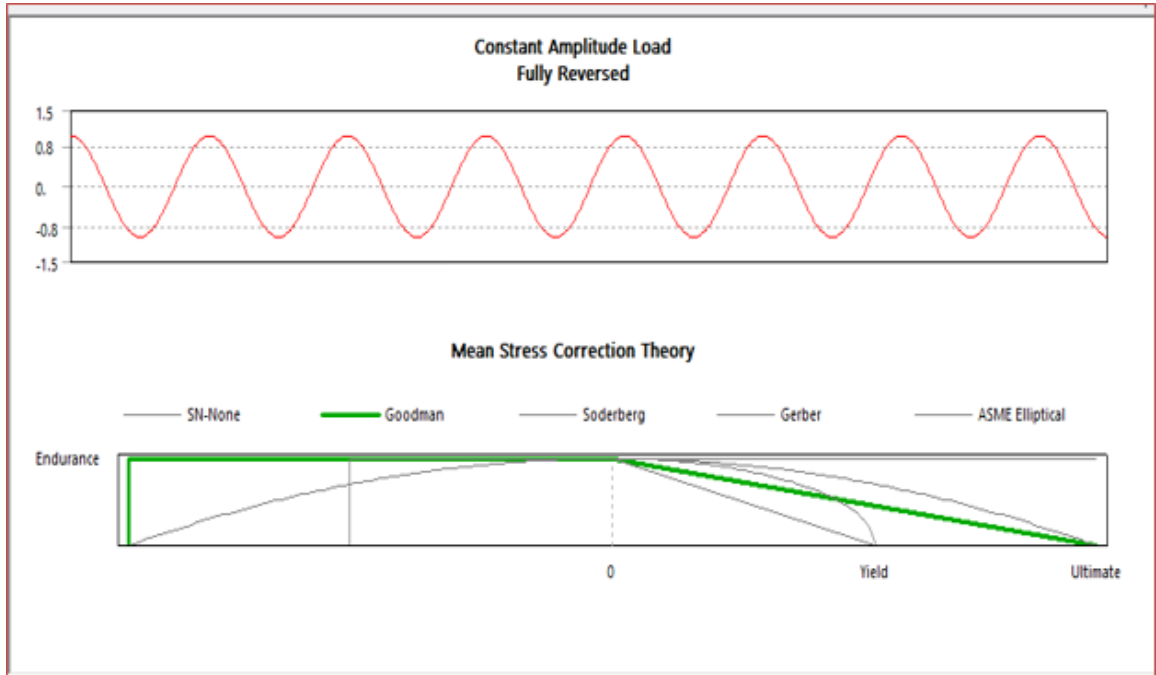


Figure 2. ANSYS Fatigue Tool settings: constant-amplitude, fully reversed load history (top, normalized) and mean-stress option (bottom, Goodman selected; no effect at zero mean stress).

It represents the margin against fatigue failure at the specified design life and is calculated from the alternating and mean stress components relative to the corresponding fatigue strength and ultimate tensile strength. Thus, the safety factor is a stress-based quantity and is not directly equal to the ratio of the predicted fatigue life to the design life. In the present analysis, a fatigue-life upper limit of 10^7 cycles was used. Consequently, load cases whose predicted lives

reached or exceeded this limit were reported as 10^7 cycles, although their safety factors remained different because their calculated stress states were different. Crack propagation at the critical hub–flange fillet was analyzed using the ANSYS SMART Crack Growth technique under mixed-mode loading [24, 25]. Crack growth was described using the Paris law, $da/dN = C(\Delta K_{\text{eff}})^m$, where C and m are the material crack-growth constants. The effective stress-intensity-factor range was calculated from the Mode I, II, and III components as

$$\Delta K_{\text{eff}} = \sqrt{(\Delta K_I)^2 + (\Delta K_{II})^2 + (\Delta K_{III})^2} \quad (5)$$

where ΔK_I , ΔK_{II} , and ΔK_{III} represent the stress-intensity-factor ranges for Modes I, II, and III, respectively. This formulation was adopted to account for the three-dimensional mixed-mode stress state generated by the combined axial and torsional loading. The initial crack was introduced at the critical hub–flange fillet, and crack propagation was evaluated as a function of crack length and loading cycles. The Paris-law constants used in the analysis were $C = 1.5 \times 10^{-10}$ and $m=3.7$. Thus, 5 was adopted specifically to account for the three-dimensional Mode I–II–III stress state generated by the combined axial and torsional loading, rather than because it is universally superior to the Tanaka formulation.

3. Results and discussion

3.1. Defect-free and defective simulation results

The defect-free and defective model included combined casting defects (porosity, shrinkage, and bifilms) modeled as 2 mm, 1.5 mm, 1 mm spherical voids at critical locations. The results are shown in Table 5, 6, and Figure 3 below under different loading conditions. The spherical geometry was used as a simplified numerical representation of the defect volume and local stress-concentration effect, rather than as an exact representation of the actual defect morphology.

Table 5. Defect-Free Simulation Results under different loading conditions

Case	Axial Force (kN)	Torque (kN·m)	Max. Mises (MPa)	von-Stress	Fatigue (cycles)	Life	Safety Factor (min)
1	10	2	49.41		$\geq 10^7$		1.5179
2	10	4	98.262		846,280		0.76327
3	10	6	147.22		81,245		0.50945
4	20	2	50.422		$\geq 10^7$		1.4875
5	20	4	98.821		822,410		0.75895
6	20	6	147.62		80,304		0.50806
7	30	2	52.005		$\geq 10^7$		1.4422
8	30	4	99.684		786,880		0.75238
9	30	6	145.51		85,340		0.51544

Table 6. Defective Simulation Results under different loading conditions

Case	Axial Force (kN)	Torque (kN·m)	Max. Mises (MPa)	von-Stress (cycles)	Fatigue Life (min)	Safety Factor
1	10	2	57.881	$\geq 10^7$	1.2958	
2	10	4	115.6	348,110	0.64866	
3	10	6	173.41	38,232	0.43251	
4	20	2	58.786	$\geq 10^7$	1.2884	
5	20	4	115.76	345,620	0.64788	
6	20	6	173.48	38,147	0.43232	
7	30	2	58.786	$\geq 10^7$	1.2758	
8	30	4	116.03	340,940	0.64639	
9	30	6	178.13	33,376	0.42015	

Defect-free and defective contour for high loading conditions (axial force = 30 kN and torque = 6 kN·m). The result shows von Mises stress distribution for the defect-free model and maximum stress is concentrated at the fillet region, indicating a critical location for fatigue crack initiation.

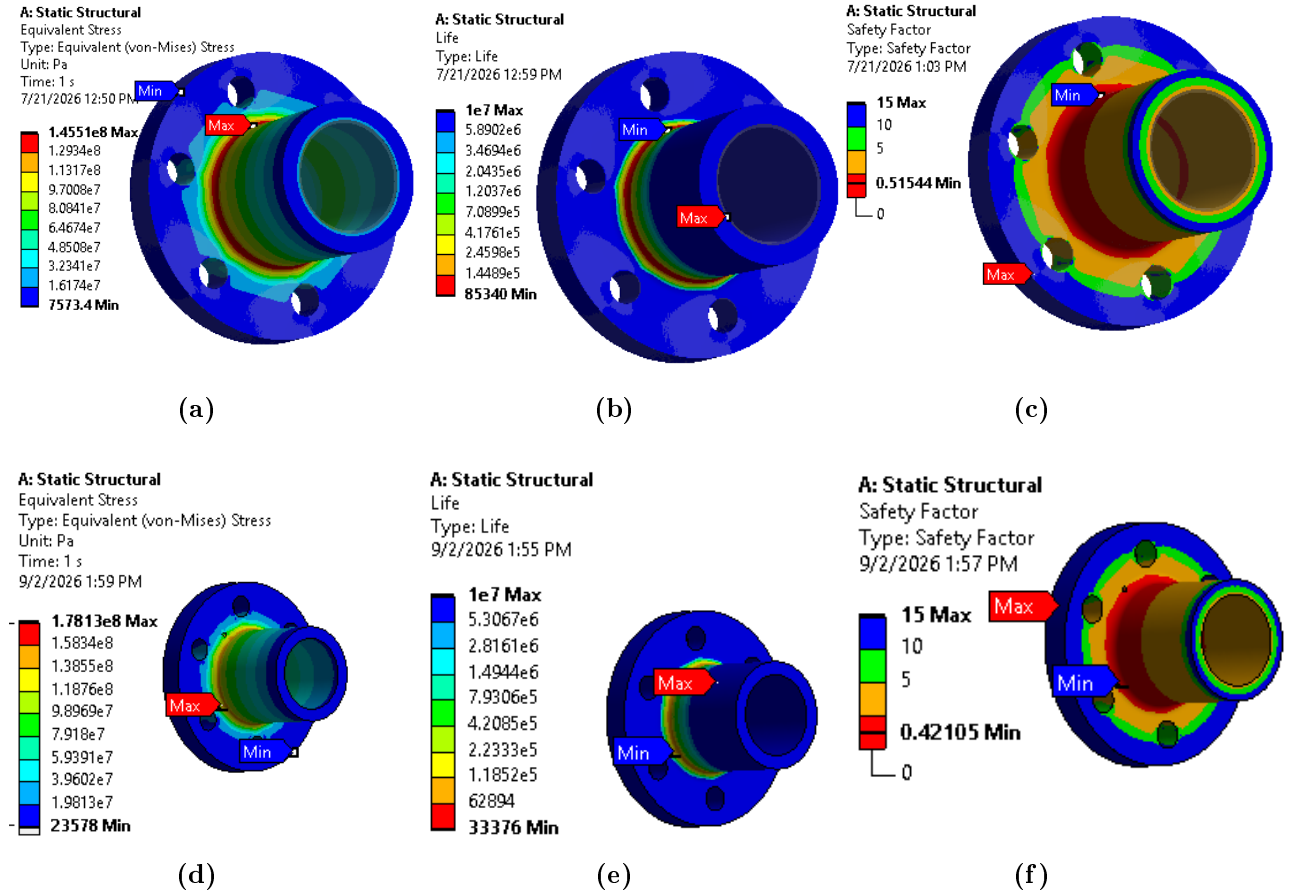


Figure 3. Finite element analysis results of the flange coupling: (a) defect-free equivalent von Mises stress, (b) defect-free fatigue life, (c) defect-free safety factor, (d) defective equivalent von Mises stress, (e) defective fatigue life, and (f) defective safety factor.

3.2. Effect of defects on stress and fatigue life

Casting defects increase local von-Mises stress by 13–22% and reduce minimum fatigue life by 50–61% (Table 7) across the loading range studied, but the penalty is not load-independent. At the lowest torque (2 kN·m) both models remain in the 10^7 -cycle runout regime and the stress increase does not translate into a measurable life penalty, consistent with the defect-tolerant regime of the Kitagawa-Takahashi framework used to benchmark A356-T6 fatigue behavior [32]. From 4 kN·m upward, however, the defective model’s life falls to roughly 39–48% of the defect-free value, with the deepest penalty (60.89%) occurring at the most severe combined case. This shows that defect sensitivity scales with loading severity rather than acting as a fixed percentage: as torque rises, the multiaxial stress state at the hub-flange fillet increasingly compounds with the defect-induced stress raiser rather than simply adding to it, a trend consistent with the multiaxial continuum-damage-mechanics findings reported for welded flange shafts by Xu *et al.* [9] and with the multiaxial fatigue behavior of A356-T6 characterized by Roy *et al.* [30].

This compounding effect also explains why defect location matters as much as defect size. Shrinkage porosity sits at the hub-flange fillet, the same location as the geometric stress concentration already present in the defect-free model, so its local K_t^{eff} Equation (3) acts multiplicatively on an already-elevated stress field – consistent with the position- and morphology-dependence documented by Rotella *et al.* [19] and the affected-depth sensitivity reported for defective A357-T6 by Majed *et al.* [28]. Gas porosity near the bolt holes and the bifilm at the inner bore sit in comparatively lower-stress regions and are expected to contribute less to the overall life reduction, consistent with the scatter and size-effect trends reported by El Khoukhi *et al.* [17]. This has a direct practical consequence: quality-control effort, where non-destructive inspection capacity is limited, should prioritize the hub-flange fillet over the inner bore or bolt-hole region.

Table 7. Comparison of defect-free and defective models (highest load case: 30 kN + 6 kN·m).

Parameter	Defect-Free	Defective	Change
Max. von-Mises stress (MPa)	145.51	178.13	+22.41%
Minimum fatigue life (cycles)	85,340	33,376	−60.89%
Minimum safety factor	0.51544	0.42015	−18.48%

Figure 4 through Figure 6 summarize the fatigue life, safety factor, and stress comparisons across all nine loading cases, confirming that the reduction in fatigue performance is consistent and substantial across the full range of axial force and torque combinations studied.

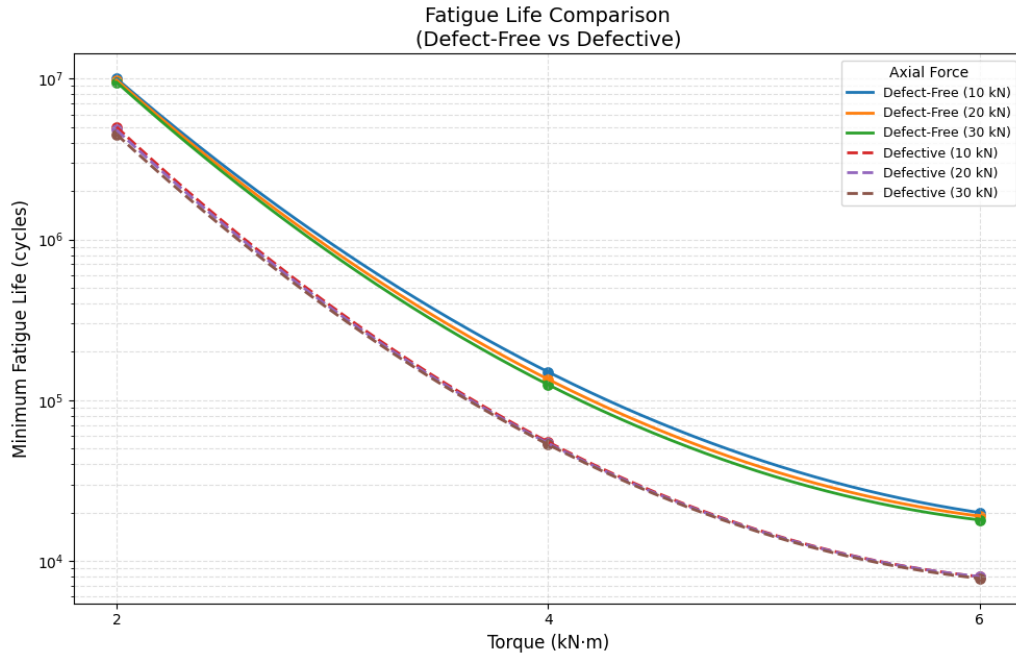


Figure 4. Fatigue life comparison between defect-free and defective flange couplings.

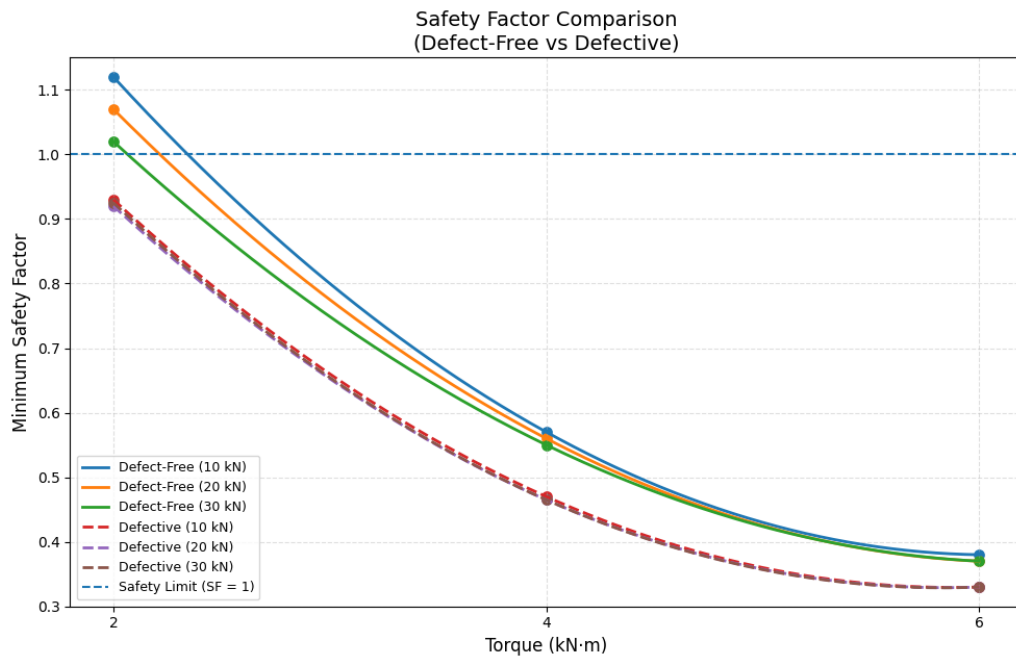


Figure 5. Safety factor comparison between defect-free and defective flange couplings.

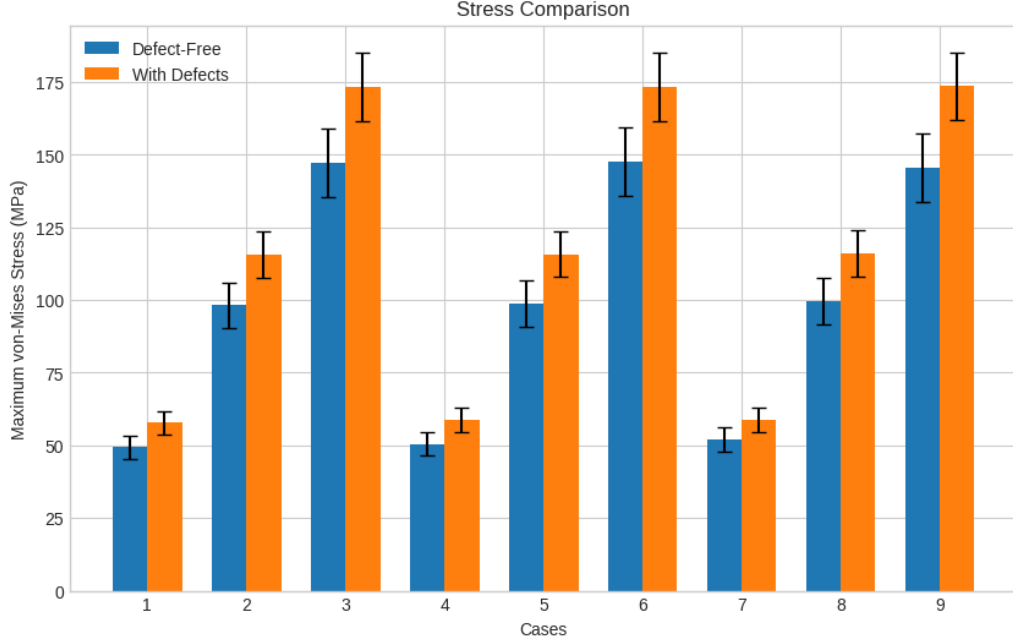


Figure 6. Maximum von-Mises stress comparison between defect-free and defective flange couplings.

3.3. Comparison of the damage model with the FE results

Two independent estimates of the life reduction were obtained, and they are not combined. (i) The bulk damage estimate applies the CDM relation (equation (1)) and the life relation (equation (2)) to a locally porous region with $f_v = 8\%$ ($D \approx 0.0541$). With the assumed exponent $k = 4-8$ it predicts a life reduction of 20–35.9% (35.9% for $k = 8$). (ii) In the FE model the defects are three discrete voids of 1–2 mm diameter, whose global volume fraction is of order 10^{-5} ($D \approx 7 \times 10^{-6}$), so the bulk term is negligible (below 0.01%) for this model. The 60.89% reduction obtained from the FE model therefore results entirely from the local stress increase at the voids: the maximum von Mises stress rises by 22.4% (145.51 to 178.13 MPa), which on the S–N curve of table 4 corresponds to 0.41 decades in $\log N$ (a factor of 2.6) and reduces the life from 85,340 to 33,376 cycles.

The two values describe different defect states and cannot be compared like for like. The exponent k was assumed rather than calibrated against test data, and reproducing the FE result with the bulk model would require $k \approx 17$, outside the assumed range. The comparison indicates that void volume fraction alone does not predict the life reduction: with the assumed exponents, a very small void volume located at a stress concentrator reduced the life by more than the bulk estimate for 8% porosity, and the bulk estimate is unconservative (longer life) relative to the FE result.

A direct FE evaluation of the stress concentration at the 2 mm shrinkage void gave $K_t = \sigma_{max}/\sigma_{nom} = 151.95/67.53 \approx 2.25$, where σ_{nom} is the stress at the same location in the defect-free model. Equation (3) with $\alpha = 2$ and $a = r$ gives 3.0, but this is the two-dimensional (circular-hole) value and overestimates a three-dimensional void; it remains only an order-of-magnitude estimate for sharp, elongated defects. The classical solution for an isolated spherical cavity in uniaxial tension, $K_t = (27 - 15\nu)/[2(7 - 5\nu)]$, gives 2.06 for $\nu = 0.33$ [37]. The FE value is about 9% higher, which is plausible because the void lies at the hub–flange junction, where the nominal stress varies steeply over the void size. Irregular shrinkage cavities and crack-like bifilms oriented across the load can give higher stress concentration than a sphere, so their local

severity may be underestimated here.

Practically, acceptance criteria based on porosity fraction alone are insufficient for fatigue-critical flange couplings, because the position of a void relative to geometric stress concentrators such as the hub–flange fillet matters as much as its volume. Thick-section junctions of this kind are typical hot spots for shrinkage porosity, so controlled cooling and fillet-region gating and feeding design are the relevant process measures [33, 34].

3.4. Crack propagation analysis

Crack propagation was analyzed using the XFEM [24], with an initial crack seeded at the hub–flange fillet is the critical location identified from the static analysis. Two complementary outputs were extracted from the simulation: individual Mode I, Mode II, and Mode III stress intensity factors as a function of crack length, and the resulting crack-length evolution with number of loading cycles for the defect-free and defective models.

It should be noted that the XFEM analysis requires a prescribed initial crack and therefore does not directly predict fatigue-crack initiation. In this study, the initial crack was introduced at the critical hub–flange fillet identified from the static analysis, while the shrinkage-porosity defect was located in the same critical region; the gas porosity and oxide bifilm were positioned near the bolt holes and inner bore, respectively. The defects and initial crack were modeled as separate features. Therefore, the XFEM results are interpreted as the subsequent crack-propagation response under defect-induced local stress conditions, rather than as a direct prediction of crack-initiation life.

The equivalent stress intensity factor formulation in Equation 5 was selected because the present flange coupling is subjected to combined axial and torsional loading, producing a three-dimensional mixed-mode crack-tip condition involving Modes I, II, and III. The classical Tanaka formulation is primarily expressed for mixed Mode I–II loading and therefore does not explicitly account for the Mode III contribution generated by torsion [36]. In the present study, the crack-growth simulation is three-dimensional and the calculated K_{III} contribution becomes significant as the crack propagates; therefore, an equivalent-SIF formulation incorporating all three modes was considered more appropriate for representing the crack-driving force. This choice is also consistent with previous mixed-mode fatigue-crack-growth approaches in which equivalent SIFs are used to combine the individual mode contributions into a single parameter for Paris-law-based crack-growth prediction.

Figure 7 shows the individual mode contributions together with the effective (equivalent) stress intensity factor (SIFS), computed as Equation 5 consistent with the equivalent-SIFS treatment reported for mixed-mode fatigue crack growth in aluminum alloys by Sajith *et al.* [29]. Mode I is dominant throughout crack growth, reflecting the axial-load-driven opening stress at the fillet, and ΔK_{eff} tracks K_I closely up to approximately 7 mm of crack extension. Beyond this point, Mode II and Mode III grow sharply from near-zero to roughly 1.5 MPa·m^{0.5} and 1.0 MPa·m^{0.5}, respectively, by the end of the simulated crack path and ΔK_{eff} begins to diverge visibly above K_I alone. This late-stage mode-mixity growth is consistent with the multiaxial fractographic evidence reported for A356-T6 under combined loading by Roy *et al.* [30]: as the crack extends beyond the immediate influence of the initiating defect, it increasingly samples the torsion-induced shear stress acting across the fillet, so the shear (Mode II/III) contribution to the crack-driving force grows disproportionately relative to Mode I. A single-mode (Mode-I-only) Paris-law analysis would therefore underestimate the true crack driving force once the crack has advanced past its initiation stage, confirming that the equivalent-SIFS formulation adopted here is necessary rather than a mathematical convenience.

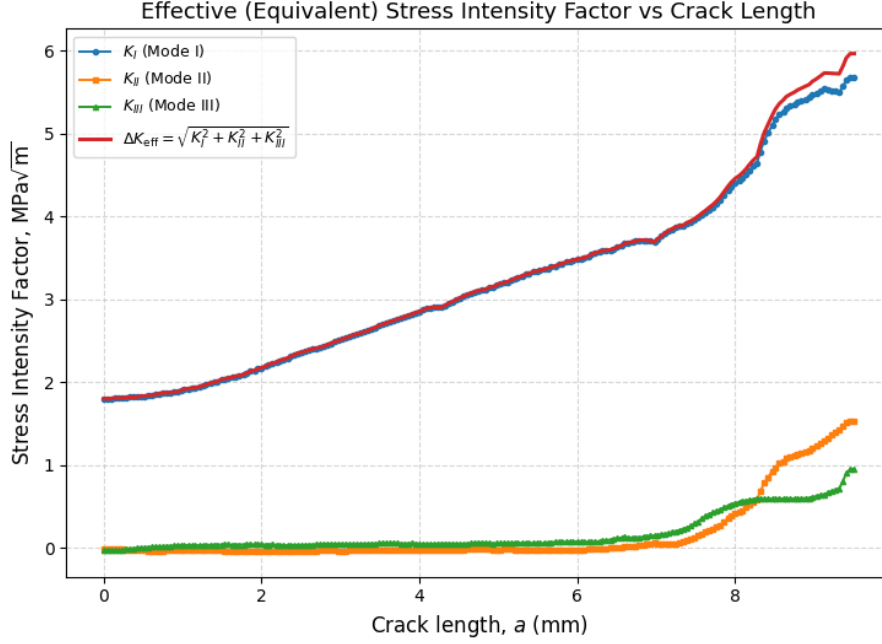


Figure 7. Individual mode stress intensity factors (K_I , K_{II} , K_{III}) and the resulting effective stress intensity factor ΔK_{eff} (Equation (5)) as a function of crack length.

3.5. Limitations

The results should be interpreted within the following limitations. (i) *Defect shapes*: all defects were idealized as spherical voids, although shrinkage cavities are irregular and bifilms are crack-like; a sphere gives a lower stress concentration ($K_t \approx 2$) than sharp or elongated defects, so the local severity of those defects may be underestimated. (ii) *Defect sizes and locations*: the sizes (1, 1.5 and 2 mm) and positions were taken from literature-informed ranges and were not measured on cast flange couplings, and one void per location was modeled, whereas real castings contain statistically distributed defect populations. (iii) *Experimental validation*: no fatigue tests or defect measurements (e.g. CT) were carried out. The model is numerically verified (mesh convergence) and benchmarked against published data, but it is not experimentally validated, so the absolute lives should not be used for design without testing. (iv) *Multiaxial fatigue assumptions*: axial force and torque were assumed to act in phase; the loading was idealized as fully reversed with zero mean stress, which is more severe than pulsating service loading, so the lives are conservative in that respect; the von Mises equivalent stress was combined with a uniaxial S–N curve without a multiaxial or critical-plane criterion; and residual stresses, surface finish, bolt preload and contact were neglected (single flange, bolts not modeled). The damage exponent k was assumed and not calibrated. (v) *Crack growth*: only the stress intensity factors of a single seeded crack were computed. No crack-growth life is reported, because Paris-law constants are uncertain for sand-cast A356-T6 and were not calibrated, so any growth-life estimate would carry substantial uncertainty.

4. Conclusion

This study indicates that realistic sand-casting defects such as gas porosity, shrinkage porosity, and oxide bifilms; considerably reduce both the static strength margin and fatigue service life of A356-T6 aluminum alloy flange couplings under combined axial-torsional cyclic loading. Defects increased maximum stress by 13–22% and reduced minimum fatigue life by 52–61%. The results indicate that void volume fraction alone is not sufficient to characterize the fatigue effect of

casting defects, because defect geometry and location can substantially influence local stress concentration and fatigue life. FEA, and CDM framework presented here provide a numerical approach for assessing defect-aware fatigue performance of sand-cast aluminum flange couplings, particularly in situations where experimental characterization is limited. The study is numerical; its limitations, including idealized spherical defects of assumed size, the absence of experimental validation and the simplifications of the multiaxial fatigue analysis, are listed in Section 3.5, and experimental validation with controlled artificial defects is recommended.

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