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Research Article

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Effects of energy density on the mechanical properties, residual stress and thermal-fatigue of Fe-Cr alloy fabricated by laser directed energy deposition

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**Abstract:** Aiming at the problem that AISI4340, a common material for fully mechanized coal-mining equipment, is prone to wear failure in harsh working environment. To repair damaged area and improve service performance, the high-strength Fe-Cr alloy coatings having different laser energy densities were fabricated on the AISI4340 by laser directed energy deposition. The effects of the energy densities on the tensile properties, hardness, residual stress, wear and thermal-fatigue damage were systematically studied. The models of thermal-fatigue damage and service life were established and improved, and the prediction accuracy were verified. The results indicated that with the increasing energy density, the tensile strength and Rockwell hardness increased first and then decreased, and the residual stress on the coating surface aggrandized with increasing temperature gradient. When the energy density was 35.01 J/mm$^2$, the wear depth and wear rate were 51.8 μm and 1.91×10$^{-2}$ mm$^3$·N$^{-1}$·mm$^{-1}$, and the wear resistance was increased by two times compared with the substrate. Considered the effective crack propagation and loading order, the accuracy of the service life models were improved from 65.9% and 23.1% to 14.6% and 6.7%, respectively. Selecting appropriate energy density is beneficial to improve the mechanical properties and decrease the thermal-fatigue damage of Fe-Cr alloy coatings.

**Keywords:** Laser directed energy deposition; Laser energy density; Mechanical properties; Residual stress; Thermal-fatigue.
1 Introduction

AISI 4340 is a high-performance hardened and tempered steel which has been widely used in the main components of fully mechanized coal mining equipment due to its good hardness and impact toughness, such as shearer drum and roadheader cutting head [1-3]. The main components are subjected to the impact of coal-rock and alternating load via the extremely harsh working environment, which are prone to wear failure with a high failure frequency, and reduce the working efficiency, reliability and stability of the fully mechanized mining equipment [4, 5]. The frequent maintenance and replacement of equipment seriously limits the production efficiency and sustainable development of coal production enterprises. Therefore, the research on the high efficiency and high performance remanufacturing repair of damaged components has significant engineering value for prolonging the service cycle of fully mechanized mining equipment and realizing energy conservation and reducing emission.

It is known that the damaged parts repaired by laser directed energy deposition (DED) technology has been widely used in industry applications, such as railway equipment [6–8], aerospace blade [9–11] and gears [12]. As an emerging repair technology, LED technology has low environmental pollution, wide material applicability, high flexibility and automaticity and can realize metallurgical combinations of coatings and substrates [13, 14]. Therefore, it provides the possibility of high quality and high performance repair of key parts of fully mechanized mining equipment. With the further exploration of high-energy laser technology, many published works have reported on the research results of using laser additive technology to prepare high-performance coatings and repair layers for equipment components. Jing et al [15] produced 300M steel specimens by high power laser melting and investigated their defects, microstructure, tensile properties and impact toughness. It is found that the parts is much
superior to that of weldments and forgings. Pellizzari et al [16] deposited the H13 steel claddings on precipitation hardening copper-beryllium alloy substrate using direct laser deposition. The claddings showed a better load-bearing capacity, surface hardness and wear resistance. Liu et al [17] adopted laser cladding technology to deposit Co-based alloy powders to improve the wear resistance of train brake discs. The results showed that Co06 coating exhibited better wear resistance and oxidation resistance at high temperature than the substrate. Ding et al [18] prepared the Fe-based alloy claddings with different WS$_2$ powder content on the railway CL60 wheel material. The microstructure, wear and damage behaviors of laser claddings were explored and the optimum content of WS$_2$ powder was determined to be 6%.

It has been proved that the coating mechanical properties of DED technology depend on various process parameters, such as laser power, scanning speed, powder feeding rate, lapping ratio and others [19-21]. To avoid the complexity of multiple parameters, the laser energy densities have been introduced and their effects on the mechanical properties of claddings have been studied [22-24]. Cao et al [25] and Wang et al [26] investigated the effects of laser energy density on the microstructure, defects and mechanical properties of 24CrNiMo high-strength low-alloy steel fabricated by laser melting, and found that the appropriate laser energy density could significantly improve the coating properties. Luo et al [27] found that energy density has significant effect on the microstructure, phase and wear resistance properties of Fe$_3$Al coatings. The optimized energy density was 0.3 J/mm, which could form the coating with no defects and exhibit excellent dry sliding wear resistance. Yin et al [28] clarified the relationship between laser energy density and macrostructure, microstructure, microhardness and wear resistance of modified aluminum bronze coating, and found that the reduction of laser energy density effectively improved the formability of the coating surface. Luo et al [29] studied the effects of
energy density on the geometric characteristics, microstructure and corrosion resistance of FeCrNiMnAl coatings and believed that the modulation of laser energy density was an effective method to optimize coating performance.

The above results indicate that appropriate laser energy density is beneficial to obtain better mechanical properties of the coating, but there is still a lack of a comprehensive understanding, especially residual stress and fatigue thermal damage. In the previous work, we preliminarily explored the mechanism of different laser energy densities on the microstructure and defects of Fe-Cr alloy coatings [30], but the previous experiments did not focus on the effects on the mechanical properties. Therefore, it is crucial to establish an effective framework to systematically reveal the comprehensive understanding of laser energy density on the mechanical properties of Fe-Cr alloy coatings.

In this paper, experimental studies on the mechanical properties of Fe-Cr alloy coatings fabricated by DED technology with different laser energy densities were carried out. The evolution of tensile properties, hardness, residual stress, wear resistance and thermal-fatigue damage of the coatings were analyzed. The nonlinear continuous thermal-fatigue damage model and life prediction model of Fe-Cr coatings were established and improved, and the prediction accuracy of the models were verified.

2. Experimental

2.1. Materials and experimental process

In this study, a forged AISI 4340 steel plate with a size of 40 mm×30 mm×20 mm was adopted as the substrate for the experiments. The deposited material was the high-strength and wear-resistant Fe-Cr alloy powder fabricated by the atomization method with particle size of 45–150 μm. Prior to the preparation of the coatings, the AISI 4340 steel surface was polished
with a grinding machine and cleaned with alcohol and acetone. The substrates and powder were placed in a vacuum dryer for 30 min at 50 °C to remove moisture. The chemical compositions of the AISI 4340 and Fe-Cr alloy are shown in Tab. 1.

The DED experimental system and processing characteristics are shown in Fig. 1(a), which consisted of a Laserline LDF-3000 diode laser, a KUKA KR30-3 HA industrial robot, a Laserline OTS-2 coaxial powder feeding nozzle and a Raycham RC-PGF-D-2 powder feeding system. Argon of 99.99% purity was used as the carrier gas and the shielding gas to prevent the oxidation of the molten pool, and the flow rates were 5 L/min and 10 L/min, respectively. Aiming to study the effect of laser energy density on the mechanical properties of Fe-Cr alloy coatings, the different laser power of 900 W, 1100 W, 1300 W, 1500W and scanning speed of 10 mm/ s, 12 mm/s, 14 mm/s were employed. Table 2 exhibits the laser energy density calculated adopting the following formula [28, 30]:

$$E = \left[ \frac{P}{\pi r^2} \right] \left( \frac{2r}{V} \right)$$

where $E$ is the laser energy density (J/mm$^2$); $P$ is the laser power (W); $r$ is the laser spot radius (mm), $r$=2 mm and $V$ is the scanning speed (mm/s). Each of the coating is composed of 12 cladding tracks for a total of 10 layers, with a lapping ratio of 45%, and the scanning strategy involves a side-by-side anisotropic path.

| Table 1 Chemical composition of the Fe-Cr alloy and AISI 4340 (wt. %) |
|-------------------------|--------|--------|-------|------|------|--------|------|------|--------|
| Element                 | Cr     | Si     | B     | Cu   | Mo   | Ni     | C    | P    | S      | Mn   | Fe     |
| Fe-Cr alloy             | 16.79  | 4.0    | 3.5   | 3.0  | 3.0  | 2.14   | 0.23 | 0.07 | 0.005  | _    | Bal.   |
| AISI 4340               | 1.5    | 0.30   | _     | _    | 0.2  | 1.55   | 0.34 | _    | _      | 0.5  | Bal.   |
Table 2 The experimental scheme of DED Fe-Cr alloy

<table>
<thead>
<tr>
<th>Group</th>
<th>Laser power $P$/W</th>
<th>Scanning speed $V$/mm·S$^{-1}$</th>
<th>Laser spot radius $r$/mm</th>
<th>Laser energy density $E$/J·mm$^{-2}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>900</td>
<td>14</td>
<td>2</td>
<td>20.46</td>
</tr>
<tr>
<td>2</td>
<td>1100</td>
<td>14</td>
<td>2</td>
<td>25.01</td>
</tr>
<tr>
<td>3</td>
<td>1300</td>
<td>14</td>
<td>2</td>
<td>29.56</td>
</tr>
<tr>
<td>4</td>
<td>1100</td>
<td>10</td>
<td>2</td>
<td>35.01</td>
</tr>
<tr>
<td>5</td>
<td>1500</td>
<td>12</td>
<td>2</td>
<td>39.79</td>
</tr>
</tbody>
</table>

Fig. 1. The DED system and experimental process

2.2 Mechanical properties tests

The tensile test and fatigue test were performed by Shimadzu AG-100 kN universal testing machine with a tensile rate of 1 mm/min, as shown in Fig 1(b). The energy density of each group was repeated for three times and the average value was taken, and the surface of the specimens were polished with sandpaper to reduce the error. The Akashi MVK-H11 tester
(loading 500 g, time 10 s, point spacing 100 μm) and the Dechuan HR-150 tester (loading 60 kg, time 10 s) were adopted to measured Vickers microhardness and Rockwell hardness, respectively. The tribological behavior was carried out by Weipin YP-TBT friction machine. The friction time was 1800 s and the friction mode was demonstrate in Fig 1(c).

3. Result and discussion

3.1 Tensile properties

The tensile properties of alloy metal materials formed by laser cladding are affected by densification and microstructure. The fracture location of the specimen is always located in the defect region of pores and cracks and is related to the number of porosity [26]. Previous studies have found that the porosity of multilayer Fe-Cr alloy coatings increases with the increasing laser energy density [30]. Fig. 2 reveals the effects of laser energy densities on tensile strength, yield strength and elongation of coatings. When the laser energy density is 20.46 J/mm², the tensile strength and elongation of coating are 1090±52 MPa and 13.2±2.9%, respectively. This is due to the insufficient laser energy leading to incomplete melting of powder and weakened bonding ability between cladding tracks. In addition, the lack of molten pool existence time results in the inadequately release of bubbles, which increases the porosity and decreases the densification, and seriously affects the tensile strength of the specimens. With the increase of laser energy density, the tensile strength increases first and then decreases, and the elongation increases gradually. The tensile properties of Fe-Cr alloy coating reaches the optimal state as $E=35.01$ J/mm², with the tensile strength is 1248±36 MPa, the yield strength is 844±26 MPa, and the elongation is 18.1±2.9%.
The molten pool fluctuation, particle splashing and porosity are intensified while the laser energy density reaches 39.97 J/mm$^2$, meanwhile, the coarsening of grain size also affects the tensile properties of coatings. The grain size and yield strength are expressed by the Hall-Petch formula [31]:

$$\sigma_p = \sigma_0 + K_y D_g^{1/2}$$

(2)
where, $\sigma_0$ is the frictional stress, which is determined by the crystal microstructure and the dislocation density, and $K_y$ is the Hall–Petch slope. After calculation, the $\sigma_0$ is 683.63 MPa, the $K_y$ is 294.09 MPa·μm$^{1/2}$, and the correlation coefficient $R^2$ is 0.8057 indicates that the relationship between yield strength and grain size of Fe-Cr alloy coatings with different laser energy densities follows the Hall–Petch formula, as shown in Fig.3. Therefore, it is considered that the grain size is one of the main factors affecting the tensile properties and the appropriate laser energy density is beneficial to improve the tensile strength and toughness of Fe-Cr alloy coatings fabricated by DED technology.
3.2 Hardness

The Vickers microhardness and the Rockwell hardness are adopted to analyze the microhardness evolution rules and macrohardness ability of the Fe-Cr alloy coatings with different laser energy densities, respectively. The evolution trends of microhardness from coatings to substrates are stepped as shown in Fig.4. Considered that Mo and Ni can enhance the solution strengthening among elements, meanwhile, the strengthening phase (Cr$_{23}$C$_6$, Fe$_2$B and Cr$_2$B) [30] and the amorphous forming ability of B and Si elements are the main reason for the significant hardness improvement of Fe-Cr alloy coatings. Due to the diffusion of eutectic compounds, the microhardness from the fusion zone to the substrate decreases gradually. The thickness of fusion zone is about 400 μm and thickens with the increasing laser energy density. This is because the higher laser energy increases the melting depth of the matrix, resulting in an increase in the number of indentation measurement points in the fusion zone, accompanied by larger error fluctuations. The microhardness of heat affect zone is lower than that of fusion zone but slightly higher than substrate via the quenching treatment under the high temperature of laser processing.
Fig. 5 reveals the hardness variations of Fe-Cr alloy coatings under different laser energy densities. The average Vickers hardness of the coating are 615.4 HV$_{0.5}$, 614.8 HV$_{0.5}$, 612.8 HV$_{0.5}$, 594.3 HV$_{0.5}$ and 573.1 HV$_{0.5}$, which are 1.92, 1.91, 1.91, 1.86 and 1.79 times higher than that of the substrate, respectively. The coating microstructure tends to coarser and the effect of refined crystalline strengthening decreases via the increasing laser energy density. Meanwhile, larger energy input weakens the diffusion strengthening effect of hard interphase [32], which leads to the gradual decrease of Vickers hardness. With the increasing laser energy density, the Rockwell hardness increases first and then decreases, which is consistent with the trend of tensile properties. Inadequate melting powder and poor densification of coating due to low laser energy density are the main reasons for the worse macroscopic hardness. The Vickers microhardness and Rockwell hardness reach the maximum as $E=20.46$ J/mm$^2$ and $E=29.56$ J/mm$^2$, which are 615.4±19.8 HV$_{0.5}$ and 55.3±2.1 HRC, respectively. Therefore, the appropriate laser energy density plays a key role in regulating the hardness of coatings.

**Fig. 4.** Evolution rules of Vickers hardness with different laser energy densities
3.3 Residual stress

To describe the residual stress in the X direction of Fe-Cr alloy coatings with different laser energy densities, the Vickers hardness indentation method is adopted to quantitatively characterize the residual stress in this study. It is found that the state and magnitude of residual stress can be determined and calculated by the shape and size of Vickers hardness indentation [33, 34]. The indentation morphology is consistent with that of diamond indenter when the indentation area does not exhibit residual stress, which is a standard rhombus, as shown in Fig. 6(a). When the indentation area is expressed as tensile stress and pressure stress, the indentation boundary depresses inward and compresses outward, as depicted in Fig. 6(b) and 6(c), respectively. In Fig. 6, \( A_{nom} \) is the standard indentation rhombic area, \( A_{rea} \) is the actual indentation area, \( L_1 \) and \( L_2 \) are the diagonal lengths of the longitudinal and transverse axes of the hardness indentation, respectively.

Assuming that, the residual stress of Fe-Cr alloy coatings are bidirectional equal-biaxial state, and the uniaxial stress-strain curve satisfies the power function relation:

\[
\sigma = K \varepsilon^p \tag{3}
\]
where, $\sigma$ is the stress; $\varepsilon_p$ is plastic strain; $K$ and $n$ are constant, which can be obtained by fitting the uniaxial stress-strain curves with different laser energy densities, as fitted in Fig. 7.

\[ H_V = C (\varepsilon_{repr} + \varepsilon_{res}) \]  

(4)

\[ c^2 = c_0^2 - 0.32 \ln \left( 1 + \frac{\sigma_{res}}{\sigma_{(\varepsilon_{repr} + \varepsilon_{res})}} \right) \]  

(5)

where, $H_V$ is the Vickers microhardness of the testing point; $C$ is a constant, which only related to the diamond indenter geometry, usually $C=3$; $\sigma_{(\varepsilon_{repr} + \varepsilon_{res})}$ is the yield stress corresponding to effective plastic strain ($\varepsilon_{repr} + \varepsilon_{res}$), $\varepsilon_{repr}$ is the representative value of ($\varepsilon_{repr} + \varepsilon_{res}$), which is 0.08 while using Vickers indenter, $\varepsilon_{res}$ is the von Mises effective residual plastic strain under equal-biaxial stress state; $c_0^2$ is the Vickers indenter constant, $c_0^2=1$; $c^2$ is the ratio of the actual indentation area to the standard indentation rhombic area, $c^2=A_{real}/A_{nom}$. The actual indentation area can be obtained from the polygon fitting measurement module of the ultra-depth optical microscope. The $A_{nom}$ can be calculated by Eq. (6), and the $\varepsilon_{res}$ and $\sigma_{res}$ can be deduced as shown in Eq. (7) and (8) in combination with Eqs. (3)–(5), respectively.
\[ A_{\text{hom}} = \left[ \frac{(L_4 + L_2)}{2} \right]^2 \]  

(6)

\[ \varepsilon_{\text{res}} = \left( \frac{H_V}{3K} \right)^{\frac{1}{n}} - 0.08 \]  

(7)

\[ \sigma_{\text{res}} = K \varepsilon_{\text{res}}^n \left[ \exp \left( \frac{c^2 - 1}{0.32} \right) - 1 \right] \]  

(8)

Fig. 7. Tensile stress-strain and fitting curves of coating with different laser energy densities, (a) \( E = 20.46 \text{ J/mm}^2 \), (b) \( E = 25.01 \text{ J/mm}^2 \), (c) \( E = 29.56 \text{ J/mm}^2 \), (d) \( E = 35.01 \text{ J/mm}^2 \) and (e) \( E = 39.79 \text{ J/mm}^2 \).

Fig. 8 shows the variations of residual stress at the indentation point with different laser energy densities. The residual stress at different laser energy densities fluctuates, but the overall variation trends are consistent. When the laser energy density is 20.46 J/mm\(^2\), 25.01 J/mm\(^2\), 29.56 J/mm\(^2\), 35.01 J/mm\(^2\) and 39.79 J/mm\(^2\), the residual stress ranges of the upper surface of coatings are -101.5~156.3 MPa, -139.8~163.4 MPa, -128.9~188.7 MPa, -154.1~226.0 MPa and -152.8~233.6 MPa, respectively. With the increasing laser energy input, the temperature gradient and thermal stress increase, resulting in an increasing trend of residual stress range. For the upper surface of the coatings, the residual stress at the turning point is pressure stress, while the middle zone is tensile stress. The temperature at the turning point is higher than that.
in the middle zone of coating via the track-to-track laser scanning, and the temperature gradient in the middle zone is smaller and the cooling rate is faster, so the residual stress is expressed as tensile stress. At the same time, the residual stress at turning point of coating is compressive stress due to the restriction of substrate and middle zone. Excessive residual stress will seriously affect the mechanical properties of the coatings [34]. Therefore, the reasonable optimization of process parameters can be adopted to restrain the generation of residual stress.

**Fig. 8.** Variations of residual stress at indentation points with different laser energy densities

### 3.4 Friction characteristics

The changes in friction coefficient of substrate and coatings at different times are shown in Fig. 9. The friction coefficient of the substrate increases rapidly and then enters the stable wear stage, with an average of 0.83, while the friction coefficient of the coatings increases slowly until the end of the tests without a stable wear stage. With the laser energy density increasing from 20.46 J/mm² to 39.79 J/mm², the friction coefficient of the coatings are 0.74, 0.73, 0.59, 0.60 and 0.69, respectively, which are consistent with the change trend of the macrohardness with the laser energy density. The reason for this phenomenon considered that,
the adhesion of the cladding material decreases gradually with the increasing hardness, and the plastic deformation and material slip are weakened during the friction process, which reduces the friction resistance between the coatings and the grinding ball, resulting in the decreasing friction coefficient.

![Friction coefficient of substrate and coatings at different laser energy densities](image)

**Fig.9.** Friction coefficient of substrate and coatings at different laser energy densities

Fig. 10(a) illustrates the morphologic profile of the wear section. Under the self-lubricating wear condition of grinding ball and disk, the wear section profiles present a circular arc shape, and the wear profile of the substrate is deeper and wider than that of the coatings. Friction characteristics of substrate and Fe-Cr alloy coatings are displayed in Fig. 10(b) and Table 3, respectively. The maximum wear depth, wear volume and wear rate of the substrate are 105.1 μm, 0.80 mm³ and 4.02×10⁻² mm³·N⁻¹·mm⁻¹, respectively. When the laser energy density is 35.01 J/mm², the wear rate of coating is 1.91×10⁻² mm³·N⁻¹·mm⁻¹, which decreases by 52.0% compared with the substrate, and the wear resistance is about twice that of the substrate.
Fig.10. Friction characteristics of substrate and coatings (a) wear width and wear depth and (b) wear rates and friction coefficient of each coating.

Table 3 Friction characteristics of substrate and Fe-Cr alloy coatings

<table>
<thead>
<tr>
<th>Friction characteristics</th>
<th>Substrate</th>
<th>Laser energy density J/mm²</th>
</tr>
</thead>
<tbody>
<tr>
<td>Friction coefficient</td>
<td>0.83</td>
<td>0.74 0.73 0.59 0.60 0.69</td>
</tr>
<tr>
<td>Maximum wear depth (μm)</td>
<td>105.1</td>
<td>82.1 80.9 52.3 51.8 65.6</td>
</tr>
<tr>
<td>Wear volume (10⁻¹-mm³)</td>
<td>0.80</td>
<td>0.59 0.55 0.39 0.38 0.46</td>
</tr>
<tr>
<td>Wear rate (10⁻²-mm³N⁻¹-mm⁻¹)</td>
<td>4.02</td>
<td>2.96 2.75 1.98 1.91 2.30</td>
</tr>
</tbody>
</table>

To analyze the wear mechanism of the substrate and coatings, the wear morphology characteristics of specimens are presented in Fig. 11. The substrate material is sheared and transferred by the grinding ball as its hardness is far lower than that of the grinding material. In Fig. 11(a), a large number of exfoliation (white area), attachment and fine furrows can be seen on the testing area, therefore, the wear mechanism of substrate materials are adhesive wear and abrasive wear. In Fig 11(b)-(f), the hard asperities embedded in the coatings and produce furrows with the reciprocating movement of grinding ball, meanwhile, the abrasive debris and oxide particles are observed scattered on the surface of the specimens. Hence, the wear mechanism of Fe-Cr alloy coatings exhibits typical abrasive wear. When the laser energy...
densities are 20.46 J/mm², 25.01 J/mm² and 39.79 J/mm², the furrows are deeper and wider, and the more significant plastic deformation and furrows are produced. The white areas in Fig. 11(d) and 11(e) are remaining pits by the exfoliation of coating, thus causing the discontinuity of furrow wear morphology. The lamellar exfoliation of coating is attributed to the peeling of bonding phase caused by shear stress on grinding material under the fatigue action of reciprocating cyclic tangential load and the hard asperities cutting. Therefore, the wear mechanism of Fe-Cr alloy coatings also manifests adhesive wear and fatigue wear.

![Image](image_url)

**Fig.11.** Wear morphology of substrate (a) and coatings (b) \(E=20.46\) J/mm², (c) \(E=25.01\) J/mm², (d) \(E=29.56\) J/mm², (e) \(E=35.01\) J/mm² and (f) \(E=39.79\) J/mm².

### 3.5 Thermal-fatigue

To further evaluate the thermo-fatigue coupling damage of Fe-Cr alloy coatings, considering the thermal damage caused by laser energy, the nonlinear continuous fatigue cumulative damage model proposed by Chaboche et al. [35] is introduced, as shown in Eq. (9):

\[
dD_c = \left[ F(\sigma_{\text{max}}, \sigma_m, D) \right] dN
\]

(9)

where, \(D_c\) is the specimen damage; \(\sigma_{\text{max}}\) is the maximum stress of fatigue testing; \(\sigma_m\) is the
average stress of fatigue testing; \( N \) is the number of fatigue loads. For the uniaxial fatigue performance of laser additive manufacturing specimens, Chaboche et al. [35] suggested that Eq. (10) be adopted to describe the relationship between the number of fatigue loads and the specimen damage.

\[
dD_C = \left[1 - (1 - D_C)^{\beta+1}\right]^{\alpha_C} \left[\frac{\sigma_a}{M_0(1-b\sigma_m)(1-D_C)}\right]^\beta dN
\]  

(10)

where, \( \sigma_a \) is the stress amplitude of fatigue testing; \( \beta, M_0 \) and \( b \) are the performance parameters of cladding materials; \( \alpha_C \) is a calculation parameter related to fatigue damage and load. Dattoma et al. [36] suggested that the \( \alpha_C \) can be expressed as:

\[
\alpha_C = 1 - \frac{1}{H_a} \left(\frac{\sigma_{\text{max}} - \sigma_R}{\sigma_\alpha - \sigma_{\text{max}}}\right)^f
\]  

(11)

where, if \( x>0, \langle x \rangle = x; \) if \( x\leq 0, \langle x \rangle = 0; \) \( \sigma_R \) is the fatigue limit of the specimen under the condition of the corresponding stress ratio, in this paper, the stress ratio of symmetric loading is \( R_1=-1; \) \( H_a \) and \( f \) are experimental constants, for metal materials, \( H_a=0.0801 \) and \( f=0.434. \)

When the load is large enough, the crack can overcome the obstruction of the plastic zone and then expand, and the crack is still closed while the load is insufficient. Therefore, the effective range of stress intensity factor of the coating material [37] can be represented as:

\[
K_{\Delta \text{eff}} f = K_{\text{max}} - K_{\text{op}}
\]  

(12)

where, \( K_{\Delta \text{eff}}, K_{\text{max}} \) and \( K_{\text{op}} \) are the effective range, maximum and opening stress intensity factor of coating, respectively. \( K_{\text{op}} \) is mainly determined by the flexibility of coating, the crack will propagate while \( K_{\Delta \text{eff}} > K_{\text{op}}, \) hence,

\[
\xi = \frac{K_{\Delta \text{eff}}}{K_\Delta} = \frac{K_{\text{max}} - K_{\text{op}}}{K_{\text{max}} - K_{\text{min}}} = \frac{1}{1-R_1} \left(1 - \frac{K_{\text{op}}}{K_{\text{max}}}\right)
\]
where, $\xi$ is the effective factor of crack propagation; $K_\Delta$ is the range of stress intensity factor.

The carbon content of Fe-Cr alloy is 0.23%, so the effective factor of crack propagation of carbon steel is chosen as:

$$\xi = 0.75 + 0.3R_{-1} + 0.15R^2_{-1}$$  \hspace{1cm} (14)

By introducing the effective factor of crack propagation and considering the crack closure effect, Eq. (9) is modified and expressed as Eq. (15):

$$dD_{\xi C} = \left[ F \left( \sigma_{\max}, \sigma_m, \xi D \right) \right] dN$$ \hspace{1cm} (15)

where, $D_{\xi C}$ indicates the specimen damage considering the crack closure effect. Therefore, Eq. (10) can be modified as:

$$dD_{\xi C} = \left[ 1 - (1 - \xi D_C)^{\beta+1} \right]^{\sigma_c} \left[ \sigma_m \left( 1 - \frac{\sigma_m}{\sigma_{\max}} \right) \left( 1 - \xi D_C \right) \right] dN$$ \hspace{1cm} (16)

Considered that the residual stress generated during laser additive manufacturing [37] adversely affects the fatigue performance of the coating. Therefore, the influence of residual stress is taken into account when calculating the average stress, as shown in Eq. (17):

$$\sigma_m = \sigma_{m0} + \sigma_{r,\max}$$ \hspace{1cm} (17)

where, $\sigma_{m0}$ is the average stress without residual stress; $\sigma_{r,\max}$ is the maximum residual stress. Considering the residual stress generated by laser processing, Eq. (18) can be improved as:

$$dD_{\xi C} = \left[ 1 - (1 - \xi D_C)^{\beta+1} \right]^{\sigma_c} \left[ \sigma_m \left( 1 - \frac{\sigma_m}{\sigma_{m0} + \sigma_{r,\max}} \right) \left( 1 - \xi D_C \right) \right] dN$$ \hspace{1cm} (18)

The thermal damage $D_{Ct}$ of coating caused by laser energy input is introduced, where $0 < D_{Ct} < 1$. Assuming that the initial damage state of the specimen is $D_C (N=0) = 0$ ($N=0$), the damage failure life of the specimen under fatigue load is defined as $N_f$, also $D_C (N=1) = 1$ ($N=N_f$), and the integral interval $D_{\xi C} \in (D_{Ct}, 1)$ is set, thus the fatigue life model of the specimen after...
According to Eq. (19), the expression of thermal damage $D_{Ct}$ is:

$$D_{Ct} = 1 - \left( 1 - A \right)^{\frac{1}{1+\beta}}$$  \hspace{1cm} (20)$$

where, $A = \left[ 1 - \left( 1 - \xi \right)^{1+\beta} \right]^{-\xi N_f (1-\alpha_c)(1+\beta)} \left\{ M_0 \frac{1-b\left(\sigma_{m0}+\sigma_{r,\max}\right)}{\sigma_a} \right\}^{-\beta}$.

The tensile and compression fatigue testing is carried out with axial symmetric loading (average stress $\sigma_m=0$ MPa) to determine the fatigue properties of Fe-Cr alloy coatings. The testing frequency is 2 Hz, and the three fatigue testings are conducted for each stress level (600 MPa, 550 MPa, 500 MPa, 450 MPa, 425 MPa and 400 MPa). The fatigue strength $S$-$N$ curve of Fe-Cr alloy is shown in Fig. 12 (a), and the fatigue limit is 378 MPa. According to Eq. (10) and Fig. 14(a), when average stress $\sigma_m=0$ MPa, the parameters $\beta=3.72$, $M_0=4832.6$ and $B = -0.0025$ can be obtained by calculation. The values of tensile strength and residual stress of the coating above are substituted into Eq. (20) to determine the change curve of the effects of laser energy densities on the initial thermal damage of Fe-Cr alloy coatings, as shown in Fig. 12(b) (testing stress 550 MPa) and Table 4, respectively. It can be seen that the laser energy can cause thermal damage to the mechanical properties of specimens. When the laser energy density is 39.79 J/mm$^2$ and 20.46 J/mm$^2$, the initial thermal damage reaches the maximum value of 0.472 and the minimum value of 0.421, respectively. The initial thermal damage has a strong correlation with the tensile properties, residual stress and fatigue strength of the coating, and shows a trend of decreasing first and then increasing with the increasing laser energy density. When the laser
energy density is 29.56 J/mm², the initial thermal damage of the coating is relatively small, and the mechanical properties are excellent.

Fig.12. Fatigue strength S-N curve (a) when \( E = 39.79 \text{ J/mm}^2 \), and the initial thermal damage curve (b) with different laser energy densities as the maximum stress is 550 MPa.

**Table 4** Initial thermal damage of Fe-Cr alloy claddings with different laser energy densities

<table>
<thead>
<tr>
<th>Laser energy density ( E ) (J/mm²)</th>
<th>( \sigma_{r_{\text{max}}} ) (MPa)</th>
<th>( \sigma_b ) (MPa)</th>
<th>( N_f )</th>
<th>( D_{C_{f1}} )</th>
</tr>
</thead>
<tbody>
<tr>
<td>20.46</td>
<td>156.3</td>
<td>1091</td>
<td>8992</td>
<td>0.421</td>
</tr>
<tr>
<td>25.01</td>
<td>163.4</td>
<td>1103</td>
<td>9133</td>
<td>0.426</td>
</tr>
<tr>
<td>29.56</td>
<td>188.7</td>
<td>1220</td>
<td>11332</td>
<td>0.423</td>
</tr>
<tr>
<td>35.01</td>
<td>226.0</td>
<td>1248</td>
<td>11896</td>
<td>0.445</td>
</tr>
<tr>
<td>39.79</td>
<td>233.6</td>
<td>996</td>
<td>9002</td>
<td>0.472</td>
</tr>
</tbody>
</table>

Two-stage stress fatigue experiments are performed to further verify the prediction accuracy of the nonlinear thermal-fatigue damage model of the Fe-Cr alloy coatings. The high and low stresses are 550 MPa and 450 MPa, and the loading times were 5000 times and 30000 times, respectively. The two-stage loading testings are carried out from high-to-low (550 MPa to 450 MPa) and low-to-high (450 MPa to 550 MPa), respectively, in which the first stage loading is the fixed times, and the second stage loading is applied until the specimen fractured. Table 5 exhibits the results of the two-stage fatigue experiments and the prediction values of the fatigue life model (Eq. (19)). The results show that the prediction results are unsatisfactory.
in accuracy, with the maximum error of 65.9%. To improve the prediction accuracy of the model, considered the influence of loading sequence on crack closure, the calculated damage of the specimen is defined as

\[ D_{Ca} = \frac{n_1 + n_2}{n_{f1} + n_{f2}} \]  \hspace{1cm} (21)

where \( D_{Ca} \) is calculated damage, \( n_1 \) and \( n_2 \) are the times of the first and second stage loadings, respectively, \( n_{f1} \) and \( n_{f2} \) are the times of specimen fracture corresponding to the first and second stage loadings, respectively. Meanwhile, the effective factor of crack propagation is modified as

\[ \xi_c = \frac{\xi}{D_{Ca}} \]  \hspace{1cm} (22)

where \( \xi_c \) is the effective factor of crack propagation considering the loading sequence. Therefore, the fatigue life model can be modified as shown in Eq. (23).

\[
N_{f2} = \frac{1}{\xi_c (1 - \alpha_c)(1 + \beta)} \left\{ \frac{M_0 \left[ 1 - b \left( \frac{\sigma_{m0} + \sigma_{rmax}}{\sigma_a} \right) \right] \beta}{\left[ 1 - \left( 1 - \xi_c \right)^{\beta + 1} \right]^{\gamma - \alpha} - \left[ 1 - (1 - D_{\xi_c}) \right]^{\alpha \gamma} \left[ 1 + \alpha \gamma \right]} \right\}
\]  \hspace{1cm} (23)

The prediction error of modified model decreased from 65.9% to 14.6% for the high-to-low (550 MPa to 450 MPa) loading, and the error decreased from 23.1% to 6.7% for the low-to-high (450 MPa to 550 MPa) loading. The model accuracy is significantly improved, which can be effectively applied to the prediction of thermal-fatigue damage and service life of the coating fabricated by DED technology.

**Table 5** Comparison between the fatigue experimental values and model predicted results

<table>
<thead>
<tr>
<th>Loading method</th>
<th>Second stage loading times ((\times 10^4))</th>
<th>Eq. (19) Error (%)</th>
<th>Eq. (23) Error (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>1</td>
<td>2</td>
<td>3</td>
</tr>
</tbody>
</table>

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## 4. Conclusion

(1) With the laser energy density increasing from 20.46 J/mm$^2$ to 39.79 J/mm$^2$, the tensile strength of Fe-Cr alloy coating increases first and then decreases. Concluded that grain size is the main factor affecting the tensile properties based on the fitting results of the Hall-Petch formula. The changing trend of Rockwell hardness is consistent with the tensile properties. The hardness of coating is related to the dendrite size coarsening, insufficient melting powder and poor densification caused by the variations of laser energy density.

(2) According to Vickers hardness indentation method, the residual stress at the turning point of the upper surface of coatings are pressure stress, while the middle zones are tensile stress. The wear rates of substrate and coating ($E=35.01$ J/mm$^2$) are $4.02 \times 10^{-2}$ mm$^3$·N$^{-1}$·mm$^{-1}$ and $1.91 \times 10^{-2}$ mm$^3$·N$^{-1}$·mm$^{-1}$, respectively, and the wear resistance is increased by two times, which is significantly improved.

(3) To evaluate the impact of thermal damage caused by laser energy and cumulative fatigue damage on coatings life, the prediction models of coating damage and life based on nonlinear thermal-fatigue damage are established. The prediction accuracy of the models are improved from 65.9% and 23.1% to 14.6% and 6.7% by introducing the effective crack propagation factor considering the loading order, respectively.

### Ethical Approval

Not applicable.

### Consent to Participate

Not applicable.

### Consent to Publish
Not applicable.

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Authors Contributions


Declaration of interest statement

The authors declared that they have no conflicts of interest to this work. We declare that we do not have any commercial or associative interest that represents a conflict of interest in connection with the work submitted.

References


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